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Preface

This, the 59th volume of the Transactions of the American Institute of Electrical Engineers, contains approved technical program papers and related discussion presented at the following AIEE conventions and District meetings:

1. North Eastern District meeting, Springfield, Mass., May 3–5, 1939: 14 papers and related discussion; 4 other approved papers and related discussion were published in the 1939 volume.
2. Combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939.
3. Great Lakes District meeting, Minneapolis, Minn., September 27–29, 1939.
4. Middle Eastern District meeting, Scranton, Pa., October 11–13, 1939.
5. 1940 winter convention, New York, N. Y., January 22–26.
6. 1940 summer convention, Swampscott, Mass., June 24–28: 24 papers and related discussion; remaining 17 approved papers and related discussion are scheduled for the 1941 volume.

In addition this volume contains:

1. A special color insert "Color, Light, and Structure at Golden Gate Exposition" reprinted from the June 1939 issue of Electrical Engineering to supplement a paper "Painting the Golden Gate International Exposition With Light."
2. A special article, with color insert, "Lighting the New York World's Fair."
3. 1940 annual report of the board of directors.

Under the present publication policy a large proportion of the content of this volume was given advance publication in the Transactions sections of the monthly issues of Electrical Engineering during 1940, and extra sheets were printed at the same time for binding in this volume. The papers and discussions so published comprise pages 1 to 682, inclusive; pages 683 to 1264, inclusive, did not appear in Electrical Engineering, but were given advance publication in the Supplement to Electrical Engineering—Transactions Section. All other material in this volume is reprinted from Electrical Engineering.

Full correlation of all material in this volume has been accomplished through the medium of the multientry reference index beginning on page 1327. A reference to any of the several subject entries for any technical paper will lead directly to the paper and to any published discussion on that paper.

Statements and opinions given in papers and discussions published in Transactions are the expressions of contributors for which the American Institute of Electrical Engineers assumes no responsibility.

Errata

Page 189, line 21, G. Coffin should be G. Goffin; equation 3, exponent of ϵ should be $-\frac{2\pi nt}{T_0}$.

The Characteristics and Power Requirements of Spinning Frames

E. A. UNTERSEE
NONMEMBER AIEE

THE POWER required to drive the many types of textile machines has always been an important problem confronting the manufacturer of all the textile fibers, especially the cotton-goods manufacturer, who leads all other branches of the industry.

When the majority of cotton mills were driven by steam or water power, this problem was not critical, but when the large groups of machines were broken down to individual drives and smaller groups driven by electric motors, it assumed greater importance.

The earlier designs of large motors replacing the steam engine or water wheel were extremely liberal, and the addition of a few extra machines on a large motor-driven group seldom worried the manufacturer or the motor. A greater knowledge of fundamentals and the more economical use of materials today permits the design of more efficient motors, very close to their name-plate ratings, which demands a more accurate knowledge of power requirements in applying the motors.

Advancements in the manufacture of fabrics and the breaking up of group drives into individual motor applications for greater flexibility in operation necessitates a much closer study of the power requirements of the individual machines.

The usual method of meeting these problems was to take electrical instruments to the mills and make power tests on the machines in question, recording all textile data at the same time. These data were compiled and served as a basis for determining power requirements in many other mills.

Complete segregation and analysis of the power requirements of various parts of the machines were seldom made.

Table I. Spinning-Frame Tests—Frame Data

Width—39 inches, length—51 feet
Number of spindles—360
Number operated during tests—348; others removed
Whorl diameter—$1^{1}/_{8}$ inches
Gauge spindle—$3^{1}/_{4}$ inches

Width tape—$^{5}/_{8}$ inch
Traverse—7 inches. With 2-inch and $2^{1}/_{4}$-inch ring traverse increased to $7^{3}/_{8}$ inches
Cylinder diameter—9 inches
Cylinder bearings—ball
Ring diameter—$1^{1}/_{2}$ inches, $1^{3}/_{4}$ inches, 2 inches, $2^{1}/_{4}$ inches

Type ring—number 2 FDA
Traveler weight and number—number 2 flange—5/0–$6^{1}/_{2}$ GR Bowen used on doffs 1–2–3. Doff number 5 used number 2 travelers. Doffs 6–7 use number 2 travelers. Two flange circle Al, style *RP*
Number yarn spun—33/1. Twist per inch—13
Doffs 5–6–7 20s yarn
Yarn spun—warp
Two-ply roving—3.50 hank

Type builder—combination set for warp
Thread board—traversing
Diameter front roll left side, 1 inch; diameter neck, 0.750 inch
Diameter front roll right side, 1 inch; diameter neck, 0.750 inch
Diameter middle roll left side, $^{7}/_{8}$ inch; diameter neck, 0.592 inch

Diameter middle roll right side, $^{7}/_{8}$ inch: diameter neck, 0.650 inch
Diameter back roll left side, $^{7}/_{8}$ inch; diameter neck, 0.592 inch
Diameter back roll right side, 1 inch; diameter neck, 0.750 inch
Tape idler pulleys, ball bearing $2^{3}/_{4}$ inches diameter
Weight on front rolls four pounds

Gearing:

Cylinder	26 teeth
Jack	112 teeth
Twist	44 teeth
Front roll	100 teeth
Lay	51 teeth
Crown	140 teeth

Top-roll gearing:

Back roll left side	33 teeth
Back roll right side	34 teeth
Middle roll left side	29 teeth
Middle roll right side	26 teeth
Intermediate each	40 teeth
Back roll (large) each	106 teeth

Top rolls cork—front and back

Weighting of top rolls:

Front = 22 pounds
Middle = 15 pounds
Back = 9 pounds

The power required to drive the machinery in the yarn spinning department usually represents approximately 50 per cent of the total power required for operating the mill, and power studies have usually been confined to this department because it has the greatest possibility of economic improvement.

In recent years many changes in spinning-machine designs have been made. Longer frames, larger spindles, bobbins capable of handling more yarn have been demanded by the textile manufacturer. These new designs and improved materials have made it possible to raise the operating speed, increase production, and reduce manufacturing and operating costs, necessitating more accurate power information.

The machinery manufacturers, the textile manufacturers, and the electric-motor manufacturers are vitally concerned with the problems of power requirements.

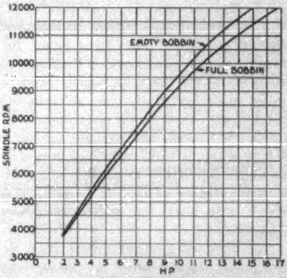

Figure 1. Spinning-frame tests—348 spindles, 1 1/2-inch-diameter ring

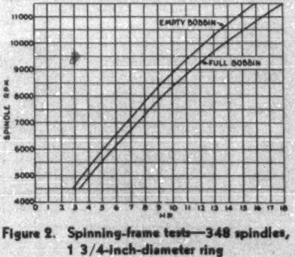

Figure 2. Spinning-frame tests—348 spindles, 1 3/4-inch-diameter ring

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Paper number 39-103, recommended by the AIEE committee on industrial power applications, and presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3–5, 1939. Manuscript submitted February 17, 1939; made available for preprinting March 30, 1939.

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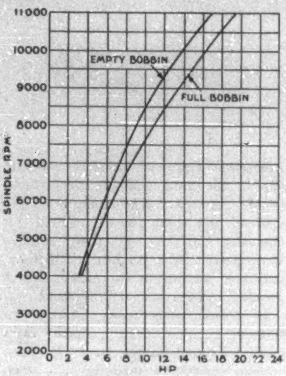

Figure 3. Spinning-frame tests—348 spindles, 2-inch-diameter ring

The machinery manufacturers are interested in producing the most efficient operating machines for the textile manufacturer. The textile manufacturer, in turn is interested in higher quality and higher production at minimum cost, while the motor manufacturer is interested in designing motors with characteristics which will suit the machine and meet all of the operating conditions.

The generally accepted methods of testing spinning frames have never disclosed power requirements of the different elements of the machine, which could be relied upon generally for all spinning machines. Tests made on a number of duplicate frames, all spinning the same size yarn, show wide variations in power; so much so, that it is necessary, for interchangeability of motors, to use ratings which will meet the maximum power conditions.

The lack of accurate information on the power requirements of the various elements of a spinning frame and the effect of changes in ring diameter, spindle speed, number of spindles, and different sizes of yarn on the power requirements has stressed the need for a careful analysis of the problem.

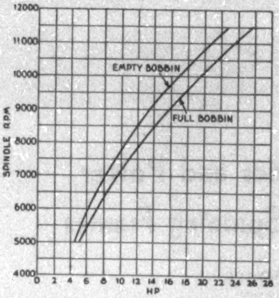

Figure 4. Spinning frame tests—348 spindles, 2 1/4-inch-diameter ring

With this in mind, extensive power tests were made to establish the characteristic requirements and derive an empirical formula, if possible, which can be relied upon by the machinery manufacturers and the textile manufacturers to determine the horsepower required to drive the various types of spinning machines.

A modern 348-spindle frame with long-draft spinning system was selected for making the tests. The data in table I will show all the mechanical features of the frame.

Tests were carried on throughout the spinning cycle starting with a $1^1/_2$-inch-diameter ring and a medium-size spindle, followed by similar tests after changing the size of the ring to $1^3/_4$ inches diameter. The yarn spun during these tests was an average of 30*s* yarn, this being what may be classified as medium-count yarn in a textile mill.

On the completion of these power tests, a series of breakdown tests were carried on to determine the separate power requirements of the various elements of the frame, such as the cylinder only, cylinder and spindles; cylinder, spindles and front rolls; cylinder, spindle, full bobbins builder and no rolls.

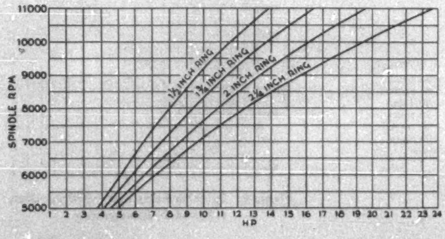

Figure 5. Spinning-frame tests — 348 spindles

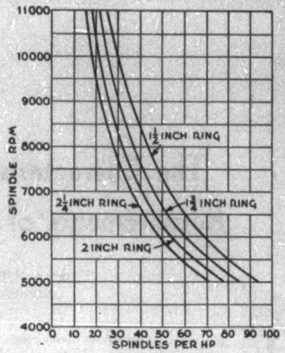

Figure 6. Spinning-frame tests—348 spindles

Following these tests a heavier type spindle was installed on the frame with a two-inch-diameter ring and power readings taken throughout the spinning cycle.

After this test, the 2-inch-diameter rings were replaced by $2^1/_4$-inch-diameter rings and similar tests conducted followed by a set of breakdown tests. The yarn

Table II. Spinning-Frame Tests—Breakdown Tests

Small Spindles

Condition	Horsepower	Spindle RPM	
Cylinder, bare spindles, no steel rolls and no builder	2.0	4,115	
	2.9	5,488	
	4.0	6,860	
	6.2	9,227	
	8.6	11,234	
	12.5	13,618	
Cylinder, bare spindles, *front steel rolls* and no builder	2.6	4,061	
	5.2	7,063	
	6.3	8,090	
	9.0	10,348	
	11.0	11,869	
	13.5	13,139	
Cylinder, spindles, empty bobbins, no steel rolls, no builder	2.1	4,202	
	3.1	5,785	Empty bobbins, no appreciable power
	4.7	7,463	
	6.3	9,408	
	7.7	10,427	
	12.5	13,312	
Cylinder, spindles, empty bobbins, builder, no steel rolls	1.9	3,880	
	3.3	5,558	Builder, no appreciable power
	4.5	6,930	
	5.9	8,820	
	7.7	10,286	
	8.7	11,038	
	12.0	13,077	
Cylinder, bare spindles, builder, no steel rolls	2.1	4,163	
	3.5	5,864	
	4.9	7,212	
	5.8	8,294	
	7.5	9,862	
	13.0	13,800	
Cylinder, spindles, full bobbins and builder motion, no steel rolls	3.3	4,547	
	4.1	5,688	
	5.1	6,758	
	7.4	8,247	
	10.5	10,129	
	17.0	12,900	

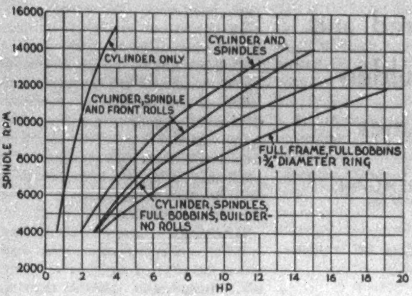

Figure 7. Spinning-frame tests—348 spindles, small spindles

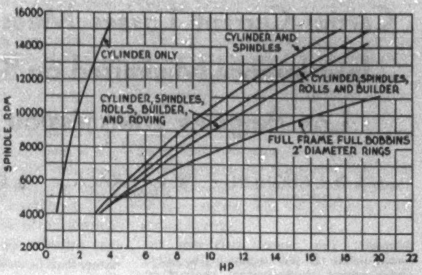

Figure 8. Spinning frame tests—348 spindles, large spindles

spun on the 2-inch and $2^1/_4$-inch rings was an average of 20*s* yarn.

During the spinning tests power readings were taken at the empty bobbin position at various spindle speeds and again at the full bobbin position with each size of ring.

The results of these power tests are shown on the accompanying curves for the four different ring diameters (figures 1, 2, 3, and 4).

As the most important data on these tests are the full-bobbin position, or maximum horsepower of the frame, the full-bobbin readings are plotted together and show very clearly the increase in power as the ring diameter increases, and also as the spindle speed is increased. It also shows that the ring diameter has a very important bearing on the power requirements of a spinning frame (figure 5). This is probably more clearly shown on the curve of spindles per horsepower when operating with different-diameter rings (figure 6).

The breakdown tests with either small or large spindles (figures 7 and 8) show definitely that the cylinder alone requires a very small percentage of the total power of the frame. The addition of spindles shows a very marked increase in the power required at various spindle speeds. The addition of the steel rolls does not materially increase the power. The addition of the full bobbins while not spinning shows a considerable increase in power which is due to windage and some small amount of friction. The full-power curve on these figures, which is the full-bobbin position while actually spinning yarn, shows a marked increase over the amount of power required by the cylinder, spindles, steel rolls, and full bobbins.

It is appreciated that the spindle speeds were increased beyond a point where practical spinning could be done, but this was necessary to establish the power requirements at high spindle speeds, and establish the characteristics of the frame.

The upper half of figure 9 is an analysis of the previous curves made and shows the characteristic increase in horsepower for various ring diameters plotted against the peripheral speed of the full bobbins in feet per minute. On this curve the solid lines are plotted from test data while the dotted lines are pro-

Table III. Spinning Frame Tests—Breakdown Tests

Large Spindles

Condition	Horse-power	Spindle RPM	
Cylinder only, no tapes	0.8	4,170	
	1.0	5,930	
	1.3	7,780	
	1.8	9,720	
	2.5	11,950	
	4.0	15,300	
Cylinder, bare spindles, no steel rolls, no builder	3.1	4,147	
	5.9	7,000	
	9.4	10,000	
	16.1	13,947	
	15.7	13,986	
Cylinder, bare spindles, no steel rolls, with builder	2.8	4,116	Builder, no appreciable power
	5.5	6,907	
	7.9	8,851	
	11.2	11,344	
	15.9	14,315	
Cylinder, bare spindles, front steel rolls, builder	3.3	4,061	
	5.5	6,287	
	9.3	9,023	
	13.9	11,908	
	16.6	13,429	
Cylinder, bare spindles, all steel rolls, builder	5.3	6,000	Back steel rolls, no appreciable power
	7.8	8,004	
	12.6	11,148	
	18.1	13,908	
Cylinder, bare spindles, all steel rolls, builder and roving	3.3	3,888	
	10.8	9,408	
	18.5	13,868	

Table IV. Spinning-Frame Tests—Analysis

Ring Size (Inches)		Revolutions per Minute for Peripheral Speeds of Feet per Minute								
		2,000	2,500	3,000	3,500	4,000	4,500	5,000	5,500	6,000
$1^1/_2$	Spindle speed	6,110	7,640	9,170	10,700	12,200	13,750	15,300	16,800	18,350
	Horsepower	5.25	7.5	10.0	13.2					
$1^3/_4$	Spindle speed	5,110	6,370	7,640	8,910	10,200	11,460	12,730	14,000	15,300
	Horsepower	4.25	6.45	8.75	11.22	14.25				
2	Spindle speed	4,370	5,460	6,560	7,650	8,750	9,840	10,290	12,000	13,100
	Horsepower	5.37	7.72	10.15	12.8	15.75	19.3			
$2^1/_4$	Spindle speed	3,490	4,500	5,400	6,300	7,200	8,100	9,000	9,890	10,800
	Horsepower			5.8	7.95	10.22	12.95	15.9	19.13	22.75

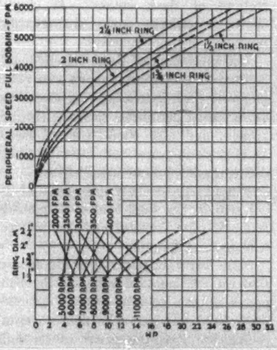

Figure 9. Spinning-frame tests—348 spindles, analysis

Carrier-Current Losses Measured and Interference Minimized on Boulder-Los Angeles Transmission Lines

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Synopsis: Two unusual facts were disclosed during the field testing of the supervisory-control carrier-current channel on the 287-kv Boulder Dam-Los Angeles transmission lines. Carrier-current losses were much higher than calculations and previous experience had indicated. The interference from power equipment arcs could not be isolated by the use of resonant choke coils, requiring special circuit designs for satisfactory operation.

Background

SUPERVISORY CONTROL for the operation of the circuit breakers, disconnecting and ground switches of the Boulder Dam-Los Angeles transmission lines uses a carrier-current channel. These are the highest voltage lines operating in the United States and there was, therefore, no definite information available on the losses to be encountered; nor was there much knowledge of the magnitude of interference that might be caused by static, corona, or arcs from power equipment. Field tests have been made which have brought to light some interesting facts. It is the purpose of this article to record these experiences so that future applications of a similar nature may be facilitated.

Interference

Line noise, lightning, and arcs from the operation of oil circuit breakers, disconnecting, and grounding switches create carrier-frequency waves on the lines which interfere with the reception of normal working carrier signals.

The interference due to line noise is ever present and changes over wide limits. Dry dust or sand-laden air blowing across the line produces some interference. Rain increases the interference level. Increase in corona and lightning during wet weather produces the maximum interference. To overcome line-noise interference on the 287-kv lines, it is necessary to have a carrier signal of ten milliwatts at the receiver, in order to provide a sufficient ratio of signal to interference for satisfactory operation. The receiver sensitivity can then be adjusted to give no receiver relay operations from interference. This minimum power value may be applied to lower-voltage lines also since the gradients are of the same order of magnitude.

Interference from disconnecting switches is very serious on long lines where the received signal is relatively low. Measurements indicated that the interference from a disconnecting switch opening the charging current of a 287-kv breaker bushing 180 miles away was approximately ten times that produced by maximum line noise. It was found that interference from the high-voltage lines was a maximum at a receiver when it was raining at the receiver location.

The resonant choke coils do not hinder the transmission of arc interference. The arcs of each phase go out every half cycle and re-establish. As the arc again breaks down, the steep wave front (practically square) produces a large band of frequencies which are not stopped by the resonant choke coils.

On opening a disconnecting switch, the arc continues for more than one second with gradually increasing interference. Its peak value occurs just before the arc is extinguished. The interference is greatest when the second disconnecting switch is opened on the end of an energized section since the capacity of the line raises the voltage at that point. The first disconnecting switch, in opening, only opens a parallel path with the last one and, therefore, results in opening a lower voltage arc. On closing a disconnect, the interference is of a lower level and shorter duration.

Oil circuit breakers when opening and closing any loads or line-charging current create the same magnitude of interference but last a very short time [two or three cycles (60)].

The interference from ground-switch operation is low in magnitude, being that of grounding the static charge stored on the de-energized line conductors. It is a maximum when grounding a section which is paralleling an energized line.

Effect of Interference on Signaling Codes

Practically all supervisory-control systems depend on a code of pulses for selection, indication, and operation. To dis-

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jections of these curves. It clearly shows a uniform power characteristic regardless of ring diameters.

The lower part of figure 9 (solid lines) is interesting because it shows a decrease in power with increasing ring diameters at the same peripheral speed of the full bobbin, and also shows (dotted lines) an increase in power with increasing ring diameters at the same spindle speeds.

It would appear that approximately 65 per cent of the total load of a spinning frame is pure friction, and the balance windage and spinning.

From test data taken, it would appear that the friction load varies as the 1.42 power of the spindle speed. On the other hand test curves in figure 5 seem to indicate that the power of the full frame while spinning varies as the 1.68 power of the speed with a $1\frac{1}{2}$-inch ring, the 1.765 power with a $1\frac{3}{4}$-inch ring, the 1.865 power with a 2-inch ring, and the 1.99 power with a $2\frac{1}{4}$-inch ring.

To use this, one must have some definite power reading on the frame in question, but since this is not always available it would be much more satisfactory to have a formula which is based on spindle speed, ring diameter, and a constant from which the power of any frame may be calculated. For example the formula:

Horsepower per spindle =

$$0.000284 \times R^{1.42}\left(\frac{\text{rpm}}{1{,}000}\right)^{1.\text{[illegible]}}$$

seems to hold fairly well for the test data thus far obtained, but it may be necessary to modify this formula or introduce another constant to make it apply to larger ring diameters and twisting operations.

tinguish one code from another, the number and timing of the impulses must be of a definite character.

The effect of arc interference on the codes varies with the type of operation and the device being operated. First, consider operations by the dispatcher. During the selection and check, no operations of power equipment occur and there is no interference. The dispatcher then initiates the operation control code to start the remote apparatus to close or open, as the case may be. The interference starts here. On closing or tripping a circuit breaker, there is a short arc of one to three cycles depending on the speed of the breaker. To the carrier equipment this represents a short pulse. The breaker auxiliary switches, which initiate the supervision indication, operate about the same time. Should the interference pulse be permitted to operate the receiver relay, an extra pulse would be added to the indication code and thus cause the supervisory control equipment to lock-out. It is then necessary for the dispatcher to reset the equipment before the new indication is reported.

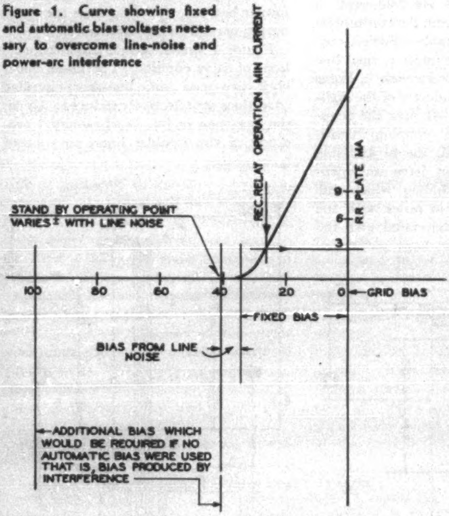

Figure 1. Curve showing fixed and automatic bias voltages necessary to overcome line-noise and power-arc interference

For disconnecting switches, the operation is different for opening and closing in the length of arcing time and the time when it occurs with respect to the supervisory code. In opening the switch, enough time elapses between the operation of the pallet switches which initiate the signal and the start of the arc to get the new signals properly registered. In closing, the arcing time is shorter and is complete before the pallet switches initiate the signal to indicate the position.

Automatic or manual operation of the equipment at the substation sends in a selecting code to the office, which is immediately followed by the indication code. With circuit breakers, the arcing is completed before the signalling begins, but may add onto the start of the selection code if the first pulse of the selection code occurs within 0.05 second of the arc pulse. In opening a disconnect from the dispatching office at Boulder, the signal is finished before the arc starts, while in closing the arcing is complete before the signalling begins.

It is advisable, therefore, to make the receivers inoperative when the arcs are occurring. This is done by rectifying the output of a separately tuned circuit (excited by components in the interference-signal of some frequency other than the frequency of carrier operation and preferably lower) and using it as a heavy negative bias on the receiver tubes. See figure 1. Thus, methods of overcoming the interference are (1) make the interference miss the time of signalling and (2) make the receiver less sensitive during the interference.

Carrier Transmission Losses

Probably the most interesting part of the field tests concerned the measurement and segregation of carrier transmission losses under various combinations of line sections in service.

In order to segregate the transmission losses into their component parts, it is advisable to divide them into their two main divisions. These are the primary or line circuits and the secondary or station losses.

Primary Circuits

High-voltage capacitors with a series tuning inductance and a coupling transformer are used to connect the carrier transmitters and receivers to the line. Paralleled tuned resonant choke coils are necessary in isolating the channel from spur lines and other paralleling circuits. These, with the line conductors are known as "primary" circuits and may be segregated into phase-ground, phase-phase, and phase-phase-ground connections.

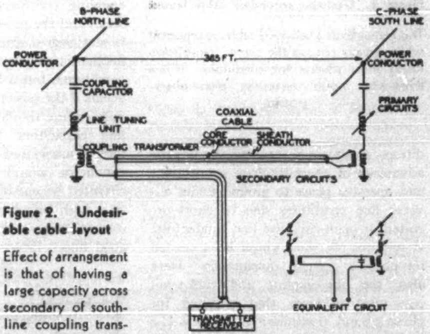

Figure 2. Undesirable cable layout

Effect of arrangement is that of having a large capacity across secondary of south-line coupling transformer. This is caused by the high capacity of sheath to ground of secondary cable, with abnormal line conditions, the resulting unbalance can create large carrier transmission losses

Phase-to-ground transmission requires, for the same reliability, the most power per unit distance. It is useful for short distances (30 miles or so) at low-power (15 to 30 watts) inputs. The connection has the advantage of using the minimum of coupling capacitors, line tuning units, and resonant choke coils.

Phase-to-phase transmission will give about two to over three times the power per unit distance as compared to the phase-ground connection. (See appendix where the ratio was 3.25.) It has the lowest losses for channels on single-tower lines. On double-tower lines where a phase wire of each circuit is used, the phase-to-phase loss may not be much less than phase to ground. The lower losses result in higher received carrier power. Also, the amount of coupling equipment is roughly twice that required for phase-to-ground connections.

Phase-to-phase-to-ground transmission is the most reliable. Under normal conditions, this type of transmission has the advantage of phase-to-phase connections and operates phase to ground under adverse line conditions due to short-circuited or open-circuited line conductors. It requires the same power level needed for phase-to-ground transmission. Here also, the line-coupling and choke-coil equipment is twice that required for phase-ground transmission. This is the system in use on the supervisory-control system controlling the various switching devices on the Boulder Dam-Los Angeles twin-circuit transmission lines.

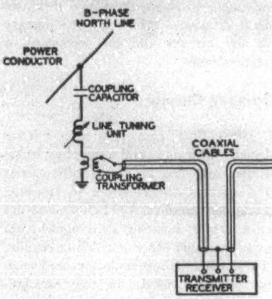
B-PHASE NORTH LINE
POWER CONDUCTOR
COUPLING CAPACITOR
LINE TUNING UNIT
COUPLING TRANSFORMER
COAXIAL CABLES
C-PHASE SOUTH LINE
TRANSMITTER RECEIVER

Figure 3. Desirable secondary cable layout

This layout gives a balanced cable arrangement which greatly reduces the carrier transmission losses under adverse line conditions. It is a three-wire system operating phase-phase-ground

Secondary Circuits

The secondary circuits consist of the secondary windings of the coupling transformers, cable circuits, and the primary windings of the transmitter output as well as the receiver input transformer. The voltage ratio of the coupling transformer is chosen to match the impedance characteristic of the high-frequency cable. Low-loss coaxial cable is available for such service and was used on the Boulder lines. The cable used has approximately 35 times the high-frequency losses of the phase-to-phase primary circuit channel of the same length.

Special attention must be paid to the type and arrangement of the connecting cable. It can be the source of tremendous losses under certain adverse line conditions if not properly connected. With the cable arrangement shown in figure 2, the secondary windings of the coupling transformers are connected in series and the power from the transmitter is sent over a single cable. Such a connection is satisfactory under normal line conditions, but a large increase in losses occurs if the power conductor of the high-voltage line should break near the coupling capacitors. The coupling transformer associated with the broken line conductor would then have an open-circuited secondary winding, which acts as a high impedance in series with the transformer winding associated with the good line. Hence, the large portion of the transmitted voltage appears across the transformer where it can do no good. The Boulder line tests showed that, with the line broken, the loss is 14 decibels greater than under normal conditions with the average distances as shown. The cross-tie portion of the cable also may approach parallel resonance (see equivalent circuit of figure 2) with the inductance of the coupling transformer. Resistors could be used to parallel the coupling-transformer primary-circuit winding and thus reduce the impedance of the open-circuited coupling transformer but this would increase the losses during normal line conditions.

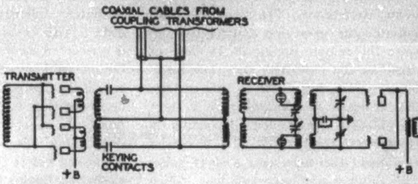
COAXIAL CABLES FROM COUPLING TRANSFORMERS
TRANSMITTER
RECEIVER
KEYING CONTACTS
+B
+B

Figure 4. Schematic arrangement of connections of the secondary coaxial cables to the transmitter and receiver circuits

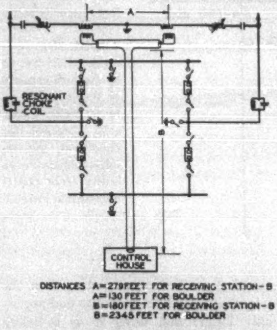
A
RESONANT CHOKE COIL
B
CONTROL HOUSE
DISTANCES A=279 FEET FOR RECEIVING STATION-B
A=130 FEET FOR BOULDER
B=180 FEET FOR RECEIVING STATION-B
B=2345 FEET FOR BOULDER

Figure 5. Boulder-Los Angeles lines—terminal stations

Layout of stations—single-line diagram. Shows lengths of high-frequency cable necessary to connect indoor transmitter-receivers to lines

Figure 3 shows the desirable connections of cable circuits. The losses under both line-open and line-short-circuited conditions are effectively reduced. Under the line-open or line-short-circuited condition of the Boulder lines, the output

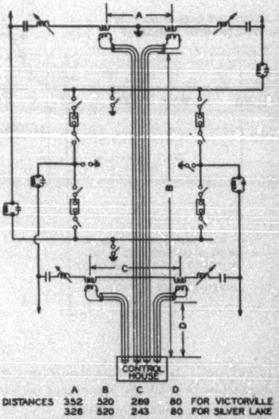
A
B
C
D
CONTROL HOUSE
A B C D
DISTANCES 352 520 269 80 FOR VICTORVILLE
326 520 243 80 FOR SILVER LAKE

Figure 6. Boulder-Los Angeles lines—intermediate switching stations—layout of stations

Single-line diagram shows lengths of high-frequency cable necessary to connect indoor transmitter-receivers to lines power is reduced only one-half, representing an additional loss of 3 decibels instead of 14 measured with the cable arrangement of figure 2. Each line of the double phase-ground transmission circuit (see figure 4) is independent of the other except for slight changes in the regulation of the transmitter, since each half of the circuit operates independent of the other but from a common power supply. The input circuits into the receiver are also divided into two transformers and input tubes, so that changes on one line will not be seriously reflected into the other or affect the tuning on the working half.

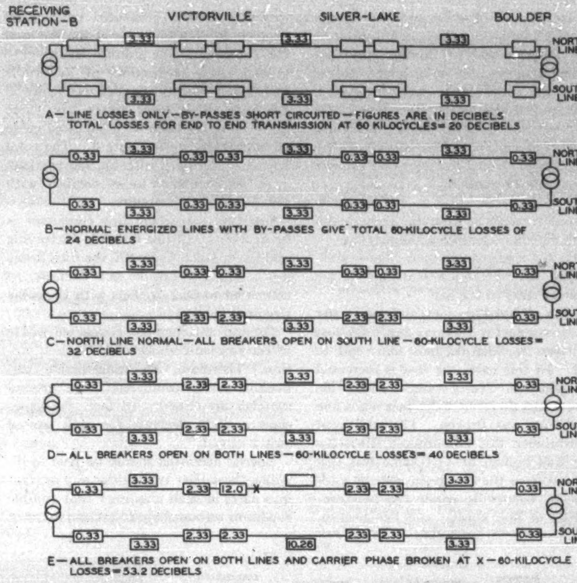
RECEIVING STATION-B
VICTORVILLE
SILVER-LAKE
BOULDER
NORTH LINE
SOUTH LINE
3.33
0.33
2.33
12.0
10.26
X
A—LINE LOSSES ONLY—BY-PASSES SHORT CIRCUITED—FIGURES ARE IN DECIBELS TOTAL LOSSES FOR END TO END TRANSMISSION AT 60 KILOCYCLES=20 DECIBELS
B—NORMAL ENERGIZED LINES WITH BY-PASSES GIVE TOTAL 60-KILOCYCLE LOSSES OF 24 DECIBELS
C—NORTH LINE NORMAL—ALL BREAKERS OPEN ON SOUTH LINE—60-KILOCYCLE LOSSES=32 DECIBELS
D—ALL BREAKERS OPEN ON BOTH LINES—60-KILOCYCLE LOSSES=40 DECIBELS
E—ALL BREAKERS OPEN ON BOTH LINES AND CARRIER PHASE BROKEN AT X—60-KILOCYCLE LOSSES=53.2 DECIBELS

Figure 8. Schematic layout of Boulder-Los Angeles transmission lines showing approximate distribution of carrier-current transmission losses under various line connections

The Boulder Line Characteristics

In measuring the carrier transmission losses on the lines using cable connections of figure 2 at the intermediate stations and of figure 3 at the end stations, some interesting loss characteristics were recorded. (See figures 5, 6, 7, and 8.) First, with the high-voltage lines continuous throughout but with no by-passes in the circuit, the loss at 60 kilocycles was 20 decibels. This loss was measured from end to end and the value 3.33 decibels in figure 8*A* was obtained by dividing the total loss by six, it being assumed that the loss in each section is the same. The lines are approximately 270 miles in length and the calculated loss is only 4.43 decibels. This gives a ratio of measured to calculated losses of approximately five to one (see appendix). Previous tests reported[1] on lower-voltage single-tower systems have not greatly exceeded a 2.5 to 1 ratio of actual to calculated losses. The additional loss may be due to the large spacing between power phases (approximately 265 feet) as compared to distances of the phase conductors to ground and the overhead ground wires. The distance is great because the carrier channel uses the *B* phase of the north line and *C* phase of the south line.

With the by-passes connected in the circuit and the lines energized throughout, the over-all carrier transmission loss increased to 24 decibels. Since the line measured 20 decibels the additional loss of 4 decibels divided by 12, the number of coupling units, gives 0.33 decibels as the equivalent loss per coupling unit. See figure 8*B*. The loss per by-pass was calculated to be 3 decibels. The difference is due to coupling to the other unchoked phases, which forms a parallel circuit to the by-pass equipment. (See figure 9.)

As circuit breakers are opened, the parallel paths through the other unchoked phases are interrupted and the over-all losses increase. The unused phase wires would not affect transmission if the current in them were zero. The transmission line transpositions are ineffective at 60 kilocycles because of the unbalanced loadings. An unbalance occurs because the terminating impedances to ground on the unused phases are not the same at 60 kilocycles since choke coils used in these phases are tuned to different frequencies. This unbalance makes the by-pass losses lower (see figure 8*C*) on the closed line side than on the open breaker side where all transmission is through the by-pass equipment. Where a phase wire of each line is used, the opening of one line only increases the loss to 32 decibels. Dividing this increase of 8 decibels among the four south line by-passes affected gives 2.33 decibels total loss per coupling unit The total loss per by-pass (four coupling units) is now increased to 5.33 decibels

When breakers of both lines are opened, both sides of the transmission channel are affected, and the loss increases by another 8 decibels. This additional loss is divided by four and the result added to

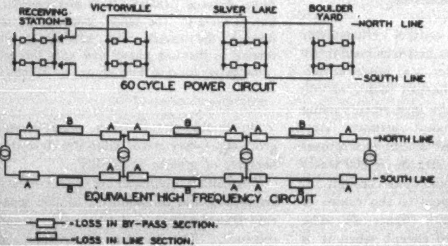
RECEIVING STATION-B
VICTORVILLE
SILVER LAKE
BOULDER YARD
NORTH LINE
SOUTH LINE
60 CYCLE POWER CIRCUIT
A
B
EQUIVALENT HIGH FREQUENCY CIRCUIT
=LOSS IN BY-PASS SECTION.
=LOSS IN LINE SECTION.

Figure 7. Single-line power and high-frequency circuits—Boulder-Los Angeles transmission line

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[1] For all numbered references, see list at end of paper.

by-passes at Victorville and Silver Lake. Tests showed that changes in output at Los Angeles and at Boulder were not changed appreciably by opening breakers at these points. The total loss at a switching station is 9.32 (four times 2.33) with all line breakers open. With one breaker opened at either switching station, an additional loss of 4 decibels was measured. Figure 8*D* gives losses for lines when all carrier power is transmitted through all coupling devices and cable circuits connecting these devices.

These changes do not appreciably change the impedance matching of the cable circuits to the line.

With all breakers open and one carrier phase conductor broken, the total loss increases to 53.2 decibels from end to end. In this case, the loss is increased 13.2 decibels. Tests made at Victorville gave a loss there of 12 decibels when line conductor was broken. The additional transmission loss encountered when the line was broken at Victorville was calculated from the change in voltage readings at Victorville under the two conditions of line normal and line broken. (This is shown in figure 8*E*.) Neglecting small increase in losses at Silver Lake, (measurements showed that opening line at Victorville does not appreciably change losses at Silver Lake) the line-to-ground loss for the 90 miles from Victorville to Silver Lake is 10.25 decibels. The loss per mile phase to ground is 0.114 decibel. Of this value 0.037 decibel (based on measured value of 20 decibels for 270 miles) is in the line and 0.077 decibel in the ground circuit.

With cable connections at Victorville and Silver Lake unbalanced (see figure 2) the over-all by-pass losses are three decibels higher than with balanced cable circuits as shown in figure 3. The total loss is 37 decibels, with all line breakers open, as compared to 40 decibels with unbalanced cable circuits.

For the case where line conductor is broken on north line between Victorville and Silver Lake, figure 8*E*, the total losses are 5 decibels less or 48.2 decibels, as compared to 53.2 decibels with the cable circuits unbalanced.

Ground switches are connected inside of the resonant choke coils at each station. Therefore, no transmission difficulties are encountered when ground switches are closed. In fact, the losses show a slight decrease for each ground switch closed.

Special attention should be paid to dividing the carrier transmitter and receiver into parts in such a manner that trouble condition on one phase-to-ground primary circuit will not materially affect the operation of the other half.

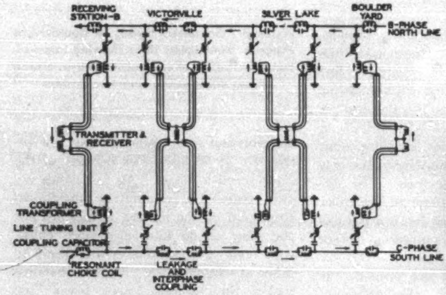

Figure 9. Carrier-current circuit

Primary and secondary arrows show instantaneous current values throughout system. Proper phasing is necessary to get leakage and interphase coupling current to add in phase with current through by-pass

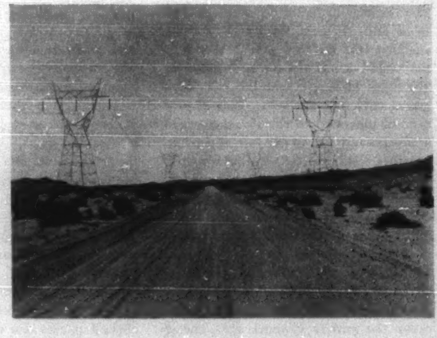

Figure 10. View of double tower construction used for approximately 78 per cent of line length

Note distance between lines and construction of towers. The center conductor passes through the tower. The conductors are rotated so that one phase wire goes through the tower for one-third of the line length

The Carrier Transmitter

The supervisory-control transmitter on the Boulder lines consists essentially of an oscillator, buffer, and power amplifier with a normal output rating of 400 watts. To obtain the best results using the phase-phase-ground primary connections, the amplifier circuit is arranged with two push-pull outputs of 200 watts each. These are connected through independent transformers to the cables of the secondary circuits (figure 4). The plate current is zero except when it is being keyed by the supervisory control equipment.

The Receiver

A heterodyne receiver is used with a beat frequency of 600 cycles or more from zero beat. The input, for double-circuit reception, consists of two input transformers (see figure 4) with series tuned circuits on the secondary side and parallel tuned circuits on grids of the input tubes. The coupling between the two sides of the receiver input circuit is low. Thus grounds or open line conductors do not change the tuning on the unfaulted side of the circuit.

Conclusions

To overcome line noise, the minimum power received for satisfactory operation at any point should be ten milliwatts above line noise level.

For greater interferences, such as that caused by arcs, more received power is necessary or special means are required to prevent altering of coded signals.

On double-circuit lines, phase-phase-ground primary connections are desirable because of greater reliability.

The cable arrangement of the secondary circuits should be balanced to the separate transformers of the transmitter and receiver. This is the equivalent of two independent phase-ground systems.

High-frequency losses over the 287-kv Boulder-Los Angeles power lines measured approximately five times those calculated, using the present accepted methods for such calculations (see appendix). Previously reported tests gave a maximum of approximately 2.5 times. It is apparent that changes in the present working formulas will be necessary.

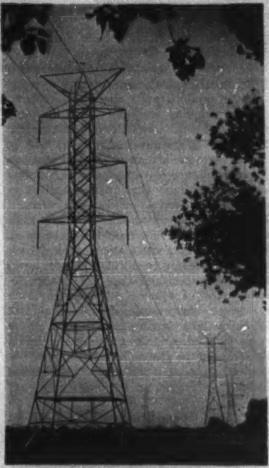

Figure 11. View of single-tower construction used for approximately 22 per cent of line length

The phase wires of the two lines are relatively close with none passing through the towers

Appendix

Calculations of Line Losses

The high-frequency resistance for tubular conductors as given in Bureau of Standards Circular No. 74, page 301 is

$$\frac{R_{ac}}{R_{dc}}=10\sqrt{2}t(0.01071\sqrt{f})=0.1514t\sqrt{f} \qquad (1)$$

where

t = conductor wall thickness in centimeters
f = frequency in cycles per second
$\frac{R_{ac}}{R_{dc}}$ = ratio of a-c to d-c resistance

At 60 kilocycles, and wall thickness of 0.278 centimeters.

$$\frac{R_{ac}}{R_{dc}}=0.1514\times0.278\sqrt{60{,}000}=10.32$$

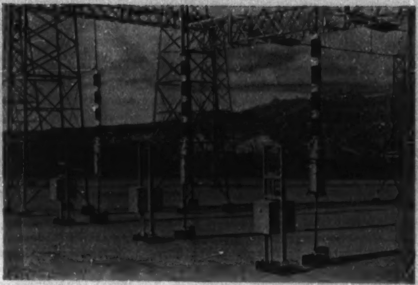

Figure 12. One corner of an intermediate switchyard showing outdoor line-tuning units. Underground coaxial cables connect the carrier circuits to the indoor transmitter-receiver sets

The d-c resistance of conductor (512,000 circular mils) is 0.22 ohms per loop mile. Hence the 60-kilocycle resistance for conductor is

$$R_{ac}=10.32\times0.22=2.27 \text{ ohms per loop mile}$$

The approximate formula for the real part of the attenuation constant is as follows:

$$a=\frac{R}{2Z_0}+\frac{CZ_0}{2} \qquad (2)$$

for the real part of the propagation constant where

R = resistance in ohms
G = leakage conductance in ohms
Z_1 = surge impedance = 600 ohms phase to phase
a = attenuation constant

For high-voltage open-wire lines $G=0$ and (2) reduces to

$$a=\frac{R}{2Z_0} \qquad (3)$$

In terms of current, for long lines

$$I_x=I_0\epsilon^{-ax}$$

$$\text{DB}=10\log_{10}\frac{I_0^2}{I_0^2\epsilon^{-2ax}}=10\log_{10}\epsilon^{2ax}$$

$$=20ax(0.4343)=8.686ax$$

Substituting from (3)

$$\text{DB}=\frac{8.686R}{2Z_0}=\frac{4.343R}{Z_0}\text{ per unit length} \qquad (4)$$

In this case the a-c resistance is 2.27 ohms per loop mile.

$$\text{DB}=\frac{4.343\times2.27}{600}=0.0164 \text{ per loop mile}$$

For 270 miles of line, the loss as calculated is 4.43 decibels.

When transmitting phase to ground, the calculated losses are as follows:

The ground circuit can be along a copper circuit consisting of two quarter-inch-diameter copper wires for which the 60,000-cycle resistance is 2.55 ohms per mile as calculated by Bureau of Standards formulas. For this value of resistance the loss is

$$\text{DB}=\frac{4.343\times2.55}{300}=0.037 \text{ per mile}$$

In this case $Z_0=300$ ohms as measured. The total line-to-ground transmission loss is $0.0164+0.037=0.0534$ decibels per mile.

References

1. C. A. Boddie. IRE *Transactions*, June 1927.

2. A Self-Checking System of Supervisory Control, M. E. Reagan. AIEE Transactions, volume 57, 1938, pages 600–05 (October section).

3. Symposium on Operation of the Boulder Dam Transmission Line—Carrier Current Equipment, J. D. Laughlin. AIEE Transactions, volume 58, 1939, pages 147–51 (April section).

Discussion

W. A. Lewis (Cornell University, Ithaca, N. Y.): There are two points in connection with this paper that I would like to augment.

The discussion points out the serious effects on the operation of the carrier-current supervisory equipment caused by arcing at the opening and closing of disconnecting switches and circuit breakers. Those engineers who are concerned with the operation of carrier-current relaying equipment may have some fears regarding the effects of similar arcing on the performance of carrier-current relaying equipment. However, the large number of satisfactory installations of carrier-current relaying suggests that such fears are not warranted. Tests made a number of years ago perhaps indicate why this is so. It was found at that time in tests made at low voltage that a flaming arc in air did not produce appreciable amounts of high-frequency transients in the carrier-frequency range, whereas spark discharges, as are obtained by the opening of disconnecting switches which open only a small amount of charging current, and also arcs in circuit breakers which are very quickly extinguished both produce large amounts of high-frequency energy which may operate the carrier-current receivers of the relaying equipment. Fortunately, the necessity for the receiver responding only to the carrier-current energy transmitted by the transmitter at the opposite end of the line is only critical for the duration of the fault, and after proper circuit breakers have been selected, and have started to open, it is unimportant that the carrier-current receiver may be momentarily energized. In the event that circuit breakers or disconnecting switches are opening at the moment a fault occurs on an adjacent line section, it is conceivable that the carrier energy from the operation of the circuit breaker or disconnecting switch may interfere with the relay operation, but in any event it is probable that the most serious effect will be a short delay in the operation of the line relays.

The method of computing the transmission loss is described in the paper, and it may be seen that the only losses considered are the high-frequency copper losses in the copper conductors actually involved in the circuit. From the tests, it is evident that there are additional losses which are not taken into account by this method of calculation. From the physical standpoint the supervisory-control circuit consists of a single-turn loop, one side of which is the *B*-phase conductor of the north line, and the other side is the *C*-phase conductor of the south line, separated by 365 feet from the other side of the loop. It may readily be visualized that this loop forms the primary circuit of an air-core transformer, the secondary of which consists of eddy-current paths in the earth immediately below the carrier transmission circuit. It is to be expected that eddy currents would be produced in the earth, and their losses would appear as transmission losses to the carrier-current circuit. Also, there will be some radiation, and some current induced in the other neighboring conductors. All of these losses will add to the conductor copper loss in the carrier channel. For the calculation of ground-return circuits at power frequencies, the formulas developed by J. R. Carson, or some modification of them, have been used with considerable success. Investigation of the underlying premises indicates that theoretically, at least, these formulas are applicable to the carrier frequencies in the range covered by this installation. It would be extremely interesting, therefore, to calculate the losses of this circuit by means of the Carson formulas, and determine if these formulas give an adequate representation of the losses. In any event it is to be expected that such calculations would give a much closer approach than is obtained when only the conductor copper losses are included.

Fred B. Doolittle (Southern California Edison Company, Los Angeles): Appreciation is due the authors for making available the information contained in their paper entitled "Carrier Current Losses Measured and Interference Minimized on Boulder-Los Angeles Transmission Lines". It is to be hoped that further study will reveal the reason for the five-to-one ratio between measured and calculated transmission losses.

While the secondary circuits used by the authors were different than those used by the Southern California Edison Company on the application of carrier-current protection to the 96-mile Gould-Magunden 220-kv transmission line and while the line construction is also different it would hardly seem that these factors could account entirely for the discrepancy which appears to exist between phase-phase and phase-ground transmission losses on the two lines. The received power phase to phase being 3.25 times that phase to ground as reported for the Boulder line is a much greater difference than found on the Gould-Magunden line. Tests on the latter in 1932 indicated phase-to-ground transmission was about 70 per cent as efficient as phase-to-phase.

The ingenious scheme described in the paper for blocking the receiver during the occurrence of high-voltage switching interference is of course well suited to the purpose for which it is used. However, it should not be used where the carrier signal is for the purpose of preventing the operation of tripping relays for transmission line protection. When, in 1932, it was observed that switching arcs on the Edison 220-kv system produced interference of sufficient magnitude to flashover all of the protective gaps associated with the carrier-current coupling equipment it was first thought that it would be impracticable to use sufficient carrier-current power to override interference of such magnitude. However, further experimentation demonstrated that such was not the case and that a $7^1/_2$-watt carrier-current transmitter over a 30-mile line could produce three or four times as much average detector plate current in a properly biased receiver as was produced by the interference created by operation of a 220-kv disconnecting switch. This fact made possible the interlocking type of carrier-current protection which was placed in service on the East Gould-Laguna Bell 220-kv line in April 1933.

While use was made of the fact that comparatively small amounts of carrier power would override violent interference, the reason therefor was not definitely proved until some months later when, by means of a cathode-ray oscilloscope, the wave form of the detector grid voltage was viewed. This wave form under switching interference was that of a damped oscillation in the resonant circuit of the receiver produced by shock excitation, twice each cycle of the power frequency when the instantaneous power voltage reached sufficient magnitude to jump the switch gap. The maximum magnitude of the damped oscillation could be controlled by the setting of the protective gaps in the carrier-current coupling circuit. Of course the continuous wave received from the remote transmitter was not damped and produced a higher average detector grid voltage than the series of damped oscillations caused by the interference.

J. D. Laughlin, W. E. Pakala, and M. E. Reagan: Doctor W. A. Lewis points out that experience has shown that interference from breakers and disconnecting switches does not produce faulty operation on carrier-current relaying systems. It is fortunate that interference power level in the carrier-frequency spectrum is sufficiently low to enable us to use less than 50 watts of power for carrier-current relaying and receiver sensitivities so low that interference has no effect on the receiver relays. Interference, however, should be considered, and too low carrier power levels should not be used. For carrier loss calculations it has been general procedure to calculate copper losses as in the paper, and then use factors obtained by experience on high-frequency transmission over power lines to obtain line losses for the design of equipment. We have made calculations taking into consideration the losses in the ground circuit and have obtained loss value of 30.4 decibels (line only) for earth resistivity of ten ohms per meter cube.

Fred B. Doolittle has brought up a point which was not made sufficiently clear in the paper. One phase-to-ground loss was measured as 26 decibels and the phase-to-phase loss as 20 decibels. The statement made in the paper that the phase-to-ground loss is 3.25 times the phase-to-phase loss is based on a calculated value, assuming that all ground current is concentrated in the counterpoise wires.

The effect of interference from switching on the detector plate meter and receiver relay is not, as pointed out by Mr. Doolittle, as serious as would be expected from observation of breakdown of the protective gap. High crest voltages of only a few microseconds duration have no effect on the receiver relay, which is relatively a low device connected in series with a vacuum tube which can act as a current-limiting device. However, interference of long duration, such as from the 287-kv disconnecting switches, will pick up the receiver relays unless some means is provided to desensitize the receiver during interference time. The system described in the paper requires carrier transmission for 270 miles at the time interference is transmitted for much shorter distances.

Hydrogen-Cooled Turbine Generators

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ONE of the most radical innovations in the design of electrical machinery in recent years has been the adoption of hydrogen cooling for large turbine generators. Now that a number of the large hydrogen-cooled units are in operation, it would seem timely to review the experience gained in building and operating this new-type equipment.

The developments leading up to the design of the present large machines may be summarized briefly as follows. In 1923, Max Schuler of Germany took out an American patent covering the cooling of rotating electrical machines with hydrogen gas. The company with which the writers are associated took out a license under this patent in 1924 and began development work on a suitable sealing device to prevent escape of gas along the shafts of the machine where they come out of the gas-tight housing. The shaft sealing glands, as they shall be referred to hereafter, were developed in essentially their present form in 1926. Work was then started on a 7,500-kva 3,600-rpm turbine generator with hydrogen cooling, and this was built and tested in 1928. A second machine of the same size was built and tested in 1930. The results of these tests are covered in a previous paper by one of the authors.[6]

During the years 1930 to 1936, no commercial applications of this new method of ventilating machines were made largely because of the small number of machines sold during that time. Early in 1936, there developed a considerable demand for large 3,600-rpm high-pressure topping units to be used to modernize old stations. The increased output from a given size machine with hydrogen cooling permitted units to be built in ratings up to 50,000 kw with the materials and experience then available. Since 1936, over 580,000 kva of hydrogen-cooled turbine generators have been sold representing 11 units, the largest of which is rated 81,250 kva at 80 per cent power factor at 3,600 rpm. One unit rated 75,000 kva at 80 per cent power factor at 1,800 rpm has been in operation for some time. All the other units are designed for a speed of 3,600 rpm. The smallest unit is rated 37,500 kva at 80 per cent power factor at 3,600 rpm.

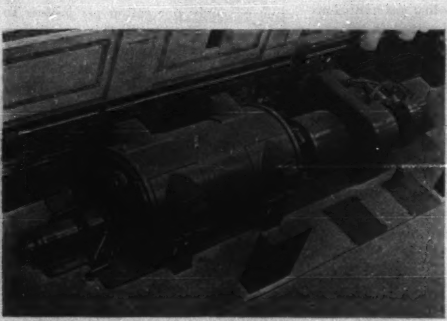

Figure 1. A 53,000-kw hydrogen-cooled generator and turbine installed at Waterside station, New York City

Advantages of Hydrogen as a Cooling Medium

The advantages of hydrogen cooling have been covered fully in a number of papers. It is sufficient, therefore, only to state briefly the principal points here, as follows:

1. Reduced windage, friction, and ventilating losses, because of the low density of the hydrogen gas. The ventilating losses are proportional to the gas density. Full load efficiency of the generator may be 0.6 per cent or more higher than for the corresponding air-cooled machine.

2. Increased output per unit volume of active material, because of the high heat storage capacity and thermal conductivity, and heat transfer coefficients of hydrogen. This advantage of hydrogen cooling makes it possible to build turbine generators for higher ratings than are possible with air cooling.

3. Reduced maintenance expense, because of the freedom from dirt and moisture.

4. Increased life of the insulation on the stator winding, because of the absence of oxygen and moisture in the presence of corona.

5. Reduced windage noise, because of the low density of the gas.

Description of Hydrogen-Cooled Generator

Hydrogen-cooled generators are designed so as to be "explosion-safe"; that is, all equipment is built to withstand a hydrogen explosion without damaging life or property. During normal operating procedure, an explosive mixture of hydrogen and air will not exist in the generator; however, in order to take care of faulty operation or unforeseen conditions, a suitable control and signal system is provided to warn when operation is approaching dangerous conditions. The operating hazard of a hydrogen-cooled generator is no greater than that of an air-cooled machine. Hydrogen is usually associated with dangerous explosions, but it is important to note that oxygen is required in considerable quantity before hydrogen will burn; explosive mixtures of air and hydrogen contain from 5 to 70 per cent hydrogen by volume, various authorities differing a small amount on these figures. The purity of the hydrogen in the generator is maintained above 95 per cent hydrogen, well above the upper explosive limit. The operating procedure together with the control system make a hydrogen explosion a remote possibility.

The construction features of a hydrogen-cooled generator are shown in figures 2 and 3. The generator frame and end domes are fabricated from steel plate which is rolled and welded to form the required shapes; the generator shell being cylindrical and the end heads being dome shaped as these shapes will best withstand the high internal pressure which would result from an explosion. On the basis of the most favorable mixture for an explosion, the pressure developed inside the machine enclosure

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M. D. Ross is section engineer, a-c generator section, and C. C. Sterrett is electrical design engineer, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

6. For all numbered references, see list at end of paper.

would not exceed one-half the maximum theoretical value, because of the large heat-storage capacity of the machine and the exposed surfaces of the large mass of the parts of the machine. Analysis and experience indicate that the maximum actual explosion pressure will not exceed 85 pounds per square inch; consequently, it has become accepted practice to design the housings of hydrogen-cooled machines to withstand an internal pressure of 100 pounds per square inch without failure. Frame welds are tested individually for gas leaks and the complete generator frame and end domes are tested for leaks with water pressure at 75 pounds per square inch. The joints in the generator frame and end head assembly which must be gas-tight are made with suitable gaskets and cement.

Armature laminations are built into the frame after the water pressure test. The stator laminations are varnished and baked, and then the stator coils are installed in the stator slots, the coils being transposed and stranded to reduce eddy currents to a minimum. The generator main leads are brought out through gas-tight capacitor-type bushings which are bolted to the generator frame. Nine armature temperature detectors are located in the armature windings, and four temperature detectors are located in the gas passages, one at the inlet and outlet of each gas cooler.

The generator rotor is made from a single forging in which slots are machined to receive the rotor winding. Ventilating passages are provided under the coil slots and in the end windings to secure efficient ventilation of the rotor. The rotor end turns are supported by coil retaining rings which are shrunk on the rotor body.

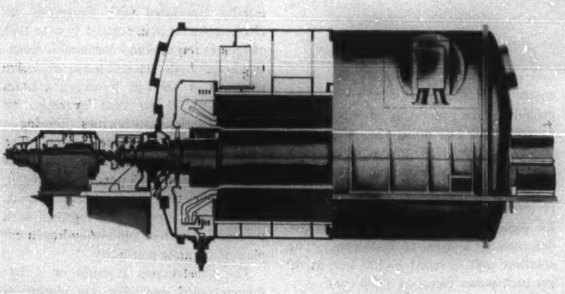


Figure 2. Longitudinal section view of a 53,000-kw hydrogen-cooled generator with direct-connected exciter

Hydrogen-cooled generators are equipped with finned-tube-type gas coolers, located above or below the stator core; in either case, the gas coolers are located so that the tubes run crosswise in the generator frame. The nozzle end of the gas cooler is bolted solidly to the generator frame, while the rear end is permitted to move freely with temperature changes. The finned tubes are supported so as to eliminate vibration. The cooler tubes may be cleaned without losing gas from the generator housing by removing the nozzle and rear end water boxes, a flexible rubber diaphragm on the rear end between the generator frame and cooler serving to keep the gas in the generator. The gas coolers are installed so as to be readily accessible, and so as not to weaken the generator cylindrical shell.

The hydrogen-gas ventilating circuit is shown in figure 2. A propeller-type blower, consisting of a steel hub with aluminum blades, is mounted on each end of the rotor, and these circulate the hydrogen through the rotor and stator, where it picks up heat, and then through the finned-tube gas coolers, where the heat is transferred to the cooling water in the tubes. The cooling gas may complete 30 to 40 such circuits in a minute. The interchange of cooling gas between the two ends of the generator is very small, so that both coolers are required when the generator is running. Hydrogen-cooled generators are designed to carry at least 75 per cent rated load, with normal temperature rises, running air cooled.

Manholes in the end domes, and portlights on the side of the generator are provided so as to permit observation of the internal machine parts without disassembling the generator end domes.

The generator tool-steel collector rings, spiral grooved to improve commutation, are located outside the generator bearings. The main and pilot exciters are driven at the same speed as the generator rotor, and have one or two bearings, depending on the size of the exciter. Two-bearing exciters are flexibly connected to the generator rotor. The special floating-type commutator on the exciter provides satisfactory operation at 3,600 rpm.

Description of Generator Shaft Gland Seals and Oil System

A hydrogen-cooled generator requires shaft gland seals on each end of the generator rotor to keep the hydrogen gas in and the air out of the generator housing. The shaft gland seal details are shown in figure 4. The special feature of the shaft gland seal is the sealing rings which are located in grooves in the seal member and are fitted close to the shaft and are free to move with the shaft radially, but are pinned so that they cannot rotate with it. These sealing rings reduce the flow of oil to a relatively small quantity, approximately one gallon per minute to each seal. Gland seal oil is supplied at approximately five pounds per square inch behind the sealing rings, and this oil flows out to the rotor shaft through radial holes in the sealing rings. The oil then flows both ways from the center of the sealing rings, part toward the air side of the seal and part toward the hydrogen side of the seal. As long as gland seal oil pressure is maintained, hydrogen gas will not escape along the shaft even if the gas pressure approaches that of the hydrogen gland seal oil pressure. The gland seal oil leaving both the air and hydrogen sides of the seals is collected and returned to the gland seal oil reservoir. A very small amount of gland seal oil is lost to the main bearing oil at the shaft gland seals.

The relation of the various elements which comprise the complete gland-seal-oil-supply system is shown diagrammatically in figure 5. The gland seal oil that is drained from the hydrogen side of the sealing rings flows by gravity to the defoaming tanks, where the foam is permitted to settle out, before the oil is returned to the gland-seal-oil reservoir. This defoaming tank also acts as a valve in case of an explosion in the generator as sufficient oil is provided in the tank, and the oil and gas passages are of such relative size, that it cannot be emptied in the 10 to 15 seconds in which the machine pressure might be above atmospheric pressure. This prevents driving hydrogen gas into the main and gland oil reservoirs under such conditions.

The gas pressure in the generator and

defoaming tanks is balanced by a column of oil in the loop seals located beyond the defoaming tanks.

The gland-seal-oil reservoir is integral with the main oil reservoir, but the oil interchange between these two tanks is very small. Sufficient oil from the main-bearings oil-return line is admitted to the gland-seal-oil reservoir by the float-controlled valve to make up that gland seal oil that is lost to the bearing system at the shaft gland seals.

It is necessary to remove the gas and moisture from the gland-seal-oil supply by a vacuum-treating process, otherwise the oil leaving the hydrogen side of the shaft gland seals would liberate gas and moisture in the generator housing and this would reduce the hydrogen purity. In the vacuum-treating unit the gland seal oil is subjected to a vacuum of one inch mercury absolute or less. As the oil is sprayed into the vacuum, the water flashes into steam, and the entrained gas in the oil expands and bubbles out of the oil. The vacuum pump draws these gases out of the vacuum tank, discharging the gases out the vent to outside, and draining the water out of the separator tank just beyond the vacuum-pump exhaust. The vacuum-treating unit is so arranged that it may be segregated from the gland seal oil supply system for a limited time for inspection and maintenance without materially affecting the operation of the generator. Continued operation without the vacuum-treating unit would result in air being liberated into the generator housing and the addition of hydrogen would be required to maintain the hydrogen purity in the generator.

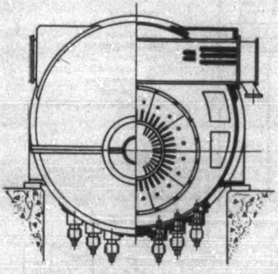


Figure 3. End section view of a 53,000-kw hydrogen-cooled generator showing the gas-cooler arrangement

The shaft-gland-seal-oil supply is normally secured by an oil-turbine-driven oil pump, using high pressure oil from the impeller on the turbine shaft for driving power. If this pump should fail, a pressure-operated Mercoid switch operates to start an electric-driven seal oil pump. Also an emergency connection is provided between the main bearing supply system and the gland seal oil supply system, whereby a diaphragm-operated regulating valve operates to admit bearing oil pressure to the gland-seal-oil system in case the pressure drops to approximately three pounds per square inch. Shaft gland seal oil is thus guaranteed so long as the generator rotor is running. When using the main bearing oil for the shaft gland seals through the emergency connection, it is necessary to add hydrogen from time to time to maintain the hydrogen purity. The vacuum-treating unit should not be operated when the emergency seal supply system is used.

An important feature of the gland-seal-oil supply and main-bearing oil supply systems is the ventilating blowers which are direct connected to the gland-seal-oil supply pumps. These ventilating blowers create a vacuum of about two inches of water pressure in the gland-seal-oil and main oil reservoirs, in the main-bearing drains, and in the bearing pedestals. All of the bearing drain lines are designed so as to flow partially full of oil, leaving a ventilating passage. There is, therefore, a flow of ventilating air through the labyrinth oil sealing strips at the bearing pedestals, which air flows through the partially full oil drain lines to the main oil tank, thence to the gland-seal-oil reservoir, and through the ventilating blower to the vent to outside. Hydrogen accumulation in the pedestals and drain lines, and oil tanks, is thus eliminated.

Gas Control System for Hydrogen-Cooled Generator

The gas control system is shown schematically in figure 6. The principal functions of the gas control system are to provide means for maintaining the hydrogen in the generator within the proper purity, pressure, temperature, and moisture limits; and to provide means for filling and scavenging the generator housing with gas; and to warn the operator when operating conditions are not normal.

Figure 6 illustrates the gas control system during normal operating conditions. The hydrogen pressure in the generator housing is automatically maintained at approximately 5 to 10 inches of water pressure in order to prevent air leakage into the generator housing. A Mercoid pressure switch energizes the solenoid hydrogen inlet valve when the gas pressure drops to about 5 inches of water pressure. A diaphragm operated relief valve regulates the upper pressure limit by exhausting gas to atmosphere at approximately 20 inches of water pressure. A push button is located on the gauge board for manually operating the solenoid hydrogen inlet valve to increase the hydrogen purity, the excess pressure resulting from the hydrogen admission being released by the diaphragm-operated relief valve.

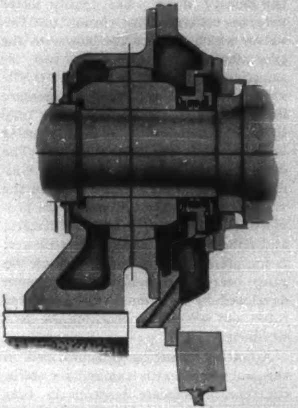


Figure 4. Section view of pedestal bearing and shaft gland seal for a hydrogen-cooled generator

A machine gas pressure gauge, containing high and low alarm contacts, is located on the gauge board. Also another pressure gauge is located adjacent to the manual gas-supply bottles so that the operator can observe the machine gas pressure during the scavenging and filling operations. Both of these pressure gauges are of a special design capable of withstanding the explosion pressure, although the full-scale range is only 0 to 60 inches of water pressure.

The purity of the hydrogen gas in the generator housing is indicated on the gauge board by a "density meter" which gives a direct check on the hydrogen purity as the density of hydrogen is much less than the density of air. The density-meter blower runs at constant speed, being driven by a lightly loaded induction motor, and samples the machine gas continuously. The pressure developed by this blower is directly proportional to the density of the gas, and this pressure is used to actuate the density meter on the gauge board which is merely a 0 to 20 inches of water differential pressure gauge. The density-meter dial is graduated 0 to 150 per cent density, 100 per cent density being arbitrarily taken as the density of air. The density meter may be calibrated by closing the two valves which connect the blower to the generator, and opening the two air sampling valves. The orifice needle valve which connects the two pressure lines leading to the density meter may then be adjusted until the density meter indicates 100 per cent density. The density meter blower can then be connected so as to sample and indicate the machine gas density. Contacts are provided for indicating high gas density, and failure of the density meter blower. A fan pressure gauge is provided on the gauge board which indicates the pressure developed by the fans on the generator rotor, and serves as a check on the density meter.

A bottle-pressure gauge is provided on the gauge board with high and low alarm contacts to indicate respectively that the gas reducing regulators at the automatic hydrogen supply bottles have failed or that the gas bottles are empty and require replacement. Also an indicating thermometer with a high alarm contact is provided on the gauge board to indicate the gas temperature in the generator housing.

Hydrogen-cooled generators require a small gas dryer which removes a small amount of moisture from the gas in the generator housing, and thus keeps the dew point of the cooling hydrogen below the temperature of the gas in the cooler tubes, and prevents condensation within the generator. Hydrogen is circulated through the reactivated alumina dryer by means of the generator fan pressure. At approximately weekly intervals, the dryer is disconnected from the generator and reactivated by heating the activated alumina and blowing room air through the dryer tank, these operations being readily performed by operating valves provided and starting the heater and air blower. The normally blue activated alumina crystals turn gray when full of water, as evidenced by the sight glass on the side of the dryer tank.

Water detector float switches are connected to the low points in the bottom of the generator frame, and these sound an alarm when full of water, indicating water is collecting in the generator.

When operating conditions are abnormal, an alarm is sounded at the gauge board and a signal light indicates the source of the alarm. The following alarms are normally provided on a hydrogen-cooled generator:

1. High gas density
2. Low gas density
3. High gas pressure
4. Low gas pressure
5. High bottle pressure
6. Low bottle pressure
7. High gas temperature
8. Water detector full
9. Gland low oil pressure
10. D-c power supply off
11. Vacuum oil treating unit

The gas supply bottles and operating valves are usually located conveniently under the generator. A manual supply of hydrogen and carbon dioxide is provided for filling and scavenging the generator housing with hydrogen. It is necessary to scavenge the oxygen from the generator housing with an inert gas, such as carbon dioxide (or nitrogen) before introducing the hydrogen into the generator housing to prevent mixing of air and hydrogen. Two volumes of inert gas will reduce the oxygen content to a safe figure for introducing hydrogen. Three and one-quarter volumes of hydrogen are then required to bring the hydrogen purity up to 95 per cent hydrogen. The density meter may be used to check the various gas purities.

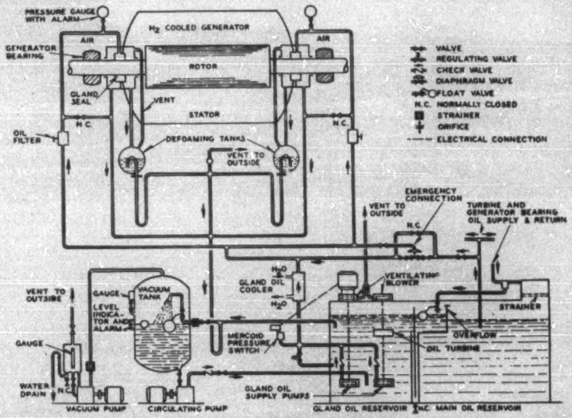

PRESSURE GAUGE WITH ALARM
H_2 COOLED GENERATOR
AIR
AIR
GENERATOR BEARING
ROTOR
GLAND SEAL
VENT
STATOR
N.C.
N.C.
OIL FILTER
DEFOAMING TANKS
VENT TO OUTSIDE
VALVE
REGULATING VALVE
CHECK VALVE
DIAPHRAGM VALVE
FLOAT VALVE
N.C. NORMALLY CLOSED
STRAINER
ORIFICE
ELECTRICAL CONNECTION
EMERGENCY CONNECTION
VENT TO OUTSIDE
N.C.
TURBINE AND GENERATOR BEARING OIL SUPPLY & RETURN
H_2O
GLAND OIL COOLER
H_2O
VENTILATING BLOWER
STRAINER
VENT TO OUTSIDE
GAUGE
VACUUM TANK
LEVEL INDICATOR AND ALARM
MERCOID PRESSURE SWITCH
OVERFLOW
OIL TURBINE
GAUGE
WATER DRAIN
N.C.
VACUUM PUMP
CIRCULATING PUMP
GLAND OIL SUPPLY PUMPS
GLAND OIL RESERVOIR
N.C.
MAIN OIL RESERVOIR

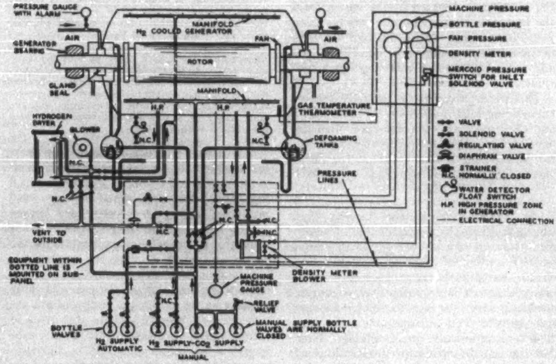
Figure 5. Schematic diagram of the shaft-gland-seal oil-supply system for a hydrogen-cooled generator

During normal operation of a hydrogen-cooled generator the gland seal oil carries air in and hydrogen out of the generator. An air in-flow of one cubic foot requires the addition of 24 cubic feet to maintain the hydrogen purity at 95 per cent. When it is required to remove the hydrogen from the generator housing, approximately three volumes of inert scavenging gas are required to reduce the hydrogen purity to a safe figure for adding air.

Operating Experience

The first unit to be filled with hydrogen went into operation on May 5, 1938. Since that time, four other units have gone into operation with hydrogen cooling. Hydrogen gas used by these machines runs from 20 to 90 cubic feet per day when the generator is running and considerably less when the generator is shut down. Some gas is lost due to variations in machine gas temperature which causes fluctuations in gas pressure sufficient to operate the relief valve. Gas purity remains at well over 95 per cent without adding gas to improve the mixture showing that the infiltration of air with the gland seal oil is quite small.

As in nearly every radically new design, some troubles were encountered at the outset. Shortly after the first unit started up with hydrogen, water was noticed in the water detectors connected to the bottom of the machine. While the quantity was not large, its presence was disturbing. After some investigation, it became apparent that the water in the generator was coming from the gland seal oil. Small quantities of water in the gland seal oil were being evaporated at the shaft sealing glands on the hydrogen side and the water vapor eventually found its way into the generator by diffusion through the gas. When it entered the generator, the moisture content of the gas increased until the dew point reached the temperature of the tubes of the gas coolers. Water then condensed on the cooler tubes and ran down into the water detectors. As originally designed, the shaft sealing glands used oil from the lubricating system without special treatment other than running it through baffles in the gland seal tank to remove air bubbles. Any water that got into the lubricating system was, therefore, likely to be taken over into the generator. To overcome this situation, three changes were made in the machines. A vacuum-treating system was inserted in the gland seal oil system to remove the major portion of the water in the oil pumped to the shaft sealing glands. Additional drains were installed in the sealing glands so that oil coming off the air side of the seals could be caught and kept separate from the lubricating oil and drained back to the gland-seal-oil reservoir. This move practically isolated the gland seal and lubricating oil systems one from the other so that the vacuum-treating system was required to treat the same quantity of oil over and over again rather than to be treating a large amount of oil from the lubricating system.


PRESSURE GAUGE WITH ALARM
GENERATOR BEARING
AIR
H2 COOLED GENERATOR
MANIFOLD
FAN
ROTOR
GLAND SEAL
MANIFOLD
H.P.
H.P.
HYDROGEN DRYER
BLOWER
N.C.
N.C.
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VENT TO OUTSIDE
S
EQUIPMENT WITHIN DOTTED LINE IS MOUNTED ON SUB-PANEL
MACHINE PRESSURE GAUGE
RELIEF VALVE
BOTTLE VALVES
H2 SUPPLY AUTOMATIC
H2 SUPPLY-CO2 SUPPLY
MANUAL
MANUAL SUPPLY BOTTLE VALVES ARE NORMALLY CLOSED
DENSITY METER BLOWER
AIR
GAS TEMPERATURE THERMOMETER
DEFOAMING TANKS
PRESSURE LINES
MACHINE PRESSURE
BOTTLE PRESSURE
FAN PRESSURE
DENSITY METER
MERCOID PRESSURE SWITCH FOR INLET SOLENOID VALVE
VALVE
S SOLENOID VALVE
REGULATING VALVE
DIAPHRAM VALVE
STRAINER
N.C. NORMALLY CLOSED
WATER DETECTOR FLOAT SWITCH
H.P. HIGH PRESSURE ZONE IN GENERATOR
ELECTRICAL CONNECTION

Figure 6. Schematic diagram of the gas control system for a hydrogen-cooled generator

Tests at the factory on a shaft sealing gland showed that even though the vacuum system reduced the water in the oil to less than 0.003 per cent, a certain portion of this small quantity of water would get into the generator and would in time raise the dew point to the saturation point corresponding to the cooler tube temperature. A small dryer of the activated-alumina type was, therefore, connected into the gas system so that a small portion of the hydrogen would be circulated continuously through the dryer to remove the last traces of moisture that might get into the generator.

These three changes have proved to be entirely satisfactory in keeping water out of the generator. The addition of the vacuum-treating system and the dryer have added very little to the work of the station operators and it should be noted that neither piece of equipment is vital to continuous operation and can be cut out of service at any time for several hours for inspection without affecting the operation of the generator.

One other major trouble encountered was the excessive vibration of the tubes of the gas coolers which resulted in wear of the fins on the tubes where they passed through the tube support plates. As pointed out in a companion paper, the two-pole generator stator is subject to double-frequency vibrations set up by the magnetic field. This vibration is always present in such machines but had not been recognized as important until these large-diameter two-pole machines were built. It was not recognized that a cooler construction suitable for mounting on the foundation of the unit would not be adequate when closely connected to the frame of the generator. As soon as the problem was realized, the solution of adding extra tube-support plates in the coolers to change their vibration periods became apparent. A number of other detail changes were also made and no further trouble from this source has developed.

Exciters

This paper would not be complete without some mention of direct-connected exciters for the large 3,600-rpm units. While some doubts were expressed by the purchasers at the time of buying these large units, concerning the possibilities of trouble with exciters up to 165 kw rating at 3,600 rpm, their operation to date has been entirely successful. A 65,000-kw unit is now on order for which a direct-connected 260-kw exciter will be provided. All the large units have been provided with pilot exciters with armatures overhung on the end of the main exciter shafts.

Conclusion

With operating experience on a number of units and over a considerable period of time at hand, the initial stage of development of hydrogen-cooled generators is passed. There will no doubt be minor difficulties to correct as time goes on, but these will be of no more serious nature than are to be found in air-cooled generators. It will also be possible to simplify considerably the generator and its attendant parts in future machines as greater experience is gained with this type of machine. Cost of supplying hydrogen gas is very low, and it has been the experience in most stations that the additional attention on the part of the operators as compared to that for an air-cooled machine is very small. Hydrogen cooling will probably be adopted more and more for units of 30,000 kw and above where the additional cost of the gas-tight housing and gas control can be justified economically.

The adoption of hydrogen cooling for large machines has enabled the size of 3,600-rpm machines to be pushed up to 81,250 kva at 80 per cent power factor. With further experience, it will no doubt soon be possible to build 100,000 kva, 80 per cent power factor at 3,600 rpm with this method of cooling.

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Discussion

Sterling Beckwith: See discussion, page 38.

S. H. Mortensen: See discussion, page 48.

C. F. Hill and L. J. Berberich: See discussion, page 48.

D. S. Snell (General Electric Company, Schenectady, N. Y.): I wish to compliment Messrs. Ross and Sterrett on the stator-frame construction developed for their hydrogen-cooled generators, which allows the use of somewhat larger and more accessible coolers for given shipping dimensions than practicable with the conventional round frame construction. In our designs, we have preferred the use of four coolers placed lengthwise of the casing, in place of the two-cooler-crosswise arrangement, to permit removal of any one cooler from service without shutting down the machine, and at the same time maintaining uniform temperature distribution in the cooling medium.

The type of shaft sealing gland described in this paper has the advantage over the solid-ring type of seal, constructed integral with the bearings, of requiring a considerably lower oil flow, and thereby reducing the size of the oil-treating equipment. However, it has the disadvantage of having loose parts which may under certain circumstances seize the shaft and be destroyed, and also introduces the possibility of misalignment with the shaft, which is not present with the solid-ring type of seal. While we are using a shaft seal of the floating-ring type with some of our 1,800-rpm hydrogen-cooled generators, we consider the solid-ring type of seal to be preferable with generators of the 3,600-rpm class, since machines of the latter class are more susceptible to vibration difficulties.

In connection with the seal oil supply system described in this paper I would suggest that a slight improvement in the degree of vacuum treatment of the gland sealing oil would result in obtaining an appreciable increase in the percentage hydrogen purity held in the generator casing and would also decrease the amount of moisture entering the casing through the gland seals. I would ask the authors if they do not consider the effectiveness of their vacuum-treating system to be somewhat reduced by the reabsorption of air by the oil in the gland oil reservoir. Tests by F. M. Clark, reported in the *Journal* of the Franklin Institute, volume 215, 1933, page 39, showed that vacuum-treated oil exposed to air in an open vessel absorbs air rapidly, and I should think that the extensive oil surface exposed to air in the gland oil reservoir would permit a considerable absorption of air by the oil. Much of this reabsorption of air could be prevented by simply floating a board on the oil surface.

I note that no provision is made for automatically interrupting the vacuum in the event of failure of the circulating pump, to prevent flooding the vacuum pump with oil foam, and presume this is done manually. With the system described in my own paper an automatic interrupting device is used, due to the relatively high vacuum maintained in the vacuum tank, which would cause the tank to fill quickly with oil foam following a shutdown of the circulating pump if the vacuum were not interrupted.

The use of a dryer for the hydrogen within the generator casing such as is described in this paper, has not been found necessary with our own hydrogen-cooled generators. This may be partly due to the relatively high degree of vacuum treatment of the shaft sealing oil which is obtained, and also to the fact that a small dryer is provided in connection with the hydrogen-purity indicator. During the first few weeks of operation, however, considerable moisture is present in the casing as a result of the casing having been open to atmosphere, which requires rather frequent renewal of the dryer of the purity indicator. We have provided, with our earlier machines, a dryer for drying the hydrogen admitted to the casing from the hydrogen containers, but this has been found unnecessary due to the high degree of dryness of the hydrogen in the containers, and is being omitted with our later machines.

This paper is a tribute to the extensive engineering work performed by the authors and their associates in connection with the development of the hydrogen-cooled generator, and I congratulate them on the excellent design they have achieved.

F. D. Troxel (Sargent and Lundy, Inc., Chicago, Ill.): A review of the developments and use of hydrogen gas as a medium for the cooling of rotating electrical machines, particularly large high-speed turbine generators, is very timely since several machines have now been in service for several months and considerable operating data and experience are available. Now that the initial steps in the practical use of hydrogen-cooled generators are well under way, there are several facts which are quite evident:

1. That larger and more efficient hydrogen-cooled generators can be successfully built into a given space than air-cooled machines.
2. That the shaft seals which were long the subject of much discussion and many costly experiments, are operating without any serious trouble and with a small amount of gas leakage.
3. That the fear of disastrous explosions is rapidly disappearing.

The authors have prepared an excellent paper and have discussed the design and operating experience of hydrogen-cooled generators, particularly from the viewpoint of the manufacturer. While the operation of hydrogen-cooled generators, has, in general, been considered satisfactory, from the operating company's viewpoint there are certain improvements and developments which should be made in connection with future machines, namely:

A. The hydrogen auxiliaries should be simplified and reduced in number.

B. Means should be developed for taking the hydrogen out of the generator and for refilling in a much shorter time.

C. Standardization of the explosion strength requirements of the shell should be adopted to cover both distortion and failure, and standardization of factory tests to prove the design. Before adopting any standards relative to shell strengths, the question of the real necessity of explosion-proof shells, and their economic justification should be carefully considered.

In connection with the planning and installation of hydrogen-cooled generators, there are certain precautions which should be kept in mind, as follows:

A. All oil tanks and connecting pipes should be carefully vented and grounded. Several small explosions have resulted from lack of attention to these details.

B. There should be no pockets under the generator and around the generator foundation in which gas can collect. If such pockets are unavoidable, some type of ventilating system is required to keep the air slowly moving through these areas.

C. It is desirable for safety reasons to store the hydrogen-supply bottles outside of the building. By locating the hydrogen-supply bottles outside of the building and there connecting them to a header piping system which pipes the gas into the generator, the amount of hydrogen in the building is reduced by about one-half, assuming a normal supply approximately equal to that in the machine. This is desirable from safety and insurance consideration. If the bottles are stored and racked outside of the building on a sheltered platform of suitable height located alongside a driveway, the bottles can be unloaded from a truck or reloaded with very little labor. On the other hand, if the bottles are located under the machine, it is necessary to unload them from a truck, pick them up with the crane, move and lower them to a position near the generator, and then roll them into their rack. The handling of the empty bottles is, of course, similar in the reverse order. All of these steps require time and labor, the cost of which in the course of a year amounts to a considerable sum.

The use of hydrogen cooling introduces one problem in connection with the temperature rise and rating of generators which has not been encountered in air-cooled machines. Since the coolers are located within the machine casing, the distance from the points where the gas leaves the coolers and where it enters the laminations, is very short, and as a result, there appears to be a stratification of the gases in those areas in so far as the gas temperatures are concerned. Several degrees difference in temperatures have been noted on thermocouples a few inches apart. This difference is particularly important since the temperature-detector elements upon which the temperature rise of the generator is determined are located in these areas. This condition does not exist in the air-cooled machines, since the coolers are generally located some distance from the generator, and the cooling medium is thoroughly mixed and has reached a uniform temperature when it enters the generator.

As a result of this condition, it has been suggested that for hydrogen-cooled machines the temperature of the cooling water in going to the air cooler be used as a base, that the temperature rise be determined from this base, and the rise so determined be used in the guarantees and in the determination of hydrogen-cooled generator ratings in a manner similar to that which has been used for many years in connection with water-cooled transformers. We feel that this suggestion has considerable merit.

C. C. Sterrett: We wish to thank the following gentlemen for their discussions of our paper: S. H. Mortensen, Sterling Beckwith, F. D. Troxel, D. S. Snell, C. F. Hill, and L. J. Berberich.

Mr. Mortensen's comments relative to removing air or gas from the generator housing by evacuation are interesting. We have preferred to use the inert-gas method of removing air or gas from the generator housing because we believe this is simpler and has considerable advantages not provided by the evacuation method.

In hydrogen-cooled turbine generators of our design, three-fourths load may be carried, air or CO_2 cooled. We are, therefore, able to change the gas in the generator housing with three-fourths load, whereas if the evacuation method were used, the generator load would have to be reduced to zero while the generator housing was evacuated.

The possibility of a fire in a hydrogen-cooled machine, or of a failure of the gas control or oil system is rather remote, but if this should occur, it would be desirable to have a positive means of scavenging the generator housing readily. This may be done with a CO_2 scavenging system, whereas the evacuation method would require a vacuum pump or ejector, and it might be that the power for operating this equipment would not be available when required.

We are able to scavenge air or hydrogen from our generator housing with CO_2 in less than 45 minutes, and we doubt that the evacuation method would reduce this time unless a very large vacuum pump or ejector were used.

The only advantage of the evacuation method is that it require slightly less hydrogen for the filling operation. In our gas system, two volumes of hydrogen are required to fill the generator at standstill, whereas with the evacuation method, Mr. Mortensen points out only $1\frac{1}{3}$ volumes of hydrogen are required. Since the additional hydrogen required by our method would cost about $15 a filling, we cannot justify the evacuation method. We are able to add the necessary hydrogen to our generators when running (four volumes) in an hour or less and this time is reduced in half when the generator is at standstill.

Mr. Mortensen informs us that he has found seal oil treatment unnecessary in his design. Our experience does not bear this out. Turbine oils, and especially those used in "topping units," contain traces of water. This water will be liberated into the generator housing at the shaft seals and will eventually build up the dew point in the generator. We would suggest Mr. Mortensen measure the dew point of the gas in their hydrogen-cooled machines. We believe he will find a high moisture content in the gas in the generator housing. As pointed out in Messrs. Berberich and Hill's discussion of our paper, low moisture content in the generator housing is very desirable.

Sterling Beckwith's suggestion to simplify the gas and oil systems of hydrogen-cooled machines is timely. In our paper, the statement is made that we have passed through the development era of hydrogen-cooled turbine generators, and we are now turning our attention to simplification of the auxiliary equipment. Hydrogen-cooled turbine generators which are now in operation require no more attention than a corresponding air-cooled machine. We have probably been too conservative in the design of our auxiliary equipment, and are now simplifying our equipment so that it will require much less attention. The operating companies have accepted hydrogen cooling very favorably and have offered many valuable suggestions which will aid in simplification of our auxiliary equipment.

We do not expect to eliminate the relief valve as suggested by Mr. Beckwith. Our later machines are equipped with a very simple relief valve, and we set it to release gas at a pressure above the normal operating pressure range. The relief valve acts to limit the pressure; however, excess pressure resulting from failure of the inlet system would do no harm. We believe it is desirable to keep the gas pressure under control automatically. As pointed out by Mr. Beckwith, the relief valve does not aid in reducing the explosion pressure by releasing gas, since the explosion pressure only lasts a few seconds.

Mr. Beckwith suggests that operation of hydrogen-cooled generators at higher pressures might not be justified due to increased hydrogen consumption. We expect to be able to build gas-tight housings for turbine generators which will not require more than 100 cubic feet of hydrogen per day to maintain 15 pounds in the generator. The cost of this gas will be insignificant with respect to the value of the additional load available at the higher operating pressures.

We enjoyed reading F. D. Troxel's discussion because it represents the operating companies' viewpoint. Mr. Troxel suggests it would be desirable for safety and operating reasons to store the hydrogen bottles outside the building. We do not believe this is necessary from a safety standpoint, but we agree that it might simplify the handling of the hydrogen bottles. So far as we are concerned, the hydrogen and CO_2 supply may be located wherever the customer desires, but we have left this location up to the customer.

Mr. Troxel further suggests that the temperature of the cooling water going to the gas coolers should be used as a base instead of the gas temperature leaving the coolers, for determining the temperature rise used in guarantees. We are in full agreement with Mr. Troxel on this point and we favor a standardization program on this basis.

Relative to Mr. Snell's discussion, we have the following comments. In our earlier generators we provided two gas coolers placed crosswise in the generator housings. Each cooler had a single section and it was necessary to shut down the generator for servicing either or both gas coolers. Operation with these machines has been very satisfactory. However, in our later design we have installed two and three sections in each of the two coolers and a section may be cleaned at a time without reducing the load on the generator. Mr. Snell has suggested that the type of shaft seal used in our design has the disadvantage of having loose parts which may fail, or not be aligned with the shaft. We disagree with Mr. Snell and point out that the floating-ring type seal which we are now using has operated satisfactorily in seven machines for a considerable time on both 1,800- and 3,600-rpm machines. The floating-ring type seal we use is similar to the seals which have been used with success on turbine shafts for a number of years.

Relative to Mr. Snell's comments on the effectiveness of our vacuum-treating unit, we refer to our paper under "Operating Experience." We stated there that our machines were using 20 to 90 cubic feet of hydrogen per day and maintaining hydrogen purity well above 95 per cent. We believe this indicates satisfactory performance of our seal oil system. Mr. Snell has questioned as to how we control excess foaming in the vacuum oil tank. We do this with a "vacuum breaker," which admits a small amount of air and breaks the foam. The vacuum breaker is operated by a float which rides the foam in the vacuum tank. It is our experience that an excess amount of water will overload the vacuum tank and cause foaming. In our design this operating condition is taken care of almost instantly by the vacuum breaker.

We wish to take this opportunity to compliment Mr. Snell on the splendid paper he has presented on hydrogen cooling. He has covered the subject of hydrogen cooling thoroughly, and in our opinion left very little open for discussion.

Electrical Equipment on Machine Tools

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NONMEMBER AIEE

THIS is an interesting opportunity to speak to electrical engineers, to praise them for the excellent motors and controls which have helped us to improve the performances of machine tools, and yet to prod them a bit about a few features of their designs which are obstacles to a greater use of electrical equipment. This paper is thus a partial report to electrical designers describing some of the recent applications of electrical equipment to machine tools and outlining a few of the problems which have been encountered.

Machine tools are rapidly becoming motor-drive machines. Ten years ago we would estimate that about 40 per cent of the machines made by Brown and Sharpe were motor driven. Today over 90 per cent are equipped with motors and many new machines are being designed solely for individual motor drive with no provision for overhead group drive. Not only has there been a shift from group to motor drive, but there has been an increase in the number of motors used on machines. Today milling machines commonly employ three individual motors while grinding machines require from five to seven separate motors.

Now if individual motors are to be used to drive various units of a machine, it is a logical step in design to use more fully the abilities of these motors, to interconnect their controls, and to co-ordinate their functions. Thus instead of engaging and disengaging a clutch to start and stop a mechanism, the driving motor may be started and stopped. Instead of applying a mechanical brake, a motor may be plugged to rest, and in place of a reversing clutch, the motor may be reversed.

Electrically Controlled Milling Machine

The milling machine shown in figures 1 and 2 successfully uses these natural functions of its motors. This is a plain milling machine in which separate shell-type motors drive the spindle and table. The table starts, stops, and reverses with its motor. In stopping the motor is plugged to rest or to the point where it actually makes a few revolutions in the opposite direction. The coast of a few turns in the opposite direction withdraws the work a few thousandths from the cutter, but has no influence on the point of farthest table advance or the point of reversal. By reversing and plugging the motor, it is possible to maintain with great accuracy, the point at which it is desired to stop a cut or to reverse in the cut when cutting to a shoulder. A small motor can stop in less than two revolutions when plugged. With an 1,800-rpm motor, producing a two-inch feed rate, two revolutions represent about 0.002 inch of table movement. Variations in stopping movement due to changes in line voltage might be as much as one-half motor turn which would mean 0.0005 inch at the table. Now of course variations in the operating time of control devices will have an influence on the reversal position yet even with these, the point of table reversal is maintained with surprising accuracy. For example, it is possible to take a very deep heavy cut in which the table is reversed by a table dog while in the cut. The curved surface left by the cutter on the work may be coated with Prussian blue and the table cycle repeated. The cutter will just kiss the blued surface, but will not completely scrape the color from the surface. The cycle may be repeated many times with the cutter always touching the blued surface yet never completely removing the original surface. For such accuracy of reversal, credit is given to the motor and controls for in mechanical designs, we have not obtained this degree of accuracy.

Figure 1. Electrically controlled plain milling machine

Figure 2. Control compartments on milling machine

A clutch shifted by a solenoid gives feed and fast travel rates of table movement. Using a solenoid to shift the clutch, it becomes comparatively easy to tie in the spindle rotation with the movement of the table. In a normal cycle the cutter spindle will rotate only when the table is moving at a feed rate. A selector switch provides other possibilities where the spindle rotates continuously or rotates only when the table is in motion. This interrelation of spindle and table movements is a feature obtained quite readily in an electrically controlled machine. This same feature is not available in mechanical designs.

When cutting to a shoulder, the table feed is usually stopped by a shift from feed forward to fast travel in reverse. In this move, it is essential that the shift to fast travel be delayed until the motor has been reversed or has at least reached zero speed in its reversal. If quick traverse is engaged before this, the table will jump ahead, jamming the work into the cutter. To obtain the desired sequence of action, a viscosity switch is employed. This is a switch which is controlled by

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B. P. GRAVES is director of design of the Brown and Sharpe Manufacturing Company, Providence, R. I.

the direction of motor rotation. Typical switches are shown in figures 3 and 4. The inner drum is mounted directly on the motor shaft and the outer cylinder is rotated a few degrees by the viscous drag of a 0.002-inch film of oil. The circuit of the clutch solenoid is held open by the viscosity switch and is closed only when the motor has completed its deceleration and is moving in the reverse direction.

Since the viscosity of an oil changes rapidly with temperature, a viscosity switch cannot be expected to act at exactly the same point each time the motor is stopped by plugging. A switch may be expected to open somewhere between a speed of 500 rpm and zero or if a very light restoring spring is used between zero and −500 rpm. Since a few thousandths table drift away from the cutter is not only acceptable, but desirable, the zero to −500 rpm zone has been adopted for the table motor. In stopping, the motor always passes through zero speed and makes a few revolutions in the opposite direction. A solenoid-operated detent prevents the switch drum from being carried through neutral to make contacts on the opposite side as would be done in a complete reversal.

The cutter spindle motor is plugged to rest. Here the switch springs are selected to overcome the viscosity drag when the motor reaches the 500-rpm-to-zero speed range. The plugging is stopped before the motor reaches zero speed, but in plugging, a 3,600-rpm motor to 500 rpm or less 98 per cent of the kinetic energy has been absorbed and a light-duty brake can be used to bring the motor to a complete stop and hold it there. A solenoid releases a spring which applies the mechanical brake as soon as the viscosity switch opens the control circuits.

Making more complete use of electrical equipment, it is possible to adopt a two-speed feed motor. A mercury switch tipped by table dogs selects the high or low table feed. With the possibility of halving feed rates at desired points in the cut changes in depth or width of cut can be more readily handled. Change of feed during a cut is a feature made possible by the use of electric controls. It is a feature which has not been offered in mechanical designs.

One other interesting advantage of an electrically-controlled machine is that the machine can be made inoperative as soon as any door or cover is opened. The doors of the spindle and table gear cases of this milling machine both open motor control circuits and make it impossible to start the machine accidentally while gears are being changed.

Electrically Controlled Grinding Machine

The electrically driven and controlled plain grinding machine shown in figures 5, 6, and 7 is probably the most completely electrified of any standard machine tool now made. It employs seven motors and two d-c generators; the direct current being used for variable-speed drives.

Briefly described, the machine has five constant-speed motors which are connected to the line and may be alternating current or direct current depending on the power supply. These constant speed motors are:

1. *Spindle Motor.* This is the main motor which is mounted on the wheelslide and drives the wheel spindle through a short V-belt drive. The motor runs all the time the machine is in operation.

2. *Coolant-Pump Motor.* This motor is started and stopped to start and stop the flow of coolant.

3. *Lubricating-Pump Motor.* Starting and stopping with the wheelslide motor, this motor-driven pump supplies lubricating oil to the entire machine.

4. *Wheelslide Quick-Return Motor.* (Figure 8) This motor with its solenoid-controlled brake, starts and stops each time the wheelslide is withdrawn from or advanced to the working position.

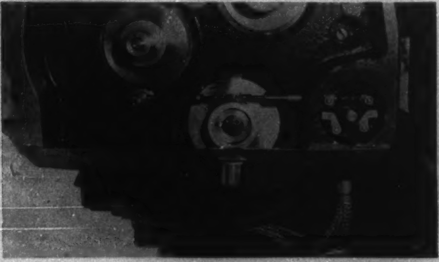

Figure 3. Spindle-motor viscosity switch

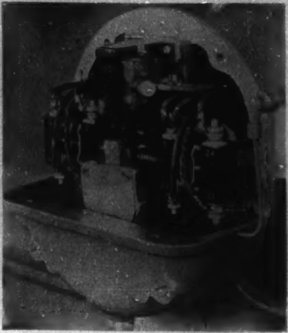

Figure 4. Table-motor viscosity switch

5. *Generator Motor.* (Figure 9) Running whenever the machine is in operation, this motor drives the two d-c generators.

Of the two generators, one is a constant-potential machine, and the other, a variable-voltage generator which is used in a Ward-Leonard table drive. The constant-voltage generator supplies power for relays and contactors, and for the fields of the variable-voltage generator and motor. In addition, it feeds the adjustable-speed motor used to drive the headstock.

The speed of this totally enclosed headstock motor may be varied through a four-to-one range by a rheostat connected in the field circuit. When the motor circuit is opened, a resistance is placed across the armature and rapidly brakes the headstock to rest.

The table motor is controlled by a rheostat in the field of the variable-voltage generator. Reversal of the generator field reverses the table motor. This Ward-Leonard drive was chosen because table loads were essentially constant-torque loads and because a low-speed motor having a small kinetic energy could be readily reversed. Since reversing contactors handle only the small generator field currents, light control units can be used. Speed changes of four to one (900 to 225 rpm) are obtained in the high-gear drive and eight to one (900 to 115 rpm) in the low-gear drive. To obtain an accurate reversal in the high-gear series the motor is slowed down to a low speed just before the reverse contact is tripped.

The outstanding advantage of a completely electric machine is the opportunity given to interconnect the controls of its various units. Controls can be simplified

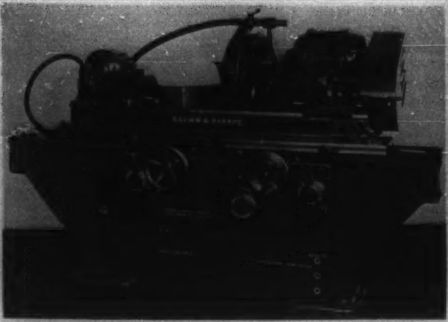

Figure 5. Electrically controlled plain grinding machine

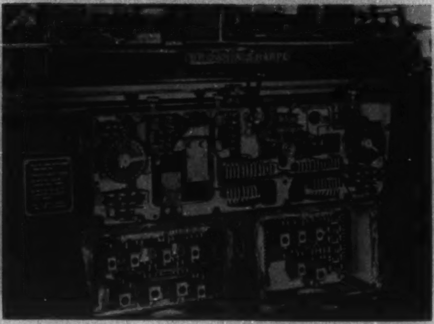

Figure 6. Machine of figure 5 with front plates removed

and co-ordinated, safety features can be easily incorporated, automatic control becomes possible, and in general the machine becomes more responsive to the operator. No other method of control can so easily tie together the various elements of a machine. This is the function of electric control which should receive greater emphasis and it is here that the electrical engineers can do some helpful work in the machine-tool industry. Machine tools are now using a large number of electric motors but the possibilities in controls are still to be developed.

Now taking the basic driving elements of the grinding machine just described, let me discuss a few of the possibilities in control. The piece to be ground having been placed in position, the operator must start the headstock, must turn on the flow of coolant, must start the table, and must bring in the cross slide to the grinding position. Four separate units must be controlled. If all of these are electrically powered as in the machine described, they can be simultaneously started by the rotation of the cross-slide handwheel. Four operations are thus easily replaced by one. When the piece is ground to size there are five steps to be taken: the wheelslide must be withdrawn, the table must be stopped, coolant must be shut off, power to the headstock must be disconnected, and a brake applied to quickly stop the work. By interconnecting controls all of these functions can be executed by a partial turn of the cross-slide handwheel.

An operator is traverse grinding with a table dwell at each end of the table travel and has controls centered in the handwheel. The grinding wheel must be trued. To do this the following moves must be made.

(*a*). Table dwell should be eliminated.

(*b*). A slow rate of table movement must be selected.

(*c*). As a safety measure the wheelslide quick return should be made inoperative.

(*d*). Normally the coolant pump and headstock start together. Here it is desired to start the flow of coolant, but to keep the headstock at rest.

(*e*). The controls centered in the handwheel should be separated so that the table and coolant flow can be started together by a control knob.

All these operations can, in an electric machine, be made simply by turning a selector switch or by changing the position of a knob from "grind" to "true." Having dressed the wheel, all original settings can be restored by turning the switch back to the "grind" position. No time is lost in resetting speeds and feeds, and there is no uncertainty on whether or not all the necessary moves have been made.

With electric control for all units of a machine, it is comparatively easy to break into the control circuits for the purpose of tying in special attachments or devices which are to take part in the operation of the machine. For some production jobs, automatic sizing or gauging devices are placed on the work. Such gauges can be wired to make or break a control circuit and thus to withdraw the wheelslide and stop the table, headstock, and coolant pump, leaving the machine ready for removal of the work.

In much the same way, an oil-operated piston can be placed in the lubricating system and can be wired to stop the entire machine whenever oil pressure becomes abnormally high or low.

The two machines described suggest some of the possibilities in machine-tool design which are offered by electric motors and controls. Through the wise use of electrical equipment machines can be given new abilities or features and can be made to perform standard functions with greater ease and increased accuracy. Of special importance are the gains which can be made through the use of electric controls. Most new machines will be motor driven and it is thus suggested that design development be focused on using to the fullest extent, the abilities of motors and on co-ordinating and simplifying their controls.

Controls

In starting out on the design of electrically controlled machine tools we realized that there were several characteristics of machine-tool service which were different from the service for which many control units had been designed. Life tests were thus made on a number of different makes and types of relays and contactors, to determine those which were best suited for our requirements. In these long tests, it was found that many standard relays and contactors performed poorly or failed completely in machine-tool service. Since electrical engineers are considering altering their designs to meet some of these new service conditions, it may be helpful to discuss these in detail.

Frequency of Operation

Electrical control equipment for machine tools should be designed to have a life of at least 10,000,000 cycles or operations. In the machine-tool industry, it is expected that machines will serve reliably for many years and any machine which requires frequent replacement of

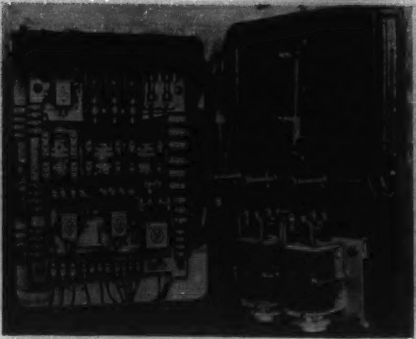

Figure 7. Side compartment for wheelslide controls

Figure 8. Wheelslide quick-return motor and cam control

parts is handicapped in its appeal to the trade. On production or manufacturing machines, control devices are operated almost continuously. Ten-second work cycles are common and in a complete cycle relays and contactors may be operated two or more times. With controls being energized every five seconds, it does not take many months to get into the million zone of operations. This is a far different service from that where controls are mounted on the walls of a room and operated a few times a day.

From the life tests we have conducted, it has been found that many standard controls have operating lives far under the 10,000,000 standard which we have tentatively adopted. With this number of operations, fatigue and wear failures were common. Pins and washers wore out, screws became loosened and springs, guides, and contact arms broke. Especially common were the failures of shading coils which wore or broke and caused noisy operation. In several cases these coils had failed in a few hundred thousand operations. Life tests frequently ended with the failure of the relay coils. The small wires with which the coils are wound, broke off at or just before the points at which they were joined to the coil terminals. The terminals were not well supported or anchored in the coil jacket or tape covering and the small movements and vibrations of the terminals caused a fatigue failure of the leads. It would appear that greater attention to the mechanical design of controls would improve their useful lives and would better qualify them for use in machine tools.

Certainty of Operation

Most machine tools of the manufacturing type have a fast travel movement which brings the work into cutting position. The point at which the shift from the fast travel to the feed rate occurs must be consistent, for operators set dogs for the shortest possible feed movement and variations in shifting position may cause the work to hit the cutter while still moving at the fast travel rate. As an example, the table of a machine moving at 300 inches per minutes is allowed a variation in shifting position of 1/16 inch due to variations in time of operation of electrical devices. This 1/16-inch represents but 1/80th of a second. If electrical equipment does not operate consistently within this variation limit, there will be occasional crack-ups in which cutters are injured or the work destroyed.

From life tests, it has been found that very few devices will consistently operate within the 1/80-second limit suggested. Designs which have several rubbing surfaces seem to be the least qualified to function with accuracy. Pin friction and losses in links and arms are variable and changes in friction conditions easily cause delays in operation, especially in the time of dropping out after being de-energized. Direct-acting units with simple movements have in our tests had the greatest consistency of operation.

After repeated operations, we were surprised to observe that relays and contactors would occasionally stick and fail to drop out or would be very sluggish in their dropping out. Examination revealed that the surfaces where the armature pressed against the pole faces were sticky. It was at first assumed that the black gummy coating was caused by dirt which had entered the control compartments. To check this, devices were operated in a sealed box. The same coating still appeared to cause sticking. Next, it was assumed that the varnished coils were contributing the gum and so unvarnished coils were tried with no success. It became apparent then that the laminated steel members of units were themselves contributing the gum. The varnish or resinous coating used as an insulating material on the iron sheets was very slowly working itself on to the seating surfaces. Baking the sheets had not prevented the coating from being hammered into a plastic gum.

On checking with other manufacturers, we learned that this trouble with sticking relays and contactors is common. Since the experience of having a relay stick is to a machine operator much the same as

Figure 9. Motor generator set

that of a driver who steps on a brake to find it useless, it is suggested that electrical designers study the problem in their new designs.

By using intermittent-rated coils where the application permits it, it is possible to get greater pulls and thus use greater opening-spring pressures. These will make sticking failures less common, but the move is only a partial solution to the problem. Another partial solution is the use of two units in series. The probability of both devices sticking at the same time is of course far less than that of a single unit sticking. The best solution we have found is the use of opposed relays. As one is de-energized, the other is energized and assuming the first sticks, the second after moving one-fourth inch, gives the first a blow which is vigorous enough to separate sticking surfaces. Should the second coil fail, the first acts just as it would if the second were eliminated. The unit just described has the greatest certainty of operation of any device tested, thus far. The primary objection to this design is its cost.

Ease of Change for 50–60 Cycle

When machines are manufactured in lots of 25 to 100, it is necessary to insert motors and controls in order to run off and inspect machines before going into stock. Since many machine tools go abroad, it is frequently necessary to change these stock machines from 60 to 50 cycle. Motors are made for both 50/60-cycle service but controls are not so rated and must be changed over. It would be a great help to machine-tool builders if relays and contactors could be made for 50/60-cycle service. From tests we have made, it appears that present 60-cycle coils are fully qualified for 50-cycle service and it is difficult to see why manufacturers are so reluctant to give their coils a 50/60-cycle rating. If coils cannot be made 50/60 cycle then designers must simplify their devices so that one coil can be substituted for another in a few simple steps. We have had cases where it has been necessary to eliminate some makes of controls just because the devices had to be practically disassembled to substitute a 50-cycle coil for 60 cycle. In the same way inaccessible over-load heaters make change-overs costly.

Space Requirements

When a control unit is mounted on the wall of a room, a few extra inches in its space requirements are unimportant; however, when the same unit must be mounted in a machine casting, these marginal inches are of critical importance. They may limit the possible power movements of, or may upset the proportions of the machine. In general, the mechanisms in machine tools have been laid out with economy of space as a controlling factor in design. Gears, shafts, clutches, and cams, are assembled in compact units, for steam-shovel proportions cannot be accepted in a sensitive tool-room machine. On such a machine it is a difficult problem to mount electrical equipment and more compact units would greatly aid machine designers. At present, we use few limit switches and push-button stations in their original casings or housings, for these require too much space and have an awkward appearance when attached to the outer walls of a machine. Greater compactness and more economy in use of space would make electrical devices more readily adaptable for machine-tool applications.

Machine Wiring

In wiring a machine tool, it is well to assume that sooner or later, wires will be exposed to oil even though detail design steps have been made to keep them apart. In assembly or repair work, oil is often unintentionally admitted to wire ducts or terminal boxes, and when admitted, it is not likely to be removed. We have thus found it an economical practice to use the high-quality wires insulated with synthetic compounds which make them oil resistant. Wherever possible, ducts of ample size are cast in the machine bed so that wires may be well protected from physical contact with moving parts. It is also very helpful to use prepared harnesses in wiring, for, with wires cut to length, equipped with identifying tags and terminals, and bound together, individual wires do not have to be drawn through the machine and wiring becomes an easier job.

Although oil-resistant wire is available there is no oil-resisting tape to be used on these wires. Taped joints are equally as important as circuit wires and defense against oil will only be half complete until an oilproof tape is developed.

The problem presented by taped joints is aggravated by the terminal boxes on fractional-horsepower motors. Imagine totally enclosed motors being made with terminal boxes which resemble strawberry boxes with their open seams and corners. The fact that an enclosed motor is specified, indicates that surrounding conditions will not be favorable and it is inconsistent to furnish a partially open terminal box on such a motor. A terminal box full of pasty rubber and slimy tape is a hazard in any motor application. Fortunately integral-horsepower motors have well-designed terminal boxes. It would be helpful to bring those on fractional-horsepower motors up to the same standard.

Conclusions

Two conclusions are suggested by this paper. First: that through the use of motor drives and electric controls, machine tools can be improved in performance and can be made simpler and easier to operate. Second: that many standard control devices are poorly qualified for machine-tool service and should be redesigned for this more frequent and exacting duty.

Discussion

P. H. Wilmarth (nonmember; The Blanchard Machine Company, Cambridge, Mass.): Your paper on "Electrical Equipment on Machine Tools" was studied by several of my associates and we all have agreed that the build-up for your final conclusions carry many excellent statements of "constructive inference."

Your illustrations show the last word in good practice in applying both motors and control. The "Frequency" and "Certainty" sections are the meat of the whole situation because the final decision as to whether or not a mechanism should be used in preference to an electrical unit is finally rooted in the fact that ruggedness comes before low cost.

We have recently adopted a design to overcome the 50/60-cycle difficulties you mentioned. This merely consists of a 0.25-kva transformer with a dual-voltage primary (220/440) connected to one phase of the line and a dual-voltage secondary (110/95) for the entire control circuit so that the identical holding coils may be used on either frequency. Further merit comes from the safety feature of the low-voltage control circuit plus a flexibility of application on line voltages which will allow this one panel to be used on about 85 per cent of the machines for which it is designed. The taps allow for a simple change, even after the machine is stocked, because they are brought out to a small terminal block and the whole thing only adds about $10 to the cost of the entire machine tool—a small premium for its manifold benefits. Twenty-five-cycle or 550-volt currents account for nearly another ten per cent but still have the transformer, with fixed ratio windings.

B. N. Foster (nonmember; Kingsbury Machine Tool Corporation, Keene, N. H.): We have read this paper over and agree with practically everything you say. Some of the points which you bring up are not of much interest to us personally and there are some points which you do not bring up which are of vital interest to us, but from our small knowledge of the subject you have covered the milling and grinding machine situation very thoroughly and accurately.

I am a little bit sorry not to be able to offer some sort of constructive criticism here for I realize that your request for criticism was seriously made and that other viewpoints might be of use to you in your work with the committee. However, I can't think of anything offhand to talk about unless I get into a long discussion of some points not brought up in your paper which have no direct relation to the particular corner of machine tools which you are covering, and I don't know whether you would want this information at this time, and furthermore I don't know how we would have time to do this.

You realize, of course, that I am very much interested in the work which is being done by your committee in an effort to straighten out this entire electrical snarl and will be very glad to do anything I can to help. At present, however, the business conditions here do not permit me to spend very much time on matters of this sort. However, if specific points come up on which you would like to have me express an opinion, I would be only too happy to do so and would like to have you feel free to write me at any time on these matters and hope that I will be able to answer you satisfactorily.

G. H. Garcelon (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): I think you will find that manufacturers of industrial control keenly appreciate the problems encountered in machine-tool applications and are giving them considerable thought in their development programs. One characteristic difficulty is that several of the features required tend to increase the cost and size of the equipment which is, of course, directly opposed to the general desires of the machine-tool industry. You have cited items in your paper directly illustrating this point, as for example under the section titled "Certainty of Operation." There are several ways to secure greater accuracy in the time of operation and to decrease the possibility of failure to operate, but unfortunately all of them increase cost, size, or both.

In regard to the ease of changing the coils from 50 to 60 cycles you state that tests indicate that 60-cycle coils are fully qualified for 50-cycle operation. Normal 60-cycle coils and magnets are designed for an operating range of 85 to 110 per cent of rated voltage which is considered necessary to insure satisfactory operation. If these coils and magnets were designed to cover 50 cycles, as well as 60 cycles, it would be the equivalent of applying approximately 20 per cent greater operating range and this would necessarily mean larger and more expensive magnets and coils.

Some tests may indicate that commercial designs seem adequate for this range, but I am afraid that certain conditions have been overlooked. The operation may be satisfactory in the open or in a large enclosure, but the manufacturer must design these units so that they may also be operated in small enclosures which may represent an increase in effective temperature of the air surrounding the coil amounting to 20 to 30 degrees centigrade. The tendency of manufacturers is to design for a more conservative maximum temperature of coils than is permitted by the standards in an attempt to avoid coil failures after several years' operation under somewhat adverse conditions.

The percentage of 50-cycle applications is small and the coils are rather inexpensive, therefore the best solution of this problem would seem to be along the lines of making the designs such that coil changes can be made readily.

F. H. Penney (nonmember; General Electric Company): I am sure the manufacturers of electrical equipment are deeply indebted to Mr. Graves for his paper. It is at once evident that a great deal of painstaking thought has been given to the selection of the proper control units.

I would like to comment on the reference to the difficulties encountered because of the various voltages and frequencies found not only in the United States but abroad. Unfortunately it has not proved practical to permit the operation of magnetic switches through voltage or frequency ranges wide enough to warrant a dual voltage or frequency rating unless voltage and frequency vary in the same ratio and even then only to a limited extent. Size limitations and the normal voltage fluctuations encountered on standard power systems require about all the margin in design which can be built in and maintain the highly desirable features now provided without adding a range on top of this.

However, a scheme used on certain large panels and to some extent on smaller units offers one way toward the goal of a standardized panel for machines through the 50/60-cycle range with a voltage range from 200 to 550 volts. The inclusion of 50 cycles is particularly important because of the foreign business now coming to machine-tool builders.

The plan briefly is to segregate the control circuit entirely from the motor or power circuit by means of a transformer, selecting some standard control-circuit voltage such as 220 volts, 60 cycles. The transformer, it is true, adds something to the cost. However, the advantage is likely to be considerable where machines are produced in lots of 25 or more since all magnetic switches can be equipped with 60-cycle 220-volt operating coils. Only the overload relay heaters need await assembly when the order comes in specifying voltage and frequency.

The transformer primary equipped with taps can provide for the voltage range 208, 220, 440, 550, 60 cycles; the secondary voltage will remain 60 cycles 220 volts. Likewise if 50-cycle systems are to be provided for, taps are available primary and secondary whereby the correct voltage for 50-cycle operation can be secured. A small terminal board primary and secondary will make possible the correct connections of numbered terminals with very little time or trouble.

The transformer size need only be great enough to care for the maximum number of switches which will be energized at any given time. The cost is more than likely offset by the reduced spares and the reduction in delays occasioned by special coils, etc.

Mr. Graves also refers to the question of a suitable tape for use with Flamenol. This problem seems well along toward a solution and samples of a tape which will conform to the characteristics of Flamenol are being tried where oil and other compounds have made such a product a necessity. The preliminary reports indicate that very shortly a general distribution of the new product can be made.

R. S. Elberty (Landis Tool Company, Waynesboro, Pa.): Mr. Graves' paper is indeed a timely one. There is a growing field for electrical engineers in the machine-tool industry. The electric milling machine and electric grinder are excellent examples of the results obtained in applying good electrical engineering to machine tools. This subject has not received the attention it merits from electrical engineers in general or from the AIEE. Instead, we find such electrical subjects discussed by the ASTE, ASME, or the Machine Tool Electrification Forums held by Westinghouse.

Electrical drives must compete with mechanical or hydraulic devices that are well known to the average designer of machine tools. A. E. Rylander of the Midland Steel Company discusses "The Trend to Hydraulics" in the *Tool Engineer* for April 1939. The 1935 Machine Tool Show disclosed many advances in electric drives, but hydraulic drives had also developed rapidly. In 1939 we shall probably see more evidence of the very keen competition between hydraulic and electric drives.

The electric drive offers many advantages to the machine-tool designer. We have our troubles, as Mr. Graves outlines, but the electrical manufacturers have always been very helpful in developing and applying equipment to meet our needs. Our problems are specific and individual depending on the type of machine tool to which the drive is applied.

Many of my friends have suggested the formation of a society of machine-tool electrical engineers. I do not favor such a move. We can get together on electrical problems through some one of the existing engineering organizations. Perhaps this paper of Mr. Graves will bring about an aroused interest in machine-tool electrification among electrical engineers in general. Certainly there is room right in the AIEE for more papers of this nature.

B. P. Graves: In reply to P. H. Wilmarth's discussion on the paper entitled "Electrical Equipment on Machine Tools" I would like to say that we have done this in the past, although our control voltage is usually twice as high, namely, 220 volts on 60 cycles, and 187 on 50 cycles.

The objection raised is that it makes it more difficult for foreign users to obtain coils, that is, they can sometimes obtain 220-volt 50-cycle coils, but not 220-volt 60-cycle coils.

Because of this difficulty in obtaining 220-volt 60-cycle, we have refrained from using the transformer.

In reply to G. H. Garcelon, I would like to state that the reason that this subject of 50/60 coil was put into the paper, was to bring out the various difficulties and possibilities along this line. If this feature could be accomplished, it would be a material saving in our estimation, to machine-tool builders, and of course we in the machine-tool industry will have to be governed by the various opinions of the manufacturers of controls.

Our reason for suggesting this thought was the fact that we have been so successful in the use of 50/60-cycle motors.

In reply to the discussion as entered by F. H. Penney of the General Electric Company—there are several points in Mr Penney's discussion which are of considerable interest, on some of which I feel I would like to express my opinion.

In reference to the difficulty encountered because of the various voltages and frequencies found not only in the United States, but abroad, I rather take exception to Mr. Penney's remarks when he says it has not proved practical to permit operation of magnetic switches through voltage or frequency ranges wide enough to warrant a dual voltage or frequency rating. This has been an argument for sometime among the electrical manufacturers, but now we find one company which has agreed to our plea and in starting this dual-voltage arrangement, several other companies are now following, and this is proving to the machine-tool industry a big saving, both in time and patience. I feel that with a little further study, all control manufacturers will follow along this line.

In reference to adding a transformer, I feel that the general idea is good, and some day we may feel obliged to follow this practice, but it does increase the difficulty of foreign purchasers obtaining a correct supply of 60-cycle 220-volt operating coils. This is one of the points that has been brought up to our attention on many occasions by foreign purchasers.

In reference to suitable tape for use with Flamenol, I am pleased to state that since the meeting we have received two large sample rolls and at the present writing this tape looks exceedingly good to us and may be the means of solving one of our very annoying problems.

In reply to Mr. Elberty's discussion on the paper, there is very little that I can say other than to agree thoroughly with the points that he has brought out.

In reference to the point of machine-tool builders having to compete with mechanical and hydraulic devices, I think this is a point well taken as in my opinion, the electrical companies don't seem to realize that this is a matter that must be most thoroughly investigated by the average designer of machine tools, because it is very imperative in bringing out a new design that it should be in very close alignment with competitive machines, both for price and features.

Regarding the point of designing electric drives and the electrical manufacturers having been very helpful in developing and applying equipment to meet our needs, because of the fact that our problems are specific and individual, depending on its type. This is one of the reasons why it is becoming much more difficult for electrical companies to do the engineering for the machine-tool manufacturers, but I do feel that it should be possible for us to get together with electrical manufacturers, more especially now that we machine-tool builders are inclined to consider the electrical end as part of our daily work, and in tying our two organizations together, to bring about much more interest in the electrification of machine tools.

Modern Trends of Low-Voltage Air Circuit Breakers

J. W. SEAMAN
ASSOCIATE AIEE

Synopsis: The history of low-voltage air circuit breakers has paralleled the development of power distribution. The desire for improved contacts led from an automatic knife-switch type of circuit breaker to an air circuit breaker equipped with a laminated copper brush. The trend from d-c systems to polyphase a-c systems led from the "nontrip free" type of air circuit breaker to the "trip free" breaker; and now the requirements for low maintenance, safety of operation, increased continuity of service, together with the trend to steel enclosing of switchboards and individual devices, is being met by new air circuit breakers equipped with solid silver main contacts, alloy arcing tips, and a new type of arc interrupter herein described.

THERE has been a considerable change in air-circuit-breaker construction during the past several years, due principally to the increased requirements for low maintenance, safety of operation, and increased interrupting ability. In order to emphasize the recent improvements, a review is made of the history of air-circuit-breaker construction.

The first device, which may be classed as an air circuit breaker, was little more than an automatic knife switch. This device probably originated about 1885. Figure 1 illustrates a typical breaker of this construction. Two fundamental requirements, which we have to this day, were met by this device, that is, an operable contact member to open and close an electrical circuit, and an overcurrent trip responsive to abnormal circuit conditions. The movable contact blade was held in the closed position by a mechanical latch that could be released by the overcurrent trip thus causing the opening of the blade and consequent interrupting of the circuit. Secondary contacts of carbon and copper were utilized with this type of device to take the burning on opening and closing of the circuit.

The next major step occurred about 1892 and dealt with the current-carrying ability of air circuit breakers. At this time we find the knife-blade type of contact being superseded by a laminated-brush contact. Improved secondary contacts were also provided for protecting the main current-carrying contacts during opening and closing of the circuit. Figure 2 shows a typical device of this period. This construction was a distinct improvement for carrying current continuously, largely due to the wiping action of the several laminations employed in the main contacts.

Also during this period the first air circuit breakers utilizing a magnetic-blowout chute were produced in answer to the demand for a satisfactory breaker for the many d-c railway projects then active. Figure 3 shows a typical breaker of this period equipped with such a chute.

The three-phase a-c system appeared during this time. A-c motors and other connected devices were then quite small and easily controlled, so that it was not

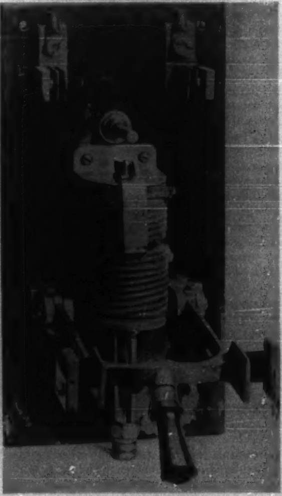

Figure 1. Knife-switch type of air circuit breaker

Paper 39-110, recommended by the AIEE committee on protective devices, and presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3-5, 1939. Manuscript submitted March 24, 1939; made available for preprinting April 14, 1939.

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until 1905 that the increase in the size of such systems required suitable a-c air circuit breakers. The development of three-phase breakers brought the first real regard for safety of operation. Prior to this period, air circuit breakers for d-c service had been what was popularly called "nontrip free," that is, the operating handle was definitely tied mechanically to the movement of the breaker contacts, and as a result it was possible for operators to hold such breakers in against a severe overload merely by holding the handle in the closed position. As can be readily appreciated, the closing of a breaker of this type against a severe overload or short circuit could readily result in disastrous consequences. The generally accepted procedure for coping with this situation was to utilize a switch in series with the circuit breaker, which could be closed after the circuit breaker had been latched in, thereby permitting immediate opening of the air circuit breaker if the switch was closed against abnormal circuit conditions. To eliminate the necessity for such switches, the so-called "trip free" breaker was generally adopted. With an air circuit breaker of this type, closing against abnormal circuit conditions results in the operating handle being disengaged mechanically from the movable contact structure, rendering impossible the holding in of the breaker under abnormal circuit conditions. Breakers of this type were first brought out around 1905, and as illustrated in figure 4, formed the basic model for all air circuit breakers manufactured during this period up to approximately 1930. The similarity in construction can be readily appreciated by comparing the air circuit breaker shown in figure 4 with the one illustrated in figure 5.

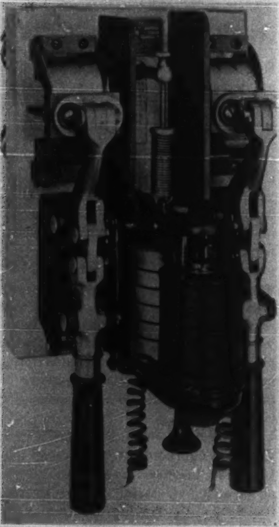
Figure 2. Early breaker with laminated brush

Figure 3. Early magnetic-blowout air circuit breaker

This résumé, while very brief, covers the general improvements up to date. The major contributions to the art appear to be the development of the laminated-copper-brush contact and the trip-free type of mechanism. The use of carbon-tipped arcing contacts which separated in air provided the means for arc extinction. Figure 5 illustrates the type of air circuit breaker in general use in 1930 and in some instances, with minor modifications, still available commercially.

Modern Air Circuit Breakers

In the light of what has transpired in the years prior to 1930, the trend of the past eight years is worthy of note. During this period satisfactory answers to the demand for low maintenance, increased continuity of service, greater safety, and improved current-interrupting performance have become increasingly important. A considerable premuim has also been placed upon such physical factors as space requirements and weight. The work done during the past several years has, we believe, satisfactorily answered and met most of these modern requirements.

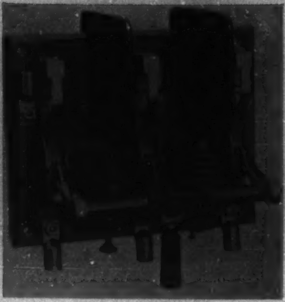
Figure 4. Early trip-free air circuit breaker

Contacts

The first requirement to meet, and the most important at the time, was developing main current-carrying contacts which would require relatively little maintenance and insure the greatest continuity of service. The main contacts utilized prior to this period were of the laminated-brush construction, each lamination consisting of a piece of spring copper. While these contacts were generally satisfactory, it is necessary to acknowledge that copper, in itself, has undesirable oxidation characteristics. If through poor maintenance copper oxide is allowed to form on the contacts, cumulative heating is carried to the point where the spring copper laminations are annealed and the service impaired or interrupted.

Also, the increasing development of industrial processes which involved intermittent circuit overloads as a normal part of the processing cycle made it desirable to develop a contact which would withstand overheating that might cause the laminated-brush type of contact to fail.

After many laboratory tests on numerous materials, pure fine silver was selected as the contact material which would satisfactorily solve these problems. Silver stood out among the materials tested, primarily because of its low contact resistance and stability. Relatively little oxidation occurs on silver. This oxide breaks down at comparatively low temperatures and pressures. Copper oxide on the other hand is mechanically tough and requires rather high temperatures to break it down.

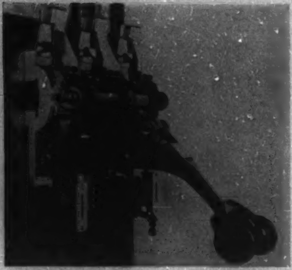

Figure 5. Trip-free type of air circuit breaker, manufactured in 1930

Figure 6 shows a typical contact construction employing fine silver blocks for the main contact surfaces. Contact pressure is obtained by a separate compression spring not in the current circuit. As may be appreciated, a contact of this type may be subjected to severe overload conditions without impairing its current-carrying ability. As may be noted the silver blocks on the movable contact have a convex surface to obtain a line contact. This utilizes efficiently the pressure of the contact spring and assists in breaking down the silver oxide that may form. Figure 7 furnishes a comparison of the physical size of a laminated copper brush and a silver-faced contact of the same current rating. The considerable reduction in size, obtained by the use of silver, has contributed materially to the building of more compact breakers as well as reducing the interrupting time due to the lowering of the inertia of the movable-contact structure. About eight years of field experience with this type of contact has been obtained with very satisfactory results. Following this favorable experience, all of our modern air circuit breakers have been equipped with this type of contact.

Similar work has also been done on improving the arcing contact structure associated with the main current-carrying contacts. The use of alloys which have a much lower resistance than the carbon materials formerly employed and also greater mechanical strength provides the greatest improvement in this particular phase of construction. These alloys permit much better current transfer from the main contacts during arc interruption than was formerly enjoyed.

Interrupting Units

The next problem dealt with the necessity for handling large blocks of power, due to the substantial increase in the short-circuit capacity of a-c systems. Until recently, no particular attention had been paid to the interrupting capacity of air circuit breakers as the connected capacity was generally small. A listing of interrupting ratings for low-voltage air circuit breakers was published on May 26, 1938, by the large-air-circuit-breaker group of the National Electrical Manufacturers Association under section *SG*-7-26 and is the first generally recognized list of air-circuit-breaker interrupting ratings. It had been generally considered that in low-voltage a-c systems it would be impossible to get short circuits of much more than 20,000 amperes. However, in recent years increased system capacity and larger industrial processes have resulted in the possibility of obtaining short-circuit currents of the order of 60,000 to 80,000 amperes, and in some instances even higher. Thus the necessity of air circuit breakers with adequate and proved interrupting ratings. This, coupled with the fact that in the interest of safety and low cost of installation, there has been a considerable trend toward factory-built steel-enclosed switchboards and steel-enclosed individual breakers, necessitated a considerable study for adequate and safe means of controlling high-power arcs.

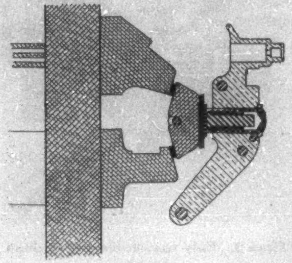

Figure 6. Typical silver-contact arrangement

While prior to 1930 considerable work had been done on arcs in oil, relatively little had been done concerning the study of high-capacity arcs in air. It is granted, of course, that the addition of arcing contacts to air circuit breakers, as shown in figure 5, would considerably increase the current-interrupting ability of air circuit breakers, particularly if considerable head room above the arcing contacts is provided. In addition to this, the magnetic-blowout chute, as shown in figure 8, is adapted to interrupting large amounts of currents if no difficulty in the nature of adequate air space over the top of the chute is encountered. As a matter of fact, the device, as shown in figure 8, equipped with magnetic-blowout chute has satisfactorily interrupted currents of the order of 330,000 amperes root-mean-square in a remarkably short time, as is illustrated in figure 9. Such a test demonstrates very clearly the rapidity with which currents of such magnitude can be completely extinguished. It is not feasible, however, to apply such breakers in the limited compartments associated with steel-enclosed air circuit breakers, the reason being that inadequate head room will greatly impair the interrupting ability of such a device. The satisfactory handling of currents of large magnitude in small steel compartments, therefore, greatly complicated the problem of developing a suitable arc interrupter which would be applicable over a wide range of interrupting ratings. After investigating several different ways of attacking this problem, the "pin-type arc quencher" was decided upon as the type of interrupter most generally applicable to a wide range of interrupting ratings. This device has now been successfully applied to air circuit breakers whose interrupting ratings range from 10,000 amperes to 80,000 amperes, at 600 volts alternating current.

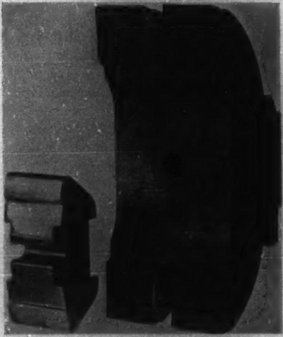

Figure 7. Size comparison of solid-silver contact and laminated-copper-brush contact

A typical arc chute of the pin type is shown in figure 10. A pair of soft-iron U-shaped magnets is attached to the alloy arcing contacts to drive the arc rapidly into the pins located above. The pins are positioned with respect to each other so as to develop the maximum voltage gradient. As the arc is driven into the pins it is broken into a series of arcs which travel through the pins until extinction is complete. Located above the pins is a series of baffles spaced apart so as to provide a series of narrow slots through which the gases generated by the arc escape. The width of each slot is proportioned so that the arc itself is confined within the chute. This, coupled with the cooling of the arc gases in traveling through the slots, eliminates the possibility of the arc striking to nearby grounded metal parts. The extinction of the arc is due primarily to two reasons—first, to the voltage gradient obtained from the sum of the cathode drops of the series arcs, and second, to the effective projection of the arc against efficient cooling surfaces resulting in rapid deionization of the arc stream.

Table I. Data From Three-Phase Tests on a Type AL-2, 6,000-Ampere Air Circuit Breaker Equipped With "Pin Type" Arc Quenchers

Duty—O, Test—600 Volts, Three Phases, 60 Cycles

Current (Amperes), Maximum Loop		Duration of Short Circuit (Half Cycles)	Arc Duration (Half Cycles)
Peak Value	Root-Mean-Square Value		
52,000	30,000	7.0	0.9
59,000	34,000	7.1	0.7
79,000	46,000	7.1	0.7
103,000	62,000	6.4	0.5
128,000	76,000	6.7	0.9
83,000	50,000	6.7	0.7
125,000	76,000	6.0	0.8
116,000	78,000	6.4	0.8
137,000	80,000	6.4	0.7
166,000	100,000	6.0	0.8
245,000	142,000	5.9	0.5
192,000	114,000	5.8	0.7
192,000	119,000	5.5	0.7
210,000	134,000	5.7	0.7
220,000	130,000	5.6	0.6

The chart illustrated on figure 11 gives a comparison of the arcing times of a conventional open-break type of air circuit breaker tested without an enclosure and an enclosed air circuit breaker equipped with "pin type" arc quenchers. It may be noted that the arcing time of the former is quite erratic even when tested in the open, where the enclosed "pin type" breaker interrupts consistently over a wide current range in two half cycles or less. Operation of this sort greatly reduces the stress on the equipment to be protected and contributes materially to the safety of the operating personnel. Table I shows data from interrupting tests conducted on a 6,000-ampere air circuit breaker, equipped with pin-type arc quenchers, at 600 volts, three phase, up to and including a maximum current of 125,000 amperes. It is to be noted that the arcing times are one-half cycle or less, and indicates definitely the consistent performance of this type of interrupter. Figure 12 shows an oscillogram of the maximum current interrupted during this particular test series.

In figures 13 and 14, typical breakers equipped with pin-type arc quenchers for interrupting ratings of from 10,000 to 80,000 amperes are shown. The introduction of such devices constitutes a major step in the art. Experience during the past three years, since these interrupters were introduced to the field, has indicated a very satisfactory performance. Needless to say, due to the efficient way in which these interrupters control the arc, outward objectionable disturbances such as noise, hot arc gases, and other volatile arc products have been greatly reduced, so much so that typical steel-enclosed breakers of this type manifest no outward disturbance even when tested at their full interrupting ratings.

General Structure

Figure 15 illustrates a more recent trend of air-circuit-breaker construction. Heretofore, the majority of air circuit breakers have been of the manually operated type. Recently, however, there has been a decided increase in the use of air circuit

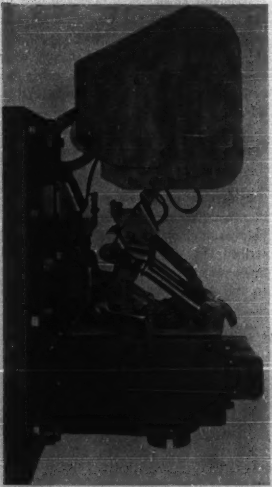

Figure 8. Modern magnetic-blowout type of air circuit breaker

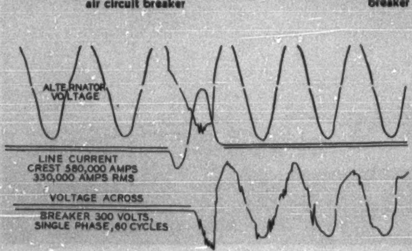

Figure 9 (below). Oscillogram of high-current test on modern magnetic-blowout type of air circuit breaker

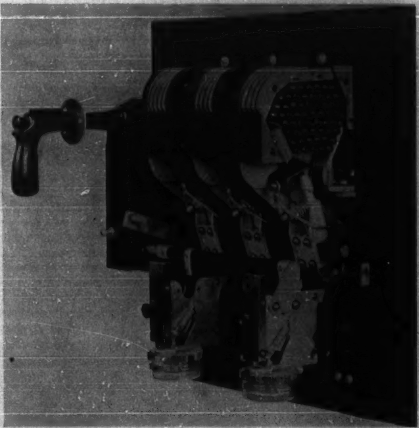

Figure 10. Typical pin-type arc-quencher construction

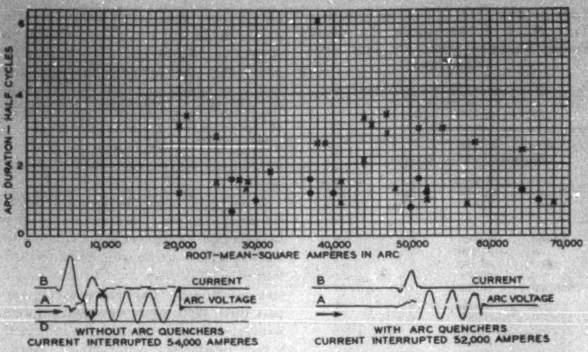

Figure 11. Comparison of arcing times on an open-type carbon-break air circuit breaker and an enclosed air circuit breaker equipped with pin-type arc quenchers

Type *AL-2* air circuit breaker, 600 volts, 1,200 amperes, interrupting rating 40,000 amperes

Crosses—Tests at 600 volts, single phase, plain break, open

Circles—Tests at 600 volts, single phase, with arc quenchers

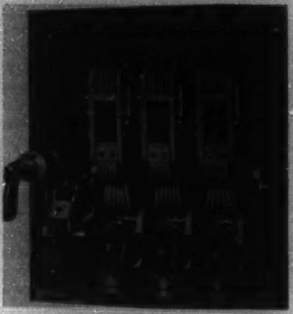

Figure 13. Modern small air circuit breaker equipped with pin-type arc quenchers

breakers which can be operated satisfactorily from a remote point. The generally accepted practice for coping with such a requirement has been to equip an air circuit breaker of the manually operated type with a separate electrical mechanism which, while perfectly satisfactory from an operation standpoint, requires a considerably larger space for mounting. As this trend is associated, however, with the increased use of steel-enclosed air circuit breakers, it is obviously a considerable handicap to increase the size of such steel enclosures merely to provide the necessary additional room for the electrical operating mechanism. To meet this demand without sacrificing space, there has been developed the air circuit breaker structure as shown in figure 15. These latest air circuit breakers require exactly the same space for either manual operation, or electrical operation. This is obtained by simply applying either a suitable manually operated unit or electrical operated unit to the main body of the air-circuit-breaker mechanism. As can be readily realized, this type of construction has met with considerable favor in the field and is, we believe, indicative of the general trend in all future designs.

Summary

From the foregoing, it is evident that considerable progress has been made in increasing the general usefulness of low-voltage air circuit breakers. We have described the evolution from an adapted knife-switch device up through the years to the present-day air circuit breakers

Figure 12. Typical oscillogram of an interrupting test on a 6,000-ampere air circuit breaker equipped with pin-type arc quenchers

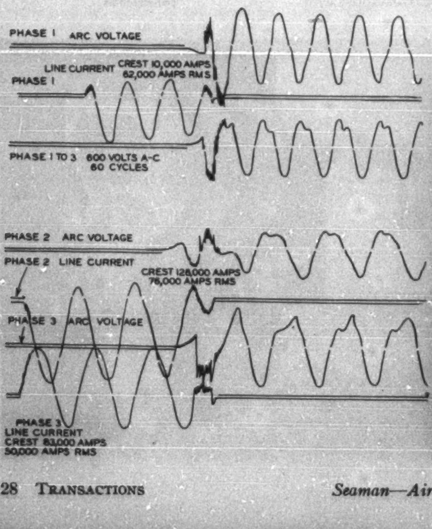

Figure 14. Modern large air circuit breaker equipped with pin-type arc quenchers

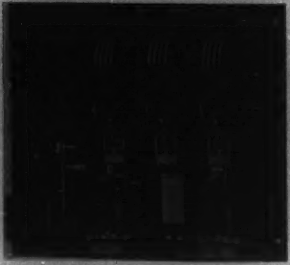

Figure 15. Modern electrically operated air circuit breaker

applicable to all low-voltage power and lighting circuits where continuity of service, safety of operation, and low maintenance are desired. The development of the solid-silver line-contact construction has contributed materially to the increased speed of operation, reliability of service, and reduction of size which are such paramount factors today. The pin-type arc quencher is a material contribution to the safety of personnel and protection of equipment, and its performance renders possible the use of air circuit breakers in steel-enclosed compartments without impairing the interrupting ratings.

Discussion

A. E. Anderson (General Electric Company, Philadelphia, Pa.): Mr. Seaman has described certain developments that have taken place in order to produce low-voltage air circuit breakers having improved performance features, such as low maintenance, improved current-interrupting performance, together with reduction in weight and space requirements.

Any improvement in the design of the main current-carrying parts, which will result in reduced maintenance and increased continuity of service, is a step in the right direction. However, the question frequently arises as to the necessity of circuit breakers of interrupting ratings in excess of 40,000 amperes. A rule of thumb that has been quoted many times is that it is difficult, if not impossible, to obtain currents in excess of 20,000–40,000 amperes on circuits up to 600 volts alternating current, due chiefly to the presence of contact or arc resistance. Experience has shown that it is possible to obtain currents in excess of this value.

Outside of machine characteristics, the factor of resistance is the only one that appears in connection with d-c circuits and there seems to be no question that very large currents are possible and values in excess of 200,000 amperes at 600 volts have been obtained. The resistance found in such circuits during fault conditions is exceedingly low. Accordingly, due allowance has been made in selecting circuit breakers for such applications.

As far as a-c circuits are concerned, we must consider in addition the reactance of the external circuit and that of the generating or transformer equipment. The per-unit reactances of low-voltage generators are usually considerably less than those in the higher-voltage machines, thus tending to increase proportionately the value of short-circuit current. Also, the reactance of low-voltage conductors is less than that of higher-voltage conductors due to decreased spacing. Looking at this from the voltage-regulation standpoint, it would be expected that the allowable maximum regulation would not exceed ten per cent for satisfactory operation of motors and other apparatus. Accordingly, the short-circuit current available at the time that the circuit breaker opens would be at least ten times the continuous-current rating of the circuit. This result takes into consideration the circuit resistance as well as the circuit reactance.

In the case of switchgear, the maximum short-circuit current is obtained in the vicinity of the circuit-breaker terminals, which means that only the connection copper, busses, cables, and other primary circuit apparatus between the point of short circuit and the generator or transformer should be taken into consideration. Generally speaking, less than ten per cent of the total impedance of this part of the system is found in the cable or apparatus external to the generator or transformer. In fact, this impedance is usually so low that it can be neglected. A further consideration shows that the resistance component of this impedance has a minor effect on the calculated result. The use of silver at the various points of contact in the switchgear, further tends to reduce this factor. Faults some distance from the switchboard, such as at the end of feeder cables, or at some group bus supplying motors or other apparatus, will usually result in lower short-circuit current due to the relatively higher impedance of these smaller cables.

This then leaves the item of arc resistance and its contribution to the over-all result. An open arc is a random affair. Mr. Seaman's paper brings out the design features in a circuit breaker that have to be taken into consideration in order to control the arc and keep it confined within a restricted space.

As the installation becomes larger, a greater amount of short-circuit current is developed and a greater space would be required for the interruption of any free or open fault arc. The spacing of conducting parts at the rear of the switchboard does not permit this arc to expand to the extent that its resistance would have an appreciable effect, or to reduce the current to one-third or one-half of its calculated value, which would sometimes be required in order to limit its value to 40,000 amperes as mentioned above. Therefore the effect of resistance of any open or free arc that might be obtained on the circuit is omitted in calculating the short-circuit current.

The mechanical and thermal requirements of current transformers, connection copper, and associated apparatus must also be taken into consideration. For example, the bus may not only be required to have a rating as high as 10,000 amperes, or more, but may have to withstand stresses imposed by fully offset values greater than 200,000 amperes root-mean-square. This part of the application brings in the stress set up by the electromagnetic forces, coupled with vibration at fundamental and harmonic frequencies, due to resonance between bus, supports, and the frequency set up by the electrical system.

Modern practice calls for an enclosed type of circuit breaker. This feature provides a safe and compact installation and, in one variety of switchgear, a design that permits ready means for withdrawing the unit for inspection or maintenance. The circuit breaker is so interlocked that it must be in the open position before it can be connected to or disconnected from the bus. The smaller units are arranged vertically, usually three high. The larger and heavier units are mounted on one level only and rolled into position.

Units of this kind have been applied to motor, generator, transformer, lighting, station auxiliaries, and other loads. Provisions are included to facilitate shipment and installation, as well as to permit future expansion. The latter possibility should include a consideration of the maximum amount of generating or transformer capacity in order to meet the short-circuit requirements that may arise in the future.

D. C. Prince (General Electric Company, Philadelphia, Pa.): From time to time the question has been raised whether it is necessary to have interrupting ratings of air circuit breakers of the order of 40,000/60,000 amperes or more where these circuit breakers are installed on 440- or 600-volt circuits. It has been contended that even though calculations might show such currents could exist, actually in practice they would not, and, therefore, discount factors have been employed, and smaller circuit breakers have been installed than those bearing the ratings of the calculated maximum short circuit. There was a feeling for some time that for some reason or other short circuits actually obtained did not correspond to calculated values. In the last year or two a good deal of work has been done along this line, taking great pains to include in the calculation allowance for all available reactance and resistance, and it is now certain that where the calculation is made with sufficient care, the currents for a bolted short circuit will correspond very closely to the calculations.

The next question raised is whether an actual short circuit occurring in the field may have the properties of a bolted short circuit. It goes without saying that most short circuits will not have such properties. Particularly in calculations involving control apparatus, the short circuits are likely to include considerable cable runs, and when a breakdown does occur, such as between winding and ground in a motor, the fault is likely to have a considerable amount of impedance.

On the other hand, where a switchboard is involved, any fault occurring in the switchgear structure may approach the full value of a bolted short circuit. The question has been raised whether a foreign body such as a copper bar will not be blown clear, say from a bus structure across which it has fallen, without giving currents of greatest magnitude. Tests of this particular configuration have been made with the result that currents of the order represented by the circuit breaker ratings will blow a solid bar clear but not quickly enough to prevent the current from reaching its maximum value. Even in the switchboard structure, most faults are likely to have less value than the maximum calculated fault. For instance, a short section of control wiring getting across the busses will be fused almost instantaneously and may produce an arc of sufficient length to keep the current well below the maximum possible fault. However, if the calculation has been carefully made, it is certain that the maximum fault current calculated may occur, and for that reason air circuit breakers should be installed with ratings equal to or greater than the maximum calculated fault if it is necessary to be sure that the fault will be cleared by each breaker in question.

In considering the application of air circuit breakers, however, there is one more point which should be covered. If an oil circuit breaker is incorrectly applied and is stressed beyond its capacity and fails to clear, serious damage may result. The circuit breaker may burst, allowing the escape of oil, which may produce secondary damage of considerable magnitude in addition to the loss of the breaker itself. An installation of oil circuit breakers under conditions where the maximum fault is known to be beyond the circuit breaker capacity may represent a real hazard and not merely a business risk. On the other hand, if an air circuit breaker is placed in a circuit where the maximum possible short circuit may exceed its interrupting ability, the circuit breaker itself may be burned up, but secondary damage is likely to be of a relatively minor character.

It has been found that where a number of small circuit breakers are backed up by one larger one, which is free to trip instantly in the event of a heavy short circuit, the cascade combination will satisfactorily clear a fault up to the capacity of the larger circuit breaker. Tests have been made to verify the range in sizes for which the smaller breaker will be protected. With these factors in mind, it is possible to design a distribution system including various sizes of air circuit breakers, the smallest of which are of a rating less than the maximum short circuit which they may be asked to clear. In the unlikely event that such a short circuit does occur, it should not be necessary to replace the particular small circuit breaker involved, but even if the smaller breaker is damaged, this may be regarded as a legitimate business risk. In such an installation, of course, there must always be some circuit breakers of adequate capacities to take care of any fault which may possibly occur, and of adequate speed of operation to afford some real protection to the smaller breakers. Air circuit breakers are available which have these characteristics, proved by actual tests, both free and in enclosures, sufficient to meet the most exacting demands.

In past times, the application engineer has faced the dilemma that if he recommended large enough capacities to be safe, his installation was uneconomical, while a recommendation, sound from an economic point of view, was hazardous to both life and property. With knowledge of the new air circuit breakers, he can, by suitably selected and proportioned backup protection, secure a practical solution of this problem. Small air circuit breakers protect utilization equipment from all but the exceptional faults at a minimum of cost. Master breakers of adequate rupturing capacity back up the first line and protect both equipment and small breakers from the exceptional fault.

Special Problems of Two-Pole Turbine Generators

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THE two-pole turbine generator differs from similar generators with a higher number of poles, in that the mechanical rigidity of the rotor body varies from a maximum value on the pole axis to a minimum value on the winding axis, and the stator core is subjected to radial magnetic forces on only one diameter corresponding to the axis of the resultant magnetic field.

The unequal mechanical rigidity of the rotor body on the two major axes results in a variation in the static deflection of the rotor as it rotates. This variation in the deflection occurs at a frequency equal to twice the rotating frequency and acts as a double-frequency vibratory force in causing the rotor to vibrate.

The unequal magnetic forces on any two major axes of the stator core cause it to distort and take an elliptical shape, with the short axis of the ellipse coinciding with the axis of the air gap magnetic field. The distorting magnetic forces rotate with the rotor and thus produce a stator core distortion which rotates at synchronous speed. Since the elliptical shape rotates with the rotor, the resulting vibratory motion of any point on the stator core has a frequency twice that corresponding to the rotating speed.

These rotor and stator double-frequency vibratory forces and resultant vibrations are present in every two-pole generator irrespective of rotational speed or physical proportions. The vibration is more pronounced in the case where the frequency of the forces is approximately equal to the resonant frequency of the part in question. The effects are relatively small in 25-cycle units due to the fact that the frequency of the disturbing forces is low with respect to the resonant frequency of the generator and foundation parts. They are also negligible in small-capacity 3,600-rpm generators because of the relatively small double-frequency magnetic forces involved. Double-frequency stator vibrations have been observed on two-pole 60-cycle generators for ratings of 18,750 to 20,000 kva. Both types of double-frequency vibrations become of increasing importance for 60-cycle 3,600-rpm units of relatively large capacity. In such units of larger capacity, the double-frequency vibratory forces are transmitted to mechanically connected associated apparatus, bearing supports, foundations, and station structural parts, and result in objectionable vibrations and noise.

It is the purpose of the present paper to discuss the general problems of double-frequency rotor and stator vibrations of two-pole generators and present methods by which the disturbing force producing the vibration can be eliminated or the vibration restricted to specific isolated parts.

Double-Frequency Rotor Vibration

The general problem of 3,600-rpm-generator double-frequency rotor vibration due to unequal mechanical rigidity on the two major axes of the rotor was investigated by the writers in 1928 and 1929. Rotor models, dynamically similar to the rotors for the largest 3,600-rpm generators which were then contemplated for the future, were made. Running tests were made on the models over a speed range from zero to 4,500 rpm, and vibration measurements were made for

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the entire speed range. The following facts were obtained:

1. The models vibrated with quite large amplitudes at rotating speeds corresponding to one-half their fundamental natural frequencies. The frequency of the vibration was twice that of the rotation of the models.

2. The double-frequency vibration persisted throughout the entire speed range and it was not influenced by changes in the balance of the models.

3. The models were found to have two fundamental natural frequencies corresponding to the two principal rotor rigidities. These critical frequencies were about 50 rpm apart and were less than one-half the normal operating speed of 3,600 rpm. The vibration of the models throughout this 50-rpm speed range was unstable, that is, the amplitude of vibration would steadily increase when the speed was held constant.

4. The single-frequency vibration predominated throughout this 50-rpm speed range and over this speed range the models were quite sensitive to the amount of unbalance present.

The following conclusions applying to large-capacity 3,600-rpm generators were reached from a study of the data obtained from the model tests:

1. The internal damping in the rotor body of an actual generator due to the friction of the conductors on the coil slot sides was considered to be sufficient to ensure stable operation of the rotors throughout the total speed range.

2. Rotors of the length in question were known to be more difficult to balance than the shorter, more rigid ones, but it was thought that with proper care being given to the placing of the balance weights along the axis of the rotor, the job could be done without difficulty.

3. It was realized, before the model tests were made, that the double-frequency vibration would persist throughout the speed range of the rotor, but no trouble with it was anticipated because of the great difference in speed between one-half the first critical speed and the normal operating speed.

Since these tests were made, 3,600-rpm turbine generators have been built with conventional rotor designs for ratings of 43,750, 50,000, and 58,750 kva. The running performance of the rotors of these generators has demonstrated the accuracy of our predictions as based on the model tests. In no case did any of the above rotors give evidence of instability or excessive vibration at either the fundamental critical speed or at the subcritical speed equal to one-half the fundamental. The rotors were more difficult to balance, but with the more accurate balancing equipment now available, this was accomplished satisfactorily. Two of the above rotors were entirely satisfactory in regard to double-frequency rotor vibration at full speed. The magnitude of the double-frequency vibration in the rotor of the 58,750-kva generator, although acceptable, was somewhat higher than originally anticipated. Corrective action may be taken on this rotor at a future date to reduce the magnitude of the double-frequency vibration.

It was originally anticipated that two-pole 60-cycle generators above 62,500 kva would necessitate changes in the rotor design adequately to take care of the double-frequency forces and vibrations.

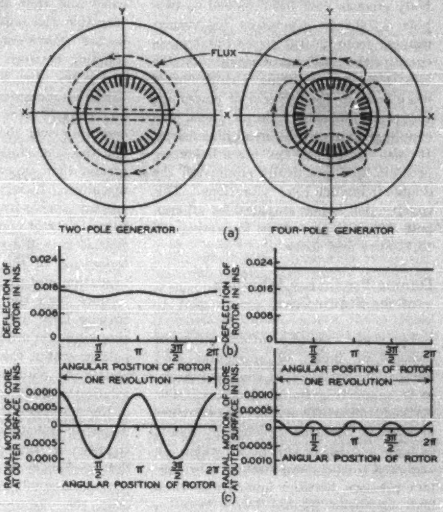

Figure 1. Diagrams illustrating comparison of two-pole and four-pole turbine-generator rotor and stator vibration

A first natural suggestion might be to provide flexibility in the bearing supports so that the vibratory forces would not be transmitted to the foundation, station parts, and other equipment. This type of corrective measure can be considered to be only partial solution at best, even though it were 100 per cent effective, since it permits the rotor shaft ends to move freely and thus transmit the double-frequency motions to the collector rings, couplings, and coupled apparatus. The effects of double-frequency vibrations on these parts are of equal or greater importance than on foundation and station equipment.

A second and more fundamental method of solving this problem is to equalize the mechanical rigidity on the two major axes and thus reduce the double-frequency disturbing forces to values of inappreciable magnitude. There are two methods of equalizing the mechanical rigidity on the two major axes. One is to machine annular grooves in the rotor body to such depth that the additional rigidity contributed by the pole sectors and rotor teeth will be inappreciable. This is not a feasible method for two-pole 60-cycle generators due to the fact that the rotor diameters are inherently small and the provision of annular grooves of appreciable depth would result in an intolerable flexibility of the rotor. A second and more feasible method is to reduce the rigidity on the pole until it is essentially equal to the combined rigidity of rotor teeth and field coils on the winding axis. This can be accomplished by machining longitudinal or transverse slots in the pole body. If longitudinal slots are used, it is necessary to remove an appreciable percentage of the pole material to get the necessary change in rigidity. Thus, in order to avoid too much saturation and a reduction of the magnetizing flux in the pole body, it is necessary to replace the removed material with magnetic filler bars in the machined slots. The use of transverse slots in the pole body is the most effective method of obtaining a required reduction in rigidity with a minimum amount of material removed. Narrow, segmental-shaped, transverse grooves can be provided in the pole body to accomplish the desired results without intersecting the winding slots and without removing an amount of magnetic material which would upset the magnetic saturation conditions. Both of these methods of equalizing the rigidity on the two major axes of two-pole generators are covered by United States Letters Patent number 1,994,922, which was issued in 1934.

Transverse, segmentally-shaped rotor body grooves have been applied to two-pole 3,600-rpm generators for ratings ranging from 20,000 to 58,750 kva to equalize the mechanical rigidity on the two major axes. This change made more feasible the problem of machining the shaft journals, collector rings, and couplings to true cylindrical dimensions. It also simplified the rotor-balancing problem, and practically eliminated the double-frequency rotor vibrations. This construction is now standard for all two-pole 60-cycle generators for ratings of 18,750 kva and above.

Double-Frequency Vibration of the Stator Core

The stator core of a turbine generator consists of layers of segmental steel laminations arranged in complete circles in a supporting frame structure. The stacked laminations are maintained under pressure in the axial direction by means of bolts and heavy end plates which are anchored to the stator frame. The contact pressure between laminations is so high that the friction forces between laminations are of such magnitude as to ensure that the built-up stator core acts as a solid steel cylinder in resisting distortion. The stator core is maintained in a fixed circumferential position with respect to the frame structure by means of dovetail keys or projections. Since the stator punchings are stacked in the stator frame with normal building clearances, it is obvious that the frame structure can contribute little or no rigidity to the stator core in resisting distorting forces. However, there is sufficient mechanical contact between the two members to permit the transmission of vibratory forces from the stator core to the frame structure.

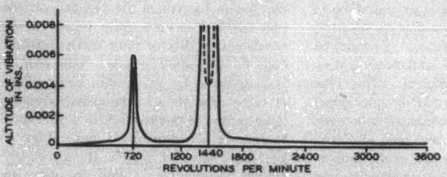

Figure 2. Vibration curve of unsymmetrical rotor

There has been a wide variation in the amount of vibration and noise emitted from large-capacity 3,600-rpm generators of duplicate design and for units of different ratings, but using the same stator punchings. It is believed that the amplitude of vibration of the stator cores is essentially the same for all such cases and that the variation in frame vibration and noise is due to differences in the effectiveness of the mechanical coupling between the respective frames and cores, due to building variations. Vibration measurements have been made on the stator cores of such machines when operating over the complete load range, and the magnitude of the motions is no greater than experienced on lower speed machines. It is not believed that the present double-frequency vibrations existing in stator cores of two-pole 60-cycle generators will result in any undue deterioration of the stator core, winding, insulation, slot wedges, and main leads.

Since both the double-frequency disturbing force and the elliptical type of core vibration are inherent with the two-pole generator, the cause of the vibration cannot be eliminated. A first-thought solution would be to reduce the magnitude of the vibrations by reducing the air-gap magnetic density and by increasing the rigidity of the core to bending. If drastic changes were made in both items to give a combined change of 100 per cent, the vibrations would be reduced approximately 50 per cent and would still be transmitted in the conventional generator to the stator frame, foundation, and other station parts. The cost of the generator would be increased approximately 75 per cent and the manufacturer and purchaser would both still be confronted with the problem of determining whether a 50 per cent reduction in the amplitude of the vibration would be satisfactory. On the basis of the present data and operating experience available on large two-pole 60-cycle generators, it is felt that the results obtained would not be acceptable and the generator could not be justified economically. Since the magnitude of present stator-core vibrations is not considered deleterious to the life and performance of the generator, a more logical and economical solution would be to isolate the stator core and reduce the magnitude of the disturbing forces transmitted to other parts.

A mathematical analysis was made by J. G. Baker, formerly of the research department, Westinghouse Electric and Manufacturing Company, to determine the motion of any point in a freely supported two-pole turbine-generator stator core when subjected to a rotating diametrically applied distorting force. The essential results of this analysis are as follows when considering the stator core as a curved beam:

(*a*). The path of a point on the neutral axis of the stator core is an ellipse, the major axis of which is radial and twice the length of the minor axis.

(*b*). For points at a larger radius than that of the neutral axis, the tangential motion decreases as the radius is increased. At a distance beyond the neutral axis equal to one-third the radius of the neutral axis, the tangential motion is zero, and the motion is purely radial.

(*c*). For points at a shorter radius than that of the neutral axis, the tangential motion increases as the radius is reduced and the path of a point in the tooth zone is approximately circular.

(*d*). The maximum change in the dimensions between adjacent teeth is only 10 to 15 per cent of the radial motion at the back of the core.

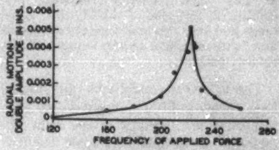

Figure 3. Resonance curve for stator core

In order to check the calculated characteristics of the stator-core vibration and determine methods to prevent the transmission of vibratory forces from the stator core to other parts, a complete experimental investigation was made on a full-size generator model. The model consisted of a stator core 16 inches long built from standard laminations used in the construction of the largest 3,600-rpm generators, a stationary laminated rotor member to complete the magnetic circuit, and a flexibly connected ring-type

Figure 4. Model of stator core for 3,600-rpm turbine generator

frame member. A two-pole three-phase winding was provided in the stator core and was supported in the slots by slot wedges installed in the conventional manner. The model was mounted with the axis in the vertical direction in order to simplify and reduce the cost of the assembly. The flexible ties between the stator core and the frame allowed relative radial motion of individual parts of the stator core with respect to the frame, but prevented relative lateral motion. Practically no tangential flexibility was provided in the supports. The stator winding had sufficient capacity to produce a rotating magnetic field in the air gap of 60,000 lines per square inch for any single- or three-phase supply frequency up to 130 cycles per second.

Tests were made to determine the effect of frequency, magnitude of air gap magnetizing flux, pressure between laminations, and the flexibility of supports on the vibration of the stator core and frame housing. The test results showed that the curve of stator-core vibration as a function of frequency had a sharp resonance peak with the natural frequency at 222 cycles per second. Actual vibration measurements at different points on the stator core checked closely with the values obtained by analysis. The reduction in pressure between laminations obtained by relieving the tension on the through bolts had no effect on the vibration performance of the core, although it is believed that this was due to the fact that the punchings were tightly cemented together with varnish and did not loosen when the bolt pressure was relieved.

The vibration data obtained by test and analysis indicates that the radial motion of any point at the back of the stator core is approximately 0.001 inch and the tangential motion is essentially zero. This simplifies the support problem since flexibility is required in only the radial direction. The general type of problem is basically the same as that satisfactorily applied to slower-speed single-phase 25-cycle generators except that in the single-phase generator, the output torque is pulsating at double frequency and tangential flexibility must be provided for the stator-core supports, whereas in this case radial flexibility only must be provided. Test results on the model showed that the use of the flexible stator-core supports decreased the magnitude of the double frequency transmitted to the frame to a small percentage of the value obtained with a conventional assembly.

In order to expedite the solution of the mechanical problems and develop the construction details involved in providing stator-core supports with radial flexibility, this principle of design is being first applied on a 12,500-kva 3,600-rpm turbine generator. This generator will be operated under both load and sudden short-circuit conditions to determine the adequacy and effectiveness of the design in accomplishing the desired results from the standpoint of objectionable noise and vibration.

Summary

Double-frequency rotor vibration due to different rigidity constants for the two major axes in two-pole 60-cycle generators can be eliminated by equalizing the rigidity constants on the two axes. This can be accomplished without introducing any reduction in flux capacity by machining narrow transverse slots in the body of the poles.

The double-frequency vibratory forces inherent in two-pole generator stators cannot be eliminated. It is further believed that acceptable results cannot be economically obtained by reducing the air-gap magnetic densities and increasing the rigidity of the stator core over that normally obtained when magnetic conditions are adequately satisfied. Also the stator vibration is not of an amplitude considered to be harmful; consequently, the most practical solution is to isolate the stator core by means of flexible supports, and thus materially reduce the magnitude of the disturbing forces transmitted to the stator frame, foundation supports, and associated apparatus.

Discussion

M. D. Ross (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper covers two very important moves to reduce or eliminate vibrations of rotor and stator parts of two-pole generators, which have been present to a certain degree in all machines of this type. These effects are aggravated in the larger two-pole machines and have reached the point where it was desirable to take some steps to reduce them to levels comparable to those in the smaller two-pole machines.

The stator-frame vibration in some of the large machines which have been built has been responsible for considerable 120-cycle noise; whether it is noticeable in the turbine room depends largely upon the number of machines operating in the same room and upon the size and shape of the room. Noise levels in most turbine rooms at the operating floor level usually run from 95 to 100 decibels. This noise is made up of a large number of frequencies and usually varies markedly from one point to another due to reflections of sound waves from turbine-room walls and floor. Reducing the vibration of the generator frame as outlined in this paper will reduce the generator noise, but may not greatly reduce the noise level reading of the room due to noises from other sources.

It would, therefore, seem logical to consider using sound-proofing material on the walls and ceiling of the turbine room, to absorb the noise and reduce the over-all noise level in the room. The average turbine room with its hard walls and floor has very low sound-absorption properties. When a large total sound energy is released in such a confined space, the noise level is considerably increased due to the reflections from the walls. If, on the other hand, the walls had high absorptive properties, the reflections would be largely eliminated. A striking example of this may be noted in the case of a well-known sound-proofed telephone booth in considerable use in turbine rooms. One side of this booth is open to the room, but the noise level inside is low enough to permit satisfactory telephone conversation while this would be impossible outside the booth.

Where sound-proofing of the engine room has been tried with Diesel engines, we understand the reflected sounds have been practically eliminated. It would seem that this factor should be considered in the design of future turbine rooms. With such moves as described in this paper to reduce noise at its source and a related move to reduce the effect of such noise as remains in the room, a much more satisfactory installation could be obtained.

H. D. Taylor (General Electric Company, Schenectady, N. Y.): The authors' observations regarding double-frequency rotor vibration agree for the most part with tests made by the writer and described in the ASME paper to which they refer. One exception may be mentioned that in our tests the double-frequency rotor vibration did not persist throughout the entire speed range, but was found to be small at the main critical speed, and entirely negligible at running speed.

With regard to the suggestion that internal damping in the rotor bodies of large turbine-generators might be sufficient to overcome the tendency toward instability at the main critical speed, it has always been the writer's understanding that internal or "rotating" damping was an extremely undesirable property for high-speed rotors, in that it tends to promote "whipping" or nonsynchronous vibration at the critical frequency when operating above the critical speed. This tendency is entirely apart from the promotion of similar vibration by the oil film in journal bearings at speeds of about twice critical or higher, although the two may both be present and add their effects.

The running performance of some 15 or 20 large 3,600-rpm generators from 25,000 to 66,667 kva placed in service by the General Electric Company in the past two or three years has failed to disclose any difficulty due to unequal mechanical rigidity of the rotor body. The subcritical has in general been barely discernible; the main critical has always yielded satisfactorily to balancing operations; and at running speed, the double-frequency vibrations have been so small as to be entirely harmless.

Magnetic vibration of the stator core and frame structure, however, appearing when excitation is applied, has been a real problem. All of these machines have exhibited this form of double-frequency vibration in greater or lesser degree. Contrary to the authors' experience, we have found the amount of vibration *on the machines themselves* to be very consistent on machines of similar design. The amount of noise resulting from this vibration has varied widely, depending on at least two sets of conditions: (1) the presence or absence of adjacent structures responsive to 120-cycle stimulation, serving as "sounding boards," and in a number of cases producing a great deal more noise than the generator itself; (2) the acoustic properties of the turbine room, which may promote reflections and reverberations of the radiated sound waves in such a way as to amplify the general noise level.

We have made a study of the design factors affecting the magnitude of this magnetic vibration, and, on the basis of analysis and test data, have concluded that a very definite and desirable reduction can be obtained economically by increasing the rigidity of the core. Reductions of the order of two to one can be had, we believe, with only a small increase in the cost of the generator, of the order of 5 per cent or less. The authors' point as to whether this amount of reduction will be satisfactory is well taken. We have made tests in one customer's plant in which the generator voltage was reduced to a point where the noise became unobjectionable, and the corresponding reduction in vibration amplitude was found to be just two to one. This, we realize, may not hold in all cases. For the past year, however, all our new designs for large 3,600-rpm machines have had the core structure stiffened to reduce magnetic vibration; one of these machines has recently gone through factory tests, and shows a very gratifying improvement, meeting our expectations fully.

As a further step to take care of cases which may need a greater reduction, we have also been developing an elastic mounting of the core structure inside the frame, for the purpose of isolating the remaining vibration at its source. Our analysis and tests have both indicated that, even on a core of great radial depth, the tangential vibration at the outer diameter may not be negligible, and we feel therefore that it is desirable to provide both tangential and radial flexibility in the core supports.

Sterling Beckwith (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The papers by Snell, Ross and Sterrett, Laffoon and Rose, and Mortensen and Ryan are all closely related, and will all be discussed together.

The use of hydrogen in condensers and frequency changer sets is not new, nor is its use in small experimental turbine generators new. However, the spectacle of at least a dozen large turbine generators all about twice as large as the largest previous machines and all on order and designed for hydrogen by three different companies before a single one was in operation is exceedingly unusual and interesting.

One of the first things noticed about a hydrogen-cooled machine is the additional piping and control necessary above that required for an air-cooled machine. A large part of this apparent complication is due to three factors. The first is the control circuit, but if this circuit is compared with other station control circuits, it will be found to be relatively simple. The second factor is the hydrogen-control piping which is perhaps somewhat complicated in its entirety, but since most of it is used only at very rare intervals when the hydrogen is admitted to or exhausted from the generator, its operation is not a problem. The third factor is the oil-supply and vacuum-treating equipment, but as a second supply is provided so that shutdown of the vacuum-treated supply is permissible, the reliability of the machine as a whole is not greatly affected by the added complications of the oil-supply system.

Some of the complications are perhaps unnecessary, and may be eliminated in the future. The relief valve, for example, is hardly necessary since the duration of any explosion would be too short for it to afford any relief of pressure in the machine, and secondly the complete failure of the hydrogen-supply control valve which may admit a cylinder or more of hydrogen to the machine would not necessarily produce dangerous pressures. Furthermore the alarm system gives an immediate indication of excessive pressures in the machine.

When a hydrogen-cooled machine is in operation, its extreme quietness compared with other 3,600-rpm machines is very apparent. This is due chiefly to the heavy weight of the explosion-proof housing, and the absence of flat panels in its exterior walls, as it would be quiet with air as well as with hydrogen.

In operating at high hydrogen pressures to obtain increased generator output, hydrogen leakage through the housing will vary very nearly in proportion to the square root of the pressure. Consequently a normal leakage of 30 cubic feet a day at four inches of water will become about 300 cubic feet per day at 15 pounds pressure. Such a large leakage may not be justified for continuous operation, but could readily be justified for short time or emergency overloads.

Critical speeds of a two-pole rotor have an additional subharmonic because of the lack of slots near the center of the poles. This is shown in figure 3 of the paper by S. H. Mortensen and J. J. Ryan, and in figure 2 of the paper by C. M. Laffoon and B. A. Rose. That one machine only should cause trouble from this source, as mentioned in the second of these articles, is somewhat unexpected and leads one to wonder how such other possibilities as forging dissymmetry and foundation variations can be eliminated. The writer's experience with several large machines has indicated no difficulty from the unequal moments of inertia.

B. A. Rose: Mr. Taylor is correct in his statement that internal damping in a shaft tends to promote nonsynchronous vibration at the critical frequency when the rotor is operating above the critical frequency. The statement in the paper to which this point refers is a little misleading in that it should have referred to operation at the critical speed only. On the other hand, the Westinghouse company has encountered no trouble with its 3,600-rpm generator rotors from this cause, even though most of them are designed to operate above their critical speeds.

Mr. Taylor further states that the General Electric Company have experienced no trouble with double-frequency rotor vibration on any of their large-capacity 3,600-rpm machines. The writers believe this can be explained by the fact that the machines in question have no rotor critical frequencies near 120 cycles per second. We have found that our ability to calculate the critical frequencies for the mode of vibration in question is not sufficiently accurate to rely on it for this purpose and have found that the elimination of the nonuniform rigidity is a more positive means of avoiding the trouble.

We are pleased to observe from Mr. Taylor's discussion of the stator vibration problem that, in general, his experiences and theories concerning the phenomenon agree with ours. Their method of providing a flexible core support when announced probably will differ in its detail construction from the one described but will accomplish the required isolation of the core from the frame structure and foundation.

The Hydrogen-Cooled Turbine Generator

D. S. SNELL
ASSOCIATE AIEE

HYDROGEN cooling for electrical machinery was first proposed to the industry through an AIEE paper presented in 1925.[1] The first hydrogen-cooled machine to be placed in commercial service was a 12,500-kva synchronous condenser, installed in 1928.[2] This was followed by other machines of this type, and later, the hydrogen-cooled frequency-converter was introduced. There are now in operation in the United States 20 of these two types of hydrogen-cooled machines, with a combined output of over one-half million kva, and their records of performance have been highly satisfactory. Prior to 1937, the application of hydrogen cooling to turbine generators had been confined to developmental machines in the manufacturers' plants, although several air-cooled generators had been built with provision for later adaptation to hydrogen cooling. In October 1937 the first hydrogen-cooled generator built for commercial service was placed in operation at Dayton, Ohio. This was a 3,600-rpm unit, of General Electric manufacture, rated at 31,250 kva at 0.8 power factor. Since then, a total of 10 hydrogen-cooled generators have been placed in service in various parts of the country, and 27 others are in the course of installation or construction. The total capacity of these 37 generators is over 2,000,000 kva, and their sizes range from 17,000 kva to 81,250 kva at 3,600 rpm, and from 75,000 kva to 176,470 kva at 1,800 rpm.

Two papers have already been presented before the Institute, in which the development and design work of one large manufacturing company in connection with the hydrogen-cooled generator have been described.[3,4] It is the purpose of the present paper to describe the work of another large company in the commercial development of this new-type machine, and to present some of the results of several months' operating experience with eight of these machines.

Advantages of Hydrogen as a Cooling Agent

These have been stated many times before, and are here mentioned only in the interest of completeness.

1. Windage and ventilation losses in hydrogen are only about one-tenth of their value in air, due to the lower density of hydrogen.
2. The rating of a machine can be increased approximately 20 per cent for a given amount of active material, by operation in hydrogen instead of in air, due to the superior cooling properties of hydrogen.
3. The life of the winding insulation is increased, due to the absence of moisture, oxygen, and dirt, and to the fact that corona damage is eliminated, in the hydrogen-cooled machine.
4. The fire hazard is eliminated.

The reduction of the windage and ventilation losses of a turbine generator with hydrogen cooling increases the full-load efficiency about 0.7 per cent for 1,800-rpm machines and about 0.9 per cent for 3,600-rpm machines. In figure 1 are shown the estimated net yearly savings with hydrogen cooling for different sizes of generators, corresponding to these increases in efficiency. These savings assume a power cost of 0.3 cent per kilowatt-hour and an operating time of 80 per cent and take account of the cost of the hydrogen and carbon dioxide required for operating and filling. They also assume the installed cost of the hydrogen-cooled generator to be the same as that of the air-cooled generator, which is usually the case. The savings thus calculated are shown to range from about $4,000 a year for a 25,000-kw 3,600-rpm unit to about $20,000 a year for a 160,000-kw 1,800-rpm unit.

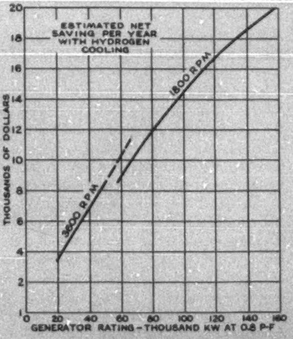

Figure 1. Estimated net yearly saving by using hydrogen in place of air as the cooling agent, for generators in different sizes

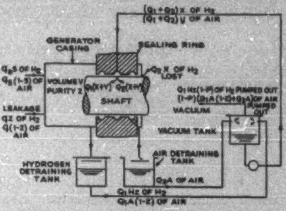

Figure 2. Schematic diagram of shaft-sealing system for hydrogen-cooled generators, using vacuum-treated oil

The greater output per pound of material obtained with hydrogen cooling, extends the limit of output of generators of the 3,600-rpm type and allows the building of generators with self-ventilation in capacities which, if built for air cooling, would require external fans. The largest capacity at 3,600 rpm now considered practicable with air cooling is about 62,500 kva, and such a machine would have external fans; with hydrogen cooling, generators as large as 81,250 kva at 3,600 rpm are practicable, ventilated with internal fans.

Type of Shaft Seal Used

The most important part in the development of the hydrogen-cooled generator was the design of a means for sealing the rotating-shaft extensions against the outward leakage of hydrogen. After investigating many different sealing arrangements, of the mechanical, combination mechanical-and-liquid, and liquid-film types, the General Electric Company finally adopted the liquid-film type of seal for use with its hydrogen-cooled generators. This type of seal is illustrated schematically in figure 2 and includes a sealing ring surrounding the shaft, con-

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Paper 39-105, recommended by the AIEE committee on electrical machinery, and presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3–5, 1939, and at the combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939. Manuscript submitted March 3, 1939; made available for preprinting April 1, 1939.

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The development of the hydrogen-cooled generator described in the foregoing was carried out under the direction of M. A. Savage, and in collaboration with Chester W. Rice, E. H. Freiburghouse, and Robert Hannah. The electrical system of the hydrogen control cabinet was designed by E. J. Flynn and A. J. Bialous. The tests on the Logan and Cincinnati generators were obtained through the co-operation of the Appalachian Electric Power Company and the Cincinnati Gas and Electric Company, which co-operation is hereby gratefully acknowledged.

1. For all numbered references, see list at end of paper.

Figure 3. A 6,250-kva 3,600-rpm generator built for operation in hydrogen. Coolers are located beneath the rotor. Vacuum tank for treating shaft-sealing oil shown in background

structed integral with the bearing, to which vacuum-treated lubricating oil is supplied under pressure.[6,8] This creates a film of oil completely surrounding the shaft which prevents the escape of hydrogen from the casing at this point. This type of shaft seal has the advantages of simplicity and ruggedness, and through operating with vacuum-treated oil both permits a high degree of hydrogen purity to be maintained in the generator casing and prevents the contamination of the oil in the bearing lubrication system by the hydrogen.

Tests on a 6,250-Kva Hydrogen-Cooled Generator

In 1926 a 6,250 kva, 3,600-rpm hydrogen-cooled generator was built by the General Electric Company for developmental purposes. This machine, figure 3, was provided with a heavy, cylindrical casing, cast-steel end shields, and with two gas coolers, contained in a rectangular box beneath the rotor. A shaft seal of the oil-film type was provided in each generator bearing. This machine was tested as a synchronous condenser in hydrogen and in air over a period of about 12 months, during which time the operating characteristics of the seals and auxiliary equipment could be observed and information on the heating of the machine in hydrogen and in air obtained. The more important of the heating data obtained in the tests are summarized in figures 4 to 6.

Figures 4 and 5 give the field and armature heating characteristics of the 6,250-kva generator for different loss intensities on the coil surfaces, with air, carbon dioxide, and hydrogen as the cooling media, the tests in hydrogen having been conducted at three different hydrogen pressures. Curves for cooling with helium have been added, based on calculation. The armature heating curves in figure 5 have had deducted the calculated temperature rise of the gas in the air gap due to field I^2R loss. This rise is of the order of from three to five degrees centigrade. This was done in order to use the curves for estimating the armature temperature rise at 0.8 power factor loads, the tests having been made at zero power factor.

The temperature difference between armature copper and between-coil temperature detector for different armature loadings, in air and in hydrogen, is shown in figure 6. At normal load, corresponding to a loss intensity of about 0.3 watt per square inch, this temperature difference is about 15 degrees centigrade in air as compared with 11.4 degrees centigrade in hydrogen at 0.44 pound per square inch pressure. The lower values of the temperature difference at higher hydrogen pressures indicate a slight increase in the thermal conductivity of the insulation at the higher pressures. The crossing of the curves at the higher armature loadings may be due to changes in the insulation occurring at the higher temperatures.

From the foregoing tests, and tests made on stationary models, it was determined that the thermal conductivity of the armature insulation was increased about ten per cent by operation in hydrogen instead of air, while that of the field insulation was practically doubled. These increases in thermal conductivity are practically independent of the hydrogen pressure. The forced heat convection from the internal machine surfaces was estimated to be increased between 30 and 50 per cent by operation in hydrogen at 0.44 pound per square inch pressure instead of in air. For increased hydrogen pressures the forced heat convection varies approximately with the 0.8 power of the absolute hydrogen pressure.

The heating data obtained in these tests indicate that the 6,250-kva generator as originally designed could have its rating at 0.8 power factor increased by very large percentages by operation in hydrogen at various pressures instead of in air, for the same temperatures of the windings. However, these increases in rating would be at the expense of electrical stability, as the short-circuit ratio of a generator is decreased in the same proportion as the output is increased. To obtain the same stability from the hydrogen-cooled generator as from the air-cooled one, the former must be proportioned somewhat differently. For the 6,250-kva generator redesigned for the same short-circuit ratio at all ratings (by maintaining the same stator core dimensions but varying the rotor diameter) the increases in output with hydrogen cooling over the normal air rating, for the same winding temperatures, would be approximately 20, 30, and 35 per cent for hydro-

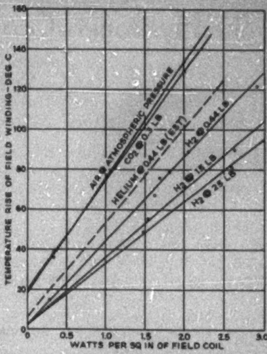


Figure 4. Field heating characteristics of a 6,250-kva generator with respect to temperature of gas leaving coolers, from tests with generator operating in different cooling gases

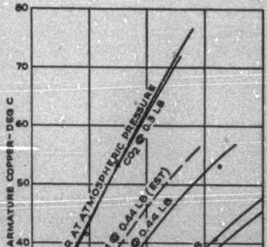


Figure 5. Armature heating characteristics of a 6,250-kva generator with respect to temperature of gas leaving coolers, from tests with generator operating in different cooling gases

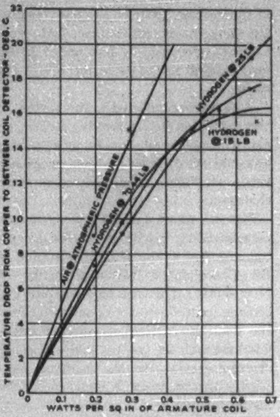


Figure 6. Temperature drop from armature copper to between-coil temperature detector, for different watts per square inch of armature coil, from tests on a 6,250-kva generator operating in air and in hydrogen

gen pressures of 0.44, 15, and 25 pounds per square inch, respectively.

Other Cooling Gases

Tests on the 6,250-kva generator using carbon dioxide as the cooling agent indicated, as shown in figures 4 and 5, that the heating of a machine in this gas is practically the same as with air cooling. Although the density of carbon dioxide is about 50 per cent greater than that of air and its thermal conductivity is about 35 per cent less, these disadvantages are compensated for, with respect to the heating of the windings, by the fact that the forced heat-convection coefficient is about 20 per cent greater in carbon dioxide than in air, and the product of specific heat and density is 39 per cent greater for carbon dioxide than for air, so that 39 per cent more heat can be absorbed by a given volume of the gas for the same temperature rise. Although there is no advantage in the use of carbon dioxide as the cooling agent for generators, as its higher density greatly increases the windage losses, there appears to be a distinct field for its use in the case of induction motors intended for operation in explosive atmospheres. In the past year the General Electric Company has placed in service two induction motors of this type, using carbon dioxide as the cooling agent, the motors being provided with shaft seals similar to those used with hydrogen-cooled generators.

The use of helium as the cooling gas for rotating machinery has been frequently proposed, and has the advantage of effecting a reduction in windage and ventilation losses comparable with that obtained with hydrogen, without the attendant disadvantage of inflammability in mixture with air, possessed by hydrogen. However, as shown by the curves in figures 4 and 5, the cooling effect with helium is distinctly inferior to that obtained with hydrogen, so that the increase in output of a generator by using helium in place of air as the cooling agent would be considerably less than that obtained by the use of hydrogen in place of air.

Generator Construction

The type of construction employed with most of the hydrogen-cooled generators built by the General Electric Company is illustrated in figure 7 and includes a cylindrical, gas-tight, outer casing, reinforced with a number of annular supporting-plates. Four gas coolers are usually provided, located within the casing with their long axes parallel to the horizontal axis of the machine. The cooler tubes are made accessible for cleaning without removing the hydrogen from the casing by removing cover plates at either end of the frame. In another design two coolers are used, placed lengthwise of the generator and located in either the top half or the bottom half of the casing.

The outer end-shields of the generator are of either cast-steel or fabricated construction and are usually employed to support the generator bearings, thus minimizing the distance between bearing centers. The joint surfaces between the end-shield halves and between the shields and the casing are carefully machined, and are provided with grooves within the bolting line into which a plastic compound is forced, thereby minimizing the possibility of hydrogen leakage from the joints

The frame construction is designed to withstand safely the explosion pressure of the most explosive mixture of hydrogen and air, with the generator operating at a

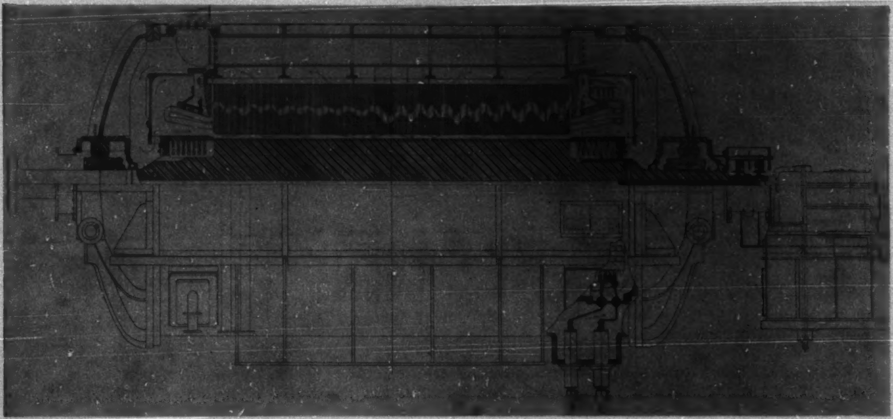


Figure 7. Longitudinal cross-section view of a 55,555-kva 3,600-rpm hydrogen-cooled generator

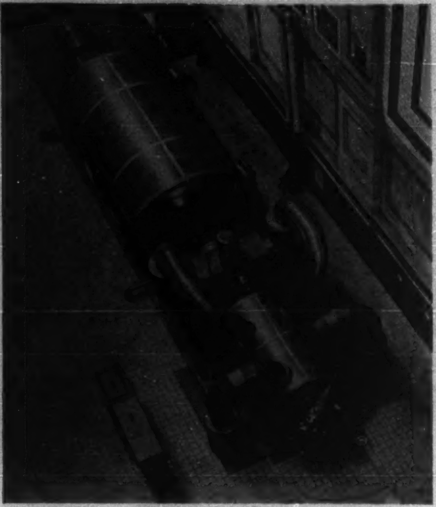


Figure 8. A 53,000-kw 3,600-rpm steam turbine-generator set, generator hydrogen cooled

hydrogen pressure of 15 pounds per square inch. This explosion pressure is estimated to be approximately 100 pounds per square inch.

The rotors of 3,600-rpm hydrogen-cooled generators consist of solid, one-piece steel forgings, having slots milled out of the body portion for carrying the field winding. For many of these generators the field winding is made from aluminum, instead of from copper, which, due to the lower specific weight of aluminum, permits a larger rotor diameter to be employed than with the copper winding and thereby secures a greater rigidity for the rotating shaft. The low rotational loss in hydrogen permits such an increase in rotor diameter, which would not be practical with air cooling.

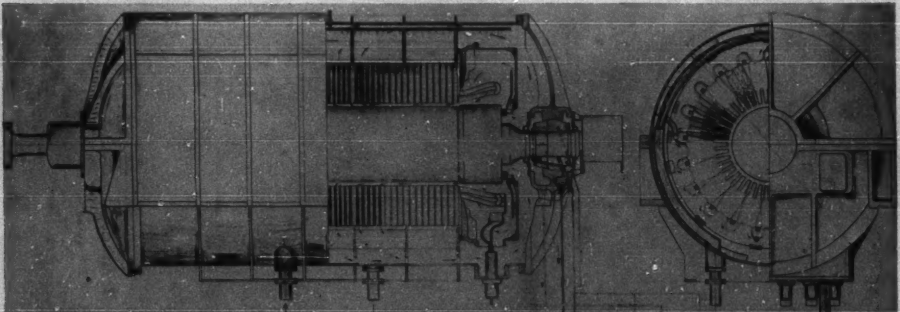
The ventilation of these generators, in all sizes but the very largest, is provided by two centrifugal fans, mounted one on either end of the rotor. For the 176,470-kva 1,800-rpm hydrogen-cooled generator of the Chicago District Electric Generating Corporation, State Line, Ind., ventilation is provided by four motor-driven fans, housed in domes in the top of the casing.

A device is connected to the bottom of the generator casing which operates an alarm if water or oil should start to collect, as could result from a defective cooler-tube or from improperly-functioning shaft seals.

Figure 8 shows a 58,889-kva 3,600-rpm General Electric hydrogen-cooled generator, installed in the Waterside station of the Consolidated Edison Company of New York. This machine has four gas coolers, located two above and two below the floor line. Distilled water is circulated through the coolers, and a heat exchanger external to the machine, supplied with raw water, is used to cool the distilled water.

The 50,000-kva 3,600-rpm hydrogen-cooled generator of the Appalachian Power Company at Logan, W. Va., employs a novel cooling system in which the stator core is cooled by direct contact of the laminations with a number of hollow metal pads, located between the lamination packages, through which distilled water is circulated.[8] The construction of this machine is illustrated in figure 9. The rotor, and the armature end-windings are cooled by the circulation of hy-

Figure 9. Longitudinal and end cross-section views of a 50,000-kva 3,600-rpm hydrogen-cooled generator having water-filled cooling pads in the stator core



drogen over them, the heated hydrogen being cooled at the center of the machine in passing to the back of the core through ducts formed between special finned pads and the laminations. A heat exchanger external to the generator is used to cool the distilled water circulated through the pads. While no other generators of the pad-cooled type have since been built, this cooling arrangement is believed to have several advantages which may sometime encourage its more general use. The use of cooling pads in place of the customary gas coolers permits a more compact construction and reduces the volume of gas that needs to be circulated.

Operating Hydrogen Pressure

The hydrogen-cooled generators built by the General Electric Company are designed to operate normally at a hydrogen pressure of approximately one-half pound per square inch. Several machines have been built, however, with provision for operating at hydrogen pressures up to 15 pounds per square inch, if such operation should become desirable for securing additional kilovolt-ampere output or lower generator temperatures under abnormal water-temperature conditions. Operation at hydrogen pressures greater than about 1.5 pounds per square inch requires the provision of additional control features in the seal-oil and gas control systems.

Shaft Sealing System—Characteristics

Seals Supplied with Vacuum-Treated Oil

The equations for the shaft sealing system employed with these generators are derived in the appendix. These show that, with vacuum-treated oil supplied to the shaft seals, the degree of hydrogen purity maintained in the generator casing is a function chiefly of the degree of vacuum treatment of the oil and the amount of leakage from the casing. The purity is shown to change exponentially with time, following the initial starting-up of the generator, finally reaching a constant value. The amount of hydrogen continuously required by the generator is shown to equal approximately the casing leakage plus the amount of hydrogen absorbed by the sealing oil.

Figure 10 shows the calculated variation in hydrogen purity with time following the starting-up of the generator, for different degrees of vacuum treatment of the sealing oil, for the 50,000-kva 3,600-rpm Logan generator. A leakage from the casing of 0.27 cubic foot per hour has been assumed, based upon tests. The curves show that several weeks of operation are required for the hydrogen purity in the casing to reach the final value corresponding to the vacuum and leakage also that the final value is independent of the initial hydrogen purity in the casing.

Figure 11 shows the calculated final hydrogen purity in the generator casing for different degrees of vacuum treatment of the sealing oil and different assumed values of the casing leakage, for the Logan generator. A curve showing the estimated yearly cost of the windage and ventilation losses of this machine for different values of hydrogen purity in the casing is also given, which illustrates the economic advantage of operating at a high purity. This curve was calculated for an assumed power cost of 0.3 cent per kilowatt-hour and an operating time of 80 per cent. It can be shown from the curves of figure 11 that for values of vacuum treatment less than about 29 inches, a slight casing leakage may increase the hydrogen purity sufficiently to effect a saving in cost of windage loss comparable with the cost of the hydrogen wasted. In practice, however, it is preferable to maintain a high hydrogen purity by maintaining a high degree of vacuum treatment, rather than by leakage, and to have the casing leakage as small as possible.

A high hydrogen purity is, of course, essential from the standpoint of safety, as well as of economy, of operation, since a mixture of less than 72 per cent hydrogen in air is explosive. In practice it is usual never to allow the hydrogen purity in the generator casing to decrease below 90 per cent.

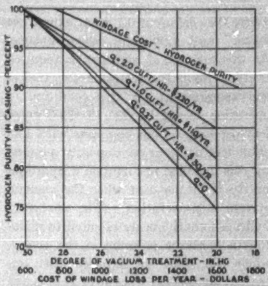


Figure 11. Calculated per cent final hydrogen purity in casing of a hydrogen-cooled generator for different degrees of vacuum-treatment of oil supplied to shaft seals and different rates of hydrogen leakage q. Also estimated yearly cost of windage losses versus per cent hydrogen purity

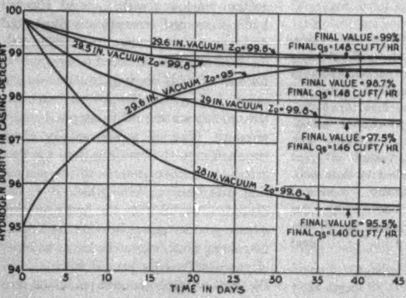
Hydrogen pressure ten inches water

The degree of vacuum treatment of the sealing oil is determined by the effectiveness of the treating system. For the spray-recirculation system of vacuum treatment used with these generators, the vacuum treatment obtained is generally between 0.1 and 0.3 inch less than the vacuum held in the vacuum tank.

Seals Supplied with Untreated Oil

If the shaft seals are supplied with untreated oil, as may occur under certain emergency conditions of operation, the air given up from the oil flowing to the hydrogen side of the seals will cause the hydrogen purity in the casing gradually to decrease. This will require that the casing be scavenged at intervals with fresh hydrogen to keep the purity always above 90 per cent. Figure 12 shows the calculated and test values of hydrogen purity under this condition of operation, for the Logan 50,000-kva generator.

Figure 10. Calculated per cent hydrogen purity in casing of a hydrogen-cooled generator versus time for different degrees of vacuum-treatment of oil supplied to shaft seals



Barometer 30 inches. Final hydrogen purity by test 99 per cent for 29.7 inches vacuum in vacuum tank. Hydrogen consumption by test 1.48 cubic feet per hour. Hydrogen pressure in casing 10 inches water

The curves show that, for the normal rate of oil flow to the seals, with untreated oil, it is necessary to scavenge the generator casing with fresh hydrogen about once every 15 hours, to maintain the purity between 90 and 95 per cent. The amount of hydrogen of 99.8 per cent purity required to increase the hydrogen purity in the casing from 90 to 95 per cent is, approximately,[7]

$$0.7\times 2.3\times 1{,}000\log\frac{99.8-90}{99.8-95}=500\text{ cubic feet}$$

where the coefficient 0.7 is an "experience factor" and 1,000 the casing volume. As this amount of hydrogen is required every 15 hours the average hourly hydrogen requirement for this condition of operation would be 500/15=33.3 cubic feet per hour. This compares with 1.48 cubic feet per hour continuously required by this machine to maintain a hydrogen purity of 99 per cent when the vacuum system is in operation. (The amount of hydrogen continuously required to maintain a constant pressure in the casing, when the seals are supplied with untreated oil, is usually negative, that is, gas must be vented from the casing. This follows from the fact that for small values of casing leakage more air is brought into the casing by the sealing oil than hydrogen is discharged by absorption and leakage.)

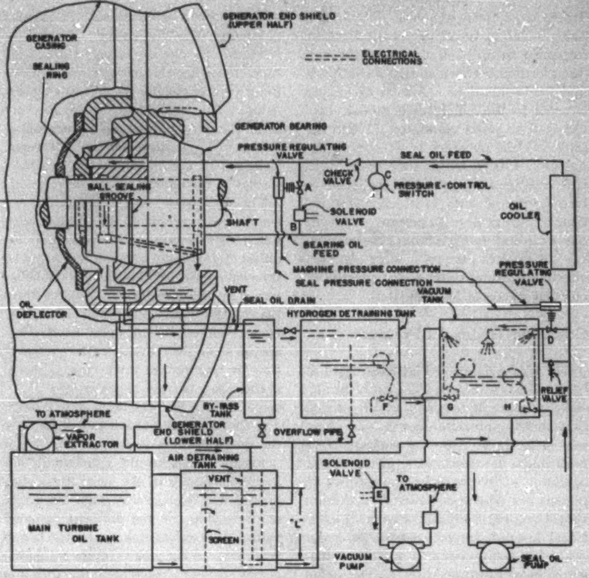

GENERATOR END SHIELD (UPPER HALF)
GENERATOR CASING
SEALING RING
ELECTRICAL CONNECTIONS
GENERATOR BEARING
PRESSURE REGULATING VALVE
SEAL OIL FEED
CHECK VALVE
C
PRESSURE-CONTROL SWITCH
BALL SEALING GROOVE
A
SOLENOID VALVE
B
SHAFT
OIL COOLER
BEARING OIL FEED
MACHINE PRESSURE CONNECTION
SEAL PRESSURE CONNECTION
PRESSURE REGULATING VALVE
VACUUM TANK
VENT
SEAL OIL DRAIN
HYDROGEN DETRAINING TANK
OIL DEFLECTOR
D
F
G
H
RELIEF VALVE
BY-PASS TANK
TO ATMOSPHERE
VAPOR EXTRACTOR
GENERATOR END SHIELD (LOWER HALF)
OVERFLOW PIPE
AIR DETRAINING TANK
VENT
SOLENOID VALVE
E
TO ATMOSPHERE
MAIN TURBINE OIL TANK
SCREEN
"L"
VACUUM PUMP
SEAL OIL PUMP

Figure 13. Arrangement of shaft-sealing system for hydrogen-cooled generator, with detail of shaft-sealing arrangement shown for one generator bearing. Arrangement for other bearing is similar

Standstill Operation of Seals

The characteristics of the shaft-sealing system with the generator at rest are somewhat different than with the generator operative, owing to the different values for the gas solubilities in the oil to be used in the seal equations. With vacuum-treated oil supplied to the seals, two different values for both the hydrogen and the air solubilities must be used; for the absorption of gas by the vacuum-treated oil, about the same solubilities may be assumed whether the shaft is rotating or stationary, while for the release of gas by the oil the gas solubilities should be taken as about one-tenth their values for the rotating condition. With untreated oil supplied to the seals, the gas solubilities used in the seal equations should be taken as about one-tenth their values for the rotating condition. The practical results of these differences in seal characteristics for the stationary and rotating conditions are, that with vacuum-treated oil supplied to the seals the hydrogen requirement is only slightly less with the shaft at rest than with the shaft rotating, but the hydrogen purity in the casing is slightly higher. With untreated oil supplied to the seals, however, the amount of hydrogen required with the shaft at rest is about equal to the casing leakage, and the hydrogen purity decreases only about one-tenth as rapidly as with the shaft rotating.

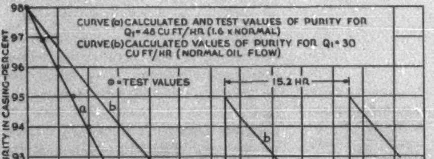


Figure 12. Calculated and test values of per cent hydrogen purity in casing versus time, for a hydrogen-cooled generator operating with untreated oil supplied to shaft seals. Casing scavenged when purity decreases to 90 per cent

Hydrogen pressure 18 inches water

Shaft Sealing System—Arrangement Details

Vacuum-Treating System

The arrangement of the shaft-sealing system employed with General Electric hydrogen-cooled generators is illustrated diagrammatically in figure 13. The sealing ring, which is attached to the generator side of each bearing, contains an annular feed groove to which vacuum-treated lubricating oil is supplied under pressure. The oil passes axially along the shaft in both directions from the feed groove to release grooves in the sealing ring and bearing, into which it is deflected. The oil from the hydrogen-side release grooves passes into the hydrogen-detraining tank, where the larger bubbles of gas absorbed by the oil flowing from the seals are given up and pass back into

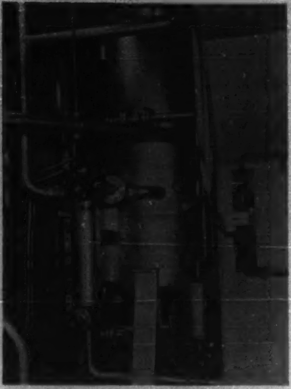


Figure 14. Combination vacuum and hydrogen-detraining tank with auxiliary equipment, for shaft-sealing system of a hydrogen-cooled generator

the generator. The oil from the air-side release grooves unites with the bearing oil and passes to the main oil tank, whence an equal amount flows by gravity to the air-detraining tank (or to an air-detraining compartment in the main oil tank) where the larger bubbles of air contained in the oil are given up. From the hydrogen-detraining and air-detraining tanks the oil passes into the vacuum tank, where practically all of the hydrogen and air remaining in the oil is removed.

Oil is pumped to the seals from the vacuum tank by a rotary pump of the constant-displacement type, having a capacity several times the normal seal oil flow. Pressure regulating valve *D* holds a constant differential between the seal and hydrogen pressures by diverting into the vacuum tank the part of the pump delivery not sent to the seals. The oil diverted through valve *D* enters the vacuum tank through spray nozzles, which serve to expose large areas of the oil to vacuum treatment.

A vacuum of between 0.1 and 0.5 inch of mercury, absolute, is held in the vacuum tank by the operation of a rotary vacuum pump. As the vacuum pump discharges to atmosphere an explosive mixture of hydrogen and air, the pump is made explosion proof.

The by-pass tank shown in figure 13 is used to divert the oil from the seal drain to the air-detraining tank if it becomes necessary to remove the hydrogen-detraining tank from service. Its cross-section area is sufficient to permit partial detraining of the hydrogen from the oil under this emergency condition of operation.

The principal elements of the vacuum-treating system are usually constructed as a unit, as shown in figure 14, and the whole arrangement is conveniently located underneath the generator.

EMERGENCY SEAL OIL SUPPLY

In the event of any failure in the vacuum-treating system which would cause failure of the discharge pressure of the seal oil pump, the shaft seals would be supplied with untreated oil from the bearing supply system through pressure-regulating valve *A*, figure 13. This valve is normally open but is prevented from supplying oil to the seals by the normally-closed solenoid-valve *B*. Failure of the discharge pressure of the seal oil pump opens the pressure switch *C*, allowing valve *B* to open. This operation also closes the normally-open solenoid-valve *E* in the suction line of the vacuum pump, preventing the rising of foam in the vacuum tank, which would occur with the spray nozzles unsupplied with oil.

With the shaft seals supplied with oil from the bearing supply system, hydrogen-saturated oil from the seals passes to the air-detraining tank, and thence to the main oil tank, through the overflow pipe in the hydrogen-detraining tank. Operation of the vapor extractor is relied upon to maintain the atmosphere above the oil in the main oil tank well below the lower limit of hydrogen-air inflammability (four per cent) under this condition of operation.

ATMOSPHERIC RELIEF AND SCAVENGING VALVES

The maximum operating hydrogen pressure for generators not designed for operation up to 15 pounds per square inch pressure, is represented in figure 13 by the elevation of the oil level in the main oil tank above the low point of the overflow pipe from the hydrogen-detraining tank (dimension *L*). An atmospheric relief valve of the mercury-column type is provided at the hydrogen-detraining tank, figure 15, which discharges hydrogen to atmosphere if the pressure in the casing approaches this limiting pressure. If this valve should fail to operate, hydrogen would be discharged into the vent pipe in the air-detraining tank and would be carried to atmosphere by the vapor extractor.

A solenoid-operated scavenging valve is also provided at the hydrogen-detraining tank, figure 15, operated from the hydrogen control cabinet, which allows hydrogen to be discharged to atmosphere if it becomes necessary to increase the purity of the hydrogen in the casing.

Hydrogen Control

CONTROL SYSTEM

The system of hydrogen control employed with these generators is illustrated in figure 15. The hydrogen supply is contained in several commercial cylinders, or "bottles," connected with a manifold through pressure-reducing regulators. A solenoid-operated valve controlled by a pressure switch admits hydrogen to the casing from the connected cylinders to maintain the casing pressure within predetermined limits. These are normally from 8 to 12 inches of water.

In filling the casing with hydrogen initially, or in removing the hydrogen from the casing, carbon dioxide is first admitted, through a pipe at the bottom of the casing, and the air or hydrogen discharged from the hydrogen feed pipe. To determine when sufficient carbon dioxide has been admitted to render the mixture nonexplosive with hydrogen or air, an Orsat is used, or, in some cases, a special scale on the hydrogen purity indicator is provided.

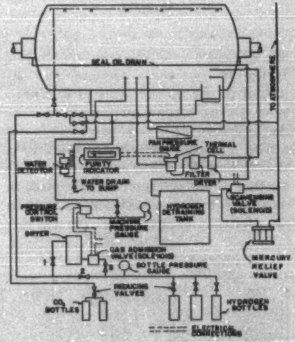


Figure 15. Arrangement of hydrogen piping for hydrogen-cooled generator

PURITY INDICATORS

An indication of the percentage hydrogen purity in the generator casing is provided by an instrument operating on the thermal-conductivity principle. Hydrogen from the casing is passed continuously through the analysis cell of the purity-indicating instrument by the action of the generator fans or by continu-

Figure 16. Instrument panel of hydrogen-control cabinet for a hydrogen-cooled generator

Table I. Heating Tests on 31,250-Kva 3,600-RPM Turbine Generator for Dayton Power and Light Company Operating as a Synchronous Condenser at Zero Power Factor Overexcited

All Temperatures in Degrees Centigrade

	Cooling Medium	
	Air	Hydrogen
Kilovolt-amperes at zero power factor	19,200	30,000
Field input, kilowatts	62.5	68
Temperature of gas to coolers	63.4	46.1
Temperature of gas from coolers	40.3	33.1
Temperature rise of gas through coolers	23.1	13.0
Temperature of water to coolers	15.6	18.3
Temperature of water from coolers	26.5	26.5
Temperature rise of water in coolers	10.9	8.2
Water flow to coolers, relative	1.41	1.0
Temperature rise field winding above ingoing gas	67	67
Temperature rise field winding above ingoing water	91.7	81.8
Temperature rise maximum armature temperature coil above ingoing gas	38	32
Temperature rise maximum armature temperature coil above ingoing water	62.7	46.8

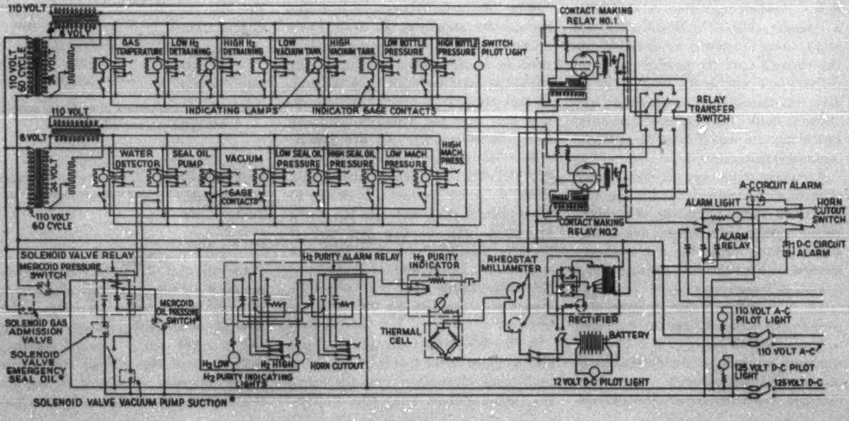
ally wasting a small amount of hydrogen. In the analysis cell a platinum coil, arranged in one of the arms of a Wheatstone Bridge circuit, is passed over by the hydrogen at a slow rate. This coil carries a heating current, supplied by a 12-volt battery, which causes the coil to increase in temperature, and therefore in resistance, by an amount depending upon the thermal conductivity of the gas surrounding it. Since the thermal conductivity of hydrogen is about seven times that of air, a small percentage of air in the hydrogen will cause a relatively large change in the thermal conductivity of the hydrogen and will thus effect a proportionately large change in the coil resistance. The purity indicator is a galvanometer-type of instrument, and indicates the purity of the gas in the analysis cell by comparing the resistance of the above coil with that of a standard coil arranged in another arm of the Wheatstone bridge circuit. A contact in the indicator case operates an alarm to indicate low hydrogen purity.

A secondary indication of the hydrogen purity in the generator casing is provided by the reading of a gauge showing the pressure developed by the generator fans. As the fan pressure, with the generator operating at constant speed, is proportional to the hydrogen density, the scale of this gauge can be calibrated to indicate percentage hydrogen purity.

CONTROL CABINET

The gas admission device, purity indicating equipment, and the other indicating instruments required for operating the generator in hydrogen are housed in a control cabinet, usually located near the generator on the operating floor. Figure 16 shows the instrument panel of the control cabinet for a 55,555-kva 3,600-rpm hydrogen-cooled generator. The large round instrument shown at the center of the upper row of gauges is the fan-pressure gauge. Directly below it is the hydrogen purity indicator. Other instru-

Figure 17. Schematic diagram of electrical circuits of the control cabinet for a hydrogen-cooled generator. Items with (*) are located away from cabinet


110 VOLT
6 VOLT
110 VOLT 60 CYCLE
24 VOLT
GAS TEMPERATURE
LOW H2 DETRAINING
HIGH H2 DETRAINING
LOW VACUUM TANK
HIGH VACUUM TANK
LOW BOTTLE PRESSURE
HIGH BOTTLE PRESSURE
SWITCH PILOT LIGHT
INDICATING LAMPS
INDICATOR GAGE CONTACTS
110 VOLT
6 VOLT
24 VOLT
110 VOLT 60 CYCLE
WATER DETECTOR
SEAL OIL PUMP
VACUUM
GAGE CONTACTS
LOW SEAL OIL PRESSURE
HIGH SEAL OIL PRESSURE
LOW MACH PRESSURE
HIGH MACH. PRESS.
CONTACT MAKING RELAY NO.1
RELAY TRANSFER SWITCH
CONTACT MAKING RELAY NO.2
A-C CIRCUIT ALARM
ALARM LIGHT
HORN CUTOUT SWITCH
ALARM RELAY
D-C CIRCUIT ALARM
SOLENOID VALVE RELAY
MERCOID PRESSURE SWITCH
SOLENOID GAS ADMISSION VALVE
SOLENOID VALVE EMERGENCY SEAL OIL*
MERCOID OIL PRESSURE SWITCH*
SOLENOID VALVE VACUUM PUMP SUCTION*
H2 PURITY ALARM RELAY
H2 LOW
H2 HIGH
H2 PURITY INDICATING LIGHTS
HORN CUTOUT
H2 PURITY INDICATOR
THERMAL CELL
RHEOSTAT MILLIAMETER
RECTIFIER
BATTERY
12 VOLT D-C PILOT LIGHT
110 VOLT A-C PILOT LIGHT
110 VOLT A-C
125 VOLT D-C PILOT LIGHT
125 VOLT D-C

ments shown are the gauge for indicating the hydrogen presssure in the generator casing, the gauge for reading the reduced hydrogen-bottle pressure, a gauge for reading the vacuum in the vacuum tank, and a dial thermometer for indicating the hydrogen temperature at discharge from the coolers. The different instruments are provided with contacts which operate the alarm system of the cabinet to indicate any abnormal condition. The cabinet is mounted on rubber supports to protect the instruments against vibration.

The three hand-valves shown at the bottom of the cabinet in figure 16 correspond with valves 1, 2, and 3 in figure 15.

The alarm system for the control cabinet is represented in figure 17 and includes two contact-making relays of the vacuum-tube type which are used to operate two alarm horns. Operation of the alarm contact of any instrument lights a signal lamp and causes a current to flow through a small step-up transformer; this impresses a grid-bias on the vacuum tube of one of the contact-making relays, causing the relay to drop out and operate the horns. This arrangement permits a minimum passage of current through the instrument contacts for the operation of the horns.

Operating Experience with Eight Hydrogen-Cooled Generators

Within the past 18 months eight General Electric hydrogen-cooled generators in capacities ranging from 31,250 kva at 3,600 rpm to 176,470 kva at 1,800 rpm and with a combined output of 522,164 kva, have been placed in service. The individual ratings of these machines, and their service records, are given in figure 18. These units have operated almost continuously since installation, with no outages from faulty operation of the hydrogen features, and have amply demonstrated the many advantages of hydrogen cooling. The rates of hydrogen consumption have averaged from 35 to 60 cubic feet per day for the six 3,600-rpm units, and are about 65 and 120 cubic feet per day respectively for the two 1,800-rpm units. The costs of these amounts of hydrogen range from $0.45 to $1.50 per day. The hydrogen purities in the casings of these machines have been maintained at values between 97.5 and 99 per cent.

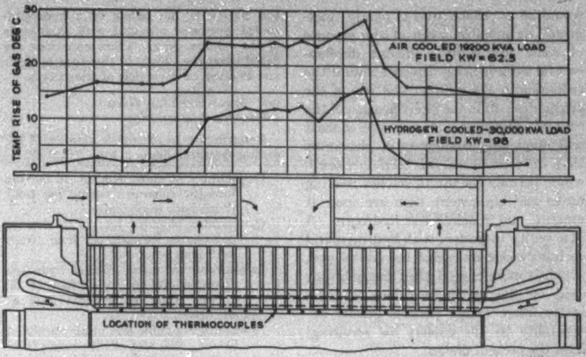


Figure 19. Temperature rise of cooling gas in ventilation circuit of a 31,250-kva 3,600-rpm generator built for operation in hydrogen, from tests in air and in hydrogen, with generator operating as an overexcited synchronous-condenser at zero power factor

Paths of ventilating gas shown by arrows

Although some little time is usually required to familiarize the operators with the various details of the auxiliary equipment no real operating difficulties have been encountered, and in most cases the operators have come to regard the operation of hydrogen-cooled generators as being only little more complicated than the operation of the air-cooled type of machine.

Heating Tests on a 31,250-Kva Generator

The first hydrogen-cooled generator to be placed in commercial service, the 31,250-kva 3,600-rpm Dayton machine, was given extensive tests in hydrogen and in air previous to shipment. Some of the test results are shown in table I and figure 19. It will be observed from table I that this machine, operating at zero power factor, carried in hydrogen approximately 56 per cent greater load than in air for the same temperature rise of the field winding, with respect to the temperature of the gas leaving the coolers, while the armature winding operated at a temperature rise even lower. Operating with air as the cooling medium, this generator could carry nearly full load rating with the same heating of the windings as for a standard air-cooled machine. In hydrogen the full-load temperature rises are considerably lower than the normal values for air-cooled machines. The curves of figure 19 show the marked reduction with hydrogen cooling, in the temperature rise of the gas in the air gap—which has an important influence on the heating of the rotor—due to the lower rotational loss in hydrogen.

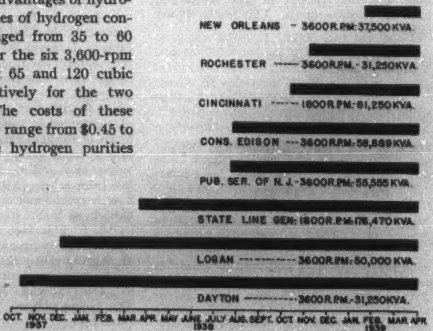


Figure 18. Ratings and lengths of time in service of eight General Electric hydrogen-cooled generators

Future Trends in Generator Design

It will be apparent, from the large number of hydrogen-cooled generators now in successful operation, that this new type of machine has now definitely emerged from the experimental stage and has become an accepted piece of central-station equipment. Much remains to be done, however, now that some operating experience with these machines has been gained, toward simplification of the equipment and improvement of the general design. In developing the design described in the foregoing, the General Electric Company has endeavored to obtain a system that would be as reliable and as nearly "foolproof" as possible, even at the expense of some complexity. Present developmental work is being directed toward the reduction in the number and size of the auxiliaries and in the simplification of the control equipment. A new type of shaft seal employing smaller shaft clearances than the present seal has been developed, which permits the use of smaller treating tanks and equipment than are used at present, and requires less hydrogen. A new purity indicator, with hydrogen and carbon dioxide scales, has been developed, which is more rugged than the indicator used on the earlier machines. A new stator frame design permits greater accessibility to the coolers for servicing, than the present design.

At present no hydrogen-cooled generators smaller than 28,125 kva at 3,600 rpm or smaller than 81,250 kva at 1,800 rpm have been built, or are under construction, by the General Electric Company. For sizes appreciably smaller than these, the gains with hydrogen cooling do not at present appear sufficient to justify the additional expense and complication involved in its use. However, further improvements in design, and increasing familiarity with hydrogen cooling on the part of the industry may eventually make desirable the extension of hydrogen cooling to include even the smaller sizes of machines. For the larger sizes, that is, above about 37,500 kva at 3,600 rpm and 75,000 kva at 1,800 rpm, it is probable that practically all future generators will be hydrogen cooled.

Appendix

Equations of the Shaft Sealing System

In the following are derived the equations for the hydrogen consumption and the hydrogen purity in the casing of a hydrogen-cooled generator provided with shaft seals of the oil-film type. These equations are based upon the theory of this type of shaft seal as given in the article of reference 5.

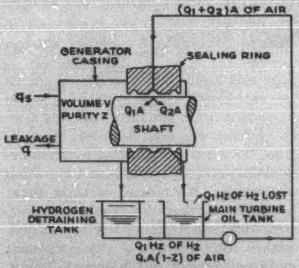


Figure 20. Schematic diagram of shaft-sealing system for hydrogen-cooled generators, using untreated oil

(a) Seals Supplied With Vacuum-Treated Oil

Referring to the schematic diagram of the sealing system in figure 2, let

Q_1 = number of cubic feet of oil flowing into the generator casing per hour (from both shaft seals)
Q_2 = number of cubic feet of oil flowing out on the air side per hour (from both shaft seals)
x = number of cubic feet of hydrogen contained in one cubic foot of oil coming from the vacuum tank at the time t
y = number of cubic feet of air contained in one cubic foot of oil coming from the vacuum tank at the time t
q = number of cubic feet of gas leaving the casing per hour as leakage at the time t
q_s = number of cubic feet of hydrogen of purity S entering the casing per hour at the time t
t = time in hours
H = solubility of hydrogen in the oil leaving the seal (that is, the number of cubic feet of hydrogen in one cubic foot of oil)
A = solubility of air in the oil leaving the seal (that is, the number of cubic feet of air in one cubic foot of oil)
z = per unit hydrogen purity in casing at the time t
$1-z$ = proportion of air in casing at time t
Z_0 = per unit initial hydrogen purity in drum (that is, $z=Z_0$ at $t=0$)
V = total volume of gas in drum and connected apparatus in cubic feet
S = proportion of hydrogen in gas supply per unit
$1-S$ = proportion of air ~~in gas~~ supply, per unit
P = absolute pressure corresponding to degree of vacuum treatment of oil, atmospheres

For the determination of the hydrogen consumption, referring to figure 2, note that the amount of gas in the oil flowing to the seals equals the difference between the amount of gas in the oil entering the vacuum tank and the amount of gas discharged from the vacuum pump. This can be shown to be

$$Q_1P[Hz+A(1-z)]+Q_2AP$$

which must equal

$$Q_1(x+y)+Q_2(x+y)$$

Equating the hydrogen terms and the air terms in these two expressions gives

$$x=Q_1\frac{PHz}{Q_1+Q_2} \tag{1}$$

and

$$y=\frac{Q_1PA(1-z)}{Q_1+Q_2}+\frac{Q_2AP}{Q_1+Q_2} \tag{2}$$

For the condition of constant gas pressure in the generator casing the following equation must be true

$$Q_1(x+y)+q_s=q+Q_1Hz+Q_1A(1-z) \tag{3}$$

Substituting the values of x and y from equations 1 and 2 in (3) and solving for q_s gives

$$q_s=q+Q_1[Hz+A(1-z)]-\frac{PQ_1}{Q_1+Q_2}\{Q_1[Hz+A(1-z)]+Q_2A\} \tag{4}$$

cubic feet per hour

for the amount of hydrogen that must be continuously supplied to the generator casing to make up for that lost through absorption in the sealing oil and that lost by leakage. For high degrees of vacuum and purity both P and $(1-z)$ are small so that the terms of equation 4 containing these quantities may be neglected and the equation for the approximate hydrogen requirement of the generator written as

$$q_s=q+Q_1Hz \text{ cubic feet per hour} \tag{5}$$

To determine the degree of hydrogen purity z that would be maintained in the generator casing with the system shown in figure 2, let the purity at time t be z, and at time $t+dt$ be $z+dz$. The latter purity can be shown to be

$$z+dz=\frac{Vz-[Q_1(Hz-x)+qz-q_sS]dt}{V} \tag{6}$$

Substituting the value of x from equation 1 an expression for dz/dt is obtained of the form

$$\frac{dz}{dt}=\frac{-z}{V}C+D \tag{7}$$

the solution of which is

$$z=\frac{DV}{C}+\left(Z_0-\frac{DV}{C}\right)\epsilon^{-\frac{Ct}{V}} \tag{8}$$

where

$$C=q+\frac{Q_1}{Q_1+Q_2}[Q_1(1-P)+Q_2]\times[H-S(H-A)] \tag{9}$$

and

$$D=\frac{S}{V}[q+Q_1A(1-P)] \tag{10}$$

The second term in equation 8 becomes zero for t = infinity, so that the first term represents the final hydrogen purity in the casing. In figure 10 the hydrogen purity in the casing of the Logan generator, as a function of time following the starting-up of the generator, has been calculated from equation 8 for different values of P, for the value of q obtained in tests on this machine. In figure 11 the final values of purity from equation 8 have been calculated for different values of P and q. In these calculations S has been taken as 0.998. The rates of oil flow from the gas and air sides of the seal have been taken from tests as $Q_1=30$ and $Q_2=61.6$ cubic feet per hour, respectively. An average temperature of the seal oil of 42 degrees centigrade was used, for which the hydrogen and air solubilities were taken as $A=0.11$ and $H=0.041$, respectively. The casing volume V was taken as 1,000 cubic feet.

(b) SHAFT SEALS SUPPLIED WITH UNTREATED LUBRICATING OIL

For this condition of operation figure 20 applies. For the condition of constant gas pressure in the generator casing we have, using the symbols given under (*a*),

$$q_s + Q_1A = Q_1Hs + Q_1A(1-s) \tag{11}$$

whence

$$q_s = q - Q_1s(A-H) \text{ cubic feet per hour} \tag{12}$$

for the amount of hydrogen continuously required by the generator with the seals supplied with untreated lubricating oil. For small values of q, q_s is negative, that is, hydrogen must be discharged from the casing to hold a constant pressure.

To determine the hydrogen purity in the generator casing under this condition, referring to figure 20 the purity at any time $t+dt$ can be represented by the equation

$$s + ds = \frac{Vs - (Q_1Hs + qs - q_sS)dt}{V} \tag{13}$$

whence

$$\frac{ds}{dt} = \frac{-ZE}{V} + F \tag{14}$$

The solution of equation 14 is

$$s = \frac{F}{E}V + \left(Z_0 \frac{-FV}{E}\right)e^{-\frac{Et}{V}} \tag{15}$$

where

$$E = q + Q_1[H + S(A-H)] \tag{16}$$

and

$$F = q\frac{S}{V} \tag{17}$$

In figure 12 the hydrogen purity in the casing of the Logan generator, with untreated oil supplied to the shaft seals, has been calculated from equation 15 for two different values of oil flow to the hydrogen side of the seals, namely, $Q_1 = 30$ and $Q_1 = 48$ cubic feet per hour. For $Q_1 = 48$ cubic feet per hour, using an air solubility $A = 0.11$ in equation 15 gave values of purity s which checked the test values of purity exactly. This value of A was used, therefore, in calculating curve b in figure 12 and the curves of figure 10.

Solubilities of Air and Hydrogen in the Sealing Oil

In applying the equations of the foregoing section, the solubilities of air and hydrogen in the oil supplied to the shaft seals must be either known or assumed. The curves in figure 21 give the solubilities of air and hydrogen in transformer oil, which has a viscosity considerably lower than that of turbine oil and should therefore absorb gas more readily. Tests made by the writer indicated the solubility of air in turbine oil to be 10.8 per cent at 40 degrees centigrade, which agrees closely with the curve for transformer oil in figure 21.

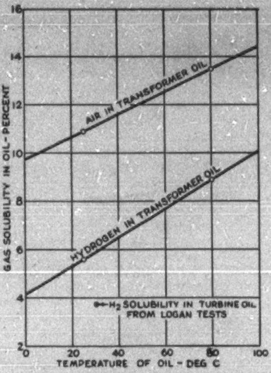


Figure 21. Solubilities of air and hydrogen in transformer oil at atmospheric pressure and at the temperature of the determination, for different oil temperatures, from tests by Rodman and Maude[9]

In the calculation of figure 12 in the preceding section the assumption of an air solubility of 11 per cent, for oil at 42 degrees centigrade, was found to give transient values of hydrogen purity in the casing of the Logan generator which checked the test values exactly. This value for air solubility also agrees closely with the curve for transformer oil given in figure 21. However, calculation of transient values of hydrogen purity for the Cincinnati generator indicated that an assumption of an air solubility of seven per cent, for oil at 36 degrees centigrade, was required in order to check the test values of hydrogen purity. However, the turbine oil for the Cincinnati machine had a viscosity of 200 seconds Saybolt Universal at 36 degrees centigrade, as compared with a viscosity of 130 seconds Saybolt Universal at 42 degrees centigrade for the oil in the Logan turbine. Also the shaft speed of the Logan generator was 45 per cent higher than that for the Cincinnati generator, so that there was greater likelihood that all the air in the seal oil of the Logan machine would be given up to the hydrogen in the casing than would be the case for the Cincinnati generator.

It may be deduced from the above that, for the solubility of air in the turbine oil to be used in the equations for the shaft seal, the curve for air solubility in transformer oil in figure 21 may be assumed correct if the oil is of low viscosity, and if the shaft speed is of the order of 12,000 feet per minute; however, for oils of high viscosity and at fairly low shaft speeds, the air solubility should be taken as 60–70 per cent of the values for transformer oil.

For determining the solubility of hydrogen in the turbine oil, tests made on the Logan generator at standstill with the shaft seals supplied with treated and with untreated oil were used. Substituting the test values of hydrogen consumption for these two conditions in equations 4 and 11 of the preceding section gave a value for hydrogen solubility of 0.037 at 30 degrees centigrade. This is 62 per cent of the solubility of hydrogen in transformer oil at this temperature. Assuming this same percentage to apply for other temperatures, the hydrogen solubility at the running temperature of 42 degrees centigrade was estimated to be 0.041 and this value was used in estimating the curves in figures 10 and 11.

Until more definite information is available regarding the solubility of hydrogen in turbine oil under the conditions which obtain at the shaft seals of a hydrogen-cooled generator, it may be considered sufficiently accurate to assume this solubility to be from 60 to 70 per cent of the solubility for hydrogen in transformer oil, as given by figure 21.

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Discussion

Sterling Beckwith: See discussion, page 34.

E. H. Freiburghouse (General Electric Company, Schenectady, N. Y.): The reliability of hydrogen cooling of large turbine generators has been thoroughly demonstrated. Referring to figure 18 of Mr. Snell's paper, it is seen that a continuous service record without interruption from any cause associated with the hydrogen-cooling features has been obtained on all of the generators of the design described in his paper. Nine of these machines are now in service. As a result of our operating experience with these machines, however, a number of simplifications and improvements have been found possible and these are being incorporated in the designs of our newer hydrogen-cooled generators. Some of these design changes and novel construction features will be mentioned in this discussion.

Figure 1 of this discussion shows the type of construction used with our latest hydrogen-cooled generators of the 3,600-rpm type, and the arrangement of the gas-

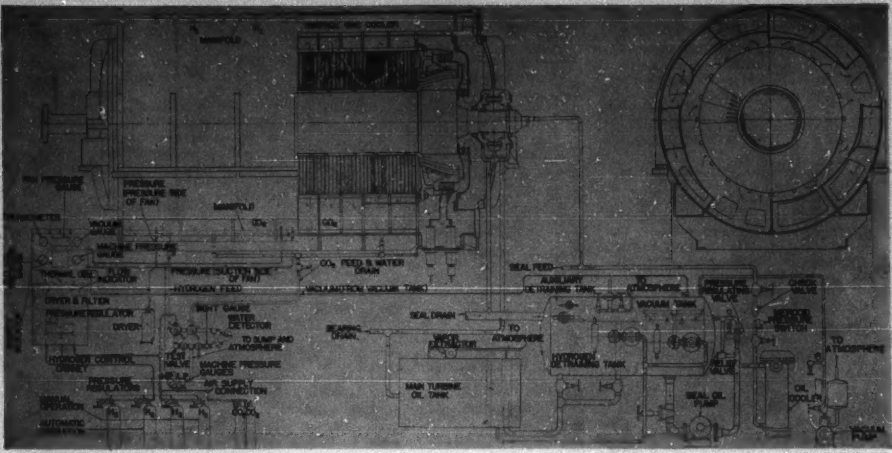

MANIFOLD
FAN PRESSURE GAUGE
PRESSURE (PRESSURE SIDE OF FAN)
MANIFOLD
CO_2
CO_2
VACUUM GAUGE
MACHINE PRESSURE GAUGE
CO_2 FEED & WATER DRAIN
FLOW INDICATOR
PRESSURE (SUCTION SIDE OF FAN)
HYDROGEN FEED
VACUUM (FROM VACUUM TANK)
DRYER & FILTER
PRESSURE REGULATOR
DRYER
SIGHT GAUGE
WATER DETECTOR
TO SUMP AND ATMOSPHERE
HYDROGEN CONTROL CABINET
TEST VALVE
MACHINE PRESSURE GAUGES
PRESSURE REGULATORS
AIR SUPPLY CONNECTION
MANUAL OPERATION
AUTOMATIC
SEAL FEED
AUXILIARY DETRAINING TANK
TO ATMOSPHERE
PRESSURE REGULATING VALVE
CHECK VALVE
VACUUM TANK
SEAL DRAIN
BEARING DRAIN
TO ATMOSPHERE
VAPOR EXTRACTOR
HYDROGEN DETRAINING TANK
RELIEF VALVE
TO ATMOSPHERE
MAIN TURBINE OIL TANK
SEAL OIL PUMP
OIL COOLER
VACUUM PUMP

Figure 1

control and shaft-sealing systems. In this design the four gas coolers are located above the floor line, with the water connections at the extreme ends of the frame instead of at the sides, as in the earlier design. This construction permits easier cleaning of the cooler tubes than with the former arrangement.

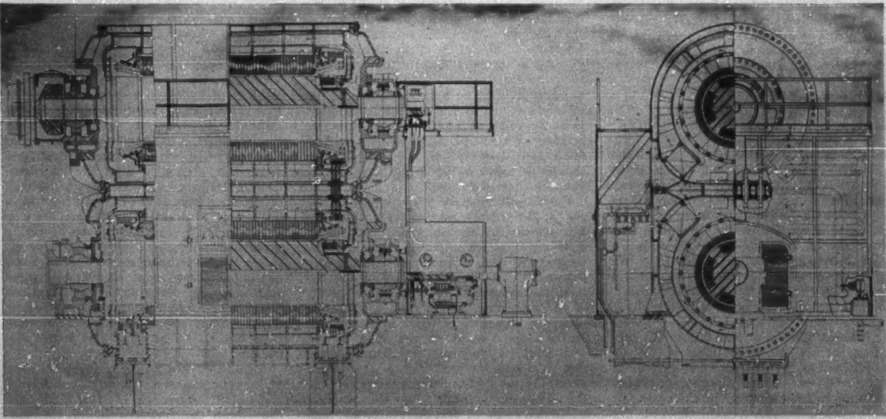
The arrangement of the gas control system shown in the above figure is appreciably simpler than the arrangement formerly used. Wherever possible, valves with diaphragm-packed stems are being used to reduce the possibilities of leakage. The solenoid gas-admission and scavenging valves, used with our present machines, have been replaced in the arrangement shown in figure 1 with diaphragm-type valves. This was done to avoid the leakage experienced with the solenoid valves, and also to eliminate the mercury relief valve.

The arrangement of the carbon-dioxide equipment shown in figure 1 is similar to that used with the machines now in service, and includes heating equipment which permits the carbon dioxide to be admitted to the generator casing as a gas. Carbon dioxide in the liquid form may be used thereby reducing the time for purging and eliminating the heating equipment, however, somewhat more carbon dioxide would be required.

Our laboratory has developed a new type of hydrogen purity indicator for use with hydrogen-cooled machines, which is more rugged and also more accurate than the indicator now used. This operates on the thermal-conductivity principle, as does the present indicator, but uses alternating instead of direct current, thus eliminating the need for the storage battery and rectifier.

The horizontal arrangement of shaft-sealing equipment shown in figure 1 is being used with a number of generators now under construction. This provides greater accessibility of the parts; however, a vertical arrangement of the treating tanks has been used wherever it was more suited to the station space. The different elements of either arrangement may be mounted on a common base and assembled as a unit.

Figure 2



One of the more unusual designs for hydrogen-cooled generators is that employed with the 137,500-kva vertical compound turbine generator set built for the Ford Motor Company, which is illustrated in figure 2 of this discussion. This generating unit, which is now being installed at Dearborn, Mich., consists of two separate hydrogen-cooled generators, each of 68,750 kva at 1,800 rpm, which are mounted one on top of the other. Each generator is provided with a double armature winding, and the windings of the two machines are paralleled through connections at one end as shown in the above figure. The casing of one generator is connected with the casing of the other through the conduits used for the electrical connections between the two armatures, so that the two casings are considered as one in filling or emptying the casings with hydrogen and carbon dioxide. However, each machine is provided with its own hydrogen purity indicator and a separate fan pressure gauge. Two gas coolers are provided with each generator, those of the upper generator being located in the bottom of the frame, while those of the lower generator are in the upper part of the frame. The hydrogen control panel, item 11, figure 2, is located in the side of the frame structure of the lower generator. A single set of treating tanks is provided for the shaft-sealing oil for both generators, also a single seal oil pump and vacuum pump.

This paper clearly indicates that much has been accomplished and important progress is being made in regard to the cooling of turbine generators by means of hydrogen. Operating experience is being rapidly accumulated, also further improvements in design and materials may be expected. It is, therefore, certain that hydrogen cooling will be an essential feature of large high-speed machines which are to be built, and generators of even larger power output per shaft will be possible in the near future.

Carl J. Fechheimer (consulting engineer, Milwaukee, Wis.): The desirability of using hydrogen as a cooling medium in large high-speed electrical machinery has been recognized since the presentation of the splendid paper by Knowlton, Rice, and Freiburghouse in 1925. Having done considerable work some years ago in the development of gland seals, devices for control and proportioning of parts when hydrogen cooling was beginning to be discussed, the paper by Mr. Snell is of especial interest to me.

It seems to me that the fear of an explosion has caused our engineers to be overly cautious. If an inert gas is used properly as an intermediary when changing from air to hydrogen or the reverse, it should not be necessary to burden the generator with the additional expense attended with making it explosion proof. Particularly with the large diameters associated with the large slower speed 1,800-rpm machines, it is essential to make the enclosing brackets with thick walls, probably with ribbing, and to curve them, so that they will withstand an explosion safely. I would inquire of the author whether consideration has been given to the design which will be sufficiently strong for ordinary operation only, and if so, by what percentage the cost of a large 1,800-rpm generator would thereby be reduced? Also, if the cost would thereby be reduced appreciably, and if the control equipment and shaft seals were simplified, as suggested by Mr. Snell, would it not be economically desirable to build hydrogen-cooled turbine generators for smaller ratings than recommended in the paper?

It is of interest to study table I on temperatures. The losses with air for 64 per cent of the load with hydrogen are 78 per cent higher as given by the gas temperature rise, and 87 per cent higher as given by the water temperature rise. The fairly close check between the two methods of computation is interesting. Undoubtedly, the windage loss was a very large percentage of the total loss. Even with considerably higher I^2R losses, the field temperature rise above the ingoing gas temperature was the same, but was ten degrees less with hydrogen, if referred to the ingoing water temperature. As it is the ingoing water that is the ambient, why not take the water temperature, and not the gas temperature, as reference?

The armature-coil temperature rises are even more favorable for hydrogen, they being only 75 per cent as high (referred to the ambient water), even though the I^2R loss was approximately 2.4 times as great. This considerable gain was undoubtedly due chiefly to the relatively high heat conductivity with hydrogen, which reduced the thermal drop from the copper to the iron, the transverse drop in the iron packages, and the heat transfer drop from the cooling surfaces to the cooling medium. Has Mr. Snell given consideration in the design to the feasibility of using thicker iron packages, and of increasing the current density in the armature copper, than would be employed in an air-cooled machine?

It seems from the test results that the usual ratio of rating for hydrogen to air cooling of 1.2 to 1.3 is entirely too conservative. If the data for this machine are typical of other hydrogen-cooled turbine generators, even 50 per cent increase in rating is conservative.

In figure 9 the use of diffusers to improve the efficiency and increase the pressure of the centrifugal fan is interesting, and was considered many years ago. It was found that propeller fans were more efficient, were not so much restricted at entrance as centrifugal fans, and could be applied to larger sizes of machines. It is worthy of note that with the very high peripheral velocities of the centrifugal fans, more pressure can be developed than is necessary; the result is that the fans are not operated at the most favorable point on their pressure-volume curves. Is it not likely, as shown in table I, that the high gas temperature rise when air is used, is largely due to the high fan pressure and rather low fan efficiency? If so, perhaps the ratio of ratings would not be so high (as suggested in the preceding paragraph) if the temperature rise of the air when passing through the fans were reduced?

As indicated in figure 9, a number of hollow metal pads between lamination packages, were used for the water coolers. The details of construction are not entirely clear and I would inquire whether the pads interfered with the flow of hydrogen, and whether the thermal drops from laminations to water were appreciably altered, as compared with the usual finned surface cooler.

Perhaps one thing that has helped to bring the construction of hydrogen-cooled machines to the forefront, is that now we can calculate quite closely the fans, pressures and volumes, as well as the distribution of the cooling medium throughout the machine, which could not have been done a comparatively few years ago.

It seems that today, instead of hydrogen-cooled machines being hazardous, they are actually the safest machines, because there is no internal gas to support combustion, there is negligible damage from corona, and they are as clean after years of service as when first placed in operation.

P. L. Alger (General Electric Company, Schenectady, N. Y.): When hydrogen cooling for electrical machinery was first proposed, the chief objection to its use was the fear of an explosion. Tests made to determine the pressure developed in an explosion of the most explosive hydrogen-air mixture, initially at atmospheric pressure, indicated that the explosion pressure, for a machine assembled with core and windings, would not exceed 50 pounds per square inch. The theoretical explosion pressure, for no heat loss to the surroundings, is 180 pounds. Since, however it is not difficult to construct the frame of the machine to withstand a force of 200 pounds per square inch, it is apparent that there is very little to fear from an explosion in a hydrogen-cooled machine.

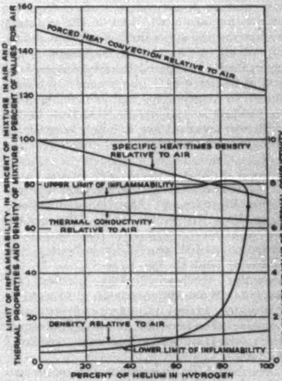


Figure 3

Nevertheless, it is interesting to consider other gases which could be used for cooling that are not subject to these explosion risks, slight as they are, thus promoting economy in methods of installation and handling. An obvious possibility is the use of helium. The light weight of helium gives very low windage losses, but its materially higher cost and its inferior cooling properties make it undesirable for use as a pure gas. It therefore occurred to me that the properties of a mixture of helium and hydrogen should be investigated, in the hope of obtaining a gas free from explosion risk, that would preserve practically all of the advantages of pure hydrogen. The results of a theoretical study of this subject by Doctor Saul Dushman and D. S. Snell are presented in the two accompanying figures.

Figure 3 of this discussion shows the limits of inflammability of a mixture of hydrogen and helium, also the thermal properties of the mixture compared with air. Figure 4 shows the calculated effect on the explosion pressure at the upper limit of inflammability, of mixing helium with hydrogen. The curves were calculated for an assumed heat loss of 25 per cent. The amount of helium shown to be required, and its cost, in order to produce a significant reduction in the explosion pressure, are so large, however, as to make this procedure unattractive.

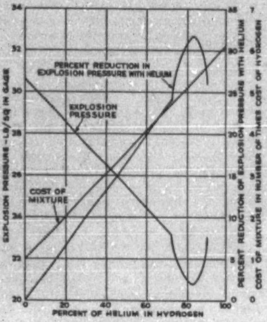


Figure 4

C. F. Hill and L. J. Berberich (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The operating experience described in these papers on hydrogen-cooled generators indicates that hydrogen will in the near future be the generally accepted cooling medium for machines of the larger ratings. It should be mentioned again that electrical insulation is subjected to fewer deteriorating influences when operating in hydrogen as compared with air.

We should like to re-emphasize one or two of these. The effects of oxygen, ozone, and oxides of nitrogen, the last two being produced only under corona conditions, are completely absent. These phenomena have been discussed in previous papers and need be only mentioned here. But even more important, is the problem of moisture absorption by the insulation which can be almost completely eliminated in the hydrogen-cooled machine.

Here the only source of moisture is from the oil supplied to the hydrogen seals. The moisture is, of course, introduced into the lubricating oil system by the turbine. When moisture-laden oil is passed into the seals, which run in the neighborhood of 100 degrees centigrade, a considerable amount of water is vaporized into the machine. This water vapor acts as a contaminant to the hydrogen and increases the apparent hydrogen consumption. Mr. Snell in his paper stresses the contaminating influence of air from the seal oil, but it is our experience that moisture can also be a serious diluent of the hydrogen.

This moisture problem has been eliminated by the combination of an oil vacuum treating unit and a gas drying unit which are described by Messrs. Ross and Sterrett. The simple vacuum-treating unit reduces the water content of the oil to less than 0.003 per cent by weight. With added complications, the vacuum system could be made to reduce the moisture content still lower. But even at very low concentrations, some moisture is always vaporized from the oil in passing over the seals which will gradually build up moisture concentration in the machine and this will eventually increase the hydrogen consumption. This, together with the possibility of failure of the oil vacuum treating system, justifies the addition of the exceedingly simple activated-alumina gas dryer described by Messrs. Ross and Sterrett.

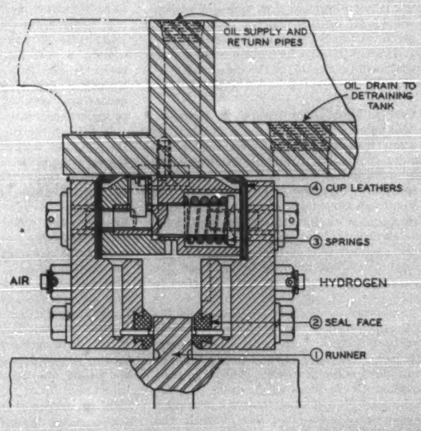
We have studied one of these machines and the results may be of interest. Actual determination of the moisture content of the gas in a 50,000-kw machine which had been in operation for some time has been made. With the vacuum-treating unit and dryer in operation the moisture content was approximately 0.01 gram per cubic foot of gas. This corresponds to a relative humidity of 0.4 per cent at machine temperature of 55 degrees centigrade (machine was only partially loaded at time of test), a dew point in the neighborhood of −25 degrees centigrade, and volume concentration of water vapor of approximately 0.06 per cent. The effectiveness of this system of moisture and air removal is attested by the fact that the hydrogen consumption for this machine is only 20 cubic feet per day. A similar test was made on the same machine with the moisture elimination system not in operation and a water content of roughly 1.0 gram per cubic foot gas was found. This corresponds to a relative humidity approximately 25 per cent at 55 degrees centigrade, a dew point of 30 degrees centigrade and volume concentration of water vapor of 4.8 per cent. Under these conditions the hydrogen consumption was very high and water was condensed from the gas at the coolers which were operating approximately 10 degrees centigrade below the dew point. The necessity of removing moisture from the oil going to the seals is thus indicated.

Returning again to the electrical insulation, the combined oil-treating and gas-drying system so effectively removes moisture and air from the oil that the insulation may be considered almost hermetically sealed in an inert medium. The problems caused by the effects of ozone, oxides of nitrogen, moisture, and accumulation of dirt are all eliminated. The performance of the insulation in hydrogen should therefore be far better than in air.

S. H. Mortensen (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors of the papers on hydrogen-cooled turbine generators have so admirably covered the design and operating problems incidental to hydrogen-cooling of turbine generators, that I find nothing to add except a description of the Allis-Chalmers company's hydrogen-shaft seal and a discussion of its effect upon certain faces of the generator operation.

The Allis-Chalmers company's shaft seal is shown in figure 5 of this discussion and consists of a small runner, item 1, provided on the generator shaft at each end of the machine close to the main bearing, and against each face of the runner a conventional babbitt thrust bearing, item 2, is held by springs, item 3, permanently set to give a pressure equal to about a third of the normal capacity of the bearing. Around the inner periphery of each thrust bearing, but separated from it by a groove, is a continuous babbitt face about an eighth of an inch wide. During operation this continuous face provides the required seal, since it is

Figure 5. Shaft seal



separated from the runner by only the thickness of the oil film in the thrust bearing. Oil under pressure is circulated around the flange between the two thrust-bearing faces to provide cooling and lubrication and to oppose the pressure of the hydrogen at the small clearance between the continuous babbitt face and the flange. The oil pressure counterbalances the spring pressure so that resulting bearing load is very low. Cup leathers, item 4, are provided at the outer periphery of the oil chamber to retain the oil and provide flexibility.

The Allis-Chalmers type of seal is suitable for operation with hydrogen pressures from zero up to ten pounds per square inch without oil or seal adjustments.

As this type of seal provides an air-tight joint, air or gas can be removed from the generator by evacuation. This makes it practicable to simplify the scavenging process described by authors to the extent of eliminating the use of carbon dioxide and offering advantages such as;

1. No carbon-dioxide or other inert gas is required for scavenging generator.
2. Since interchange of hydrogen and air takes place at one fifth of an atmosphere or less, only about one fifth as much excess hydrogen above one volume is required.
3. Extra pipes to drain all "pockets" of carbon-dioxide are not essential.
4. Less time is required to empty or fill generator with hydrogen.
5. The filling operation is simplified.

Operating experience, over a period of months, further shows that the sealing oil volume which gets into contact with the generator hydrogen is so small that oil treatment becomes unnecessary except for its passing through a hydrogen detraining tank before it is returned to the oiling system.

It is expected that additional operating experiences will result in further simplifications in the application of hydrogen as a ventilating medium.

Philip Sporn (American Gas and Electric Service Corporation, New York, N. Y.): Early in 1935 studies indicated the desirability of installing at Logan, W. Va., a 40,000-kw 3,600-rpm high-pressure superposition turbine, in order most economically to develop and increase the capacity of the existing power plant at this location. At that time the manufacturer stated that cooling of the rotor for a generator of this size and speed could be accomplished only by utilizing hydrogen ventilation. Following our extensive and successful experience with seven large-capacity hydrogen-cooled synchronous condensers then in service on the American Gas and Electric Company system, we unhesitatingly accepted hydrogen cooling for this new generator unit. In the case of synchronous condensers, the bearings are entirely enclosed in the outer shell, and therefore the problem of sealing the shaft against gas leakage is not encountered. However, extensive tests and factory operating experience with the oil seal at Schenectady over a period of years, provided a basis for anticipating satisfactory performance from this shaft-sealing arrangement in power-station service.

The Logan generator went into operation as a hydrogen-cooled machine on December 2, 1937, and from that time until February 28, 1939, the unit has run a combined total of 232 days. The running time lost during this period has not been occasioned by any trouble or maintenance in connection with the generator proper or its gas sealing system. No difficulty of any great importance has been experienced with the shaft-sealing system and its accessory equipment. No failure to maintain the seals has occurred at any time. As of February 28, 1939, this seal system had operated 308 days. We are now maintaining a purity varying from 99 to 100 per cent and the gas consumption is averaging approximately 40 cubic feet per day. The delivered cost of this gas, including handling of the cylinders, amounts to approximately 2 cents per cubic foot, so that the yearly cost on this basis is less than $300.

Mr. Snell describes in his paper the pad cooling arrangement for direct water cooling of the stator. Plant condensate was used as make-up for this pad liquid system. In order to eliminate the possibility of air pockets in the pads, it is necessary to obtain a high vacuum before filling with condensate. In this respect some difficulty was experienced initially but by circulating the pad liquid for some time and by wasting a portion from the overhead expansion tank, it was possible to remove practically all of the air still remaining in the pads. The operation of the system has been entirely satisfactory.

Mention is made of operation at gas pressures as high as 15 pounds per square inch. The Logan unit has been run for short periods at these higher pressures with satisfactory performance of the shaft-sealing system and without obtaining excessive consumption of gas. We have had considerable experience with the operation of hydrogen-cooled synchronous condensers at pressures up to and including 15 pounds per square inch. We hope to present detailed operating data covering such performance in a coming paper before the Institute.

D. S. Snell: I wish to thank Mr. Fechheimer for his interesting and valuable discussion. Mr. Fechheimer has contributed much to the development of hydrogen cooling and his comments on the subject are welcomed. As to the saving effected with hydrogen-cooled generators of the 1,800-rpm type through making the machine structure strong enough only for normal operation, but not strong enough to withstand an explosion, I believe that for the type of construction employed with our hydrogen-cooled generators no saving would be obtained. For with a construction in which the generator bearings are supported in the end shields, a construction that is rigid enough for ordinary operation is also strong enough to withstand an explosion. At the present time the cost differential between the hydrogen-cooled and the air-cooled machine is principally due to the cost of the auxiliary equipment, and as this cost does not vary greatly with different sizes of machines, it is difficult at the present time to justify the use of hydrogen cooling for generators in sizes much smaller than the minimum sizes mentioned in the paper.

Regarding Mr. Fechheimer's proposal that ingoing water temperature rather than ingoing gas temperature be used as the reference for basing temperature rises for hydrogen-cooled machines, we have given considerable thought to this matter but still feel that the use of the ingoing gas temperature offers the best basis for making guarantees on this type of machine. The use of the ingoing gas temperature as a basis for temperature rise allows an accurate comparison between different machines as the true rise of the machine alone is obtained, whereas if the ingoing water temperature were used as a basis, a designer could skimp the design of the generator and use an extra large cooler, obtaining the same over-all temperature rise with this machine as with a machine of more liberal design. Obviously the machine with the lower temperature rise within itself is the more liberal design.

Also, since it is easy to test the coolers and the generator separately, there is little or no reason to combine the two in one over-all guarantee, and especially is this true due to the fact that the generator is the major portion of the investment and the more critical.

The Institute at the present time has adopted the ingoing gas or air as a basis for the ambient temperature on which rises should be predicated, and we can see no reason for the Institute's making two different standards for large rotating equipment because one happens to be enclosed and the other an open machine, as basically the temperature rises, which are satisfactory for one machine are applicable to the other type.

Regarding the thickness of the stator iron packages and the armature current density in the hydrogen-cooled machine, both these quantities have been increased over their values for the air-cooled machine.

Mr. Fechheimer's suggestion that the increase in rating of a generator by the use of hydrogen should be considerably greater than the 20 per cent mentioned in the paper, based on the data given in table I, neglects the fact that the hydrogen-cooled generator must be proportioned differently than the air-cooled machine, to obtain the same electrical stability. If this change in design is considered it will be found that the increased output per pound of material with hydrogen will be of the order of 20 to 25 per cent.

Regarding the use of diffusers in conjunction with the centrifugal fans employed with these machines, while these are generally provided with air-cooled generators they have been omitted from many of the hydrogen-cooled machines for the reason that fan efficiency is relatively unimportant in a hydrogen-cooled machine, while the reduction in machine length obtained by omitting the diffusers is of value. This omission results in a rather high windage loss and temperature rise of the air when the machine is operated in air, as Mr. Fechheimer has observed in connection with table I; however, since these machines are intended principally for operation in hydrogen, this feature is not considered important. As to the relative merits of centrifugal and propeller fans, we recognize that while the propeller fan has a slight advantage in efficiency it also requires a greater amount of axial space on the shaft for its support, thus increasing the distance between bearing centers.

Referring to Mr. Fechheimer's inquiry as to the construction of the machine illustrated in figure 9, figure 6 of this discussion illustrates the construction of the metal pads which are employed for removing the heat from the stator core and the cooling gas. The pads of the plain type are used in the end sections of the stator core and remove the generated heat by direct conduction to the cooled pad. The finned pads are used in the center part of the core and absorb heat both by conduction through contact with the laminations, and by convection, through passage of the heated gas from the air gap to the back of the core between the fins. The finned pads take the place of the surface gas coolers used with the ordinary type of generator. The fan pressure required to ventilate this type of machine is practically the same as is required with the ordinary type, for the same volume of gas circulated.

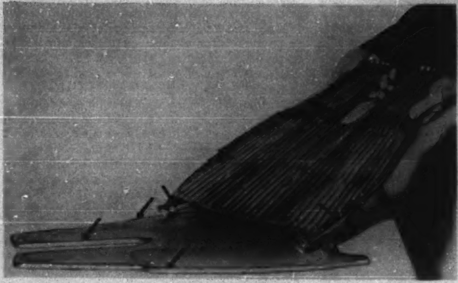


Figure 6. Monel-metal cooling pad for hydrogen-cooled generator, copper-brazed

Arrows indicate brazed joints

It is true, as Mr. Fechheimer suggests, that the development of the hydrogen-cooled machine has been accelerated by the progress previously made in methods of calculating fan performance and ventilation characteristics, and also, I would add, in methods of predetermining the thermal behavior of electrical machines. For much of this progress we are indebted to the researches of Mr. Fechheimer.

The shaft seal described by Mr. Mortensen possesses many desirable features. Several years ago we tested a seal of a somewhat similar type, in which a stationary carbon ring pressed axially against a runner carried on the shaft. Trouble was experienced, however, through air being pumped into the generator casing between the ring and runner by centrifugal force. In Mr. Mortensen's seal this pumping action is prevented by supplying lubricant to the rubbing surface under pressure.

While it is desirable to eliminate from the sealing system as many auxiliaries as possible, the elimination of the vacuum tank, as suggested by Mr. Mortensen has certain disadvantages. Without the use of vacuum-treated oil in the shaft seals, the hydrogen purity in the generator casing will decrease, even if at a slow rate, and the operator must manually scavenge the casing at intervals in order to maintain the desired hydrogen purity. If the oil is vacuum treated, however, no scavenging is required. Also, the vacuum treatment of the sealing oil, aside from its value in the operation of the hydrogen system, is of definite value with respect to the operation of the bearings of the turbine generator set, since vacuum treatment of oil removes impurities and thereby prolongs its life. Considering that the vacuum tank requires little space and practically no servicing, it does not seem that the elimination of this device offers as many advantages as its retention.

The evacuation of the generator casing before filling with hydrogen, or when emptying, as proposed by Mr. Mortensen, has some advantages over the use of an inert gas. However, to evacuate the casing requires that the generator be at rest, whereas the inert gas may be introduced with the generator rotating, which is frequently done to save time.

Messrs. Hill and Berberick mention the problem of contamination of the hydrogen by moisture given up from the sealing oil as being of equal importance to the problem of contamination by air from the oil. While this is probably true where the temperature of the oil leaving the seals is in the neighborhood of 100 degrees centigrade, it is not the case where the temperature of this oil is approximately 60 degrees centigrade, as is the case with the type of seal described in my paper. Since the radial shaft clearance of this seal is the same as that of the bearing the energy loss in the oil film is low compared with the oil flow so that only moderate oil temperatures are obtained. This precludes the possibility of moisture being vaporized out of the oil into the generator.

Mr. Beckwith suggests the elimination of the hydrogen relief valve. In the system described in my paper a relief valve of the mercury type is used to prevent the discharge of hydrogen into the air detraining tank in the event of excessive pressure in the casing. In our later designs a diaphragm type of relief valve is used, which serves also for scavenging. In our case this valve can hardly be dispensed with.

The hydrogen requirement for these machines has been found to vary approximately with the square root of the hydrogen pressure, as stated by Mr. Beckwith, although theoretically the part of the hydrogen requirement due to absorption in the sealing oil should vary about directly with the absolute pressure. The discrepancy is probably due to a slight opening-up of the casing joints at the higher pressures. Tests on the Logan 50,000-kva generator showed a hydrogen requirement of 40 cubic feet per day at one-half pound pressure and 300 cubic feet per day at 15 pounds.

Mr. Sporn describes operating experiences with the Logan generator, which was one of the first hydrogen-cooled generators to be placed in service. It is gratifying to us that this machine, which embodied two radical departures from conventional design-hydrogen cooling and pad cooling—should have operated practically without trouble since installation. Mr. Sporn's company has been a pioneer in the use of hydrogen-cooled machinery, having installed in 1928 one of the first hydrogen-cooled synchronous condensers and in 1937 one of the first hydrogen-cooled generators. His faith in this new development has contributed largely to its success and it is gratifying to us to see that his confidence in hydrogen cooling has been justified.

Mr. Alger mentions a theoretical study made to determine the desirability of using a mixture of helium with hydrogen to reduce the explosion pressure. At the time this investigation was made the cost of commercial helium was approximately six times the cost of hydrogen. Since then the cost of helium has been greatly reduced and it can now be obtained for approximately twice the cost of hydrogen. Even with this reduction in cost, however, the use of helium as a cooling agent is not considered desirable since its cooling properties, as explained in the paper, are considerably inferior to those of hydrogen. Since the type of generator construction and the shaft-sealing and gas-control equipment would be substantially the same whichever cooling medium were used, and the helium-cooled generator would be appreciably larger than the hydrogen-cooled machine for a given output, it will be evident that the use of helium is not economically advantageous.

Mr. Freiburghouse has mentioned some of the recent improvements that have been made in the design of the hydrogen-cooled generator, also some of the details of construction of the 137,500-kva hydrogen-cooled generating set for the Ford Motor Company. This generator set has recently been placed in service with hydrogen. The shaft seals used with these generators are of a different type from those used with our earlier generators and consist of metal rings bearing radially on the shaft, the rings being supported in a housing attached to the end shield. This type of seal requires less oil than the type described in the paper, so that smaller treating tanks and equipment may be used.

Besides the Ford generators, two other hydrogen-cooled generators have been placed in service by our company since my paper was written. We have now 12 of these machines in operation, their combined rating being 770,000 kva. The largest 3,600-rpm machine is rated 66,667 kva while the largest 1,800-rpm generator is rated 176,470 kva. A total of 19 hydrogen-cooled generators are under construction at the factory, with a combined output of 1,035,000 kva. One of these units is rated 50,000 kva at 3,000 rpm and will be installed at Buenos Aires. This is the first hydrogen-cooled generator to be sold for foreign installation. The growing popularity of this new type of generator indicates that the former apprehensions regarding hydrogen cooling have been largely dissipated by the successful operation of the units now in service and that the many advantages of hydrogen cooling are becoming widely recognized.

Some Factors in the Mechanical Design of High-Speed Turbogenerators

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Synopsis: The following paper deals with the approach and the results obtained by advanced engineering technique applied to a few of the problems in large high-speed turbogenerator rotor design. Research and design details incidental to yoke vibrations, application of hydrogen for ventilation, etc., may be dealt with in a future paper.

PROGRESS in steam-turbine design combined with the rapid upturn in power requirements experienced in 1936–37, created a demand for high-pressure high-temperature steam-turbine units of large capacity. It is desirable to operate this type of prime mover at the maximum practicable speed and a number of large air- or hydrogen-cooled 3,600-rpm 60-cycle machines were ordered from the company the writers are associated with. As our previous experience was limited to machines of capacities below 9,500 kva, the larger machines presented further problems, making it advisable to check experimentally the reliability of standard and advanced calculating methods to insure reliable machines, and the ground work for the development of larger capacity machines.

As an example of recent-type two-pole 60-cycle generator construction figure 1 may serve, which depicts a 43,750-kw air-cooled turbine generator with a rotor diameter of $33^{1}/_{4}$ inches. To limit the axial length of this machine it was designed with external blowers mounted inside the yoke above the armature. The cooling elements for the ventilating air were also made part of the yoke, thereby reducing the external generator space requirements to a minimum.

To keep this paper within bounds, it will deal only with three of the many factors investigated, which, in the following, are discussed in the order of their original consideration:

1. The determination of critical speeds[1,2] by test for comparison with calculations for symmetrical- and unsymmetrical-section two-pole rotors.
2. Stress analysis of the generator rotor body.
3. Analysis of the lubrication characteristics of the generator bearings to determine maximum bearing efficiencies.

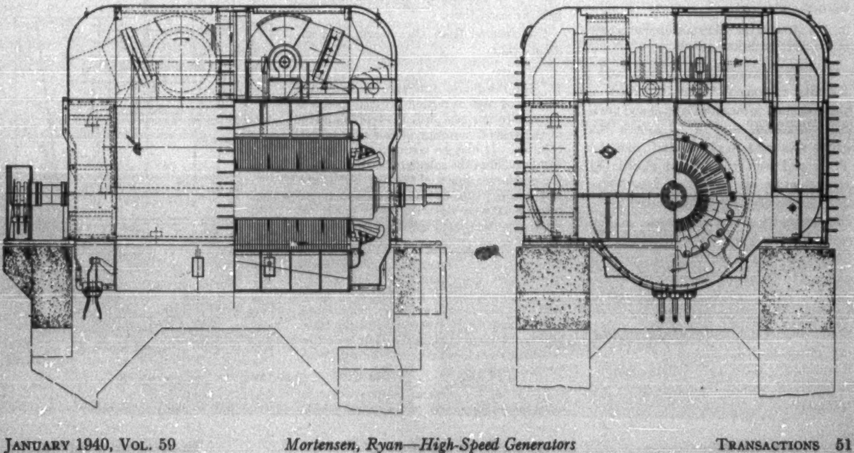

Figure 1. Outline of 43,750-kw turbogenerator

A. Vibration Analysis of Long Rotors

To investigate the accuracy of critical-speed calculations and the vibration behavior of long turbogenerator rotors, a shaft model was constructed having scalar dimensions similar to 3,600 rpm turbogenerators. This rotor model was operated at various speeds, and the vibrations of the shaft and pedestal supports were observed and recorded.

The tests were made for two conditions of the model; first, as a round, symmetrical rotor; and, second, as a two-pole slotted rotor. The maximum shaft diameter was six inches with a 12-foot span between bearing centerlines. It was mounted in $2^{1}/_{2}$-inch by six-inch bearings, and driven by a motor having a maximum speed of 4,000 rpm. The weight of the unslotted rotor was 815 pounds and its moment of inertia 63.63.

Following the tests on the round rotor, slots were machined in the shaft model, to obtain a cross section having two moments of inertia at right angles, approximately to the scale of the average

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The co-operation of the steam-turbine department, Allis-Chalmers Manufacturing Company, and the aid of Charles D. Wilson are respectfully acknowledged in the measurement and analysis of the lubrication tests.

[1] For all numbered references, see list at end of paper.

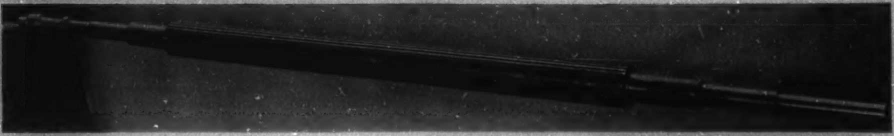


Figure 2. View of model shaft—slotted

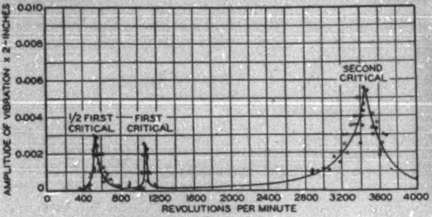


Figure 3. Amplitude-frequency graph of model-shaft vibrations

Two-pole slotted rotor

Figure 4. Two-pole turbogenerator rotor

two-pole generator rotor. The slots were cut one-fourth inch wide and one inch deep, spaced $^1/_{28}$ of a circle, with ten slots per pole. The shaft was operated as before and the vibrations observed. A photograph of the shaft after machining is shown in figure 2.

The weight of the slotted rotor was 695 pounds; its maximum moment inertia 52.27; its minimum inertia 42.17.

As stated, the objects of the tests with the model rotor were:

1. To determine experimentally the first (n_1) and second (n_2) critical speeds for comparison with calculated values;

2. To measure the effective flexibility of the pedestals; and

3. To observe the vibration characteristics of the shaft at various speeds.

A Geiger recording vibrograph[3] and a Karelitz vibrometer[4] were mounted on the pedestals to measure lateral and vertical vibrations, and a $^1/_{1,000}$-inch dial gauge was used to check shaft motions. The speed was read on an electric tachometer.

As originally set up, the vibrations of the symmetrical shaft were too small to enable observation of the critical speeds. The rotor was unbalanced a sufficient amount to obtain measurable vibrations. After slotting the rotor for the second tests, it was necessary to straighten the shaft, and balance was obtained by placing plugs in balancing holes. At the final balance, the critical speeds could not be detected, and a slight unbalance was restored.

The calculated and measured critical speeds compared as follows:

		Test	
	Calculated	Horizontal	Vertical
Symmetrical rotor	$n_1 = 1,060$	$n_1 = 1,080$	1,085
	$n_2 = 3,400$	$n_2 = 3,280$	3,370
Slotted rotor	$n_1 = 1,072$	$n_1 = 1,074$	
	$n_2 = 3,497$	$n_2 = 3,430$	

In figure 3 are plotted the values of the measured total vibration of the outboard pedestal in a horizontal direction at various speeds for the slotted unsymmetrical rotor. The existence of three critical vibrations were established with no other disturbances in the running range. The amplitudes of vibration were proportional to the arbitrary unbalance conditions.

Conclusions from the above tests indicate that:

1. The maximum difference between the calculated and measured critical speeds is about three per cent, which is representative of the general accuracy of this type of calculation. It may be assumed that on full-scale machines the calculated critical speeds are within the limits of test measurements. The average moment of inertia of the slotted rotor section is the effective moment of inertia for the critical-speed calculations.

2. For the symmetrical rotor, the pedestal flexibility has no appreciable effect on the first critical speed, and only about a three per cent reduction in the horizontal direction over the vertical at the second critical. This quantity is representative of the effect on full-size apparatus, and is maintained by requiring rigid foundations and sturdy bearing pedestals.

3. Running the shaft at various speeds to observe the vibration characteristics indicated that no appreciable vibration occurred except in the immediate vicinity of the criticals. For the slotted rotor, a subcritical was evident at one-half the first critical speed, due to the variable moment of inertia of the shaft. As shown in the vibration curve, figure 3, the slotted rotor would operate as well above the second critical speed as between the first and second. Thus it may be further concluded that very long turbogenerator rotors operating normally above the second critical speed would be entirely practical from a vibration, balancing, and critical-speed standpoint. The present critical speeds for 3,600 rpm turbogenerators could be reduced from 1,600 and 5,000 rpm, respectively, to 900 and 2,700 rpm, making possible a considerable increase in generator capacity with

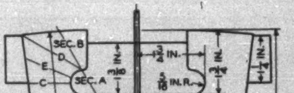


Figure 5. Working sketch of photoelastic model

Transparent Bakelite

added flexibility in design. Such a trend may be anticipated for future consideration

B. Rotor Stress Analysis

The electrical output of a turbogenerator is roughly proportional to the amount of field copper which may be safely imbedded around the periphery of the rotor. Figure 4 shows the general construction of the type of rotor considered. The teeth between the radial coil slots must be adequate to carry magnetic flux, withstand mechanical stresses, and also carry vent ducts.

To obtain the most efficient and safe design, the stress in the rotor must be accurately known at every point, and the maximum stresses at overspeed of 20 per cent must not exceed the elastic limit of the material.

The area having the most complex stress distribution occurs at the outer extremity of the teeth, at which point slot wedges transfer the centrifugal forces of the winding to the teeth. Further, the bore stress of the central exploration hole is usually of great importance. The following paragraphs discuss the manner in which the stresses were analyzed to obtain the most effective design.

1. Design of Rotor Teeth and Wedges

To determine accurately the centrifugal stress distribution in the rotor teeth and wedges due to the windings, the photoelastic method of analysis was applied. Several transparent Bakelite models were made of the proposed designs to a scale approximately twice full size for use in a large photoelastic polariscope.[5] With load applied under the wedge the respective stress distributions showed the effect of the changes in curvature of the slot wedge fillets. The photoelastic isochromatic pictures determine the difference of the principal stresses while the sums of the principal stresses were measured at points on the sections by means of a thickness-indicating device employing a Huggenberger Tensometer.[6] Configurations giving unpredictable stress concentration were discarded.

A sketch of the final wedge model, its general dimensions, and method of loading in the testing machine is shown in figure 5. A steel bar is clamped between the vent wedges to simulate the built-in condition of the rotor section.

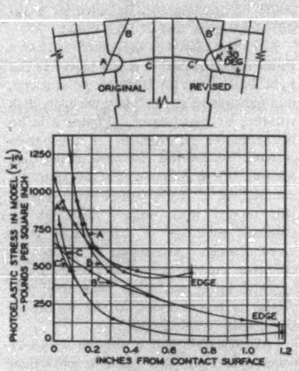


Figure 7. Curves of maximum shearing stresses for original and revised sections

The photoelastic stress distribution[7] for the final tooth and wedge element is shown in the isochromatic picture, figure 6. The stress concentrations along the contact surfaces are entirely removed and a uniform distribution of loading is obtained. The maximum shear stresses on these sections for the preliminary and final designs are shown in figure 7, the latter being indicated by prime letters. An immediate comparison of the effectiveness of the final design was observed by counting the number of bands or fringes proportional to the shearing stress. The maximum tensile stress at the fillet of the tooth, section *C*, was only slightly lowered by the increase of the fillet radius.

To further decrease these stresses the surface area in contact under pressure was increased by slightly reducing the slot width above the wedge, and the vent slot was made narrower to increase the minimum tooth section. The wedge stresses were not adjusted further since materials for that section are available with any desirable elastic limit.

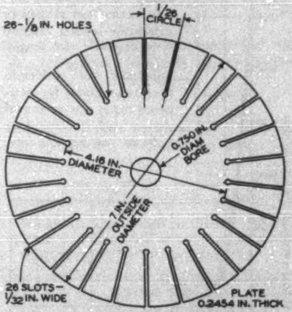


Figure 8. Sketch of photoelastic model of rotor section as rotating disk

Standard calculations for the revised design, considering compression, bending, and shear, checked very closely with the photoelastic stress analysis. For the minimum section (section *C*) of the tooth in tension, however, the photoelastic stresses were approximately 30 per cent higher than the calculated stresses; thus it would be assumed that the

Figure 6. Photoelastic picture—revised winding slot wedge shape

Figure 9. Photoelastic picture—rotating disk at 9,200 rpm

stress concentration at the fillet is 1.3.

The applications of these tests in the design of machines have resulted in structures which are completely and accurately analyzed, and for which the maximum stresses do not exceed the elastic limits of the forging materials. Further adjustments may be made in the development of larger machines based upon the data obtained in this analysis.

2. Rotor Bore Stresses

In determining the tangential stresses at the surface of the exploration hole bored in the center of turbogenerator rotor forgings, it is ordinarily assumed that the centrifugal loads on the teeth and the teeth themselves act as a uniformly distributed external loading on a thick cylinder equivalent to the net rotor section. On the stresses obtained in such a thick cylinder are superimposed the stresses due to rotation of the net rotor section as a rotating disk.

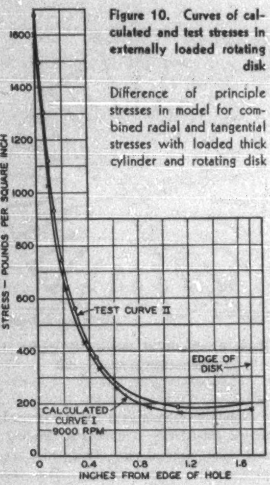


Figure 10. Curves of calculated and test stresses in externally loaded rotating disk

Difference of principle stresses in model for combined radial and tangential stresses with loaded thick cylinder and rotating disk

To verify the accuracy of the above assumption a model of a section of a 37-inch-diameter turbogenerator rotor body having $7^1/_8$-inch deep slots and a four-inch diameter hole at the bore was made of transparent Bakelite to a reduced scale for test at high speed.

After the disk was machined as shown in the sketch, figure 8, it was placed in the light field of the photoelastic polariscope, and pictures of stress taken with the disk rotating at high speed. Figure 9 is an isochromatic photograph of the stress distribution around the bore of the disk for rotation at a speed of 9,200 rpm. A comparison of stress values derived from this record with values obtained by standard methods[8] of calculations coincided closely. As shown in figure 10, the two curves are nearly coincident, and the linear variation from this condition is proportional to the square of the speeds $(9,200/9,000)^2$.

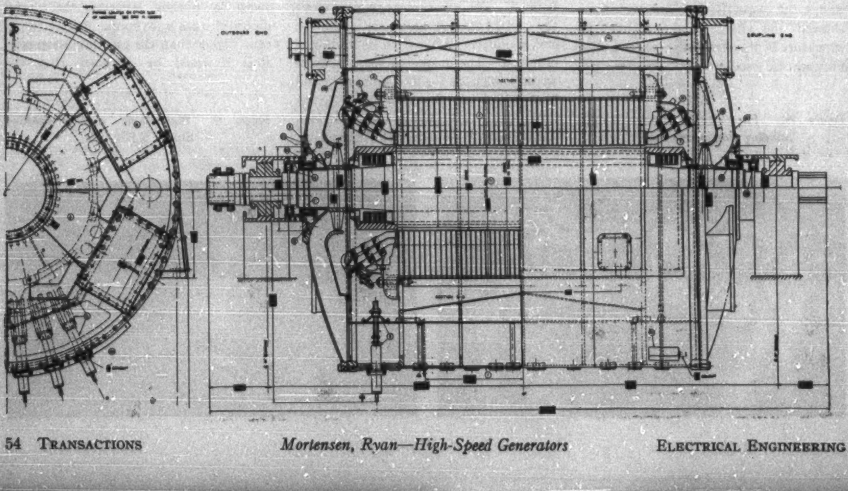
From the foregoing it may be definitely concluded that the present analytical theory applied to the calculation of stresses in such bodies as turbogenerator rotors gives a high degree of exactness.

C. Lubrication Characteristics of Bearings

In addition to the vibration and stress investigations carried out in connection with the development of large 3,600-rpm generators, consideration was given to the various factors determining efficiency. As no promising features appeared for reduction in electrical and magnetic losses, the mechanical features such as windage and bearing friction were investigated. Generator designs based upon hydrogen as the cooling medium were developed. A machine of this kind ordered from the company with whom the writers are associated is shown in figure 11. This machine has explosion-proof yoke and end covers, oil-type shaft seals, and is designed for operating hydrogen pressures up to ten pounds per square inch. It has been in successful commercial operation as a hydrogen-filled machine for several months.

Assuming that hydrogen cooling reduces the generator windage losses to a minimum, bearing losses remain as a possible source for loss reduction. Tests on 43,750-kw 3,600-rpm and smaller machines indicated the possibility of further loss reduction by means of bearing design and lubrication control. For this reason investigations were made of the usual factors of lubrication such as the diameter, length, clearance, and pressure for the journal bearings, and the volume, temperature, and viscosity characteristics of the lubricants. The bearing losses were determined from the heat transfer to the lubricating fluid during circulation through the bearing. The losses due to heat radiation and convection were found small enough to be

Figure 11. Hydrogen-cooled generator, 3,600 rpm, 60 cycles



neglected for comparative purposes.

The generators were driven by a direct-coupled test motor, and test conditions were varied by adjusting speed, oil flow, and oil cooler temperatures. Test data were obtained by readings taken at intervals. In most cases readings were not made until they remained constant for at least 15 minutes. The bearing kilowatt losses were calculated from the test data using density, specific heat values, and viscosities obtained from the oil distributors and checked in the laboratory.

The results of this series of tests may be summarized in the following statements:

It was observed that heat loss was proportional to the volume of oil supplied to the bearings. The curves in figure 12 compare the gallons of oil flow per minute against kilowatt losses for 12-inch by 12-inch bearings, and $13^1/_2$-inch by $13^1/_2$-inch bearings. The points *A* on the curves indicate the normal operating oil flows for the bearings. Points *C* are located to mark the calculated side leakage[9] quantities. The recommended oil flow is arbitrarily set at $2^1/_2$ times the side leakage, points *B*, for safe and efficient operation. Thus excessive quantities of oil forced to flow through the bearings produced high losses which would be considered unnecessary for normal operation.

It was also observed that the bearing losses were in proportion to the average oil temperatures. For all volumes of oil flow, with higher oil-supply temperatures minimum losses were obtained. The curves in figure 13 show a comparison of the losses measured for various quantities of oil flow at two inlet temperatures. Thus for the 12-inch by 12-inch bearing tests above, the kilowatt losses were reduced from 65 to 36 by adjustment of the oil temperature and the quantity of oil flow.

The physical dimensions and other data necessary for the theoretical calculations of power losses in the journal bearings are given in table I.

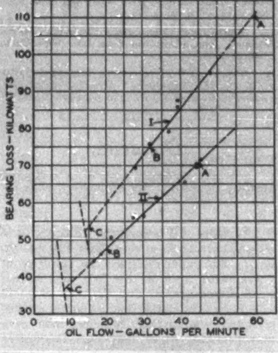


Figure 12. Test curves for bearing loss as a function of oil flow

For 3,600-rpm turbogenerators

Curve I—$13^1/_2$-inch by $13^1/_2$-inch bearing
Curve II—12-inch by 12-inch bearing

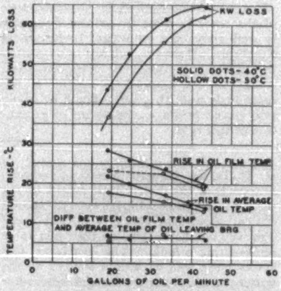


Figure 13. Test curves for bearing loss and temperature rise as a function of oil flow for two inlet temperatures

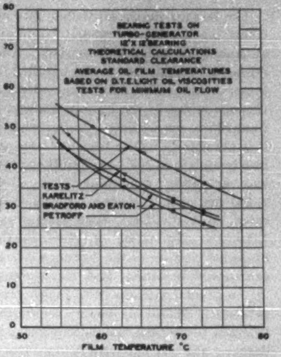


Figure 14. Theoretical calculations and test data for bearing losses with variable oil-film temperatures

The calculated kilowatt bearing losses for the 12-inch by 12-inch bearing, considering only the active portion in the lower half (except for the Petroff equation), are shown in figure 14, for methods by Karelitz,[9] Bradford and Eaton,[10] and Petroff[11], as a function of average oil-film temperature. Several test points for this bearing with low quantities of oil flow are plotted. From these data it would appear that the measured losses are approximately 25 per cent higher than those calculated by the Karelitz or Bradford and Eaton methods.

Table I. Bearing Dimensions and Data

Bearing (inches)	$13^1/_2$ by $13^1/_2$	12 by 12
Diameter of journal	13.478	11.980
Diameter of bearing	13.500	12.000
Nominal clearance	0.022	0.020
Nominal length	13.5	12
Effective length (inches)	11.75	10.5
Bearing load (pounds)	25,800	16,400
Bearing pressure (projected) (pounds per square inch)	162.5	130
Speed (revolutions per minute)	3,600	3,600
Bearing angle (deg)	90	90
Nominal oil required (gallons per minute)	60	40
Calculated end leakage (gallons per minute)	13.8	8
Adequate oil supply (gallons per minute)	31.5	20
Nominal bearing babbitt temperature (deg C)	60 (140 deg F)	60
Suggested bearing babbitt temperature (deg C)	77 (170 deg F)	77
Calculated minimum oil-film thickness, 77 deg C (inches)	0.003	.003

Recent tests by Tichvinsky[12] on a 7- by $10^1/_2$-inch bearing at 3,600 rpm would indicate losses identical to those measured above, for a 12-inch by 12-inch bearing at a film temperature of 60 degrees centigrade. However, at 75 degrees centigrade the measured losses were 13 per cent lower than those which would be predicted by Tichvinsky's tests.

As a result of these tests, the following conditions have been established for the efficient operation of large high-speed bearings:

The oil flow to the bearing should be restricted to a safe minimum. An adequate amount is $2^1/_2$ times the side leakage calculated for the bearing. The top half of the bearing is relieved to allow this flow with the least possible pressure resistance and the bearing is designed with additional side relief to maintain an increased oil reserve. The oil supply to the bearing is metered through an orifice from a high pressure oil line, and quantity rather than pressure is the control criterion.

The oil outlet temperature should be maintained between 63 degrees centigrade and 70 degrees centigrade corresponding to thermocouple temperatures in the babbitt between 70 degrees centigrade and 77 degrees centigrade. The higher oil temperature is obtained by reducing the water flow in the oil cooler.

The bearing design must take into account the increased temperatures in the allowance of clearance dimensions. Adequate safeguards are to be installed to preserve the oil supply, controlled by thermocouples in the babbitt connected to auxiliary service lines. In this manner it is possible to reduce bearing losses approximately one-half based on the prevalent operating conditions (and effect a 0.3 per cent increase in efficiency at rated load for large turbogenerators). Thus the elimination of losses in the unloaded portion of the bearings due to excessive lubrication, and the decrease in the general viscosity with higher temperatures obtain a closer correlation between theoretical calculations and test data.

Summary

The preceding paper indicates:

1. The practicability of analytically predetermined critical speeds of rotors with cross sections having different moments of inertia at right angles.
2. The value of the photoelastic method for stress determination in rotating bodies.
3. The possibility of increased machine efficiencies by control of bearing lubrication.

As a résumé of the tests and calculations described, it is believed that in the recent development of large machines, efficient and strong structures have been realized and the door opened for the design of larger and more efficient generators.

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12. Tests of a 7- by $10^1/_2$-Inch Bearing at 3,600 RPM, L. M. Tichvinsky. ASME *Transactions*, volume 60, number 5, July 1938, page 395 (figure 6).

Discussion

Sterling Beckwith: See discussion, page 34.

B. A. Rose (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper discusses the vibration of a generator rotor at the first and second natural frequencies, and for each of these frequencies the nodal points (points of zero vibratory motion) of the vibrations were very probably at or very near the center of the bearings or at certain spots in the pedestal structures themselves. We have found that a higher mode of vibration can exist such that the nodal points are located very close to the ends of the rotor body. The frequency at which resonance will occur is quite high, being of the order of 7,200 cycles per minute. This mode of vibration is not excited by the unbalance of the rotor because its natural frequency is well above the rotational frequency of 3,600 rpm. However, it is apt to be excited by the double-frequency vibratory force caused by the nonuniform rigidity of the rotor body and, therefore, should be taken into consideration in the design of generators of this type.

H. D. Taylor (General Electric Company, Schenectady, N. Y.): With regard to the three-to-one relationship between the second critical speed and the first critical speed observed by the authors on their test rotor representing a long turbine generator, this agrees quite well with data secured by the writer on a somewhat heavier test rotor weighing about 5,300 pounds. Calculated and test values of critical speeds for this rotor, when running with exceptionally rigid bearing supports, were as follows:

Critical Speed	Calculated	Test	
		Vertical	Horizontal
First	1,200	1,200	1,125
Second	4,000	3,900	3,250

It may be noted that the criticals in the vertical plane checked with calculations very closely, but that in the horizontal plane the first critical was reduced about 6 per cent, and the second about 17 per cent, due to slight horizontal flexibility.

We have also had experience with light rotors weighing up to 600 or 800 pounds, which showed little or no reduction of critical speeds below calculated values. It is common experience that for the heavy rotors of full-size turbine generators, the first critical is reduced considerably, as much as 20 or 30 per cent below the value calculated for rigid bearings, especially in the horizontal plane. Although supporting data are meager, the writer ventures the opinion that such machines also have a second critical speed reduced a great deal more from the calculated value, so that the ratio of the second critical to the first may actually be, instead of three to one, only two to 1, or even less.

However, there seems to be nothing particularly dangerous about the second critical speed. Tests with the 5,300-pound rotor demonstrated that the amount of vibration experienced at the first critical is a direct function of the amount of "static" unbalance in the rotor, and is not influenced by "dynamic" unbalance or "couples"; likewise, that the second critical responds only to couples, and is independent of static unbalance. A rotor having good static balance runs smoothly at the first critical; and with good dynamic balance, at the second critical also. In other words, good balance practically eliminates these critical speeds.

If a critical speed of 900 rpm is considered for a long generator field, the writer would anticipate the possibility of having a third critical speed within the running range, depressed from a higher calculated value by bearing flexibility. Also the possibilities for other types of vibration such as "whipping," would be greatly multiplied. However, I share the authors' view that lower critical speeds than 1,800 rpm will eventually be found practicable.

S. H. Mortensen and J. J. Ryan: It is assumed that the higher mode of vibration referred to by B. A. Rose is the third critical speed for turbogenerators. The deflection curve for this critical condition has nodes near the ends of the rotor body and at the bearing pedestals. A two-pole rotor with variable moment of inertia would probably set up double-frequency vibrations at one-half the third critical speed.

For a first critical speed of 1,600 rpm, a second of 5,000, and an assumed third of 10,000, the speed necessary to exhibit this type of vibration would be 5,000 rpm for a standard 3,600-rpm turbogenerator.

Such vibration phenomena has not been observed in the operation of these machines, but it is possible, under certain circumstances, this critical condition may be very close to the operating speed.

For the two-pole slotted rotor discussed in the paper it was observed that a double-frequency vibration occurred as the second critical speed was approached. The Geiger vibrograph showed approximately 0.0015 inches total motion at 6,600 vibrations per minute superimposed upon the single-revolution-per-minute frequency of 3,300 vibrations per minute for the maximum recorded motion of 0.003 inch. Since the double frequency disappeared at the second critical speed, it was not considered further.

However, the interesting discussion of H. D. Taylor stimulated the urge to further investigate the effect of the third critical speed, and the slotted rotor shaft was again operated on the test floor. As the second critical speed was approached the phenomena was repeated as above. Calculation of the third critical speed is thus a necessary factor in design to avoid double-frequency resonance of the nonsymmetrical rotor.

Turbine-Electric Textile-Range Drives

E. L. RICHARDSON
NONMEMBER AIEE

A TEXTILE RANGE—the definition is offered for the benefit of those who may be unfamiliar with textile parlance—consists of a group of coordinated machines through which a continuous band of cloth is passed in rope or flat form (figure 1). Carrying on, in a group, the maximum number of operations of a textile finishing process which manufacturing conditions will permit lowers the cost of production by reducing the time and labor costs, and improved methods of driving such ranges may lead to still further increases in the production rate per unit of equipment, with attendant reduction in unit cost of production.

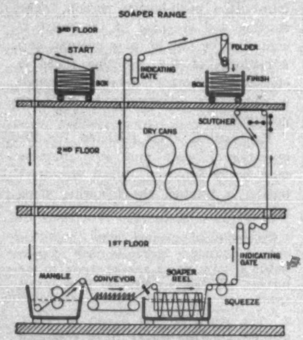

Figure 1. Group of related machines known as a soaper range

As an example of this, we have observed mills in which the cloth was subjected to two successive drying processes, perhaps with an intermediate sizing operation. The cloth was passed through a range and either batched on a roll or folded into a box, after which it was passed through a second range consisting of a starch mangle and dry cans.

Early textile mills were driven entirely by mechanical means, usually by a combination of hydro and steam-engine power. As a textile range almost always included a drying process, the use of steam engines for driving them became general.

It may be noted at this point that ranges are frequently built for processing a wide variety of textures and weights of fabric. Therefore, a range of operating speeds as great as three or four to one is required. In addition, a threading or adjusting speed of perhaps ten per cent of the maximum operating speed for short periods is sometimes desired.

Based on these considerations, engine drive had two important advantages, and some of these drives are still in use.

1. Exhaust steam from the engine was used in the drying process, allowing the driving power to be obtained as a by-product.
2. Throttle control of the engine provided a flexible and inexpensive means of obtaining the desired range of speeds.

It had also, certain notable disadvantages.

1. Losses and maintenance costs resulting from complicated mechanical means of interconnecting sections of the range.
2. Inaccurate speed relation of sections due to belt slippages and nonuniform stretch of different fabrics processed on the same range.

The development of electric equipment and its general substitution for mechanical drive in textile mills led to consideration of electric drive for ranges. Earlier drives of the single-motor type retained some of the disadvantages noted in connection with engine drives. Later drives have been of the multiple-motor type, with basic speed control applied to the motor driving the lead section and motors driving follower sections held in step with the lead motor by compensating gate control. Such drives have been of the variable-voltage d-c type or variable-speed a-c type and have overcome many of the disadvantages common to single-motor and engine drives. It has also been possible to increase the number of sections comprising a range and to locate these sections on as many as three floors of a building when manufacturing reasons dictated the wisdom of such procedure.

Multiple-motor drives have provided an adequate solution of range problems in finishing plants where the efficient use of steam was already effected by the generation of power by turbine generators in a power station, with a low-pressure steam main carried into the mill to meet the demands for process steam.

For plants in which power is not being generated, even though there is a demand for low-pressure steam, the turbine-electric range drive offers interesting possibilities. This drive, in its usual form, consists of a gear-turbine flexibly coupled to a slow-speed a-c generator which has a shaft extension providing some form of mechanical drive for a section of the range then designated as the lead section. The generator is electrically connected to a-c motors, ordinarily of the induction type, which individually or in closely related groups drive other sections of the range (figure 2). Although a new arrangement, the components of this drive are standard pieces of equipment in simple form.

Basic speed adjustment is accomplished by varying the speed of the turbine either by hand or by remote throttle control or by the more expensive means of variable-speed governor control. The turbine satisfies the need for flexibility of speed control, and at the same time its use results in a low operating cost by providing the power needed for driving the range as a by-product from steam required to meet process demands. The fuel cost to supply the heat converted to

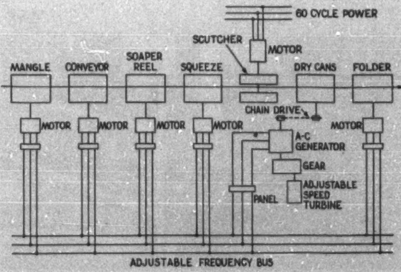

Figure 2. Schematic diagram of turbine-electric drive for a soaper range

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E. L. Richardson is with the General Electric Company, Schenectady, N. Y.

Figure 3. Motor drive to folder. Part of turbine-electric textile range drive in New England mill

Figure 4. Steam-pressure-regulating and control valve of turbine-electric range drive in New England mill

power in the turbine is usually the equivalent of an electric-power cost of approximately one mill per kilowatt-hour.

With this form of drive, a section can be selected as the lead section on the basis of consideration of space and the convenient location of the prime mover. As this section is driven mechanically, its speed varies in direct proportion to that of the turbine.

The a-c generator, being driven by the turbine, generates a frequency directly proportional to the speed of the turbine and of the lead section. Motors driving other sections and having this generator as their power source are then driven at speeds having a uniform relation to the frequency of their power source except for the effect of motor slip due to load changes.

This effect of motor slip is advantageous because it allows motors to be initially placed in a fixed speed relation to the sections which they drive and yet provide ample flexibility, through a slight amount of belt action between sections, to accomodate the variable stretch factor of different grades of cloth. Special consideration is given to the slip factor and torque characteristics of motors for this application, the amount depending on local conditions to be met.

About a year ago a large New England mill installed a drive of this type which involved many interesting problems related largely to control.

In this case, the dry cans were mechanically driven. As it was desired to warm up the dry cans in motion and to wash them on shut down, with the balance of the range stationary, simple switches were provided to disconnect the follower motors from the generator. This required a range of operating speeds from full speed to one-third speed, but did not demand low speed for threading or adjustment, a fact which somewhat simplified the control problem.

The operator is concerned only with a four-button push-button station. The starting and stopping of the range is controlled by two of these buttons, and adjustment of the operating speed is effected by the other two buttons through a pilot-motor adjustment of valve or governor.

The control cycle initiated by the operation of "start" and "stop" buttons may be of interest. When the start button is pressed, the following steps take place:

1. A solenoid-operated pilot valve is opened, causing a hydraulic relay to open slowly a valve which admits steam to the turbine, allowing acceleration to a preset speed.

2. The generator field circuit is closed, allowing electrical energy to be generated as soon as rotation starts.

3. The generator field rheostat is short-circuited, thereby applying full field to the generator in order to allow the follower motors to develop maximum torque at very low frequency.

Figure 5. Turbine generator set, showing mechanical drive to dry cans and centrifugal field-control switch

Figure 6. General view of turbine generator set with chain drive to dry cans

Figure 7. Motor-operated control valve for turbine-electric range drive in New England mill

Figure 8. Motor-driven cloth conveyor

4. A timing relay is started which, after a generator frequency sufficient to supply adequate motor torque is reached, breaks the short circuit across the generator field rheostat and thereby reduces the field current to a normal value.

When the stop button is pressed, the following operations occur:

1. The solenoid-operated pilot valve is closed, causing the hydraulic relay to close a valve interrupting the steam supply to the turbine.

2. A magnetic switch is closed. This connects a three-phase resistor across the generator terminals, supplying braking effect for rapid deceleration.

The generator field circuit remains closed until the generator comes to rest, when the field circuit is opened by a centrifugal relay driven by the generator.

Each follower-motor circuit contains a thermal overload relay. The contacts of all these relays are connected in series with the stop button circuit, so that excessive load on any motor will shut down the entire range.

It should be understood that when this type of drive is started, the mechanically driven section starts with the turbine, at which time the voltage and frequency at the generator terminals is at zero.

Carefully conducted tests indicate that follower motors start when the generator frequency reaches a value somewhere between two and three cycles and that there is a time lag of about $1^1/_2$ seconds between the starting of the turbine and the starting of the follower motors.

For this reason it has been found desirable to provide floating rolls, capable of accommodating about three feet of cloth, between the mechanically driven

Figure 9. Motor-driven soaper reel. Part of turbine-electric range drive in New England mill

Figure 10. Main control panel for generator and braking resistor

Figure 11. Motor-driven soaper reel

A High-Gain D-C Amplifier for Bio-electric Recording

HAROLD GOLDBERG
ASSOCIATE AIEE

Synopsis: This paper describes a d-c amplifier for use with the cathode-ray oscillograph whose maximum voltage amplification is 6×10^6. This amplification is attained with a noise level referred to the input of less than two microvolts over a 5,000-cycle-per-second band, and without the drift due to variations in battery voltages and circuit constants characteristic of such amplifiers. The amplifier is push-pull throughout and provides a choice of two input circuits. Voltage sources in which both terminals are at a high impedance to ground are connected grid-to-grid, symmetrically to ground, while sources in which one terminal is at ground potential are connected to either grid and ground, the first stage, in this case, acting as a phase inverter. Stability and phase inversion are obtained by means of a circuit that makes the output relatively independent of battery voltage and tube-constant variation. The amplifier is simple and rugged, uses ordinary radio-type tubes which do not have to be carefully matched, is constructed of ordinary radio-type components, and has reasonable battery requirements.

THE PROBLEM of recording the potential-time variations produced by bio-electric phenomena is a difficult one. The maximum potential differences occurring in various biologic preparations range from 10^{-6} to 10^{-1} volts. The potential-time variations themselves range from unidirectional and very low-frequency recurrent types (figure 5) to phenomena whose period is less than a millisecond. The string galvanometer has been used for such work in the past and still has wide acceptance. Its ability to record d-c phenomena and its high sensitivity under proper conditions, 10^{-4} volts per millimeter, make it almost ideal for this work. Recently, sensitivities of the order of 10^{-6} volts per millimeter have been obtained by using low-gain low-noise-level amplifiers with the string galvanometer. Opposed to the advantages of this instrument, however, are its poor frequency response (0–250 cycles per second), its variation in sensitivity with temperature, the fragility of the quartz string in the galvanometer, and its low input impedance when used without an amplifier.

The cathode-ray tube, on the other hand, has none of these disadvantages and with present day tubes and high-speed films, the recording of nonrecurrent biologic phenomena is not difficult even with moderate anode voltages. The cathode-ray tube is inferior to the string galvanometer only in its sensitivity, three volts per millimeter, for a representative three-inch tube with 1,000 volts on its anode. While the string galvanometer, whose sensitivity is 30,000 times as great, can record bioelectric potential differences directly, the cathode-ray tube must be associated with an amplifier to accomplish the same result. In order to obtain a sensitivity of 10^{-6} volts per millimeter, to make use of the high-frequency response of the cathode-ray tube, to be able to reproduce faithfully d-c phenomena, and to have sufficiently low noise level to justify the high sensitivity, one must associate with the cathode-ray tube a d-c amplifier responding to a band width of 0–10,000 cycles per second which has a maximum voltage amplification of 3×10^6, and a noise level, referred to the input, of less than two microvolts over a 5,000-cycle-per-second band.

Such performance requirements, while desirable in an instrument for laboratory research, need not be as severe in special applications such as human electrocardiography as an example. The human heart beats from 70 to 80 times per minute in a normal person, and an amplifier which responds down to one cycle per second will faithfully reproduce the action potential produced by the heart once during each beat. One can construct resistance-capacitance coupled amplifiers that will do this without great difficulty, and such amplifiers are generally used for this work. One could not, however, practically construct such an amplifier to reproduce faithfully phenomena such as presented in figure 5. Such an amplifier would have to have a time constant in the neighborhood of 150 seconds and the coupling capacitors required would be enormous. Since even lower-frequency phenomena occur, the d-c amplifier is seen to be the type that must be used.

If the cathode-ray tube is to be used for this work, therefore, an amplifier must be constructed fulfilling the requirements already stated. The most difficult problem to be solved in the design of such an amplifier is the one of compensating for the "drift" in the operating voltages of the output stage due to small variations in operating voltages and tube constants in the input stage. This problem becomes an increasingly difficult one to solve as the voltage amplification is increased, since the maximum uncompensated residue of the variations in the input stage that can be tolerated becomes smaller and smaller. The amplifier described in this paper solves this difficulty and attains a maximum voltage amplification of 6×10^6, sufficient for a sensitivity of 5×10^{-7} volts per millimeter with a three-inch cathode-ray tube operated with 1,000 volts anode potential. The amplifier is compact, easy to operate and adjust, and inexpensive to construct. Only moderate battery voltages are required and the current drain is very low. It has been found that low noise level and high stability may be attained even with the use of ordinary radio-receiver-type circuit components.

Design and Theory

NOISE LEVEL

The 38-type tube, a power output pentode, when operated at very low plate and screen voltages, is a practical amplifier tube possessing very low inherent noise. The noise voltage output of a single amplifier stage using this tube operated as stated, is equivalent to a noise voltage of less than two microvolts impressed on the grid of the stage, pro-

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section and the motor-driven sections.

Experience has shown that, after essential adjustments have been made, this type of drive can be easily and successfully operated by the ordinary finishing-plant operator.

The use of a-c motors simplifies wiring installation and reduces the maintenance expense to a minimum.

Smooth acceleration and deceleration of the turbine-electric drive combined with accurate speed control reduces damage to the fabrics in process, resulting in maximum production.

vided the stage passes no frequencies higher than 5,000 cycles per second. The high emission of the power-type tube makes this tube practicable as a voltage amplifier even at plate voltages and screen voltages of 12 volts and lower. Four such tubes, two per stage, are used in the first two stages.

AMPLIFICATION

The remainder of the desired voltage amplification is obtained with two additional stages. Four type *6J7G* tubes are used, two to a stage. These are radio-type pentodes of the sharp-cutoff variety. They provide a voltage amplification of about 130 per stage. A plate-supply voltage of 225 volts on the output stage provides for an undistorted voltage output sufficient to cover the cathode-ray screen.

STABILIZATION

There are two methods, in general, for overcoming the "drift" in operating voltages due to slow variations in battery voltages and tube constants. One method is to provide an automatic means for compensating for such drift; the other, is to make the circuit operating voltages relatively independent of such changes without at the same time destroying the amplifying properties of the circuit. Several circuits have been proposed in the literature for use with the "electrometer" tube in single-stage amplifier circuits. These operate according to the first method. The employment of such means in a multistage amplifier would complicate the operation and adjustment to the extreme.

The second method of attack is more practical for a multistage amplifier and has several advantages. A circuit whose operating voltages are relatively independent of battery voltage and tube-constant variations will be relatively simple to adjust and maintain. The replacement of tubes and aging of batteries will have little effect on amplifier adjustment and performance. Furthermore, there is no necessity for a knowledge of the exact characteristics of each tube to be used in the amplifier as is often necessary in compensation schemes.

The use of push-pull circuits is immediately suggested as a possible means of rendering the steady-state output voltage of the amplifier independent of tube and battery variations. With perfectly matched tubes, any variations in the output due to variations in supply voltages will cancel. Furthermore, the tubes may easily be self-biased without affecting the amplifying properties of the circuit. Such a circuit, however, would not suffice to keep the operating voltages of the output stage at optimum values should excessive changes develop in the batteries supplying the first stages or should matched pairs of tubes be replaced by other matched pairs of somewhat different characteristics. In addition, some provision for phase-inversion would be necessary for use with voltages in which one terminal is grounded unless one would be willing to use only one side of the amplifier for signal voltages.

Even if one decides, however, that the last mentioned difficulties are not too important, there remains the fact that in a practical sense, commercial-type tubes cannot be perfectly matched. A push-pull circuit using unmatched tubes will not completely cancel variations in output voltages due to drift but will only partially cancel them, and for that reason the push-pull circuit must be used with additional modifications to take care of the uncancelled residue.

The fact that unmatched tubes must be used brings about still another difficulty. It is desirable to maintain balanced push-pull voltages in all stages ($E_1 = -E_2$ in the arrow directions, figure 2). It will be generally impossible to do this in a simple push-pull circuit that uses unmatched tubes without some circuit modifications. While this balanced condition is not too important in a low-gain amplifier, it becomes increasingly important as the voltage amplification is increased.

The circuit finally used in the amplifier is a modified push-pull circuit in which phase inversion is an inherent property, balanced push-pull voltages are automatically maintained despite tube differences and circuit unbalances, optimum operating voltages are maintained in all stages even under large variations of

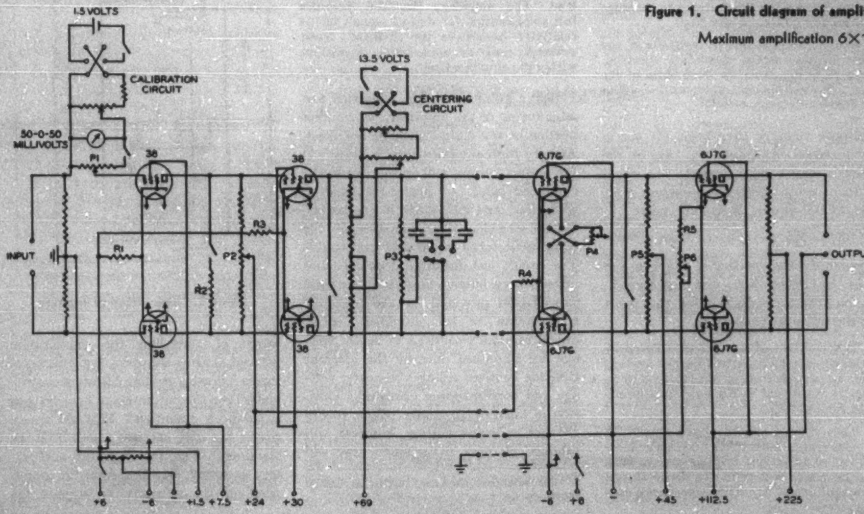

Figure 1. Circuit diagram of amplifier

Maximum amplification 6×10^6

supply voltages and despite tube replacements, and the "drift" due to the previously mentioned uncancelled residue may be made as small as desired. The basic circuit that accomplishes these results will now be discussed.

The Basic Circuit of the Amplifier (Figure 2)

The circuit characteristics set forth in the following discussion depend on calculations which assume operation over the linear portion of the tube characteristics. Such calculations are valid since the amplifier is actually operated over the essentially linear portion of the tube characteristics. The discussion is valid not only for pentodes, the tubes used, but holds equally well for tetrodes and triodes. The calculations upon which the discussion is based are more complex in the case of tetrodes and pentodes than they are in the case of triodes.

The following symbols will be used:

R_0 is the plate load resistor and has the same value on each side of the amplifier stage.

R_p is the dynamic plate resistance of the tube. The subscripts (1, 2) refer to tubes *VT*1 and *VT*2 and indicate that the tube constants may differ for different tubes.

R_{ps} is the static plate resistance of the tube at a given operating point and is the ratio of static plate voltage to static plate current.

μ is the amplification factor of the tube at the operating voltages used. The subscripts have the meaning already given.

R is the common cathode resistor.

E_1 and E_2 are the input voltages to the stage in the arrow directions specified.

E_3 and E_4 are the output voltages of the stage in the arrow directions specified. Since R_0 is the same for both sides of the stage, $E_3=-E_4$ is equivalent to $I_1=-I_2$ in the arrow directions.

Supply voltages have been left out in the interests of simplicity. All of the above voltages and currents are signal components of voltage and current only.

If, in this circuit, R has the value

$$R \geqq 10(R_0+R_p)/\mu$$

in which μ is the smallest of μ_1 and μ_2 and R_p is the largest of R_{p1} and R_{p2}, a condition on R that is easily fulfilled, the circuit has the following properties:

1. If the circuit of figure 2 is replaced by the usual equivalent circuit for the vacuum tube, the cathode resistor R is replaced by an effective resistance which has the approximate value of $2R\mu$.

2. The property stated above makes the plate current of both tubes practically independent of variations in plate-supply voltage, the degree of dependence depending on the magnitude of R. This dependence may be made arbitrarily small, within practical limits, by making R arbitrarily large.

3. The property of "1" also renders the plate current of both tubes independent of variations in the dynamic plate resistance of the tubes. This property renders the operating voltages of the circuit independent of tube aging and tube replacement.

4. A single-ended input voltage E_1, $E_2=0$, applied to one side of the amplifier stage will produce essentially balanced, push-pull output voltages. The total output voltage, E_3-E_4, will be the same as that produced by an input voltage, $(E_1'-E_2')$, applied from grid-to-grid, where $(E_1'-E_2')$ is equal to the voltage E_1 applied from one grid to ground.

This feature is the phase-inverting property of the circuit and has already been discussed for triodes by Goldberg.[2] This phase-inverting property of the circuit enables the amplifier to produce the same output voltage for a given input voltage irrespective of the manner in which the input voltage is applied to the input circuit of the amplifier.

5. Essentially balanced, push-pull output voltages are produced by the stage regardless of unbalances in input voltages, or differences in the characteristics of the component tubes. (A calculation made for a typical circuit to which balanced, push-pull input voltages are applied but in which the plate resistances of the component tubes differ by 10 per cent shows that the output voltages differ by only 0.1 per cent when $R=7(R_0+R_p)/\mu$, a value of R less than that recommended.)

6. The final and one of the most important properties of the circuit is that amplification of balanced push-pull voltages is not affected by the introduction of the cathode resistor, R, since the plate and screen currents due to such voltages cancel in the common cathode lead. The amplifier, therefore, provides full amplification for desired signals but is relatively insensitive to "in-phase" input voltages and to undesirable variations within the amplifier itself.

Before going on to the operation and adjustment of the amplifier, yet another portion of the design must be discussed. At very high amplifications, a "drift" in the output voltage will appear due to the combined effect of heater-voltage-drop with time, and differences in the heater-voltage-emission characteristics of the component tubes in the first two stages. The circuit just discussed does not minimize such variations except for the partial cancellation provided by the push-pull construction of the amplifier. Small, quick, random variations in heater voltage are smoothed over by the inherent time lag between changes in heater voltage and corresponding changes in emission in heater-type tubes. Slow, steady changes in heater voltage, however, will produce changes in emission and will cause drift if the emission changes are different in the tube components of each of the first two stages of the amplifier. This effect may be reduced to a negligible quantity by providing a portion of the grid and screen voltages of the first two stages from the heater-voltage supply in such a way that any change in the heater voltage, will change the emission and the grid and screen voltages in such an antagonistic manner so as to keep the steady-state plate current constant. It has been found that an adjustment of the circuit that will do this for any one type 38 tube in the first two stages, will do it sufficiently well for any other type 38 tube that may be used to replace the original so as to keep the drift due to this cause within a tolerable minimum at maximum amplification. The paper will now discuss the operation and adjustment of the amplifier.

Operation and Adjustment

It is desirable from the standpoint of the proper operation of the amplifier to provide a means of adjusting the static operating voltages in each stage differentially, so that the static plate-to-plate voltage of each stage may be adjusted to zero or very nearly to zero. Such an adjustment provides for the maximum of stability in the amplifier, provides a means of controlling the amplification without shifting the operating voltages of stages following the amplification control, and allows the output stage to operate in a balanced condition. This last condition is not only necessary both from the standpoint of efficient operation and battery economy, but also from the standpoint of the necessity of keeping the

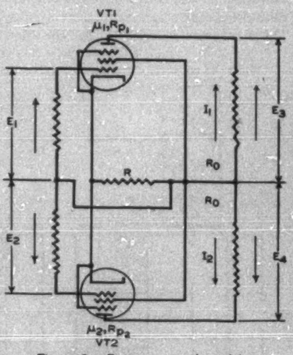

Figure 2. Basic circuit of amplifier

cathode-ray-tube spot near the center of the screen with zero input voltage to the amplifier. Since it is sometimes desirable to shift the zero input voltage position of the spot to various other positions on the screen, the static plate-to-plate voltage balancing control of some one stage may be used to unbalance the static output voltage of the amplifier sufficiently to move the spot to the desired position.

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[2] For numbered reference, see list at end of paper.

In addition to the balancing controls, and a means of varying the voltage amplification, there should be some method for controlling the width of the band of frequencies transmitted by the amplifier, at least at the higher amplifications, so that the inherent amplifier noise may be reduced to considerably less than a microvolt in those cases where the input voltage is very small and the reduction in band width is not objectionable. Finally, there should be some means for quickly and simply checking the condition of the static plate-to-plate voltage balance of each stage.

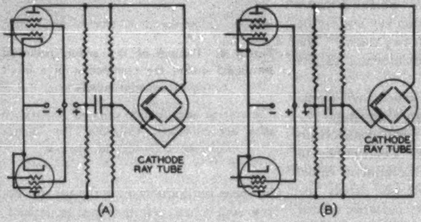

Figure 3. Suggested schemes for connecting the amplifier to ordinary cathode-ray tubes with internal connections between two plates and the anode

The scheme for checking such balances is as follows: Plate-to-plate short-circuiting switches are provided in the second and third stages. The closing of such a switch automatically balances the static plate-to-plate voltages applied to the grids of the following stage but does not affect the mean operating voltages of that stage unless the preceding stages are seriously unbalanced, a condition that does not exist in this amplifier unless some portion of the amplifier is defective. These balance indicating controls are provided for the second and third stages and not for the output stage since the static plate-to-plate voltage of the output stage is never more than a few volts (depending on the component tubes) and is not objectionable. The third stage is balanced by closing the second-stage plate-to-plate short-circuiting switch and adjusting the balance control in the third-stage plate circuit until alternate opening and closing of the third-stage plate-to-plate short-circuiting switch does not produce motion of the spot on the cathode-ray-tube screen. This condition occurs only when the static plate-to-plate voltage of the third stage is zero, the condition of balance. Adjustment of the second stage is accomplished in a similar manner. The first stage balance is set with the aid of a voltmeter whenever tubes are replaced in that stage, and does not need readjustment as long as the same tubes are used. In normal use of the amplifier, the second stage is not adjusted to balance, the balancing circuit in this case acting as a centering device, coarse and fine controls being provided for ease in adjustment.

Circuit Description

The input to the amplifier, as illustrated in figure 1, is high-impedance grid-to-grid, the grid-leak resistors providing push-pull input to the first stage. An input that is unbalanced to ground may be connected from either grid to ground, the remaining grid being grounded in this case. As has been stated, the output voltage from the amplifier for a given input voltage is the same whether it be applied from grid to grid or from one grid to ground. For this reason, calibration voltages may be supplied by the injection of known direct voltages into the grid circuit of one tube of the input stage. These voltages may be varied from 2×10^{-6} to 5×10^{-3} volts. The 100,000-ohm potentiometer, P_2, is used to balance the static plate-to-plate voltage of the first stage. Since this control can adjust for a static plate-to-plate voltage not greater than one volt, the tubes in this stage must be matched to the extent that the *IR* drops in their plate load resistors differ by not more than one volt. Tubes which will match in an ordinary tube checker will meet this requirement in about 50 per cent of the cases, although the operating voltages in the amplifier differ considerably from the rated operating voltages of the tubes as provided by the tube checker. The voltage amplification of the first stage may be changed from 15 to 1.25 by shunting a 75,000-ohm resistor, R_1, figure 1, from plate to plate. This reduction makes the over-all gain of the amplifier about 500,000 maximum, as compared to 6,000,000 with the shunt removed. This lower range is desirable when the maximum available voltage amplification is not needed since the lower amplification of the first stage will minimize unbalances in the second stage due to d-c polarization voltages which may occur at the electrode contacts with the biologic specimen being studied.

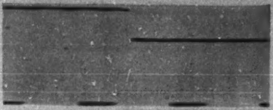

Figure 4. Response of amplifier to 100-microvolt calibration voltage applied by manually depressing calibrator key

The dashes are a portion of the timing record and represent 0.04-second intervals

Tube matching on a tube checker is sufficient for tubes in the second stage. The balance control, which is also the centering control, is also used to counteract any effect of steady input voltages brought about by electrode polarization, etc. This control circuit injects a continuously variable push-pull voltage into the second-stage plate circuit which may be reversed by means of the reversing switch. The one-megohm variable resistor, P_3, shunted between the plates of the second stage provides a continuously variable means of controlling the magnitude of the amplification. Reduction in band width is obtained by means of three different shunting capacitors across the control and are effective only when the gain control is near maximum.

There are two means for bringing about static balance in the third stage and these are of sufficient range so that unmatched tubes, taken at random from the dealer's shelves, may be used for this stage. A coarse means is provided by the six-ohm variable resistance, P_4, and a reversing switch, which provides a variable resistance in the heater circuit of either tube at will. This control cannot be used for fine adjustments because of the time lag in the heater-voltage-cathode-emission characteristic. It is used only when discharging batteries have brought about a marked drop in heater voltage or when tubes are replaced in the stage. Fine adjustment is accomplished with the 10,000-ohm potentiometer, P_5, in the plate circuit of the third stage.

Tubes matched in a tube checker are sufficiently alike in their characteristics so that they will work satisfactorily in the output stage. Balancing is unnecessary since the static output voltage is never more than a few volts with such tubes, and has a negligible effect on the position of the cathode-ray-tube spot.

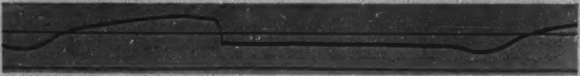

Figure 5. Record of one cycle of the injury action potential from the surface of a tortoise heart

The dashes represent 0.04-second intervals. The total elapsed time for each cycle is 4.3 seconds. This record illustrates one type of low-frequency phenomena handled by the amplifier

Grid bias for the output stage is partially variable by means of the variable resistor, P_6, so that it may be adjusted to the optimum value under conditions of actual operation.

The output connections of the amplifier as shown in figure 1, are intended for a cathode-ray tube which has separate connections to all deflecting plates. The mid-terminal is connected to the anode, and the two outer connections to a pair of plates of the cathode-ray tube. Figure 3*A* and *B*, illustrate possible connection schemes which may be used with cathode-ray tubes possessing internal connections between two deflecting plates and the anode. The connection, *A*, makes use of only half of the output voltage of the amplifier, and for this reason, the plate-supply voltage of the output stage must be increased over that indicated in figure 1, so that half of the undistorted voltage output will be sufficient to give full deflection. The scheme, *B*, applies push-pull voltages to both sets of deflecting plates and results in a deflection proportional to 0.707 of the output voltage of the amplifier. The direction of deflection in this case is along a line making an angle of 45 degrees with the axis of either set of plates.

Constructional Details

The amplifier is built in two units to minimize the effects of stray coupling. The first unit, which is well shielded to prevent pickup, contains the first two stages of the amplifier and all of its associated batteries with the exception of the six-volt storage battery supplying the heater voltage. Small 22.5-volt *C* batteries are used to supply plate and screen voltages with a total drain of less than a milliampere. Tubes are packed in absorbent cotton, shielded, and mounted in suspension mounts by means of rubber bands for the purpose of minimizing microphonic disturbances. Shielded cables connect to the calibration key and to the unit containing the last two stages. The maximum voltage amplification of the first two stages is 350.

The last two stages, which have a voltage amplification of approximately 17,000, are housed in a small commercial-type amplifier chassis. Connections to batteries are by a multiconductor cable. Light-duty 45-volt batteries supply the 225 volts of plate-supply batteries needed for the last two stages. Circuit arrangements are such that the actual supply voltages to stages one to four are 22.5, 57, 67.5, and 225, a total of 372 volts, although the total plate battery supply is only 294 volts. Proper bias voltages are obtained for each stage, despite the large cathode resistors employed, by proper circuit design.

Performance

In actual operation, a warming-up period of about 15 minutes is required to secure stability at the higher amplifications. After the amplifier has reached steady-state conditions, minor adjustments to the static balance may be necessary but can be made in no more than a minute or two if necessary. During use, after initial adjustments have been made, only the amplification, centering, and calibration controls need be used.

The amplifier described has been in constant use for a year and a half and has given extremely satisfactory results. In all this time, there has been no necessity for battery or tube changes and the aging of the batteries used with the amplifier has in no way impaired its performance. The amplifier has proved itself to be very dependable and has justified its design in every way. That the performance of the amplifier is not fortuitous is demonstrated by the fact that another such amplifier has been built and been in use for the last year and it has performed just as dependably as the original.

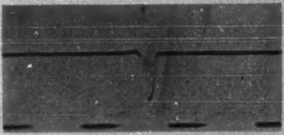

Figure 6. Record of the action potential produced during the contraction of a frog's gastrocnemius muscle

The voltage amplification used for this record was approximately 450,000. The dashes represent 0.04-second intervals

This paper, therefore, has described a d-c amplifier whose over-all voltage amplification is comparable to those obtained in resistance-capacitance-coupled amplifiers and is limited only by the unavoidable noise level of the tubes in the first stage.

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Capacitor Relay Timing in Industrial Control

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Synopsis: The trend toward electrical means of providing timing for motor control is evidenced in the use of capacitor-timed relays and contactors. Though employed for some time in welding applications, the advent of capacitor timing in the motor control field is fairly recent. The general analytical considerations are presented, followed by a discussion of the component parts of the timer. Some attention is paid to limitations, particularly in regard to the effect of line voltage fluctuations.

THE provision of simple, reliable, and inexpensive timing means has been a perennial problem with industrial-control engineers. Many different physical phenomena involving time have been made to serve as the basis of timing systems, with varying success. During this process of development, the accepted method of motor acceleration has passed through an earlier stage in which current- or voltage-responsive methods were in favor, to that of more recent practice which consists of carrying out the various steps of armature and field acceleration in accordance with a definite time sequence. Thus, the development of timing methods has been accelerated by their increasing importance in the field of motor control.

Moreover, the recent rapid development of the resistance-welding field has presented to control engineers timing problems of a very exacting nature, and has resulted in refinement of existing methods and the introduction of new ones. These methods are now being carried over into the older domains of industrial control with beneficial results.

The earlier forms of timing employed in motor acceleration were mostly of mechanical type, such as dashpot and clockwork mechanisms. More recently, however, the trend has been toward what may be termed electrical means, based on the storage of energy in self-inductance elements or in capacitors. The former type has been in successful use for a number of years (in the so-called "inductive time limit" or "magnetic time" controllers).[1]

On the other hand, though long viewed as a hopeful possibility, capacitor timers have presented many difficult problems only recently overcome in such a manner as to make their use entirely practical. Recent improvements in electrolytic capacitors, particularly those of the etched-plate and fabricated-plate types, have aided in solving such problems, and at least one manufacturer of industrial control is already employing capacitor timing quite extensively both in resistance welding[2] and in motor control. It is the purpose of this paper to outline the general principles of this type of control, and to describe some typical applications.

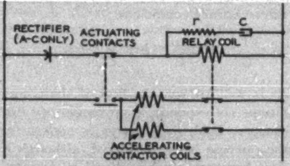

Figure 1. A circuit for timing two accelerating contactors employing a capacitor timing relay having two independent armatures

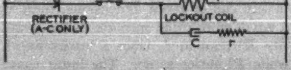

Figure 2. Capacitor timing circuit suitable for lockout-type accelerating contactor

Typical Capacitor-Timing Circuit

The earliest capacitor timers in general use were those employing paper capacitors of about one-microfarad capacity in conjunction with rheostat having resistance on the order of one megohm. This timing combination was connected in the grid circuit of a vacuum or gas triode which acted as an amplifier and controlled power switching apparatus of either magnetic or electronic type. Such "capacitor-resistor" timers have been widely used in resistance-welding control and elsewhere.

However, for motor-acceleration timing and many other purposes this arrangement is too complicated to be practical, particularly in the typical case where two or more successive timing periods are involved. The necessity for periodic replacement of tubes has been an additional barrier.

In an attempt to overcome these disadvantages, it has been found possible to simplify the capacitor-resistor idea by the employment of capacitors of much larger capacity (on the order of 25 microfarads) making possible the elimination of the amplifying tube. Also, to meet the need for multiple timing, relays have been developed which provide two or more independently adjustable time periods with a single magnetic structure.

The circuit used for this purpose is extremely simple (figure 1). The timing-relay coil is connected in parallel with a capacitor and has two or more independent armatures which are connected in circuit with coils of accelerating contactors. When the actuating contacts are in the position shown, the capacitor is charged and the relay coil is energized. When the actuating contacts are moved into the other position, the coil and capacitor are cut off from the line, and the relay armatures drop in turn after definite time intervals.

Variations of this simple scheme are obviously possible, such as connection of the relay coil in series with the capacitor in a charging circuit. However, the ap-

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[1] For all numbered references, see list at end of paper.

parently inevitable, though small, leakage current in electrolytic capacitors prevents ultimate reduction of current to zero in a charging circuit and makes the shunt discharge arrangement shown the more desirable.

It is also possible to use the delayed-release magnet as a lockout or restraining magnet in relays or contactors having a separate closing coil (figure 2).

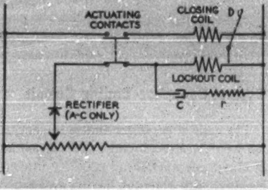

Figure 3. Same as figure 2 except for adjustment of timing by varying initial ampere-turns

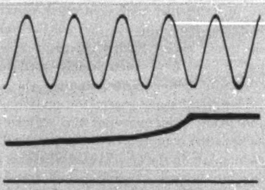

Figure 4. Variation of coil current at start of timing period

Upper trace is 60-cycle timing wave. Ohms in resistor r equal twice ohms in coil

This arrangement has the distinct advantage that the delayed-release coil can be designed for most efficient delay exclusively, whereas in the scheme of figure 1 this magnet has also to perform the function of pulling in the relay. Both figures 1 and 2 are being used, but the former more extensively because it permits using a single standardized timing relay with a wide variety of contactors of standard type, both direct current and alternating current. When used on alternating current it is merely necessary to add the half-wave rectifier unit indicated on the figures.

Nature of Timing Transients

The capacitor-relay timer differs from the electronic timers in that the discharge path contains self-inductance as well as resistance. It is shown further on in this paper that the former may be neglected in most practical cases, but for purposes of analysis it is necessary to take self-inductance into account initially. This involves the well-known transient relationships in a series L-C-R circuit[3] which are repeated here only insofar as they clarify the points under discussion.

Let

L = self-inductance of coil, henries
R = resistance of coil (plus external resistor if used), ohms
C = capacity, farads
i = transient current, amperes
i_0 = initial transient current, amperes
ϵ = 2.718
t = time, seconds
A_1 and A_2 = integration constants
$B=\sqrt{R^2-4L/C}$
E = line volts = initial volts on charged capacitor
n = turns in coil
m = ampere-turns at which magnet releases armature

The fundamental equation is

$$L\frac{d^2i}{dt^2}+R\frac{di}{dt}+\frac{i}{C}=0 \tag{1}$$

and is applicable to either charging or discharging conditions. For the present purpose, however, the discharging condition is the one of practical importance and is the only one considered here. The general solution of equation 1 is

$$i=A_1\epsilon^{\frac{-R+B}{2L}t}+A_2\epsilon^{\frac{-R-B}{2L}t} \tag{2}$$

Applying the terminal conditions $i = 0$, capacitor volts $= E$, when $t = 0$, to equation 2 results in the equation:

$$i=\frac{E}{B}\left[\epsilon^{\frac{-R+B}{2L}t}-\epsilon^{\frac{-R-B}{2L}t}\right] \tag{3}$$

The value of B in the exponential permits three distinct conditions:

$$R^2<\frac{4L}{C},\quad R^2=\frac{4L}{C},\quad R^2>\frac{4L}{C}$$

As is well known, the first of these conditions corresponds to an oscillatory discharge. Such a discharge is useless for the purpose here considered, although it does have practical applications in magnet operation in the so-called *impulse* operation of magnetic contactors.[4]

Delayed-release magnets of the type here discussed must necessarily have coils of high impedance, being wound with many turns of fine wire. As the turns are increased by reducing the size of wire in any coil of given dimensions, there is a much more rapid increase in the quantity R^2 than in L, since L varies as n^2, while R^2 varies as n^4. The lower space factor of finer wire accentuates this condition. Furthermore, the values of capacity involved are large (on the order of 25 microfarads).

From the above facts it follows that the normal condition in the present application is that $R^2>4L/C$. In practical cases of coils and capacitors used for timing of this type, R^2 varies from 2 to 2.5 times $4L/C$. Calculation of timing under such conditions which neglect L altogether are found to give results within the limits of accuracy established by the normal manufacturing variations of the circuit components.

Where L is neglected, equation 3 reduces to:

$$i=i_0\epsilon^{\frac{-t}{CR}}=\frac{E}{R}\epsilon^{\frac{-t}{CR}} \tag{4}$$

which is solved for t as follows:

$$t=CR\ \log_\epsilon\frac{i_0}{i} \tag{5}$$

These are the equations applicable to the simple capacitor-resistor timer of discharge type.

In a practical capacitor-relay timer, the timing magnet will release at some definite value of ampere-turns, which can be introduced into equation 5 by writing it:

$$t=CR\ \log_\epsilon\frac{ni_0}{ni} \tag{6}$$

Methods of Adjustment

Questions of relay and coil design and selection of circuit constants are to some extent dependent on the method adopted for providing adjustment of timing. The available methods are implicitly expressed in equation 6.

Type 1—Adjustment of Capacity. Practicability of this method suffers from the

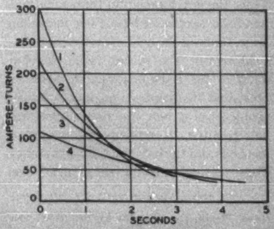

Figure 5. Ampere-turns versus time for different values of external resistance r

Curve 1—r = 30,000 ohms
Curve 2—r = 50,000 ohms
Curve 3—r = 70,000 ohms
Curve 4—r = 122,000 ohms
Coil resistance = 20,000 ohms

lack of adjustable capacitors of large capacity. It is, however, being used on resistance welding timers where the limited timing range makes stepped adjustment of capacity (by a fan switch) permissible. Adjustment by capacity has the advantage that it permits the use of an insulated dial with definitely calibrated points.

Type 2—Adjustment of Resistance. This method is not practicable because there is no way to adjust the resistance of the relay coil.

Type 3—Adjustment of Initial Ampere-Turns. This may be done by varying the voltage to which the capacitor is charged. Without unreasonable circuit complication it is only applicable where the delayed-release magnet has only a restraining function, as in figure 2. Figure 3 shows a practical circuit, which, however, is not in use because of the greater simplicity of other methods.

Type 4—Adjustment of Dropping Ampere-Turns. To accomplish this simplest method of adjustment it is only necessary to provide for changing the magnetic gap of the relay armature when in closed position. It is applicable where the delayed-release magnet has both a pickup and restraining function (figure 1) as well as where it has a restraining function only (figure 2).

Factors Affecting Timing

In all cases it is desirable to use a resistor unit *r* in series with the capacitor. The ohmic value of this resistor may be anything from a minimum value which is recommended for protecting the capacitor against surges, to a much higher value which may be desirable to obtain maximum timing efficiency. Whether the latter may be used depends upon whether it is permissible to take appreciable time to recharge the capacitor between operations. In some cases, additional actuating contacts are used to commutate this resistor so that its ohmic value is low for the charging condition and high for the discharging condition.

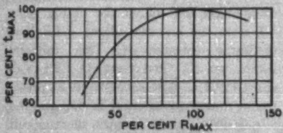

Figure 6. Plot of equation 10

Referring to figure 1, it will be seen that the ampere-turns in the relay coil, before the actuating contact is opened, are simply determined by the coil characteristics and the line voltage. When the actuating contact is opened, the current in the relay coil drops in a transient period of negligible duration to a value depending upon the initial charge on the capacitor and the resistance in circuit, figure 4. Thereafter the current varies substantially in accordance with equation 4.

Where the magnet has a pickup as well as a holding function, the coil design is determined by the former. Where the function is holding only, on the other hand, the coil is wound with the finest wire which can be wound economically. Figure 5 is a set of curves plotted in accordance with equation 4, for a particular coil and capacitor and for various values of *R*. These curves are plotted in ampere-turns rather than amperes because the pickup and dropping characteristics of relays are so expressed.

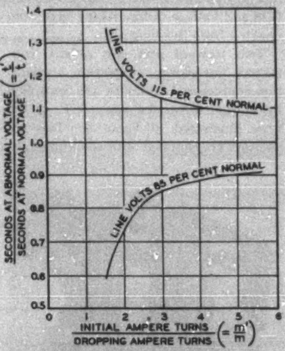

Figure 7. Effect of voltage variation on timing, expressed as a function of the ratio of initial to final ampere-turns

To determine the value of *R* which results in maximum timing, the form of equation 6 may be changed to

$$t = CR \log_e \frac{En}{mR} \tag{7}$$

wherein *m* is the dropping ampere-turns.

Differentiating equation 7, and equating to zero to find *R* for maximum *t*, results in

$$R_{max} = \frac{En}{m\epsilon} \tag{8}$$

$$t_{max} = CR_{max} \tag{9}$$

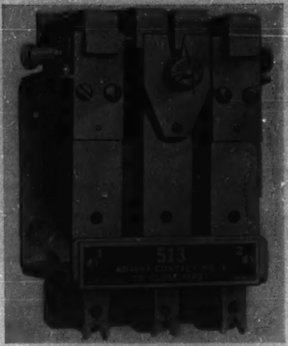

Figure 8. The relay for armature acceleration has two (outer) armatures for timing, and a third (center) armature for insuring correct operating sequence

There are two practical considerations which make it undesirable to use the full value of total resistance indicated by equation 8:

1. Variation of coil ampere-turns near the point of relay armature release may be so gradual that small changes in release ampere-turns cause appreciable changes in timing.
2. Effect of line-voltage fluctuations on the timing is excessive. This factor is discussed in the next section of this paper.

Combining equations 7, 8, and 9 results in the following equation which expresses the effect on the timing of using a value of *R* which differs from R_{max}:

$$\frac{t}{t_{max}} = \frac{R}{R_{max}} \log_e \frac{R_{max}\epsilon}{R} \tag{10}$$

This equation is plotted in figure 6 and shows that reduction of *R* to 50 per cent of R_{max} only results in 15 per cent reduction in timing. This value of *R* corresponds approximately to that used in actual practice. Where the timing adjustment is by variation of closed magnetic gap (type 4) the value of *R* is important not only for its effect in increasing the time periods available with given apparatus, but also because the flatness of the timing curve determines the range of timing adjustment available. Thus with curve 3 of figure 5 the range of timing from zero to four seconds is obtainable by changing the dropping ampere-turns from 170 to 35, whereas to obtain the same range of adjustment under the conditions of curve 1, figure 5, may be seen to require a much wider adjustment of dropping ampere-turns, which may not be obtainable in practice.

Effect of Line-Voltage Fluctuations

Fluctuation in line voltage constitutes the most serious adverse factor in this timing method. It is desirable to show the extent of its effect and how the latter may be minimized.

Let

m = dropping ampere-turns, as above
m' = initial ampere-turns, at nominal line volts
k = ratio of actual to nominal line volts
t and t' = timing at nominal and actual line volts, respectively

Then equation 6 may be written:

$$t = CR \log_e \frac{m'}{m} \tag{11}$$

and, since initial ampere-turns is proportional to line volts,

$$t' = CR \log_e \frac{km'}{m}$$

or

$$\frac{t'}{t} = \frac{\log_e \frac{km'}{m}}{\log_e \frac{m'}{m}} \tag{12}$$

In figure 7, equation 12 is plotted for $k = 0.85$ and 1.15. These curves clearly show that low values of m'/m accentuate the effect of line voltage fluctuations. If $R = R_{\max}$ is used in order to obtain maximum timing, the initial ampere-turns are, using equation 8,

$$m' = \frac{En}{R_{\max}} = m\epsilon \text{ or}$$

$$\frac{m'}{m} = \epsilon = 2.718$$

It was stated in the preceding section that use of the full value of $R_{\max}$ leads to excessive sensitivity to line voltage fluctuations. Reference to figure 7 shows a variation of roughly ±15 per cent in time for a ±15 per cent voltage variation, which might be permissible. But in the case of a type 4 adjustment, timing is reduced by increasing the dropping ampere-turns; that is, by increasing m in equation 11, with corresponding decrease in m'/m. Figure 7 shows that decreases in m'/m by this method below 2.718 over any appreciable range would result in very excessive voltage sensitivity over the lower end of the range.

On the other hand, by using considerably lower values of R, such as $R_{\max}/2$, this tendency is greatly reduced since, for maximum timing, $m'/m = 5.43$. An inspection of figure 7 shows that the permissible range of variation of m'/m in this case is considerably greater than when $R = R_{\max}$. Experience has shown this to be an acceptable compromise value.

Typical Controller

Figure 8 illustrates a capacitor-timed relay generally used for armature acceleration. The outside armatures are provided with normally closed contacts and individual closed magnetic gap adjustments for timing. A center armature carries a normally open contact and a fixed closed magnetic gap. Figure 9 shows a similar relay with two independently timed armatures carrying normally open contacts. This relay finds particular use in field acceleration.

A typical automatic d-c motor control with both armature and field acceleration is shown in figure 10. Figure 11 is the elementary diagram for this control. For the sake of simplicity, the overload relays, dynamic braking contactors, and field and voltage failure relays have been omitted from this diagram.

Two forward and two reverse contactors are shown. Armature acceleration contactors 1*A* and 2*A* are controlled by a capacitor-timed relay, *CT*, of the type illustrated in figure 8. Two capacitors are supplied with this timing relay in reversing controllers of this type, for reasons outlined below. The capacitors are designated *CAP.*-1 and *CAP.*-2 on the diagram and are connected in the circuit with normally closed auxiliary contacts on the reversing contactors, as indicated. A capacitor relay, *FA*, identical with that shown in figure 9, provides field acceleration.

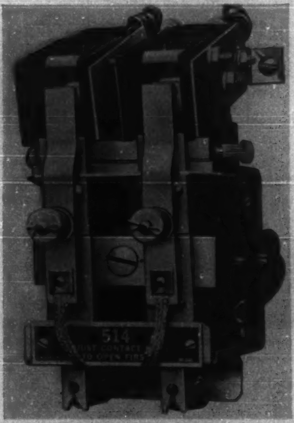

Figure 9. The relay for field acceleration has two independently timed armatures

Figure 10. A full-reverse nonplugging, dynamic-braking d-c motor control with three steps of armature and two steps of field acceleration

Operation of this control is, briefly, as follows. With voltage on lines L_1 and L_2, relay *CT* picks up, closing contact *CT-N.O.* and opening contacts CT_1-*N.C.* and CT_2-*N.C.* Capacitors, *CAP.*-1, *CAP.*-2, and *CAP.*-3 are charged through their respective external resistors. Relay *FA* also operates, closing contacts FA_1 and FA_2.

When the "forward" push button is closed contactors 1*F* and 2*F* close, connecting the motor armature to the line through the two steps of starting resistance. Contactor 1*F*, in closing, opens an auxiliary contact 1*F-N.C.* which disconnects the coil of the timing relay *CT* from the line and allows *CAP.*-2 to discharge through it. This initiates timing on *CT*, and contacts *CT*1-*N.C.* and *CT*2-*N.C.* close successively at preset timing intervals to energize 1*A* and 2*A* respectively, which in turn short-circuit the two steps of starting resistance. During this time, contact *CT-N.O.* has opened and the main contactors 1*F* and 2*F* are maintained through the interlocking resistance. Contactors 1*F* and 2*F* are picked up only when contact *CT-N.O.* is closed. The latter is operated by the center relay armature which is provided with a larger open magnetic gap than the armatures carrying contacts *CT*1-*N.C.* and *CT*2-*N.C.* This assures that the latter two contacts are opened before contactors 1*F* and 2*F* close. Contactors 1*A* and 2*A* cannot therefore close at the same time as 1*F* and 2*F* and connect the motor armature directly across the line.

When contactor 2*A* closes, it operates an auxiliary contact 2*A*-*N.C.* which disconnects relay *FA* from the line and allows *CAP.*-3 to discharge through the coil. Contacts *FA*1 and *FA*2 open successively at preset intervals inserting resistance in series with the motor shunt field. The rheostat permits manual control of the motor speed.

As shown on the diagram, two capacitors are used in conjunction with the relay coil *CT*. While operating in the forward direction *CAP.*-2 discharges through coil *CT* to provide timing. On reversal, the auxiliary contact 2*R*-*N.C.* opens and allows *CAP.*-1 to discharge through coil *CT*. An alternate circuit utilizing a single capacitor is designated by the broken line on the diagram. Because of the series resistor, a definite time interval is required to charge the capacitor to line voltage. When the "reverse" button is pressed immediately after the motor has stopped from "forward" operation, sufficient time might not be available for a complete charge of the single capacitor. This would result in shorter timing of contactors 1*A* and 2*A*, and a too rapid acceleration of the motor. To protect against this possibility, a capacitor is provided for each direction of rotation of the motor. While it is operating in one direction, the capacitor circuit controlling timing in the reverse direction remains across the line and is charged to full line voltage.

Conclusion

Although the development described in the foregoing paper is quite recent, several hundred controllers employing capacitor-relay timing for acceleration are already in successful service in varied applications. The inherent advantages of this method, as compared with earlier methods, appear to justify the belief that it will be used very extensively in the future.

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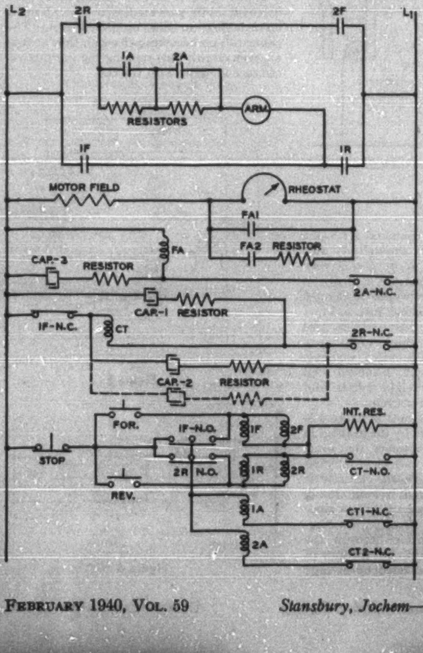

Figure 11. An elementary diagram of connections for the controller illustrated in figure 10

Discussion

F. H. Winter (nonmember) and **L. T. Rader** (both of General Electric Company, Schenectady, N. Y.): We agree with Messrs. Stansbury and Jochem that accurate means for obtaining various time intervals is a very important part of industrial-control work; and the use of capacitors for this purpose offers a very flexible and accurate method of control. In applications where dirt may interfere with operation, operators object to small mechanical arrangements such as escapements, dashpots, and motors to obtain time delays. One of the standard devices having no moving parts but using electrical induction is the coil with short-circuited secondary or copper jacket. It has, however, the two limitations of:

1. A maximum time delay drop-out of seven or eight seconds.
2. It does not lend itself to easy adjustment during operation.

Therefore, as stated by the authors, the comparatively recent reduction in cost of manufacture and decrease in sizes of capacitors, as well as the demand for longer time intervals and more flexible control, has intensified their application. In circuits using no electronic devices we have obtained ranges in time from seven cycles to three minutes. The latter figure was obtained on a motor-starter using 600 microfarads at 250 volts.

We should like to show several uses of capacitor circuits other than those presented. These are:

1. Compensation for voltage changes.
2. A high pickup force.
3. A fast pickup and fast drop-out.
4. Time delay pickup.
5. Use of capacitors as interlocks.

Figure 7 in the authors' paper shows the effect on timing of line voltage fluctuation. Where good accuracy is required, we have eliminated this variation in timing with variation in voltage by the use of the circuit shown in figure 1 of this discussion.

A compensating coil is in series with a regulating rheostat across a potentiometer so that its ampere-turns are directly proportional to the line voltage and in opposition to those of the main coil. When the master control switch (*MCS*) is closed, the normally open interlock completes the circuit to the potentiometer connection, thus energizing the compensating coil. Meantime, the second *N.O.* contact on the master control switch has allowed the capacitor to become rapidly charged from the line through only a small limit resistance. The device picks up and opens the *N.C.* interlock, thus inserting the discharge resistance which will give the proper time constant. To start the time-out operation, the master-control-switch contacts are opened, disconnecting the main coil from the 250-volt bus and putting it across the capacity and discharge resistor in series. The compen-

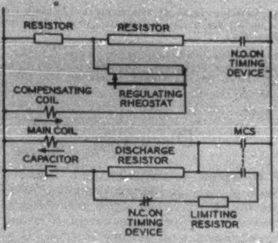

Figure 1

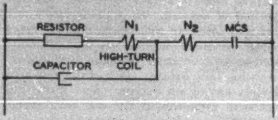

Figure 2

sating coil remains connected to the main bus and for any value of regulating resistance and capacitance, compensation is obtained by proper adjustment of its bucking strength and drop-out spring tension. The only factor which can affect this device adversely is a change in bus voltage which occurs between the time the master-control-switch contact opens the circuit to the main coil and the time the device drops out to the de-energized position. With 100 microfarads capacity, 75,000 ohms discharge resistance, and 16 volts across the compensating circuit, the following time drop-out values for changes in regulating rheostat were obtained:

3,000 ohms—26 seconds
2,250 ohms—22.5 seconds
1,500 ohms—18 seconds
750 ohms—11 seconds
0 ohms—1/5 second

By changing the capacitance, a different range in times may be obtained. Voltage compensation, of course, takes place for any or all the above settings.

The regulating rheostat also gives a means of remote control of time delay when desired. On the above device, when the voltage changed from 150 volts to 280 volts, test results showed a maximum variation of less than one per cent in time drop-out over the complete range.

The authors state that a coil design for a magnet is determined primarily by the pickup value required. However, by using a capacitor, a two-section coil may be designed to give both a high pickup force and an economical holding action. Such a circuit is shown in figure 2.

A high-resistance high-turn coil N_1 is in series with a discharge resistor R, across a capacitor C. N_2 is the second section of the coil consisting of a low number of turns and low resistance, compared to N_1. When *MCS* is closed the N_2, C circuit draws an initially high surge of current, thus producing the required high force through the open or high-reluctance path of the contactor.

By proper design, a high value of ampere-turns, sustained through an ample time, can be obtained in a very small winding space due to the short duration of the current. The major part of the winding space is then available for the N_1 winding which performs the holding function after the magnet has picked up. The discharge resistor shown is used solely to obtain time delay drop-out.

In an application where fast pick-up and fast drop-out of a contactor is desired, a connection as shown in figure 3 may be used.

N_1 and N_2 are both 6,000-turn coils, the latter having a resistance only $^1/_{20}$ of that of the former. When contacts *MCS* are closed, the current rush through N_2 aids normal current through N_1 to give a very fast pickup. On drop-out the coils are in opposition so that the capacitor discharges through a circuit of low inductance and fast drop-out. A contactor connected in this manner picked up in three cycles and dropped out in one to 1.5 cycles, whereas, the same contactor with standard coil picked up in seven to eight cycles and dropped out in four to five cycles.

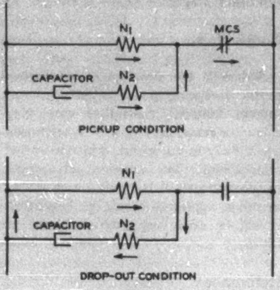

Figure 3

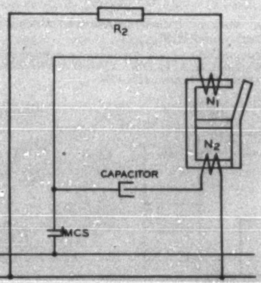

Figure 4

The following circuit shown in figure 4 is very similar to that shown by the authors in their figure 2. We feel, however, that some operating characteristics may be of interest.

With the connection shown, electrolytic capacitors cannot be used if long timing of great accuracy is required due to their leakage characteristics. With proper magnetic reluctance relationship between the top and bottom magnetic circuit, the time of pickup is only slightly affected by voltage changes. With two identical coils of 26,000 turns and 4,200 ohms resistance, and with C equal to 50 microfarads, the following results were obtained:

Volts	Time Pickup (Seconds)
285	3.0
250	2.9
200	2.7
150	2.7
100	2.8

To prevent change of timing with temperature, a resistance R_2 of zero-temperature-coefficient wire is used in series with the pickup coil to consume about 80 per cent of the power of the circuit.

With the following connection (figure 5) electrolytic capacitors can be used and much longer times can be obtained.

With the same coils and capacitance as in figure 4, the following data were obtained:

Volts	Time Pickup (Seconds)
280	14.0
250	14.1
200	14.0

Figure 6 shows a simple connection where a capacitor can be used in place of an interlock which would normally short out a resistance on starting.

When *MCS* is closed, the capacitor acts as an initial by-pass for the current, allowing enough flow to pick up the device. The capacitor, on becoming charged, then acts as an open circuit thus putting the limiting resistance in series with the coil.

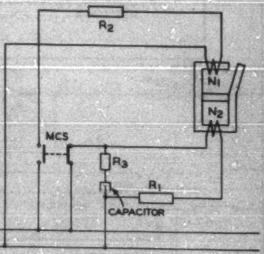

Figure 5

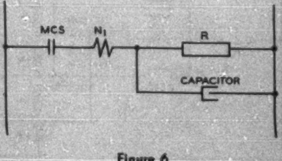

Figure 6

Recent Developments in Telegraph Switching

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IN TIMES when so much thought and attention are given to the streamlined development of modern life, such as aviation, de luxe high-speed trains, airmail, radio, television, sound movies, and other wonders of science, it is quite natural that an industry such as the telegraph should find itself called upon constantly to fill an ever increasing demand for new services or functions created by this world of today. With every new development in modern industry, new needs arise for quick interchange of information—accurate and in written form for permanent record to insure against misunderstanding or forgetfulness. It is these needs which the telegraph, through its system of fast record communications, has been able to anticipate and provide for. Engineering research and developments in telegraphy have fully kept pace with the modern trend, and during the last year or two, there have been brought to conclusion several developments which promise to make the telegraph more necessary than ever to our social and industrial life.

The reperforator switching system of the Western Union is one of these recent products of its engineering laboratories—one that seems destined to change the entire character of the telegraph office, and to advance the art of handling telegraphic communications in a manner fully comparable with other trends of present day life.

It is seldom that new developments embody new principles. Oftener than not, the newest improvements in technology are refinements of what has already gone before. They are what may be considered to be an intelligent fitting together of elements handed to us by our predecessors. Thus the reperforator switching system is but the result of the careful selecting, refining, and fitting together of certain devices and ideas which have come down through the years of telegraph, together with the development of certain new elements which are essential to the success of the system as a whole.

Relay Offices

By its nature, telegraph business consists of the transmission, in record form, of multitudinous short messages from a large number of sending points to an equally large number of receiving points. Like a railroad system with its variously routed traffic, the telegraph from its earliest days has been able to function economically only through the medium of central transfer points, or relay offices, each of which has direct connection to numerous originating and destination points, and direct wires to one or more other relay points. At these relay offices, as high as 95 per cent of the telegrams relayed are received over printing telegraph circuits and retransmitted over similar circuits, the only exceptions being those messages handled over Morse-operated circuits or by pneumatic tubes connecting with branch offices.

Obviously this system permits handling the maximum amount of business over a minimum number of wires, but it also requires a considerable amount of manual work at the relay offices. To be more specific, the reception of a telegram in a relay office involves the service of a receiving operator who scans the telegram for errors, times it, marks it off on a tally sheet, and releases it. It is then manually or mechanically carried to a distributing center where it is routed to the circuit over which it is to be retransmitted and is manually or mechanically carried to the terminus of that circuit. The transmission out of the relay office requires the services of a transmitting operator. These offices must be adequately staffed with operators, distributing clerks, and supervisory forces, to insure rapid handling, for telegrams must be at their destinations within a few minutes after they have been filed at the originating point. It was with a view of eliminating as many of these manual operations as practicable and replacing them with a single switching operation that the development of reperforator switching was undertaken.

Early Experiments With Storage Devices

During the many years since Morse first invented the telegraph, there have been developed many devices for the automatic transmission and reception of telegraph signals, all intended to increase speed and improve accuracy. However, there has always remained the impracticability of switching signals directly from an incoming to an outgoing circuit, without creating difficulties on the incoming one while waiting for the outgoing one to clear of other business. These waiting times would either cause intolerable congestion of messages on the incoming circuits, or would be seriously wasteful of valuable circuit time, depending on the type of system employed. In order to maintain continuous high-speed operation, it is essential that means be provided for storing the incoming signals without delaying their reception, for moving them immediately from the incoming to the outgoing circuit terminals, and then for retransmitting them on the outgoing circuit just as soon as the latter is clear.

Thus the elements for a successful switching system for commercial telegraph service must include provision for a distant station to transmit into the switching system at any time and at its own convenience, a means of distributing these telegrams at high speed to the transmitting terminals of the proper outgoing circuits at each of which are accumulated, in one storing device, the telegrams for that circuit, and a means for transmitting these accumulated telegrams as fast as the line will accept them. Finally, all of this should be accomplished so efficiently that when circuits are available, an outgoing telegram will be on its way immediately. In addition to these fundamental elements, there must be numerous devices for check purposes to insure that each telegram is handled correctly.

If space and time permitted, it would be interesting to discuss the many different schemes and inventions which previous workers have evolved for a speedier and more efficient handling of telegraphic communications. But such a study would serve principally to demonstrate, once more, that new developments are often but refinements of old ideas, with certain important changes and additions. It would show, for example, that paper tape perforated with holes corresponding to the telegraph code was

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used for automatic sending as long ago as in 1846—only two years after completion of the first telegraph line between Washington and Baltimore. It would show that keyboard perforators for preparing such tape were patented over 80 years ago. Automatic recording by a perforated tape was proposed a few years later, while automatic retransmission direct from recorder tape was proposed about 50 years ago. Other inventions which might call for special mention would include various tape perforators controlled by incoming signals, mechanisms for operating type-setting devices from received perforated tape, and printers operating directly from received tape. There also have been arrangements for both printing letters and perforating tape, simultaneously, from received signals.

Numerous inventors have devised mechanical arrangements for receiving and storing signals, such as pin storage devices using plates, drums, disks, or chains and devices employing balls which were dropped in recesses in "letter blocks" that acted as links in a chain. In general, however, mechanical storing transmitters have not met with much commercial success, largely because they are definitely limited in storage capacity. If large capacity is provided in original design, it proves costly and usually entails parts that are too heavy for high-speed operation.

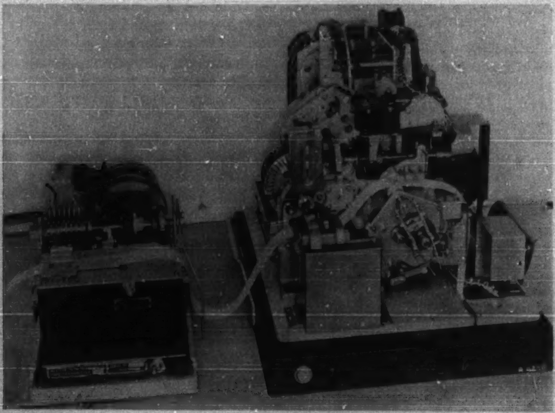


Figure 1. Printer-perforator and transmitter

From the foregoing, it will be evident that the fundamental elements of reperforator switching systems are really not new. In fact, the use of reperforators, or some form of storing transmitter, was contemplated by the Western Union in 1912, when its multiplex system was being designed, and code combinations were actually assigned to permit a sending operator to select a distant printer or reperforator, as the case might be.

Reperforators were used to a limited extent in Western Union service about 1925 in connection with transatlantic picture transmission by the Bartlane system, and during 1930 a six-unit reperforator was developed for retransmitting into quotation service branch circuits. This was later modified for five-unit code and was used in inaugurating timed wire service in 1931. The following year perforating attachments were provided for standard multiplex tape printers and teleprinters, so that a perforated tape as well as a printed tape was produced. Although developed primarily for timed wire service, these attachments found many other applications and proved to be especially valuable in relaying large files, such as from the 1932 Olympic games in Los Angeles and the national political conventions.

The first practical attempt by the Western Union to handle regular telegraph traffic by means of automatic reperforation was made in Newark, N. J., in 1933. In this experiment, each telegram, in perforated tape form, was physically transferred from the receiving position to the outgoing circuit, but the results of the trial almost immediately demonstrated that such an arrangement offered no outstanding advantages over existing methods. The difficulties of reading perforated tape at high speed and handling large quantities of short lengths of paper tape, together with the possibility of delaying telegrams in so doing, seemed to offset any advantages that might be gained in other respects.

From the experience gained at Newark, it was evident that automatic recording and retransmission could not be satisfactorily applied to the handling of relay traffic without means for quickly and easily reading the destination of the telegram when it was received in the form of perforated tape, and also some method whereby the tape could be fed continuously through a transmitter associated with the receiving perforator, thus avoiding the accumulation of tape at the receiving position. It was obvious, also, that facilities must be provided for switching the transmitter to various outgoing lines.

Development of Reperforator Switching

The next development, therefore, was the embodiment of these various features into a complete reperforator switching system installed at Fort Worth, Tex., in 1934. This installation in its present form employs perforating printers which print letters on one tape and perforate the code on another, and a switchboard at which the transmitter associated with an incoming message is plugged directly to the outgoing connection. This outgoing connection is not the outgoing circuit itself, but an intraoffice circuit which serves to clear the incoming transmitter and transfer the message to a second storage directly associated with the outgoing circuit. This intraoffice circuit operates at 90 words per minute, as compared with 65 on the incoming circuits, and thus can take up the slack caused by any delay in switching. Transmission on the outgoing circuit is automatic and starts immediately, or at least as soon as the line is cleared of other business. Another important feature is the automatic numbering machine on each outgoing circuit, which inserts consecutive serial numbers in all telegrams sent, so that there is a permanent identification for every message.

Later Improvements in Reperforator Switching

The installation at Fort Worth conclusively demonstrated that the general scheme of operation was correct and that with the system applied to larger offices, particularly those handling a relatively large proportion of relay traffic, substantial improvement in operation could be effected. It also developed the need of further refinement in certain features. The development of these followed, and in October 1937, the present system was placed in operation in Richmond, Va. Of the numerous features which distinguish this project from the Fort Worth one, most important is the printer perforator, a new printer which simultaneously prints the telegram and punches the code perforations on the same tape, so that the telegram may be read easily by anyone having no knowledge of the telegraph code, and its destination determined as soon as it is received.

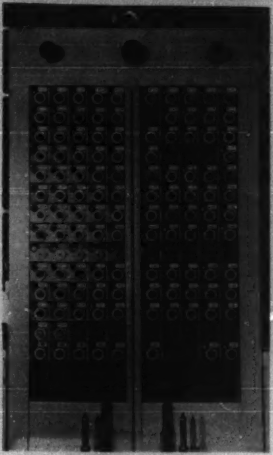


Figure 2. Switchboard

Another improvement was the provision for still higher speed on the intraoffice circuits. It was found at Fort Worth that circuits which received continuously for hours, occasionally became congested, due either to gradual accumulation of time employed for switching, or to "waiting time" caused by a needed outgoing circuit being pre-empted by another transmitter. The higher speed necessary to avoid this congestion was obtained through the use of multiwire intraoffice circuits which permitted the transmission of all five impulses of each letter or character simultaneously, instead of consecutively, as in the previous single wire circuits. This arrangement necessitated the development of new high-speed receiving perforators at the outgoing transmitters, but it made possible an intraoffice speed of 125 words per minute. The multiwire circuits were made possible through the development of special jacks and plugs designed to transfer a total of nine circuits simultaneously—five for the code impulses and four for special cord-circuit functions necessary to the switching operations.

Except for a general redesign of the equipment assemblies and arrangements, the only other important novelties incorporated in the Richmond installation were the provision of special switching and storage facilities to permit of differentiation between different classes of business, such for example, as certain rush traffic which requires special handling, deferred rate business which often must be temporarily held in storage, and other traffic which for one reason or another cannot be immediately transmitted to its destination. A more detailed description of the new reperforator switching system follows.

Theory of Operation

In the reperforator switching system which Western Union has installed at Richmond, Va., all printing telegraph circuits terminate in a special switching section. Each of these circuits is, in effect, two circuits, one transmitting and one receiving. All receiving circuits are grouped together in one section of the switching unit, and all transmitting circuits are grouped in another section. Intraoffice circuits between the two sections permit transmission from the receiving section to the transmitting section.

Each receiving circuit terminates in a recording device called a printer-perforator which prints each received character along one edge of a paper tape and simultaneously perforates holes in the tape immediately below the printed character. These holes are printing telegraph code combinations which represent the letter printed above them, and when the tape is passed through a transmitting device they cause that letter to be transmitted. Telegrams may therefore be stored in this paper tape and retransmitted over any other circuit by passing the tape through the proper transmitter (figure 1 shows a printer-perforator and a transmitter).

Each transmitting circuit terminates in a transmitter that is permanently connected to it. Telegrams to be transmitted over these circuits are electrically transferred to them from the receiving positions over intraoffice circuits. These intraoffice circuits are in two parts. One part is formed by a transmitter located immediately adjacent to each printer-perforator in the receiving section, and this transmitter terminates in a cord and plug on the shelf of a switchboard. The other part is formed by a printerless perforator located immediately adjacent to each transmitter in the transmitting section, and it terminates in multipled jacks in all of the switchboards (figure 2 shows a switchboard).

When a telegram is received on a printer-perforator, the perforated tape produced by this device passes through the intraoffice transmitter beside it. The plug of that transmitter is inserted into a selected one of the jacks in the switchboard which completes a circuit to a printerless perforator where the message is again recorded in the form of perforated tape. This perforated tape automatically passes through the line transmitter immediately beside it and the telegram is transmitted to a distant station.

This theoretical description of reperforator switching is, of course, much simpler than the actual operating installation. Many other functions have to be performed in order to make the system practical.

Layout

The switching equipment of all receiving terminals is mounted on double deck-tables in order to concentrate the printer-perforators as closely as possible to the switchboard which serves them, and in order to conserve floor space. Circuits which handle a large number of telegrams are placed in groups of four to each switchboard. In these groups, two printer-perforators, one on the lower shelf and one on the upper, are placed on each side of the switchboard. On more lightly loaded circuits, eight receiving positions are associated with each switchboard. In these cases, two upper and two lower shelf positions are used on each side of the switchboard. A switchboard and its quota of four or eight associated printer-perforators and intraoffice transmitters form a complete receiving terminal unit.

All of these switching units are grouped in two long banks which face each other in such manner as to provide a working aisle where all manual switching functions are performed (figure 3).

Transmitting positions are equipped

with intraoffice automatic perforators, line transmitters, and numbering machines which will be described later. These units are grouped in a separate section of the office on double-deck tables which also face each other to form a working aisle for supervisory and regulating purposes (figure 4).

A third section of the room is formed by manual transmitting and receiving positions similar to those in any other telegraph office (figure 5). Telegrams received over Morse wires or by pneumatic tube or local telephone are transmitted from this section into the switching unit where their handling is identical with the switching operation already described. Telegrams received in the switching unit to be retransmitted over Morse circuits, pneumatic tubes, or local telephone are switched into one of the receiving positions of the manual section where their handling is identical with that in any other telegraph office. There is, however, one difference between these manual positions and those of an ordinary telegraph office. They are not permanently connected to individual circuits, therefore the loads may be concentrated on the fewest possible positions and operators do not have to move from one position to another.

Switching Operations

In the switching section each receiving circuit terminates in a printer-perforator which serves that circuit alone. The printed and perforated tape which comes out of that instrument tends to form a loop between the printer-perforator and the intraoffice transmitter. This loop drops into a glass tank immediately under the printer-perforator and accumulates there until the intraoffice transmitter functions to transfer it across the room to the transmitting positions. The intraoffice transmitter will not operate, however, until a connection has been established through the switchboard. Since this connection is not started until a telegram has been completely received, some accumulation of tape is normal. This accumulation may at times exceed one telegram due to the fact that one switching clerk handles several receiving circuits, but the quantity of unswitched tape on hand is always visible to the clerks and supervisors. Any accumulation of tape which will require more than a minute or two for intraoffice transmission is given continuous attention by the clerk until it has been eliminated.

Before a tape can be switched, it is necessary for the clerk to ascertain its destination and select the switchboard jack connected to the proper transmitting circuit. The printed characters on the tape facilitate this operation. The destination usually appears in the tape about eight inches from the beginning and is readily visible to the clerk.

After noting the destination, the clerk picks up the plug associated with that transmitter and inserts it in the jack of the switchboard which serves the desired transmitting circuit. This operation completes the clerk's work on that particular telegram and she is free to proceed to another circuit.

It is essential that this manually performed portion of the switching operation be completed on the first attempt by the clerk with no waiting. It must be possible for her to insert plugs into jacks whenever that operation is most convenient, and leave all subsequent operations to the functioning of purely automatic equipment. There are frequent occasions when two or more transmitters will have need for the same intraoffice circuit simultaneously, therefore there must be a plurality of jacks in each switchboard which serve the same intraoffice circuit, and each intraoffice circuit must be multipled to all switchboards.

Circuit Allotter

In order that plugs may be inserted simultaneously in these multipled jacks without interrupting one another, a device known as a circuit allotter is used. When a plug is inserted in a jack of the switchboard only a potential connection is established. A plurality of simultaneous potential connections to the same intraoffice circuit are converted one at a time into actual connections through the action of the circuit allotter. This allotter breaks the existing connection at the end of each message and establishes an actual connection from one of the potential connections, repeating this operation until there are no more potential connections.

The circuit allotter is caused to function at the end of each telegram by a special signal which is transmitted by the station which transmits the telegram into the Richmond office. This signal is two periods and one space. It is essential that intraoffice connections be broken at the end of each telegram in order that each telegram may be individually routed over that circuit which will give it the most direct path to its destination. It is the exception rather than the rule that two consecutive telegrams received over any interoffice circuit will be destined to the same outgoing circuit. Therefore all intraoffice transmission is "single shot" or one switchboard operation for each telegram. The intraoffice transmitter, of course, is stopped automatically after each telegram. If a plug is left in a jack after transmission is completed on the telegram for which the connection was established, it will not interfere with subsequent use of the intraoffice circuit by other positions and the circuit allotter will not subsequently re-establish the connection unless the plug is removed from the jack and then reinserted. These operations on the part of the circuit allotter make it practicable for the switching clerk to insert plugs in jacks immediately upon arriving at a switchboard, and then remove that plug from the jack at any convenient time after transmission is completed.

Numbering Messages

Experience has shown that certain safeguards are needed in handling telegrams. One of these safeguards is the use of identifying numbers. These numbers which are in consecutive order starting with number one each day, are affixed and transmitted by transmitting stations and are checked off on number sheets by the receiving stations. Each circuit has its individual series of numbers, therefore a telegram received over circuit *A* under one number, will in all probability be retransmitted over circuit *B* under a different number. This enables the two terminals of each circuit to maintain a constant check on transmission.

In the reperforator switching system it was found to be impracticable to manually insert numbers on telegrams when they were retransmitted. A method was therefore devised to insert these numbers automatically. In order to serve their purpose, numbers must not only be transmitted, but a record must be made at the transmitting office which will indicate which number was assigned to each telegram. This is accomplished by inserting an automatic numbering machine in the intraoffice circuit between the switchboard jack and the automatic perforator on the transmitting circuit. It thus becomes a part of the apparatus associated with the transmitting terminal. When an intraoffice connection is established through the insertion of a plug in a jack and the ensuing operation of the circuit allotter, the automatic numbering machine instantly pre-empts the circuit, transmits its next consecutive number into the automatic perforator and then establishes the connection between the

Figure 3. Switching aisle in receiving section, Richmond, Va.

Figure 4. Supervisory aisle in transmitting section

automatic perforator and the transmitter which selected it. The transmitter then starts automatically and transmits its message. The perforated tape produced by the automatic perforator for transmission on the outgoing circuit will then have the next consecutive number for that circuit immediately preceding the telegram. The tape serves as a record for the switching office to show the relationship of all numbers and their associted telegrams.

Supervisory Measures

In addition to the apparatus employed in the actual handling of telegrams, there are many supervisory devices to facilitate operation and insure safety. In order that switching clerks may know when a telegram has been received on a printer-perforator and is waiting to be transferred through the switchboard to a transmitting circuit, a signal lamp mounted on top of each switchboard lights when a message starts arriving on a printer-perforator served by that switchboard. This indicates to the clerk that a telegram is being received in that group of circuits. Another lamp mounted in a signal indicator panel individual to each printer-perforator also lights to indicate the particular circuit over which the telegram is coming.

In order to provide visual supervision of the functioning of the intraoffice circuits, two small signal lamps are mounted adjacent to the seat of each transmitter plug flush with the switchboard shelf. When a plug is inserted into a jack of the switchboard, one of its lamps will light and remains lighted until the intraoffice connection has been completed and transmission started. When transmission of each telegram is completed, the second lamp will light, indicating that the connection has completed its function and the plug may be removed from the jack.

Since there is a distance of several inches between the punch block of the printer-perforator and the transmitting pins of the transmitter, it is necessary to step out a few inches of blank tape at the end of the last telegram in a group, in order that the last character received may pass through the transmitter. On receiving circuits, push buttons are installed which, when manually depressed, will cause the printer-perforator to step out a predetermined length of blank tape. On transmitting circuits, when the tape between transmitter and automatic perforator becomes taut, the automatic perforator will automatically step out enough tape to allow the last character to pass through the transmitter. When the tape again becomes taut after this operation, no more tape will be stepped out until another telegram has been received.

It may happen that the tape produced by one of the automatic perforators on the transmitting circuits may become broken or jammed in the punch block, or the center holes of the tape stripped so the transmitter will not pull it through. It is very important that such a condition be detected quickly and that the intraoffice connection be maintained until the trouble has been corrected so that a rerun of the tape may be secured. If the connection to the intraoffice transmitter is broken it will be difficult to determine from which transmitter the telegram came. In order to obviate this, alarm circuits are set up to attract attention and to hold the existing intraoffice connection. On the automatic perforator the tape passes between two rollers. The shaft of one roller carries a cam which actuates two sets of contacts in such manner that failure of the tape to move properly while signals are being received by the perforator sets up an alarm signal. When this happens, the action of associated relays will cause the intraoffice transmitter to stop and trouble signals will appear at the automatic perforator position and also at the transmitter position. The circuits are so designed that the trouble at the automatic perforator may be cleared and then by depressing a push button leave the automatic perforator in readiness to function as soon as the tape has been reset in the transmitter. After the tape has been reset at the transmitter, depressing the push button at that position causes the transmitter to start. Transmission cannot be resumed, however, until the push buttons have been operated at both the automatic perforator position and at the transmitting position.

In addition to the problems which concern the actual movement of telegrams, there are problems which relate to good service. Certain classes of telegrams require more expeditious handling than usual. They must be given precedence over other telegrams already in storage or in the process of transmission. In order to accomplish this end, a special position known as the *X* center has been installed. Urgent telegrams are switched direct to this position through special jacks and intraoffice circuits. They are received at the *X* center on printer-perforators in order that their destinations can be quickly read. The transmitters are connected to cords and plugs like the receiving positions of interoffice circuits. A special switchboard serves this center and the jacks are so connected that when a plug is inserted in them, the transmitter at the regular transmitting position is disconnected and stops transmitting, while the transmitter of the *X* center picks up and pre-empts that circuit until transmission of the urgent telegram is completed, at which time the circuit is restored to its regular transmitter which resumes transmission. Urgent telegrams may be and frequently are transmitted in between the first portion and the last portion of ordinary telegrams just the same as in manual transmission.

Traffic Routing

One of the most important operating features is the routing of telegrams to the circuits which have been designated by the circuit layout and routing engineer as the path over which they shall transit. While Richmond has direct connections with many cities, telegrams are received destined to all parts of the world. Direct connection between all cities and towns is obviously impracticable and it is necessary that telegrams be directed toward various offices having connections with the different cities not reached by Richmond. To systematize the handling and to insure that relay messages receive the minimum number of handlings, routing charts are provided and maintained at each relay office. These charts indicate the proper routing of telegrams for points not worked direct.

Complete route charts, covering outside cities, and the distribution between the various branch offices in Richmond, are located between alternate switchboards. This insures that a clerk will be not over five feet from routing charts at any switchboard.

In case a direct circuit fails, it is necessary to route the traffic over some alternate path and continue this routing until the direct circuit is restored. A small neon lamp associated with each jack indicates the condition of that circuit. When a circuit fails, transmission on it stops, and lights at all switchboards indicate that connections are not to be made to that circuit. It is impossible for a transmitter to start if a connection is made to that circuit and the signal lamp commences flashing and continues to do so as long as the plug remains in the jack. All clerks are notified immediately of irregular routings. This information is placed on a blackboard located so that it is visible from any part of the switching aisle. Information regarding irregular routings is immediately placed on this board, and switching clerks immediately start transmission over the irregular paths.

Spill-Over Positions

Certain practical problems connected with operation make it desirable to provide a means of giving special supervision to some of the telegrams transiting through a relay office. In order to meet this requirement at Richmond, four special positions, termed "spill-over positions," are provided. They are similar to the regular receiving positions, having printer-perforators, intraoffice transmitters, and switchboards. When a telegram is to be handled through one of these spill-over positions, a connection is formed through the switchboard in the receiving section to an automatic perforator in the transmitting section. The tape from this automatic perforator passes through another transmitter which transmits into a printer-perforator at the spill-over position. The reason for passing the telegrams through the automatic perforator rather than directly into the spill-over printer-perforator is due to the fact that the intraoffice transmitters on receiving positions transmit at a speed of 125 words per minute and automatic perforators can accommodate this speed, but printer-perforators are designed to operate at line wire speeds of about 60 to 70 words per minute. Therefore it is necessary to transmit from receiving section to automatic perforator at high speed and then retransmit at lower speed into the printer-perforator of the spill-over position. It is necessary to have these telegrams at the spill-over position in printed and perforated form in order to facilitate supervision and expedite retransmission away from the spill-over position. One of the uses of the spill-over position is in connection with telegrams destined to the smaller communities where the telegraph office is closed at night and for a period of the day on Sundays. During the hours when these small offices are closed other means of getting important telegrams to these points are adopted. Sometimes they are sent to the railroad office in the city of destination, and sometimes they are transmitted to a nearby larger city from which point they are telephoned.

All telegrams destined to closed offices receive close censoring at the last relay office in order to determine whether or not they should be held for the office to open or if other action is necessary. This censoring cannot be delegated to switching clerks, therefore when the signal light on the switchboard indicates that a circuit is closed, the switching clerk switches the telegram to a designated spill-over. Full-rate telegrams and deferred-rate telegrams are transmitted into separate spill-overs. The supervisor censors these telegrams and takes any necessary action. If no action is necessary, full-rate telegrams are immediately switched from the spill-over into the regular storage position and held for the office to open. The deferreds are switched into the regular storage positions some time in the early morning, prior to the opening of the office. As the full rates were switched earlier and are already in the tape at the transmitting position, the outside office first receives these full rates and then the deferreds.

The spill-over positions can be used as sidetracks for telegrams to be held pending restoration of failed circuits in those cases where an alternate routing cannot be obtained.

Double Storage

In the foregoing description of reperforator-switching at Richmond, it has been shown that telegrams are stored twice in the switching office—once at the printer-perforator on the receiving circuits and again at the automatic perforators on the transmitting sides of circuits. It has also been shown that the intraoffice transmission between these two perforators is at a speed of 125 words per minute. The reasons for this "double storage" and "high-speed intraoffice transmission" will now be discussed in more detail.

It would be possible to connect the transmitting terminals directly to jacks in the switchboard and switch the signals received on printer-perforators directly to the outgoing line. That would dispense with the need for intraoffice circuits and secondary storage in automatic perforators on the transmitting terminals. Much less equipment would be required and theoretically the handling would be faster because it would be more direct. Such an arrangement is impracticable, however, where a large number of telegrams are relayed between a multiplicity of circuits. Reception on certain circuits will be continuous for hours. Since the speeds of all circuits are approximately equal, it is apparent that transmission away from the printer-perforators could never be faster than reception. Actually it could never be as fast due to the time required to perform the switching function and due further to the fact that at times the transmitting circuit sought by a printer-perforator would be pre-empted by another printer-perforator. Under such circumstances the number of stored messages would increase steadily and continuously. In order to avoid these difficulties it is necessary to transmit away from the printer-perforators at a rate of speed that is much higher than reception. This intraoffice speed is

125 words per minute, as described later. As the outbound circuits will not accommodate such speed, it is necessary to provide an intermediate handling and step this speed down to approximately 65 words per minute. This is accomplished by using high-speed automatic perforators as individual storage bins, each of which accumulates all of the messages that are to be transmitted over one circuit. This creates a condition where messages are accumulated on transmitting terminals over intraoffice circuits at 125 words per minute and transmission to line is at a speed approximately one-half as fast. This condition is not undesirable, however, for transmission to line is continuous as long as there are telegrams on hand to transmit while reception into the secondary storage over the intraoffice circuit is intermittent. If the total volume of traffic to be transmitted over an interoffice circuit is greater than a speed of 65 words per minute can accommodate satisfactorily, additional facilities must be provided. In this respect the problem is identical with similar conditions under manual operation and is not peculiar to the switching method.

The method used in achieving an intraoffice speed of 125 words per minute has been referred to previously, but it is interesting and will be described in some detail. In printer telegraphy each letter, figure, character, or function is selected by the transmission of five electrical impulses. Individual selection is through the various possible combinations of positive and negative impulses in groups of five impulses per character. The total possible number of selections is approximately doubled by printing one character when the printer is in the lower case or letter shift position and another character when the printer is in the upper case or figure shift position, for each combination of impulses.

On all interoffice telegraph circuits the five impulses of each character are transmitted consecutively, each impulse having the same length or duration of time. By means of synchronous operation of the transmitting and receiving devices, each impulse in a group of five controls an individual selecting magnet, therefore each group of five impulses sets up the selection of a character in the receiving printer.

It is possible greatly to increase the speed of transmission by using five wires between the transmitter and the printer and transmitting all five impulses of a character simultaneously. The cost of providing five wires on interoffice circuits is prohibitive and is not used on such circuits. At Richmond, however, the five-wire system is used in order to provide the intraoffice speed of 125 words per minute. By this means an accumulation of messages at the printer-perforator position can be disposed of at a speed approximately twice as fast as they are being received. This high speed also reduces the time that an intraoffice connection is being used, thus reducing holding time from the various switchboards.

It was necessary, in order to use the five-wire system, to develop special plugs and jacks. These carry nine conductors and the plugs are formed by metallic rings separated by nonconducting rings. The jacks have spring contacts that are so spaced that they fit the metallic rings of the plugs. The four extra connections are used in control and signal circuits (figure 6).

While the plugs are larger than the present standard telegraph and telephone plugs, no difficulty has been experienced by the clerks in manipulating them. The jacks and signal lights occupy about one square inch each on the surface of the switchboard but the switchboard has not proved too large for fast switching. With a much larger installation, it may be necessary to reduce the size of the jacks and plugs, in which case a cam switch or a control plug with relay banks may be substituted.

Conclusions

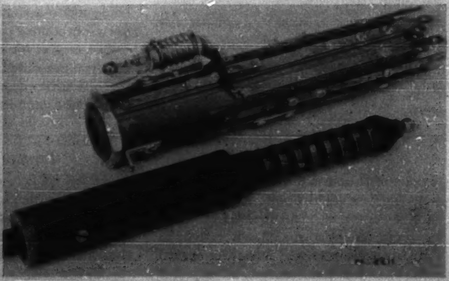
In concluding this discussion of reperforator switching, it seems in order to summarize briefly a few of the results which have been realized in the Richmond installation since its operation started on October 16, 1937. First, relay business moves through the office in about one-quarter of the time required with manual operation. Second, the capacity of intercity circuits has been increased practically 25 per cent due to the fact that transmitters function continuously as long as there are telegrams to be transmitted. Under manual operation, an operator cannot always keep the transmitter busy, even when there are telegrams awaiting transmission.

Another result is a gain in accuracy, due to the elimination of manual reperforation for the retransmission of each telegram. Finally, operator production, under both average and peak load conditions, has been increased substantially. In spite of this increase, the physical and mental effort required of the operating personnel is much less than in other methods of operation. There are also advantages from the central-office standpoint, since the manual and mechanical distribution of telegrams about an office is done away with and the office equipment occupies considerably less floor space than that used previously.

The Richmond installation has successfully withstood every test of practical operation, and has demonstrated its superiority under all conditions. In

Figure 5. Operating positions in manual section

Figure 6. Jack and plug used in nine-wire transmission



Surge-Voltage Breakdown Characteristics for Electrical Gaps in Oil

ROYAL W. SORENSEN
FELLOW AIEE

Synopsis: This paper, emanating from a request for information regarding surge voltages required to break down gaps in oil, gives surge breakdown voltage values with different gap spacings for $^1/_4$-inch, $^1/_2$-inch, and 1-inch round rod electrodes with hemispherical ends immersed in transformer oil. As expected, these values are a function of "time to breakdown" but for each condition curves show constant breakdown values for breakdown times of the order of about 16 microseconds and longer with the $1^1/_2$ x 40 negative wave, used on all the tests reported. Particular attention is called to the relatively small amount of oil required for tests of this type, made possible by the special type of test barrel used.

INSULATION co-ordination and oil-circuit-breaker designs make desirable a knowledge of surge voltages required to break down insulating oil. The lack of data pertaining to this subject was called to our attention by E. K. Sadler and J. L. Thompson, engineers with the Kelman Electric and Manufacturing Company. The test results published in this paper were made possible by the co-operation of J. N. Kelman, who provided some of the equipment required for making the tests, and by a great deal of arduous and careful work done by graduate students, particularly G. D. McCann and A. E. Harrison, in the high-voltage laboratory at the California Institute of Technology.

Voltage for making the tests was obtained by means of a 1,500,000-volt Marx circuit surge generator comprising 30 50-kv 0.5-microfarad General Electric capacitors and a suitable d-c charging circuit. The test gaps in oil were formed by using specially constructed electrodes assembled in a test barrel devised by the author and shown in figures 1 and 2. All tests were made with a standard $1^1/_2$ x 40 negative wave. The voltage for each discharge was measured by means of a cathode-ray oscillograph of the cold-cathode continuous-beam type designed and built by Doctor Howard Griest,[1] and a resistance potentiometer constructed according to the required standards for such work. Figure 3 shows a typical oscillogram. The voltages thus measured were at intervals checked as to peak values by comparison with standard sphere gaps.

All gap test electrodes used were round rods terminating as hemispheres having the same diameter as the rods. This type electrode was chosen in preference to spheres mounted on rods or shanks in order to avoid under surge conditions, uncertainties regarding the influence of shank diameter upon the field uniformity around the gap, and, hence, upon the results obtained by our tests.

Electrodes ranging from one-fourth inch to six inches in diameter were prepared. This report contains data for electrodes having diameters of one-fourth, one-half, one inch, and two inches, respectively. The data for the two-inch electrodes are less complete than for the other rods, because the voltage available, 1,500 kv, would not permit tests with the two-inch electrodes at the larger spacings. An increase in surge-generator voltage is being made and further tests will be reported in a later paper.

One reason for the absence of data of the type presented in this paper has been the difficulty encountered in providing a convenient lead-in bushing for making tests of this type. This difficulty we overcame by making a test barrel in which the electrodes were mounted, and its lead-in bushing of insulating material having about the same dielectric constant as insulating oil. Fortunately, we had available for making this device two cylinders made of paper and varnish, known by the trade names Micarta and Herkolite, respectively. The larger cylinder, 40 inches in diameter and 72 inches long, forms the test chamber, and the smaller one, $23^3/_4$ inches in diameter and 48 inches long, serves as a lead-in bushing. These cylinders were open at both ends when obtained. An oil-tight bottom for the test barrel was obtained by placing the insulating cylinder upon a steel plate on the surface of which a circular gasket seat had been machined. A cork gasket which could be clamped tightly between the end of the cylinder and the gasket seat was used. A steel clamping band, as shown in figure 2, drawn tightly around the Micarta cylinder by a bolt and drawn down hard upon the cork gasket by bolts through studs welded to the band and engaging threaded holes in the steel bottom, provided means for keeping this cork-gasket joint tight. Any oil leakage which might occur, due to accident or other cause, was amply provided for by placing the test barrel in a shallow oil-tight steel receptacle, large enough to hold all the oil used. With this arrangement, requiring only eight barrels of oil, the test electrodes are freer from field distortion due to adjacent materials than would be possible with the use of containers of metal or other conducting or even semiconducting material, unless such containers were made very large. That is, the use of oil in a barrel of insulating material having about the same dielectric constant as oil, makes possible with a small amount of oil, electric gaps immersed in what, in effect, is a column of oil insulation surrounded by the atmosphere of the test room.

Calculations before construction, and tests after construction, show, for all practical purposes, that this device provides a means of having test gaps in oil completely free from field distortion due to surrounding objects.

The Herkolite cylinder serving as the lead-in bushing is closed at the lower end with a fiber and metal bottom designed to hold oil between it and a steel tubing concentric with the insulating cylinder and through which the upper and adjustable electrode extends into the test barrel. This metal part of the bottom is constructed in such a way as to provide for a well-distributed electric field around the

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[1] For all numbered references, see list at end of paper.

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periods of subnormal loads, such as Sundays and late night hours, it permits the highest degree of concentration of work. In times of abnormal loads, such as Christmas, New Year's, Easter, or special events of national interest, it expands naturally to accommodate itself to the increased burden. Under all conditions, it handles the telegrams with greater speed and accuracy than ever before, and thus has brought about significant progress in the telegraphic art.

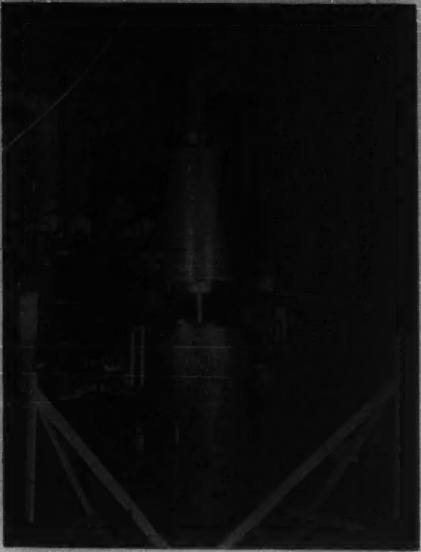


Figure 1. Test barrel and lead-in bushing showing bushing raised for examination and polishing electrodes

lower end of the bushing. The lower electrode, supported by and attached to the bottom of the barrel, was not made adjustable, but constructed so as easily to be changed or removed for cleaning and polishing. During tests the part of the lead-in bushing between outer and inner cylinders is kept filled with oil and flashover from lead-in conductor to the barrel is avoided by having its lower end project into the test barrel a few inches below the surface of the oil in the barrel. This arrangement was proved entirely satisfactory as a means of limiting all arc-overs to the test gap.

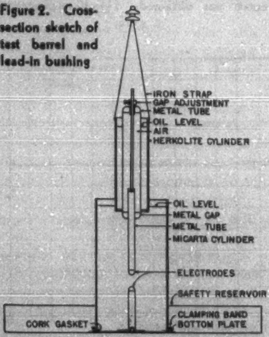


Figure 2. Cross-section sketch of test barrel and lead-in bushing

The tests reported in this paper show only the surge voltages required to break down gaps in oil formed between spherically ended cylindrical electrodes of different diameter. All oil used was carefully selected, filtered, and tested according to standard oil testing practice before starting the tests and as often during any test as was necessary to keep the dielectric strength at top value, this top value being determined by a standard oil gap test set capable of applying 30 kv to the one-tenth inch test gap formed between one-inch-diameter flat electrodes. The oil was considered in proper condition when it would not break down for a two-minute application of this voltage. To keep the oil up to this standard during a test program was not easy, filtering often being found necessary after only a few oil punctures had been made; and frequently after the puncture value of the oil had been decreased, it was necessary to put the oil through the filter several times to restore its dielectric strength to 30 kv. Whenever the dielectric strength of the oil dropped to a value of 22 kv or less, the surge voltage required to break down any given gap was reduced an amount which indicates that perhaps as further tests of this type are made, the dielectric strength of the oil during a test run should be determined after practically every oil breakdown. Also, if we wish to know the reason for some of the variable results obtained, it may be necessary to determine oil puncture strength with a precision which will show small variations in dielectric strength in the range above 30 kv, as determined with the standard test cup.

Early in our test program we found that, for consistent breakdown values, only a few discharges could be made between electrode polishings. This was more noticeable for the smaller electrodes than for the larger ones. On rare occasions only one or two discharges which, perchance, occurred from the same spot on one of the electrodes, would make necessary a repolishing of electrodes before consistent breakdown values could be obtained. In all tests it was necessary to observe a time interval of not less than 5 minutes between successive discharges, and for the longer gaps time intervals of 15 to 20 minutes were required for the oil to return to what might be called a normal state.

These influence factors, plus the all-prevailing time-lag influence upon surge-breakdown values of voltage for gaps in oil, added to those well-known difficulties encountered in obtaining similar data at power frequencies, soon made evident the fact that breakdown values for gaps in oil would have to be considered as statistical values obtained from a large number of tests, some of which would show values of breakdown voltage varying a considerable amount from any curve drawn to show the probable reasonable breakdown values. In fact, surge-voltage-breakdown values in oil seem to be even more sensitive to the influences which make testing such gaps at power frequency a major operation, than is the case for the power-frequency tests; and it may almost be said that on some days sport breakdown values occurred, for which there just seems to be no way of accounting.

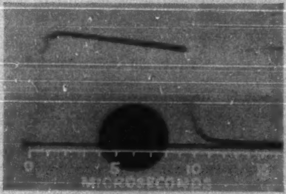


Figure 3. Typical oscillogram, showing oil puncture occurring at 860 kv crest and 9½ microseconds time lag for eight-inch gap, one-half-inch-diameter electrodes

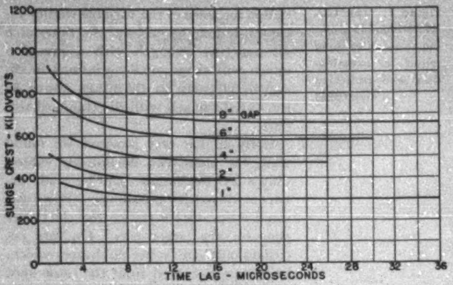


Figure 4. Time-lag curves for one-fourth-inch electrodes with hemispherical ends, 1½x40-microsecond wave

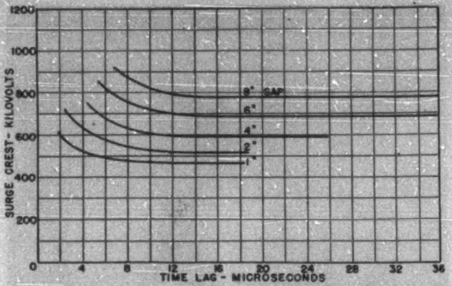


Figure 5. Time-lag curves for one-half-inch electrodes with hemispherical ends, 1½x40-microsecond wave

In making the volt-time tests, the operating procedure was as follows: The surge generator was adjusted to give a 1½x40-microsecond negative wave having a crest value well below the minimum flashover voltage required to strike across the gap under test, and for this setting a surge was applied to the gap. The voltage was then increased by small steps until a voltage was reached which would puncture the oil in the gap. When breakdown occurred, the surge voltage which first caused breakdown was applied a number of times. If breakdown occurred for every surge application, the voltage setting on the generator was reduced until breakdown stopped, after which the voltage was again raised until it reached the minimum value which would give breakdown for a majority of the applications.

As a check on this breakdown value, the surge-generator voltage would be alternately raised and lowered above and below the probable breakdown values. Often a one-step change on the surge generator (about three per cent) in the crest voltage value either way would change breakdown performance so that with a three per cent lowering of the crest voltage there would be no breakdowns at all; whereas with a three per cent increase in crest voltage practically every surge application would cause a breakdown.

Having in this way established the probable minimum breakdown voltage for a given gap setting and electrode size, the balance of the volt-time curve was obtained by applying the 1½x40-microsecond wave at increasing voltages and taking cathode-ray oscillograms of the voltage across the oil gap under test. For each surge-voltage application an oscillogram was made to show the value of the wave from the time of tripping the circuit to breakdown. Figure 3 is a typical oscillogram. In most cases breakdown occurred on the tail of the wave. The crest values of the waves thus recorded were plotted as ordinates on the curve sheets shown in figures 4, 5, 6, and 7, with the time to breakdown shown as corresponding abscissas. The curves thus obtained show minimum breakdown voltages and breakdown voltage versus time to breakdown for higher voltage values. Figure 7 shows the same curve for two-inch spacing, as shown on figure 6, but shows also the exact location of the points used in plotting the curve.

The probable breakdown curves shown in the paper are considered correct as to values given for prevailing conditions within a value of plus or minus three per cent. Occasionally, however, voltages more than ten per cent above the probable breakdown values shown in the curves would be required to cause breakdown. We think such discrepancies were due to undetermined variable conditions of oil and electrode surfaces, but up to the present time our analysis does not permit a statement as to what these conditions were.

When one of these extremely high first-breakdown voltage conditions was encountered, the test procedure outlined

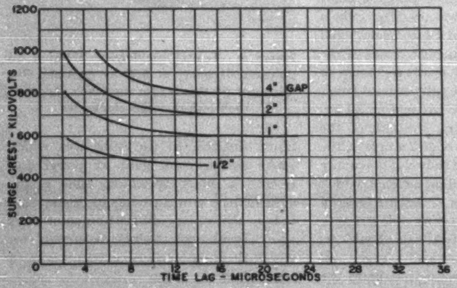


Figure 6. Time-lag curves for one-inch electrodes with hemispherical ends, 1½x40-microsecond wave

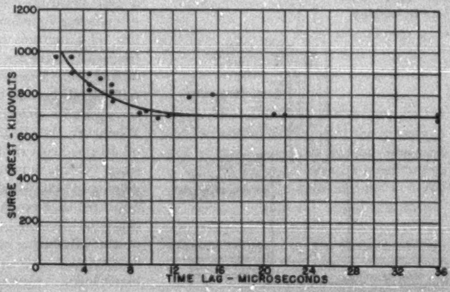


Figure 7. Time-lag curves for one-inch electrodes with hemispherical ends, showing spread of actual plotted values from which curve was obtained, 1½x40-microsecond wave

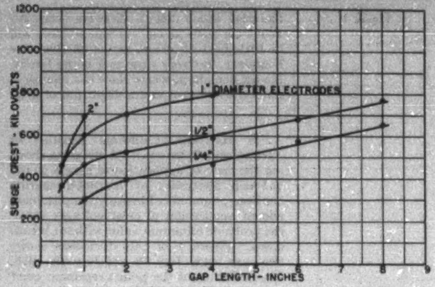


Figure 8. Curve showing relation of gap length to minimum surge crest voltage required for breakdown between cylindrical electrodes with hemispherical ends immersed in oil, 1½x40-microsecond wave

was reversed and, subsequent to the first breakdown, surges of lower crest voltage than the one causing this first breakdown, but higher than the recognized minimum breakdown, were applied. The voltages for the successive tests were gradually reduced until the probable minimum breakdown voltage for any given gap was reached. The minimum values of voltages found in this manner were in agreement with those found for the same gaps by the more commonly used increasing-voltage procedure. For the one-fourth-inch rods no excess-voltage first breakdowns were in evidence. For the one-half-inch rods this effect was indicated only, and then was limited to the longer gaps. The one-inch rods showed a very pronounced tendency to be subject to this phenomena at small spacings and for the longer gaps high breakdown points definitely occurred. The two-inch rods showed occasional high breakdown voltages for short gaps. If continued tests show any appreciable increase in the percentage of high-breakdown-voltage values, it may be necessary for the larger electrodes to indicate arc-over voltage by a broad curve showing the spread, rather than by a single-line probable-breakdown curve.

To show more explicitly what this phenomenon of high breakdown signifies, the following specific instances of such occurrence are noted.

July 11, 1938, five successive surges, all with crest voltage values more than 10 per cent above the probable minimum breakdown value applied to the one-inch gap between one-inch electrodes, failed to puncture the oil. The sixth, or puncturing surge, had a crest value 24 per cent above what appears to be the probable minimum puncture value for the gap.

July 13, 1938, with the same electrodes and same setting, five surges with crest voltages more than 10 per cent above the probable minimum breakdown voltage failed to puncture the oil. Puncture finally occurred at a crest voltage 17 per cent higher than the probable minimum.

July 14, 1938, seven surges which failed to puncture the oil had crest values more than 10 per cent above the probable minimum value applied to one-inch electrodes spaced for a two-inch gap. The breakdown finally occurred at a crest voltage 21 per cent higher than the probable minimum value for this arrangement.

Figure 8 shows data rearranged to enable one to obtain readily the relation between gap lengths and minimum puncture voltages for the gaps included in this report.

It is well to note that the limited amount of data available for design calculations as gleaned from the 1,500 surge tests made to obtain the data presented in this report point strongly to the need of continuing this work in a broader test program extended to a number of our high-voltage laboratories. A search for data pertaining to similar tests for a-c 60-cycle voltage which might be used in making comparison between 60-cycle and surge dielectric strengths of oil gaps[2] disclosed the fact that published 60-cycle data in agreement as to values for gaps formed between different types of electrodes in oil are by no means adequate for our present requirements. Although the ratio of surge crest $1^1/_2$x40-microsecond wave voltages to 60-cycle crest voltages required to puncture oil when such voltages are applied to immersed sphere gaps varies with the size of spheres and gap spacing, the present available data indicate that no great trouble will be encountered in designing apparatus if this ratio of crest surge voltage to power-frequency root-mean-square voltage is taken as three to one until sufficient data for better conclusions are available. Tests made by F. W. Peek[3] when he introduced the idea of time lag show this ratio of three to one for surges of unknown wave form as compared to 60-cycle breakdown voltages in oil.

To obtain these further data, we invite co-operation from others interested in high-voltage phenomena and recommend the use of test barrels and lead-in bushings made of insulation material having about the same dielectric constant as the insulating liquids to be tested and built according to the general plan of the apparatus described in this paper.

References

1. A Low-Voltage High-Speed Cathode-Ray Oscillograph, R. Howard Griest. Thesis for doctor of philosophy degree, California Institute of Technology, 1937.

2. Oil Breakdown at Large Spacings, Douglas F. Miner. AIEE Transactions, 1927, page 248.

3. Dielectric Phenomena in High-Voltage Engineering (a book), Peek. McGraw-Hill Book Company, 1929, page 227.

Discussion

D. C. Prince (General Electric Company, Philadelphia, Pa.): This paper adds valuable information to our store of knowledge on the behavior of insulating oils. One point of particular value to circuit-breaker designers is brought out very clearly. Under all the conditions investigated the oil presents an impulse ratio of from 2 to 3. Ordinary air clearances, rod gaps, bushings, and the like, associated with circuit breakers, have impulse ratios of the order of 1.2. It follows that failure of the oil under impulse is not limiting, a fact of obvious value to designers.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): Professor Sorensen has given us some very interesting impulse data on the breakdown of oil. Apparently, by exercising great care, he has been able to eliminate the wide variations in oil-breakdown values that usually occur in ordinary methods of testing.

There are, however, two or three points I wish to comment on.

The author apparently used hemispherical-shaped electrodes to simulate conditions in circuit breakers. The variation of the breakdown voltage with spacing does not seem to follow any law that I have ever observed. For example, his figure 8 shows that for the one-fourth-inch and one-half-inch-diameter electrodes the breakdown voltage varies roughly as the spacing raised to the 0.37 power. For one-inch-diameter electrodes the exponential value of the spacing is even less than 0.37 and bends over, indicating that considerably greater spacings than four inches would give very little increase in breakdown values.

Generally when spheres are used with spacings several times their diameter they act like sharp-cornered electrodes, so far as breakdown versus spacing is concerned. This is especially true when air is the insulating medium.

We have a large amount of data on breakdown of oil for square-edged and round

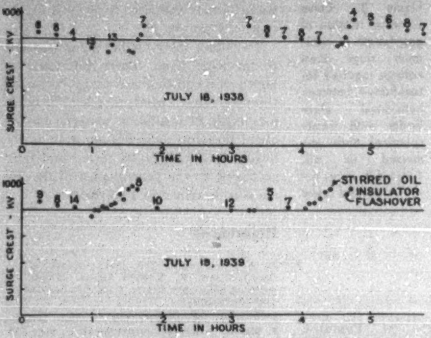


Figure 1

rods (both parallel and at right angles) and in all cases the breakdown is roughly proportional to the spacing raised to the two-third power. It appears from Professor Sorensen's data that for limited spacings—up to six or eight inches—and with small diameter electrodes, the breakdown voltage increases quite slowly with spacing. It would be interesting to know if circuit-breaker engineers find that this is true for the kind of electrodes used in circuit breakers, or do they find that the breakdown strength varies roughly as the spacing raised to the two-third power.

Eric A. Walker (Tufts College, Medford, Mass.): The paper by Professor Sorensen on the "Surge-Voltage Breakdown Characteristics for Electrical Gaps in Oil" is of considerable interest to me because it is very similar in nature to a study which has been started at Tufts College.

In making surge breakdown tests I have felt that one of the most important factors in determining the breakdown strength of the oil is the amount of moisture the oil contains; a factor which is not mentioned by Professor Sorensen, except to say that the oil was periodically filtered.

Oil in contact with air undoubtedly contains water in two forms: water in solution and water suspended as small droplets which gradually settle out and collect as free moisture at the bottom of the tank. The balance between the two types is controlled by the temperature. As the oil cools less water is held in solution, and more in suspension. I do not believe that moisture in one form will have the same effect as moisture in the other, so I feel that before consistent results can be obtained the total amount of moisture the oil contains and the temperature must be controlled and recorded.

If oil contains moisture in suspension it appears difficult to reproduce results because each successive shot draws more moisture into the gap. The reason is self-evident. Water has a higher dielectric constant than oil and so is impelled into the region where the electrical stress is greatest.

In our tests a number of sludged samples of oil were found, which had relatively high breakdown strengths when tested on a short chopped wave; and relatively low strengths when tested at 60 cycles in the standard gap. The power factor of these sludged samples was high.

I wonder if Professor Sorensen does not emphasize too much the advantages of constructing a tank of Herkolite or some other material which has a dielectric constant about the same as oil. It is true there will be little field distortion at the oil-Herkolite boundary, but very close to it is the Herkolite-air boundary, where there is a change of dielectric constant and a distortion of field.

A. E. Harrison (California Institute of Technology, Pasadena): A diagram showing the time of application of surges to the one-inch electrodes at four-inch spacing has been prepared to illustrate the phenomenon of application of crest voltages above the probable minimum value without breakdown. Figure 1 of this discussion shows two runs made July 18 and July 19, 1938. The ordinates of the points represent crest voltages, while the time in hours gives the intervals of time between application of surges. The figures above certain points give the breakdown time in microseconds. No breakdown occurred for surges indicated by open circles.

I interpret these data as follows: Application of surge voltage less than that required for breakdown changes the condition of the electrode surface, and/or the condition of the oil so that the gap will not break down when the probable minimum value of crest voltage is exceeded. The influence of operating procedure has not been established, although recent evidence points to the conclusion that the stirring of the oil after breakdown has occurred may account for this behavior. Stirring the oil when breakdown had not occurred, and waiting 15 minutes (the next to last point on July 19 and other similar tests not illustrated) would not cause breakdown to occur, however. Once breakdown has occurred at a high voltage, breakdown will then occur at the probable minimum voltage. (Note point at time two hours on July 19.)

This unusual behavior was quite consistent on days when it occurred, but there were times when the phenomenon failed to occur for no apparent reason. Also, this phenomenon did not seem to occur when the oil around the gap was not stirred after a breakdown, although failure to stir the oil seemed to reduce the strength of the oil in

Figure 2. Time-lag curves for one-inch electrodes with hemispherical ends, 1½ x 40-microsecond negative impulse wave

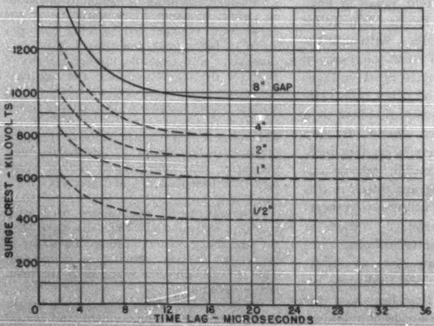
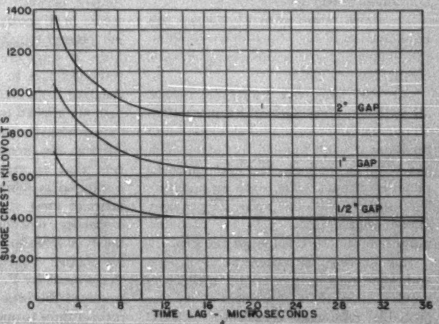


Figure 3. Time-lag curves for two-inch electrodes with hemispherical ends, 1½ x 40-microsecond negative impulse wave



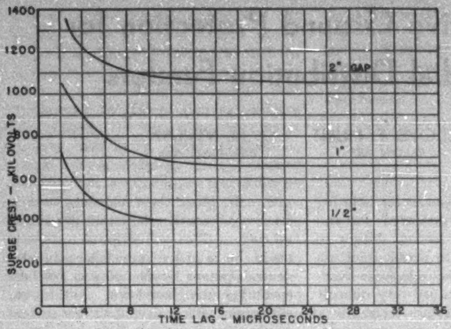


Figure 4. Time-lag curves for four-inch electrodes with hemispherical ends, 1½ x 40-microsecond negative impulse wave

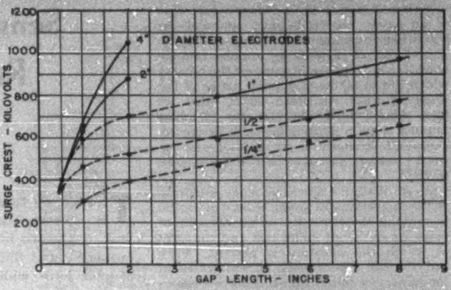


Figure 5. Curves showing relation of gap length to minimum surge crest voltage required for breakdown between cylindrical electrodes one-fourth-inch, one-half-inch, one-inch, two-inch, and four-inch diameter, with hemispherical ends, the gaps being immersed in transformer oil and subjected to 1½ x 40-microsecond negative impulse waves

some cases. We would be interested in hearing possible explanations of this unusual behavior.

A. Lovoff (California Institute of Technology, Pasadena): I would like to present additional data on the breakdown characteristics of oil, obtained in the high-voltage laboratory of the California Institute of Technology subsequent to the writing of Doctor Sorensen's paper.

Figures 2, 3, and 5 of this discussion show an extension of the data for the one- and two-inch electrodes and figures 4 and 5 show data for the four-inch electrode not included in the paper. The dotted curves are the same as those presented by Doctor Sorensen in his paper and are reproduced in this discussion for the sake of completeness. The curves of figure 5 and a curve for point electrodes if all plotted on one log-log plot will show for the larger gap spacings the manner in which the curves for the various-diameter electrodes approach the needle-gap curve. Such a log-log plot will be included in a paper now in preparation, hence will not be given in this discussion.

Recent preliminary power-frequency-voltage breakdown tests on these electrodes to determine the one-minute hold voltage disclose that the ratio of minimum surge breakdown voltage to the one-minute-hold power-frequency voltage (crest) is approximately 2.2. This impulse ratio of 2.2 checks with the figure given by Doctor Sorensen.

The anomaly of the application of greater than the established minimum breakdown surge voltages without oil breakdown occurring, as discussed by A. E. Harrison, may possibly be explained as follows. After the oil has been broken down in the gap and the oil stirred, a high enough concentration of carbon particles might remain in the vicinity of the gap to distort the field in such a way as to increase the breakdown voltage. I have noticed this failure-to-breakdown phenomena at higher than the established minimum surge voltage when testing the one-inch electrodes at the four- and eight-inch spacings. In that case the oil after being subject to several successive shots at higher than the probable minimum breakdown surge voltage without a resulting oil breakdown was restored to a normal breakdown status by proceeding as follows: the oil was very thoroughly stirred and after a ten-minute wait subjected to a surge voltage just above the probable minimum, when it was found to resume a normal performance. Three such instances noted in my records tend to substantiate the contention that foreign matter introduced during breakdown is the cause of this no-breakdown phenomena at voltage values higher than the probable minimum surge breakdown voltage.

Royal W. Sorensen: Mr. Montsinger's comments are valuable additions to the bit of information regarding surge-voltage performance for electrical conductors in oil included in this paper, which we hope has initiated a program of testing that will give us a more extensive knowledge of surge voltage performance.

All of the statements in our papers do not seem to convey to Mr. Montsinger the same meaning as that which we intended. Our reason for using rods with hemispherical ends as electrodes was not to simulate the conductors in circuit breakers, because circuit breakers do not use electrodes of that shape. The electrode shape we used was chosen because the sphere gap is a standard method of making voltage measurements. Spheres in oil with supporting shanks of small diameter, however, are more susceptible to shank influences than are similar spheres in air. Hence to avoid possible variations in arc-over voltage due to differences in shank sizes which might be used, rod electrodes with the ends rounded off as hemispheres were used.

Subsequent to the writing of the paper under discussion, we have made tests which have given us much data not available for the paper. Some of these additional data are recorded in the curves of figure 5 of Mr. Lovoff's discussion. It is interesting to note that the extended curves including these additional data are satisfactorily in accord with the curves of the paper's figure 8, to which Mr. Montsinger has referred. It is true that for the larger spacings, as Mr. Montsinger has pointed out, not only the one-inch-diameter electrode, but also the other electrodes show smaller increases in voltage breakdown values as the spacings are increased. When, however, we can obtain a high enough voltage to get out on the straight-line parts of the curves, as has been done for the one-fourth-, one-half-, and one-inch electrodes, it is interesting to note that the curves are approximately parallel when plotted on rectangular co-ordinate paper, as is the case for figure 8 of the paper under discussion, and figure 4 of Mr. Lovoff's discussion.

Mr. Montsinger states that "generally when spheres are used with spacings several times their diameter, they act like sharp-cornered electrodes so far as breakdown versus spacing is concerned." Some of our data, not yet published, bear this out, because for all electrodes when the spacing is sufficiently increased, we find that the curves of breakdown voltage versus gap length for the electrodes approach the point-gap curve.

Mr. Montsinger calls attention to the fact that he has available "a large amount of data on breakdown of oil for square-edged and round rods (both parallel and at right angles) and (that) in all cases the breakdown is roughly proportional to the spacing raised to the two-third power." I presume he refers specifically to surge-voltage breakdown. I would be interested in seeing these data, not only to note what the surge breakdown values are, but also to note whether parallel and crossed rods for surge voltages have (as is the case with power-frequency voltages) the same breakdown values for parallel and crossed rods, a fact long ago noted in Schwaiger's "Theory of Dielectrics."

Mr. Montsinger also states that for limited spacings up to six or eight inches the voltage increases slowly with spacing. Also an inspection of the curves shows that with such spacings tests on electrodes having diameters of one inch or less give for these spacings curves that are approximately straight lines. I cannot, however, give any information as to what circuit-breaker engineers find for the kind of electrodes used in circuit breakers because I have not seen any published data for such tests. The reason for the small amount of such data available is perhaps found in D. C. Prince's discussion of our paper, wherein he says "under all the conditions investigated (and reported in the paper) the oil presents an impulse ratio of from 2 to 3, whereas the parts of the breaker which are in air have impulse ratios of the order of 1.2." In other words, what we have done is verify the fact that oil under impulse is not a limiting factor in design because oil strength under normal and recovery voltage has taken care of the impulse factor.

E. A. Walker's discussion calls attention to the importance of giving careful attention in making these studies to the amount of moisture in the oil under test and the form in which the moisture is present. Very early in our experiments, as was stated in the paper, we found that breakdown values were very sensitive to the oil moisture content. So far we have simply avoided most of the difficulties of variability due to this moisture content by using only oil of high dielectric test; for example, oil which would stand 30,000 to 40,000 volts when tested with a standard 0.1-inch oil testing gap. In fact, as Mr. Walker suggests, there is no doubt a fertile field for investigation to determine the exact quantitative influence of water content and also of temperature on surge-voltage-breakdown values for oil.

I do not think, as Mr. Walker has stated, that we have at all overemphasized the advantages of using a test tank of material which has a dielectric constant approximately the same as that of oil, because after all the prime reason for the use of a tank made of insulation material rather than metal is the avoidance of the difficult lead-in bushing problem and the use of the large amount of oil required when steel tanks are used. The fact that the dielectric constant of the insulation material used is so nearly the same value as the constant for transformer oil and that the dielectric constants for both the oil and the insulation material is only two to three times the constant for air, is a happy coincidence which makes our plan practical; whereas it would not be so good if we had to use a barrel made of some material such as porcelain, which has a much higher dielectric constant than the material of which our test barrel is made.

At present I have no explanation for the phenomena described in Mr. Harrison's discussion and can only confine my remarks to expressing appreciation of the large amount of careful, conscientious work which he has done in obtaining for me much of the data used in this paper.

Mr. Lovoff also has added to the paper data which he has obtained and which extend the curves used in the paper to values beyond those available at the time the paper was written. To him and to other students who have helped in this work, I am deeply indebted, for without their help the large amount of testing necessary to produce this report could not have been obtained. In fact, like most research work of today, the results presented in this paper are available only because of much co-operation by many persons and it would certainly be unfair to close our discussion without expressing appreciation to the Kelman Electric and Manufacturing Company, which provided much of the equipment used in making the tests, and to Messrs. Kelman, Sadler, and Thompson, who contributed valuable suggestions.

Sensitive Ground Protection for Radial Distribution Feeders

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FELLOW AIEE

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ASSOCIATE AIEE

THERE are three reasons for isolating or de-energizing grounded feeders: first, danger to life; second, danger to property; and third, to permit joint use of pole lines with the telephone companies. The first two are very difficult to evaluate, but in certain territories a means of isolation is very important. The third reason can be given a definite value, and the economies resulting from joint use of pole lines can more than justify this proposed scheme of sensitive ground protection for radial feeders.

This paper will not discuss the merits of leaving a ground-faulted feeder in service for the continuity of service, as may be the practice by some utilities, but discusses the methods of clearing ground faults that may normally occur on radial feeders.

Ground Faults

The magnitude of ground currents on lower-voltage feeders with solidly grounded neutral transformers varies more according to the contact resistance of the fault than it does for the location of the fault. This effect is greater, the lower the voltage of the circuit.[1] The usual voltages used for radial feeders range from 2.3 kv to 22 kv.

Many ground tests have been made on 2.3-, 4-, 7-, 11-, and 16.5-kv systems of the Southern California Edison Company, Ltd. These tests all have shown conclusively that the contact resistance has a controlling influence on the magnitude of ground current.

Tests on a 4-kv solidly grounded neutral circuit show very small amounts of current when bare wire is ground-faulted as follows: In dust 0.3 to 0.6 amperes, in grass 1 to 3 amperes, in muddy water 25 to 30 amperes. Other tests on 4 kv on dry pavement and dry sand soil, where there had been no rain for several months, show that practically no current flows. A series of tests were made on a 7-kv impedance-grounded neutral system by dropping a 2/0 bare conductor on a green lawn. In this case, with about 20 feet in contact, the current was in the order of 2 to 3 amperes during the few minutes of each test. In another test, with a ground connected to a five-gallon can in mud, 35 amperes flowed in an 11-kv solidly grounded neutral circuit. Tests were made on 16.5-kv solidly grounded neutral circuit by dropping a partial span in a dry stubble field. It was necessary for the conductor to touch a green weed or come in contact with a conducting portion of earth before current would flow. Some of the times the conductor would lie in this stubble field as if dead; then all of a sudden a breakdown to earth would occur which was sufficient to cause current to flow. From this particular group of tests it seemed as though at certain times, when the conductor was dropped, it would break down,

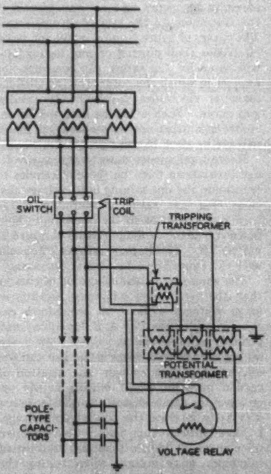

Figure 1. Sensitive ground protection for single-circuit delta system

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Paper 39-131, recommended by the AIEE committee on protective devices, and presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939. Manuscript submitted April 14, 1939; made available for preprinting May 26, 1939.

LLOYD F. HUNT is protection engineer and J. H. VIVIAN is technical assistant and superintendent of relays, Southern California Edison Company, Ltd., Los Angeles.

1. For numbered reference, see end of paper.

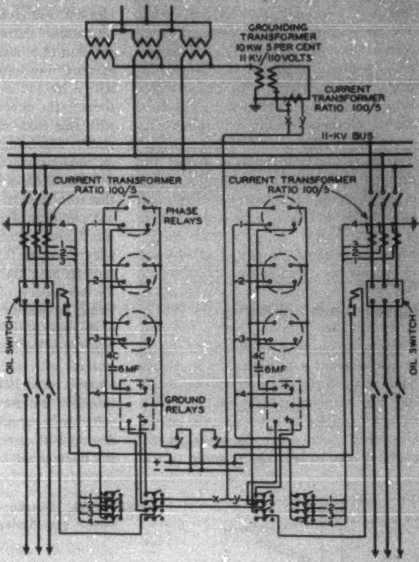
GROUNDING TRANSFORMER 10 KW 5 PER CENT 11 KV/110 VOLTS
CURRENT TRANSFORMER RATIO 100/5
X Y
11-KV BUS
CURRENT TRANSFORMER RATIO 100/5
PHASE RELAYS
CURRENT TRANSFORMER RATIO 100/5
1
2
3
4
4C
6MF
GROUND RELAYS
OIL SWITCH
OIL SWITCH
x
y

Figure 3. Sensitive ground protection for three-phase three-wire system supplying two or more feeders

and at other times it would not. It seemed to be a critical voltage where, when in contact with earth, a breakdown would occur. It can be conceived that there is a breakdown phenomenon that is governed by voltage, current, and size of conductor, or area of actual contact surface with earth. That is, there is, under a given condition, with a conductor on dry earth, a value where practically no current flows, but by raising the voltage to a critical value, a breakdown occurs, causing current to flow. There may be a variation of this point, but it will be safe to say that the lower the voltage, the more often the breakdown will fail to occur, and the higher the voltage, the more often the breakdown will occur.

From past experience, this critical voltage under normal operating conditions seems to be between 11 kv and 22 kv. Of course, there are exceptions both ways. In other words, there are times when an 11-kv circuit breaks down, and there have been times when a 33-kv line has apparently failed to break down.

This subject of breakdown is of sufficient importance to warrant some research so that engineers, as a whole, would have a common understanding of earth faults.

With this study in mind, showing that it is possible to have earth faults that have practically no current and solid ground short circuits that have many thousand amperes, a method was sought for relaying the small ground faults, and at the same time means for limiting the maximum ground current to a desirable value.

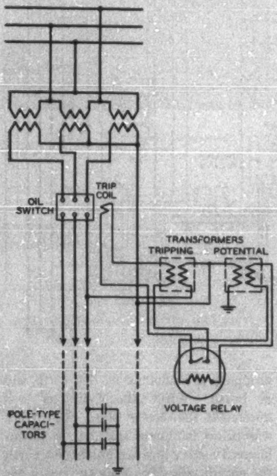

Figure 2. Sensitive ground protection for single-circuit four-wire star system

In some districts where a small section is connected to a single circuit, and it is desired to clear this circuit on light ground faults, a simple scheme was devised by using an ungrounded system, and tripping was accomplished by using a voltage relay that measured zero-sequence voltage. This was tried on a 2.3-kv delta circuit approximately four miles long and proved very sensitive, using a voltage relay set at 30 volts minimum trip. In fact, when a conductor was dropped on earth or came in contact with a blade of grass, the circuit would clear without showing any signs of arcing. This scheme was too sensitive to be practical because it started tripping-out nearly every time a fog came in. In order to prevent these false operations, it was necessary to make the installation less sensitive. This was done by making the relay less sensitive; that is, giving it a higher voltage minimum trip setting, but still the operation was not satisfactory, as the leakage current from the fog was as great as the charging current from the conductors. On these circuits the smallest-size pole-type capacitors (5 kva, 2,300 volts, 60 cycles) were added at the end of the three-phase line. These three capacitors, each connected between one phase wire and ground, are sufficient to stabilize the neutral or ground point under normal conditions. By placing the capacitors at the end of the line, an added feature was accomplished; that being open-circuit protection. Should any phase wire open between the transformer bank and capacitor, the zero-sequence voltage would be sufficient to operate the relay.

Figure 1 gives a typical connection for this scheme. With the relay set at 30 volts, the grounded feeder would have to cause about 0.5 ampere to flow; so, with this arrangement, the minimum trip would occur with approximately a 2,000-ohm fault.

Figure 2 gives a method of obtaining this protection on a four-wire system. In this case, all the grounds must be kept off the neutral wire. The minimum trip of the relay is approximately 20 volts.

In these two schemes, the use of the capacitors gives the circuit a small amount of reactive kilovolt-amperes, which is generally beneficial.

If the above scheme is used when two or more feeders are supplied from a bus, there is no method of giving feeder selection so it must be arranged to trip all feeders. If selection is required, it may be accomplished by the following method:

Figure 3 is a scheme for a three-phase three-wire system supplying two feeders that will trip only the faulted feeder. The ground relay is a standard overcurrent directional type. The overcurrent elements of all ground relays are supplied from a grounding transformer. With a ground occurring on this system, heavy currents circulate through the secondary of the grounding transformer. This gives an abundance of energy for supplying the high volt-ampere burdens of the overcurrent elements of directional relays. So, in case of a grounded feeder, this transformer supplies this energy while the residual current is called upon to operate the directional contact. Since the relay is directional controlled, as shown in figure 4, it is necessary for the directional contacts to close before the overload can operate. Referring to figure 4, when current is flowing from the ground bank through the relay coil *a*, no current can flow through *b*, although it is magnetically coupled to *a*, because the circuit is opened by contacts *c;* however, should contacts *c* be closed, which would be the case should the ground fault be on the line to which this relay is connected, then with contacts *c* closed, induced voltage from coil *b* would circulate through coil *d*. This causes rotating flux on disks *e*, closing tripping contacts *f*.

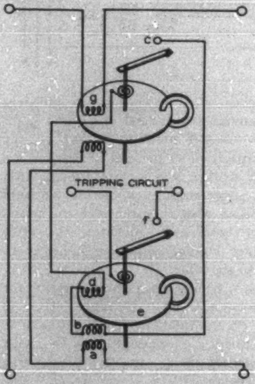

Figure 4 (left). Direction-control ground relay

The relay used is a standard power-directional relay, and the small residual current from the line current transformers is applied to the potential coil of the directional element. A six-microfarad capacitor placed in series with the coil *g* resonates at 50 cycles and reduces the impedance of the circuit, so that the directional element will operate on a very small residual current.

In fact, from our tests, the directional element will give positive closing torque on primary currents much less than one per cent of primary rating of wound-type current transformers. This means that with a 100 to 5 wound-type current transformer, the directional element will close with less than one ampere primary current. This being the result of many tests on various voltage circuits, gave us a definite factor of safety by using the rule that minimum trip of the relays should be one per cent of the wound-type current transformer rating. In order to get the relay to trip on this value, it will be necessary to have the current transformer in the secondary of the grounding transformer of such a ratio as to give 5 amperes in the normal relay with 4- to 15-ampere taps set on the 5-ampere tap. Then, on a system with the feeder current transformers rated 100/5 and a standard distribution transformer rated 11 kv to 110 volts for the grounding bank, the current flowing in the secondary of the grounding transformer will be 100 amperes with one ampere ground. With the 100 amperes, it will be necessary to use a 100/5 current transformer in this location, as shown. With one-ampere primary-current minimum trip, a maximum current of approximately 10 amperes would make a good ratio for relaying. By neglecting the impedance of the power bank and system, it is easy to figure out the size of the grounding transformer. If we use a standard 10-kw 11-kv transformer with an impedance of five per cent, the maximum current flowing to a solid ground at the station will be 10.5 amperes. It is not essential to get exactly this value, and when a four per cent transformer is used, the maximum ground current will be approximately 13 amperes.

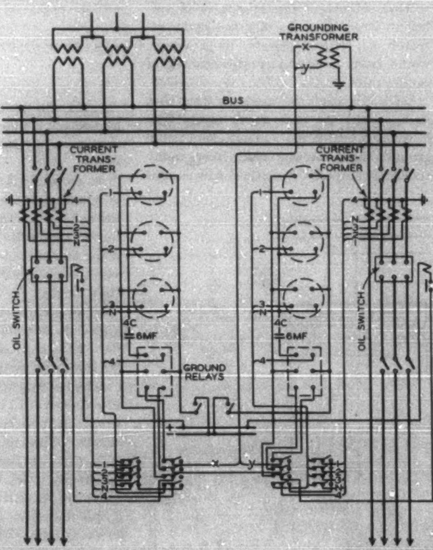

Figure 5. Sensitive ground protection for three-phase four-wire system supplying two or more feeders

If the power transformer bank has a delta connection, it will be necessary to use three transformers connected star, 11 kv neutral grounded, with the secondaries connected in delta, and proper current transformer used to give the one-ampere minimum trip, and the size and impedance to give the maximum ground current of approximately ten amperes. This one-ampere minimum trip represents a fault with approximately 5,700 ohms. At our Goldtown substation, the connection is the same as figure 3, except there are three feeders instead of two. After the installation, tests were made by dropping a conductor on dry earth and in brush. When the conductor was lowered in the tops of brush, the current was below minimum trip, but as it was lowered further into the brush, near earth the circuit relayed. Then the conductor was dropped into a small tree, but was kept clear of earth. In this case no relay action took place. The conductor was then dropped on earth in many places, either slowly or suddenly, and in every case the circuit relayed correctly. The values of ground current ranged from 2 to 8 amperes. The conductor was then solidly grounded, and the maximum current was approximately 13 amperes. The grounding transformer used at Goldtown is a 10 kw, four per cent.

Figure 5 shows the connections of a three-phase four-wire system. Since the load may be connected between one phase wire and neutral, it is very likely to have an unbalanced load with the neutral carrying current. For this sensitive ground-current system to function properly, it is necessary to have four current transformers and to have the neutral wire ungrounded and not interconnected. In this case, the residual of the four current transformers is a true measure of phase-to-ground current.

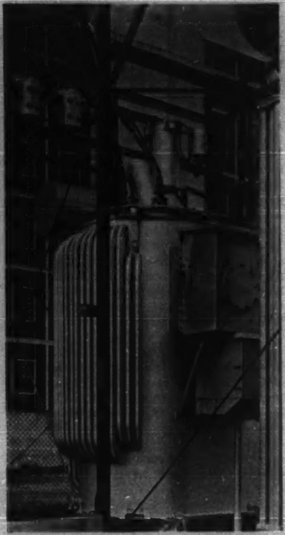

Figure 6. Grounding transformer

Figure 5 is similar to a four-wire four-kilovolt system installed at Topango substation. This installation was given a complete test by grounding feeders. The minimum trip was slightly less than one ampere. The bare conductor was pulled over trees with no relay operation, showing tree grounds would not cause unnecessary operations. The conductor was dropped on various kinds of soil, and relayed in most all cases except when dropped on very dry soil and on oiled pavement. Several cases where the low-voltage ohmmeter showed very high fault resistance, the circuit relayed when energized. One of these cases was a heavy retaining fence with several concrete poles connected together with several strands of heavy steel cable. The steel was not grounded, but was tied together. The low-voltage ohmmeter gave a resistance to ground for this installation of 40,000 ohms, yet when contacted by a conductor, the circuit relayed properly. This test confirms there must be some phenomenon of breakdown, as the minimum trip of this circuit was in the order of 2,000 ohms.

At Vernon City plant, there are 20 seven-kilovolt circuits equipped with this sensitive ground protection. This sensitive ground protection was installed on account of the desirability of joint use of pole lines with the telephone company. The normal minimum trip of each feeder was set at 600 amperes and with the possibility of several thousand amperes maximum ground fault current. With the sensitive ground protection in with 400/5 wound-type current transformers, the minimum trip is 4 amperes, and the maximum ground fault current is 40 amperes. Figure 6 shows the 20-kw, four per cent transformer used for the grounding transformer at the Vernon City plant. Figure 7 shows the installation of eight sets of feeder relays at this station; the four upper relays are the ground relays, while the corresponding three-phase overcurrent relays are directly below.

Line-dropping tests of this installation proved to be as designed; that is, the directional element gave definite operating torque on only the faulted feeder at about one-half minimum trip of four amperes, with a maximum of 38 amperes ground current.

This system of ground protection has been in operation for several years, and no unnecessary trip-outs have occurred, and many necessary trip-outs have occurred correctly. When phase-to-ground faults occur on this system, the customary voltage drop does not occur, due to the small amount of ground current flowing.

In fact, this system has proved so successful that several other installations are being made this year. Also, a system will be worked out using this principle for isolation of loop feeders during the year. At present there seems to be no means of adequately isolating phase-to-ground faults on distribution loops or interconnected systems. So, it is hoped that this system will prove as satisfactory as the one for radial feeders.

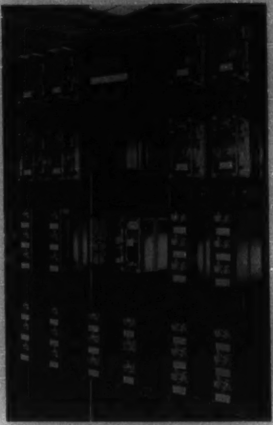

Figure 7. Ground-relay installation

Reference

1. Some Recent Relay Developments, Lloyd F. Hunt and A. A. Kroneberg. Electrical Engineering (AIEE Transactions), April, 1934.

Discussion

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): For many years the detection of high-impedance grounds occurring on relatively low-voltage circuits has been one of the unsolved problems. Some have thought to avoid the problem by the use of an isolated-neutral system. Messrs. Hunt and Vivian have pointed out that continued operation of an accidentally grounded circuit may constitute a danger to life and property. The authors' approach to the problem has been to provide a grounded-neutral system having relatively high neutral impedance. With such a system the effect of resistance at the point of fault is decreased and the range of current over which the protective relays must operate is considerably narrowed down. If the working range is relatively small, it will be easier to provide a protective relay system which may be more generally relied upon to detect faults. In general, it may be said that the authors are proposing a system having a maximum zero-sequence current of the general order of ten per cent of the circuit rating. With this system they propose a relay sensitivity of one per cent. Such an operating range of the order of ten to one is not particularly difficult to obtain with existing relay designs. The greatest difficulty lies in obtaining a relay which will actually operate at one per cent of the circuit-rating primary values. The circuit arrangement used by the authors obtains the main energy required for the operation of the relay system not from the line current transformers but from the current transformers carrying only the ground current. In this way the difficulty of obtaining very sensitive operation is overcome.

In other words, the authors are using standard equipment with impedances coordinated with the circuit rating and a modified system of relay connections. They have furthermore differentiated between the various types of circuits used for distribution service and apply solutions all of the same general class, but specifically adopted to each type of service.

In the early part of the paper under the heading "Ground Faults," the authors quote figures on various tests which were made under grounding conditions where relatively small currents were measured. The value of these test figures would be much greater if they were presented together with the actual constants of the circuit on which the tests were made. The general statements as to the amount of current which might flow through various types of soil would be of much greater value if related directly to maximum currents which would be encountered with a fault having zero resistance. More data of this type, together with a résumé of operating experience as times goes on, would be greatly appreciated by all who are interested in the solution of this particular problem.

Wm. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): In view of the tremendous increase in common-neutral distribution construction during the past six or eight years, it is unfortunate that the various methods of sensitive ground protection described by the authors are not recommended except for delta circuits or four-wire circuits with individual neutral wires grounded at the supply substation only. Many schemes have been available for years for securing sensitive ground protection on delta or unigrounded systems. However, the choice of sensitivity is practically always a compromise between conflicting requirements so that in the vast majority of installations, relay settings are less sensitive than the minimum which is readily available with standard equipment.

The authors are to be congratulated on the ingenious use of distribution transformers and current transformers, which are normally carried in stock, as a makeshift arrangement for securing a neutral reactor of some 600 ohms and a very low ratio or even step-up current transformer. If, after further experience, they should decide that they wish to maintain the extreme sensitivity and rather low upper limit of fault current, it might be well to investigate the cost of simple neutral reactors and step-up current transformers which might be less than that of the distribution transformer combination, even though the latter is a quantity-production unit.

Many methods are generally available for securing sensitivity on the order of one ampere primary as recommended by the authors. For single circuits, a standard 5/5-ampere neutral-current transformer may be used with a standard 0.5- to 2.5-ampere relay. For a simple residual connection, standard relays are available with a minimum rating of 0.1 ampere with which a pick-up of one ampere primary can be secured with line current transformers up to 50/5 amperes which would ordinarily be associated with a 1,000-kva circuit at 11 kv. Another common method is the use of a current-polarized directional ground relay having most of its volt-ampere burden concentrated in the coil supplied from the neutral current transformer, permitting low current settings even on circuits above 15 kv where bushing current transformers are frequently used. With instrument-type current transformers, the low burden of such a relay permits the use of a step-up current transformer for the current supplied from the residual of three line current transformers, allowing most any desired sensitivity to be obtained with standard relays.

Where special considerations may warrant extreme sensitivity, an arrangement was described in 1933[1] for responding to grounds with as much as 5,000 ohms resistance on a 2.3-kv three-phase circuit. The measured values in a variety of conditions at this voltage varied from 60 ohms to 3,300 ohms at point of contact.

The use of neutral impedance in limiting ground current has been widely discussed.[2] It is felt that the maximum value of ten amperes recommended by the authors is far too low for the usual conditions to give any appreciable consideration to the selectivity of branched fuses, individual distribution transformer fuses, or the ability to clear defective lightning arresters from the line or even to cause sufficient damage on them (or on other defective apparatus) to permit locating and clearing permanent trouble in any reasonable length of time. With these considerations in mind, the maximum current should probably be not less than 50 to 100 amperes.

Undoubtedly the schemes of the authors are useful in combating local conditions. They are faced with unfortunate California weather conditions, such as extreme drought resulting in heavy layers of dust contrasted with the occurrence of fogs sufficiently dense to short circuit the charging current on one conductor of a circuit.

REFERENCES

1. ZERO-SEQUENCE CURRENT TRANSFORMERS FOR GROUND PROTECTION, J. W. Graff. *Electrical World*, December 23, 1933, page 82.

2. GENERAL CONSIDERATIONS IN THE USE OF NEUTRAL IMPEDANCE, EEI Publication No. D-14.

P. E. Benner (General Electric Company, Schenectady, N. Y.): The authors are to be congratulated on their analysis and solution of the problem of de-energizing of grounded conductors. With the stretching out of circuits to pick up scattered loads, situations involving this problem of fault protection are apt to turn up on any system. There is also an increasing appreciation of the necessity for providing a means of de-energizing grounded or faulted conductors to minimize possible danger to life and property.

The common-neutral multigrounded system depends on short-circuit currents for de-energizing grounded conductors and in this way is dependent to a large degree on low ground impedance for successful operation. Unfortunately, as the authors point out, the ground and contact resistance may be almost anything with the result that de-energizing grounded feeders is somewhat uncertain with this type of protection.

In a common-neutral multigrounded system where sufficient ground current is obtained in the case of grounded conductor to cause the operation of a sectionalizing device or the substation breaker, this method of protection might possibly have the advantage of confining the outage to a fewer number of customers; however, it is doubtful whether this advantage would offset the more positive operation of the protective scheme described by the authors. In this connection it probably would not be entirely unexpected if the de-energizing of grounded feeders assumed a greater importance in future layouts than first cost or service reliability because of the fact, as mentioned by the authors, that it involves the question of danger to life and property.

In urban areas where water-pipe grounds are available, the common-neutral multigrounded systems would probably be entirely effective in isolating grounded feeders. Just how effective the multigrounded common-neutral system is for isolating grounded feeders in rural areas will unquestionably be determined in the next few years by the experience with the many miles of this type of circuit which have been recently installed. In any event, it is believed it will be advisable to keep a close check on this phase of circuit operation so that proper value can be placed on this question of providing for the de-energizing of grounded feeders on future circuits.

Frederick C. Lindvall (California Institute of Technology, Pasadena): The need for sensitive ground protection is brought out by Messrs. Hunt and Vivian through examples of peculiar grounding conditions experienced on the Southern California Edison system. In these examples the ground current either failed to flow altogether, or else was of much smaller magnitude than would have been predicted on the basis of ordinary resistivity measurements.

Verification of these experiences has been made at the California Institute of Technology both with samples of earth brought into the laboratory for study and with actual high-voltage grounds in soil outside the high-voltage laboratory.

It was found that a wire or small electrode lying on the surface of moderately dry ground could be held at a potential of 10,000 to 15,000 volts without breakdown or flow of appreciable ground current. Actual ground current flow was found to be the result of a breakdown of the soil quite similar to that of dielectric failure. Broadly summarized, the results indicate that dry to moderately dry soil should be regarded more nearly as a poor dielectric material than as a substance of definite resistivity. The laboratory studies indicated that moisture content of the soil, its composition, and the mode of contact of the electrode all have a bearing on the subsequent breakdown and flow of ground current. The wide variation of the results points to the desirability of field studies comparable with actual operating conditions rather than an intensive laboratory study of the dielectric behavior of soils, although such investigation would offer results of considerable academic interest.

Our conclusion is that a careful study of grounding conditions should be made, as the results of Hunt's and Vivian's work, together with the laboratory studies which we have made, indicate considerable uncertainty in the technique of grounds, both deliberate and accidental.

J. R. North (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper describes a unique method of obtaining sensitive ground relaying on three-phase delta (ungrounded) circuits or three-phase unigrounded circuits. This method of very sensitive relaying is no doubt advantageous, as described by the authors, in areas where the fault energy and duration must be held to extremely low values.

In the introduction, it is stated that one of the reasons for providing ground relaying on distribution circuits is "to permit joint use of pole lines with the telephone companies." It is difficult to understand just why such sensitive ground relaying is required for joint use with low-voltage distribution circuits, since there are many thousands of miles of jointly used pole lines throughout the country now operating apparently satisfactorily with distribution circuits which are not provided with ground relaying.

In the case of high-voltage joint use, detailed studies have indicated that the conditions as far as the power circuits are concerned would be properly met if the circuits are promptly de-energized in, say, five seconds or less with fault currents of the order of 50 amperes or more. Contacts with communication circuits generally result in values of fault impedance far less than in the case of a contact with dry dust and thus ample current would be available for operating ground relays or blowing fuses.

M. A. Sawyer (Southern California Telephone Company, Los Angeles): The methods of obtaining sensitive ground protection on radial distribution feeders described by Messrs. Hunt and Vivian appear to be admirably suited for use in situations involving the joint use of poles by higher-voltage power distribution circuits and communication facilities. As has been pointed out in a previous paper ("Protection Features for the Joint Use of Wood Poles Carrying Communication Circuits and Power Distribution Circuits Above 5,000 Volts," J. O'R. Coleman and A. H. Schirmer, AIEE TRANSACTIONS, March 1938), safety in joint use is dependent largely on the following factors:

1. Frequency of occurrence of contacts between power and telephone circuits.
2. Voltage on the telephone plant when contact occurs.
3. Current in the telephone plant when contact occurs.
4. Duration of contact.

The first factor is, of course, in many respects a function of the mechanical design, construction, and maintenance of the line and depends somewhat on the type of plant, tree conditions, exposure to traffic, or other hazards rather than the electrical characteristics or protective devices. The other three factors are, however, controlled to a large extent by the system of sensitive ground protection described in the paper. In addition to providing a means of promptly de-energizing the feeder in case of accidental contact with telephone plant, the voltages and currents impressed on the telephone plant are minimized by virtue of the large amount of impedance introduced in the power system by the grounding transformer.

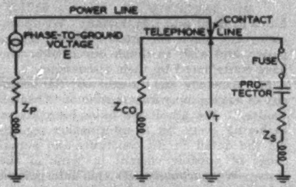

Figure 1. Fault circuit of a power-line contact with telephone plant

The voltage on the telephone plant in case of contact is dependent on the power-circuit voltage and the ratio of the impedance in the telephone plant between the point of contact and ground to the total impedance of the fault circuit. Referring to figure 1 of this discussion which shows in schematic form the fault circuit of a grounded-neutral system, it will be noted that the voltage on the telephone plant (V_t) is $E\frac{Z_t}{Z_t+Z_p}$ where Z_t is the parallel impedance of the two paths to ground. The current in the telephone plant is $\frac{E}{Z_t+Z_p}$ and divides over the two paths inversely in proportion to the impedance of the paths. If the telephone fuse opens, the voltage across the fuse and on the telephone plant then becomes $E\frac{Z_{cn}}{Z_{co}+Z_p}$ and the current is $\frac{E}{Z_{co}+Z_p}$. If the telephone fuse opens and the connection toward the central office is lost at the point of contact, the voltage across the fuse and on the telephone plant then becomes E. Since the current under these conditions becomes zero, the power circuit would not be de-energized and this voltage would remain on the telephone plant unless it was large enough to break down the insulation at some point.

Since the sensitive ground protection system for use on higher-voltage feeders employs a grounding transformer in the neutral the impedance of the power system (Z_p) is increased by an amount equal to the impedance of this transformer. If, for example, a ten-kilowatt 11-kv grounding transformer of five per cent impedance is used the added impedance would be approximately 605 ohms. This would limit the fault current that could flow into the telephone plant in case of contact to a maximum of about 10.5 amperes assuming $Z_t=0$ and neglecting the impedance of the power feeder. If it is also assumed that the impedances of the two paths to ground in the telephone plant are $Z_{co}=200$ ohms and $Z_s = 50$ ohms (resistance) and again neglecting the impedance of the power feeder the total current would still be approximately 10.5 amperes and the voltage on the telephone plant would be about 420 volts. If the telephone fuse operates the current would be reduced to about 10 amperes and the voltage on the telephone plant would increase to approximately 2,000 volts. If in this case the minimum trip current of the feeder ground relay is 2 amperes it is quite probable that the circuit will be de-energized before the telephone fuse will operate.

Where the telephone plant is cable the impedance to ground (Z_{co}) will normally be very small as compared to the impedance of the grounding transformer. A contact with the cable will therefore impress a relatively low voltage on the telephone plant, well within the range that can be handled by the telephone protector. Furthermore, with the small value of fault current the liability of a cable burn-off allowing the voltage on the telephone plant to rise to full phase-to-ground voltage is remote. It therefore appears that with this system of ground protection and with the normal cable sheath grounding and subscriber station protection practices employed in the telephone system, suitable protection co-ordination should be obtained where higher-voltage joint use with telephone cable is involved.

Where the telephone plant is largely aerial wire it is normally difficult to obtain protection co-ordination of the two systems because of the relatively high impedance of the telephone wire, and in many instances in rural areas low-impedance grounds are not available for the subscribers' protector. Due to the high impedance in the telephone plant the resulting voltages on the telephone plant will be high and the power system fault current may be limited to the extent that the circuit is not promptly de-energized. Furthermore, with the limited current-carrying capacity of the open wire as compared to the cable sheath and suspension strand, the possibility of burning off the wire is much greater than where cable plant is involved.

However, with the method of ground protection described in the paper the fault current may be limited to a relatively low value which in turn reduces the possibility of burning off the telephone open wire. Also, because of the impedance of the grounding transformer the impedance to ground of the telephone plant may be somewhat higher than would otherwise be permissible without impressing an excessive voltage on the telephone plant at the time of contact. Since the relay system may be arranged to de-energize the feeder with a fault contact resistance as high as 2,000 or 3,000 ohms it is unlikely that the impedance of the open-wire telephone plant assuming no burn-offs will prevent tripping of the circuit. With the use of auxiliary three-kilovolt protective gaps to ground in the telephone plant to limit the voltage across the fuse and to insure a continuous path to ground for the contact current, it appears that with this method of ground protection employed in the power system, protection co-ordination can often be secured in a practicable manner in open-wire joint-use situations.

While the sensitive ground protection described in the paper was originally developed to enable the tripping of distribution circuits involved in ground faults of high contact resistance it would appear to also offer a practicable means of providing the necessary degree of safety in many situations in which higher voltage joint use is the best solution. The authors are to be complimented on their development of this system and its application as described in the paper.

R. M. Smith (Westinghouse Electric and Manufacturing Company, Newark, N. J.): Mr. Hunt and Mr. Vivian are to be congratulated on the amount of field development work that they have done on this problem. The solution which they offer is certainly a simple one and from their records it appears that a sensitive-type ground protection has been achieved without incurring unnecessary trip-outs. It would be desirable to hear from other relay engineers in different parts of the country with respect to their experience in attempting to achieve a sensitive type of protection.

A recent investigation undertaken by the writer indicated that although a few attempts had been made to obtain ground-fault relaying on distribution feeders with a sensitivity as low as 5 amperes primary, the general trend was toward considerably higher currents usually with a minimum of 50 amperes. The writer believes that the danger of unnecessary trip-outs due to tree grounds has been greatly exaggerated and that no real concrete data exist as to the extent of the hazard. It is unfortunate that the novel solution offered by Messrs. Hunt and Vivian is applicable only to distribution systems operating with a single ground. The general tendency at the present time appears to be in the opposite direction; that is, multiple grounds. Perhaps it might be well for the industry to again evaluate the many factors that are involved in single versus multiple grounding and reverse this trend, if the advantages of a sensitive ground protection overweigh some of the advantages of the multiple-grounded system.

L. N. Crichton (Westinghouse Electric and Manufacturing Company, Newark, N. J.): It is my belief that this paper gives the answer to the old problem of properly protecting distribution circuits against the hazard due to fallen wires. The simplicity of the idea is unusual; it requires a standard relay and a standard distribution transformer as a grounding reactor. The requirements as laid down for proper sectionalizing are likewise simple; namely, to clear all troubles having a ground current of more than one ampere. The amount of unbalance current in the load does not enter into the problem. The method of determining the size of the ground transformer is simple and thoroughly explained in the paper.

It is to be hoped that other systems in different localities will quickly install this type of protection so as to obtain experience in parts of our country where climatic conditions are different. What we all suspect, of course, is that in most localities, tree ground currents will be a nuisance after long spells of wet weather.

Recent tests that have been made in another part of the United States indicate that tree grounds are not so likely to cause trouble as has been feared in the past. Perhaps it will be necessary to raise the minimum current to some value above the one ampere set by the authors, but the practical value may not be more than two, three, or four amperes. I am confident that the limit will be a low one and hope that the industry will soon discover what it is by a number of trials of this new scheme.

Lloyd F. Hunt and J. H. Vivian: We appreciate very much the interest shown in our paper, and wish to thank our friends who have contributed by their discussions.

Mr. Kennedy has brought out the necessity of having more information on "Ground Faults." We admit we have been lax in gathering every bit of information regarding the detail circuit constants and actual range of possible current in some of the tests. We purposely left this information brief for two reasons: first, it only applies to our conditions; and second, as Doctor Lindvall has brought out in his discussion, a very great deal is not known about earth faults and earth contact resistances, so we feel that this subject is important enough to have considerable more work done on it and a paper written covering the results; Mr. Kennedy also brings out the desirability of having operating experience. To date our operation has been all correct. However, with more experience and with this type of protection in more varied localities, it will be possible to give valuable information on its operating record.

Mr. Benner brings out a very interesting part of the story of sensitive ground protection that we purposely avoided, which is the policy of de-energizing grounded feeders. Each organization has its problem in this regard, and may make its own decisions whether or not grounded feeders should be left in service.

It is our desire to have sufficient installations to determine by experience how far we can justify this type of protection. We are now cutting over one urban territory, from 2.3 kv delta to 4 kv four-wire, to this type of sensitive ground protection. The normal change would be to 4 kv four-wire multigrounded neutral system; however, with this change it will be possible to gather interesting operating experience.

We feel, as Mr. Benner does, that with the many miles of rural lines now being put into use, more consideration should be given this problem of de-energizing them in the case of grounded conditions.

Mr. Brownlee has reviewed the prior methods of sensitive ground protection, but in his treatise no attempt has been shown to limit the ground current, which feature we feel is very desirable. In his case of using a 5/5 current transformer with one ampere tap relay, if solidly grounded on a 3,000-kva single 11-kv circuit, the relay would have approximately one ampere minimum trip, but also would give approximately 500 amperes maximum current, or a range of 500 to 1. Most every relay engineer will recognize that this range is impossible. This is more or less true in other examples brought out by Mr. Brownlee. If his method were used on the 7-kv system mentioned in the paper, it would have a minimum trip of approximately 55 amperes. This would make it necessary to use a very much more expensive joint pole construction. The saving from using the simpler construction would many times pay for the installation of our type of ground protection.

In this day of changes, the relay engineers are finally "evoluting" to a place where they can use some standard equipment, such as standard transformers for makeshift (manufacturers please note) grounding bank. All of this equipment is usable material, should further changes be made. If our present 600-ampere grounding banks were composed of standard distribution transformers instead of zig-zag, we could get some use out of them now instead of placing them in the discard. Therefore, from these experiences, we feel it is much better to use standard devices that can later be used for other purposes.

Mr. North has brought out the question of joint use with the telephone companies. We wish to bring out again that this system was developed for other reasons than joint use, but when we were confronted with joint use, especially on higher voltages, these systems of protection were available, and allowed us to use a much cheaper construction than normally used for this service, the saving being more than adequate to justify its use.

Doctor Lindvall and his associates have started a very important phase of this work of determining the phenomena regarding earth contact resistance and breakdown value. We hope he may continue his investigations, which will give us all much valuable information.

Mr. Sawyer's discussion gives in detail the advantages of our type of protection when used in joint pole arrangement. We feel that, by working with Mr. Sawyer on these problems, much better service can be rendered the telephone subscribers, who are also our customers, and at the same time more economical methods of distributing both services can be developed.

Mr. Smith brings out the unfortunate fact that this protection cannot be used on multigrounded systems. This is true, and the changing from multigrounded uninsulated-neutral systems would be expensive, and would cause a considerable write off of certain features of old construction. However, in the changeover mentioned before, a study is being made to establish the difference in actual relative costs when a new job is made.

Mr. Crichton has brought out the problem of tree grounds; also, Mr. Smith mentioned this subject. Dry trees will not cause this protection to operate when the very low minimum trip is used. It is possible that trees will cause actual phase-to-phase contacts by wind which will relay the line. In the case of wet trees, the earth must also be wet, so earth contacts would then be of less resistance. In this case it would be possible to design the system for higher minimum trip and, by taking advantage of the range of tap settings of the relays (normally 4 to 15 amperes), adjustments could be made as desired. We have had no experience with the relation of dry trees to wet trees, as compared to dry earth to wet earth, but it could be expected that the same degree of discrimination could be obtained during the wet seasons. In several cases our practice is to make the ground protection less sensitive during the rainy season.

Again we wish to express our appreciation to those who have taken part in this very interesting subject.

Transient and Steady-State Performance of Potential Devices

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THE increasing use of high-speed relaying with the resulting emphasis on the transient performance of potential devices has led to an extensive analysis of their performance under transient conditions. Both analytical and network-calculator methods have been developed for predetermining transient performance. Tests have been made to substantiate the methods of analysis and to show the character of the transient and extent of control in typical cases. Novel means have, likewise, been devised for presenting the steady-state performance of the device. In particular, the introduction of the equivalent circuit of the device represents an important advance in the visualization, both of the transient and steady-state performance. In this paper, after a brief description of circuit arrangements of the potential devices under discussion, the new theoretical methods are presented, followed by the substantiating test information.

Circuit Arrangements

A potential device may be energized from a condenser bushing or from a coupling capacitor, which may be represented as a capacitance potentiometer as illustrated in figure 1. The upper section is identified as the stack capacity and the lower section as the tap capacity. The intermediate point is called the tap. Both the stack and tap sections are contained within the porcelain housing of the bushing or capacitor.

Figure 1 shows three circuit arrangements of commercial devices and also illustrates required modifications in capacitor devices for use with carrier. Means are provided for neutralizing the effective series capacitance of the source by series reactance between the source and the burden. Variable reactor and capacitor are used in (*a*); transformer reactance in (*b*), (*c*), and (*d*). The circuit of figure 1*a* is used in the bushing potential device and the low-capacity capacitor potential device. Circuits (*b*) and (*d*) apply to the high-capacity high-accuracy capacitor potential device and include provision for obtaining several secondary voltages simultaneously through the use of a coordinated auxiliary transformer. Both the main and auxiliary transformer are designed to operate at a low flux density and will remain stable when subjected to flux densities in excess of four times normal. The circuit of figure 1*c* is used in the simplified capacitor potential device and is essentially one in which the capacitive and inductive reactances are not variable. The secondary voltage thus obtained is substantially in phase with line-to-ground voltage of the system. Simultaneous use of the coupling capacitor as a carrier-current channel, illustrated in (*d*), requires the addition of carrier-frequency chokes to complete the fundamental-frequency circuits and permit a series relation at carrier frequency between the carrier-current set and the transmission line.

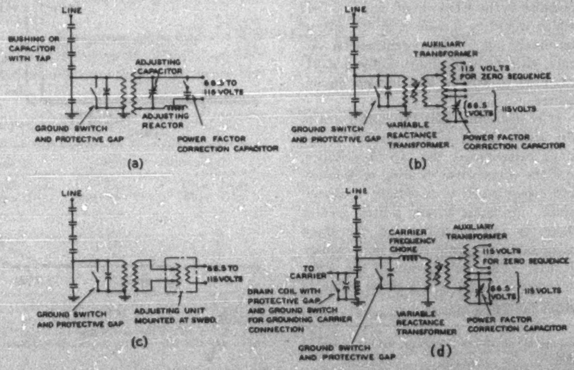
Figure 1. Circuit arrangements of potential devices

(*a*)—Bushing or capacitor device

(*b*)—High-capacity capacitor device with multiple secondary

(*c*)—Simplified capacitor device

(*d*)—High-capacity capacitor device with arrangements for carrier


LINE
BUSHING OR CAPACITOR WITH TAP
ADJUSTING CAPACITOR
66.5 TO 115 VOLTS
ADJUSTING REACTOR
GROUND SWITCH AND PROTECTIVE GAP
POWER FACTOR CORRECTION CAPACITOR
(a)
LINE
AUXILIARY TRANSFORMER
115 VOLTS FOR ZERO SEQUENCE
66.5 VOLTS
115 VOLTS
GROUND SWITCH AND PROTECTIVE GAP
VARIABLE REACTANCE TRANSFORMER
POWER FACTOR CORRECTION CAPACITOR
(b)
LINE
66.5 TO 115 VOLTS
GROUND SWITCH AND PROTECTIVE GAP
ADJUSTING UNIT MOUNTED AT SWBD.
(c)
LINE
CARRIER FREQUENCY CHOKE
AUXILIARY TRANSFORMER
115 VOLTS FOR ZERO SEQUENCE
TO CARRIER
DRAIN COIL WITH PROTECTIVE GAP AND GROUND SWITCH FOR GROUNDING CARRIER CONNECTION
66.5 VOLTS
115 VOLTS
VARIABLE REACTANCE TRANSFORMER
POWER FACTOR CORRECTION CAPACITOR
GROUND SWITCH AND PROTECTIVE GAP
(d)

The proper device for any particular application is selected with a view to economic as well as to performance considerations. The standard capacitor potential devices have the greatest accuracy and best performance, and the simplified capacitor potential device is lower in cost.

Steady-State Performance

The Equivalent Circuit

The performance of a potential device may best be visualized through an understanding of its equivalent circuit, figure 2*d*. Because of the importance of a thorough understanding of this circuit in what follows, its derivation and meaning will be carefully considered at this point.

The actual circuit of a typical single-phase potential device is shown in figure 2*a*. There are many possible variations, and this particular circuit has been selected for consideration because it includes the major elements of importance and yet it is quite simple. The concepts gained from a study of it are later extended to include some of the more important variations. There is a double transformation of voltage, first through the potentiometer, formed by the "stack" and "tap" capacitances, and second through the transformer.

In figure 2*b*, the transformer is replaced by its equivalent circuit consisting of a series leakage impedance and an exciting impedance branch. The latter is almost negligible in a liberally designed transformer and the approximation of connecting it at one end of the leakage branch as shown is entirely justified. It

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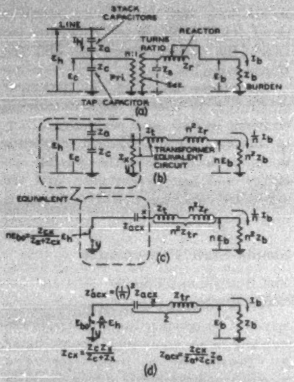
STACK CAPACITORS
LINE
REACTOR
TURNS RATIO
n:1
I_h
Z_a
E_h
Z_c
E_c
Pri.
Z_e
Z_r
Sec.
I_b
E_b
Z_b
BURDEN
TAP CAPACITOR
(a)
Z_t
$n^2 Z_r$
$\frac{1}{n} I_b$
Z_x
TRANSFORMER EQUIVALENT CIRCUIT
nE_b
$n^2 Z_b$
(b)
EQUIVALENT
Z_{acx}
$n^2 Z_{tr}$
$nE_{bo} = \frac{Z_{cx}}{Z_a + Z_{cx}} E_h$
x
y
(c)
$Z'_{acx} = \left(\frac{1}{n}\right)^2 Z_{acx}$
Z_{tr}
Z
$E_{bo} = \frac{A}{n} E_h$
$Z_{cx} = \frac{Z_c Z_x}{Z_c + Z_x}$
$Z_{acx} = \frac{Z_{cx}}{Z_a + Z_{cx}} Z_a$
(d)

Figure 2. Actual and equivalent circuits of a device without auxiliary tuning capacity on secondary of transformer

(*a*)—Actual circuit

(*b, c, d*)—Successive equivalent circuits

may then be paralleled with the tap capacitance, and for a typical case the parallel impedance of the two is only about three per cent greater than that of the tap capacitance alone.

In figure 2*c*, the applied voltage and potentiometer circuit are replaced by a proportional applied voltage and a series impedance. This series circuit (*c*) is exactly equivalent to the potentiometer circuit (*b*) according to Thevenin's theorem of networks[5] provided the following relations are maintained. The impressed voltage in the series circuit must be the same as the voltage that would appear at the output terminals *x-y* on the potentiometer circuit at no load (burden disconnected). The impedance in the series circuit must be that which would be measured at the output terminals *x-y* of the potentiometer circuit, with the line voltage terminal short-circuited to ground and burden disconnected.

It is clear then that the impedance in the series circuit is the parallel of the stack capacitance, the tap capacitance, and the transformer exciting impedance, and consequently is essentially a capacitance. Its value is only a little greater than the tap capacitance if the stack capacitance is small, as in high-voltage devices. The combination of the series impedances in (*c*) into a single value in (*d*) is obvious.

If an adjusting capacitor is used on the secondary side of the transformer as shown dotted in figure 2*a*, the final equivalent circuit takes the same form (*d*). However, the values of the applied voltage and series impedance in this case are obtained by an additional transformation, using Thevenin's theorem, to take into account this capacitance (appendix I).

Performance From Vector Diagrams

The steady-state performance may now be investigated by analyzing the circuit, figure 2*d*, which is the exact equivalent of the actual potential device. Figure 3 shows a set of vector diagrams drawn for a particular device operating at rated burden. It will be observed that with unity power factor burden (figure 3*b*) the voltage across the source capacity and the inductance are equal and are approximately 80 per cent of the voltage across the burden. The voltage across the series resistance, which includes all of the device losses, amounts to 9.5 per cent of the burden voltage. From these relations, a number of interesting facts regarding the device may be deduced. The 9.5 per cent resistance drop represents the ratio regulation from no load to full load. Since the reactance drops cancel, the resistance drop constitutes the entire source voltage drop to the burden. The tap voltage, which is equal to the burden voltage added vectorially to the reactor drop, and the result multiplied by the transformer turns ratio, is higher at rated load than at no load. This is due to the fact that the reactance drops decrease to zero as the burden current is reduced to zero.

Figure 3 (*b* and *d*) also illustrates that phase-angle adjustment of the device is obtained by varying the inductive reactance. The adjustment shown in (*b*) results in zero phase angle between the high-voltage line-to-ground voltage and the burden voltage, while that in (*d*) results in a burden voltage leading by 30 degrees. The latter is obtained by decreasing the inductive-reactance drop. It is evident that with this adjustment a large phase-angle regulation as well as an increased voltage regulation results, since the voltage drop occasioned by the burden current through the complete impedance,

(*a*)—Adjustment for zero phase angle and 50 per cent leading power factor burden

(*b*)—Adjustment for zero phase angle and unity power factor burden

(*c*)—Adjustment for zero phase angle and 50 per cent lagging power factor burden

(*d*)—Adjustment for 30-degree leading phase angle, and unity power factor burden

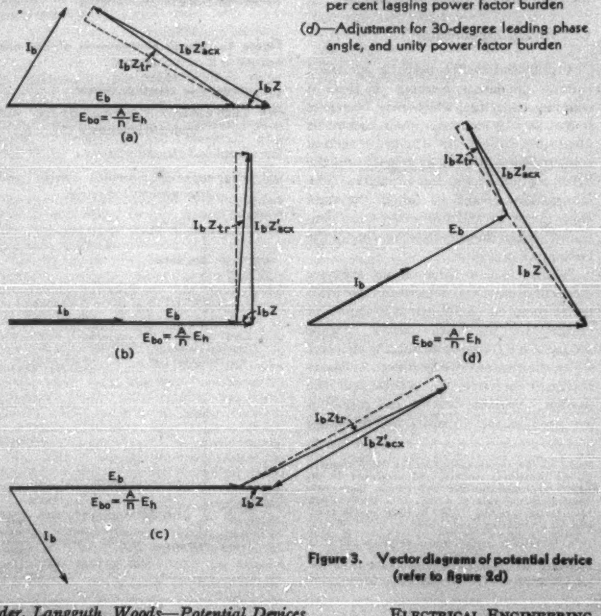

Figure 3. Vector diagrams of potential device (refer to figure 2d)

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[5] For all numbered references, see list at end of paper.

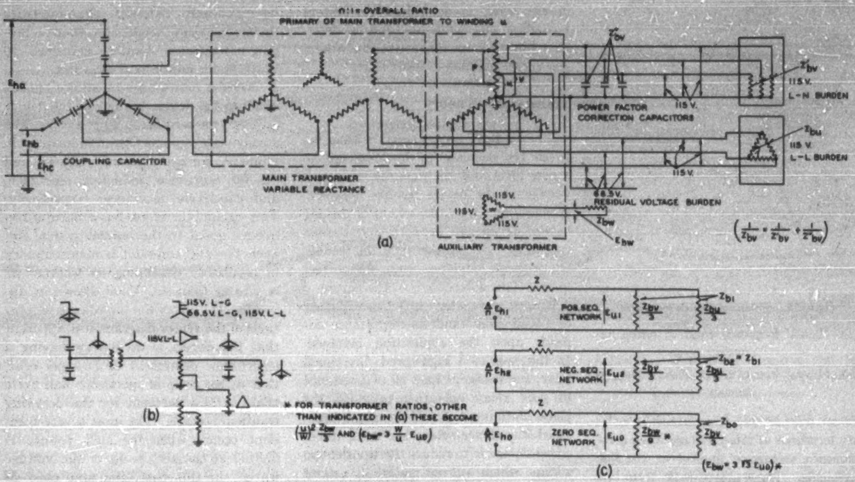

n:1 = OVERALL RATIO
PRIMARY OF MAIN TRANSFORMER TO WINDING u
E_{ha}
E_{hb}
E_{hc}
COUPLING CAPACITOR
MAIN TRANSFORMER
VARIABLE REACTANCE
P
W
V
Z'_{bv}
Z_{bv}
115 V.
L-N BURDEN
POWER FACTOR
CORRECTION CAPACITORS
115 V.
Z_{bu}
115 V.
L-L BURDEN
115 V.
66.5 V.
RESIDUAL VOLTAGE BURDEN
115 V.
115 V.
115 V.
Z_{bw}
E_{bw}
AUXILIARY TRANSFORMER
(a)
$\left(\frac{1}{Z_{bv}} = \frac{1}{Z'_{bv}} + \frac{1}{Z''_{bv}}\right)$
115 V. L-G
66.5 V. L-G, 115 V. L-L
115 V. L-L
(b)
* FOR TRANSFORMER RATIOS, OTHER THAN INDICATED IN (a) THESE BECOME
$\left(\frac{u}{w}\right)^2 \frac{Z_{bw}}{3}$ AND $\left(E_{bw} = 3\frac{W}{U}E_{u0}\right)$
Z
$\frac{A}{n}E_{h1}$
POS. SEQ. NETWORK
E_{u1}
$\frac{Z_{bv}}{3}$
$\frac{Z_{bu}}{3}$
Z_{b1}
Z
$\frac{A}{n}E_{h2}$
NEG. SEQ. NETWORK
E_{u2}
$\frac{Z_{bv}}{3}$
$\frac{Z_{bu}}{3}$
$Z_{b2} = Z_{b1}$
Z
$\frac{A}{n}E_{h0}$
ZERO SEQ. NETWORK
E_{u0}
$\frac{Z_{bw}}{9}$ *
$\frac{Z_{bv}}{3}$
Z_{b0}
$(E_{bw} = 3\sqrt{3}E_{u0})$*
(c)

Figure 4. Equivalent burdens and sequence networks

(*a*)—Polyphase connection of potential devices with burdens on various windings of auxiliary transformers

(*b*)—Shorthand representations

(*c*)—Sequence networks

Z, decreased to zero as the burden current is reduced to zero. In fact, as the burden current becomes zero with normal line voltage, the phase-angle error becomes practically zero regardless of the angular adjustment of the device at rated burden.

Table I indicates the voltage performance and burden performance for various types of potential devices, while figure 10 illustrates the characteristic curves for two specific designs.

Symmetrical-Component Theory

The performance of potential devices having balanced three-phase connections operating under unbalanced line voltage conditions is best approached through the method of symmetrical components. This performance is defined as the overall ratio from high-voltage line voltage to burden voltage, and includes both magnitude and phase angle. The sequence networks for a typical device and its connected burdens are outlined in figure 4, using the equivalent circuit of the single-phase potential device previously developed.

The positive- and negative-sequence performance characteristics are the same. In either case the performance is identical with that of a single-phase device supplying a burden impedance equal to the positive- (or negative-) sequence phase-to-neutral impedance of the total three-phase burden, on the particular turns base used. Thus for all three-phase and line-to-line fault conditions, involving only positive- and negative-sequence quantities, the performance characteristics of all secondary voltages are the positive-sequence performance, and are given directly by the single-phase test data.

The zero-sequence performance will in general differ from the positive-sequence performance since the zero-sequence burden impedance is different from the positive-sequence burden impedance as illustrated in (*c*). For fault conditions involving zero-sequence the residual performance is identical with zero-sequence performance, so that single-phase tests using the burden impedances indicated in the zero-sequence network apply. The residual burden voltage is related to the zero-sequence burden voltage by the factor $3\sqrt{3}$ for the transformer ratios of figure 4. These statements are made on the basis of the equivalent circuit which assumes all impedances are linear and consequently does not include minor effects due to variation of transformer exciting impedance. The line-to-ground performance is a function of both positive- and zero-sequence performances and is expressed mathematically in appendix II.

Where residual voltage only is required, a single device may be used and its zero-sequence performance is the same as its single-phase performance with the three capacitors paralleled at tap and line terminals. The burden rating of such a device refers to the burden on the second-

Table I. Potential-Device Performance Data

Type of Potential Device	Voltage Performance (a) Ratio Error (Per Cent)	Voltage Performance (a) Phase-Angle Error (Deg)	Burden Performance (b) Ratio Error (Per Cent)	Burden Performance (b) Phase-Angle Error (Deg)
Low-capacity bushing device	20	30	35	5
High-capacity bushing device	8	10	20	5
Low-capacity capacitor device	5	5	15	4
High-capacity capacitor device	4	4	10	2
Simplified capacitor device	5	15	35	15

These values indicate: (*a*) The maximum errors to be expected with changes in line voltage of 5 per cent to 120 per cent of rated voltage with the device adjusted for rated watt burden at 66.5 volts, 60 cycles, and rated line-to-ground voltage. (*b*) The maximum errors to be expected with changes in burden of 0 per cent to 100 per cent of rated watt burden when energized at rated line-to-ground voltage, and with the device adjusted, for rated watt burden at 66.5 volts 60 cycles.

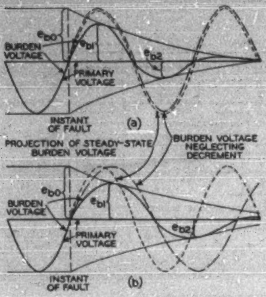


Figure 5. Potential-device transients

(*a*)—Natural frequency equal to system frequency

(*b*)—Natural frequency less than system frequency

ary terminals at rated voltage when zero-sequence voltage is applied at the line terminals. The required ratio from line zero-sequence voltage to burden voltage, in any case, is dependent on system grounding characteristics and is determined in the same manner as the ratio of star-series auxiliary transformers.

Transients—Theory

When a fault occurs there is a lag between the decrease of line voltage to a new level and the time the secondary voltage of the device reaches its corresponding new level, the extent of this lag depending on the network and burden constants. During this time the secondary voltage deviates from its correct value. This difference is in the nature of a damped wave which may be oscillatory or "dead beat" and its importance depends upon the application involved. In the case of a high-speed directional relay, the transient may be of assistance to good relay operation provided the phase shift is small. However, if high-speed impedance relays are used, it becomes desirable to reduce the transient to a value which will not materially extend the relay operating time.

TRANSIENT PERFORMANCE—SERIES CIRCUIT

The vector diagrams, figure 3, throw considerable light on the transient performance of the potential device. The equivalent circuit, figure 2*d*, derived for steady-state conditions applies also to transient conditions if the transformer magnetizing branch is neglected. The burden which was lumped as a single impedance for the steady-state conditions must be segregated into its actual components for correct analysis of the transient conditions. For example, a pure watt burden consisting of a resistor will have entirely different characteristics under transient conditions, than a lagging-power-factor burden, corrected to unity power factor by a capacitor.

Consider first a pure resistance burden equal to the device rating and the device adjusted for zero-phase angle error. The steady-state proportions, indicated in figure 3*b*, are approximately 1.1, 0.8, and 0.8 for resistance, inductive reactance, and capacitive reactance, respectively. This circuit, therefore, has a natural frequency equal to the normal system frequency. The transient is nonoscillatory or oscillatory depending on whether R^2 is greater than $4X_LX_C$ as shown in appendix III. From the impedance proportions of the vector diagram, it is apparent that the circuit is oscillatory having a logarithmic decrement of 11.5 per cent, that is, the ratio of successive half cycle peaks is 11.5 per cent for the decaying transient voltage. The most severe transient occurs when the high voltage is faulted at the zero point of the voltage wave. In this case, the first crest of burden voltage occurs one-fourth cycle

Figure 6. Single-phase transient tests for high-capacity 230-kv bushing potential device

Primary line-to-ground voltage 127 kv, 60 cycles; secondary voltage 63.5 volts

A—Timing wave
B—Secondary voltage
C—Primary voltage

(*a*)—Secondary burden 10 volt-amperes—100 per cent power factor

(*b*)—Secondary burden 40 volt-amperes—100 per cent power factor

(*c*)—Secondary burden 100 volt-amperes—100 per cent power factor

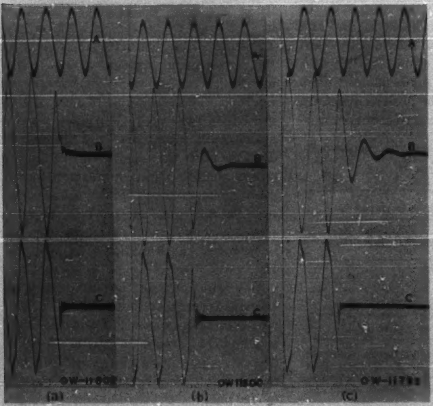


Figure 7. Single-phase transient tests for high-capacity 138-kv bushing potential device

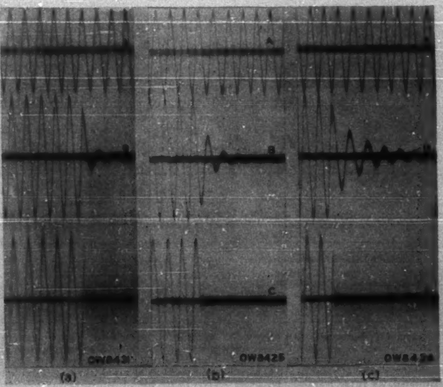
Primary line-to-ground voltage 76.2 kv, 60 cycles; secondary voltage 63.5 volts

A—Timing wave
B—Secondary voltage
C—Primary voltage

(*a*)—Secondary burden 20 volt-amperes—100 per cent power factor

(*b*)—Secondary burden 40 volt-amperes—100 per cent power factor

(*c*)—Secondary burden 40 volt-amperes—50 per cent power factor lagging



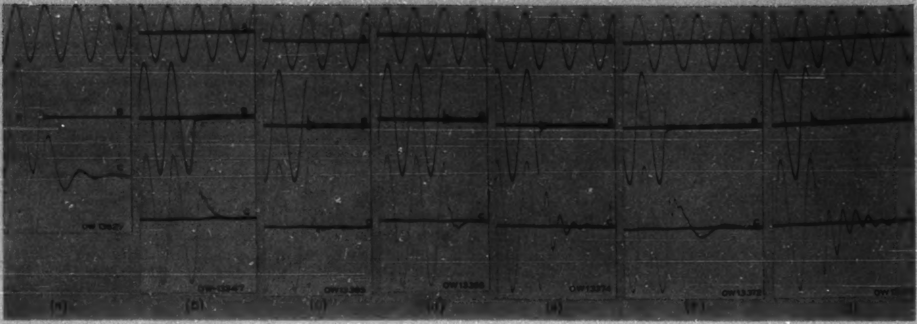


Figure 8. Single-phase transient tests for standard and modified low-capacity 161-kv capacitor potential device

Primary line-to-ground voltage 80.8 kv, 60 cycles; secondary 67.3 volts

A—Timing wave
B—Primary voltage
C—Secondary voltage

(*a*)—Standard device with secondary burden of 65 volt-amperes—100 per cent power factor

(*b*)—Modified device with secondary burden of 65 volt-amperes—100 per cent power factor and fault at voltage zero

(*c*)—Modified device with secondary burden of 65 volt-amperes—100 per cent power factor and fault at voltage peak

(*d*)—Modified device with secondary burden of 100 volt-amperes—100 per cent power factor

(*e*)—Modified device with secondary burden of 60 volt-amperes—50 per cent power factor leading

(*f*)—Modified device with secondary burden of 60 volt-amperes—50 per cent power factor lagging

(*g*)—Modified device with secondary burden of 60 volt-amperes—50 per cent power factor lagging capacitor corrected to unity power factor

after the primary voltage has been faulted, as shown in figure 5. Its magnitude is the normal value of burden voltage multiplied by the square root of the logarithmic decrement or 34 per cent for the proportions of figure 3*b*. Successive peaks are in the ratio of the logarithmic decrement. Thus the transient performance can be predicted directly from the vector diagram. In this particular case, we find that the transient is oscillatory and has successive peak values of 34 per cent, 3.9 per cent, 0.5 per cent, and so forth, provided the high line voltage is short-circuited at the instant it is passing through zero.

At reduced burdens, the current is decreased and, it is apparent that the reactive voltage drops shown in the vector diagram are smaller in direct proportion. For one-fourth burden, the proportions are 1.1, 0.2, and 0.2 for R, X_L, and X_C respectively. The transient is therefore "dead beat" which emphasizes the important point that the transient can be reduced by lowering the burden on the device.

In a similar manner, the natural frequencies and decrements or time constants have been obtained for the other vector diagrams of figure 3, the results being given in table III. From an analysis of this table, it is evident that a device adjusted for zero phase angle (inductive and capacitive reactances of the device equal) does not guarantee a natural frequency equal to the system frequency, since the burden constants also affect the natural frequency. Furthermore, it is evident that this circuit is not necessarily oscillatory, since a reduction of a resistive burden may increase the ratio of R^2 to $4X_LX_C$ to a point where the transient is "dead beat." Also, it is apparent that a decrease in the source capacitive and inductive reactances improves the transient performance for a given burden.

Table II. Correlation of Calculated and Test Transient Data

Figure Number	First Peak in Per Cent of Normal Crest Voltage		Successive Peaks in Per Cent of Previous Peak		Natural Frequency (Calculated)
	Calculated*	Test	Calculated	Test	
6*b*	51.4		26.5	23.2	60.0
6*c*	70.1		49.3	50.0	60.0
7*a*	52.1		27.3	33.3	60.0
7*b*	73.3	74.0	53.9	50.0	60.0
7*c*	81.9		67.2	65.0	53.8
8*a*	63.0		39.7	41.5	60.0
8*f*	63.8		40.9	35.6	37.4

Based on fault at zero point of voltage wave.

Table III. Natural Frequencies and Decrements Corresponding to Figure 3*

Figure Number	First Peak in Per Cent of Normal Crest Voltage		Successive Peaks in Per Cent of Previous Peak		Natural Frequency if Oscillatory		Time Constants in Seconds†	
	100 Per Cent Burden	25 Per Cent Burden	100 Per Cent Burden	25 Per Cent Burden	100 Per Cent Burden	25 Per Cent Burden	100 Per Cent Burden	25 Per Cent Burden
3*a*			45.7	16.4	88.2	117.0	0.00724	0.00193
3*b*	33.8	20.2	11.5	**	60.0	**	0.00386	0.01560
3*c*			49.7	19.2	41.2	26.5	0.00174	0.00115
3*d*			8.4	**	91.0	**	0.00221	0.00807

* Burden composed of resistance and reactance in series. † Long-time constant tabulated for nonoscillatory case. ** Nonoscillatory.

Double-Energy Transients

Two practical cases giving rise to complex circuits are the use of an adjusting capacitor on the transformer secondary

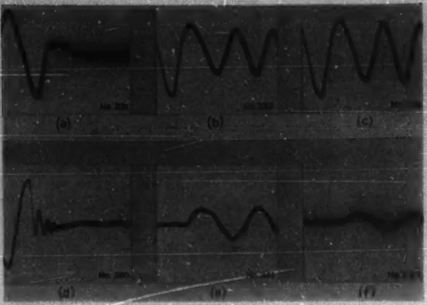


Figure 9. Three-phase transient a-c network calculator tests for modified low-capacity 161-kv capacitor potential device

Primary line-to-ground voltage 80.8 kv, 60 cycles; secondary line-to-ground voltage 67.3 volts. Phase-relay burden 43 volt-amperes—96 per cent power factor lagging; ground-relay burden 29 volt-amperes—75 per cent power factor leading

(*a*)—Phase-relay voltage for fault at device end of line at zero point of voltage wave

(*b*)—Same at middle of line

(*c*)—Same at remote end of line

(*d*)—Same as (*a*) except for fault at peak of voltage wave

(*e*)—Ground-relay voltage for fault at device end of line at zero line point of voltage wave

(*f*)—Same at middle of line

and the use of a capacitor in parallel with the burden for power-factor correction. The first of these in any practical case results in a high-frequency transient of short duration which is unimportant. The latter may produce an undesirable double-frequency transient. In the particular case of correction to unity power factor, the two frequencies are respectively above and below normal system frequency and correspond to the resonance points between the series-connected source impedances and the parallel-connected burden impedances.

RESONANCE

The terms "resonant", "oscillatory", "nonresonant", and so forth, have been somewhat loosely used in connection with potential devices in the past. In view of the foregoing discussion, they should be analyzed in an effort to define their meaning as applied to these devices and, if possible, to adopt more appropriate nomenclature for the various devices available.

The term "nonresonant" applies strictly to steady-state conditions. It is, however, ambiguous and has in some cases been confused as implying a freedom from the effects of oscillation of the burden voltage when changing to a new level. Nonresonant devices are actually a simpler, cheaper form of device, termed "nonresonant" because they do not have the equal capacitive and inductive reactive branches in the source. Actually, this simply means that such a device does not resonate at system frequency with a pure resistance burden. It will, of course, resonate at some other frequency if the burden includes inductance, as it usually does, and will involve the double-energy transients mentioned above if corrective capacity is used with the burden. The term "nonresonant" has also been used to distinguish such a device from the resonant device which has good regulation. Since the standard potential devices (so called "resonant" devices) may have superior performance characteristics under both steady-state and transient conditions, it would be preferable to distinguish them from the less elaborate arrangement by referring to the latter as the "simplified" device, which would not involve any implication as to its relative transient performance.

TRANSIENT TEST DATA

A comprehensive series of transient tests were made to verify the theory outlined above, as well as to show the transient characteristics which may affect the performance of high speed relays, and the efficacy of several means of control. These investigations included field tests previously described,[2,3] single-phase fault tests made on typical devices in the factory, and a-c network-calculator tests.

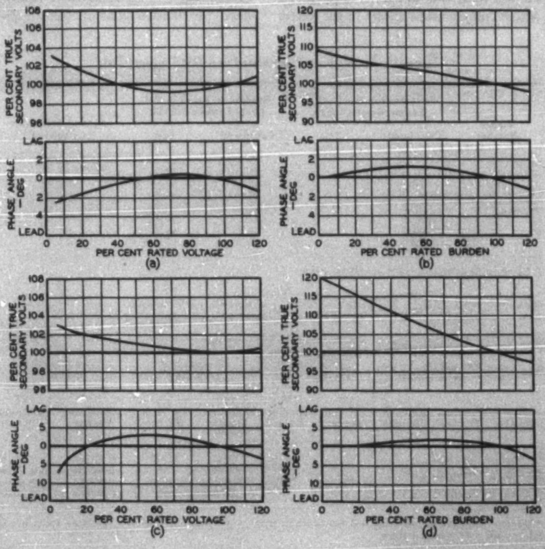
Figure 10. Capacitor and bushing potential-device performance curves

(*a*)—High-capacity 161-kv 60-cycle capacitor potential device—voltage performance

(*b*)—High-capacity 161-kv 60-cycle capacitor potential device—burden performance

(*c*)—High-capacity 161-kv 60-cycle bushing potential device—voltage performance

(*d*)—High-capacity 161-kv 60-cycle bushing potential device—burden performance



In the latter the recently devised method[4] for utilizing the a-c network calculator for transient studies was employed. While 115 and 66.5 volts are the standard line-to-line and line-to-neutral voltages, respectively, the test data have been taken at the various service voltages indicated in the captions.

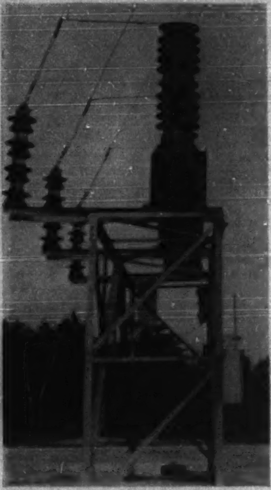


Figure 11. Low-capacity 115-kv capacitor potential device equipped for carrier-current coupling

Figures 6, 7, and 8 illustrate typical single-phase tests, and in general show natural frequencies, amplitudes, and decrements of the burden voltage transient as expected from the theoretical analysis, this correlation being indicated in table II. Since the point of voltage wave at which fault occurs was not controlled in these tests, the magnitude of the first peak after the fault could be compared only in the one case where the fault occurred at the zero point of the voltage wave. Of particular interest are the effects of burden variations figures 7*b* and *c*, and figures 8*c*, *e*, *f*, and *g*. Figures 8*a* and *b* illustrate the improvement obtained by modifying the device as suggested by the theory. Figure 8*g* shows the double-energy transient resulting from power-factor correction of the burden by parallel capacitor. The "subtransient" resulting from the use of adjusting capacitors on the secondary of the transformer is evident in several of the oscillograms of figure 8. Comparison of figures 8*b* and *c* illustrates that the maximum overshoot is obtained for a fault at the zero point of the voltage wave.

The a-c network calculator has been shown to give natural frequencies, amplitudes, and decrements of the transient in close agreement with theoretical methods and with results obtained by oscillographic tests on actual devices. Its flexibility has made possible a thorough analysis covering a wide range of conditions. This study has included for example the effect of location of fault along the transmission line, the effect of point on the voltage wave at which a fault occurs, and of different types of faults such as three phase, line-to-line, line-to-ground, and double line-to-ground. The effects of various proportions and arrangements of the device parts have been studied, a modified device having resulted from this study. The transients have been observed on the ground relays and on the phase relays of sound and faulted phases with balanced and unbalanced arrangements of relay burdens.

Typical photographic records of these tests are given in figure 9, parts *a*, *b*, and *c* illustrate the effect of moving the fault along the transmission line, *a* and *d* the effect of point on the voltage wave at which the fault occurs, and *e* and *f* show the ground relay voltage for faults at the zero and peak points of the voltage wave. The subtransient is maximum for faults at the peak of the wave where a quick energy dissipation in the capacitance divider is necessary. The main transient is greatest for fault at the zero point of the wave.

Conclusions

A general method has been presented utilizing an equivalent circuit for analyzing the performance of potential devices from a design and application standpoint, a method which offers an advantageous point of view for visualizing the phenomena involved as well as providing the specific solutions. This method, including the use of symmetrical components for unbalanced system conditions, covers both steady-state and transient performance. The a-c network-calculator method of transient testing has been found to give reliable engineering results in this problem even though both the power system and measuring system constants needed to be represented simultaneously on the calculator circuits.

Improvements in transient performance have been found possible by changes in design constants, and tests on devices built in line with the theory have shown the expected improvements. It has been shown that the transient phenomena is roughly divisible into a subtransient due to adjusting capacity on the secondary of the transformer, and a main transient in the series circuit composed of the device and its burden. The subtransient is negligible due to its short duration and high frequency. The main transient is of considerable importance, and proper control should be exercised in design and loading to keep it within application requirements. Low noninductive burdens result in the best performance. Double-energy main transients may be avoided by omission of burden-paralleling capacitors.

The studies described have afforded basis for the design of a new capacitor potential device utilizing a variable reactance transformer and having windings for supplying simultaneously star- and delta-connected phase burdens and residual burdens.

Appendix I. Steady-State Performance

Nomenclature

FUNDAMENTAL CONSTANTS

On circuit voltage base, refer to figure 2 except as noted.

Z_a —Impedance of stack capacity
Z_c —Impedance of tap capacity
Z_x —Transformer exciting impedance, primary basis
Z_t —Transformer leakage impedance, primary basis
n —Transformer turns ratio, high voltage to low voltage
Z_s —Impedance of tuning capacity, in parallel with tap capacity but located on secondary side of transformer
Z_r —Impedance of the variable reactor
Z_b —Burden impedance
Z —Source impedance to burden, secondary basis
E_h —High line voltage to ground
E_c —Tap voltage to ground
E_b —Burden voltage
E_{bo} —Burden no-load voltage
A —Over-all potentiometer effect multiplier
k —Over-all no-load transformation ratio E_h/E_{bo}

$$\frac{1}{k}=\frac{A}{n}$$

Other derived and intermediate quantities are defined by the equations.

DERIVATION OF EQUIVALENT CIRCUIT, FIGURE 2 (dotted capacitor not used)

In figure 2*b* the parallel of Z_c and Z_x is

$$Z_{cx}=\frac{Z_cZ_x}{Z_c+Z_x} \qquad (1)$$

By Thevenin's theorem the potentiometer circuit composed of Z_a and Z_{ex} may be replaced by the simple series circuits of figure 2*c* in which:

$$nE_{b0}=\frac{Z_{ex}}{Z_a+Z_{ex}}E_h=AE_h \tag{2}$$

where E_{b0} is seen to be the burden no-load voltage.

$$A=\frac{Z_{ex}}{Z_a+Z_{ex}} \tag{2a}$$

$$Z_{aex}=\frac{Z_{ex}}{Z_a+Z_{ex}}Z_a \tag{3}$$

and being essentially capacitive reactance is shown as such. The transformer leakage impedance and the reactor combine to form the inductive reactance of the source,

$$n^2Z_{tr}=Z_t+n^2Z_r \tag{4}$$

In figure 2*d* the secondary turns base has been used although this is purely a matter of preference. The total source impedance on this base is:

$$Z=\frac{Z_{aex}}{n^2}+Z_{tr} \tag{5}$$

The "burden performance" of the device, or ratio and phase-angle regulation, may be expressed in the form:

$$E_b=E_{b0}-I_bZ \tag{6}$$

$$E_b=\frac{A}{n}E_h-I_bZ \tag{6a}$$

where the no-load voltage E_{b0} and the source impedance Z are defined above.

DERIVATION OF EQUIVALENT CIRCUIT, FIGURE 2 (dotted capacitor used)

The capacitance divider and transformer exciting impedance can be converted as before to an equivalent series circuit having internal voltage E_s and impedance Z_{aex} defined as follows.

$$Z_{ex}=\frac{Z_cZ_x}{Z_c+Z_x} \tag{7}$$

$$E_s=\frac{Z_{ex}}{Z_a+Z_{ex}}E_h \tag{8}$$

$$Z_{aex}=\frac{Z_{ex}}{Z_a+Z_{ex}}Z_a \tag{9}$$

Combining the source with the transformer leakage impedance, the new source impedance Z_{aext} is defined, the internal voltage being still given by (8)

$$Z_{aext}=Z_{aex}+Z_t \tag{10}$$

On the primary turns base the adjusting capacitor has an impedance n^2Z_s. Converting the potentiometer circuit formed by Z_{aext} and n^2Z_s by a series circuit, according to Thevenin's theorem, an internal voltage nE_{b0} and impedance Z_{aexts} are obtained.

$$nE_{b0}=\frac{n^2Z_s}{n^2Z_s+Z_{aext}}E_s$$
$$=\frac{n^2Z_s}{n^2Z_s+Z_{aext}}\cdot\frac{Z_{ex}}{Z_a+Z_{ex}}E_n \tag{11}$$

or

$$nE_{b0}=AE_h \tag{12}$$

where

$$A=\frac{n^2Z_s}{n^2Z_s+Z_{aext}}\cdot\frac{Z_{ex}}{Z_a+Z_{ex}} \tag{13}$$

Also

$$Z_{aexts}=\frac{n^2Z_s}{n^2Z_s+Z_{aext}}Z_{aext} \tag{14}$$

This term is essentially capacitive in cases where the adjusting capacity is used at all, and corresponds roughly to z_{acx} of figure 2*d*. Combining it in series with the reactor, the over-all source impedance Z becomes on the secondary turns base:

$$Z=\frac{Z_{aexts}}{n^2}+Z_r \tag{15}$$

and the burden performance characteristic is:

$$E_b=E_{b0}-I_bZ \tag{16}$$

or

$$E_b=\frac{A}{n}E_h-I_bZ \tag{16a}$$

where A and Z are defined by equations 13 and 15.

Appendix II. Symmetrical-Component Resolution

(Refer Also to Appendix I)

Nomenclature

E —Voltage
a, b, c —Phases
1, 2, 0 —Sequences
h —High-voltage line
p, u, v, w—Turns or turns base designation

Examples: E_{u2}, negative-sequence voltage, line-to-neutral at location indicated in figure 4 on turns base u; E_{h1}, positive-sequence component of the set E_{ha}, E_{hb}, E_{hc}.

In figure 4 the derivation of sequence networks on the particular turns base u follows well-known theory. The ratio n used in determining Z and the applied voltage should include not only the main transformer, but also the auxiliary transformer from p to u. The auxiliary transformer when liberally designed may be treated as a "perfect" transformer of negligible exciting and leakage impedances or if not negligible it may be treated as a three-winding transformer since not more than three windings are involved in any sequence network. The zero-sequence impedance on turns base u due to the load Z_{bw} is in general $\frac{Z_{bw}}{3}\left(\frac{u}{w}\right)^2$ which for the particular ratio shown is $Z_{bw}/9$.

The corresponding general expression for E_{bw} is

$$E_{bw}=3\frac{w}{u}E_{u0} \tag{17}$$

Under both normal and fault conditions the voltage E_{ha}, E_{hb}, E_{hc}, and hence the voltages E_{h1}, E_{h2}, E_{h0}, will be fixed rigidly by the power-system constants and the type and location of fault.

The corresponding burden sequence voltages are determined from the sequence networks, figure 4*c*.

$$E_{u1}=\frac{Z_{b1}}{Z+Z_{b1}}\frac{A}{n}E_{h1} \tag{18}$$

$$E_{u2}=\frac{Z_{b2}}{Z+Z_{b2}}\frac{A}{n}E_{h2}=\frac{Z_{b1}}{Z+Z_{b1}}\frac{A}{n}E_{h2} \tag{19}$$

$$E_{u0}=\frac{Z_{b0}}{Z+Z_{b0}}\frac{A}{n}E_{h0} \tag{20}$$

From these the burden line-to-neutral voltages are obtained

$$E_{ua}=\frac{A}{n}\left[(E_{h1}+E_{h2})\frac{Z_{b1}}{Z+Z_{b1}}+E_{h0}\frac{Z_{b0}}{Z+Z_{b0}}\right] \tag{21}$$

$$E_{ub}=\frac{A}{n}\left[(a^2E_{h1}+aE_{h2})\frac{Z_{b1}}{Z+Z_{b1}}+E_{h0}\frac{Z_{b0}}{Z+Z_{b0}}\right] \tag{22}$$

$$E_{uc}=\frac{A}{n}\left[(aE_{h1}+a^2E_{h2})\frac{Z_{b1}}{Z+Z_{b1}}+E_{h0}\frac{Z_{b0}}{Z+Z_{b0}}\right] \tag{23}$$

Adding and subtracting a zero-sequence term:

$$E_{ua}=\frac{A}{n}(E_{h1}+E_{h2}+E_{h0})\frac{Z_{b1}}{Z+Z_{b1}}-$$
$$E_{h0}\frac{Z_{b1}}{Z+Z_{b1}}+E_{h0}\frac{Z_{b0}}{Z+Z_{b0}}$$
$$=\frac{A}{n}\frac{Z_{b1}}{Z+Z_{b1}}E_{ha}+$$
$$E_{h0}\frac{A}{n}\left[\frac{Z_{b0}}{Z+Z_{b0}}-\frac{Z_{b1}}{Z+Z_{b1}}\right] \tag{24}$$

Similarly

$$E_{ub}=\frac{A}{n}\frac{Z_{b1}}{Z+Z_{b1}}E_{hb}+$$
$$E_{h0}\frac{A}{n}\left[\frac{Z_{b0}}{Z+Z_{b0}}-\frac{Z_{b1}}{Z+Z_{b1}}\right] \tag{25}$$

$$E_{uc}=\frac{A}{n}\frac{Z_{b1}}{Z+Z_{b1}}E_{hc}+$$
$$E_{h0}\frac{A}{n}\left[\frac{Z_{b0}}{Z+Z_{b0}}-\frac{Z_{b1}}{Z+Z_{b1}}\right] \tag{26}$$

Equations 24, 25, and 26 show that with zero-sequence voltage present, and zero- and positive-sequence burdens unequal, the ratio of high to burden line-to-neutral voltages is not a constant, but depends on the zero-sequence voltage, the difference in burdens (Z_{b0} and Z_{b1}) and the stiffness of the device (value of Z). If Z is small, both terms in the parenthesis approach one resulting in a relatively small voltage error. Also if the positive- and zero-sequence burdens are made equal the voltage error can be eliminated.

The delta voltages may be obtained by subtracting equations 24, 25, and 26 in pairs and defining the high line-to-line voltage as

$$E_{hab}=E_{ha}-E_{hb} \tag{27}$$

$$E_{uab}=E_{ua}-E_{ub}=\frac{A}{n}\frac{Z_{b1}}{Z+Z_{b1}}E_{hab} \tag{28}$$

$$E_{ubc}=\frac{A}{n}\frac{Z_{b1}}{Z+Z_{b1}}E_{hbc} \tag{29}$$

$$E_{u0a} = \frac{A}{n}\frac{Z_{b1}}{Z+Z_{b1}}E_{h0a} \qquad (30)$$

Equations 28 to 30 show that the ratio of high to burden delta voltages is a constant independent of the residual burden, the system constants, and type of fault.

The residual voltage is

$$E_{bw} = 3\frac{w}{u}E_{u0} \qquad (31)$$

and from equation 20

$$E_{bw} = 3\frac{w}{u}\frac{Z_{b0}}{Z+Z_{b0}}\frac{A}{n}E_{h0} \qquad (32)$$

Equation 32 shows that the ratio of high zero-sequence voltage to residual voltage is a constant independent of the amount of delta burden, the system constants, and the type of fault.

Appendix III. Transients

The equivalent circuit, figure 2*d*, with the slight approximation of ignoring Z_2, is also an equivalent under transient conditions. It shows that the device and its connected burden may be treated as a series circuit of *R*, *L*, and *C*, provided the burden approximates a resistance or resistance and reactance in series. Tests indicate that this is generally the case unless burden-power-factor-correction capacitors are used.

It is well known that the *R*, *L*, *C* series circuit is oscillatory only if

R^2 is less than $\frac{4L}{C}$

or if

R^2 is less than $4X_LX_C$

where X_L and X_C are the inductive and capacitive reactances at any arbitrary frequency such as 60 cycles. If R^2 is greater than given by this value, the circuit is not oscillatory but "dead beat."

Nonoscillatory Case

R^2 is greater than $4L/C$.

The solution for current *i*, as a function of time *t*, for pure resistance burden with fault occurring at the instant of current zero (which coincides with burden voltage zero) is a unidirectional surge as follows.

Burden Current

$$i = A\left(\epsilon^{\frac{-t}{T_1}} - \epsilon^{\frac{-t}{T_2}}\right) \qquad (33)$$

where

$$A = \frac{\sqrt{2}E_0}{L}\frac{T_1T_2}{T_1-T_2} \qquad (34)$$

in which E_0 is the root-mean-square capacitor voltage prior to the fault.

Time Constants

$$T_1 = \frac{2L}{R-\sqrt{R^2-\frac{4L}{C}}} \text{ seconds} \qquad (35)$$

$$T_2 = \frac{2L}{R+\sqrt{R^2-\frac{4L}{C}}} \text{ seconds} \qquad (36)$$

T_1 is the long-time constant determined largely by *RC*, and fixes the decay of the surge. T_2 is the short-time constant determined largely by L/R and determines the shape of the front of the wave. The exact values are given by equations 35 and 36.

The burden voltage following the fault is:

Burden Voltage

$$e_b = iR_b = R_bA\left(\epsilon^{\frac{-t}{T_1}} - \epsilon^{\frac{-t}{T_2}}\right) \qquad (37)$$

This is a unidirectional surge of voltage which reaches its crest in a time:

Time to Crest

$$t_m = \frac{T_1T_2}{T_1-T_2}\log\frac{T_1}{T_2} \qquad (38)$$

Crest Value

$$e_b \max = R_bA\left(\epsilon^{\frac{-t_m}{T_1}} - \epsilon^{\frac{-t_m}{T_2}}\right) \qquad (39)$$

Oscillatory Case

R^2 less than $4L/C$.

The solution for fault occurring at the zero point of the voltage wave with pure resistance burden is an oscillation of the form

$$e_b = e_{b0}\epsilon^{\frac{-t}{T}}\sin 2\pi f_0t \qquad (40)$$

where e_{b0} is the crest value of steady-state voltage prior to the fault.

The characteristic factors are given as follows:

Natural Frequency

$$f_0 = \frac{1}{4\pi L}\sqrt{\frac{4L}{C}-R^2} \text{ cycles per second} \qquad (41)$$

Time Constant

$$T = \frac{2L}{R} \text{ seconds} \qquad (42)$$

Ratio of Successive Peaks or Decrement (Refer to figure 5)

$$D = \frac{e_{b2}}{e_{b1}} = \epsilon^{\frac{-1}{2Tf_0}} = \epsilon^{\frac{-R}{4Lf_0}} = \epsilon^{\frac{-\pi R}{2X_{L0}}} \qquad (43)$$

where the inductive reactance at natural frequency is X_{L0}.

Successive peaks are one-half cycle apart whereas the initial peak occurs but one-fourth cycle after the fault. The first peak is:

First Peak

$$\frac{e_{b1}}{e_{b0}} = \sqrt{D} \qquad (44)$$

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Discussion

H. A. P. Langstaff (West Penn Power Company, Pittsburgh, Pa.): The West Penn Power Company has made extensive use on the 132-kv transmission system of the bushing-type potential device closely allied in characteristic operation with the pedestal or suspension-type capacitor potential device, none of which have been used. Thirty-six bushing potential devices are now in active service located at seven 132-kv substations. The evident economy in cost of apparatus and in simplification of structure layout has well warranted the selection and use of this equipment. Purchase and installation has extended from 1927 to the present year; the two original devices purchased in 1927, rebuilt and modernized, are still in service.

The uses to which the devices have been put are as follows:

1. To obtain potential indication and source of synchronizing potential for incoming 132-kv lines. In two instances synchronism is checked from a device on one line to a device on another line. In other instances it is from a line device to a standard potential transformer. Where a phase difference occurs such as in synchronizing from the phase-to-ground-connected device to a phase-to-phase-connected potential transformer it has been compensated for by adjusting the phase angle of the potential device, a very convenient and wholly satisfactory method.

2. To obtain potential for directional phase relays and with the use of an auxiliary star-series potential transformer for directional ground relays.

3. To obtain zero-sequence potential for directional ground relays. In this instance a three-phase potential unit was constructed composed of two open-delta-connected standard 132-kv potential transformers, to the three external 132-kv bushings of which were attached three bushing potential devices. The potential transformers are used for directional phase relays and the bushing potential devices supply zero-sequence voltage for directional ground relays. Two of these complete three-phase units are installed at Charleroi substation, one on each section of the 132-kv bus. To each unit is connected the phase and ground protective relays of four incoming 132-kv lines. These units, designed by West Penn Power Company and assembled by the Westinghouse Electric and Manufacturing Company constituted an economical and satisfactory means of obtaining the desired result using potential transformer cores available for that purpose. The tanks are large enough to take a third potential transformer core.

4. At the Kiski Valley substation three devices on a 132-kv breaker are connected to detect zero-sequence potential and to cause the breaker to trip, if a 132-kv ground fault persists for a period exceeding the time setting of a voltage relay. (The 132-kv bank winding is delta connected at this substation.)

The first potential device was put in service on May 10, 1928, and was used for 132-kv line potential indication and for synchronizing. An article by the writer in the *Electrical World* of November 24, 1928, described this application. On April 6, 1938, a series of 132-kv ground-fault tests was made on a complete directional phase and ground relay application at Washington substation where two incoming 132-kv lines connected to a bus from which power was distributed through two 15,000-kva banks. One bushing on the line side of each line breaker was equipped with a potential device for line potential indication and for synchronizing. Three devices on the bus side bushings of each breaker supply potential to phase and ground protective relays. Oscillograph records were taken by the Westinghouse company and the analysis proved the acceptability of the potential devices for protective work.

Troubles and poor performance which were experienced on the devices purchased during their early stage of development were due almost wholly to causes originating in the lead-in cables. Corona deterioration occurred in the cables and breakdown occurred in the bayonet due to moisture entering through poorly made joints. About 20 cables were replaced in 1931 and some of them have given trouble since that time from the same causes. Due to this experience bayonet and cable improvements have been made and the present connected bayonet and cable gives promise of being free from the past troubles.

A special star-series auxiliary potential was designed for West Penn Power Company use in order that zero-sequence potential might be obtained and it has been used with complete success with the potential devices. In figure 4*a* of the paper under discussion is shown an auxiliary transformer, apparently now an integral part of the potential device, which makes available without any external apparatus voltage for line-to-neutral burden, line-to-ground burden, and residual burden. The Westinghouse company is to be congratulated upon this improvement.

The performance on the West Penn Power Company 132-kv system of the potential devices has been entirely satisfactory so far as electrical characteristics are concerned. They have not been called upon to operate with relays of the impedance type or of modern high-speed nature. Hence many of the problems and their solution discussed in this paper by Messrs. Harder, Langguth, and Woods have not been encountered, sufficient time of relay operation having obviated the effect of transient variations.

J. D. Laughlin and **T. M. Blakeslee** (Bureau of Power and Light, City of Los Angeles, Calif.): From an operating standpoint, it is gratifying to read a paper demonstrating such complete concordance of performance with theory as is disclosed in the paper by Messrs. Harder, Langguth, and Woods.

The protection of important transmission lines with high-speed relays has developed to a point where it is highly desirable, if not absolutely necessary, that potential sources, as well as current sources, for the relays be obtained from devices inside the protected circuit.

Instrument potential with sufficient accuracy for relaying purposes can be obtained from capacitor potential devices and the capacitors can also be used as coupling devices for carrier-frequency circuits. The dual functions of these devices and their lower cost justifies their use in many installations that might be questioned if standard potential transformers had to be used.

Since the most important use of these devices is for obtaining relay potential, and as relays function only under fault conditions on a system, it seems highly important that these devices be designed to reproduce accurately the primary voltage conditions when the system voltage is low and to remain stable when surges appear on the line.

As explained in the paper, there are many variations of the device circuit, some of which are not adaptable to all applications. The oscillograms in the paper show that for some secondary circuits with resonant characteristics the secondary voltage may continue for a time after the interruption of the primary voltage. This condition is undesirable for applications using relays with potential restraint coils that are expected to function in less than one cycle. The accurate predetermination of performance is necessary to select the proper type of device for specific applications.

The development of potential devices with multiple secondary voltages for obtaining delta, star, and residual voltages has simplified relay installations.

Prior to the application of potential devices on the Boulder transmission lines, instability, due to the parallel circuit made up by the auxiliary or tap capacity and the device transformer, was discovered in certain capacitor potential devices by our testing laboratories. This was corrected, however, before extensive use of the units, with the result that their performance in service has been satisfactory. This unstable phenomenon is that referred to by Mr. Clem (reference 6) and was corrected by designing the device transformer with low magnetic density so that the circuits approach the linearity assumed in the mathematical analysis presented in the paper under discussion.

The Los Angeles Bureau of Power and Light has accepted the increasing popularity of capacitor potential devices. All 287,500-volt lines are completely equipped to provide potential for relays, meters, indicating lamps, and for synchronizing. The capacitors are also used to couple carrier-frequency circuits to the lines, there being three carrier-frequency circuits operating over each transmission circuit. The bulk of the 110-kv and 132-kv lines are also fully equipped, and there are some installations on 34.5-kv lines.

Since the potential device bids fair to take the place of the more expensive potential transformer in every application, except that of power metering, some standardization of nomenclature would be desirable. The present paper points out the ambiguity of the terms "resonant" and "nonresonant." The capacity, which is in series with the line, is identified by the authors of this paper as the "stack capacity," whereas other authors refer to this as the "coupling capacity." The capacitor in parallel with the potential device is called the "tap" and "auxiliary" capacity, respectively, by different authorities.

The ability of the manufacturer to predict accurately the performance of these devices, as disclosed in this paper, will be of great assistance to application engineers by permitting them to take full advantage of their uses, select the proper type of devices for individual applications, and use them with confidence.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): Successful relay application requires complete knowledge of the characteristics of the sources of supplies of current and potential. This paper dealing, as it does, with a frequently used source of potential is of interest to all relay engineers. The fact that it recognizes the importance of the transient as well as the steady-state conditions of potential devices should make it of increased value.

The requirements for a supply of relay potential may be very briefly summarized as follows: For directional relays the phase-angle error should be small and reasonably constant; for distance relays or other relays using voltage restraint the ratio error should be small. Both types of relays of modern construction are precision instruments carefully designed to operate correctly under certain assumed conditions.

Let us consider for a moment what has been shown in the paper by Mr. Harder and his associates. Figure 8*a* shows that several half cycles of apparently normal frequency are required for one standard potential device secondary voltage to reach zero after the primary voltage has been made zero. Other illustrations show various transients of which some are negligible and others persist for several oscillations. These latter show frequencies which vary from half normal to more than twice normal.

The first type of transient, that in which there is no change in frequency, but only a time delay before reaching a new level, will in general have only the effect of slowing up the relay response. The second type, that in which there is a change in frequency, must be separately considered for different types of relay elements. As already indicated, the relays are designed for precision operation under certain assumed conditions, one of which is that energy is supplied at rated frequency. It is, of course, obvious to all of us that a modern high-speed directional relay energized with current at normal frequency but with a voltage of half normal frequency as shown in figure 8*f* would respond erratically and oscillate each half cycle instead of producing its expected steady torque in one direction.

Other types of relays will, of course, respond differently and each kind must be separately investigated. Some relays have circuits which are carefully tuned for normal frequency and these cannot help but be affected by changes in frequency.

The ideal potential supply, therefore, would seem to have as its first requirement a low and fixed phase-angle error under transient as well as steady-state conditions; thus any frequency shift is prevented. As a second requirement, of course, would be a low and fixed ratio error but if there was a transient error which resulted in a secondary voltage which at times was higher than expected (as in figure 8*a*), this could be used except in rare cases where the slight increase in over-all time of relay operation was of the utmost importance.

The authors have concluded that a low burden which is noninductive will give the best results and that burden-paralleling capacitors should be avoided. All relay potential coil circuits are inductive and to obtain the noninductive load, it is necessary either to especially design the relay elements with series capacitors to obtain the noninductive circuit for potential device applications only, or if a standard relay applicable to other sources besides the potential device is used, then the noninductive characteristics of the load can only be obtained by the use of paralleling capacitors. We should not overlook the fact, at this part of the discussion, that many relays, which have what appears a noninductive burden, obtain this through the use of internally mounted capacitors. This paper, while pointing out in its theoretical considerations how to obtain the desired results, fails to complete the job by insertion of tabular data which the average application man could use to determine whether or not his particular case would have trouble from transients.

I was rather surprised to note in appendix III that the equivalent circuit under transient conditions was set up by making the approximation of omitting Z_x. Since Z_x represents the magnetizing or exciting component, it is somewhat surprising to find this element ignored in any transient study. It may be that the potential device is greatly different from all other circuits containing an iron-core element, but past experience certainly indicates that the variations in the value of Z_x under various transient conditions would play a very large part in determining the over-all device performance. I will be glad to have the authors' comments on what recorded data they have which justifies this assumption.

Ralph Higgins (The Ohio Brass Company, Barberton, Ohio): Secondary transients have long been recognized as one of the inherent characteristics of capacitance potential devices and the authors additional contribution to this problem is indeed a worthy effort and quite instructive. To reduce the transient effect, the authors of this paper have shown that the best results can be expected when the burden requirements, both the resistive and the inductive components, are reduced to a minimum. While this is quite true, an impression is apt to be conveyed that one must limit the burden requirements when desiring to operate high-speed relays from capacitance potential devices. Thus, it would seem that the economies effected by the use of this potential device would be questionable if one must limit the available output from this type of equipment which already has a limited output. Also, it is noted that the authors have stressed the effect of the burden-paralleling capacitance generally used to adjust the phase-angle relation and to correct the power factor of the burden.

Figure 1 of this discussion shows a simple modification of the usual capacitance-potential-device circuit which may be employed very effectively to dampen out any undesired transient oscillations, and at the same time, this scheme requires only a slight reduction in the available output. Quite a few successful installations have been made of this scheme within the last few years.

This damping effect is accomplished by means of two adjusting resistors, one in parallel with the burden, the other in parallel with the transformer secondary. The usual adjusting capacitor, instead of being connected directly in parallel with the burden, as is the usual practice, is connected from one side of the burden to a higher tap on the transformer secondary. Hence any oscillatory circuit that may be set up in series with this capacitance will also be in series with the transformer winding; and thereby can be made nonoscillatory or deadbeat by means of the resistor paralleling this winding.

Figure 2 shows a few typical oscillograms taken during a three-phase laboratory setup, using 138-kv bushing potential devices energized to full potential, to simulate double line-to-ground faults close to the station. This type of fault, perhaps, presents the most hazardous condition resulting from transients upon the operation of high-speed directional line relays.

Oscillogram *A*, taken of the voltage across the relay on the short-circuited secondary phase, illustrates the effectiveness of the damping accomplished with the potential devices loaded to the full rated output, and with a highly inductive burden. While the transient as shown by this oscillogram is not completely damped within the first cycle, and therefore the transient performance may not be ideal, nevertheless, repeated tests show that successful relay operation could be expected from the arrangement.

Oscillogram *B* illustrates the improvement accomplished by reducing only the inductive component of the burden, and oscillogram *C* that accomplished by a further reduction of both the resistive and inductive components.

We are in full agreement with the authors' objection to the classification of various types of potential devices as either resonant or nonresonant. The term "resonant" is usually associated with a circuit requiring accurate tuning. To obtain the desired adjustment with the network, as shown in figure 1, the potential device can be given an in-phase adjustment, and yet no part of the network need be in resonance at the normal operating line frequency. Also, with the same set of adjusting impedances required for an in-phase adjustment, it is possible to obtain practically any other desired phase-angle relation from 30 degrees lagging to 120 degrees leading with very little change in the available output. Figure 3 is a schematic diagram of such an arrangement. The adjustments requiring a large lead angle are accomplished by simply connecting the adjusting reactor in parallel with the burden. Since the one type of potential device may be made applicable to all normal requirements, it naturally may not come under the nomenclature of resonant or nonresonant.

Another important advantage gained by the use of this network scheme is that numerous tap connections on the transformer are not necessary to meet the normal adjustment requirements of the potential

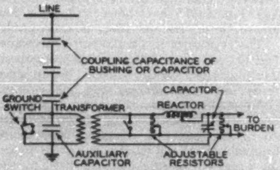

Figure 1

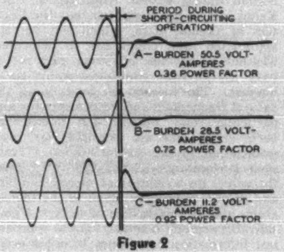

Figure 2

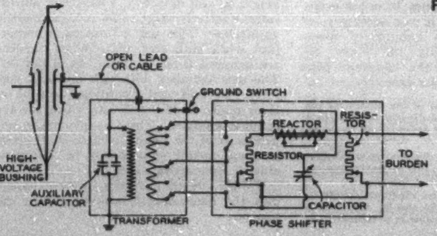

Figure 3

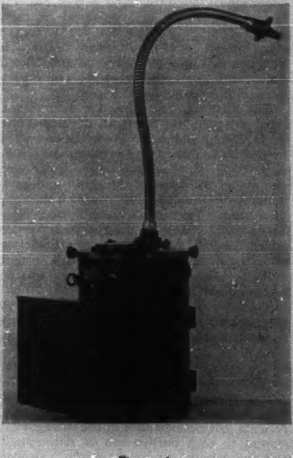

Figure 4

device. This feature readily permits the mounting of the various adjusting impedances in a small cabinet separate from the transformer housing. Figure 4 shows the improved standard potential device used in connection with capacitance tap bushings. Since only three leads are used between the transformer and the adjusting network, it is sometimes found convenient to mount this cabinet in the station close to the burden.

C. A. Powel (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The potential devices referred to in this paper are those used in connection with transformer or circuit-breaker bushings or with coupling capacitors to obtain potential from high-voltage circuits without the use of the more usual but more expensive potential transformer. The Westinghouse company first devised these capacitance transformers back in 1928 by the simple process of tapping the outside layers of their well-known condenser bushings. The low-voltage tapped section of the capacitor supplies a network device consisting of a small transformer, adjustable reactors and capacitors variously connected, depending on what it is to be used for.

With the advent of high-speed relaying, it was found that the potential devices built for other purposes did not give entirely satisfactory results with such relays, which operate in from one to three cycles. When the line potential fell, the secondary voltage in these devices was found not to follow immediately, and, moreover, it might oscillate about the new level for several cycles. If it happened that the line potential went to zero, the secondary voltage might even overshoot so that the relay obtained an indication of voltage in the wrong direction.

In the course of solving this difficulty, the authors developed an analytical method of investigating what happens in the devices under transient as well as steady-state conditions. To date, such a tool has not been available. The analytical method can readily be carried out for a simple single-phase circuit, but when the circuit becomes involved by virtue of unbalanced faults, the analytical work becomes very burdensome if not almost impossible.

The authors, therefore, for such cases, have adopted the method developed by R. D. Evans and A. C. Monteith described in AIEE Transactions, volume 56, 1937, pages 695–703, of setting up the equivalent circuits on the network calculator and have actually applied the faults in which they are interested. They have found that they thus obtain reliable results and their studies afforded a basis for the design of a capacitor potential device capable of giving a correct voltage ratio for short time operations as fast as one cycle.

The steady-state analysis may also be complicated when delta, residual, and star-connected burdens are on the same device. The delta burden involves the positive- and negative-sequence circuits, the residual burden involves the zero-sequence circuit, and the star burden involves all three. Through the concepts of symmetrical components, the performance of the device with respect to each burden can be determined and the criteria set up to obtain a design giving uniform results for all burdens. The paper describes in detail how this is accomplished.

E. L. Harder, P. O. Langguth, and C. A. Woods: The several discussers have added valuable comments and supplementary information indicative of the rather widespread interest in the transient performance of potential devices.

1. Mr. Langstaff brings out a very important point which deserves emphasis, namely that with normal-speed relays the transient performance is relatively unimportant because the relay operating time extends beyond the transient period.

2. Messrs. Laughlin and Blakeslee have indicated that installations of capacitor potential devices are being made very generally on their important line projects and it may be noted that this is generally true of all major transmission projects of recent date. These discussers have further emphasized the desirability for standardization of nomenclature. The functioning of the over-all device may best be viewed as a double transformation, the first consisting of a capacitance potentiometer and the second consisting of the transformer. The entire potentiometer comprising two sections which we have called the "stack capacity" and the "tap capacity" constitute the coupling to the power circuit. Hence, these terms seem more significant than the words "coupling capacity" and "auxiliary capacity."

3. That the complete analysis of the transient performance involves consideration of the specific relay performance as well as that of the device is well brought out in the discussion by Mr. Kennedy. He points out that the obtaining of frequencies other than normal would be objectionable for relays carefully tuned to normal frequency. It is our feeling that relays dependent upon system frequency being accurately maintained are to be avoided in any case in view of departure of system frequencies from normal during system disturbances. Mr. Kennedy also has suggested that the insertion of tabular data, which the average application man could use to determine whether or not his particular case would have trouble from transients, would be of interest. The method of determining the natural frequencies and decrements for any particular relay burden and device has been given and an example has been worked out in the section headed, "Transient Performance—Series Circuit". Also, the values for a number of specific burdens and devices have been given in the oscillograms and summarized in table II. A considerable amount of similar typical transient test information had been included originally in the paper but it was necessary to delete this information because of space limitations. Summarizing the steps involved in determining the transient performance in any particular case:

1. Determine the constants of the device for the particular adjustment and of the connected burden and reduced to an equivalent series circuit as shown in figure 2.
2. Determine whether oscillatory or nonoscillatory, based on whether R^2 is less or greater respectively than 4 L/C.
3. If nonoscillatory, the time constants should be determined from equations 35 and 36, the time to crest from 38 and the crest value or overshoot from 39. This is based on the primary voltage falling to zero.
4. In case the circuit is oscillatory the natural frequency would be determined from equation 41, the ratio of successive peaks or the decrement from 43, and the first peak in the case of pure resistance burden from 44.

For the more complicated burdens and connections the a-c network calculator provides the most convenient method of solution.

Mr. Kennedy has expressed surprise that the term Z_e could be ignored in the transient analysis. It is true that to be perfectly general this term should be included. However, for the device described the exciting impedance is very high compared with the tap-capacitor impedance. It is only when saturation occurs that this impedance would fall to low enough value to affect results. The specially designed transformers used with the devices described in the paper operate at flux densities sufficiently low that saturation does not occur even under transient conditions. Tests on complete three-phase devices substantiate these conclusions.

4. Mr. Higgins has presented an interesting circuit utilizing two damping resistors, one in parallel with the transformer secondary and the other in parallel with the burden. Early in our development an analysis was made of these possibilities and it will be of interest to show from the theory given in the paper the results to be expected. Considering a high-power-factor burden such as a pure resistance, it is evident that the connection of a second resistor in parallel will lower the total R of the circuit and as shown in equation 43 will increase the ratio of successive peaks, which is opposite to the desired effect. Also since R^2 is smaller compared with $4L/C$, the circuit tends to become more oscillatory than before. With pure inductive burden, damping resistance at this location will introduce loss in the resonant circuit and reduce the ratio of successive peaks. Obviously actual burdens will generally fall between these two limits, but our analysis has indicated that down to quite low power factors the net effect is unfavorable.

The resistance in parallel with the transformer secondary will exercise a pronounced damping effect on the resonant circuit since it is connected across the high-voltage point of this circuit. However, it will also make the impedance Z_{acr} shown in figure 2*d* of the paper a resistance-capacitance branch rather than nearly pure capacitance. As a result it cannot be neutralized by the reactance Z_{tr} and, therefore, the vector I_bZ (figures 3*a*, *b*, *c*) becomes relatively large. The resulting steady-state performance of the device is affected as it is no longer a "stiff device". Changes in burden cause relatively large ratio and phase-angle variations. This will be especially important when both star and residual burdens are supplied from the same set of devices. The star voltages, although adjusted correctly for balanced conditions, will be in error under fault conditions which involve residual voltage. This is explained in appendix II of the paper. These effects may be small enough so as not to be objectionable in some applications.

5. Mr. Powel has very nicely summarized the main points brought out in the paper.

Progress of the Art in Electrical Machinery—1934–39

THIS REPORT on the progress of the art in electrical machinery covers the five-year period from 1934 to June 1939.

D-C Machinery

There have been no radical changes in the design of d-c machinery for many years. Since the advent of the commutating pole, development has been almost entirely to extend the limits of performance along known lines.

There has been a slow extension of the speed of the machines, particularly the exciters and dynamometers. The speed of dynamometers both in revolutions per minute and peripheral velocity has been higher than that of the exciters. The reason for this is that although the dynamometers are all capable of operating at high speeds, in actual practice they run very little at the top speeds. For exciters, the peripheral speeds of the cores of machines in service have reached 13,000 feet per minute with 10,000 feet per minute on the commutator. One manufacturer is now building 3,600-rpm exciters having a peripheral of 15,000 feet per minute.

There is evidence for need of development of d-c motors which can be started across the line. At the present time, the practical limit of 230-volt compound motors is about 7 $^1/_2$ horsepower at 1,750 rpm and in the 550-volt motors, the compound-wound three-horsepower is about the limit. Shunt-wound motors that may be line-started are very much smaller. There seems to be a definite need for the extension of line-starting practice to larger machines as soon as designs will permit.

On the adjustable-speed motors, that is, motors whose speed is controlled by weakening the field, the present maximum ratio is six to one. Most adjustable-speed applications are served by a four-to-one range. Reversing planers and certain other special applications have been successfully served for several years by six-to-one speed range by field weakening.

The use of glass insulation is being tried on various applications for d-c motors. At the present time, there has been no report of any pronounced disadvantage in the use of this type of insulation.

Transformers

Insulation impulse tests have been established, and levels for guarantees and tests proposed for use by the industry. This work is probably one of the principal features for this period. It is not yet quite complete as levels for steep-wave-front testing have not been established.

The standardization of transformer ratings on the basis of temperature rise without reference to ambient temperature has been proposed and very generally accepted.

Considerable progress has been made in recommending additions to the material given in AIEE Standard No. 100 entitled "Recommendations for the Operation of Transformers," dated 1930. These additions include curves for short-time overloads (1) for recurrent conditions and (2) for emergency conditions ranging in time from a few seconds (short circuit) to several hours. As a rough guide for operating by oil temperature, oil temperature limits are given for various loads.

This work has resulted in the adoption of a pamphlet entitled "Guides for Operation of Transformers" which is to be incorporated as an appendix to the Proposed American Standards for Transformers, Regulators, and Reactors" to be published soon by the American Standards Association. The work on these operating recommendations is being continued in order to determine whether different recommendations should be made for transformers operated with expansion tanks or with inert gas above the oil level.

The question of the possibility of operating transformers at higher temperatures when excluding oxygen from the oil by the use of an inert gas cushion, or the use of an expansion tank (conservator) has been under discussion. Further consideration may show that by excluding oxygen from the oil, higher temperatures may be permissible.

Mercury-Arc Rectifiers

The use of the multianode metal-tank mercury-arc rectifier is well established in several fields. There are at present approximately 500,000 kw of this type of rectifier in operation on railway properties in America. The units vary in capacity from 500 to 3,000 kw and in voltage from 500 to 3,000 volts direct current.

The outstanding railway rectifier installations recently finished and put in operation include the San Francisco-Oakland Bay Bridge Railway, where 20,000 kw of rectifier capacity was installed for voltages of 600 and 1,200 volts direct current and capacities up to 2,500 kw per unit; and for the City of Philadelphia for heavy-duty subway service, consisting of two 3,000-kw 630-volt rectifiers.

In recent years, the mercury-arc rectifier was introduced in electrolytic plants. Some of the larger installations were for chlorine, sodium, and aluminum production. At present, there are in use in this country over 175,000 kw of rectifier capacity supplying this industry. One of these plants has 20 rectifiers with a total capacity of 55,000 kw. These were put in commercial service in the latter part of February 1938.

Due to the fact that a concentration of 55,000 kw of rectifiers in one substation became a major load of the supply network, it became necessary to reduce the influence of the harmonics on neighboring communication systems. In order to overcome this difficulty, an arrangement was used which provided phase displacement of each unit, by means of autotransformers, by different amounts in order to increase the effective number of rectifier phases. The installation, considering all units as a whole, operates as a 60-phase system, and the harmonic currents generated by the rectifiers in the a-c network were reduced to such an extent that the effect of the rectifier load on the power and adjacent communication systems was practically eliminated.

Paper 39-135, prepared by the AIEE committee on electrical machinery, recommended by the AIEE committee on electrical machinery, and presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939. Manuscript submitted April 24, 1939; made available for preprinting May 10, 1939.

AIEE Electrical Machinery Committee, 1938–39: C. M. Laffoon, *chairman;* J. L. Hamilton, vice-chairman; P. L. Alger, J. E. Clem, J. H. Cox, C. M. Gilt, I. W. Gross, M. S. Hancock, F. E. Harrell, A. P. Hayward, C. C. Herskind, F. Ellis Johnson, L. A. Kilgore, O. K. Marti, V. M. Montsinger, T. H. Morgan, S. H. Mortensen, F. L. Moser, C. A. Muller, J. R. North, F. D. Phillips, H. V. Putman, P. H. Rutherford, C. C. Shutt, J. B. Swering, H. D. Taylor, B. R. Teare, B. Van Ness, Jr., and G. A. Waters.

Subcommittees and Chairmen, 1938–39: Induction machinery, G. A. Waters; transformer, F. J. Vogel; rating of electrical machinery and apparatus, F. E. Harrell; synchronous machinery, H. D. Taylor; test code for fractional-horsepower motors, J. L. Hamilton; mercury-arc-rectifier, O. K. Marti; d-c electrical machinery, M. S. Hancock; insulation-resistance, J. B. Swering.

This illustrates an inexpensive means of reducing the effect of rectifier harmonics by utilizing the large number of anodes available in a large installation for increasing the number of phases.

Development work has been done toward perfecting the use of energized grids to obtain voltage regulation, and to obtain rapid extinction of the arc during abnormal conditions such as short circuits and arc backs. By applying a negative potential to the grids, current from properly functioning anodes may be blocked and prevented from feeding into a fault, and by the use of auxiliary electronic tubes, or high-speed relays, this type of arc extinction may be accomplished in the order of one cycle.

The need for heat exchangers to overcome the corrosion problem in steel-tank mercury-arc rectifiers has been eliminated for many cases by soldering or welding copper cooling coils to the tank walls. Of course, this method does not eliminate heat exchangers where the water available deposits a prohibitive amount of scale, necessitating straight tubes for mechanical cleaning.

Table I

Number	Kilovolt-Amperes	Kilowatts	Customer	Manufacturer*	Date Installed
2	25,000	20,000	Louisiana Steam Generating Co.	W	1938
1	29,500	25,000	Connecticut Light and Power Co.	W	1938
1	31,250	25,000	Gulf States Utilities Co.	W	1938
1	25,000	20,000	Kansas Gas and Elec. Co.	W	1938
1	37,500	30,000	Boston Edison Co.	W	1939
1	25,000	20,000	Metropolitan Edison Co.	W	On order
1	43,750	35,000	Iowa Power and Light Co., Des Moines	AC	1938
1	31,250	25,000	Central Illinois Public Service Co.	AC	1938
1	25,000	20,000	Southern Indiana Gas and Elec. Co.	AC	1938
1	25,000	20,000	Madison Gas and Elec. Co.	AC	1938
1	18,750	15,000	City of Springfield, Ill.	AC	1938
1	31,250	25,000	Central Illinois Public Service Co.	AC	On order
1	31,250	25,000	Public Service Co. of Colorado	GE	1937
1	28,571	20,000	United Power Mfg. Co.	GE	1937
1	25,000	25,000	Monongahela West Penn Public Service Co.	GE	1937
1	25,000	20,000	N. Y. State Elec. and Gas Corp.	GE	1938
1	50,000	40,000	Hartford Elec. Light Co.	GE	1938
1	25,000	20,000	Ohio Edison Co., Mad River	GE	1938
1	37,500	30,000	Potomac Edison Co.	GE	1938
1	25,000	20,000	Public Service Co. of N. H.	GE	1938
1	25,000	20,000	Public Service Co. of Okla.	GE	1938
1	42,857	30,000	City of Jacksonville, Fla.	GE	1939
1	28,571	20,000	Houston Lighting and Power Co.	GE	1939
1	31,250	25,000	Consolidated Gas Elec. Light and Power Co. of Baltimore	GE	On order

* W—Westinghouse Electric and Manufacturing Company; AC—Allis-Chalmers Manufacturing Company; GE—General Electric Company.

The ignitor type of rectifier, or ignitron, constitutes the most radical departure from conventional practice in America during the past few years. Considerable attention has been given throughout the world to single-anode tank mercury-arc rectifiers, and the ignitron belongs to this class. This type of tube makes use of an ignitor which starts the arc in a tube by forming a cathode spot or a pool cathode at the beginning of each conducting period for a particular anode and permits the arc to extinguish during the nonconducting period. This type of operation has permitted the location of the anode directly over and relatively close to the cathode, with a reduction in the amount of shielding. This results in an appreciable reduction in arc voltage. Control of voltage, and arc extinction during faults, is accomplished by operating on small auxiliary thyratron tubes in the ignitor circuit, which control the time of firing.

Ignitrons are now in service in various industries. There are 13 units in mining service at 275 and 600 volts and in capacities from 300 to 500 kw. In electrochemical service, there are seven units at 280 and 600 volts and with capacities from 200 to 1,000 kw, and in railway service, there are two 600-volt 3,000-kw units. These latter have an anode rating equal to that of the largest multianode rectifier.

Recently, some units of permanently evacuated pumpless rectifiers have been installed, for lower-capacity requirements, at both 250 and 600 volts. Some of these are multianode glass-bulb mercury-pool rectifiers; some are metal-tank single-anode ignitrons; and others are metal-tank thermionic cathode phanatrons. These sealed-off rectifiers are attractive for small-capacity requirements because of the absence of vacuum-pumping auxiliaries. However, the need for periodic replacement must be balanced against objections associated with continuously operating vacuum auxiliaries.

Turbine Generators

The two principal items of progress in turbine generators during the past few years are (*a*) the development of the two-pole 3,600-rpm unit for ratings required by the central-station industry, and (*b*) the application of hydrogen as the cooling medium.

The use of higher temperatures and pressures has greatly increased the range of capacities of the higher-speed units into the field formerly held by 1,800-rpm units or even of 1,200-rpm units.

The higher rotational speed makes it possible to use smaller physical dimensions and weights for the turbine. This simplifies the design and construction problems introduced by the use of high pressures and temperatures of the steam.

Large-capacity air-cooled, 3,600-rpm turbine generators on order or in service are listed in table I.

The advantages of hydrogen as a cooling medium for turbine generators are:

1. Reduced windage, friction, and ventilating losses, because of the low density of the hydrogen gas. The ventilating losses are proportional to the gas density. Full-load efficiency of the generator may be 0.6 per cent or more higher than for the corresponding air-cooled machine.

2. Increased output per unit volume of active material, because of the high heat-storage capacity, thermal conductivity, and heat-transfer coefficients of hydrogen. This advantage of hydrogen cooling makes it possible to build turbine generators for higher ratings than are possible with air cooling.

3. Reduced maintenance expense, because of the freedom from dirt and moisture.

4. Increased life of the insulation on the stator winding, because of the absence of oxygen and moisture in the presence of corona.

5. Reduced windage noise, because of the low density of the gas.

Hydrogen-cooled turbine generators either in service or in manufacture are listed in table II.

With operating experience on a number of units and over a considerable period of time at hand, the initial stage of development of hydrogen-cooled generators is passed. There will no doubt be minor difficulties to correct as time goes on, but these will be of no more serious nature than are to be found in air-cooled generators. It will also be possible to simplify considerably the generator and its attendant parts in future machines as greater experience is gained with this

Table II

Number	Kilovolt-Amperes	Kilowatts	Customer	Manufacturer*	Date Installed
3,600-rpm units					
1	50,000	45,000	West Penn Power Co.	W	1938
1	58,889	53,000	Consolidated Edison Co. of N. Y., Inc.	W	1938
1	58,750	50,000	Philadelphia Elec. Co.	W	1938
1	43,750	35,000	Cincinnati Gas and Elec. Co.	W	1938
1	81,250	65,000	Consolidated Edison Co. of N. Y., Inc.	W	On order
1	43,750	35,000	Pa. Power Co.	W	1939
1	43,750	35,000	Columbus and Southern Ohio Electric Co.	W	1939
2	43,750	35,000	Georgia Power Co.	W	On order
1	43,750	35,000	Pa. Elec. Co.	W	On order
1	18,750	15,000	Wis. Elec. Power Co.	AC	1938
1	31,250	25,000	City of Lansing, Mich.	AC	On order
1	31,250	25,000	Dayton Power and Light Co.	GE	1937
1	50,000	40,000	American Gas and Elec. Co., Logan, W. Va.	GE	1937
2	66,667	60,000	American Gas and Elec. Co., Windsor station	GE	1— 1939 1—On order
1	58,889	53,000	Consolidated Edison Co. of N. Y., Inc.	GE	1938
1	37,500	30,000	New Orleans Public Service Co.	GE	1939
1	55,555	50,000	Public Service Elec. and Gas Co.	GE	1938
3	43,750	35,000	Consumers Power Co.	GE	On order
1	28,125	22,500	Ind. and Mich. Elec. Co.	GE	On order
3	50,000	40,000	Pacific Gas and Elec. Co.	GE	On order
1	50,000	40,000	Virginia Elec. Power Co.	GE	On order
2	50,000	40,000	Duke Power Co.	GE	On order
2	62,500	50,000	Public Service Elec. and Gas Co.	GE	On order
1	62,500	50,000	Philadelphia Elec. Co.	GE	On order
1	81,250	65,000	Consolidated Edison Co. of N. Y., Inc.	GE	On order
1,800-rpm units					
1	75,000	60,000	Duquesne Light Co.	W	1938
1	176,470	150,000	Chicago District Elec. Generating Corp.	GE	1937
1	81,250	65,000	Cincinnati Gas and Elec. Co.	GE	1938
2	68,750	55,000	Ford Motor Co.	GE	1939
1	60,000	54,000	Ind. and Mich. Elec. Co.	GE	On order
1	100,000	80,000	Central N. Y. Power Co. (Oswego)	GE	On order

* W—Westinghouse Electric and Manufacturing Company; AC—Allis-Chalmers Manufacturing Company; GE—General Electric Company.

type of machine. Cost of supplying hydrogen gas is very low, and it has been the experience in most stations that the additional attention on the part of the operators as compared to that for an air-cooled machine is very small. Hydrogen cooling will probably be adopted more and more for units of 30,000 kw and above where the additional cost of the gas-tight housing and gas control can be justified economically.

Synchronous Condensers and Motors

A novel synchronous condenser rated 25,000 kva at 3,600 rpm (W)* has been built for the American Gas and Electric Company with hydrogen cooling, and excitation from an ignitron rectifier. A 50,000-kva 50-cycle 600-rpm hydrogen-cooled synchronous condenser (W) was furnished the Southern California Edison Company. The design of this condenser permits operation at 60,000 kva and 60 cycles. Two 30,000-kva 1,200-rpm hydrogen-cooled synchronous condensers (GE)† were supplied to the City of Seattle and the Ohio Power Company, respectively, during the year, and very recently a 60,000-kva, 600-rpm hydrogen-cooled condenser (GE) has been ordered by the City of Los Angeles. Air-cooled condensers (AC)‡ rated at 15,000 kva, 900 rpm, and 20,000 kva, 720 rpm were furnished the Memphis Power and Light and the Illinois Northern Utilities Company respectively. A 25,000-kva condenser (W) was installed at the Massena plant of the Aluminum Company of America in 1937. A similar 15,000-kva air-cooled condenser (W) is now on order for the Florida Power and Light Company.

Three vertical-shaft synchronous motors out of an order for six were installed for the Metropolitan Water District of Los Angeles, rated 12,500 horsepower, 450 rpm (W), built for exceptionally severe starting duty; these are similar to machines rated 9,000 horsepower, 400 rpm (GE) and 4,300 horsepower, 327 rpm, (AC) previously installed on the same water supply project. In marine work, two turbine-electric tankers of unusual interest were placed in service by the Atlantic Refining Company, the *J. W. Van Dyke* and the *Robert H. Cooley*, each with a main-drive synchronous motor rated 5,000 horsepower, 90 rpm supplied with power by a turbine-generator of 4,500-kw capacity (GE), the output of the generators being used when in port for driving pumps for rapid loading and unloading.

Electric Couplings

Electric couplings are now coming into general use in geared ship-propulsion installations to provide the necessary flexibility to protect the gears from tortional vibration from the engine. In addition, they limit shocks in the system by having a definite maximum torque which they will transmit, this torque being independent of speed. They also can be disconnected instantaneously by interrupting their field current.

In construction, the form generally used consists of a salient-pole field member excited by direct current and a secondary member similar to that used on an induction motor. The secondary rotates with the field except for a small slip.

At the present time, one tanker has been equipped with two 700-horsepower (Elliott) couplings and there are on order 68,000 ship horsepower (W) in 24 couplings for use on vessels being built for the United States Maritime Commission.

Induction Motors

There has been a material increase in the number of high-speed induction motors built for high-pressure pump applications. The ratings of two-pole 60-cycle induction motors have reached 2,500 horsepower.

The noise level of high- and moderate-speed motors has been reduced to the point where it is well below that of a modern power house.

Explosion-resisting motors of cast or fabricated construction are now being built for ratings up to 600 horsepower, 3,600 rpm. Glass insulation is playing a part in the development of these motors.

The completion of two 1,000-horsepower 3,000-rpm totally enclosed motors (GE), ventilated by carbon-dioxide gas circulating through surface coolers, marks an important new trend in this field. This design provides security against explosion risks in sizes larger than can conveniently be made of the heavy explosion-resisting construction.

Frequency Converters

A 20,000-kw variable-ratio frequency changer (GE) of the Scherbius type built during the year was unusual in having the Scherbius equipment on the 60-cycle end. The induction machine of this set is a wound-rotor type with 24 poles, rated 27,000 kva, 300 rpm, 13,800 volts, and is designed for 0.8 power factor overexcited operation with a slip adjustable over a total range of one cycle. A 60,000-kva, 50–60 cycle 600-rpm hydrogen-cooled frequency changer (W) was built for the City of Los Angeles and installed late in 1936.

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* Westinghouse Electric and Manufacturing Company.

† General Electric Company.

‡ Allis-Chalmers Manufacturing Company.

Water-Wheel Generators

Most of the available water-power sites which are economically important from the standpoint of electric-power production and disposition alone have been developed. The majority of the hydroelectric developments that are being undertaken at the present time are justified economically for the production of electrical energy only when considered in connection with irrigation, flood control and navigation, or a combination of these factors. Undertakings of such broad scope are necessarily of a public nature and consequently have been carried on as complete or partial-public or governmental enterprises.

The principal improvement in the hydraulic turbine has been the development of the adjustable-blade propeller to improve the efficiency performance of the turbine under different water flow conditions. No major changes have been required or made in the design of generators driven by this type of prime mover.

A large number of hydraulic turbine driven generators has been purchased or placed in service. Two more Boulder Dam units rated 82,500 kva, 180 rpm (GE) were tested and placed in service, and two others (W) were built and ready for installation. The City of Los Angeles has purchased two additional 82,500-kva units (W) for installation at Boulder Dam. At Bonneville Dam two 48,000-kva 75-rpm generators (GE) were installed, and two more rated 60,000 kva (GE) were on order. For the Tennessee Valley Authority developments, two 40,000-kva 81.3-rpm units (W) went into service at Pickwick Dam; three rated 27,000 kva, 69.2 rpm (GE), under construction for Guntersville, have the distinction of having the largest diameter and highest overspeed of any generator so far designed of the overhung type; for the Hiwassee Dam, one 64,000-kva 120-rpm unit (W) has been ordered; and three units of 30,000 kva 75 rpm (AC) have been ordered for Chickamauga Dam. Interesting units for export include two horizontal machines of unusually high speed, rated 10,118 kva, 500-rpm, 25 cycles (GE) built for the government of Mysore, India, and three vertical machines rated 70,000 kva, 125 rpm (W), on order for the Sungari River development in Manchukuo. Among recent orders are the three 108,000-kva 120-rpm machines (W) for Grand Coulee, these having larger dimensions than the famous Boulder Dam units; and four rated 75,000-kva, 120 rpm (GE) for the Shasta development of the Bureau of Reclamation. Two 34,000-kva 120-rpm units (GE) and two 34,000-kva 120-rpm units (W) are under construction for Santee Cooper. Four 16,000-kva, 150-rpm units (W) for Grand River have been shipped. Four 20,833-kva $138^{1}/_{2}$-rpm units (W) for Appalachian Electric Power Company have been shipped. One 27,500-kva 120-rpm unit (W) has just gone into service on the property of Anglo Newfoundland Development Company.

Electrical Engineering and the Petroleum Refiner

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THE object of this discussion is to present a brief history of the use and development of electric power in the oil-refining industry and to indicate how refinery design and operation have been affected by recent developments in electrical engineering.

An oil refinery consists of equipment for the receipt and storage of crude oil, apparatus for converting it into salable petroleum products, and additional storage and distribution facilities for the finished products. The first and last of these classes of equipment are composed largely of tanks, piping, and pumps. In the second category is the refinery proper, which may be compared to a shop containing many machines, the refinery being composed of various processing units, each of which performs a definite task upon a certain crude or previously processed feed stock. The most important of the units carry out the process of fractional distillation, obtaining numerous products ranging from heavy tar or coke to light highly volatile hydrocarbons. Fractionation is often combined in a given unit with the supplementary processes of cracking, polymerization, etc.

A typical distillation unit consists of one or more furnaces, a battery of pumps, fractionating towers, smaller pressure vessels, heat exchangers, condensers, etc. Many of these units operate under pressure and temperature conditions comparable to those encountered in modern boiler plants.

Other units are required to condition the products of the fractionating process for sale; these consist of stabilizer plants, chemical treating plants, blending units, dewaxing plants, etc. A third type of unit is the by-product plant which may produce anything from insecticide to compounds used in cable insulation. In addition to the refinery proper there must be auxiliary plant equipment for supplying steam, electric power, compressed air, repair and construction services, office facilities, etc.

Safety Precautions to Be Observed

The refining of petroleum involves the safe handling of inflammable and corrosive liquids and gases. The major part of refinery electrical equipment is therefore designed to be suitable for use with equipment performing such operations. Suitable electrical equipment is approved by the Underwriters' Laboratories for use in what is termed class 1, group *D*, hazardous locations.

Apparatus, to be classed as explosion resisting, must be designed so that it is not possible for an internal ignition of explosive vapors to be propagated to the outside atmosphere. Sturdy cases with ground-joint heavily bolted flanged covers are required or the apparatus must be deeply immersed in suitable steel tanks filled with oil. Screwed joints must have at least five tight threads. Conduit entrances to spark-producing apparatus must be sealed with compound in an approved sealing chamber to prevent propagation of an explosion along the conduit

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system; long conduit runs are sealed at intervals for the same purpose.

The presence of sulphurous gases, furnace smoke, and salt air (since many refineries are located on seacoasts) makes it necessary to use porcelain insulators of much higher rating than the service voltage—15,000-volt insulators are commonly used for 2,300-volt service. In addition, all electrical apparatus must be protected to the greatest possible degree against corrosion, or designed of suitable materials to withstand the corrosive effect.

Type of Load

The refinery load is one that usually offers a unique opportunity for economical power generation. Waste refinery products are generally available as a cheap fuel in both liquid and gaseous forms, the need for process steam makes possible an economical station heat balance even with a restricted supply of condensing water, the use of dual steam-electric-driven pumps and other equipment furnishes a means of adjusting the heat balance, and finally and perhaps of most importance, the high percentage of the generated power used for driving synchronous motors and fully loaded induction motors which operate for long periods without shutdown makes for high load factors and high power factors. Continuous operation of a refining unit for 30 days is not unusual and runs of six months or longer are expected for some processing operations. There are seasonal variations in total load on the generating equipment due to seasonal variations in demand for various products (see figure 1), however, these are not ordinarily so great that an economical heat balance cannot still be maintained. Daily load variations are very small since the lighting load is small and the power load is continuous.

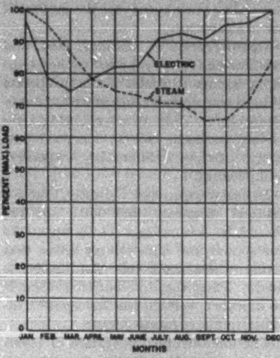

Figure 1. Monthly variation of steam and electrical load in a refinery

Steam and Compressed Air

Process steam is generally supplied to the refinery from the boiler plant at pressures in the vicinity of 125 pounds gauge, and is used in considerable quantities for supplying distillation heat, for oil-burner atomization, stripping operations, and for gas freeing of equipment. Certain engines and turbines are steam driven from the 125-pound mains for driving fans, pumps, and compressors, especially in older units. These units usually exhaust to the low-pressure steam main at about 10 pounds pressure. Such low-pressure steam is used in heating buildings and heavy-oil storage tanks, and is also employed to maintain a low viscosity in pipe lines circulating heavy oil through the refinery by carrying a small steam line next to the oil line under an insulating jacket. Steam is also used to some extent for fire protection.

A supply of compressed air is necessary for the pneumatic tools used in refinery maintenance. At every "turn-around" or shutdown period, the tubes of tubular furnaces are cleaned by air-driven tools. Drills, grinders, tube-expanders, jackhammers, and riveters, are in use. The necessary compressed air is supplied in the typical refinery by synchronous-motor-driven compressors of large size.

Electric Power

The introduction of high-pressure high-temperature steam equipment for industrial power-plant application and the development of the high-back-pressure turbine made refinery engineers more conscious of the merits of electric drive, especially from the standpoints of economy of operation and saving of space. The type of mechanical equipment used in refineries then underwent major changes as manufacturers of this equipment were called upon to develop new and entirely different apparatus than was used hitherto.

The high-back-pressure turbine was much in demand when first introduced as it supplied the large quantities of medium-pressure steam for driving slow reciprocating pumps and other relatively inefficient processing equipment. Then, as the opportunity occurred to replace worn out and inefficient steam equipment with electric drive, the need for large amounts of medium pressure steam decreased and correspondingly, the requirements for electric power increased with the result that use of condensing turbine again became practicable. However, the high-back-pressure turbine was soon followed by the very flexible bleeder-condensing turbine so that today the refinery engineer may select from a wide range of equipment to give him an efficient power plant tailored to fit the scheme of operation of his particular refinery.

The most popular generating voltage is 2,400 volts because it has a wide economical range of uses without transformers together with very good distribution characteristics. Other voltages such as 6,900 and 13,200 volts are being used in individual cases for specific reasons such as long-distance distribution which might be necessary for a very large refinery.

With the increase in power used at 2,400 volts, a new class of switching equipment had to be developed to handle safely the increased capacity generated at what today is considered a low voltage. This equipment had to embody the safety principles of utility-company equipment and yet be reasonably low in first cost. Such equipment is the present-day medium-interrupting-capacity short-time high-current-carrying-capacity high-speed oil circuit breaker with high-speed relays, all factory assembled in dead-front steel cubicles. Still a further development necessitated by the high currents in the present large low-voltage generating station was improvement in bus design, so that electrical engineering borrowed from structural practice the angle, channel, and box designs that we have today. The larger stations have main and synchronizing busses with reactor protection between generator bus sections or bus sectionalizing reactors to limit short-circuit currents to a reasonable figure in order to make use of economical types of switchgear.

Distribution System

A refinery distribution system is required to supply substations that are located to serve the different operating areas or load centers. In a large refinery, units are continually being added to or withdrawn from the refinery system so that the load centers are continually changing. The basic distribution scheme therefore must have maximum flexibility to meet the ever-changing requirements.

Depending on the size and layout of the particular refinery and its power sources, loop, radial, or combination loop-radial systems are used for the high-voltage distribution. The processes are such that a definite closely knit load center can be established for each unit, thus tending to simplify the secondary-distribution problem.

Overhead lines have been widely used for high-voltage distribution, but refineries are now following the modern trend to underground construction. This is highly advantageous from the standpoints of safety, reliability of service, and clearing the air to allow freedom in fighting fires, moving cranes, etc. Overhead distribution is retained where the economics of the situation so dictate. Substations are required to supply high voltage directly to feeders for large motor-driven compressors and pumps, to transformers for 440-volt power distribution to small- and medium-size motors, lighting transformers, and numerous miscellaneous services.

A typical substation consists of a small building and adjoining fenced-in transformer enclosure.

Substation switchgear in older installations was of the pipe-frame and panel type with panel-mounted instruments and controls and oil circuit breakers pipe-frame mounted behind the panels. Such equipment is being superseded by metal-clad and steel-cubicle-type switchboards in new construction; advantages being greater safety to personnel, greater reliability due to enclosure of bus bars and control circuits, and smaller space requirements.

Motors

Advances in pump design have been of great importance to refinery power engineers since this type of load far exceeds any other. Slow-speed reciprocating pumps with high steam consumption have been replaced to a great extent by high-speed space-saving centrifugal pumps with efficient direct-coupled motor drive.

When such pumps were first used in refineries, they were driven by open motors and the motors and pumps were separated by fire walls. Fire walls do not completely eliminate the fire hazard in all cases and are expensive and inconvenient in any case. The explosion-proof totally enclosed motor has been developed until today this type of motor is standardized in sizes up to 350 horsepower. It is reasonable in first cost and the maintenance on this type is far less than was experienced with the open type.

The extension of explosion-proof motors to include gearmotor and close-coupled motor and pump units has substantially contributed to the increased use of electricity.

Improvements in bearings, fans, and insulation have been applied to explosion-proof motors to enable them to operate at high temperatures in corrosive refinery atmospheres. These motors, being completely enclosed, can be mounted outdoors, thereby eliminating expensive pump houses.

Refinery experience indicates that for greatest economy individual motors should be low voltage (440) up to 75 horsepower and high-voltage above that rating where 2,400-volt distribution is used. When the distribution system is at a higher voltage the horsepower dividing line is, of course, higher.

The uninterrupted operation of essential pumps while the unit is "on stream" is of extreme importance to the refinery operator. In this case a dual drive is frequently used, that is, the centrifugal pump has the impeller shaft extended through both ends of the casing, one end being coupled to a motor and the other end to a steam turbine. If the motor full-load speed is 3,550 rpm, an automatic valve would be set to admit steam to the turbine at about 3,500 rpm. Should there be a power failure or other fault in the electric drive the turbine would automatically take the load until the motor could be again brought into use.

Individual enclosed induction motor units as large as 1,250 horsepower at 3,600 rpm have been developed for use in refining areas, the enclosures are of heavy steel plate, and cooling is accomplished by means of water coils mounted within the enclosures. The enclosures are airtight except for the shaft opening and are filled with inert gas at a pressure higher than atmospheric. The use of such motors is being extended to slightly or occasionally gaseous areas by omitting the inert-gas protection.

Synchronous motors are used in refinery work to a considerable extent for driving reciprocating compressors. These are used for air, refrigeration, and hydrocarbon gases.

The ease with which low-speed salient-pole synchronous motors may be designed and operated is the chief reason for their selection for compressor drive. In many instances the motor is installed between cranks of duplex machines in a manner analogous to the installation of shaftless induction motors in power tools, giving a valuable saving in space together with desirable mechanical characteristics. Compressor motors are usually large enough to require connection to the refinery high-voltage system but are not so large as to prevent full-voltage starting.

Where inflammable gases are handled or where refrigeration machinery is located in proximity to petroleum vapors, the motors are protected by fire walls or enclosed in an airtight steel casing with cooling air brought in through a duct from a gas-free area. Air is forced through the duct with an explosion-proof motor-driven blower having its starter interlocked with the compressor motor starter so that fresh air is supplied to the compressor motor before it can be started.

Motor Control

To obtain widest benefits from the previously discussed motor developments, it was necessary to obtain starting equipment especially designed for refinery service. Small low-voltage motors use standard air-break motor starters enclosed in cast-iron explosion-proof housings. Larger low-voltage motors are controlled by deep-oil-immersed starters of the across-the-line type. Satisfactory relays of both thermal and magnetic types have been developed for use under oil so that such equipment includes all features of non-oil-immersed equipment. Since the refinery distribution system is designed to carry large blocks of power, low-voltage motors are line starting except for a few of the largest sizes in refineries using distribution voltages above 2,300. Such starters are considered special.

The latest practice in motor control requires an air circuit breaker of the "arc dissipating" type in an explosion-proof housing at the motor branch junction. This breaker is depended upon for short-circuit protection and also serves as a disconnect.

An alternate practice is the use of oil-immersed starters combined in the same enclosures with oil-immersed fused switches. The necessary fuses are individually air sealed to prevent oil from interfering with their proper operation.

Since high-voltage motors are fed directly from a substation, the motor control equipment is usually located in the substation and is installed as a component part of the substation cubical switchgear. Approximately 50 per cent of these starters are required to be reduced-voltage type. Since such switchgear is required to have up to 50,000 kva interrupting capacity, the starter contactors are similar to modern oil circuit breakers in design. Remote push-button control is also used with these starters. As a late development the high-voltage starters are being constructed explosion resisting for certain locations, such starters have oil-tight circuit-breaker-type tanks and open-air-type control equipment under a diving bell immersed in oil.

The necessary breakers and starters for all the motors, other than high-voltage motors, in a given refinery unit are usually grouped in a switch house and mounted upon racks. The switch house may be a simple roofed structure without sides, usually enclosed in chain-link fencing. The starters are push-button operated from explosion-proof push-button stations located at the motors and/or in the unit control room.

Instruments

For proper operation, the temperature at a multitude of points in the unit must be indicated at the unit control house so that liquid flow in various lines may be adjusted to maintain process requirements. To observe temperature conditions at the hundreds of necessary points and to correlate this information at necessary intervals would be a Herculean task in itself were it not for the use of recording pyrometers. Resistance bulb thermometers are in limited use but the majority of temperatures are measured by iron-constantan thermocouples. These and other types of instruments are motor driven. Instruments which may be rated explosion resisting are a current development.

While the total electric load developed by such instruments is not large, the instruments of an oil refinery do represent a problem in maintenance and in investment cost.

Automatic regulation of oil flow in certain lines in accordance with unit temperature conditions is obtained from instruments. This requires the use of electrically operated valves ranging in size from fractional-inch solenoid valves to large-size motor-driven valves.

Shops

Oil refineries are usually equipped to maintain and repair equipment. The extent to which repair equipment is installed depends in a large degree upon the location of the refinery and the availability of adequate industrial facilities in the vicinity.

Where the quantity of the work permits, a refinery will have foundry, boiler, machine, blacksmith, carpenter, electrical, instrument, and pipe shops, all of which require electrical power. This load is supplied by 440-volt three-phase lines.

Welding

In recent years, following improvements in welding technique, the scheme of certifying approved welding personnel, and the application of radiography, the use of welding in the oil industry has increased rapidly and we now have welded tankage, pressure vessels, piping for pressures up to 1,500 pounds, structures, pipe supports, etc. Welding is also used for applying stainless-steel linings to pressure vessels. The proper technique has been developed for welding the various chromium and nickel steel alloys which are commonly used for refinery piping. At present there appears to be no phase of refinery construction to which welding may not be applied. Refineries are now commencing to install complete systems of three-phase 440-volt welding outlets for operating portable motor-generator welding sets. These outlets are of explosion-proof construction having a switch (or circuit breaker) and interlocked receptacle. Refinery metal-working shops are usually provided with welding equipment.

Lighting

On most installations less than five per cent of the refinery load is composed of electric lighting. Lighting fixtures for use in processing areas are of the vapor-proof type with heavy glass globe and guard. Anodized-aluminum reflectors are used with such units when installed on the top level of structures, platforms, walkways, etc. Floodlights are installed on the equipment structures to illuminate the general ground areas in the immediate vicinity of the units.

Modern refinery design calls for the following lighting intensities:

Location	Foot-Candles
Control rooms	10 to 15
Pump rooms, switch houses	6 to 8
Main operating platforms	6 to 8
Yard areas and ordinary platforms	2 to 4

These intensities have tended to make work and maintenance around the units safer. This would not have been possible but for the fact that manufacturers developed suitable switches, plug receptacles, and conduit fittings.

Miscellaneous Power Use

The larger refinery units use oil- or gas-fired furnaces to heat the feed stock; however, in the specialty products field, units and quantities are small and electric heating equipment is applied to certain of them, presenting another 440-volt load. Isolated locations where steam is not available may require the use of electric heat.

A great many refinery instruments are connected by means of tubing to the various pipes and vessels. It becomes necessary especially during cold weather to heat certain of these instrument lines to keep their contents fluid; this may be done by wrapping the interconnecting tubing with electric heater cable. The instruments themselves are similarly protected by heater cables where necessary.

Long pipe lines and particular operations sometimes require cathodic protection. In the case of pipe lines, power must be supplied to cathodic protection units at intervals along the lines which adds distribution difficulties. Cathodic protection of boiler-house equipment and condensers is of standardized type.

Summary

The use of electricity in refineries has increased tremendously in the last decade; this increase can be primarily traced to:

1. Development of equipment that is suitable for use in refinery atmospheres.
2. Development of improved mechanical equipment.
3. Development of generating equipment of industrial size having suitable operating economies.
4. General advance in the engineering arts.
5. Development of increasingly complex refinery processes.

Discussion

Everett E. Thomas (General Electric Company, Schenectady, N. Y.): Mr. Weeks and associates have prepared a timely paper for drawing attention of people in the electrical industry to requirements for the application of electrical power in oil refineries. They are to be congratulated for this work which should benefit all who are interested in refinery electrification.

Methods of finding and producing crude oil, as well as progress in processes for refining it into commercial products, have interested me during the past decade and my interest grows as I become better acquainted with the oil industry. My close contact with power-application problems in the crude-oil industry has given me considerable respect for explosive and hazardous conditions to be met by proper design and application of electrical equipment for safer operation in gaseous areas.

Up to about 15 years ago, some refineries installed standard open-type motors with control in gaseous areas by placing them in an air-conditioned room and projecting the motor shafts through a fire wall to the driven units while most refineries used electricity only for lights and for power in nongaseous areas. This situation prevailed until the development of explosion-proof motors and associated equipment made it economical and advisable to electrify processing apparatus. Also, substitution of a few tube stills for many shell stills called for much more power and much less low-pressure process steam. These conditions gave a decided impetus to practically complete electrification in crude-oil refineries.

As early as 1918, the electrical manufacturers began to realize the need for motors for explosive conditions in various industrial plants and the Underwriters' Laboratories tested an especially built motor later to determine the construction features really needed to meet hazardous conditions. Some 50-horsepower and 75-horsepower wound-rotor fan-cooled motors involving explosion-proof features were built for Russian oil-field drilling rigs as early as 1922. Later, a line of fan-cooled explosion-proof squirrel-cage motors up to about 50 horsepower was available and, as experience and knowledge increased, the line was rapidly extended so that at the present time motors up to 400 horsepower with "label" and larger without "label" are available.

Mr. Weeks and associates have included a discussion of individual enclosed induction motors. Economical and physical limitations of the explosion-proof construction prevent its use at the present state of the art in motors which are comparatively large either because of high rating as in case of hot oil pumps or low speed as for compressor drives. Inert-gas-filled construction has greatly extended the field of application in hazardous areas and is believed to be an outstanding contribution of the electrical manufacturers to refinery electrification. About nine years ago, there were built some 100-horsepower low-speed synchronous motors of totally enclosed fan-cooled construction and filled with inert gas under slight pressure for operation in a gaseous location. These motors were mounted on the crank shafts of reciprocating compressors.

Recently, an installation for driving hot oil pumps has been made using 1,000-horsepower 3,000-rpm induction motors of inert-gas-filled design and employing built-in heat exchangers in which water is used for cooling the inert gas. In the larger sizes, this substitution of water cooling for fan cooling results in a smaller and less expensive motor and is comparable in size to open-type motors. For nonhazardous areas where enclosed motor construction is required, several of the surface-cooler motors in larger sizes have been installed using air instead of inert gas. As compared to enclosed ventilated synchronous or induction motors, this has advantages of eliminating air ducts and, also, eliminating problems of supplying quantities of clean air.

Dual drives imply use of motors and steam turbines of same capacity. One modification is to use a turbine of about half the capacity of the motor for starting "on stream" and for emergency service to prevent coking in case of electrical power outage.

The trend of underground-cable construction for distribution involves suitable duct ventilation not only for dissipation of heat but to prevent accumulation of refinery gases in the duct work.

The fact that about half of the refinery load requires motors large enough to warrant using 2,200-volt design has caused general adoption of 2,400-volt generation and distribution. Employment of 4,000-volt wye generation and transmission may be considered initially since distribution expense would undoubtedly be reduced for large blocks of power and the larger motors can still be designed for this voltage but only for use without "labels" at this time since the Underwriters' Laboratories feel that there are some questions to be settled which include corona effects. It may even be found uneconomical in the long run to distribute the increasingly large blocks of power at either 2,400 or 4,000 volts for a distance of more than a few thousand feet, in which case 6,900-, 11,500-, or 13,800-volt generation for distribution to transformer substations located near the various load centers would be the solution. It is desirable to restrict these higher voltages to nonhazardous areas.

Short-circuit rupturing capacity of all power-control devices should be checked against initial and ultimate requirements. System growth makes it imperative for the user to check suitability of these devices from time to time. Frequently, 2,200-volt control requires protection which may be provided by use of group feeder reactors. The 440-volt control should be checked for short-circuit capacity even though its feeders may have considerable impedance.

Use of the synchronizing bus scheme in power stations is an effective way to provide protection for circuit breakers by means of current-limiting reactors. From 10,000- to 15,000-kw in steam-turbine generation at 2,400 volts may be the limit without resort to some scheme of short-circuit-current limitation as otherwise the initial short-circuit-current inrush becomes too great for conservative application of commercial circuit breakers. This limit is modified by pump-back from synchronous motors. The same breakers can be used on larger systems of higher voltage because the initial current is reduced with the increase in voltage. This means that the one-second rating of the breaker, which is a measure of its thermal and/or mechanical strength, becomes the limiting feature rather than its rupturing capacity at lower voltages. Hence, proper choice of voltage is a matter for close scrutiny with respect to the short-circuit capacity of available control and its relation to the over-all economics of the distribution system, including possible future growth.

An important recent development is the use of silver-coated contacts on devices and bus joints to minimize the effects of oxidation. Oxides on copper contacts must be wiped off frequently to prevent overheating if these contacts are fully loaded. Silver contacts do not overheat for normal duty but usually require arcing tips to prevent burning the silver.

Skimming or topping plants require about $1^1/_2$ horsepower-hours per barrel of crude oil and the cracking plant about 3 horsepower-hours. A complete refinery requires about $5^1/_3$ horsepower-hours per barrel of crude oil. There are about 460 active oil refineries in this country, and they represent throughput of 3,300,000 per day. Computations based upon these figures show approximate connected horsepower to be 1,800,000 and this includes 700,000 horsepower for electric motors. Evidently, there is much left to be done toward further electrification in crude-oil refineries.

G. R. Weeks, H. W. Giesecke, and C. M. Lathrop: Mr. Thomas' discussion is an interesting supplement to the paper. Alternate schemes for using dual-driven pumps have been presented; it should be made clear that considerations of economy and refinery setup determine whether the turbine unit of a dual-driven pump should have full or partial capacity. Dual drive is usually applied only to the largest or most important pumps of a unit. Unit charging pumps furnish the most conspicuous examples of dual drive. Small pumps are generally installed in duplicate with the spare unit arranged for steam drive. Where the unit is of large size and the refinery steam supply is ample, it is advisable in most cases to have full-capacity turbines for dual-driven machines in order to continue uninterrupted operation of the unit in case of electrical-power failure. Where the steam supply is limited or where the function or size of a unit is such that its shutdown in case of more than momentary power failure is not a serious matter, the selection of half-capacity turbines is warranted.

As to the choice of voltage for refinery distribution systems, it may be pointed out that an organization operating more than one refinery derives considerable benefit from selecting a voltage that can be commonly used by its refineries and that, at the same time, corresponds to general industrial practice; 2.4 kv is unrivalled in this respect.

Aside from designing for maximum safety, reliability of service must be considered above other factors in laying out an electrical system for a refinery.

It is felt that wye- or delta-connected rotating machinery is equally suitable for refinery use; however, there are reasons for recommending that systems operate ungrounded and that transformer banks consist of delta-connected single-phase units. The ability to operate on open delta up to the thermal capacity of the transformers is valuable in case one of a bank of transformers must be removed from service. Since a large margin of safety in insulation is usually built into refinery equipment and since the distribution voltages are comparatively low, the effect of abnormal voltage disturbances that may arise in ungrounded systems is considered to be slight. With an ungrounded system, operation of circuit breakers and shutdown of machines is avoided in most cases of momentary single-line faults due to lightning or accident.

The authors' experience covers one large refinery which uses 440- and 6,900-volt distribution. No difficulty has arisen here in obtaining motors of all required types and although 6,900-volt control equipment is somewhat special it is not comparably more expensive than 2,400-volt equipment. Such equipment consists of cubicle or panel and frame mounting of oil circuit breakers and accessory equipment. Where distribution at higher voltage is required the use of transformer substations, as Mr. Thomas suggests, is unquestionably indicated.

High-Capacity "Hydro-Blast" Circuit Breaker for Central-Station Service

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Synopsis: The increasing demand for oilless circuit breakers has resulted in a number of contributions in that field. Outstanding among these from the standpoint of interrupting capacity at generator voltages is the "hydro-blast" circuit breaker, which has been developed in ratings up to 1,500,000 kva. This breaker has the feature of complete interchangeability with the standard *H*-type oil circuit breaker and thus combines all of the well-known advantages of that type with the total absence of inflammable material. This paper describes in detail the construction and operation of the 500,000-kva hydro-blast circuit breaker for central-station service, several of which have been in service for over half a year.

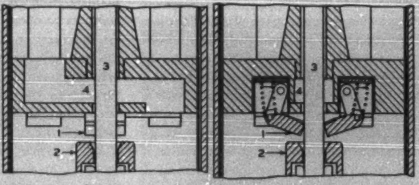
WHILE the oil circuit breaker has for many years given a very good account of itself in the United States, there has nevertheless been a gradually increasing interest in the elimination of inflammable liquids from circuit breakers as well as from other electrical apparatus. While not ordinarily thought of as a good insulator, water has a much higher dielectric strength for short periods than is commonly appreciated, and could therefore be expected to be effective in interrupting an arc. From the standpoints of noninflammability and availability, of course, it is very attractive. An investigation was therefore inaugurated some time ago looking toward the development of a practical breaker using water as the interrupting medium.

Two requirements of primary importance in such a device are that the water should not be subject to continuous dielectric stress in either the closed or the open position of the breaker and that all parts coming into direct contact with the water be carefully selected in order to eliminate corrosion in connection with these parts. These requirements are admirably satisfied in what has been known for many years as the *H*-type breaker in which each of two breaks per phase takes place in a small separate tank, to which the liquid is confined. The major insulation is air and the corrosion problem is simplified by the fact that all major moving parts, except for a part of the contact rods themselves, are outside of the pots and thus kept away from the water.

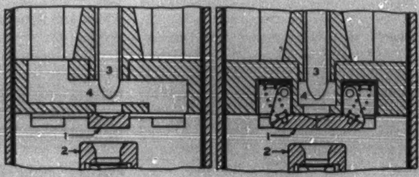
Principles of Operation

Early tests were therefore made on this type of breaker, supplied with cross-blast baffles of substantially the same construction as that used with oil. These baffles have been previously described in the literature, but a brief explanation of their action will be repeated here. Figure 1*a* shows schematically two cross-sectional views, taken at right angles, of a part of a pot using one of these baffles as applied in the oil circuit breaker and with the contacts in the closed position. The interrupting process is started by raising of the contact rod (3). This draws an arc initially from the bottom of the contact rod to the arcing bell (2). As the rod continues to rise and is withdrawn into the central hole of the baffle (figure 1*b*) the metal gates (1) at the lower end of this hole close under the influence of their compression springs, blocking off any direct flow of liquid or gas up through this hole and dividing the arc into two portions in series, one below the baffle and one in its central hole. The arc below the baffle generates pressure which forces the liquid through the cross-blast passage (4), which is shown in figure 1*b*, as entering the baffle from the bottom at the right, then passing horizontally over to the left side of the baffle and finally leaving the baffle from the top on this side. The horizontal passage intersects the central hole of the baffle, so that the liquid forced through this passage by the pressure below the baffle crosses the path of the arc. In oil this had been found to be a very effective means of arc extinction.

Tests on such an arrangement with water as the interrupting medium indicated the scheme to be fundamentally sound, but brought out the fact that the pressure generated by a light-current arc of given length is considerably less in water than in oil, while at high currents the pressures in the two liquids tend to be of about the same magnitude. Thus with an arc of fixed length, the pressure was either too low to supply the necessary cross-blast velocity for interruption at light currents or excessive from the standpoint of material stress at



(a) CLOSED POSITION



(b) INTERRUPTING POSITION

Figure 1. Type H oil-circuit-breaker cross-blast baffle and associated parts in cross section

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Paper 39-132, recommended by the AIEE committee on protective devices, and presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939. Manuscript submitted April 13, 1939; made available for preprinting May 15, 1939.

W. F. SKEATS is technical assistant to engineer and W. R. SAYLOR is application engineer, General Electric Company, Philadelphia, Pa.

The authors wish to express their appreciation of the co-operation furnished by many individuals, without which the success of this development would have been impossible. Within the General Electric Company, valuable suggestions have been contributed by members of the large-oil-circuit-breaker engineering section and by the staff of the short-circuit testing laboratory, while investigations in the Philadelphia works laboratory have contributed very largely to the choice of materials. The Philadelphia Electric Company is to be commended for its initiative in being the first to place breakers of this type in regular service on its system, thus providing the initial service experience.

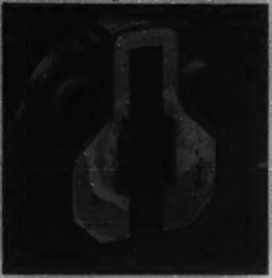

Figure 2. Arc-control baffle

high currents. What was needed was a long pressure-generating arc at low currents and a short one at high currents.

This combination has been obtained without the addition of any moving parts by means of the arc-control baffle shown in figure 2. This baffle consists of a laminated iron core embedded in a disk of insulating material, which is shown as transparent in the photograph, but which is actually a hard rubber material that has been found suitable. It is located between the stationary contact and the cross-blast baffle in such a way that the arc is drawn through the central hole. Its action utilizes a modification of the well-known principle that an arc drawn in a slot of magnetic material is drawn magnetically toward the bottom of the slot. Now fundamentally, the "bottom" of the slot is that end at which the flux in question encounters the lower magnetic reluctance drop. Since iron has a saturating magnetic characteristic, it is obvious that a double-ended slot may be arranged as in figure 2, one end of which has a complete iron path of small section and therefore comparatively low reluctance at light currents and high reluctance at heavy currents, while the other end has an iron path of large section, but with a gap in it, so as to have a reluctance substantially independent of current magnitude. In this slot, light-current arcs are drawn to the end having the complete iron path, while heavy-current arcs are drawn to the end having the larger section.

This results in an arrangement whereby shortly after being drawn light-current arcs will tend to assume one path and heavy-current arcs a different path. If, then, the one path is made long and the other short, as indicated in figure 3, the object is achieved of providing a long pressure-generating arc for light currents and a short one for heavy currents. The pressure generated by this is now sufficient to bring about interruption at all values of current and excessive at none within a rating of 500,000 kva, which is equal to that of the corresponding oil circuit breakers. This results in a satisfactory interrupting performance throughout this range. Figure 4 shows a photograph of a cutaway section of the interrupting element showing the actual configuration.

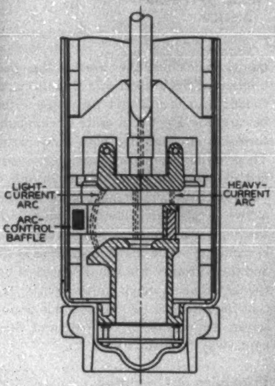

Figure 3. Diagram showing arc-control action

In order to remove all dielectric stress from the interior of the pot when the breaker is standing in the open position, a set of external disconnecting contacts has been provided as shown by figure 5 and figure 6. A wood crosshead is used which supports two segment-type contacts at each end. These segment-type contacts co-operate with bayonets which are mounted on cap insulators on top of the pots so as to make contact when the breaker is in the closed position and throughout the major part of the 20-inch stroke, but to leave a gap of about $2\frac{1}{2}$ inches above each pot when the breaker is in the open position. The path of current through the interrupting contacts of the breaker then runs from the bottom of one tank up through the contact rod to the two segment contacts which are connected in parallel at one end of the crosshead, thence to the bayonets mounted on the cap insulators, down these and across on the copper tie rods to the top of the cap insulators on the other pot of the same phase, then by a path similar to that followed on the first pot but reversed, to the bottom of this pot. The primary contacts make direct contact from pot to pot, shorting out the bayonets above the pots as well as the internal contacts.

The primary current-carrying contacts of the breaker are of the high-pressure line-contact type which combines the advantages of simplicity, effectiveness, and lightness of moving parts. The 600- and 1,200-ampere breakers use the butt type of contact in figure 5. The ratings of 2,000 amperes and above use double-finger wipe contacts which are also very compact as indicated by figure 7.

Adaptation to Field Service

The use of the arc-control baffle and series air disconnecting contacts described above resulted in the evolution of a hydro-blast breaker capable of supporting a 500,000-kva interrupting rating which is the same as that of the corresponding oil circuit breaker. With the use of the primary contacts, the current-carrying capacity is likewise brought into line. These changes had been made without increasing the space requirements of the breaker and in such a way as to permit modernization of the standard oil breaker in its original cell.

Figure 4. Cutaway section of interrupting element

From the point of view of adapting the breaker to the reliable service expected in field operation, the problems involved were mainly those of choice of material. It was obvious from the beginning that exclusive of the new features which made successful the utilization of water as an interrupting medium, a number of changes would have to be introduced to cope with the altered chemical and physical picture presented inside the interrupting units. The comparable oil breaker had shown itself to be more than satisfactory over a period of many years' service as evidenced by the fact that over 30,000 of these breakers had been put into service throughout this period and operated successfully from the standpoint of both undiminished interrupting ability and unimpaired current-carrying qualities over long periods with very moderate maintenance. The goal set was a breaker using water as an interrupting medium that should be comparable not only in interrupting ability, weight, and cost, but also in sturdy and enduring construction to its proved counterpart in oil.

As a direct outgrowth of the type *H* oil circuit breaker, this breaker assembly includes a number of elements which have undergone only the changes necessary to accommodate them to immersion in water. The Herkolite and fiber parts as well as the plywood baffles and certain steel parts were immediately eliminated because of their deterioration when immersed in water. In their place, a synthetic material was found to be practical for the cylindrical spacers which hold the baffles in their proper locations; the bushing and the arc control baffle are molded of a hard substance which has shown the combination of long life and good mechanical and electrical properties, including ability to withstand the erosive effect of an arc; the tank lining is made of a material which adapts itself readily to the inside of the tank; the cross-blast baffle uses material which has ability to stand up under water and has shown itself to have excellent resistance to the arc. The metals to be used were chosen as a result of extensive steam and weathering tests on various combinations to determine which of them would produce a minimum of corrosion. This resulted in an assortment of metallic materials, including bronze, Elkonite, brass, stainless steel, and tin-dipped steel tank, that showed only slight tarnishing after prolonged exposure.

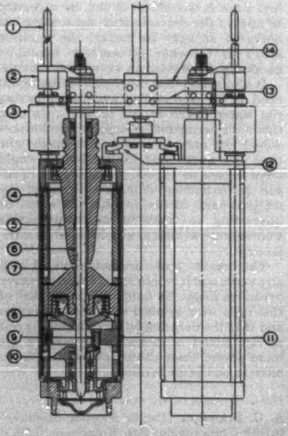

Figure 5. Hydro-blast circuit breaker, 500,000-kva rating

1—Bayonet
2—Segment-type contact
3—Cap insulator
4—Tank lining
5—Bushing insulator
6—Contact rod
7—Cross-blast baffle
8—Gates
9—High-current runner
10—Low-current runner
11—Arc-control baffle
12—Primary contacts
13—Copper tie rods
14—Wood crosshead

Water differs from oil in that its breakdown under the influence of the arc is a completely reversible process, leaving no permanent gases. This gave rise to the thought that a vent might be found unnecessary on these breakers and circuit interruption tests without a vent have shown this to be a fact. Thus the separating chambers and vent piping which are conventional on oil circuit breakers are entirely dispensed with.

With the elimination of the vent, the pot becomes practically a sealed chamber and the loss of water by evaporation may be expected to be very slight. Laboratory tests have shown this to be the case. In these tests, a tank was subjected to a heating cycle of 12 hours on and 12 hours off, resulting in temperature fluctuations from about 20 degrees centigrade to about 70 degrees centigrade. Under these severe conditions the loss by evaporation was at the rate of about $1^1/_2$ inches per year. Field experience, as might be expected, has shown an even lower loss. The circuit interruption tests listed in table I demonstrate that a water level variation of 3 inches may be allowed without any effect upon the breaker performance, so that a very reasonable maintenance schedule will be satisfactory from this standpoint.

Table I. Representative Arc Durations at Different Water Levels, 500,000-Kva Hydro-Blast Breaker

Single-Phase Tests at 14,500 Volts, 60 Cycles. Water Level Range—Three Inches

Maximum Water Level		Minimum Water Level	
Current (Amperes)	Arc Duration (Cycles)	Current (Amperes)	Arc Duration (Cycles)
1,200	2.1	1,200	1.5
2,100	1.2	2,900	1.1
4,300	1.5	4,000	1.3
6,200	1.3	6,900	1.3
9,900	1.6	10,000	1.1
12,900	1.4	13,000	1.3
14,300	1.3	20,000	1.2
17,800	1.1	20,500	1.2
19,600	1.3	21,000	1.0
20,000	1.0	24,000	1.0
26,000	1.3		

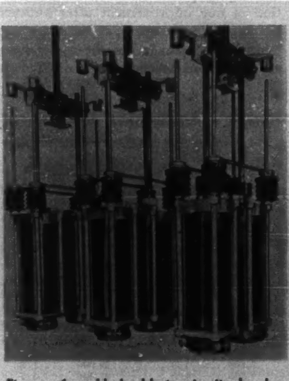

Figure 6. Hydro-blast circuit breaker, 500,000-kva rating, in open position

Tests

The breaker has, of course, been subjected to exhaustive tests. Table I,

Figure 7. Double-finger wipe contacts

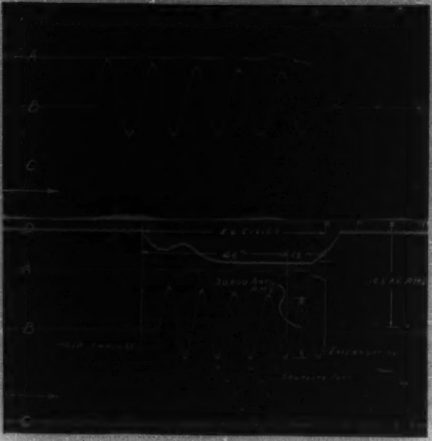

Figure 8. Typical interruption at breaker rating

A—Arc voltage
B—Current
C—Pressure below baffle

A—Arc voltage
B—Current
C—Contact travel—one-half inch per step
D—Trip-coil current

which has already been referred to, shows excellent performance on single-phase tests under the severe conditions of 14,500 volts impressed across a single pole. Table II shows similar data from three-phase tests. Both of these tables show interruption of currents exceeding that corresponding to the 500,000-kva rating of the breaker. Figure 8 shows an oscillogram of a typical interruption approximately at the breaker rating. The arc duration was limited to 1.2 cycles and the over-all time from trip impulse to interruption was 5.6 cycles. The performance is thus substantially the same as that of the corresponding oil circuit breaker.

Field Experience

Six oil circuit breakers which had been in service for some years have been changed over to hydro-blast breakers of the 500,000-kva rating and been in service in this form since the early fall of 1938. During this time as many as three short circuits have been interrupted by a single breaker. All short circuits have been interrupted promptly and expeditiously and the performance has been satisfactory in all respects, the only damage being a very slight burning of the contacts, which is to be expected.

Higher Interrupting Ratings

This paper has been primarily concerned with the 500,000-kva rating of the hydro-blast breaker, which has advanced somewhat farther than the other ratings. Work has progressed far with the development of both the 1,000,000-kva and the 1,500,000-kva ratings, however. Satisfactory circuit interruption tests have been made on a development sample of the 1,500,000-kva rating at current values extending up to 60,000 amperes.

Conclusions

The hydro-blast breaker, using a noninflammable interrupting medium, has been developed in ratings from 500,000 kva to 1,500,000 kva at 15,000 volts. This breaker may be regarded as the latest stage in the development of a circuit breaker which has enjoyed continuous high popularity, as the incorporation of one improvement after another has kept it well abreast of the art. The hydro-blast breaker has the same overall dimensions as the oil circuit breaker of which it is an outgrowth, substantially the same general arrangement, and the same performance characteristics, except that the presence of inflammable liquid has been entirely eliminated. The various interrupting problems introduced by the change from oil to water have been overcome, and six months' field experience has confirmed the practicability of the arrangement.

Table II. Representative Arc Durations, 500,000-Kva Hydro-Blast Breaker

Three-Phase Tests at 14,500 Volts, 60 Cycles

Current (Amperes)	Arc Duration (Cycles)	Current (Amperes)	Arc Duration (Cycles)
1,180	1.7	16,000	1.4
1,320	1.9	15,000	1.5
1,320	1.9	17,000	1.5
4,500	1.7	18,000	1.4
4,000	1.6	16,000	1.6
4,800	1.7	15,800	1.6
6,800	1.7	23,000	1.7
6,600	1.9	22,000	1.8
7,100	1.9	22,000	1.8
9,500	1.5	27,000	1.7
9,500	1.7	30,000	1.4
10,300	1.7	20,000	1.7

Discussion

J. A. Elzi (The Commonwealth & Southern Corporation, Jackson, Mich.): Developments in circuit breakers utilizing noninflammable material in place of oil as the arc-extinguishing medium are of considerable importance and the results obtainable by using water for this purpose, as described in this paper, are highly interesting. The oscillograms and test results given seem to indicate that in general the performance of this breaker is quite comparable to that of an oil circuit breaker.

As the authors have mentioned, water is readily available, but presumably the water used in the breaker would have to meet certain requirements as to purity to avoid corrosion within the breaker and to maintain its arc-interrupting ability. It would seem that even a very small percentage of impurity in the water might be detrimental and since it would, no doubt, be necessary to inspect the contacts after the breaker has operated at near its rating, the question arises as to what precautions would be necessary when such an inspection is made.

It also seems that every precaution would need to be exercised to avoid ever having the water freeze in the breaker. In case this were permitted to happen, the breaker would not only be inoperative during the time the water was frozen, but it would seem logical to expect such freezing to result in permanent damage to some of the internal parts of the breaker. It would be interesting to know if any antifreezing solutions are available which would be satisfactory.

The breaker tanks are not provided with any vents and it would appear that excessive pressures might be built up if a breaker of this type were applied on reclosing service and that the derating factors for this type of service would of necessity be quite a bit more severe than those now applicable to modern oil circuit breakers.

A. C. Schwager (Pacific Electric Manufacturing Corporation, San Francisco, Calif.): The authors have to be commended for the ease with which they have been able to transform one of the most popular oil circuit breakers into one in which the hazard of an inflammable liquid is eliminated.

On a recent study trip in Europe, I had the opportunity of examining various water circuit breakers, but in spite of the several years of development, breakers of 1,500,000 kva, as described by the authors, were not available. In Europe the water circuit breaker has not been entirely satisfactory and just recently its manufacture has been abandoned by at least two large manufacturers. In some breakers installed it was found advisable to change the interrupting liquid back to oil, in order to obtain more satisfactory operation. In view of these facts, one can more fully appreciate the difficulties which the authors have had to overcome in developing the breaker described.

In connection with the design proper I would like to ask a few questions:

(1). In closing in slowly on a short circuit, an arc is formed and steam is generated prior to the time the contacts touch. This high-pressure steam can exert a considerable force upon the moving contact, retard and possibly prevent the closing. I wonder if this phenomenon has been observed, and if so how its effect was overcome.

(2). Although water eliminates the danger of fire hazards, European practice, using Herkolite and fiber parts within the chamber, has shown that a sustained arc can produce a sufficient amount of gas completely to smoke out a station. It would be interesting to know if this shortcoming has been eliminated in the materials developed by the authors, particularly if the gases are of nonpoisonous nature.

(3). I am sure that this new development has thrown considerable light upon the theoretically involved problem of arc interruption. The authors refer to the high dielectric short-time strength of water; the question therefore arises whether this factor alone is sufficient to explain the performance. Several cycles elapse from the time the arc is interrupted to the instant the series air gaps open. Is the insulating ability of the breaker during this relatively long time interval due to a layer of water interposed between the contacts or due to the presence of steam?

B. P. Baker (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The reaction to the unique development described in the paper by Messrs. Skeats and Saylor will be most interesting to those concerned with the trend in American switchgear development.

I concur with these gentlemen in their belief that "the oil circuit breaker has for many years given a very good account of itself in the United States."

For some years we have recognized that there are rather special applications where the duty and frequency of operation are extremely severe. In these cases the complete elimination of oil, accessibility of contacts, and reduced maintenance requirements have justified our offering a line of oilless circuit breakers up to approximately 800,000 kva at 15 kv.

As early as 1922 we became interested in water as an interrupting medium, and even went so far as to acquire rights under the Nicholson patents. For a good many years now we have watched the work of a large European company on the "expansion" breaker, which was the first commercial form of water breaker. We have bought and tested various types of these breakers for both high and low voltages, and have obtained considerable information about their performance. Up to the present time however, our conclusion has been that the water breaker is not as well suited to American requirements as the De-ion type of air breaker.

We note that Messrs. Skeats and Saylor have carried their interrupting tests on a development sample of the 1,500,000-kva rating to 60,000 amperes. In view of the rather high power represented by such laboratory tests, it would be interesting to know at what voltage they were made and whether any special test connections were used.

The authors seemed to have investigated thoroughly the suitability of materials for operation in water and have reported on their adaptability from the corrosion point of view. Their conclusions generally confirm the results obtained by us. While the effect of the water on certain materials may not be serious, I do not believe that we can safely conclude that the effect of the various materials on the water can be taken too lightly. It would, therefore, be of interest to know the kind of water used, the upper limit of conductivity, and how this is affected by time and interrupting duty.

In this interrupter the old problem of volts interrupted per inch of arc as an inverse function of current reappears. Here again a rather ingenious solution has been offered by moving the arc from its point of inception, in one direction to lengthen it for low currents and in the opposite direction to shorten it for high currents. It seems rather unfortunate that at high current it is necessary to draw a long arc and then deliberately move it from its point of inception in order to shorten it, thus consuming unnecessary time, producing extra burning, arc energy, and pressure.

The author points out that in contrast to an oil breaker the water-breaker "pot becomes practically a sealed chamber", and at another place "pressure generated by a light-current arc of given length is considerably less in water than in oil, while at high currents the pressures in the two liquids tend to be of about the same magnitude". How these two conditions can exist needs explanation. Our experience indicates that the total volumes of gas generated are approximately the same for oil and for water but that a larger part of the gases formed by the arc in water rapidly recombine or condense leaving a smaller volume of permanent gases. At high currents the rate of generation is so high that condensation during that time is almost negligible and the pressures produced are about the same as for oil. However, at low currents the condensation of the steam is not negligible and the pressures generated are less than those when oil is used. This relatively faster condensation may be caused not only by the smaller rate of gas generation but also by the smaller bubbles which have walls closer to the arc and which have a relatively larger surface area in proportion to the volume of the bubble.

I was surprised to find that the average speed of the contacts from the point of separation to arc extinction is approximately 25 feet per second and I am wondering if I have interpreted the oscillogram correctly. With two series breaks per pole the arc is lengthened at the rate of approximately 50 feet per second. I had never regarded the *H* breaker as having such tremendous acceleration and wonder if there is anything inherent in this type of water breaker which requires such speed. Since the moving parts include the main contacts as well as the auxiliaries, I would be interested in knowing what special means have been required to get the acceleration and shock-absorbing effect.

D. C. Prince (General Electric Company, Philadelphia, Pa.): For many years there has been a search for some liquid which could be used in place of oil in a circuit breaker. The ideal liquid would possess such properties of transil oil as dielectric strength, especially under arcing, and, at the same time, be noninflammable. It should, of course, have other properties, such as constant viscosity, stability, low volatility, be noncorrosive, and have low cost, to mention only a few of the requirements.

For several years we have made a practice of trying out as a circuit-breaker fluid any liquid that anybody cared to mention, which offered any hope of success. Of all the liquids tried, water alone has seemed to offer any possibility of being useful in a commercial design.

Now that a water breaker has been produced which meets the exacting requirements of the high-power test station, and which makes possible an oilless circuit breaker up to the highest commercial ratings for station service, the next step is to see how such a design will meet the requirements of day-to-day operation in the field.

W. F. Skeats and **W. R. Saylor:** Mr. Elzi and Mr. Baker have asked about the purity and other characteristics of the water. The water used in the majority of tests on the hydro-blast breaker is Schenectady tap water. A typical series of about ten interruptions at moderate and high currents has been shown to reduce the resistivity of this water about 50 per cent with no accompanying deterioration in performance of the breaker. Inasmuch as the resistivity of the water has been quite low in all tests, it seems obvious that the presence of minute amounts of impurities such as might be picked up from the metals or insulating parts of the breaker would have no deleterious effect, and the successful field experience, of almost a year, as well as the many interrupting-capacity tests seems to bear this out. In view of the wide variations in tap water throughout the country, however, distilled water is recommended for general use.

It is not necessary to inspect the contacts of the water circuit breaker any more frequently than those of the corresponding oil circuit breaker, and the necessary precautions at the time of inspection are no more stringent than in the case of the oil circuit breaker.

Since these are indoor breakers, it is not felt that the requirement that the water should not freeze is at all embarrassing. However, antifreeze has been in use in this connection in Germany for a number of years.

The pressures obtained in the air space of the breaker are but a small fraction of those below the baffle and of that which the structure will stand, so that the absence of vents will not affect the derating factor on reclosing service.

The problem of circuit closing has been

met by a modification of the disconnecting contacts causing them to close after the interrupting contacts, and an arrangement has been developed which has been found satisfactory at the highest currents.

In order to determine the nature and extent of disturbance upon failure, this breaker has been opened under a short circuit of rated interrupting capacity without water in the tanks. After a few cycles of arcing inside the tanks, the arc was transferred outside the tanks, generating no more smoke than any other arc in air. Furthermore, with the cubicle arrangement now used with these breakers, such smoke as is generated should be largely confined to its own cell. The gases are not poisonous.

Unfortunately the development throws no light on the mechanism of arc interruption in the way that Mr. Schwager anticipates. The disagreement in this field centers upon the first millisecond or less after current cessation. Beyond this time there will be little disagreement that there will be sufficient dielectric between the contacts of such a breaker as this to render the voltage gradient across it comparatively low.

With reference to Mr. Baker's question, no special test connections were used, the breaker being tested up to the capacity of the testing plant at 14,500 volts, 8,400 volts, and 4,200 volts. The 60,000-ampere tests were obtained at 4,200 volts across a single-pole unit, but with only one breaker tank per pole instead of two. With this breaker these should be the equivalent of tests at about 8,000 volts, which is very nearly equal to leg voltage at 15 kv.

It is considered very unlikely that a long arc ever exists at the pressure-generating gap under high-current conditions. The metal piece which forms the lower terminal of the high-current arc as shown in figure 3 of the paper is fairly close to the contact rod when the latter extends through the arc-control baffle, and with the normal rapid expansion of the high-current gas bubble and the magnetic effect of the arc-control baffle combined, it is believed that the arc will strike from the contact rod to this metal piece while their separation is still quite short.

The explanation which Mr. Baker offers for the difference of the pressure characteristic of water from that of oil is very interesting and parallels closely one which has been suggested by the authors' associates. Another contributing factor may lie in the fact that oil is a mixture while water is substantially a single substance. Thus the more volatile constituents of the oil in the neighborhood of the arc may be vaporized rather readily by a light current, so that more gas in proportion would be generated in oil by a light current than by a heavy current. Since no such effect occurs in the case of water, a difference in the pressure characteristics similar to that which has been observed may be expected.

This breaker uses no special means for acceleration and shock absorption other than those standard on the *H* circuit breaker.

The question of the future of this type of breaker is one which only time can answer. The authors have been aware, of course, of the recent European history of this type of breaker and have had available complete design data on the water breakers manufactured by one of the European companies. They have made tests embodying these ideas and, as Mr. Baker has done, found them not very well suited to American practice. They believe, however, on the basis of both laboratory tests and field experience that the present breaker has overcome all serious shortcomings. Therefore, since this breaker exceeds by almost two to one the interrupting capacity of any earlier oilless breaker in its voltage range, it seems to be indicated that until other oilless breakers show a much greater advance than in the past few years, the hydro-blast circuit breaker will enjoy a good field of application.

The Anomalous Behavior of the Moving Systems of Single-Phase A-C Watt-Hour Meters at No Load

F. C. HOLTZ
MEMBER AIEE

Synopsis: Investigation reveals the moving systems of induction watt-hour meters to be in a peculiar state of motion under the condition of potential excitation only. The present paper offers a simple explanation for its origin and provides a method for recording and determining the actual value of the motion in various parts of the moving system. Interesting conclusions may be drawn from the investigation which throw much light on the problem of meter-bearing performance.

IT IS generally known among electric metermen that the moving systems of a-c watt-hour meters under the condition of no load with potential element excited are in a peculiar state of vibration. This phenomenon frequently exhibits itself as a rattle or vibration of the upper pivot if unoiled and is easily felt by slightly touching the edge of the disk with one's finger.

The writer, when discussing this matter with George F. Shotter of London, England, was advised that under suitable illumination and with sufficient magnification the actual motion of the peripheral edge of the disk had been observed as a figure eight, characteristic of Lissajous' curves. This discussion led the writer to investigate the phenomenon and develop methods for more accurately observing and recording the motion. Such motion can also be predicated on theoretical grounds as illustrated by the following considerations which are sufficiently accurate to explain the observations and predict the general nature of the motion. The total labor may be considerably reduced by making some very obvious assumptions as follows:

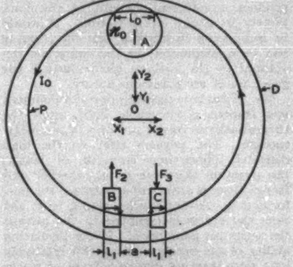

Figure 1. Elementary diagram of disk and interacting elements

1. The distribution of the driving and damping fluxes associated with the meter disk are generally symmetrical with respect to a plane perpendicular to and passing through its center.

2. The movement being very small and the upper pivot being at a considerable distance from the disk, it is assumed that the motion is parallel to the plane of the disk and that the motion of the lower pivot is substantially that of the center of the disk.

3. The total eddy-current distribution in the disk which causes the motion may be replaced by an "equivalent" filament of current interacting with the field of the permanent magnets and the a-c potential-element field.

4. The impedance of the path through

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which the filament of eddy current circulates is wholly resistance.

5. The permanent-magnet damping fields are of equal but opposite strength and symmetrically placed with respect to the plane referred to above.

With these assumptions we may proceed with the equations of motion in the steady state in which,

$\mathcal{H}_0$ = maximum value of the assumed uniformly distributed field strength as shown in figure 1
f = frequency
$\omega = 2\pi f$
I_0 = maximum value of the "equivalent" eddy current in the disk
l_0 = length of the eddy-current path in the a-c field
l_1 = length of the eddy-current path under each of the permanent magnets
H = intensity of the permanent-magnet field in the disk at the points associated with the eddy current
$\mathcal{H} = H_0 \sin \omega t$ = instantaneous value of the a-c field strength. If i is the instantaneous value of the eddy current in the disk we have, in view of (4)
$i = I_0 \cos \omega t$

Referring now to figure 1, let P denote the filament or equivalent eddy current in the meter disk, D and A, B and C, the respective locations of the a-c and d-c fields.

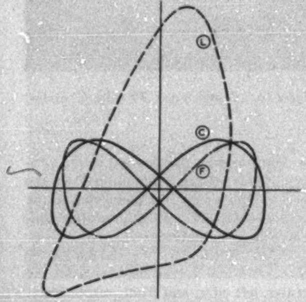

Figure 2. Motions derived from simple theory

The force caused by the interaction of the a-c field and the equivalent eddy-current filament is

$$F_1 = l_0\mathcal{H}_1 = \mathcal{H}_0 l_0 I_0 \sin \omega t \cdot \cos \omega t = K \sin 2\omega t$$

where K is a constant. It is therefore seen to be a double-frequency quantity and with the usual assumptions as to symmetry and direction of field is directed through the center of the disk along the line Y_1Y_2.

We may also assume that this produces a harmonic motion of the disk in the same direction but in view of its inertia, and other factors, the motion is not quite in phase with the force but differs therefrom by a small angle α.

With these assumptions the component of motion of the center of the disk arising from the interaction of the eddy currents and a-c field is of the form $y = K_1 \sin (2\omega t + \alpha)$. The disk, as a whole, will also be moving in the same manner.

The same eddy current will also interact with the field of the permanent magnets B and C. The filament of eddy current under each magnet may be resolved into two components which are parallel and perpendicular respectively to the direction Y_1Y_2. As may be seen in the figure, the components parallel to the Y_1Y_2 direction are relatively small and may therefore be ignored in the present analysis. It is interesting, however, to observe that since these components in respect to each magnet are relatively reversed and the magnetic field is also reversed the effect is to produce an oscillation of the disk about its center. The components of the eddy current under the magnets and perpendicular to the line Y_1Y_2 interact with the field, H, to produce forces in the disk parallel to this direction.

Under the magnet B there exists a force

$$F_2 = Hl_1I_0 \cos \omega t$$

And under C a force

$$F_3 = -Hl_1I_0 \cos \omega t$$

It is apparent that these two forces combine as a couple of moment

$$M = F_2(a+l_1) = (a+l_1) \cdot Hl_1I_0 \cos \omega t$$

Since the observed motions are of extremely small magnitude we may assume that this moment gives rise to a component of motion of the center of the disk along the direction X_1X_2 of magnitude

$$X = K_2 \cos (\omega t + \alpha_2)$$

Where K_2 is a constant for any given set of conditions.

It is to be noted that this motion is harmonic and of the same frequency as the supply. By suitably readjusting the value of α, we may rewrite the equation of motion of the center of the disk in parametric form.

$$y = K_1 \cos (2\omega t + \alpha_1)$$

$$x = K_2 \cos (\omega t + \alpha_2)$$

Since y goes through its period twice as fast as x the curve in general consists of two loops. These curves are, in fact, the typical Lissajous' curves.

In view of the numerous factors which present themselves in attempting a general solution of the disk motion, it cannot be argued that the simple equations above adequately represent all actual possible motions of the system. Some difficulty would, for example, be encountered in even the more simple case of evaluating the inertia, damping coefficients, and other restraining forces under the action of the single force in order to arrive at the equations of motion. They do, however, give a very close approximation by assigning various constants in the equations of motion.

In precisely the same way it is possible to establish the corresponding motion of other portions of the disk. This has been done and by appropriate choice of constants, and transformation equations the relative motions have been plotted in figure 2 in which C represents the motion of the center of the disk; L, the left peripheral edge, and F, the front peripheral edge between the magnets. In this sense the constants were merely varied until the curves corresponded with the observed values rather than using the equations to predict the actual observed motion. It will be seen that these curves do represent with considerable accuracy the nature of the observed motion and therefore the

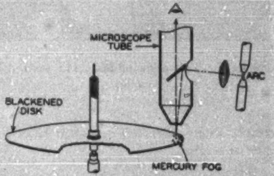

Figure 3. Setup for observing motion at edge of disk

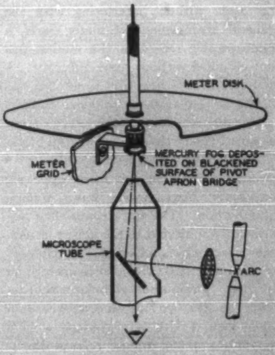
METER DISK
METER GRID
MERCURY FOG DEPOSITED ON BLACKENED SURFACE OF PIVOT APRON BRIDGE
MICROSCOPE TUBE
ARC

Figure 4. Setup for observing motion at center of disk

Figure 5. Center of disk, no excitation

Figure 6. Left edge of disk, no excitation

Figure 7. Center of disk, 115 volts, 60 cycles

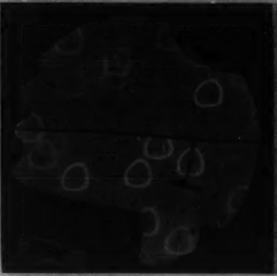

Figure 8. Left edge of disk, 115 volts, 60 cycles

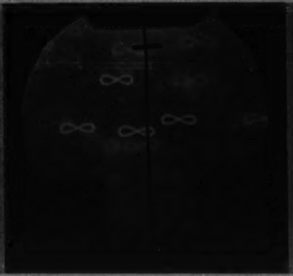

Figure 9. Front edge of disk, 115 volts, 60 cycles

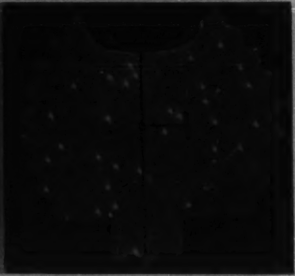

Figure 10. Center of disk, 90 volts, 60 cycles

simple assumptions are sufficient to predict its origin.

Experimental Procedure

Since the magnitude of motion was extremely small and the observations had to be made without interfering with the general arrangement of meter parts, a photographic record of the motion was not obtained without some difficulty. A simple and also beautiful procedure was finally arrived at in the following manner.

The disk was first given a heavy coat of lacquer heavily charged with lamp black. This, on drying, gave a surface which produced no reflection and hence appeared uniformly black even under intense illumination. The surface was then momentarily subjected to the action of mercury vapor which collected small globules of the order of 0.0001-inch diameter and smaller. These small globules would remain on the surface undisturbed and when viewed with a microscope of high magnification and intense vertical illumination the appearance was that of a star image of unusual brightness and small size. When the disk was in motion each tiny spot, due to persistence of vision, produced a true figure of the motion which could be measured and photographed. The general arrangement of apparatus is shown in figure 3.

In order to observe and record the motion of the center of the disk, the arrangement shown in figure 4 was used. In this case the jewel screw was replaced by a small bracket secured to the meter grid and on this was mounted the jewel which supported the meter pivot and moving system. By cutting away the dust cap surrounding the pivot until it resembled an inverted U it was possible to bridge this with a small strip on which the mercury globules had been deposited. The microscope in an inverted position gave the desired view.

Figures 5 and 6 are photographs showing the appearance of the illuminated points at the center and left edge respectively for the condition of no current and no potential excitation. In these as well as subsequent photographs the magnification was 500 diameters. Since there was some possibility that the motions might be influenced by the location of the anticreep slots, these were marked on the negative by a short bar connected to the center of the disk. No appreciable influence was however observed.

Figures 7, 8, and 9, show the motion for no current and normal excitation for the center, left edge, and front, respectively. The close similarity between these three photographs and those shown in figure 2 is to be noted, indicating that the simple assumptions made at the beginning are sufficient to account for the observed motion. As is to be expected, a reduction in voltage should give a corresponding reduction in the motion. This is shown in figures 10, 11, and 12, showing the motion at the center, left, and front edge respectively when the voltage is reduced from 120 to 90 volts. On account of the friction offered by the pivot, there is a point where the motion ceases at the center and becomes purely one of rotation. The above figures illustrate this. In a like manner increasing the voltage makes the effect more pronounced as is shown in figure 13, which illustrates the motion of the pivot at 135 volts.

Removing the permanent magnets has the effect of wiping out the single-frequency motion as is shown in figures 14 and 15 representing the condition at 170 volts, 60 cycles. Under these conditions reducing the voltage to 115 volts had the effect of completely arresting the motion as shown in figures 16, 17, and 18, illustrating the center, left, and front edge respectively. This condition is brought about by the fact that the pivot friction was sufficient to prevent motion, yet in the earlier photographs where higher forces caused the pivot to move, the double-frequency forces were able to act while the pivot was in motion.

Removing one of the magnets caused a pronounced change in motion as can be determined from the equations. In this case the motion is almost entirely in a radial direction across the remaining magnet. Figures 19 and 20 show the motion at the center and left edge respectively. Many other interesting effects can be pro-

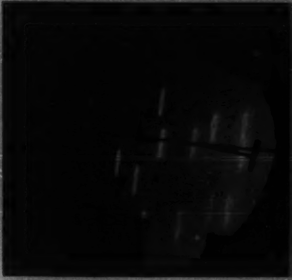

Figure 11. Left edge of disk, 90 volts, 60 cycles

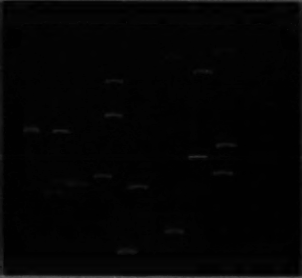

Figure 12. Front edge of disk, 90 volts, 60 cycles

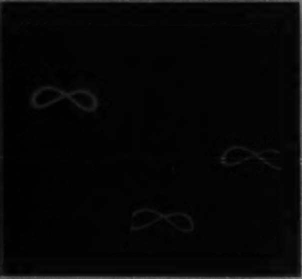

Figure 13. Center of disk, 135 volts, 60 cycles

Figure 14. Front edge of disk, magnets removed; 170 volts, 60 cycles

Figure 15. Left edge of disk, damping magnets removed; 170 volts, 60 cycles

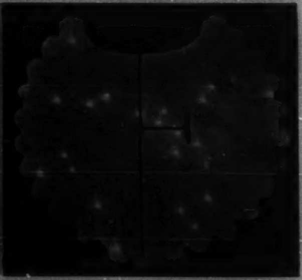

Figure 16. Center of disk, damping magnets removed; 115 volts, 60 cycles

Figure 17. Left edge of disk, damping magnets removed; 115 volts, 60 cycles

Figure 18. Front edge of disk, damping magnets removed; 115 volts, 60 cycles

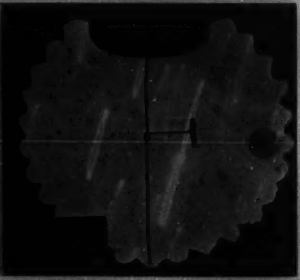

Figure 19. Center of disk, right-hand magnet removed; 115 volts, 60 cycles

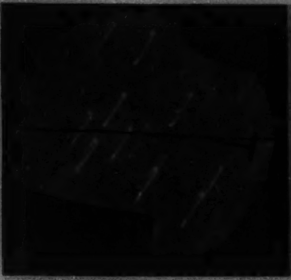

Figure 20. Left edge of disk, right-hand magnet removed; 115 volts, 60 cycles

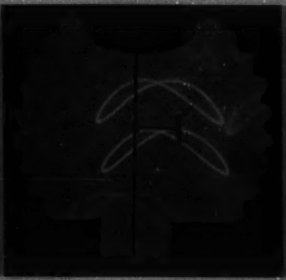

Figure 21. Center of disk, 115 volts, 40 cycles

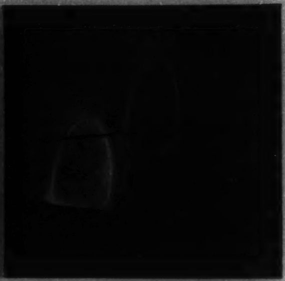

Figure 22. Left edge of disk, 115 volts, 40 cycles

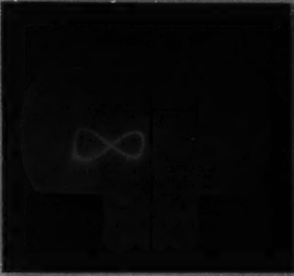

Figure 23. Front edge of disk, 115 volts, 40 cycles

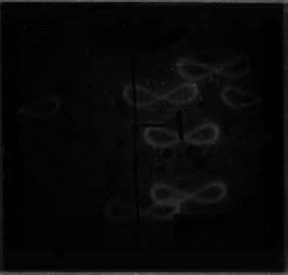

Figure 24. Ball-bearing construction; center of disk, 115 volts, 60 cycles

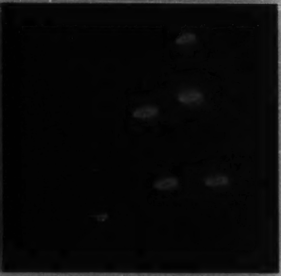

Figure 25. Ball-bearing construction; center of disk, 30 volts, 60 cycles

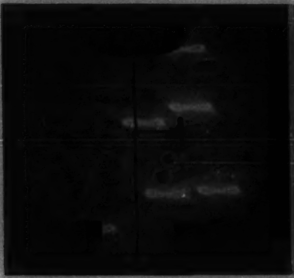

Figure 26. Ball-bearing construction; center of disk, 60 volts, 60 cycles

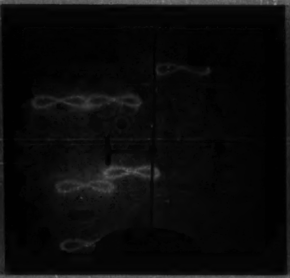

Figure 27. Ball-bearing construction; center of disk, 90 volts, 60 cycles

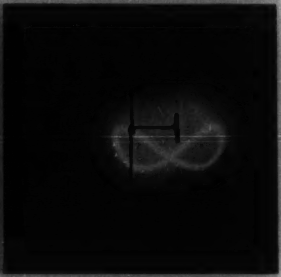

Figure 28. Ball-bearing construction; center of disk, 180 volts, 60 cycles

duced according to the variations made in the factors going into the constants of the equations. A reduction in frequency, for example, greatly amplified the figures as shown in figures 21, 22, and 23 showing the motion of the center, left, and front edge at normal voltage and 40 cycles.

Behavior of Moving System With Ball-Bearing Support

Because of the equally common use of the ball-type bearing, further tests were made to determine the relative motion of the disk under operating conditions similar to those applied to the meters with pivot-type bearing. In this type of bearing a $^{1}/_{16}$-inch ball moves between two sapphire cup jewels of approximately the same dimensions as those used with the pivot-type bearing. One jewel is carried by the meter spindle while the other is mounted in the lower jewel screw. In order to draw conclusions as to the relative motion in both types of bearings under the stated condition, several photographs were made at the same voltage and frequency used in previous tests.

Figure 24 shows the motion of the center of the disk at 115 volts, 60 cycles. This should be compared with figure 7 which shows the motion of the center of the disk using a pivot-type bearing under similar conditions. It is to be noted that in this case the extent of oscillation is approximately double, indicating greater freedom of motion but not entirely so. The mechanical characteristics of the system are such as to permit a greater motion under the same forces.

In order to determine if the ball-bearing system exhibited lower friction and was therefore more responsive to the forces, photographs were taken at lower voltages as shown in figures 25, 26, and 27. It is to be noted that even at 30 volts some motion is recorded while at 90 volts it is very considerable. Figure 27 is to be compared with figure 10 which shows no appreciable motion under similar conditions. Figure 28 illustrates the motion at still higher voltages. In order to show the motion resulting from the double-frequency forces of the potential element only, the damping magnets were removed and photographs were taken at 180 volts, 60 cycles. This is clearly shown in figure 29.

It may be said that the motion of the disk when supported by a ball bearing is similar to that which takes place with a pivot bearing but of considerably greater magnitude.

Conclusions

Among the various conclusions to be drawn from the present study are the following:

1. The simple assumptions adequately explain the observed motion.
2. The motion can be reduced by (*a*) not having the electromagnet too near the edge of the disk, and (*b*) keeping the active area of the permanent magnets as near together as possible and of equal strength.
3. The motion of the meter pivot under no-load condition is in the form of a figure eight and the total travel as taken from the curves is approximately 0.002 inch per cycle or 0.12 inch per second. It is interesting to compare this amount of motion with the amount of motion which takes place between the pivot and jewel as a result of full load on the meter.

In this case the pivot moves up the side of the cup jewel until the forces are balanced by an amount such that the point of contact on the surface of the pivot is at a distance approximately 0.005 inch from the central axis.

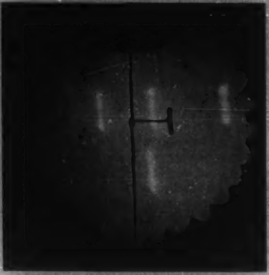

Figure 29. Ball-bearing construction, damping magnets removed; 180 volts, 60 cycles, center of disk

Using a full-load speed of 0.4 revolutions per second, this results in a motion of 0.012 inch per second which is only of the order of one-tenth that referred to above. Since the pressure between pivot and jewel is approximately the same in both cases it may well be argued that the tendency toward wear of the pivot would be roughly ten times as much at no-load as under full-load operation and that the former takes place over an area of the jewel which is directly conducive to light-load inaccuracies. It must also be remembered that these same forces are active when the meter is under load and may therefore contribute to the wear.

An old assumption not wholly in accord with facts was that the amount of pivot wear was roughly proportional to the total number of meter-disk revolutions. The present investigation as applied to the pivot and cup type of bearing tends to indicate that the amount of wear is dependent upon the total number of hours during which the meter is standing idle with only potential applied, plus some function of the total number of revolutions. This might be expressed in the form of an equation as follows:

$$W = T + R + f(T,R)$$

where

W = wear

T = time in hours

R = total number of revolutions

$f(T,R)$ = some unknown function of both time and revolutions since these effects are not wholly independent

It must, however, be stated that the nature of meter-bearing wear is so complex and subject to so many unknown variables that it is quite unsafe to make any arbitrary assumptions except as we may ascertain new facts which would throw more light on the problem and explain a relatively large mass of observations.

It is, moreover, interesting to study briefly the behavior of the ball-type bearing under similar conditions of no-load operation. Since there exists no appreciable side thrust under the condition of no load, this bearing may be thought of as no more than a ball between two flat horizontal surfaces.

Under these conditions almost perfect rolling motion is secured with the result that there should be little or no wear. Since on appreciable load there is a combination of rolling and sliding, we may, as before, write the equation for wear as follows

$$W = R + f(R,T)$$

to indicate that the wear is almost wholly a function of disk revolutions and time, the latter being necessary to take care of such matters as corrosion, etc. Heretofore it has been the practice to lubricate pivot- and cup-type bearings while the ball bearings are not lubricated, although lately some manufacturers have recommended against oil in either type.

In this respect the conclusions to be drawn from the present investigation points clearly to the fact that without lubrication and under similar conditions any given alloy will perform better in a ball-type bearing than as a pivot type as related to watt-hour meters. This conclusion appears to be completely supported by experimental evidence.

Discussion

Otto A. Knopp (Pacific Gas and Electric Company, Emeryville, Calif.): To anyone interested in research work it is a delight to read the paper presented by F. C. Holtz and especially to people who are interested in watt-hour meters.

Watt-hour meters more or less as we know them today, have been operated for some 40 years with jewel bearings of conventional construction. Still we have learned very little of the behavior of the bearing so as to improve its performance.

Up until 15 years ago the watt-hour meter had many weaknesses and limitations so that the jewel bearing was considered on a par with, or better than, the other parts of the meter. Improvements in the design of the watt-hour meter during the last 15 years have removed many of its weaknesses and limitations, and the jewel bearing now stands out as the weakest member in its structural makeup.

A great many new designs for meter bearings have been proposed and perfected during the past several years, based, however, merely on service experience and limited tests and assumptions. Because of the lack of factual information, it is exceedingly difficult to decide which type of bearing will be the best from the standpoint of long-life performance and cost.

Research work described in the paper presented by Mr. Holtz throws an entirely new light on the situation by bringing out the fact that the wear of the jewel bearing is greatest during the hours of no load on the meter. Wear on the jewel during no load hours is possible since it is a common practice to keep the meter that is in service excited with the line potential at all times.

This new and startling observation, the correctness of which has been proved so convincingly by Mr. Holtz, both by calculations and by ingenious experiments, will be of particular importance in giving a guide in the replacement of jewel bearings. It will, no doubt, also have an effect upon the design of the jewel bearings and possibly upon the design of the meter. Mr. Holtz's work represents an important scientific step in the exploration of the behavior of watt-hour meter jewel bearings, and it is hoped that more of this type of work will be done in the future until we have a clear conception of all the factors entering into the design of a satisfactory lower bearing for watt-hour meters.

There is one factor which may enter into this picture so as to reduce the excessive wear at no load. This reduction of wear may come about by a slight wearing of the jewel at the spot at which the pivot point rests during no load. The slightly increased friction may reduce this no load motion of the pivot to zero and when the load is placed on the meter, the pivot point, due to the side thrust, will be drawn away from the no load position and will slide or glide on a smooth, unworn part of the jewel cup. This side thrust, which is very large in the modern high-torque watt-hour meter, brings in new problems in the design of the bearing, and it is hoped that Mr. Holtz will continue his excellent work and thus give us more and more information of real value for the design, selection, and maintenance of the watt-hour meter jewel bearing.

J. H. Goss (General Electric Company, West Lynn, Mass.): The author points out that the phenomenon of vibration of an a-c watt-hour meter moving element under potential excitation alone has been generally recognized. The method of study of the vibration developed by the author is both novel and effective.

One effect of the vibration of the moving element is to reduce the friction in the register, top bearing, and bottom bearing. In fact the friction is reduced to the point that but a small fraction of the torque of the light-load compensating adjustment is required for it. The remainder is needed to correct the unbalance in the electromagnetic element and to compensate for the current iron permeability characteristic.

The equation of wear, $W = T + R + f(T,R)$, was set up for the pivot bearing. For a lubricated pivot bearing the wear of the pivot tends to reach a constant value as shown in a previous AIEE paper ("Lubrication Increases Life of Meter Bearings," T. A. Abbott and J. H. Goss, Electrical Engineering (AIEE Transactions), volume 54, April 1935, pages 428-31). This has been confirmed by actual tests both with watt-hour meters running under load and watt-hour meters operated with potential excitation only. The watt-hour meters operated with potential excitation only actually showed less wear by an appreciable amount than those operated under a moderate load.

The above equation would not represent the condition of wear of a well-lubricated pivot bearing, as neither the term T nor R is linear.

The author points out that in the case of the ball bearing with potential excitation only, a condition of true rolling exists. It is interesting to observe the motion of the ball in a watt-hour meter bearing under actual load conditions. This is possible if a microscope with a magnification of approximately 40 diameters is used. The ball spins erratically as the load changes and presents new points of contacts to the sapphire cups. This renews the actual bearing surfaces and coupled with the rolling action under condition of potential excitation only, no doubt accounts for the performance of the ball-type bearing operating without lubrication.

Paul MacGahan (Westinghouse Electric and Manufacturing Company, Newark, N. J.): This very timely paper by F. C. Holtz is of prime interest, both to the specialists in the field of metering and to the electrical industry in general.

The electrical industry depends entirely upon the registration of the meter for the wherewithal for its development—for its very existence, in fact. Thus, until a practical a-c meter was invented in 1888, the industry could not be financed to proceed beyond the narrow confines of the d-c system. Therefore, any factors contributing to the accuracy and permanence of the meters are of general interest to all concerned.

The reading of the paper discloses that the author is concerned with the vibratory motion of the moving element when excited by the simple single-phase magnetic field due to the voltage coil. This field induces corresponding secondary currents in the disk which do not produce rotation but only an oscillation due to harmonic attraction and repulsion.

Mr. Holtz has, in a very interesting manner, analyzed this condition by the aid of modern methods, both in theory and by laboratory. The conclusions are that the ball bearing should perform better than the pivot type in watt-hour meters.

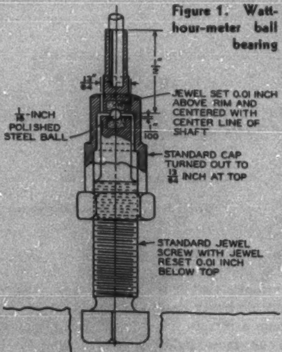
1/2"
13/64"
JEWEL SET 0.01 INCH ABOVE RIM AND CENTERED WITH CENTER LINE OF SHAFT
1/16-INCH POLISHED STEEL BALL
1"/100
STANDARD CAP TURNED OUT TO 13/64 INCH AT TOP
STANDARD JEWEL SCREW WITH JEWEL RESET 0.01 INCH BELOW TOP

Figure 1. Watt-hour-meter ball bearing

Mr. Holtz correctly assumes that all meter engineers know the early history of this development. For the benefit of other engineers not intimately associated with the American meter art, it would seem desirable to add the following brief account of the early history.

The year 1897 brought forth the original so-called "round type" Westinghouse induction-type watt-hour meter. This meter was characterized by the use of a very light moving element and a high ratio of torque to weight, compared to earlier forms. It was small, and in many ways, a distinct advance in the art. The meter was designed by Doctor Frank Conrad and H. P. Davis.

The largest of the early utilities in the a-c field was the United Electric Light and Power Company of New York City, now a part of the Consolidated Edison Company. Mr. Davis' field investigations with W. E. McCoy of the United company led to a suggestion from Mr. McCoy that it would be desirable to use a readily replaceable pivot in the form of a small steel ball.

The year 1902 found the writer engaged as assistant to Frank Conrad in the design of watt-hour meters and instruments. At that time, we had just produced and put on the market an improved form of the original meter using a front-connected terminal chamber and improved means of adjustment.

The writer was directed to have experimental models designed and made, using ball bearings. Tests made by us brought out the fact that vibration in the moving element was causing the pivot bearings to wear rather fast. It took but the sense of touch and a magnifying glass to discover this vibration and to note that it existed even with only the voltage coil connected.

Figure 1 is a reproduction of a drawing based on the original freehand sketch, dated 1902, of the experimental ball bearing as finally decided on. This design comprised the use of a steel ball between two sapphire jewels. The idea was to avoid the wear of pivots because of vibration and rubbing friction, by substituting a rolling action.

The writer recollects considerable discussion on the question of whether the ball actually rolled or whether it only turned like a pivot. The claims that it rolled were questioned in a letter to the editor of *Electrical World*, by William Hallock (see page 700, *Electrical World*, October 21, 1905). To this, the writer replied, giving an explanation of the reason for the rolling action (see *Electrical World*, November 11, 1905, page 827).

It is interesting to note that today, 37 years later, the ball which is used is exactly the same as the one selected in 1902 as being the most suitable in size and quality for this purpose.

This ball bearing was introduced as a further improvement in the so-called sub *A* Davis-Conrad meter in the year 1902.

Another advantage soon became apparent, which was that this bearing did not need any lubrication whatever. In fact, it worked better dry than oiled.

While practically all the meters of the 1902 vintage have been replaced by modern types, many cases are on record of continu-

Transformation Theory of General Static Polyphase Networks

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Synopsis: Starting from the canonical equations of a general, linear, bilateral network and by the application of transformation matrices, giving the various current constraints in typical polyphase systems, equations are obtained which give the response of the most general wye-wye, delta-delta, and delta-wye static systems to arbitrary electromotive forces at the sending and receiving ends. Mutual inductances and capacitances are assumed between the various branches of the circuits.

The paper is concluded with a brief exposition of the method of symmetrical components from the transformation point of view and an application to the general wye-wye circuit is given as an example.

THERE has been considerable work recently in the application of tensors, dyadics, and matrices to electric circuit theory by Kron, Bewley, Sah, Reed, and others. The problem of general static polyphase systems in the steady and transient states does not seem to have been treated. It appears that the great power of these methods is nowhere more evident than when general systems are considered. Once having obtained the solution of these general systems, the solution of simpler ones come under special cases.

In the interest of such generalizations, and to illustrate the power and simplicity of matrix algebra in this class of problems, the study presented in this paper was undertaken.

The properties of matrices given in the paper "Matrices in Engineering" by Pipes[1] will be assumed in the following discussion.

I. Notation

For simplicity, the following notation will be used:

Instantaneous values of currents and voltages will be denoted by small letters i_n, e_n, etc. Matrices whose elements are instantaneous values of currents and voltages will be denoted by $[i]$ and $[e]$ respectively.

Impedance operators of the form $z_{mn} = L_{mn}d/dt + R_{mn} + S_{mn}\int_{-\infty}^{t}(\,)dt$ will be denoted by small z's, and matrices whose elements are impedance operators of the above form will be denoted by $[z]$.

Complex currents and voltages will be denoted by I_n and E_n respectively, and matrices whose elements are complex currents and voltages will be denoted by $[I]$ and $[E]$.

Complex impedance and admittance operators will be denoted by $Z_{mn} = jwL_{mn} + R_{mn} + S_{mn}/jw$ and Y_{mn} respectively. Matrices whose elements are complex impedance and admittance operators will be denoted by $[Z]$ and $[Y]$. This symbolism appears to be simple and convenient.

II. General Transformation Theory

Consider a general n-mesh circuit. Let the n mesh currents have the instantaneous values $(i_1, i_2, \dots i_n)$ and let there be n voltages, $(e_1, e_2, \dots e_n)$ acting on the contours of the n meshes. The canonical equations of such a general n mesh circuit may be written in the form:

$$[e] = [z][i] \tag{1}$$

where $[e]$ and $[i]$ are columnar matrices whose elements are the n mesh voltages and currents respectively and $[z]$ is a square matrix of the nth order whose elements are the various self- and mutual-impedance operators.

If we let ϕ_s be the total flux linkage with respect to mesh s due to all the self and mutual inductances on its contour, we have:

$$[\phi] = [L][i] \tag{2}$$

In this equation $[\phi]$ is a columnar matrix whose elements are the respective flux linkages of the n meshes and $[L]$ is a square matrix of the nth order whose elements are the self- and mutual-inductance coefficients of the circuit.

If T is the total instantaneous magnetic energy of the system, then:

$$T = [i]'[\phi]/2 \tag{3}$$

where $[i]'$ is the transpose of $[i]$.

Substituting the expression in (2) for $[\phi]$ into (3) we obtain:

$$T = \frac{[i]'[L][i]}{2} \tag{4}$$

If we let $(e_{c1} \dots e_{cn})$ be the capacitance voltages induced on the conductors of the respective meshes, we have:

$$[e_c] = [S][q] \tag{5}$$

In this equation, $[S]$ is a square matrix of the nth order whose elements are the self- and mutual-elastance coefficients of the system. $[q]$ is a columnar matrix of the n mesh charges defined by:

$$[q] = \int_{-\infty}^{t} [i]dt \tag{6}$$

Now if V is the total instantaneous electrostatic energy of the system, we have:

$$V = \frac{[q]'[e_c]}{2} \tag{7}$$

where $[q]'$ is the transpose of $[q]$.

Substituting (5) into (7), we obtain:

$$V = \frac{[q]'[S][q]}{2} \tag{8}$$

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[1] For all numbered references, see list at end of paper.

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ous operation of various ball bearing meters for 20 years or over without requiring any replacement of the bearings and without any lubrication problems.

P. C. Holtz: In replying to the remarks made by Mr. Goss, the author wishes to state that the equations of wear were, unfortunately, not a rigorous treatment of the subject. Both T and R should be some undetermined function of the time and revolutions respectively. In fact, even such representation may not be sufficient to explain a mass of observed data.

The part which vibration plays in reducing the friction of a meter register and other moving parts is most interesting and is probably responsible for the very uniform performance of watt-hour meters at light load. The presence of vibration was not recognized in some of the earlier forms of watt-hour meters; however, its advantages were appreciated and special devices were employed to produce sufficient vibration to maintain the moving system in a state of motion.

Mr. Knopp has pointed out in his remarks that a slight increase in friction may be sufficient completely to arrest the motion of the moving system. This has not been observed in lubricated pivot-type meters even after several years' operation; however, it is probably true that the motion was considerably reduced. It would be interesting to study the problem from this angle and determine under what conditions the motion could be arrested. Furthermore, as pointed out by Mr. Goss, certain advantages result from keeping the moving system in a state of vibration.

Letting the induced voltages due to the resistances on the various mesh contours of the system be $(e_{r1} \ldots e_{rn})$, we have:

$$[e_r] = [R][i] \tag{9}$$

Here $[R]$ is a square matrix of the nth order whose elements are the various mutual and self-resistance coefficients of the several meshes.

If we let F be the total instantaneous rate of energy loss of the circuit, we have:

$$F = [i]'[e_r] \tag{10}$$

Substituting (9) into (10), we have:

$$F = [i]'[R][i] \tag{11}$$

Consider now that the original n-mesh system is interconnected in some manner to form a transformed system. By the transformation there will be formed new meshes and consequently new mesh currents will be required to specify the transformed system.

Let the matrix of the new mesh currents be $[i]_n$, and let the new currents be related to the old currents by means of the transformation matrix $[A]$ which need not be nonsingular. That is:

$$[i] = [A][i]_n \tag{12}$$

In general, $[A]$ is an n-rowed matrix of constants.

Now in the new system, the form of the expressions:

$$T = \frac{[i]'[L][i]}{2} \tag{4}$$

$$V = \frac{[q]'[S][q]}{2} \tag{8}$$

$$F = [i]'[R][i] \tag{11}$$

must be preserved, and we must have:

$$T_n = [i]_n'[L]_n[i]_n/2 \tag{13}$$

$$V_n = [q]_n'[S]_n[q]_n/2 \tag{14}$$

$$F_n = [i]_n'[R]_n[i]_n \tag{15}$$

for the instantaneous magnetic energy, electrostatic energy, and power dissipation in the new system. Here $[L]_n$ is the new inductance matrix, $[S]_n$, the new elastance matrix, and $[R]_n$, the new resistance matrix.

To obtain the transformations of the new quantities from the old ones we use the equations:

$$[i] = [A][i]_n \tag{12}$$

$$[q] = [A][q]_n \tag{6}$$

Now, by one of the fundamental theorems of matrix algebra we have:

$$[i]' = ([A][i]_n)' = [i]_n'[A]' \tag{16}$$

$$[q]' = ([A][q]_n)' = [q]_n'[A]' \tag{17}$$

Substituting these expressions into the equations 4, 8, and 11, we obtain for the new network:

$$T_n = [i]_n'[A]'[L][A][I]_n/2 \tag{13}$$

$$V_n = [q]_n'[A]'[A][A][q]_n/2 \tag{14}$$

$$F_n = [i]_n'[A]'[R][A][i]_n \tag{15}$$

Hence if we place:

$$[L]_n = [A]'[L][A] \tag{18}$$

$$[S]_n = [A]'[S][A] \tag{19}$$

$$[R]_n = [A]'[R][A] \tag{20}$$

we preserve the form of the energy equations. We see that the new inductance, elastance, and resistance matrices are obtained from the old inductance, elastance, and resistance matrices by the same type of transformation involving the matrix $[A]$ and its transpose.

TRANSFORMATION OF THE IMPEDANCE MATRIX

The transformation of the impedance matrix is easily obtained by noting that:

$$[z]_n = [L]_n\frac{d}{dt} + [R]_n + [S]_n\int_{-\infty}^{t}(\ )dt \tag{21}$$

and

$$[z] = [L]\frac{d}{dt} + [R] + [S]\int_{-\infty}^{t}(\ )dt \tag{22}$$

In view of (18), (19), and (20), we see that:

$$[z]_n = [A]'[z][A] \tag{23}$$

is the transformation equation for the impedance operator matrix.

TRANSFORMATION OF THE MESH VOLTAGES

In the new system, the canonical equations of the circuit may be written in the matrix form:

$$[e]_n = [z]_n[i]_n \tag{24}$$

or, in view of (23)

$$[e]_n = [A]'[z][A][i]_n \tag{25}$$

But

$$[i] = [A][i]_n \tag{12}$$

Hence:

$$[e]_n = [A]'[z][i] \tag{26}$$

However, by (1) $[e] = [z][i]$, therefore:

$$[e]_n = [A]'[e] \tag{27}$$

This is the transformation formula for the mesh voltages.

TRANSFORMATION OF COMPLEX QUANTITIES

Since the complex currents and voltages differ from the instantaneous currents and voltages only by scalar multiplicative phase constants, and the complex impedances differ from the impedance operators in that d/dt is replaced by jw and $\int_{-\infty}^{t}(\ )dt$ by $1/jw$, we see that these quantities transform in the same manner that the scalar instantaneous quantities transform, and hence we have that if:

$$[I] = [A][I]_n \tag{28}$$

that is, the complex voltages of the new system are linearly related to the complex voltages of the original system by the transformation matrix $[A]$, then:

$$[Z]_n = [A]'[Z][A] \tag{29}$$

and

$$[E]_n = [A]'[E] \tag{30}$$

III. Application to the General Wye-Wye Circuit

Let us apply the above theory to obtain the performance of the general wye-wye circuit of figure 1. For generality, we shall consider mutual inductance and capacitance between the three sending-end impedances, the four line impedances, and the three receiving-end impedances. We shall also assume that there are impressed voltages in series with the three sending-end impedances and the three receiving-end impedances.

The completed wye-wye circuit may be considered as made up by the synthesis of a ten-mesh network whose canonical equation is:

$$[e] = [z][i] \tag{1}$$

where:

$$[e] = \begin{array}{c|c} a & e_a \\ b & e_b \\ c & e_c \\ r & 0 \\ s & 0 \\ t & 0 \\ n & 0 \\ 1 & e_1 \\ 2 & e_2 \\ 3 & e_3 \end{array} \qquad [i] = \begin{array}{c|c} a & i_a \\ b & i_b \\ c & i_c \\ r & i_r \\ s & i_s \\ t & i_t \\ n & i_n \\ 1 & i_1 \\ 2 & i_2 \\ 3 & i_3 \end{array}$$

$$[z] = \begin{array}{c|cccccccccc} & a & b & c & r & s & t & n & 1 & 2 & 3 \\ \hline a & z_{aa} & z_{ab} & z_{ac} & 0 & 0 & 0 & 0 & 0 & 0 & 0 \\ b & z_{ba} & z_{bb} & z_{bc} & 0 & 0 & 0 & 0 & 0 & 0 & 0 \\ c & z_{ca} & z_{cb} & z_{cc} & 0 & 0 & 0 & 0 & 0 & 0 & 0 \\ r & 0 & 0 & 0 & z_{rr} & z_{rs} & z_{rt} & z_{rn} & 0 & 0 & 0 \\ s & 0 & 0 & 0 & z_{sr} & z_{ss} & z_{st} & z_{sn} & 0 & 0 & 0 \\ t & 0 & 0 & 0 & z_{tr} & z_{ts} & z_{tt} & z_{tn} & 0 & 0 & 0 \\ n & 0 & 0 & 0 & z_{nr} & z_{ns} & z_{nt} & z_{nn} & 0 & 0 & 0 \\ 1 & 0 & 0 & 0 & 0 & 0 & 0 & 0 & z_{11} & z_{12} & z_{13} \\ 2 & 0 & 0 & 0 & 0 & 0 & 0 & 0 & z_{21} & z_{22} & z_{23} \\ 3 & 0 & 0 & 0 & 0 & 0 & 0 & 0 & z_{31} & z_{32} & z_{33} \end{array}$$

This ten-mesh network is transformed into the wye-wye circuit of figure 1 by interconnection. When the original network is interconnected, new meshes are formed and the old currents undergo certain constraints. If we let (i_a', i_b', i_c') be the currents flowing through the impedances z_{aa}, z_{bb}, and z_{cc} in the transformed network, we have the following equations giving the current constraints imposed on the old currents by the interconnection.

$$\left.\begin{array}{llll} i_a=i_a' & i_r=i_a' & i_t=i_c' & i_2=-i_b' \\ i_b=i_b' & i_s=i_b' & i_n=-(i_a'+i_b'+i_c') & \\ i_c=i_c' & & i_1=-i_a' & i_3=-i_c' \end{array}\right\} \quad (31)$$

That is, the old currents are linearly related to the new currents by the transformation matrix $[A]$, where in this case:

$$[A]=\begin{array}{c} \\ a\\ b\\ c\\ r\\ s\\ t\\ n\\ 1\\ 2\\ 3 \end{array}\!\!\begin{array}{c} \begin{array}{ccc} a' & b' & c' \end{array} \\ \left\|\begin{array}{ccc} 1 & 0 & 0\\ 0 & 1 & 0\\ 0 & 0 & 1\\ 1 & 0 & 0\\ 0 & 1 & 0\\ 0 & 0 & 1\\ -1 & -1 & -1\\ -1 & 0 & 0\\ 0 & -1 & 0\\ 0 & 0 & -1 \end{array}\right\| \end{array} \quad (32)$$

$$[A]'=\left\|\begin{array}{cccccccccc} 1 & 0 & 0 & 1 & 0 & 0 & -1 & -1 & 0 & 0\\ 0 & 1 & 0 & 0 & 1 & 0 & -1 & 0 & -1 & 0\\ 0 & 0 & 1 & 0 & 0 & 1 & -1 & 0 & 0 & -1 \end{array}\right\| \quad (33)$$

We now compute the new impedance matrix by means of the equation:

$$[z]_n=[A]'[z][A] \quad (23)$$

By direct multiplication, we have:

$$[z]_n=\left\|\begin{array}{ccc} a_{11} & a_{12} & a_{13}\\ a_{21} & a_{22} & a_{23}\\ a_{31} & a_{32} & a_{33} \end{array}\right\| \quad (34)$$

where:

$$a_{11}=(z_{aa}+z_{rr}-2z_{nr}+z_{nn}+z_{11})$$
$$a_{22}=(z_{bb}+z_{ss}-2z_{ns}+z_{nn}+z_{22})$$
$$a_{33}=(z_{cc}+z_{tt}-2z_{nt}+z_{nn}+z_{33})$$
$$a_{12}=(z_{ab}+z_{rs}-z_{rn}-z_{ns}+z_{nn}+z_{12})=a_{21}$$
$$a_{13}=(z_{ac}+z_{rt}-z_{rn}-z_{nt}+z_{nn}+z_{13})=a_{31}$$
$$a_{23}=(z_{bc}+z_{st}-z_{sn}-z_{nt}+z_{nn}+z_{23})=a_{32}$$

We note that the new impedance matrix is symmetric. The new voltages are transformed by the equation:

$$[e]_n=[A]'[e] \quad (27)$$

In this case, by direct multiplication, we obtain:

$$[e]_n=\left\|\begin{array}{c} (e_a-e_1)\\ (e_b-e_2)\\ (e_c-e_3) \end{array}\right\| \quad (35)$$

We then have:

$$[e]_n=[z]_n[i]_n \quad (24)$$

or:

$$[i]_n=[z]_n^{-1}[e]_n \quad (36)$$

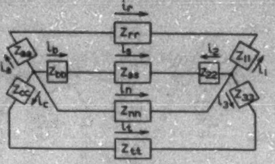

Figure 1. General wye-wye circuit

Z_{ab} is the mutual impedance between Z_{aa} and Z_{bb}, etc.

or placing:

$$[y]_n=[z]_n^{-1} \quad (37)$$

we have:

$$[i]_n=[y]_n[e]_n \quad (38)$$

Calculating the inverse of the matrix (34), we obtain:

$$[y]_n=\frac{1}{D}\left\|\begin{array}{ccc} (a_{22}a_{33}-a_{23}^2) & (a_{23}a_{13}-a_{12}a_{33}) & (a_{12}a_{23}-a_{22}a_{13})\\ (a_{31}a_{23}-a_{21}a_{33}) & (a_{11}a_{33}-a_{13}^2) & (a_{21}a_{13}-a_{11}a_{23})\\ (a_{21}a_{32}-a_{31}a_{22}) & (a_{31}a_{12}-a_{11}a_{32}) & (a_{11}a_{22}-a_{12}^2) \end{array}\right\| \quad (39)$$

and D is the determinant

$$D=\left|\begin{array}{ccc} a_{11} & a_{12} & a_{13}\\ a_{21} & a_{22} & a_{23}\\ a_{31} & a_{32} & a_{33} \end{array}\right|$$

we note that the matrix $[y]_n$ is symmetric.

The currents in the various elements are now obtained from:

$$[i]=[A][i]_n \quad (12)$$

The equation $[e]_n = [Z]_n[i]_n$ may be given a rather interesting physical interpretation. If we write:

$$[e]_s=\left\|\begin{array}{c} e_a\\ e_b\\ e_c \end{array}\right\| \quad (40) \qquad [e]_r=\left\|\begin{array}{c} e_1\\ e_2\\ e_3 \end{array}\right\| \quad (41)$$

$$[z]_s=\left\|\begin{array}{ccc} z_{aa} & z_{ab} & z_{ac}\\ z_{ba} & z_{bb} & z_{bc}\\ z_{ca} & z_{cb} & z_{cc} \end{array}\right\| \quad (42)$$

$$[z]_r=\left\|\begin{array}{ccc} z_{11} & z_{12} & z_{13}\\ z_{21} & z_{22} & z_{23}\\ z_{31} & z_{32} & z_{33} \end{array}\right\| \quad (43)$$

$$[z]_L=\left\|\begin{array}{ccc} (z_{rr}-2z_{rn}+z_{nn}) & (z_{rs}-z_{rn}-z_{ns}+z_{nn}) & (z_{rt}-z_{rn}-z_{nt}+z_{nn})\\ (z_{sr}-z_{sn}-z_{nr}+z_{nn}) & (z_{ss}-2z_{sn}+z_{nn}) & (z_{st}-z_{sn}-z_{nt}+z_{nn})\\ (z_{tr}-z_{tn}-z_{nr}+z_{nn}) & (z_{ts}-z_{tn}-z_{ns}+z_{nn}) & (z_{tt}-2z_{nt}+z_{nn}) \end{array}\right\| \quad (44)$$

Then, we may write:

$$[e]_s=[z]_s[i]_n+[z]_L[i]_n+[z]_r[i]_n+[e]_r \quad (45)$$

This equation is the same as that of a simple series circuit with the exception that the ordinary voltages, impedances, and current are replaced by matrix quantities.

THE STEADY STATE

In the above analysis, we have considered the transformation of the instantaneous voltages, currents, and the impedance operators. In case the impressed voltages at the sending and receiving ends are sinusoidal quantities of arbitrary phase, all the foregoing equations hold when the instantaneous voltage matrix, $[e]$, is replaced by the vector voltage matrix, $[E]$, the instantaneous current matrix, $[i]$, is replaced by the vector current matrix, $[I]$, and the impedance operator matrix, $[z]$, is replaced by the complex impedance matrix, $[Z]$.

We have, then, the equation:

$$[I]_n=[Z]^{-1}[E]_n=[Y]_n[E]_n \quad (46)$$

for the determination of the vector currents in terms of the complex admittance matrix and the complex voltage matrix. The impedance matrix $[Y]_n$ is given by (39) where the a's have the values given in (34) and the z's are replaced by the Z's or complex impedance operators.

TRANSIENT DISTURBANCES (GENERAL ELECTROMOTIVE FORCES)

Having obtained the equation of the circuit in the canonical form:

$$[e]_n=[z]_n[i]_n \quad (24)$$

we may proceed to find the response of the circuit to the effect of arbitrary electromotive forces impressed at $t=0$ by the use of the Laplacian transformation (see reference 2). In order to do this,

$$[E(p)]_n=p\int_0^\infty e^{-pt}[e]_n dt \quad (47)$$

$$[I(p)]_n=p\int_0^\infty e^{-pt}[i]_n dt \quad (48)$$

Then if we take (24) and multiply both sides by $pe^{-pt}dt$ and integrate them from zero to infinity, we have:

$$p\int_0^\infty e^{-pt}[e]_n dt=p\int_0^\infty e^{-pt}[z]_n[i]_n dt \quad (49)$$

Now since:

$$[z]_n=[L]_n\frac{d}{dt}+[R]_n+[S]_n\int_{-\infty}^{t}(\ )dt \quad (21)$$

we may transform (49) by integrating by parts and we obtain:

$$[Z(p)]_n[I(p)]_n=[E(p)]_n+p[L]_n[i^0]_n-[S]_n[q^0]_n \quad (50)$$

where $[i^0]_n$ is a columnar matrix giving the initial currents in the mesh inductances at $t=0$. $[q^0]_n$ is a columnar matrix giving the initial charges in the mesh elastances at $t=0$. $[Z(p)]$ is a matrix whose elements are:

$$Z_{mn}=pL_{mn}+R_{mn}+S_{mn}/p \tag{51}$$

If we multiply (50) by $[Z(p)]_n^{-1}$, we get:

$$[I(p)]_n=[Z(p)]_n^{-1}([E(p)]_n+p[L]_n[i^0]_n-[S]_n[q^0]_n) \tag{52}$$

Now, by means of the Fourier-Mellin inversion formula, we have an explicit expression for the instantaneous current matrix:

$$[i(t)]_n=\frac{1}{2\pi j}\int_{c-j\infty}^{c+j\infty}\frac{e^{pt}[I(p)]_n dt}{p} \tag{53}$$

and we have a formal solution for the circuit currents when arbitrary electromotive forces, initial currents, and initial charges are impressed upon the circuit at $t=0$.

IV. The Delta-Delta Circuit

We shall now carry out a similar treatment of the general delta-delta circuit shown in figure 2. We shall consider electromotive forces in series with the three delta-connected impedances at the sending end, electromotive forces in series with the three delta-connected impedances at the receiving end, and also mutual inductances and capacitances will be assumed between the three sending-end impedances, the three receiving-end impedances, and the three line impedances.

The delta-delta circuit may be considered as made up by the synthesis of a nine-mesh circuit which is interconnected to form the required four-mesh delta-delta circuit.

The impedance matrix of the original nine-mesh circuit is:

$$[z]=\left\|\begin{matrix} z_{aa} & z_{ab} & z_{ac} & 0 & 0 & 0 & 0 & 0 & 0\\ z_{ba} & z_{bb} & z_{bc} & 0 & 0 & 0 & 0 & 0 & 0\\ z_{ca} & z_{cb} & z_{cc} & 0 & 0 & 0 & 0 & 0 & 0\\ 0 & 0 & 0 & z_{rr} & z_{rs} & z_{rt} & 0 & 0 & 0\\ 0 & 0 & 0 & z_{sr} & z_{ss} & z_{st} & 0 & 0 & 0\\ 0 & 0 & 0 & z_{tr} & z_{ts} & z_{tt} & 0 & 0 & 0\\ 0 & 0 & 0 & 0 & 0 & 0 & z_{11} & z_{12} & z_{13}\\ 0 & 0 & 0 & 0 & 0 & 0 & z_{21} & z_{22} & z_{23}\\ 0 & 0 & 0 & 0 & 0 & 0 & z_{31} & z_{32} & z_{33} \end{matrix}\right\| \tag{54}$$

In order to specify the interconnected delta-delta circuit, we shall use the four mesh currents (i_1', i_2', i_3', i_4') shown in figure 2. The elements of the transformation matrix, $[A]$, are obtained from the set of equations:

$$\begin{matrix} i_a=i_1'-i_3' & i_r=i_3' & i_1=i_2'+i_3'\\ i_b=i_1'-i_4' & i_s=i_4'-i_3' & i_2=i_2'+i_4'\\ i_c=i_1' & i_t=-i_4' & i_3=i_2' \end{matrix} \tag{55}$$

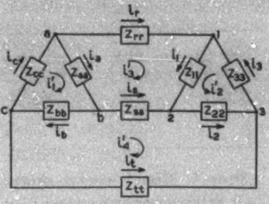

Figure 2. General delta-delta circuit

Hence the transformation matrix is:

$$[A]=\left\|\begin{matrix} 1 & 0 & -1 & 0\\ 1 & 0 & 0 & -1\\ 1 & 0 & 0 & 0\\ 0 & 0 & 1 & 0\\ 0 & 0 & -1 & 1\\ 0 & 0 & 0 & -1\\ 0 & 1 & 1 & 0\\ 0 & 1 & 0 & 1\\ 0 & 1 & 0 & 0 \end{matrix}\right\| \tag{56}$$

$$[A]'=\left\|\begin{matrix} 1 & 1 & 1 & 0 & 0 & 0 & 0 & 0 & 0\\ 0 & 0 & 0 & 0 & 0 & 0 & 1 & 1 & 1\\ -1 & 0 & 0 & 1 & -1 & 0 & 1 & 0 & 0\\ 0 & -1 & 0 & 0 & 1 & -1 & 0 & 1 & 0 \end{matrix}\right\| \tag{57}$$

By direct multiplication using the equation:

$$[z]_n=[A]'[z][A], \tag{23}$$

we obtain

$$[z]_n=\left\|\begin{matrix} a_{11} & a_{12} & a_{13} & a_{14}\\ a_{21} & a_{22} & a_{23} & a_{24}\\ a_{31} & a_{32} & a_{33} & a_{34}\\ a_{41} & a_{42} & a_{43} & a_{44} \end{matrix}\right\| \tag{58}$$

where:

$$\begin{aligned} a_{11}&=z_{aa}+z_{bb}+z_{cc}+2z_{ab}+2z_{ac}+2z_{bc}\\ a_{22}&=z_{11}+z_{22}+z_{33}+2z_{12}+2z_{13}+2z_{23}\\ a_{33}&=z_{aa}+z_{rr}+z_{ss}+z_{11}-2z_{rs}\\ a_{44}&=z_{bb}+z_{ss}+z_{tt}+z_{22}-2z_{ts}\\ a_{12}&=a_{21}=0\\ a_{13}&=-(z_{aa}+z_{ba}+z_{ca})=a_{31}\\ a_{14}&=-(z_{ab}+z_{bb}+z_{cb})=a_{41}\\ a_{23}&=z_{11}+z_{21}+z_{31}=a_{32}\\ a_{24}&=z_{12}+z_{22}+z_{32}=a_{42}\\ a_{34}&=z_{ab}+z_{rs}-z_{ss}+z_{st}-z_{rt}+z_{12}=a_{43} \end{aligned}$$

The original voltage matrix is:

$$[e]=\left\|\begin{matrix} e_a\\ e_b\\ e_c\\ 0\\ 0\\ 0\\ e_1\\ e_2\\ e_3 \end{matrix}\right\| \tag{59}$$

$$[e]_n=[A]'[e]=\left\|\begin{matrix} (e_a+e_b+e_c)\\ (e_1+e_2+e_3)\\ (-e_a+e_1)\\ (-e_b+e_2) \end{matrix}\right\| \tag{60}$$

The transformed voltage matrix.

We now have:

$$[i]_n=[z]_n^{-1}[e]_n \tag{36}$$

the formal expression for the various mesh currents.

The treatment for the steady-state and transient behavior may be carried out in the manner discussed in part III.

V. The Delta-Wye Circuit

Let us consider the delta-wye circuit shown in figure 3. As in the above examples, we may consider this circuit as made up by the synthesis of a nine-mesh circuit whose impedance matrix is given by (54).

The delta-wye circuit will be specified by the three mesh currents (i_1', i_2', i_3') as shown in the figure. In this case, the elements of the transformation matrix are given by the set of equations giving the following current constraints:

$$\begin{matrix} i_a=i_1'-i_2' & i_r=i_2' & i_1=i_2'\\ i_b=i_1'-i_3' & i_s=i_3'-i_2' & i_2=i_2'-i_3'\\ i_c=i_1' & i_t=-i_3' & i_3=i_3' \end{matrix}$$

Hence we have the following transformation matrix:

$$[A]=\left\|\begin{matrix} 1 & -1 & 0\\ 1 & 0 & -1\\ 1 & 0 & 0\\ 0 & 1 & 0\\ 0 & -1 & 1\\ 0 & 0 & -1\\ 0 & -1 & 0\\ 0 & 1 & -1\\ 0 & 0 & 1 \end{matrix}\right\| \tag{62}$$

$$[A]'=\left\|\begin{matrix} 1 & 1 & 1 & 0 & 0 & 0 & 0 & 0 & 0\\ -1 & 0 & 0 & 1 & -1 & 0 & -1 & 1 & 0\\ 0 & -1 & 0 & 0 & 1 & -1 & 0 & -1 & 1 \end{matrix}\right\| \tag{63}$$

By the equation:

$$[z]_n=[A]'[a][A] \tag{23}$$

we obtain the transformed matrix:

$$[z]_n=\left\|\begin{matrix} a_{11} & a_{12} & a_{13}\\ a_{21} & a_{22} & a_{23}\\ a_{31} & a_{32} & a_{33} \end{matrix}\right\| \tag{64}$$

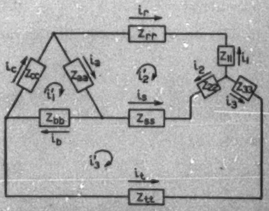

Figure 3. General delta-wye circuit

where:

$$\begin{aligned} a_{11}&=z_{aa}+z_{bb}+z_{cc}+2z_{ab}+2z_{ca}+2z_{cb}\\ a_{22}&=z_{aa}+z_{rr}+z_{ss}+z_{22}-2z_{sr}-2z_{12}+z_{11}\\ a_{33}&=z_{bb}+z_{cc}+z_{11}+z_{22}+z_{33}-2z_{13}-2z_{23}\\ a_{12}&=-(z_{aa}+z_{ab}+z_{ac})=a_{21} \qquad (65)\\ a_{13}&=-(z_{ab}+z_{bb}+z_{cb})=a_{31}\\ a_{23}&=(z_{ab}+z_{rs}-z_{sb}-z_{rt}+z_{st}+z_{12}-z_{22}-z_{13}+\\ &\qquad z_{23})=a_{32} \end{aligned}$$

The inverse matrix:

$$[y]_n=[z]_n^{-1} \qquad (37)$$

is given in terms of the a's by (39) where in this circuit, the a's have the values given by (65).

In this case, the original voltage matrix is given by (59) and by the transformation we obtain:

$$[e]_n=[A]'[e]=\left\|\begin{matrix}(e_a+e_b+e_c)\\(e_2-e_a-e_1)\\(e_s-e_b-e_2)\end{matrix}\right\| \qquad (66)$$

The various mesh currents are given by:

$$[i]_n=[z]_n^{-1}[e]_n \qquad (36)$$

and the solution for the steady state or the transient behavior follows the same reasoning of part III.

It is noted that by considering these general cases, we have covered all possible combinations of delta and wye connections. In the case that the circuits have balanced impedances and voltages, considerable simplifications are effected in the above general equations. In case even more general circuits are considered, the matrices of the original nine or ten mesh circuits whose synthesis produce the required circuits would have mutual impedances between every mesh. The synthesis of such networks would produce polyphase systems having coupling between every elements. The amount of coupling considered here is sufficiently general for practical purposes.

VI. Symmetrical Components

The discussion will be concluded with a brief consideration of the transformation theory of the method of symmetrical components. Since the method of symmetrical components is one of linear transformation, it lends itself readily to matrix formulation.

In the method of symmetrical components, the transformations are effected by the use of the nonsingular, symmetric matrix:

$$[S]=\left\|\begin{matrix}1&1&1&\cdot&\cdot&\cdot&1\\1&a^{-1}&a^{-2}&\cdot&\cdot&\cdot&a^{-(n-1)}\\1&a^{-2}&a^{-4}&\cdot&\cdot&\cdot&a^{-2(n-1)}\\\cdot&\cdot&\cdot&\cdot&\cdot&\cdot&\cdot\\1&a^{-(n-1)}&a^{-2(n-1)}&\cdot&\cdot&\cdot&a^{-(n-1)(n-1)}\end{matrix}\right\| \qquad (67)$$

where:

$$a=e^{2\pi j/n} \qquad (68)$$

The matrix $[S]$ may be written more briefly in the manner:

$$[S]=\left\|\begin{matrix}S_0\\S_{-1}\\S_{-2}\\-\\S_{-(n-1)}\end{matrix}\right\| \qquad (69)$$

where:

$$S_{-r}=(1,a^{-r},a^{-2r},\ldots a^{-(n-1)r}) \qquad (70)$$

is the same as Fortescue's sequence operator S^r.

Fundamental Properties of [S]

Inspection of the matrix $[S]$, reveals that it has the following important properties:

$$[S]'=[S] \qquad (71)$$

where $[S]$ is the transpose of $[S]$

$$[S]^{-1}=\frac{1}{n}[\bar{S}] \qquad (72)$$

where $[S]^{-1}$ is the inverse of $[S]$ and $[\bar{S}]$ is the conjugate of $[S]$ given by:

$$[\bar{S}]=\left\|\begin{matrix}S_0\\S_1\\S_2\\-\\S_{(n-1)}\end{matrix}\right\|$$

and n is the order of $[S]$.

Transformation of Voltages and Currents

Let the ordinary complex voltages and currents of an n-phase network be written in the form:

$$[I]=\left\|\begin{matrix}I_1\\I_2\\-\\I_n\end{matrix}\right\| \quad (73) \qquad [E]=\left\|\begin{matrix}E_1\\E_2\\ \\E_n\end{matrix}\right\| \quad (74)$$

and let $[I]_s$ and $[E]_s$ be the symmetrical components of current and voltage respectively. They are specified by the equations:

$$[i]=[S][I]_s \qquad (75)$$

$$[E]=[S][E]_s \qquad (76)$$

Or, since $[S]$ is a nonsingular matrix, its inverse exists and we have:

$$[I]_s=[S]^{-1}[i]=\frac{1}{n}[\bar{S}][I] \qquad (77)$$

$$[E]_s=[S]^{-1}[E]=\frac{1}{n}[\bar{S}][E] \qquad (78)$$

Transformation of Impedances and Admittances

The impedance matrix $[Z]$ associated with the ordinary currents $[I]$ and the ordinary voltages $[E]$ satisfies the equation:

$$[E]=[Z][I] \qquad (79)$$

or

$$[S][E]_s=[Z][S][I]_s \qquad (80)$$

Hence:

$$[E]_s=[S]^{-1}[Z][S][I]_s \qquad (81)$$

If we place:

$$[Z]_s=[S]^{-1}[Z][S] \qquad (82)$$

the symmetrical component matrix, then we have:

$$[E]_s=[Z]_s[I]_s \qquad (83)$$

the voltage equation in symmetrical components.

From (82) we see that $[Z]_s$ is obtained from $[S]$ by a similarity transformation. We have also the reciprocal relation:

$$[Z]=[S][Z]_s[S]^{-1}=\frac{1}{n}[S][Z]_s[\bar{S}]$$

By the fundamental definition of the admittance matrix, we have:

$$[Y]_s=[Z]_s^{-1}=([S]^{-1}[Z][S])^{-1} \qquad (84)$$

Now, by a fundamental theorem of matrix algebra, if A, B, and C are nonsingular matrices, then:

$$(ABC)^{-1}=C^{-1}B^{-1}A^{-1}$$

Applying this to (84), we obtain:

$$[Y]_s=[S]^{-1}[Z]^{-1}[S]^{-1} \qquad (85)$$

But $[Z]^{-1}=[Y]$, hence:

$$[Y]_s=[S]^{-1}[Y][S]=\frac{1}{n}[\bar{S}][Y][S] \qquad (86)$$

and we have the reciprocal relation:

$$[Y]=[S][Y]_s[S]^{-1}=\frac{1}{n}[S][Y]_s[\bar{S}] \qquad (87)$$

We see from this that the symmetrical component impedance and admittance matrices are derived from the ordinary impedance and admittance matrices by similarity transformations.

Transformation of the Vector Volt-Ampere Input

The vector volt-ampere input P in terms of the original voltages and currents of the system is defined by the equation:

$$P=[I]'[\bar{E}] \qquad (88)$$

Now by (75) and (76), this transforms into:

$$P=([S][I]_s)'[\bar{S}][\bar{E}]_s \qquad (89)$$

But $([S][I]_s)' = [I]_s'[S]'$ by a fundamental theorem.

Hence:

$$P = [I]_s'[S]'[\bar{S}][\bar{E}]_s \tag{90}$$

Now $[\bar{S}] = n[S]^{-1}$, $[S]' = [S]$, hence:

$$P = [I]_s'[S]n[S]^{-1}[\bar{E}]_s = n[I]_s'[\bar{E}]_s \tag{91}$$

But by definition, the vector volt-ampere input in the symmetrical component quantities is:

$$P_s = [I]_s'[\bar{E}]_s \tag{92}$$

hence:

$$P = nP_s \tag{93}$$

And we see that the actual vector volt-ampere input to the system is n times the amount as calculated by (92) where n is the order of the phase of the system.

VII. Symmetrical Components in General Wye-Wye Circuit

If we write (45) in terms of complex voltages, impedances, and current, we have:

$$[E]_s = [Z]_s[I]_n + [Z]_L[I]_n + [Z]_r[I]_n + [E]_r \tag{94}$$

Let us multiply this equation by $[S]^{-1}$, we then obtain:

$$\begin{aligned}[S]^{-1}[E]_s = [S]^{-1}[Z]_s[S][S]^{-1}[I]_n + {} \\ [S]^{-1}[Z]_L[S][S]^{-1}[I]_n + {} \\ [S]^{-1}[Z]_r[S][S]^{-1}[I]_n + [S]^{-1}[E]_r\end{aligned} \tag{95}$$

If we now introduce:

$[S]^{-1}[E]_s = [E]_{ss}$ the symmetrical component matrix of sending-end voltages (96)

$[S]^{-1}[E]_r = [E]_{rs}$ the symmetrical component matrix of receiving-end voltages (97)

$[I]_{ns} = [S]^{-1}[I]_n$ a matrix whose elements are the symmetrical components of line currents (98)

$[Z]_{ss} = [S]^{-1}[Z]_s[S]$ the symmetrical component matrix of sending-end impedances (99)

$[Z]_{Ls} = [S]^{-1}[Z]_L[S]$ the symmetrical component matrix of line impedances (100)

$[Z]_{rs} = [S]^{-1}[Z]_r[S]$ the symmetrical component matrix of receiving-end impedances (101)

then (95) becomes:

$$\begin{aligned}[E]_{ss} = [Z]_{ss}[I]_{ns} + [Z]_{Ls}[i]_{ns} = {} \\ [Z]_{rs}[I]_{ns} + [E]_{rs}\end{aligned} \tag{102}$$

and we have explicitly:

$$[I]_{ns} = ([Z]_{ss} + [Z]_{Ls} + [Z]_{rs})^{-1}([E]_{ss} - [E]_{rs}) \tag{103}$$

an equation for the determination of the symmetrical components of line currents. It may be noted that in case the generator and receiver voltages are balanced, a considerable simplification will be effected.

The currents in the various parts of the system are given by:

$$[I] = [A][I]_n \tag{28}$$

If we multipy this equation by $[S]^{-1}$, we obtain:

$$[S]^{-1}[I] = [S]^{-1}[A][S][S]^{-1}[I]_n \tag{104}$$

If we place:

$[I]_s = [S]^{-1}[I]$ the symmetrical component matrix of the currents in the various parts of the system (105)

$[A]_s = [S]^{-1}[A][S]$ the symmetrical component transformation matrix (106)

then (104) may be written in the form:

$$[I]_s = [A]_s[I]_{ns} \tag{107}$$

and we see that the symmetrical component matrix of the currents in the various parts of the system are obtained from the symmetrical component matrix of the line currents by means of a transformation matrix $[A]_s$, obtained from the ordinary transformation matrix $[A]$ by means of a similarity transformation.

VIII. Conclusion

The simplicity by means of which general expressions for complex networks are obtained by the use of matrix notation and transformation theory is apparent from the above examples. The systematization made possible by concise matrix manipulation makes the algebraic work a minimum and the trend of the argument is brought clearly into the foreground.

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Discussion

Myril B. Reed (Armour Institute of Technology, Chicago, Ill.): The concept of an *old* space, or mesh system, in which a network may be represented in a so-called generalized form, and from which, by means of a transformation matrix, the generalized impedance matrix may be transformed into the impedance matrix of the connected, or *new* space, network has been developed and used as the basis of the extensive work on static networks and machines written by Kron. Pipes has used this same concept.

In writing the introductory work preliminary to the application of tensors, the transformation from one space to another is not only pertinent but essential, since tensor analysis is based on transforming from one space to another. Even so, it should be pointed out that insofar as static networks are concerned the tensor concept and, therefore, transformation from an old to a new space is not necessary. In fact the C matrix of Kron and the A matrix of Pipes are somewhat artificial when applied to static-network theory. These transformations are used to establish the impedance matrix for the physical network. But this may be done rather easily by means of Kirchhoff's voltage equations in terms of Maxwell's cyclic currents, and the *old* and *new* spaces, or mesh systems, do not make the impedance matrix easier to form, understand, or use.

However, the idea of matrix transformations is useful, and can be used effectively in many ways as the papers indicated in the bibliography of Pipes' paper demonstrate. The pure mathematician has developed an extensive theory of matrices and matrix transformations which may be borrowed whole-cloth as it were, and applied to the manipulation of the set of linear equations expressing the properties of an electric circuit; and from these manipulations we may expect to reach conclusions formerly hidden by the very fact that the Kirchhoff equations are many, and, therefore, difficult to manipulate as a unit.

Louis A. Pipes: Professor Reed, in his discussion, points out that the transformation concept is used to establish the impedance matrix of the physical network and that this may be done more easily by means of a direct application of Kirchhoff's equations. For simple systems involving only a few mutual coupling coefficients, this is perhaps true. However, in complex systems, there is a very great danger that in setting up the Kirchhoff equations certain terms may be omitted because of the complexity of the problem. Once the generalized impedance matrix is written down, however, the actual impedance matrix of the system may be easily obtained without further thought on the part of the analyst by means of the "constraint" matrix, A. The philosophy of the method is very similar to the use of the Lagrangian equations in dynamics to obtain the equations of motion of dynamical systems when the energy and dissipation functions are known.

That the concept of matrix transformation is useful in electrical circuit theory has by now been amply demonstrated by the numerous papers on its application to this field which have appeared recently.

Radio-Frequency High-Voltage Phenomena

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DURING the many years that large amounts of power have been transmitted over long power lines at high voltage, an enormous amount of work has been done on research and development in connection with the high-voltage low-frequency phenomena encountered. In the years that followed, the use of low radio frequencies for communication purposes brought about the need for knowledge of high-voltage phenomena in the region of 100 kilocycles and up to about 200 kilowatts. The men associated with this equipment did a remarkably good job of design on their equipment as may be seen in some of the apparatus still in use, especially in regard to the large antenna insulators used on the long-wave antennas.

When vacuum tubes made the generation of much higher radio frequencies possible, and it was found that the shorter waves transmitted intelligence more efficiently over long distances, the use of large amounts of power for radio transmitters was discontinued, first, because there was no equipment available to produce so much power and, second, because the more efficient propagation of the shorter waves and the more efficient vacuum-tube receiving equipment provided a satisfactory means of communication with lower power. This being the case, very high voltages at radio frequencies faded into the background.

With the progression of the art, equipment capable of handling larger and larger amounts of power up to the ultrahigh radio frequencies has been developed in order to provide an ever better means of communication between the remotest parts of the globe. With the increasing power has come the problem of voltage stress but only in proportion to about the square root of the energy used, hence the high-voltage difficulties have lagged the development considerably until recently. Voltages presently in use in high-powered transmitters are sufficient to cause frequent dielectric breakdowns, but in most cases the problems have been side-stepped by overdesigning, or what was thought to be so, in order that concentrated effort could be put on other more pressing problems and also because there were apparently no tools for accurate measurement of the quantities involved.

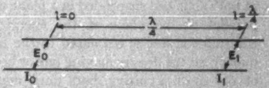
Since the advent of high-power high-frequency transmitters and of simultaneous operation of antennas on several frequencies, the issue of high-voltage radio-frequency phenomena has again come to the foreground. Voltages well over 50,000 root-mean-square volts have been developed in coupled sections[1] under test conditions and in the near future probably several times these values will be common. With the issue of insulation coming to the foreground, a simple and accurate method for high-voltage measurements seems to be a necessity. The basis of such a method has been with the electrical art ever since the development of accurate transmission line equations, the principle of the method lying in the relation between voltage and current separated by a quarter wave length of transmission line. The relation is simply that the voltage on the line at any point is equal to the surge impedance times the current at a point exactly one quarter wave length distant, either toward the receiving or transmitting end assuming no line losses. This relation holds regardless of the line terminations, the only requisite being that the surge impedance of the line over the quarter-wave region be constant.

The relation referred to was described by Steinmetz in 1908 in his "Transient Electric Phenomena and Oscillations", Steinmetz's derivation follows:

Given the general transmission line equations[2]

$$\boldsymbol{I}=A_1\epsilon^{+\alpha l}(\cos\beta l+j\sin\beta l)-A_2\epsilon^{-\alpha l}(\cos\beta l-j\sin\beta l)$$

$$\boldsymbol{E}=\sqrt{\frac{Z}{Y}}\,[A_1\epsilon^{+\alpha l}(\cos\beta l+j\sin\beta l)+A_2\epsilon^{-\alpha l}(\cos\beta l-j\sin\beta l)]$$



For simplification we can assume line losses to be zero and also let $\boldsymbol{E}_0$ be voltage at $l=0$, the reference point on the line. The $\sqrt{Z/Y}$ becomes $\sqrt{L/C}=z_0$ commonly known as the surge impedance. At the reference point

$$\boldsymbol{I}_0=(A_1-A_2)$$

$$\boldsymbol{E}_0=z_0(A_1+A_2)$$

At a point λ/4 distant, $\beta l=\pi/2$ at which point $\boldsymbol{I}_1$ and $\boldsymbol{E}_1$ shall represent the current and voltage

$$\boldsymbol{I}_1=-j(A_1+A_2)$$

$$\boldsymbol{E}_1=-jz_0(A_1-A_2)$$

From which it follows that

$$\boldsymbol{E}_0=-jz_0\boldsymbol{I}_1$$

which reduces to $E_0=z_0I_1$ when only absolute magnitudes are considered. This simple derivation turns the quarter-wave line into a radio-frequency voltmeter.

Since z_0 occurring in the above relation is usually of the order of 600 ohms, it follows that high voltages and high currents go together. Both may be produced very simply by means of the arrangement shown in figure 1, in which a quarter wave length of line is suspended below an energized transmission line, one end of the quarter-wave line being closed by an adjustable short-circuiting bar. If the section is very loosely coupled to the line, that is, hung a foot or two below the line, the current in the short circuit will be found to be very large since there is only a small amount of resistance present to

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[1] For all numbered references, see list at end of paper.

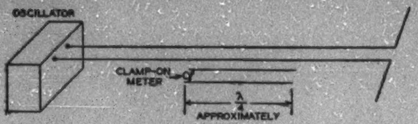


Figure 1. Quarter-wave coupled section on radio-frequency transmission line

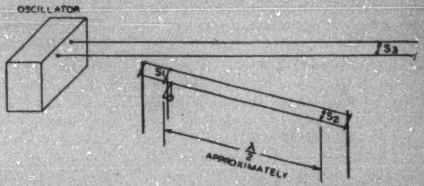
stop the flow. As pointed out above, the voltage at the open end of the section, being one quarter wave length away from the large current, will also be large and will be the absolute value of the product of the current by the surge impedance of the section.

Experience has shown that this particular arrangement led to trouble due to the difficulty of insulating the open end of the section. This was eliminated by a much more satisfactory arrangement, shown in figure 2*A*, in which a half-wave section of transmission line short-circuited at both ends and suspended in a plane not parallel to the plane of the energized transmission line is resonated to the frequency on the line by moving one or both of the short-circuiting bars until maximum current flows in the section. An arrangement of this sort may be provided with supports near or beyond the short circuit where the voltage is low, thus presenting no support insulation difficulties. Such a half-wave section may be made, for example, of number 6 copper wire with the upper short-circuiting bar fixed for current measurement and the lower one adjustable for tuning the section, being that the lower short circuit is easily accessible. The current in the fixed short circuit may be measured by a clamp-on meter or much more conveniently by an inductively coupled meter which will be referred to as a "shortometer" and is shown in figure 3. The calibration and theory of this instrument is described in appendix *A*, but it is to be noted here that such a meter reads root-mean-square values, hence all currents or voltages referred to in the following will be root mean square or effective in magnitude.

The power which is required for producing high voltages up to 60,000 volts is very moderate, being of the order of ten kilowatts. The loop resistance of the half-wave section at frequencies of the order of 13 megacycles is around one ohm with number 6 copper wire, so that if all of the line power is diverted into the section, the current developed in the short circuit is approximately equal to the square root of the power in watts, so when the section is dissipating 10,000 watts, the current in the short circuit would be about 100 amperes. With wires of the section spaced 12 inches apart, the surge impedance is 600 ohms, thus the voltage in the center of the half-wave section is about $600 \times 100 = 60,000$ volts, which, as a matter of fact, is somewhat higher than the arcing voltage of the clean number 6 wires spaced 12 inches apart. When higher voltages are desired, it is necessary to make the half-wave section of larger diameter wire or of tubing.

The best position of short circuit S_3 in figure 2*A* depends on the geometrical configuration of the transmission line and the half-wave section. When the short circuit S_1 of the section is about a foot below the line and the section is inclined at about 15 degrees, it is possible to find a position of short circuit S_3 which, when the half-wave section is nearly in tune, reduces the standing waves along the transmission line to reasonable proportions so that the transmitter is able to deliver power. The best position of this short circuit depends on how much additional resistance is introduced into the section by, say, a sample insulator under test placed across the section. Thus short circuit S_3 is made readily adjustable. This short circuit is then used as a kind of mutual-coupling control or adjustable transformer ratio.

Figure 2A. Half-wave coupled section to transmission line



High-Voltage Phenomena

The first phenomenon usually encountered with the half-wave section as the movable short circuit approaches the resonant point and the current increases to 25 or 30 amperes is that a flame somewhat similar to a Bunsen-burner flame and about six to eight inches long shoots into space from some point near the middle of the section. Such flames are usually directed upward and blow very easily in the wind to a point of low voltage where they go out. Immediately another flame spurts out from near the center of the section and repeats the process. If the section has considerable slant with respect to the ground, the flames will usually run up the wire even if there is no wind blowing and will go to the low-voltage point unless they strike a projection or sharp point where they will stop moving and will maintain themselves until the power is dropped or the section is detuned. If the wire of the section has a coating of corrosion from the weather, the voltage which the section can stand may be as low as 15,000 or 20,000 volts. This voltage can be doubled or raised even higher before an arc is produced if the wire is made clean and bright by sanding and wiping with a cloth. It is also interesting to note at this point that if the short circuits are taken off and a 60-cycle voltage is applied across the section, the region which has been cleaned shows no corona, while the parts remaining dirty glow with intense and spotty corona.

It is of particular importance to note the striking contrast that exists between 60-cycle phenomenon and 13,000-kilocycle phenomenon in regard to the mechanics of the voltage breakdown of the wires. At 60 cycles as the voltage is gradually increased, first a very faint corona glow develops. As the voltage rises and the corona becomes more intense, streamers shoot out from the wires and finally they become long enough to bridge the gap between the wires terminating in complete breakdown or arc-over. This process is seen to progress in a more or less continuous manner from no

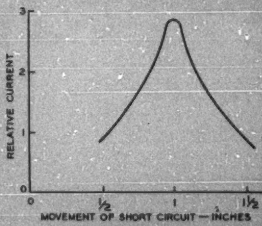


Figure 2B. Approximate half-wave-section tuning response when made of number 6 copper wires spaced 12 inches apart

indication of voltage through gradual steps to complete arc-over between wires. Such is not the case at 13,000 kilocycles. When the voltage on the section is gradually raised, there is no indication of increasing voltage stress preceding the arc into space. As soon as the voltage gets up to a certain critical value, an arc occurs not between wires, but suddenly shoots into space just as if the whole voltage had been instantly applied, there being no noise and no sign of corona accompanying the gradual rise in voltage.

The voltage that a given wire will take at 13,000 kilocycles under the same clean surface conditions varies considerably from day to day apparently depending on atmospheric conditions. Occasionally sections made of clean, smooth number 6 wires spaced 12 inches apart will stand as much as 45,000 volts under the best of atmospheric conditions. One of the strangest phenomena of all is the action of smoke from rubbish or grass fires. Wherever the smoke strikes a high-voltage region of the section, a jagged, crackling spark shoots out, and if the smoke distribution is uniform, the voltage rating of the section will sometimes drop to as low as 20 per cent of the normal rating depending on the concentration of smoke.

The electrostatic field near the midpoint of the half-wave section when a large current is flowing in the short circuits is quite extensive and produces some curious effects, for example, considerable diathermy effect can be felt in one's legs when standing at least four or five feet away. The magnetic field about the short circuit also produces some curious phenomena. If a piece of number 6 wire is fastened onto a wooden handle with iron nails bent around the wire to provide an adjustable short circuit, the nails get red hot while the current in the wire is sufficient to raise the temperature of the latter only a little above atmospheric.

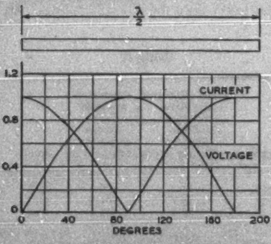


Figure 2C. Current and voltage distribution over a half-wave coupled section

High-Voltage Measurements

The voltage which occurs between the conductors of the half-wave section may be applied across what may be termed "samples under test" such as needle gaps, sphere gaps, insulators, capacitors, etc. These samples are fastened to sections at a distance of one-quarter wave length from the fixed short circuit in which the current is observed. Due to the capacity of the sample the movable short circuit has to be translated toward the fixed short circuit in order that the section may be resonated. The distance through which this short circuit is moved is a very accurate measure of the capacity of the sample and may be used for ascertaining the capacity of the latter. See appendix *B*.

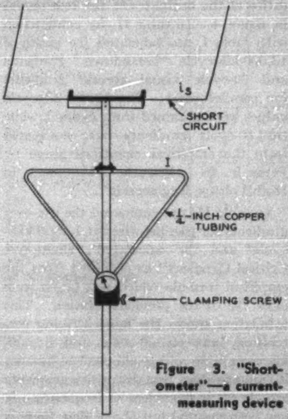


Figure 3. "Shortometer"—a current-measuring device

In order to visualize the more fundamental features of the high-frequency high-voltage phenomena involved, the following arrangements well known to the 60-cycle technique were investigated.

Needle Gaps

In the case of needle gaps the action at 13,000 kilocycles is quite different from that at 60 cycles. Figure 4 shows a curve of the very faintest sign of corona glow with points at 60 cycles in a dark room. On the same sheet are also plotted the voltages at which breakdown occurs at 13,000 kilocycles. In the latter case there is no sign of corona before brilliant white spots, or what may be more descriptively named points fire, suddenly develop at the sharp tips as soon as the gradually rising voltage reaches a certain critical value. Considerable energy is immediately dissipated at the points as indicated by the fact that tips are dulled each time a breakdown occurs and also by the fact that the section current drops immediately, showing that a loss or resistance has been introduced into the section. Due to the melting of the points, they can be used only once, after which they have to be sanded, and even then, with the most careful precautions the results are far from being exactly reproducible as shown by the dispersion of points.

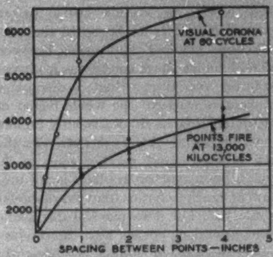


Figure 4. Behavior of needle gaps at 13,000 kilocycles and at 60 cycles

Wire Gaps or Parallel Wires

Figure 5 shows the breakdown voltages between wires of various sizes at close spacings in a configuration as shown at the top of the curve sheet. Here again no corona is seen and also no agreement is found between the shape of these curves representing arcing voltage at 13,000 kilocycles and the 60-cycle spark-over voltage as shown in figures 50, 51, and 93 in Peek Jr.'s "Dielectric Phenomena in High Voltage Engineering". However, it is of considerable interest to note that the shape of the 60-cycle corona curves in the same figures is somewhat similar to the curves of figure 5.

Sphere Gaps

Figure 6 shows a curve taken with two-inch spheres set at various spacings. Here for the first time the agreement between 60-cycle spark-over data and 13,000-kilocycle arc-over data is surprisingly good, showing that sphere gaps hold as standards for high-voltage measurements at least up to 13,000 kilocycles when the spacing between the spheres is considerably less than their diameter. As previously there is no sign of corona. In addition the results are surprisingly reproducible.

At 60 cycles, in the case of sphere gaps, when the spacing between the spheres is considerably less than their diameter, there is also no corona visible before spark-over. It is to be noted that the maximum gradient that exists between spheres under these conditions is almost uniform, and thus, when a disruptive gradient is reached, the whole region of maximum stress is simultaneously broken down producing the spark-over. In the case of points and parallel wires at 60 cycles, the maximum gradient is not uniform at spacings larger than the diameter of the wire but does reach a disruptive value producing corona near the conductors if the applied voltage is sufficiently high. At 13,000 kilocycles such an enormous loss apparently occurs as soon as a disruptive gradient is reached that the ionization is not partial causing corona as at 60 cycles, but is instead complete causing an arc.

This view is further borne out if we consider what Peek Jr. describes as "Disruptive Critical Voltage" and "Disruptive Critical Gradient" on page 188 of the previous citation. Here he shows that corona loss occurs below the voltage which produces visual corona, the voltage and gradient at which corona loss begins being named respectively, "Disruptive Critical Voltage" and "Disruptive Critical Gradient". If the gradients corresponding to voltage applied to the wires as shown in figure 5 are computed from the exact equation for maximum gradient and are plotted as shown in figure 7, a fairly good agreement is found to exist between the 13,000-kilocycle breakdown gradients and the 60-cycle "Disruptive Gradient" values calculated from Peek Jr.'s empirical formula for this gradient shown in table III. Thus again we see that apparently as soon as a voltage is reached which would produce a corona loss at 60 cycles, an arc is produced at 13 megacycles.

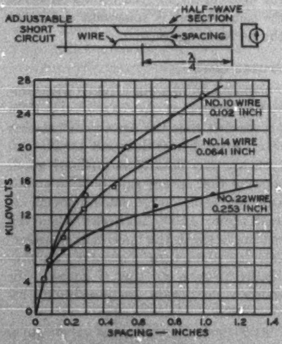


Figure 5. Breakdown tests on parallel wires at 13,000 kilocycles

The "Disruptive Critical Gradient" theory can also be used to explain the action of needle gaps at 13 megacycles. Apparently as soon as the magnitude of the 13-megacycle voltage is equal to the 60-cycle voltage which would cause the first bit of corona loss, the loss at 13 megacycles is so large that points fire is produced, from which it follows that the curve for points fire shown in figure 4 should be below the curve for visible corona at 60 cycles, and this is the case.

It is of interest to note the ratio between the 60-cycle voltage which causes the first signs of visible corona and the 13,000-kilocycle breakdown voltage for needle gaps. In table I are given the ratios of the ordinates of the two curves in figure 4. In table II, for comparison with table I, are tabulated the ratios of 13,000-kilocycle breakdown gradients and 60-cycle visual corona gradients for parallel wires. The 13,000-kilocycle values were obtained from figure 7 while the 60-cycle gradients were computed from the empirical equations given by Peek Jr. in "Dielectric Phenomena in High Voltage Engineering".

In table III are compared the arc-over gradients for parallel wires at 13,000 kilocycles and the calculated "Disruptive Critical Gradient" as obtained from the empirical formula which was taken from page 192 of the previous citation. This tabulation shows the most striking correlation between 60-cycle and 13,000-kilocycle high-voltage phenomena because it seems to show that disruptive gradients are almost independent of frequency. It has been pointed out above that there is apparently no mild dielectric breakdown at 13,000 kilocycles preceding arc-over. Furthermore, since losses are probably proportional to the second power of the frequency, it seems likely that corona cannot exist at such frequencies as 13,000 kilocycles but that complete breakdown of the region must take place as soon as the disruptive gradient is reached.

VARIABLE AIR-CAPACITOR VOLTAGE RATINGS

In the light of the fundamental facts presented above, it is now possible to begin to understand the high-voltage high-frequency performance of many of the various pieces of equipment presently in use in modern high-powered radio transmitters, and further, since it is possible to give a reasonably correct diagnosis of the faults of the apparatus, it follows that a basis for a more correct design has also been established.

Table I

Spacing Between Points (Inches)	13,000-Kilocycle Points-Fire Voltages Taken From Figure 4 (Kilovolts)	60-Cycle Corona Voltages From Figure 4 (Kilovolts)	Ratio
0.5	2.1	3.7	0.57
1.0	2.8	5.3	0.53
4.0	4.0	6.4	0.62

Variable air capacitors in high-power transmitters are one of the pieces of equipment which are subjected to rather high radio-frequency voltages, seldom much over 6,000 volts, but nevertheless sufficiently high to produce annoying and troublesome arc-overs at frequent intervals. Whenever such difficulties developed, the usual practice seems to have been to analyze the trouble in the light of 60-cycle phenomena. For example, it seems to be quite common for manufacturers to list the voltage ratings of variable air capacitors as straight-line functions of the plate spacings both at 60 cycles and at high radio frequencies. The radio-frequency ratings given are somewhat lower than the 60-cycle ratings. In

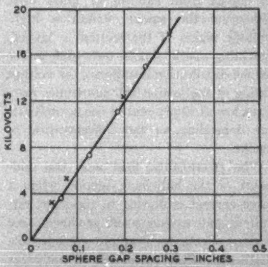


Figure 6. Comparison of sphere-gap performance at 60 cycles and at radio frequencies

Circles—13,000 kilocycles, 2-inch spheres

Crosses—60 cycles, $2^{5}/_{8}$-inch spheres

Table II

Wire Size	13,000-Kilocycle Arc-Over Gradient From Figure 7 (Kv per Cm)	Calculated 60-Cycle Visual Corona Gradient g_v (Kv per Cm)	Ratio
22	51.3	80.0	0.64
14	38.7	61.0	0.63
10	35.3	54.7	0.65

$g_v = 29.8\left[1+\frac{0.30}{\sqrt{\frac{D}{2}}}\right]$ from Peek Jr., page 56.

addition, plate thickness is given some consideration, statements usually being made to the effect that thin plates reduce the maximum voltage ratings by a small amount. It is also sometimes mentioned that the plate shape at the edges has a somewhat marked effect on the rating, square edges tending to produce a lower spark-over voltage.

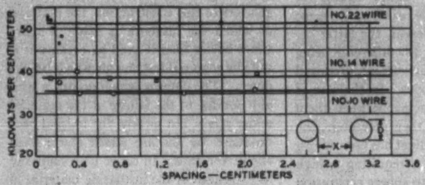


Figure 7 (above). Spark-over gradients for wires at 13,000 kilocycles

Maximum gradient

$g = \frac{e}{x} W$ where

$$W = \frac{\frac{x}{D}\sqrt{1+\frac{2D}{x}}}{\cosh^{-1}\left(\frac{x}{D}+1\right)}$$

and e = applied voltage

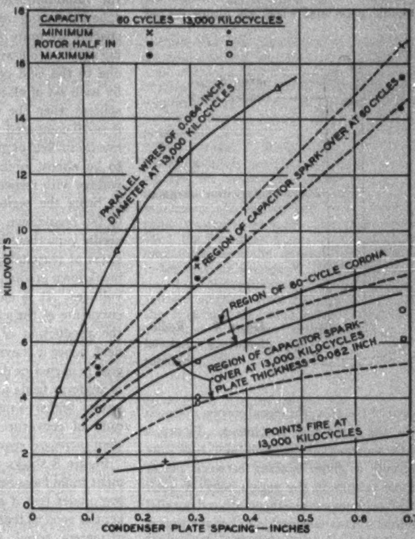


Figure 8. Spark-over voltages of variable air capacitors at 60 cycles and at 13,000 kilocycles

The 60-cycle ratings are usually correct. Even though the corona which develops during tests is not a straight-line function of voltage, the spark-over voltages vary almost linearly with spacing practically regardless of plate thickness or edge shape. The above data have shown that sphere-gap spark-over voltages agree almost to the point of coincidence, regardless of frequency up to 13 megacycles as shown in figure 6. Figure 4, the curve for 60-cycle corona glow and 13-megacycle points fire, tells an entirely different story. Herein lies the crux to the situation. The 13-megacycle points-fire curve is not at all linear with voltage, but is quite similar to the 60-cycle visible-corona curve for points, and, in addition, as table I shows, is only about 60 per cent of the corona-glow voltage for points.

Naturally, during the life of most capacitors, even though some attempt has been made in the better class of equipment to round off plate edges at least to a small degree, arc-overs occur, leaving small rough spots similar to points. This being the case, a capacitor in service is comprised of smooth plates with varying numbers of little rough spots due to arc-overs on the plate edges. Under these conditions, it is only reasonable to expect that the high radio-frequency voltage rating of such a piece of equipment might be similar to the voltage rating of needle gaps at the same spacing and at the same frequency. When the capacitors are new and reasonably smooth surfaces prevail throughout, it is possible that the voltage rating might follow along the lines of parallel wires which have diameters equal to plate thicknesses since the present common designs simulate a mesh of parallel wires at the plate edges, especially when the plates are in the position of maximum capacity.

Table III

Wire Size	13,000-Kilocycle Arc-Over Gradients From Figure 7 (Kv per Cm)	Calculated 60-Cycle "Disruptive Critical Gradient" g_d (Kv per Cm)	Ratio
22	51.3	70.7	1.38
14	38.7	41.0	1.06
10	35.8	34.8	0.99

$$g_d = 29.8\left[1+\frac{0.30}{\sqrt{\frac{D}{2}}}\frac{1}{\left(1+230\left(\frac{D}{2}\right)^2\right)}\right]$$ from Peek Jr., page 192.

Figure 8 gives a rather complete picture of the performance of a number of commercial capacitors after they had been in service for a considerable period of time. Here it is seen that the 60-cycle rating is linear with spacing as claimed by the manufacturers and that voltages well over what might be expected in the highest-powered transmitters can be applied across the capacitor terminals if the frequency of the voltage is in the 60-cycle region. The 13-megacycle high-voltage tests show an entirely different relation between spark-over voltage and plate spacing, the spark-over voltages being much lower than the 60-cycle spark-over ratings, thus explaining the unsatisfactory operation of many supposedly well-designed capacitors.

As is well-known to the 60-cycle technique and as has been pointed out above for 13 megacycles, needle gaps are rather unsatisfactory for voltage measurement compared with sphere gaps since the spark-over voltages are not exactly reproducible, but are more or less dependent on the laws of probability. Since it was pointed out that the capacitors under test were spotted with arc-over marks, it follows that their voltage ratings both at 60 cycles and 13 megacycles should depend to a certain extent on probability, as do needle gaps, hence an exact rating cannot be given, the wide dispersion of points defining regions of limits within which spark-overs will probably occur at various spacings. It is further to be noted on figure 8 that the capacitors tested apparently presented shapes more similar to points than to parallel wires since it is seen that the arc-over voltages for parallel wires having diameters about equal to the plate thicknesses of the capacitors tested were considerably higher.

At present, and at least until further investigation proves otherwise, it seems possible that the high radio-frequency voltage ratings of a variable air capacitor might be obtained from 60-cycle tests.

Previously the 60-cycle spark-over ratings have apparently proved to be much in error at high radio frequencies so an entirely different method will have to be used if the same errors are to be avoided. Tables I and II show that the points fire voltages and the arc-over voltages for needle gaps and wires are about 60 per cent of the 60-cycle visual corona voltages for the same test specimens. Figure 8 has shown that variable air capacitors having a given spacing between plates have ratings in the region between such test specimens as needle points and parallel wires, so it is reasonable to believe that a similar fraction of the 60-cycle corona voltage might be taken as the maximum radio-frequency rating of a capacitor. Figure 8 also shows a region for the first signs of 60-cycle visual corona on the group of capacitors used in the tests. However, probably due to irreproducibility of such things as corona glow and needle gap spark-over voltages, the 13-megacycle spark-over voltages are seen to be a little more than 60 per cent of the corona-glow voltages. Where corona first appears on the plates at 60 cycles is not necessarily the point where the 13-megacycle spark-over will occur; nevertheless, the points of 60-cycle and 13-megacycle spark-overs usually coincide, thus substantiating at least a part of the older test methods.

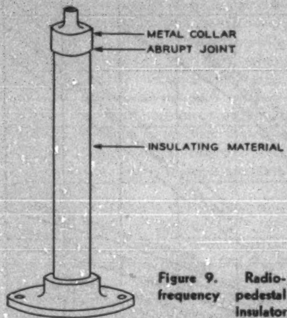


Figure 9. Radio-frequency pedestal insulator

The insulating material used to separate one set of plates from the other in a variable air capacitor provides one point of difference between an exact similarity of capacitors to needle gaps and parallel wires, and therein lies a possible source of error to be carefully avoided. At 60 cycles corona usually develops at the point of contact of the metal plates with this insulating material, the voltage at which this occurs usually having little bearing on the radio-frequency voltage rating. The corona to be observed to obtain the radio-frequency voltage rating is that from the edges of the plates into air.

In the above discussion and presentation of curves it has been pointed out that the radio-frequency voltage rating for sphere gaps and needle points at the same spacing are quite different, the latter being very much lower; the difference being due to the field configurations produced by each set of electrodes. In the case of spheres when the spacing between them is small compared to their diameters, the maximum field or gradient has been shown to be almost uniform, requiring a high voltage with respect to sphere separation to bring the region of maximum stress to the breakdown value. In the case of needle gaps there is no region of uniform stress at reasonable spacings, hence high breakdown gradients are produced at low voltages. It is clear that the radius of curvature of the sphere has all to do with the production of the desired uniform field, hence it is evident that the ultimate goal in capacitor design is the production of uniform fields at regions of maximum stress, which simply means maximum radii of curvature of plate edges rather than increased spacings.

Figure 8 shows that a large improvement could have been made in the capacitors under test if all gradients had been kept as good as that between wires having diameters equal to plate thicknesses. However, it is requisite to use radii of curvatures on the plate edges which are at least one order of magnitude higher than the dimensions of the spots produced by arc-overs, with the hope that the comparatively gradually curving surfaces will partially shield the rough spots produced. The reproducibility of sphere-gap spark-over voltages after repeated spark-overs has proved the validity of such a shielding effect. Not only do the plate edges need larger radii of curvature to reduce high gradients, but also all parts of the apparatus including such things as frame work which are at a high potential with respect to ground or with respect to any object in the near vicinity, if the ultimate in performance is to be achieved.

INSULATOR TESTS

It seems to be common practice at present when manufacturing standoff and strain insulators, to make an abrupt joint between the metal collars and the insulating material as shown in figure 9. If 60-cycle voltages were applied across such insulators, the spark-over voltages would increase almost in proportion to the length of the insulating material. At high radio frequency this is not at all the case, and, in fact, the length has only a small bearing on the matter, the actual rating of the insulator being dependent on such things as the radius of curvature of the cap at the place where it meets the insulating material. As a rule, with insulators of this construction, the material of which the insulator is made is many times better than it need be because it is not the material but the air which breaks down. The damage which occurs is usually not due to the failure of the material under stress, but merely due to local heat produced by the arc which starts from one of the caps. In order to develop the full strength of the insulating material in practice as well as in test, it is necessary to provide the insulator with some sort of corona shields very familiar to the 60-cycle practice, but at high radio frequencies these corona shields have to be considerably better than they need be at 60 cycles. What is necessary at these frequencies is a corona shield which at 60 cycles would produce spark-over before corona, that is, an almost uniform gradient. Anything short of that results in lower radio-frequency spark-over than at 60 cycles. This point is difficult to overemphasize. If for the sake of illustration we assume an insulator has been provided with shields and that during a 60-cycle test there developed corona at a sharp point along the wires leading to the insulator, perhaps three feet away from opposite terminals, it is safe to predict that at high radio frequencies a breakdown will take place at the previous corona point, and this may happen in spite of the fact that the spacing between the corona shields may be only one-half inch. In order to emphasize this point still further, it might be well to point out the effect of a sharp point in some apparatus of a common type. Quite frequently rectangular strain insulators similar to the one shown in figure 10 are used as part of antenna equipment. These insulators have shackles and pins as fastening members, the pins being provided with cotter pins to insure the permanency of the arrangement. When used in the normal way, the voltage rating at 13 megacycles is only about 18,000 volts because the sharp points of the cotter pins produce arcs into space. If the cotter pins are removed the voltage can be in-

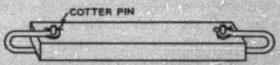


Figure 10. Radio-frequency strain insulator

creased to over 40,000 volts and still nothing happens.

In testing insulators it is well to keep in mind that so-called insulator failures occur for two entirely different reasons; (1) breakdown of the air around the metal fittings where the metal comes in contact with the insulating material; (2) breakdown of the insulating material itself. Most of the insulators in use at the present time fail for the first reason. Only a few equipped with carefully designed corona shields or means for distributing the gradient equally over the length of the insulator breakdown for the second reason. The only exception to the latter case consists of insulators of the standoff type which are provided with metal screws that go a long way into the insulating material, thus producing an enormous concentration of flux in the material.

Insulating materials such as synthetic resins prove to be unsatisfactory at high-radio-frequency high voltages. When 1,000 or 2,000 volts at 13 megacycles is applied across a strip of such material about two inches long, charring streamers start over the surface of the specimen, accompanied by considerable smoke, and as soon as the streamers from opposite sides meet, a short circuit is produced and the test is over.

Conclusion

The results of the above measurements seem to indicate rather definitely that there exists a striking correlation between high-voltage phenomena at high radio frequencies and at 60 cycles. The 60-cycle phenomenon which parallels the high-frequency breakdown most closely seems to be the 60-cycle corona. The gradients at which high-frequency breakdown occurs appear to follow the same laws which are followed by the 60-cycle corona. The values of the gradients at which high-frequency breakdowns take place appear to be systematically equal to about 60 per cent of the gradients at which the 60-cycle visual corona begins and, moreover, appear to be equal at least approximately, to the 60-cycle so-called "Disruptive Gradient", that is, those gradients at which corona losses begin to occur at 60 cycles. Our measurements are too meager to establish the extent and the degree of this correspondence between the 60-cycle disruptive gradient and the high-frequency breakdown gradients. These measurements, however, appear to be sufficiently consistent and appear to cover a sufficient variety of electrode configurations to make it seem very probable that such a one-to-one correspondence does exist. If this is the case, and we believe it is, it is not unreasonable to suppose that in the not too distant future, it will be possible to derive useful 60-cycle information from high-frequency measurements. It is only a matter of minutes to determine the breakdown voltage, and hence, the breakdown gradients between two wires or cables at a high radio frequency. If this breakdown gradient so determined is also the disruptive gradient at 60 cycles and will vary with temperature, humidity, smoke, dust, and other conditions in exactly the same way that the 60-cycle disruptive gradient does, then it would be possible with a relatively simple high-frequency setup to find out what a proposed 60-cycle arrangement will do under given conditions. If the 60-cycle disruptive gradient is really equal to the high-frequency breakdown gradient at all, it is difficult to see why the two gradients would behave differently under varied conditions.

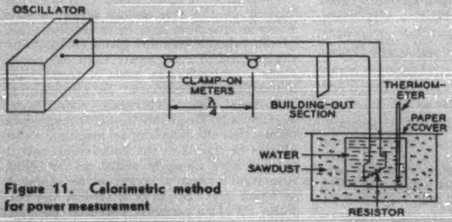


Figure 11. Calorimetric method for power measurement

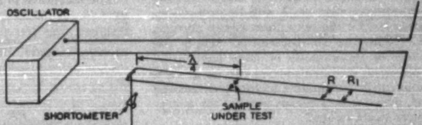


Figure 12. Test setup

In conclusion it should be added that our experiments were made with frequencies all lying in a relatively narrow band around 13,000 kilocycles. Somewhere below this band there lies a transition region in which corona ceases to exist. Our experiments were made primarily for the purpose of answering certain concrete questions in regard to a particular insulation problem and proper facilities were not readily available for extending this investigation into other frequency regions to make it more inclusive.

Appendix A

Upon the first impression it might be thought that the "shortometer", the much-used current-measuring instrument in all of these tests, would not be independent of frequency, but such is not the case as has been found by experience and may be easily proved as follows:

The current I, in the shortometer, as shown in figure 3, is dependent upon the voltage, E, induced in the loop of the instrument and inversely proportional to the reactance, ωL, of the loop. At the high radio frequencies at which the instrument is used, the impedance is of a higher order of magnitude than the resistance, hence the latter may be neglected. The voltage induced in the instrument loop is equal to the product of the mutual impedance between the loop and the short circuit, times the current, i_s, in the short circuit.

$$I = \frac{\omega M i_s}{\omega L} = \frac{M i_s}{L}$$

in which ω is equal to $2\pi f$ and M is the mutual inductance between the shortometer and the short circuit. Thus, it would seem that the shortometer current is independent of frequency so long as the above quantities predominate.

The accuracy of this whole method is seen to depend entirely upon how accurately the currents involved can be measured. At such high frequencies there is always considerable doubt as to the accuracy of the ammeters used, and, in fact, many of the instrument companies furnish a correction chart for their instruments when used at frequencies above one megacycle. In view of this fact, a calorimetric method was used to standardize two clamp-on meters as shown in figure 11.

The theory of the standardization is as follows:

The line from the transmitter to the dissipating resistor immersed in the water was very carefully balanced so that all of the energy on the line was series current which simply means that the ammeters would read the same regardless of on which side of the line they were placed. This eliminated any possibility of using the transmission line as a single conductor and the resistor as a capacitive by-pass to the line image in the ground. The resistor did not match the surge impedance of the line, hence, a building-out section was installed in order to make a good balance on the line at the transmitter possible.

The conversion of electrical energy into heat at a known rate in the calorimeter is an accurate and fundamental measure of the power being transmitted by the line. The product of the readings of the clamp-on meters, spaced one-quarter wave apart, when multiplied by the surge impedance of the line is a measure of power also, and this quantity equated to the power delivered to the calorimeter gives a standardization of the meters.

In this particular case the calorimeter and its contents had a heat capacity of 12,700 calories per degree centigrade as measured by weighing the various constituents which absorbed heat and also taking into account their specific heats. The temperature rise as noted over a ten minute period by a very accurate thermometer was 19 degrees centigrade or 1.9 degrees centigrade in one minute. This gave a value of $1.9\times 12{,}700/60=403$ calories per second which is equal to 1,690 watts when multiplied by 4.185, the conversion factor for heat into work. The line ammeters read 0.77 and 1.00, hence $600\times 0.77K\times 1.00K = 1{,}690$, $K = 1.915$, where K is the multiplying factor for the meter in order to convert its readings into amperes.

When the standardized ammeter and the shortometer are put on a short circuit at the same time and current is caused to flow in the section, the reading of the shortometer may be adjusted to any desired value by simply moving the instrument up or down the staff after loosening the clamping screw. In this way positions of the instrument on the staff may be marked so that the dial of the shortometer will read directly in amperes as shown by the clamp-on meter, taking its correction factor, K, into consideration. From the above shortometer equation it is seen that if the mutual inductance, M, between the shortometer coil and the short circuit is decreased, the shortometer current, I, will be proportionately less for the same short-circuit current, i_s. Being that short-circuit currents of the order of 100 amperes or more have to be measured, the shortometer can be given a convenient multiplying factor by decreasing M, which simply means moving the instrument down its staff.

When dealing with currents too large to be measured by the clamp-on meters at hand, the shortometer may be further calibrated against itself. For instance, the section current may be adjusted to near full-scale reading for the shortometer by adjusting the transmitter output. At this time the shortometer may be moved down its staff until the original reading is cut in half or any other desired value, thus giving a multiplying factor of two or the inverse of whatever the reading was reduced to. In order to make sure that the shortometer reading changed due only to the movement of the instrument on its staff, a meter may be clamped on the transmission line which usually carries a very small current compared to the section short circuit. By means of this meter and a remote control on the transmitter, the power on the transmission line may be kept constant during the shortometer calibration.

The shortometer may be similarly calibrated to read line currents by comparing it with a standardized ammeter clamped onto the line. Being that the field configuration in this latter case is different from that around a short circuit, the calibrations for measuring short-circuit current will be different from those for line currents. Also, since field configuration and dimensions determine the mutual inductance, M, between the shortometer coil and the circuit in question, the shortometer must always be used in the position in which it was calibrated and in conjunction with a short circuit of the same length as the calibrating short circuit. In other words, the shortometer staff must be provided with a calibration for each field configuration in which the instrument is going to be used.

Appendix B

When any piece of equipment is put across the mid-point of a half-wave section, the capacity which it introduces at that point detunes the section so that it is no longer resonant to the frequency on the transmission line. The section can be again made resonant by moving the adjustable short circuit from the point of original and fundamental section resonant point such as R' in figure 12 to a new location, R. From the length of line RR' the exact capacity of the test specimen can be obtained in the following way: It can be assumed that the capacitive reactance of the test specimen was equal to the capacitive reactance of a short section of open line at the center of the coupled section. Instead of cutting this equivalent length out of the center of the half-wave section in order to decrease the capacity to the original value and thus restore resonance, it is much more convenient to move the adjustable short circuit to the new position, R, the distance RR' being equal to the length of section which had to be cut from the center point.

The reactance of an open section is equal to

$$z=-jz_0\cot 360^\circ\frac{RR'}{\lambda}$$

or

$$\frac{1}{\omega C}=z_0\cot 360^\circ\frac{RR'}{\lambda}$$

when only absolute magnitudes are considered and which further reduces to

$$C=\frac{1}{z_0 2\pi f\cot 360^\circ\frac{RR'}{\lambda}}$$

Appendix C

Additional Precautions

During high-voltage measurements it is very important to make certain that the radio frequency is not modulated. This is particularly true when the power of the transmitter is varied by remote control and when most of the power tubes are operated below the point of saturation. In our investigations we found it very convenient to connect the vertical plates of a cathode-ray oscillograph across a few inches of the movable short circuit of the half-wave section, while the horizontal plates were connected, as usual, to the sweep circuit. This, or an equivalent arrangement, should always be employed during such measurements to avoid possible errors which may, under some conditions, amount to some 40 per cent.

Harmonics

When a half-wave section is used in making the high-voltage measurements, the second harmonic may contribute to the current but not to the voltage. The third harmonic and other odd harmonics may contribute to both. If harmonics are suspected it is quite easy to make certain whether they are actually present. This may be done by moving the adjustable short circuit to the half-wave positions for the various harmonics and by measuring the current in the short circuit with a sensitive instrument.

In our measurements the harmonic voltages were negligible, but it is quite likely that under some conditions these voltages may affect the results and for this reason this simple check for harmonics is strongly recommended.

References

1. Coupled Networks, Andrew Alford. IRE *Proceedings*, 1940.

2. Transient Electric Phenomena and Oscillations (a book), Steinmetz. Page 287, equation 17.

Discussion

E. A. Leach (nonmember; General Electric Company, West Lynn, Mass.): A brief discussion of the conclusions given in this paper is presented here to emphasize some of the valuable pointers given on insulator design, and to corroborate from independently taken measures, some of those characteristics of breakdown, on air-dielectric capacitors. Our experience with high-voltage radio-frequency insulator design has shown that the failure of an insulator could always be traced to the point where metal fittings were abruptly ended on the solid insulation. Such a condition as outlined in figure 9 of the paper is typical and the piling up of an excessive potential gradient adjacent to the juncture of metal cap and insulator will create excessive heating of the air and solid insulation at that point. By the addition of a suitable corona shield on the cap end and better yet on both ends, the safe operating voltage of a particular insulator may be improved several hundred per cent, and heating practically eliminated.

Another point that is often overlooked in insulator design is that the air film between metallic parts and a solid insulation is a bad offender. The air film, which is often only a few thousandths of an inch in thickness, may show corona breakdown at very low voltages, indicating that the gradient distribution is very poor at that point. An easy and effective way to minimize trouble from air film breakdown is to fill the air film space with a Glyptal or other insulating varnish. By so doing, you have automatically improved gradient distribution by the simple expedient of substituting a material with a dielectric constant of 3 or 4 for the air with its K of 1. Improvement of as much as 100 per cent may result from this simple device.

The correlation between 60-cycle phenomena and phenomena at moderately high radio frequencies has been used as a basis of design work here for some time. The analysis and test data given in connection with figure 8 check comparable data accumulated here using an entirely different method of voltage measurement. On small units such as the Cardwell "midway" size and Hammarlund "*MTC*" size and on which much of our test data have been taken, we have found that the 60-cycle corona voltage point and the 60-cycle flashover point come very close together. Furthermore, we have found it satisfactory on these small units to translate our radio-frequency requirements into terms of 60-cycle flashover voltages for purposes of inspection and manufacturing test work. The advantage, of course, lies in the fact that the flashover point can be more easily checked than can the corona-formation point. On large units the spread between the 60-cycle corona point and the 60-cycle flashover point is large and on these the corona point is the determining factor from which the radio-frequency capabilities are figured.

In small units we have found that rounded plates in variable capacitors do very little good at voltages lower than 1,500 peak. Above this voltage, the rounding of plates becomes more and more important and at the same time spacing and plate thickness must be increased. For any given plate thickness there is a reasonable limit of plate spacing beyond which there is little improvement of voltage breakdown characteristics. Each type of capacitor construction seems to present slightly different breakdown characteristics; hence no one set of data will answer exactly as a design standard for all methods of construction.

Andrew Alford and **Sidney Pickles:** We should like to acknowedge our appreciation of E. A. Leach's discussion of our paper.

It may be of interest at this time while discussing insulators to mention very briefly some observations which we made recently at still higher frequencies, namely at 110 and 94 megacycles. At these ultrahigh frequencies such materials as the best grades of glass and ceramics are no longer excellent insulators in that there is a much greater tendency for the insulating material to heat. For this reason metal fittings play a somewhat lesser part. At these frequencies only such excellent insulating materials as quartz and paraffin behave approximately like the best ceramics at 13 megacycles. The trend toward failures on account of heating already becomes noticeable at 20 megacycles.

The breakdown of air itself seems to occur at approximately the same voltages and behaves apparently according to the same laws. Our quantitative data, however, are not as yet sufficiently complete to be presented at this time.

A New Time Standard

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RESEARCH by many individuals since the beginning of the century has added two new instruments for time measurement to the generally accepted pendulum clock. All of these are based, of course, on the primary astronomical standard which involves the revolution of the earth on its axis.

The oldest of these secondary time standards, the pendulum clock, has been perfected to such an extent that in the form of the so-called Shortt free pendulum, any error can be discovered only with great difficulty. Consequently, this device has come to be over the past decade the accepted standard for certain important purposes. The time signals which are sent out for use by navigators and scientists are generally derived from free pendulum clocks maintained with utmost care in government laboratories. The time-keeping precision of these clocks is said to be of the order of one part in 30,000,000 or about a second a year.

The next most precise standard, namely, the vibrating quartz crystal which is universally used for the control of radio frequency and also for certain kinds of time service, has a precision which is said to exceed one part in 5,000,000 over a considerable period of time. However, this type of standard, if used for the measurement of time as distinguished from radio frequency, is very expensive and complicated and requires expert supervision. As many as ten or more vacuum tubes are needed to reduce the frequency of the quartz crystal to the standard 50 or 60 cycles of our power systems. Obviously, continuity of service with such a large number of vacuum tubes, each having a limited life, might be somewhat troublesome.

The third time standard in fairly common use has a very special form of tuning fork preferably maintained in vibration by vacuum tubes. Standards of this kind are said to operate with a precision of one part in a million, which would correspond with an error less than one-tenth of a second per day.

The effect of variations in temperature on these time standards may be detrimental and therefore it is customary to maintain them at a very constant temperature. Consequently compensating means are usually provided for the purpose of minimizing temperature effect.

Several years ago the writer, who has been concerned with problems of speed and frequency regulation that are of course based on time measurements, determined to build if possible a more simple form of secondary time standard which would be free from some of the objectionable features of the very accurate devices commonly used. Utilizing the vibrations of a stretched string or wire seemed to offer attractive possibilities. Every one knows that a piano or violin string gives out a fairly constant musical note when set in vibration. It is evident, however, that the note depends on the tension of the wire and that it would be very difficult indeed to provide absolutely constant tension through the usual form of mounting. The very fact that frequent tuning is necessary makes this clear.

The force of gravity as a means of providing tension appeared desirable. Experiments at first took the form of a vertical wire stretched over two bridges and tensioned by a weight. If the distance between the two bridges could be held exact it appeared as though such a wire should have a very constant rate of vibration. The accuracy of this arrangement was only fairly satisfactory for several reasons. These were irregular pressure on the wire over the bridges, absorption of a portion of the vibrating energy by the structure which held the bridges, a tendency of the wire to vibrate in different modes, etc.

Gradually the device evolved into the form of a vertical wire or strip supported rigidly at its upper end and tensioned by a weight at its lower end. This was set into vibration by a force supplied at its central point so that when vibrating it consisted of a single loop with nodes at both ends. Many methods of imparting vibrations were tried but eventually a very simple vacuum-tube coupling was adopted which supplied energy through the medium of a permanent bar magnet

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The author gratefully acknowledges close co-operation during a long period of research by his associates of the Warren Telechron Company, especially Charles A. Ellis.

that also constituted loading at the middle point of the stretched wire.

In order to compel vibration in a definite plane the vibrating element at first consisted of a narrow strip of metal which was stiff edgewise, but later it was found that a round wire would vibrate in a plane provided the driving magnet was accurately centered.

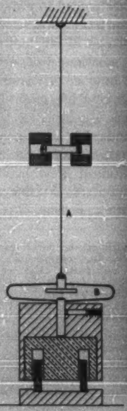
There are numerous difficulties in a device of this kind. In the first place, more than one mode of vibration may be established, for the wire can obviously vibrate in sections and it can also vibrate axially. Very soon it was discovered that if there were a fairly close correspondence between the normal rate of axial vibration of the system as a whole, including the weight, and one or more harmonics of the transverse vibration, the performance was wholly unsatisfactory. This meant that it was necessary to avoid certain proportions which involved frequency, length, and loading of the wire. Then it was found that a plain stretched wire or strip mounted in the manner described gave a frequency which increased rather rapidly with the amplitude of vibration so that in order to maintain a definite rate it would be necessary to maintain a very constant amplitude.

The effect of temperature on the vibrating wire arranged in this manner is rather complicated. Variations in length due to this cause need not be troublesome because the wire can be made of a material like Invar which has a nearly constant length over a wide range in temperature, but the effect of temperature on the modulus of elasticity of the vibrating materials is an important factor. A vertical wire tensioned by a weight has elastic stiffness in a transverse direction, that is to say, it behaves somewhat like a beam fixed at both ends vibrating under a load at its middle point. The transverse elastic stiffness has the effect of increasing the frequency of vibration beyond that caused by the tensioning weight. The elastic stiffness at a given temperature varies very rapidly in inverse proportion to the fourth power of the diameter of the wire and in direct proportion to the cube of its length.

Common materials which might be used for this purpose like steel, brass, tungsten, beryllium alloys, etc., become less stiff, that is to say, their modulus of elasticity decreases with increasing temperature. Consequently, vibrating wires made from these materials will have decreasing frequency as the temperature rises. Fortunately there are certain materials, especially nickel-steel alloys having composition very similar to Invar, that grow more stiff with increasing temperature. Vibrating wires made from this kind of material will show an increase in frequency as the temperature rises. The temperature coefficient of the elastic modulus of Invar is especially high, being in the neighborhood of 500 parts per million per degree centigrade. The temperature coefficient of the modulus of elasticity in the opposite direction of some of the other materials mentioned is much less, although still many times greater than the temperature coefficient of the length expansion.

It is possible to control the magnitude of the effect which temperature will have on the frequency for a given kind of material by planning the proportions of length, diameter, and loading of the vibrating wire. The smaller the diameter and the greater the length the less will be the effect of temperature changes which influence the elasticity of the material. For convenient lengths, however, and with reasonable loading within the elastic limit of the material used, there will usually remain frequency variations when there are temperature changes. However, these variations may be made as small as desired if advantage is taken of the opposite sign of the temperature coefficient of the elastic modulus by making the vibrating wire in part of a substance like Invar, having a temperature coefficient which gives greater stiffness with rising temperature and in part of another material like beryllium copper which becomes

Figure 1 (below). Diagrammatic representation of vibrator assembly in section



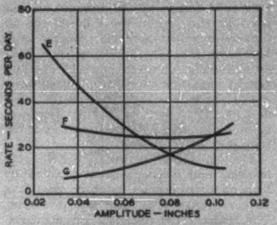


Figure 2. Rate compensation for changes in amplitude by means of a resilient bow

E—Overcompensated—bow length 2.82 inches

F—Full compensated effect—bow length 2.50 inches

G—Undercompensated—bow length 2.27 inches

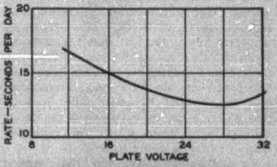
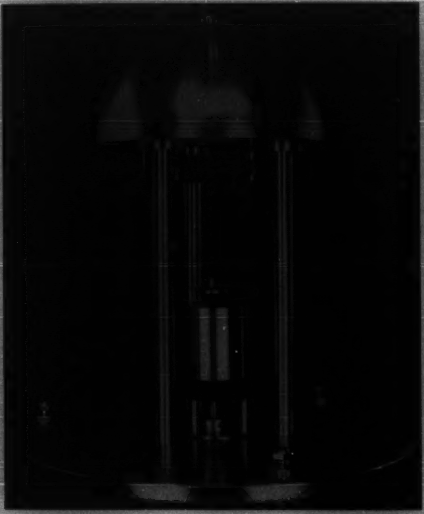


Figure 3. Effect of plate voltage upon time rate

Figure 4. Complete vibrator unit

Diameter of base 14 inches



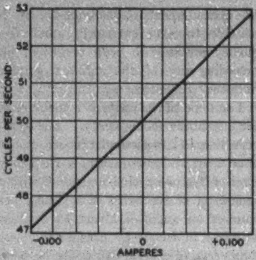


Figure 5. Relation between current in regulating coil and frequency

less stiff with rising temperature. If the relative lengths of these two materials are properly chosen the temperature effect of one may be made to cancel the temperature effect of the other as regards stiffness and if there is a slight residual of length expansion that may also be neutralized by a preponderance of one kind of elastic alteration.

There are alloys like elinvar which have a nearly constant modulus of elasticity with regard to temperature but in the experiments which have been made with this kind of material the reduction of temperature effects has been less satisfactory than by the very simple method of combining materials which have opposite signs of elastic temperature coefficients.

Further means of eliminating any slight residual temperature effect may act by opposing or adding to the gravity force of the weight by an adjustable element such as a weak spring or a thermosensitive magnet which will aid or oppose the tensioning force as the temperature changes.

Variations in rate caused by changes in amplitude can be corrected in a very simple manner. The normal increase in rate with increase in amplitude results because the vibrating wire is comparatively rigid axially so that it forms a connection having a nearly fixed length between the weight and the upper support. The weight has very considerable inertia and it tends to remain at rest in a position where the average force which it exerts on the vibrating wire is constant but the instantaneous force varies. The relationship is obviously slightly different for each amount of amplitude.

The instantaneous effect of the force exerted by the weight results in a component at the center of the vibrating wire tending to restore it to its mid-position and this component of gravity force is proportional to the tangent of the angle of deflection of the vibrating wire. If the component of force which is designated as restoring force were exactly proportional to the displacement of the wire, there would be harmonic motion and isochronism. Then there might be no change in rate with change in amplitude.

However, because of the inertia of the weight and slight axial elasticity of the wire, the instantaneous component of force varies in a different manner, being larger than the tangent of the angle for large angles of deviation and less than the tangent for very small angles. For this reason the frequency rate increases with the amplitude of vibration. In the case of an ordinary clock pendulum the reverse is true. There the rate decreases with increase of amplitude.

It has been found that if an elastic connection be interposed between the weight and the lower end of the vibrating wire, the component of gravity force which tends to restore the wire to its mid-position more nearly approximates proportionality to the tangent of the deflection angle, and if this elastic coupling between the weight and the wire is sufficiently soft the wire will actually vibrate with a slower rate as the amplitude increases.

These facts have resulted in the design of a vibrating element which consists in part of a member that is comparatively stiff in an axial direction corresponding to the wire *A* in figure 1, and another member an elliptic spring *B* which is decidedly elastic in the same direction. The wire, however, is free to vibrate transversely while the spring is comparatively rigid in that direction.

A good result can be obtained by the use of other types of springs. For example, the vibrating wire may consist of an upper portion flexible transversely and stiff axially with the lower portion in the form of a spiral spring which is flexible in both directions. The main desideratum is that the elastic flexibility of the spring member in a vertical direction shall have that scale of stiffness which insures that the frequency rate of transverse vibration of the vertical element will be approximately the same over a reasonable range in amplitude.

While it might be possible to compute the exact dimensions of the elements that are involved in the combination, the problem would be a very complicated one, but it is fairly easy to find the correct proportions by experiment. Figure 2 shows several curves which illustrate the relation between amplitude and rate of a certain vibrating wire when coupled to the tensioning weight by elliptic springs of different lengths. These curves indicate very clearly that with the correct stiffness of elliptical spring the rate of the vibrating wire scarcely changes over a fairly wide range in amplitude.

Various methods of driving the vibrating wire have been tried, all of which involve some form of coupling between the wire and a vacuum-tube circuit. One very satisfactory form consists as shown in figure 1 of a small permanent bar magnet of cobalt steel very rigidly attached at the center of the wire. One end of this magnet projects into a pickup coil and the other end is surrounded by a driving coil. These two coils are connected respectively to one grid and plate of a twin-triode tube.

The other half of the tube is used for amplifying the voltage of the first stage. In order to avoid picking up external frequency in the first stage a $22^1/_2$-volt plate battery is used. The heater current and the other plate supply voltage come from a regular power circuit. A second tube further amplifies the energy so as to deliver about two watts at 120 volts which is adequate to operate a Telechron clock motor. For greater power a small amount of this output is diverted to another stage of amplification.

The relation between the plate-battery voltage and the time rate in seconds per day is shown in figure 3. The amount of current drawn from the plate battery is less than a tenth of a milliampere so that reduction of battery voltage takes place very slowly indeed. The amount of energy required to keep the string in vibration at ordinary atmospheric pressure is less than half a milliwatt.

The effect on the rate of barometric changes in the atmosphere is extremely small; apparently less than a tenth of a second per day for the ordinary range. Therefore it does not seem necessary for most purposes to provide an air-tight enclosure for this time standard but the instrument is so constructed that a bell jar can be placed over it. The compensation for variations in temperature is so accurate that for most purposes temperature control is unnecessary.

The instrument can be constructed so that 100 degrees Fahrenheit difference in temperature will produce less than one second per day variation in rate. Where the surrounding temperature is not likely to vary more than 10 degrees Fahrenheit the error due to this cause is negligible for many purposes. Probably the best location for an instrument of this kind is on a foundation of its own in an underground enclosure where the temperature would be comparatively constant.

For the purpose of measuring the prog-

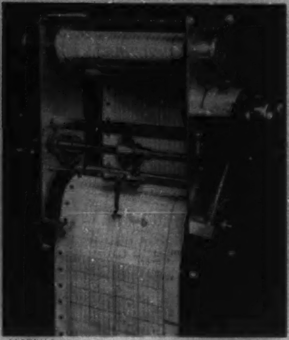


Figure 6. Graphic time comparator

ress which has been made in reducing temperature effects one of these compensated time standards has been exposed to comparatively wide temperature variations for a period of ten days. This was done by subjecting it to rather high room temperature during working hours and then cooling the room at night by shutting off the steam heat and opening the windows to the outside New England air.

The 24-hour average temperature varied from 65 to 81 degrees Fahrenheit and intermediate changes as great as 40 degrees Fahrenheit were observed. Over this period the average rate was one-tenth second per day fast. The greatest error was four-tenths second per day. No attempt has yet been made to determine the extreme precision which can be obtained with this new instrument but the indications are that time can be measured with an error less than one-tenth second per day when favorable conditions are provided.

There is an additional feature of this new time standard which may be of great importance for certain purposes. The weight which tensions the vibrating wire and very definitely controls its frequency is of course constant under the influence of gravity. Forming a part of this weight is a cylindrical Alnico magnet and in the air gap of this magnet there is a coil of fine insulated wire.

When a measured direct current is passed through the coil the reaction between the magnetic flux and this current will produce a force in the same direction as the gravity force but plus or minus in sign depending upon the direction of the current. The value of this current may be accurately adjusted by a graduated potentiometer or resistance so that the frequency rate of the vibrating wire may be controlled with great precision over a moderate range from a distant point. Figure 5 shows the relation between frequency and control current.

The amount of energy necessary to adjust the rate of a 60-cycle time standard over a range of plus or minus one cycle by this magnetic method is less than one-tenth of a watt. Consequently, there are no important heating effects which might be disturbing and the control apparatus may be very simple.

In order to study the performance of these new time standards, an interesting form of graphic comparator illustrated in figure 6 has been constructed. This consists essentially of a long revolving screw arranged transversely with respect to a moving strip chart. The screw is driven through gearing from a Telechron synchronous motor. On the screw there is a nut carrying a gear which is made to rotate by another Telechron motor. The gear reduction from this second motor to the nut is such that the screw and the nut normally revolve at the same speed and in the same direction. Any axial motion of the nut is transmitted to a light slider which carries a pen making contact with the paper chart.

Thus the line drawn by the pen indicates the axial position of the nut at any instant. One of the two synchronous motors is connected to a standard source of frequency and the other to a source of frequency being studied. This can for example be a power system in which case the line drawn on the paper chart will be an accurate record of the integrated system frequency. A small section of such a chart is shown in figure 7, curve *Q*. This record was made on one of the big eastern power systems and shows on a very open scale the integrated speed fluctuations which are continually taking place. In this particular chart the fine graduations are one-fifth of a second or 12 cycles apart. Consequently, it is possible to read extremely small frequency errors with high precision, far exceeding any of the ordinary graphic frequency recorders.

The slope of the curve is a measure of the instantaneous frequency. The transverse lines in this particular record are one hour apart. Consequently, a frequency error of $^1/_{100}$ of a cycle corresponds with a time error of six-tenths of a second per hour or an angle of about 45 degrees. An error in frequency considerably less than $^1/_{1,000}$ of a cycle can easily be seen. The kind of speed regulation on a system whether by ordinary governors supplemented by manual adjustment or by some type of automatic frequency controller, will determine the form of curve *Q* so that considerable information may be obtained from this record.

Figure 7, curve *S*, which is made by comparing one of these new time standards running at fixed temperature with another operating at variable temperature, shows very clearly the high precision of the instruments. The temperature variations to which the second instrument was subjected are shown in curve *T*. It is interesting to compare curve *Q* with curve *S* and observe how far short of perfection system frequency is at the present time, although for most commercial purposes it is very satisfactory.

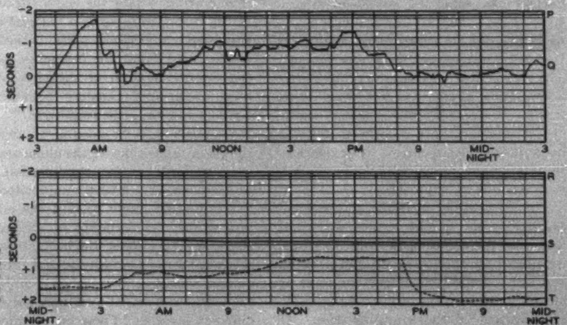
For certain requirements, precisely controlled frequency of this kind may be very important. For example, in driving large telescopes in astronomical observatories it is necessary that the image of a star

Figure 7

P and *R*—Reference lines drawn by stationary pen

Q—Integrated frequency of power system

S—Time rate with corresponding temperature variation *T* of vibrator unit



or other heavenly body should be held stationary in the field of the telescope for the purpose of making photographs which sometimes require hours of exposure. Since the telescope is swinging about an axis parallel to that of the earth one might suppose that the rate of angular motion should be exactly uniform to follow a star. Actually, it is necessary to vary this motion slightly because the refraction by the earth's atmosphere increases steadily from the zenith to the horizon.

Obviously, one of the best means of driving a telescope is a synchronous motor but if such a motor is supplied with alternating current of perfectly constant frequency some mechanical means is needed to adjust for the slight apparent error in stellar motion. The difference in motion of other heavenly bodies such as a planet or the moon would of course require a much greater adjustment of the rate.

From this explanation it will be appreciated that this new time standard contains within itself means for extremely precise and easily controlled rate adjustment and that it can supply alternating current to a synchronous motor driving a telescope so that the latter will accurately follow the motion of a heavenly body. The method of adjustment in the case of a star where the rate varies from the zenith to the horizon may become automatic through the use of suitable cams. Thus after the telescope has once been focused on a star the image will remain stationary in the telescopic field without any manipulation by the observer.

Utilization of this instrument for the purpose of driving telescopes is only one of a great many possible applications. To the scientist and engineer it may prove another useful implement in the endless campaign of progress.

Discussion

C. W. La Pierre (General Electric Company, Schenectady, N. Y.): The ordinary time standard, as we know it, is usually based upon either the gravitational constant or upon the physical constants of a particular material. The pendulum is an example of the first, and tuning forks and crystals are examples of the second. Mr. Warren has combined these two means of getting a time standard in such a way as to provide gravitational rate control such as is used by the pendulum with vibrational frequencies ordinarily associated with tuning forks. In this way, he has made provision for a very desirable feature, namely, ability to adjust the frequency of vibration over a fairly wide range. To those who have long desired an adjustable-frequency time standard, the new method opens many possibilities. Unfortunately, it also introduces into the time standard many of the difficulties of each of the previous basic methods.

As compared with the tuning fork, for instance, we find that the rate change for changes in amplitude is inherently somewhat worse than the corresponding change for a tuning fork. Mr. Warren has pointed out ingenious means for compensating for this amplitude effect.

With recent vacuum-tube circuits, the effect of amplitude change on frequency might be minimized by stabilizing the amplitude with the tube circuit. How this can be accomplished in tuning forks is described in Patent No. 2,147,492, M. S. Mead.

The ability of Mr. Warren's device to operate over a considerable frequency range by simple adjustment of current in a regulating coil is unique among standard-frequency sources. Unfortunately, we cannot regulate current to the same precision as we desire frequency. Otherwise, we could use current as the basic control for our frequency standard. In Mr. Warren's system, the error in current setting will result in a corrresponding error in only that part of the frequency which is dependent upon the flow of current. For instance, from Mr. Warren's figure 5, one-tenth ampere of current produces a change in frequency of approximately 2.3 cycles or a change in time of approximately 4,000 seconds per day. Therefore, if it is desired to operate 2.3 cycles from 50 cycles with a precision of one second per day, it would be necessary to maintain the current constant to one part in 4,000. This might involve some problems over a considerable period of time. On the other hand, such a departure from the normal frequency is not to be obtained by any of the more usual mechanical timing oscillators.

In addition to rate changes due to amplitude and possibility of frequency adjustment, Mr. Warren finds with his device, as well as with the other devices, that temperature compensation is extremely important. Quartz crystal oscillators having a temperature coefficient of one part per million per degree centigrade are common. However, the coefficient for quartz varies with ambient temperature, and it is difficult to secure this coefficient except over a limited range of, say, −30 to +50 degrees centigrade. Tuning forks can be readily made with coefficients less than ten parts per million per degree, and by special heat treatment this might be reduced to about two parts per million. Satisfactory temperature regulation on fork and crystal oscillators is relatively easy to secure, although there is always the possibility, if not probability, that the thermostatic regulator will fail and cause difficulty. Mr. Warren's figure of approximately two parts per ten million per degree centigrade indicates that he has achieved an excellent temperature compensation in this device as compared to what we ordinarily produce in crystal or tuning-fork oscillators.

Arnold White (nonmember; General Electric Company, West Lynn, Mass.): The author should be complimented for bringing this new instrument to the attention of the industry. A real need exists for a simple secondary standard of time, free from the objectionable characteristics (as noted by the author) of the standards now in use. Such a standard would enable the meter and instrument engineers to develop new test equipment which would expedite and simplify the calibration of watt-hour meters and frequency meters. For example, in the calibration of watt-hour meters, the number of revolutions of the meter disk must be counted and timed. This usually is accomplished by the test man holding a known load while visually counting and timing (by means of a stop watch or clock) the revolutions of the meter disk. This presents many chances for personal errors. Any methods frequently used to calibrate watt-hour meters, employing a light beam, photoelectric cell, and synchronous timer to count and time automatically the revolutions of the meter disk, offers a large saving in time and reduces the chances for personal errors to a minimum. The successful operation of this system depends on the availability of a source of constant frequency (such as is obtainable from a quartz oscillator or tuning fork) for operating the synchronous timer. However, due to the large investment in the source of constant frequency, this system, up to the present, has only been within the reach of the larger laboratories. It is hoped the new standard of time will bring this method of meter calibration within the reach of the smaller laboratories. The author does not show what effect, if any, local interfering vibration has upon the constancy of frequency. It is believed the same type of interference received through the base of a tuning fork and affecting its frequency may also affect the frequency of the vibrator unit thus necessitating the use of an interference absorbing mounting. The author suggests mounting the vibrator on a pier. We have had some experience with mounting a tuning fork on a pier. It was found that a pier did not materially reduce the interference from the earth tremors caused by trucks and other heavy machinery operating in the vicinity of our building. After trying several forms of Julius suspensions with little success, we found that a piece of slate (taken from an old switchboard) about two feet square and two inches thick mounted on four soft rubber balls about four inches in diameter (purchased at a local five-and-ten-cent store) relieved the situation entirely.

Henry E. Warren: Referring to C. W. La Pierre's comments, in addition to the very great reduction in rate change for changes in amplitude by the method suggested in the paper, the vacuum-tube circuit is so arranged as to hold the oscillating wire at almost constant amplitude so that time errors due to variations in voltage are very small indeed. The adjustment of rate by variations of the current in the regulating coil which adds to or subtracts from the gravity force would not ordinarily be used over a large range or for a long time. This feature is most important perhaps in regulating the angular motion of large telescopes where the rate variations due to changes in the atmospheric refraction between the zenith and the horizon and errors in the gear-tooth spacing, etc., are very small so that there should be no difficulty in holding the regulating current to the precision desired. If such a

Testing of Distribution Arresters

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THE testing of distribution lightning arresters, especially those removed from service, has increased sharply in recent years. Such testing by utilities is considered in this paper as distinct from design and routine tests made by the arrester manufacturers.

While check tests on new designs of line-type distribution arresters (rated at 15 kv and less) are sometimes made by utilities to insure that new arresters have the desired characteristics and meet specification requirements, yet few utilities make routine tests on new arresters. There seems to be little need for such routine tests in view of the good quality of present-day arrester design, manufacture, and factory tests.

Arresters in service, however, are mostly of types made about five years ago and earlier and in many cases are now known to have had originally or to have developed some deficiencies. Arrester failures or lack of adequate lightning protective characteristics have not usually been sufficient to warrant wholesale replacements or the trouble and expense of testing at the installations. In a very few cases operating experiences have justified complete replacement of some types of arresters manufactured 15 to 20 years ago.

Arresters removed from service incidental to other work, however, may or may not be in suitable condition for reinstallation. Routine inspection of such arresters, of course, serves only to eliminate those which are broken or damaged externally. In a few cases, operating experience has demonstrated that certain types should be junked upon removal.

For a large majority of arresters removed from service, information in this paper indicates that routine testing of the proper type makes available at relatively small expense arresters which are suitable for re-use. Blind junking of all distribution arresters removed from service, regardless of type, seems to be wasteful.

At the request of the AIEE lightning arrester subcommittee, the experiences of utilities with various test methods were obtained by the author for technical study.

Present Status of Arrester Testing

A questionnaire was sent in January 1939 to 55 operating companies. Replies were received from 52 companies.

Twenty-nine companies reported that tests of various types are an established procedure for distribution arresters, with only six of the companies testing new arresters. These companies included most of the largest utilities and a number of the smaller ones, all located east of the Rocky Mountains.

Table I shows a summary of test methods in use for new arresters and for arresters removed from service, as well as related cost data where furnished. Numbers were arbitrarily assigned to the 29 companies reporting tests. These utilities have been making tests on used arresters for periods ranging from 1 to 11 years, except that one company has been making 60-cycle withstand tests on removed arresters for 24 years. Six companies also test new arresters and one tests arresters at installed locations. Usually more than one type of test is applied by each utility.

Over 150,000 arresters removed from service due to causes other than trouble associated with the arresters themselves have been tested by the 29 companies. Probably over 40,000 arresters when new have been given routine tests, mainly by two utilities.

A detailed analysis of the data submitted in reply to the questionnaire is given in the following discussion, taking each type of test separately. As all of the 29 reporting companies made tests on removed arresters (see reference 1 for a description of some test methods) such tests will be discussed first.

Tests on Arresters Removed From Service

IMPULSE TESTS

Routine impulse tests on removed arresters are made by three of the companies reporting. Company 7 makes impulse tests on 3-kv arresters in accordance with AIEE Standards No. 28 for lightning arresters, using a portion of a 650-kv surge generator with 2,300-volt power follow supply. Characteristics are observed on a cathode-ray oscilloscope. Company 8 follows AIEE Standards No. 28, except that it uses surge currents of about 3,000 amperes with a 0.5x150-microsecond wave. This company uses a 100-kv surge generator with power follow supply at 2.8 and 5.6 kv for 3- and 6-kv arresters, respectively. Surge voltage is measured by sphere gap. Company 20 uses a 100-kv surge generator with a 1.5 x 40-microsecond wave and 1,200-ampere surge current. A 60-cycle voltage of 4 kv is applied to 3-kv arresters during impulse tests. This is quite severe.

Arresters rated at 3 kv are rejected by company 7 if their spark-over voltages are outside the range 8 to 14 kv or 14 to 20 kv, depending upon arrester manufacture. These limits, which agree reasonably well with the published range[2] of 12 to 22 kv for new arresters, were established by values found for new arresters.

Company 8 rejects arresters which have discharge voltage (*IR* drop) at 3,000 amperes exceeding 30 to 37.5 kv for 3-kv arresters and 35 to 47 kv for 6-kv arresters, the values used depending upon manufacture. These limits, which exceed the published maxima[2] of 15.5 and 30 kv by large margins, were selected to eliminate arresters having characteristics too far above the average for new arresters, and may be too liberal.

Company 20 has no limits other than failure to interrupt power follow current on surge test.

No information on rejections was given by company 7. For company 8, 4,167

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The author acknowledges the co-operation of 29 utilities in furnishing extensive data and the valuable assistance of E. H. Grosser in preparing the paper.

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gross shift in frequency as two cycles were to be held over a period of a day a major correction could be readily made by adding or subtracting suitable weights.

External vibration if transmitted to the oscillating wire of this time standard may have a detrimental effect but this effect is greatly reduced in the instrument itself by the use of soft springs which support the heavy mass to which the upper end of the wire is fastened. Further absorption of external vibration by simple means like that suggested by Arnold White seems desirable for locations where vibration is considerable.

arresters, or about five per cent of all arresters returned from service, have been rejected for failure to meet impulse and 60-cycle withstand tests. Their records do not differentiate between the two causes of rejection. Only about one per cent of the used arresters tested by company 20 have been rejected on impulse tests.

The impulse test is the only test which serves to indicate the ability of the arrester to protect equipment and to interrupt power follow current. A relatively simple test method appears to be that of company 20, if follow current at a suitable voltage is supplied by a transformer having bushings gapped to an impulse flashover voltage slightly higher than the normal characteristic of a satisfactory arrester. Inadequacy of the arrester is indicated either by bushing-gap flashover or by automatic opening of the supply to the transformer.

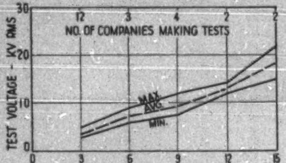

Figure 1. Sixty-cycle withstand test voltages in use for distribution arresters removed from service

Thorough testing for impulse characteristics should follow AIEE Standards No. 28.

The experience of companies 8 and 20 indicates that changes in the impulse characteristics of arresters occur in service in relatively few cases, and that impulse tests are not needed ordinarily. Company 17 also found this indication based on impulse tests on a few moderately corroded arresters of one type.

Sixty-Cycle Withstand Test

As the name implies, this test consists in applying a predetermined value of low-frequency voltage across the arrester terminals for a definite time, usually one minute. Spark-over of the arrester gap, evidenced by appreciable current flow in the circuit, and opening of the power supply by overload relay, is taken as cause for rejection.

Test voltages in use by 13 companies are summarized in figure 1. Tests are made at voltages ranging from 83 per cent to 167 per cent of arrester-voltage ratings, the average being 113 per cent. Five companies test at rated voltage.

Five companies reported data on rejections based on the 60-cycle withstand test. For tests at average voltages of 1.0, 1.1, 1.3, 1.3, and 1.5 times rated arrester voltages, the rejections in this test were roughly 0, 5, 0.1, 15, and 1.5 per cent, respectively. These results, which are probably obscured by other factors, give no indication that the higher test voltages result in a larger number of rejections.

Seven of the companies have dissected arresters rejected in the 60-cycle withstand test. The most common findings were as follows: corroded gap assemblies (seven companies); moisture in gap assemblies, gap welded, foreign particles from the valve element in gap assemblies, porcelain gap spacers cracked, and valve element punctured or damaged (each reported by one company). One utility which found corrosion in many cases reported that no trouble was apparent in some dissections of rejected arresters.

It is probable that test voltages equal to, or little more than, the rated arrester voltage could be depended upon to reject only those arresters having serious internal defects. The test, in general, is apparently not severe, since much higher percentages were rejected in other tests by other companies. Arresters rejected in this test have generally, but not always, revealed internal defects when dissected. Nine of the 13 companies making 60-cycle withstand tests, however, state that the test is satisfactory; no statement was received from four companies.

This test, even when used at slightly above rated voltage, is to be recommended only because of its simplicity. It should not be considered an infallible test for defects, but should be used in conjunction with other tests.

Sixty-Cycle Breakdown Voltage Test

This test has been used by 15 utilities, for periods ranging from 1 to 11 years, to check arresters returned from service. As shown in table I, 9 of the 15 companies also make 60-cycle leakage-current tests, using the same circuit as for the breakdown test, and three companies make breakdown tests and Megger or resistance measurements.

The circuits used for making 60-cycle breakdown tests consist of a variable-voltage testing transformer or a transformer with induction regulator or tapped autotransformer in the low-voltage circuit, a voltmeter connected to the transformer voltmeter coil or to a separate potential transformer, or (company 22) an electrostatic voltmeter, and a series resistor for limiting the current in the circuit at arrester breakdown. This resistor may be located in either the low-voltage or high-voltage circuit, as has been illustrated.[1]

Eleven of the companies employ resistors in the high-voltage circuit, two (one of which, having two test circuits, is also included in the preceding group) use resistors in the low-voltage circuit, two use no resistors but depend on the impedance of the testing transformer, and one gives no information. The ohmic values of series resistors used range from a fraction of a megohm to 100 megohms. These resistances were selected to limit the current through the arrester during breakdown to values, calculated from data submitted to the author, ranging from less than 1 milliampere to 25 milliamperes. The average of maximum permissible currents in tests by nine companies was approximately 14 milliamperes. One of these companies used two values of limiting current, 10 and 20 milliamperes, for different makes of arresters. Another company, not included in this average, used an overcurrent relay to trip the low-side breaker in about 15 cycles for currents of 15 to 35

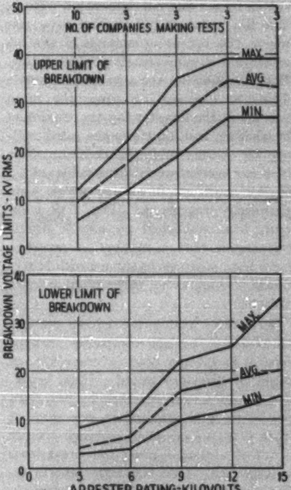

Figure 2. Acceptable limits of 60-cycle breakdown voltages in use for arresters removed from service

milliamperes, but used no series resistor.

Table I. Tests Made on Distribution Arresters by Utilities

	Test Method							
		60-Cycle Tests					Megger or Resistance Bridge	Radio Interference or Ionization
				Leakage Current				
Company Number	Impulse Voltage	Withstand Voltage	Breakdown Voltage	Microammeter	Milliammeter	Neon Tube		
Number of years test has been in use on arresters removed from service:								
1		1						1
2		24						
3		0					4	
4		7						
5		0	Several					Several
6		9						9
7	7	3					3	
8	5*	9*						3*
9			1	1			1	1
10					10**			
11			1	1				
12			4					
13			10		3			3
14			1½					
15			1½				1½	
16			2*	2*				2*
17			6	6				
18			10*					10
19		5						
20	2	2	2				2	2
21		9*	7*		9*			
22			11		11	11		
23			2	2				
24		6						6
25			2*			2*		
26		9*						9
27		1						
28		6						6
29							5	
Average years in use	4.7	7.0	4.4	2.4	8.3	6.5	2.8	4.7
Number of companies using test for:								
New arresters	1	3	4	1	1	1	0	2
Used arresters	3	13	15	5	4	2	6	12

* These tests are also made on new arresters.

** Approximate or estimated.

Only two companies regularly make correction for the voltage drop in the high-side series resistor, in determining the arrester breakdown voltage. A third company makes this correction only for arresters having resistance shunted gaps, where the leakage current prior to breakdown is appreciable. For arresters with ordinary gaps and such that they would not be rejected in the leakage-current test, the drop through the current-limiting resistor would probably be negligible. Where the current-limiting resistor is placed in the low-voltage circuit, no correction, of course, applies, since voltage measurement is always made with respect to the high-voltage circuit.

Limits of acceptable 60-cycle breakdown voltage are shown in figure 2. Minimum acceptable breakdown voltages for arresters returned from service range from 0.7 to 2.8 times arrester rated voltage, and maximum acceptable breakdown ranges from 1.8 to 4.0 times rated voltage. The low value of 0.7 applied to a case where the operating voltage of the arrester was a minor fraction of its rating. For 14 companies the average acceptable limits were about 1.3 times the rated voltage of the arrester for the minimum breakdown and 3.0 times the rated voltage for the maximum breakdown. This is in fair agreement with the values of 1.5 to 3.0 times rated voltage which were previously suggested.[1] The 15th company has established no specific limits, but stated that a number of arresters have been returned to service with breakdowns as high as 3.4 times rated arrester voltage.

Rejections for low breakdown voltage, as reported by six companies, ranged from 0 to 6.7 per cent (average 2.0 per cent) of arresters tested. High breakdown values were reported by five companies to have resulted in rejections of from 0 to 2.8 per cent (average 1.4 per cent) of arresters given the breakdown voltage test. One company reports rejections of one per cent for unsatisfactory breakdown action, that is, gradual breakdown, or high resistance after breakdown. Total rejections by seven companies in the 60-cycle breakdown voltage test averaged 4.1 per cent, and ranged from 0 to 8.8 per cent.

An attempt was made to determine whether the rejections in this test were related to the rejection limits used. No correlation could be found, either for lower or for upper limits.

Arresters rejected in this test have been dissected by nine companies, to determine the causes for unsuitable breakdown values. Usual findings were: gap electrodes pitted, burned, or springs broken (six companies); gap assembly corroded or contained moisture (five companies); foreign particles in gap assembly (one company); valve element damaged or wet (two companies). The most usual causes for low breakdown were gaps pitted and gaps shorted by corrosion or burning; for high breakdown, damaged gaps or deteriorated springs, resulting in increased gap separation. The one company (number 17) giving unsatisfactory breakdown action as cause for rejection finds each of the above causes to be responsible in varying degree. One company (number 14) included above, however, stated that no correlation was found for high breakdown voltage, and another (number 15) that, in general, no correlation seemed to exist between test results and arrester condition.

Two utilities sent in the results of dissections in some detail. One (number 12) found low breakdown voltage to be associated with pitting or corrosion of gap assemblies in each of 11 arresters dissected, but dissections of 14 arresters having breakdowns of from 4.1 to 5.6 times rated voltage failed to reveal any defects in nine cases. The other (number 25) found deteriorated metal parts in several arresters rejected for low breakdown.

A review of this test method, which is extensively used, indicates that it is mainly useful as a means of detecting internal defects, particularly in metal parts, when applied in conjunction with leakage-current measurements or megger testing. While low breakdown voltage is generally associated with defective conditions, this has not always been found for high breakdown voltages. It seems undesirable to employ the same upper breakdown limits for all makes and types of arresters used on a system, but such limits should be determined for each type by tests on good arresters. The test is more informative than the 60-cycle withstand voltage test.

SIXTY-CYCLE LEAKAGE-CURRENT MEASUREMENTS

Such measurements have been used for periods ranging from 1 to 11 years by ten companies. Only one utility (number

Table I (Continued). Tests Made on Distribution Arresters by Utilities

Company Number	Number of Arresters Tested		Per Cent of Arresters Rejected by Test		Testing Cost per Arrester Tested	Testing and Rehabilitating Cost per Arrester Returned to Stock
	Arresters Removed From Service	Part or All of New Arresters	Arresters Removed From Service	New Arresters		
1	500	0	5.0		—	$0.50 to 0.75
2	8,500	0	1 to 2		—	0.84
3	1,200	0	58			
4	?	0	?			
5	500/yr	0	?		—	0.50 to 1.00
6	?	0	6 to 10		$0.10	1.10**
7	350	0	?		0.50 to 0.75	—
8	85,000	10,050	5.6	0	0.04	0.30
9	717	0	17		—	0.60**
10	16,000 (in 4 yrs.)	0	16			
11	500	0	24		—	0.15 to 0.25
12	2,375	0	2.9		0.15 to 0.25	1.30 to 1.40
13	286#	0	20		0.25	2.00**
14	200	0	40†			
15	1,485	0	9.8		—	0.28**
16	1,477	195	23	16	0.50	0.94
17	6,600	2,200‡	8.3	Few	0.35	1.12
18	?	?	Few	Few		
19	393	0	15		0.20**	—
20	1,919	0	3.0		0.35	—
21	6,882	25,508	7.2 (incl. new arr.)		—	0.75 to 1.00
22	4,571	0	20†		—	0.60
23	4,100	0	10			
24	?	0	?		0.10**	
25	?	?	?	?		
26	?	?	30**	0		
27	20	0	0			
28	192 (1938)	0	22			
29	?	0	<5			
Total or average	143,267+	37,953.+	9.4** (weighted average)		$0.27 (average)	$0.83 (average)

† Includes rejections due to mechanical defects.
? Means no data submitted, or utilities had no records readily available.
Since 1936.
‡ Routine test on new arresters has been discontinued by this company.

10) tests for leakage current with no corresponding breakdown voltage test.

In general, the same test circuit is used for leakage and breakdown measurements, and consists of a variable high-voltage supply to the arrester, and series meter for measuring the leakage current (and small charging current) of the arrester under test. No attempt is made in most cases to shield the meters from stray fields as this is done by only two companies reporting. One company (number 10) wets down arresters before testing.

Current through the arrester at rated voltage is measured by five companies by means of microammeters, capable of indicating minimum values ranging from 4 to 50 microamperes. Three companies (numbers 10, 21, and 22) use milliammeters with minimum readable currents from 100 to a few hundred microamperes. Company 22 uses the milliammeter partly for routine tests and partly as a reference instrument; routine checks are also made by connecting an ordinary three-watt 110-volt neon lamp, with resistor in base removed, between arrester ground lead and ground. The tube glows brightly for currents greater than about 250 microamperes. Company 25 employs a neon tube in a somewhat different manner; it is connected across an 0.45-megohm resistor inserted in the arrester test circuit and glows at a critical voltage of 90 volts, corresponding to a leakage current of 200 microamperes. Company 13 uses a cathode-ray oscilloscope coupled to the circuit as a milliammeter, with minimum indication at about one milliampere.

For the five companies using microammeters, four reject used arresters if leakage current at rated arrester voltage exceeds 150 microamperes—one of these (number 9) is considering a lower value for future use; the fifth company (number 16) has an upper limit of 50 microamperes for arresters. One of the four companies using 150 microamperes as a general limit (number 17) accepts one type of arrester conditionally if leakage current is between 150 and 500 microamperes, and is tagging such arresters for future identification. Company 11 has found that serious corrosion exists in this same type of arrester only where leakage currents exceeded 300 microamperes, and is conducting more inspections to establish a separate limit for this type, to supplement the 150-microampere limit for some other arresters.

Limits set by companies using milliammeters are usually minimum readable current values, which correspond to several hundred microamperes for company 10, about 250 microamperes for company 21, and about one milliampere for company 22. Companies 13 and 25, using special methods as mentioned above, have limits of about one milliampere and 200 microamperes, respectively. All limits refer to tests at rated arrester voltage, except those for company 22, which tests 9- and 15-kv arresters at rated voltage, 3-kv arresters at 4 kv, and 6-kv arresters at 5 kv, and for company 25, which tests 3-kv arresters at 2.4 kv and 12-kv arresters at 8 kv.

Six companies, three using the 150-microampere limit, gave data on rejections in this test. The rejections ranged from 5.3 to 16 per cent. The average rejections for limiting leakage currents of 50, 150, several hundred, and 1,000 microamperes were 14, 9.6, 16, and 5.3 per cent, respectively.

Dissections of arresters rejected in this test by eight companies showed corrosion of gap assemblies in most cases. Damaged gap assemblies were reported in some cases by two of these companies, and one company occasionally finds foreign particles in the gap assembly.

Test limits were set up by these eight companies on the basis of conditions found in dissecting arresters after test. Company 16 found that with leakage currents just above 50 microamperes many arresters showed corrosion, and practically all were corroded which had more than 100 microamperes leakage. Company 17 found that the limit of 150 microamperes at three-fourths of rated voltage (this limit now applying to tests at rated voltage) resulted in rejections of no arresters in good condition, about two-thirds of those with slight corrosion, and all badly corroded arresters. This is shown in figure 1 of reference 1. Company 23 supplied data for eight dissected arresters having leakage currents ranging from 155 to 400 microamperes which showed corrosion in each case. Arresters dissected by company 10 for a readable milliammeter value (several hundred microamperes) showed some defects in all cases.

Since these tests, with the two exceptions noted, are made at the rated voltage of the arrester, limiting leakage-current values appear to be about the same for different voltage ratings.

Tests on one type of arrester by company 17 have shown that 60-cycle breakdown and leakage-current measurements are practically independent of ordinary variations in ambient temperature, and that test results are reasonably reproducible over a period of several months after removing the arrester from the system.

In summary, the leakage-current test, used in conjunction with the 60-cycle breakdown test, has been used successfully by a number of utilities as a means of weeding out arresters unfit for further service.

Megger Test

Megger tests or other resistance measurements have been used by six utilities for one to five years. A 500-volt Megger is used by company 29, 1,000-volt Meggers by companies 3 and 9, and company 15 uses a 2,500-volt Megger; the Megger voltage is not stated by company 7. Company 20 uses a 12,000-megohm bridge. Limits are set at the following minimum values: company 15, 25 megohms; company 29, 100 megohms; company 20, 1,000 megohms; company 3, infinity. This is the only test used by companies 3 and 29. Companies 7 and 9 give no limits. One other company (number 13), which makes no routine Megger tests, considers this test helpful as an auxiliary check, if low enough limits are used.

The four companies having limits of 25, 100, 1,000, and infinity megohms report rejections in this test as 9.8, less than 5, 1.5, and 58 per cent, respectively. The use of the infinity limit by the latter company appears to be the cause of the unusually and, perhaps, unnecessarily high rejection rate. The 1,000-megohm limit is used by company 20 merely for rapid selection of defective units, before subjecting them to other tests.

Because of the simplicity of Megger testing, it is of interest to consider how this test compares in results with measurement of 60-cycle leakage current. Company 13 considers resistances of one megohm or less a definite indication of corrosion, but states that even with values above 20 megohms corrosion may be present. It is to be noted that the limits in use are all above 20 megohms. Data presented by company 15, covering dissections of 92 arresters, showed that the 25-megohm limit resulted in the rejection of all badly corroded and most of the moderately corroded units. Only 15 per cent of the arresters with slight corrosion and 7 per cent of those in good condition had Megger readings which would have resulted in their rejection. Company 29 found either corrosion or dust from the valve material in the gap assemblies of practically all arresters rejected in this test. Company 7 found no corrosion in arresters having Megger readings of 2,000 megohms or more, but reported one arrester with zero Megger reading which was badly corroded, and one reading 200 megohms where the gap chamber contained a web-like fungus growth.

Another company, number 9, gave corresponding 60-cycle leakage current and Megger data on over 400 arresters. A fair relationship is shown by these data, which on analysis indicate that corrosion conditions could be detected about equally well either by the 150-microampere maximum in the 60-cycle leakage current test or by a minimum of about 25 megohms in the Megger test. This company stated, however, that it is still unable to set up definite limits within which Megger readings should fall to give an accurate indication of the internal condition of the arrester, but that in the absence of other test facilities in the field the Megger is of some value in detecting moisture. They are considering a survey by Megger test of all arresters at 13.2-kv substations.

An arrester manufacturer who has conducted numerous tests on used arresters returned to the factory reports that no correlation has been found between Megger readings and arrester condition or ability to perform properly. This manufacturer prefers the 60-cycle leakage-current test, which, however, it does not consider infallible.

Considering each test by itself, then the Megger test is the simpler of the two methods. Where 60-cycle breakdown tests, however, are made, the leakage-current measurement, which appears to be the more positive test, is to be preferred, since little additional testing equipment is required. When, as, and if tests on installed arresters become advisable, the use of only the simple Megger test may be attractive.

Radio-Interference and Ionization Tests

Such tests are made by 12 companies, including two companies (numbers 13 and 16) who make ionization tests. The functions of the two tests are essentially the same, that is, to detect high-frequency oscillations set up by corona discharge within the arrester.

Radio-interference tests are made by the five utilities who described their test method by merely operating a sensitive radio set near the energized arresters. One company (number 1) mentions a separation of eight feet between the arrester and radio set antenna. Perhaps the test conditions are unnecessarily severe since arresters in service are usually relatively far from customers' equipment.

Company 8 uses a portable battery-fed receiver with a milliampere meter added to the output circuit, but ordinarily, as with the other four companies, depends on the noise from the loud-speaker. A sixth company (number 20) also uses a conventional broadcast receiver, but coupled to the primary lead of the arrester test circuit through a tuned coil and shielded lead.

Ionization tests, as used by companies 13 and 16, employ a cathode-ray oscilloscope either coupled to the arrester circuit through an air-core transformer or connected across a series inductance inserted directly in the circuit. Arresters are rejected in this test if high-frequency oscillations appear on the oscilloscope screen when the arrester is energized at the rated 60-cycle voltage of the arrester (company 13) or 1.33 times the arrester rating (company 16).

In order to pass the radio interference test, no radio noise must be noted when the arrester is energized at operating voltage (company 8), operating voltage plus one kilovolt (company 6), rated arrester voltage (companies 5, 24, and 26), or 0.83 to one times rated voltage (company 9). Company 20 tests at rated arrester voltage for arresters having Megger readings more than 1,000 megohms and at 1.33 times rated voltage for those showing less than 1,000 megohms. Company 1 applies eight kilovolts between arrester and mounting bracket in this test for both three- and nine-kilovolt arresters. The test voltage was not stated by two companies.

Arresters which fail to pass the radio-interference test of company 6, but which appear otherwise satisfactory, are dried out in an oven at about 70 degrees centigrade for 36 hours and then retested. About half of these arresters are reclaimed by this procedure. Company 17 made a trial of a similar procedure and found considerable reduction in 60-cycle leakage current for only those arresters which had visible cracks in the sealing compound; hence, it was concluded that this improvement was not likely to be permanent.

Rejections in this test, for six of the companies, ranged from 0.5 to about 11 per cent, with an average of about 3.8 per cent of arresters tested. Four companies report that arresters thus rejected show, on dissection, corroded or damp gap assemblies or loose gap structures.

Some data are given for one of the largest urban systems, which indicate that radio-interference troubles due to faulty arresters may be too few to warrant this type of test. Field investigation of 543 complaints of radio interference during the past six years showed that the utility's equipment was responsible in only 12$^1/_2$ per cent of the cases, and that only ten or 1.8 per cent of all complaints could be traced to faulty arresters. This particular company does not test arresters for radio interference.

Radio-interference or ionization tests appear desirable as a means of reducing radio interference caused by arresters on distribution systems only where this trouble exists to a serious extent. Where such tests are made to determine radio-interference levels likely to cause complaints in service, the method described in EEI Publication No. *C*-9, "Methods of Measuring Radio Noise," is recommended.

LEAKAGE OVER PORCELAIN

Company 21 includes in its routine tests on new and used arresters a test for "leakage over porcelain." Twice rated 60-cycle arrester voltage plus 1,000 volts (except three-kilovolt arresters, which are tested at 10 kilovolts) is applied between the line lead and hanger bracket, and leakage current over the arrester body measured with a milliammeter. The maximum permissible leakage current is five milliamperes. No data were given to correlate test results with arrester conditions.

Tests on New Arresters

As shown in table I, routine tests on new arresters are made by six companies: three (numbers 21, 25, and 26) testing all new arresters, two (numbers 8 and 16) ten per cent of arresters purchased, and one (number 18) three arresters of each rating from every shipment received. Tests are made in the same manner as the tests previously described for arresters removed from service. The test limits are the same as for used arresters for these companies, except that company 16 has a 30-microampere limit in the 60-cycle leakage test.

Data regarding rejections of new arresters on routine tests were supplied by companies 8 and 26, reporting no rejections in more than 10,000 tests, and by company 16, which rejected two arresters out of 195 (representing 1,950 arresters purchased). Company 17, which no longer tests new arresters except for new designs, in earlier tests on 2,200 new arresters rejected very few by test.

These practices and results indicate that routine testing of new arresters by the purchaser may be difficult to justify. Tests on new designs and occasional check tests on representative arresters purchased, however, may be of value.

Tests on Arresters in Service

Radio interference tests are made in the field by company 7 on installed three-kilovolt arresters, incidental to this company's general investigations of radio interference complaints. Measurements are made with a portable radio set, the output of which is determined using an attenuation box. The detection of defective arresters is often aided by jarring the pole with a sledge hammer, but positive localization requires disconnecting the arrester from the line during noise level measurements.

Interference from arresters was reported to be caused most often by moisture and corrosion in the gap chamber. Of approximately 1,000 arresters checked in the field each year for radio interference since 1924, about 100 arresters have been removed annually and scrapped.

Test Combinations in Use

As shown in table I, testing procedures in use generally include more than one type of test, each test as a rule serving to detect specific types of defects. The most usual combinations, where all types of tests are considered, include: 60-cycle breakdown and leakage current (five companies); 60-cycle withstand and radio interference (five companies); and 60-cycle withstand alone (four companies). Excluding impulse, radio interference, and ionization tests, the most common combinations are: 60-cycle withstand alone (ten companies); 60-cycle breakdown and leakage current (seven companies); and 60-cycle breakdown alone (four companies).

Economics of Arrester Testing

Data on the costs of testing and rehabilitating arresters removed from service, as given by 14 companies, are included in table I. Two cost figures are shown: the testing cost per arrester exclusive of fixed and maintenance charges of the testing equipment, and the cost of tests and repairs per arrester returned to stock. The latter figure includes the cost of work on arresters which were found unsuitable for re-use, prorated as to each arrester accepted.

Testing costs range from 4 to nearly 75 cents, averaging about 29 cents per arrester tested. These costs apparently vary widely with local conditions, since they bear no relation to the number or types of tests made.

Total costs per arrester found to be in condition suitable for re-use range from about 20 cents to \$2.00, and average about 83 cents. These costs vary widely due to local conditions as well as to the amount of repair work done. Company 6, reporting a total cost of \$1.10 per arrester, replaces broken leads and caps and bakes out those arresters which initially fail to pass the radio-interference test. The total cost of \$1.30 to \$1.40 per arrester given by company 12 includes increasing the length of the line lead in all cases. Costs for neutral gaps are given as 69 cents by company 17, as compared with \$1.12 for three-kilovolt phase arresters, the difference resulting from less repair work required.

Costs for testing and rehabilitating arresters of different voltage ratings are reported to be essentially independent of rating by companies 1, 5, 12, and 15.

The average cost of testing and rehabilitating arresters for re-use amounts to between 5 and 15 per cent of the new arrester cost, for ratings of 3 to 15 kv. Assuming that the useful life of the rehabilitated arrester is equal to one-half of the life of arresters now made, then, for example, there appears to be a saving of the order of 65 per cent in re-using 3-kv arresters after thorough test, over the cost of replacement with new arresters. For higher voltage ratings the economic advantage of testing arresters is more pronounced.

General Summary for Used Arresters

A summary of the test methods in use, together with conditions and defects indicated by each test, is given below for purposes of comparison:

(*a*). *Impulse Tests*. Impulse characteristics; ability to interrupt follow current.

(*b*). *Sixty-Cycle Withstand*. Following to a serious extent: corrosion; damaged gaps or valve elements; foreign particles in gap.

(*c*). *Sixty-Cycle Breakdown*. Pitted, burned, or corroded gaps; broken springs; foreign particles in gap; damaged or wet valve element.

(*d*). *Sixty-Cycle Leakage*. Corrosion; other conditions to a limited degree.

(*e*). *Megger*. Corrosion; other conditions to a limited degree.

(*f*). *Radio Interference or Ionization.* Conditions, such as corrosion or loose contacts, causing radio interference.

Test method (*a*) serves the purpose of checking the protective characteristics of the arrester, while the other methods are used to determine the physical conditions of the arrester as affecting the expected electrical characteristics in future service. Test methods (*b*) and (*c*) serve somewhat the same purpose, although method (*b*), the simpler test, is less effective than method (*c*). Likewise, methods (*d*) and (*e*) are similar in objectives; test (*e*) is relatively simple and inexpensive, but not proved as reliable; method (*d*), however, may be readily combined with test method (*c*), where used, and may be more successful under usual conditions.

In general, testing costs represent a small proportion of the costs of restocking good used arresters, and so differences between costs of tests may be unimportant. This is, of course, only true where sufficient arresters are to be tested to justify initial costs of testing equipment.

Suggestions for Testing Used Arresters

For routine tests on arresters removed from service, suggested combinations of tests which have been found to be simple and effective, and which require little complicated or expensive apparatus are as follows:

1. Sixty-cycle breakdown and leakage current measurements.
2. Sixty-cycle breakdown and Megger tests.
3. Sixty-cycle withstand and leakage current (or Megger) tests.

In conjunction with any of these test combinations, radio interference or ionization tests may be used, where such testing is warranted by local radio-interference conditions. Also, occasional or routine impulse tests may be made on used arresters where suitable equipment is available.

Based on present practice and results of tests in use, test limits suggested for trial use by utilities starting routine testing of arresters returned from service are given below:

Impulse Test. Use reference 2 as a general guide for voltage limits, but modify them in accordance with testing experience with used arresters.

Sixty-Cycle Withstand. Test at 1.0 or $1^1/_4$ times rated arrester voltage.

Sixty-Cycle Breakdown. Acceptable voltage range: $1^1/_2$ to 3 or 4 times rated arrester voltage.

Sixty-Cycle Leakage Current. Maximum at rated arrester voltage; 150 microamperes or lowest reading on milliammeter scale.

Megger. Minimum: 25 megohms. Preferable to use 1,000- or 2,500-volt Megger.

Radio Interference. Test at rated arrester voltage.

These values should be considered as tentative and should be modified as found necessary to meet individual conditions after the testing program has been started and a number of arresters, which on test show values both above and below the trial limits, have been dissected.

References

1. TESTING AND APPLICATION OF LIGHTNING ARRESTERS, AIEE Lightning Arrester Subcommittee Report. AIEE TRANSACTIONS, 1939, page 68 (February section).

2. DISTRIBUTION LIGHTNING ARRESTER PERFORMANCE DATA, AIEE Lightning Arrester Subcommittee Report. ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), volume 56, 1937, page 576.

Discussion

E. J. Allen (General Electric Company, Pittsfield, Mass.): The replies from 29 companies now engaged in the testing of distribution lightning arresters have been well analyzed and presented. In view of the diversity of methods employed by the different companies making the six classifications of tests shown on table I, it would be of interest to determine what reasons govern the particular choice of some of the tests and what they were intended to demonstrate. In the best interests of uniform interpretation of data obtained from testing of distribution arresters, it is obvious that uniformity in test methods need be followed.

Our experience as a manufacturer with new and used arresters has demonstrated that the already prescribed test which most nearly approaches actual service conditions is the "operating duty test" set forth in AIEE Standards No. 28, issue of March 1936. As the three companies making impulse tests on arresters apparently do so with combined power voltage, the term "impulse test" applying to these companies and used by the author would preferably be extended to the term "operating duty test" as designated in the Standards. This distinction is made, since without the combined power voltage there is no indication of the arrester's ability to interrupt follow current.

Contrary to the practice described by one company, the AIEE Standards do not specify that the power voltage applied to the arrester during the operating duty test shall be in excess of maximum arrester rating, as this is not a condition for which the arrester was designed. The operating duty test on three-kilovolt arresters with four-kilovolt power voltage applied, may result in unnecessary damage to the arresters and hence should be discontinued.

Notwithstanding the fact that the AIEE Standards "operating duty test" describe a single test that will probably tell the most about the operating condition of a lightning arrester, simpler tests have been sought for use either in the laboratory or field, that would be equally indicative. The 60-cycle spark potential, 60-cycle withstand voltage, leakage current, Megger, or resistance bridge tests are examples which are described in the paper and which are used the most in the order just stated. Since these tests are only partially indicative of the operating condition of the arrester, they are subject to the following limitations:

SIXTY-CYCLE SPARK POTENTIAL TEST

(*a*). The most that this test might be expected to show is either a short-circuited arrester or an open-circuited arrester.

(*b*). This test will not necessarily show a damaged valve element, presence of moisture, or otherwise disclose the valve or resealing characteristics of the arrester. It is not an indication of the impulse protective characteristics of the arrester or suitability of the arrester for further service.

(*c*). Unless extreme precautions are observed, either or both of the gap and valve elements in good arresters may be damaged and unless the results are intelligently analyzed, good arresters may be unnecessarily rejected. Since this test subjects the valve element to 60-cycle voltage in excess of its rating, tests should be limited to not more than three applications and spark-potential reading.

SIXTY-CYCLE WITHSTAND TEST

(*a*). This test will indicate only a short-circuited arrester. As the author points out, it should not be considered an infallible test for defects and should be used in conjunction with other tests.

(*b*). The power voltage applied should preferably not be in excess of the maximum arrester rating. If the applied voltage is 1.25 times maximum arrester rating, as suggested in the paper, care should be taken to avoid sparking over the arrester gap by switching surges incidental to the test, as such spark-over of the gap could destroy the arrester by application of power voltage above the arrester's maximum valve or resealing rating.

LEAKAGE CURRENT, RESISTANCE BRIDGE, OR MEGGER TEST

(*a*). The most that this test might be expected to indicate is the possible presence of moisture or corrosion in the arrester elements that may or may not be injurious to the service operation of the arrester.

(*b*). This test will not necessarily differentiate between leakage current caused by external moisture, humidity or surface deposits, as against leakage currents caused by moisture or corrosion of internal elements. This test will not show an open-circuited arrester, damaged valve elements, or the protective characteristics of the arrester.

(*c*). While the leakage current measured at no greater applied voltage than maximum arrester rating would not be likely to damage it in any way, precautions are necessary to assure that the applied power voltage never exceeds the maximum rating.

(*d*). As the author mentions, the 60-cycle leakage-current test at maximum arrester voltage rating is to be preferred as compared with the resistance bridge or Megger.

Hence, the simpler tests which show the operating condition of a lightning arrester only to a partial extent should not be taken in themselves as definite cause for arrester rejection. Such arresters might give many years of continued reliable protection.

The author's statement that there seems to be little need for tests on new arresters by the operating company is borne out in table I of the paper in which virtually no new arresters are rejected by any of the testing methods described in the paper.

Cost data has been included covering testing and repairing by the companies reporting. The average testing and rehabilitating cost per arrester returned to stock of $0.83 apparently covers only minor re-

The Direct-Acting Generator Voltage Regulator

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Synopsis: The principal factors involved in the design and application of direct-acting voltage regulators and their relations to each other are discussed in this paper. The mathematical method used for deriving these relations is outlined. Definite conclusions are drawn concerning proper operation and design of this type of regulator.

In recent years increasingly wide use has been made of direct-acting voltage regulators. During this time, improvements in design and a better understanding of their characteristics have developed. This paper describes some of the improvements and presents a mathematical analysis of a regulating system including a generator and exciter with a voltage regulator. It will be seen that many of the improvements in design follow directly and logically from the results of the mathematical analysis.

Improvements in Design and Operation

The first requirement of a regulating device is that it hold the regulated voltage very closely to the desired value without hunting or instability. Its ability to do this is influenced by all the elements of the system, including the generator and its load, the exciter, and the voltage regulator. Therefore, in order to understand the factors involved it is necessary to study the system as well as the regulator itself. It has been found that essentially perfect regulation can be maintained by the modern regulator over a wide range of operating conditions with stability. This performance is achieved by utilizing an inherent nonlinear property of the resistance elements, by careful balancing of the electrical and spring torques at every point in the range, and by use of a stabilizing transformer.

The second important requirement of a regulator is that it make the necessary changes to return the voltage to normal as quickly as possible after any sudden change in the operating condition. The time for voltage recovery is also affected by the other parts of the system. All that the regulator can do is to go immediately to its extreme position for raising or lowering the voltage, and then as the voltage approaches normal, fall back in such a way as to prevent excessive overswing. The extent to which the modern regulator approaches this ideal is shown by the oscillogram in figure 1. Notice that the regulator resistance becomes zero in less than two cycles, or about $^1/_{30}$ second after the load change occurs. This rapid response is made possible by new materials and a superior mechanical design which permit reduction of the inertia of the moving parts and of the inductance of the regulator coil.

A third requirement of any regulating mechanism, and by no means the least important, is that it be simple and highly reliable, and that it require a minimum of maintenance. Figures 2 and 3 show that these considerations have also had their part in the evolution of the modern regulator.

Regulating System Assumed for Analysis

Figure 4 is a schematic drawing of the regulator and a connection diagram for the typical regulating system which is analyzed in this paper. The regulator operates by controlling the resistance in the shunt field circuit of the exciter in the following manner: The resistance presented by the regulator elements is varied in response to movement of the regulator armature. The position of the armature is determined by the balance between the torque of the spring and the electrical torque of the magnet. The current in the magnet coil comes from the a-c generator voltage through a rectifier. If the voltage drops, the current in the coil decreases, the armature changes position, and lowers the resistance in the exciter field until the voltage comes back to normal. The stabilizer, a simple iron-core transformer with an air gap, impresses in the regulator-coil circuit a transient voltage depending upon the change in generator field voltage. This transient voltage makes the regulator anticipate slightly the return of the alternating voltage to normal and thereby decreases the tendency to overswing or hunt.

Method of Analysis

The tensor method of analysis, which is particularly well adapted for problems

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W. K. Boice is electrical engineer in the industrial department, S. B. Crary is in the engineering division of the central-station department, Gabriel Kron is consulting engineer, and L. W. Thompson is in the voltage-regulator engineering department, General Electric Company, Schenectady, N. Y.

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pairs, as for example replacement of broken leads and caps to which reference is made in the paper.

The return on the investment in testing, and economic justification for such expenditure is probably questionable at least until the reliability of the testing methods is better established, and the loss of possible further years of service from unnecessarily rejected arresters is fully evaluated. The economic justification would also be questionable if repetitions of tests and cumulative expense for removals from service and testing over frequent intervals were necessary on the same arrester. More lasting economies would favor substitution by new arresters of modern design.

Herman Halperin: E. J. Allen has presented a critical discussion which should be stimulating to those interested in testing distribution arresters. Part of his remarks reiterate points made in the paper on shortcomings of the various simple tests in use, but in some places the criticism seems unwarranted and unsubstantiated by the experiences of utilities who have conducted tests. It is not certain that even the operating-duty test, which he emphasizes as all-important, will always eliminate an arrester which is in questionable physical condition. Detailed dissection of used arresters by utilities has disclosed a general correlation between observations and results of the tests which are made inexpensively with relatively simple apparatus. As to damage to the arresters by repeated 60-cycle tests, which should not occur with properly designed test circuits, the practices of utilities should not usually result in a given arrester's being installed and removed more than twice. The increasing number of utilities which find it desirable to test used arresters appears to justify the economic value of that practice. Further service experience with used arresters which were tested will be of immediate interest.

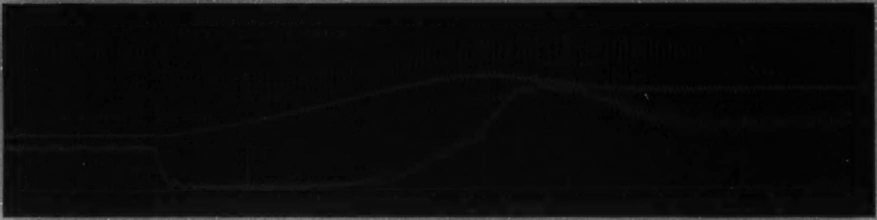

Figure 1. Oscillogram showing response of direct-acting regulator

"Exc. Volts" is measured across the exciter armature

"Reg. Volts" is measured across the regulator rheostat

Alternator originally at no load; 19.0 kva, 0.3 power factor load applied

involving many elements, was used to set up the differential equations for small variations in the voltages, currents, torques, and angles of the regulating system. These equations were then examined to determine whether or not the variations tend to die away after a slight disturbance or to become larger with increasing time. In stable systems, the variations caused by any slight disturbance diminish to zero. But if any of the variations have a tendency to become larger, the system will not be in stable equilibrium at the operating point in question. This unstable condition is manifested by continued hunting or oscillating.

Any tendency for the variations to increase is evidenced by the existence of a positive real part in one or more of the roots of the characteristic denominator of the differential equations. Such a positive real part may be detected by the use of Routh's criterion.[1] In this manner regions of stable and unstable equilibrium were determined and factors which make for stability or instability investigated quantitatively.

General Conclusions

The more important results of this analysis can be briefly summarized as follows: It was found, for the typical system studied, that

1. In order to improve regulation

(*a*). The rate of change of regulator stack resistance with change in displacement should be large.

(*b*). The rate of change of regulator electrical torque with change in coil current should be large.

(*c*). The difference between the rates of change of electrical torque and of mechanical torque with respect to regulator-armature mechanical displacement should be small.

(*d*). The difference between the rates of change of exciter armature voltage and of exciter field circuit *IR* drop with respect to exciter field current should be small.

(*e*). The rate of change of regulator stack resistance with change in current should be negative.

2. In order to improve the stability

(*a*). The factors listed in conclusion 1 should be changed in the opposite direction to that required for improving the regulation.

(*b*). The moment of inertia of all the movable parts of the regulator should be reduced.

(*c*). The exciter field inductance should be increased.

3. With reduction in the moment of inertia, the factors which influence the regulation have less influence on the stability; therefore they can be changed in the direction to improve the regulation without impairing the stability as much as would be the case with a higher moment of inertia.

4. In the region of low exciter saturation, perfect regulation, or even a slightly rising voltage characteristic with load can be obtained with stability.

The analytical results are in agreement with present laboratory and field experience. Accordingly the method presented is a dependable means by which the effects of the various influencing factors on a regulating system may be analyzed and knowledge of regulating systems be extended.

Discussion

GROUPING OF CONSTANTS

In analyzing the regulating system it was possible to group many of the circuit constants together in order to simplify the analysis and help provide a clear physical concept. Two important factors in the operation of the system, both from a stability and regulation standpoint, are designated by the terms R_D and X_D, which have been called exciter stability factor and regulator stability factor, respectively.

R_D is the difference between the effective resistance of the exciter field circuit to small current changes, and the ratio of the change in generated voltage of the exciter to the corresponding small change in exciter field current. Referring to figure 5, *OS* is the exciter saturation curve measuring the variation of exciter generated voltage with field current, and the line *OA* is the exciter-field-circuit resistance line for a particular position of the regulator. The curvature of this line comes from the nonlinear characteristic of the resistance used in the regulator. The line ss' is tangent to the curve *OS* at the operating point *P*; aa' is tangent to the curve *OA*. The greater the difference between the slopes of aa' and ss', the less susceptible the exciter voltage is to slight changes in resistance. Accordingly, the difference of these slopes can be considered as a measure of the stability of the exciter. Such a concept is well-known.

The characteristic of the regulator resistance is such that its effective resistance decreases with increase in current. As shown by figure 5, this characteristic tends to make curve *OA* ap-

Figure 2. Direct-acting voltage regulator with enclosing case

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[1] For all numbered references, see list at end of paper.

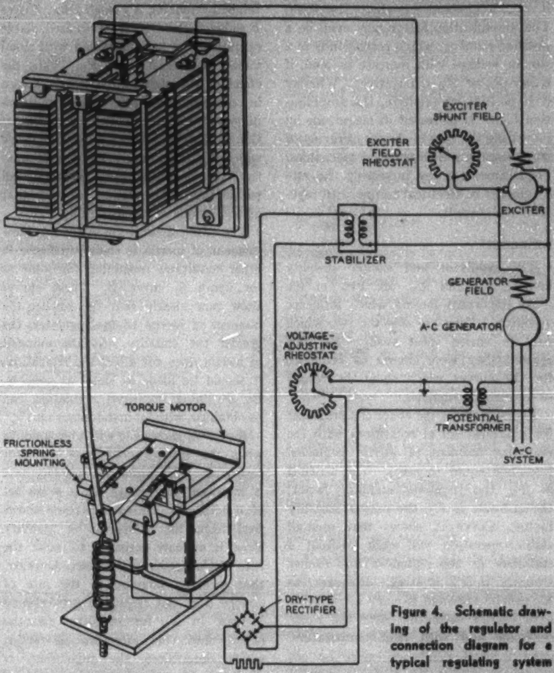


Figure 4. Schematic drawing of the regulator and connection diagram for a typical regulating system

proach *OS* in shape thereby reducing the difference between the slopes of aa' and ss', and making this difference more uniform over the operating range. If the resistance curve is sufficiently nonlinear, the difference of the slopes may be slightly negative, $-R_D$. Although the exciter by itself may be unstable under this condition, the corrective action of the regulator and stabilizer can overcome this instability and produce stable operation of the system. In fact, a uniformly low or negative value of R_D is desirable as a means of improving the regulation.

X_D, the regulator stability factor, is the difference between the rate of change of spring torque acting on the regulator armature and the corresponding rate of change of electrical torque for a given small change in mechanical displacement. See figure 6.

If the regulator armature is to be at equilibrium in any particular position, the electrical torque must just overcome the spring torque at that position. Now if the armature is moved by some external torque in the same direction as the magnetic torque, the spring will be further stressed and its force will increase slightly. Even though the alternating voltage from which the coil is magnetized remains constant during this process, the magnetic force may increase or decrease depending on the shape of the armature and pole tips. If the magnetic force decreases, or, as in figure 6, increases less rapidly than the opposing spring force, the armature will be urged back to its initial position. Such an equilibrium is commonly called stable equilibrium. But if the magnetic force increases more rapidly than the spring force, the armature will be urged away from its initial position, and the operating point will be a point of unstable equilibrium. Thus X_D, the difference between the rate of increase of the spring torque and of the electrical torque, is a measure of the stability of the regulator armature.

Figure 3. Direct-acting voltage regulator with enclosing case removed, showing operating mechanism

The more positive this quantity, X_D, becomes, the less sensitive the armature will be to a given change in electrical torque. Therefore X_D is inversely proportional to the amplification of the mechanical part of the regulator just as R_D is inversely proportional to the amplification of the exciter.

REGULATION

The regulation of a system, which is a measure of its ability to hold the regulated quantity at a constant value, is a function of what has been termed in the analysis the over-all amplification factor. This factor can be defined as the ratio of the change in the terminal voltage of the a-c machine to an arbitrary small change in the alternating voltage applied to the regulator. It normally has a negative sign, for when the voltage applied to the regulator is high, the regulator tends to reduce the alternator terminal voltage.

The total amplification factor of the system is the product of the amplification factors of the various elements which may be considered as constituting stages of amplification. This is readily visualized by consideration of figure 7 which shows schematically the different elements of the system as stages of amplification and the corresponding mathematical expressions which define their respective amplification factors.

The amplification may be very large, theoretically equal to infinity, if either the regulator stability factor, X_D, or the exciter stability factor, R_D, are equal to zero. This indicates that flat regulation can be obtained if the exciter or regulator is operated at a point where it would approach instability by itself. The amplification factor may even be a positive number, which corresponds to a rise in voltage with increase in load, if either R_D or X_D is negative. Whether it is positive or negative, the amplification factor is increased in magnitude by increasing the rate of change of regulator stack resistance with armature mechanical displacement, or by increasing the rate of change of electrical torque with regulator-coil current.

STABILITY

The regulator and exciter stability factors, R_D and X_D, are two of the most important factors which influence stability. They are also the two which can be varied most easily. For this reason, they were chosen as ordinates for the curves which show the results of the stability analysis.

Figure 8 shows the effect of a stabilizer for a typical set of conditions with the regulator moment of inertia neglected (assumed equal to zero). The abscissa is X_D, the regulator stability factor; the ordinate is R_D, the exciter stability factor. Curve *A* shows that over-all stable operation will exist without a stabilizer in the region of low exciter stability if X_D is large. However, as R_D can be expected to vary appreciably with exciter saturation over the range of loads and with field temperature, good over-all regulation must be obtained by making X_D small also. Curve *B* shows that if a stabilizer is used, stable operation can be maintained with small values of both X_D and R_D. Thus the effect of the stabilizer is clearly shown in compensating for regulator instability and in making possible good regulation with stable operation over the range which R_D may take. By design practice, the operating point is kept well within the stable regions in order that all oscillations will decay rapidly.

The effect of variation in the regulator moment of inertia is shown in figure 9, other conditions remaining the same as for figure 8, curve *B*. These curves show very clearly that the smaller the moment of inertia of the regulator, the greater the stability. As the moment of inertia does not affect the regulation, it should be made as small as possible. By so doing improved regulation can be obtained, without instability.

Figure 10 shows the effect on stability when the rate of change of regulator resistance with armature displacement is increased four times for a given set of conditions. This comparison shows that from the viewpoint of stability alone it appears desirable to make the rate of change small. Notice, however, that a given increase in the rate of change does not require a proportional increase in X_D for stability. On the other hand, the amplification factor, which determines the regulation, is directly proportional to the ratio of the rate of change of resistance to X_D. Therefore, a net improvement in regulation can be obtained without affecting the stability by increasing both the rate of change of regulator resistance with displacement and the value of X_D.

Figure 11 shows the effect of changing the exciter-field inductance, all other factors remaining the same. These curves indicate that a system is more likely to be stable if the exciter-field time constant is large, but this condition is contrary to the requirement of rapid response. Consequently, increase of exciter-field time constant is not in general a practical method for improving stability.

Analysis

ASSUMPTIONS

In order to adapt this investigation to mathematical analysis certain assumptions were made, the most important of which are the following:

1. Displacements are of small magnitude.
2. The alternator speed is constant.
3. The alternator load is purely reactive. Reactive loads usually cause the most severe regulation.
4. Voltages due to rates of change of flux linkages are small compared to voltages due to rotation of the machine.
5. The exciter speed is constant.
6. Friction in the movable parts of the regulator is negligible. Friction is kept as small as possible on most regulators for mechanical reasons.
7. At the operating point the rectifier develops changes in direct voltage proportional to changes in the fundamental alternating voltage less a linear resistance drop.

Both regulation and stability were studied on the basis of the above assumptions. This work indicated that it was reasonable to make further assumptions in the study of stability. These further assumptions were:

1. The alternator is at no load.
2. Stabilizer magnetizing current is negligible.

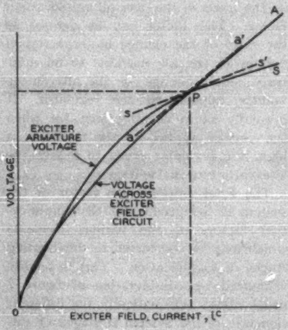


Figure 5 (left). Illustration of R_D, exciter stability factor

R_D = slope aa' minus slope ss'

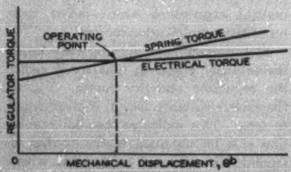
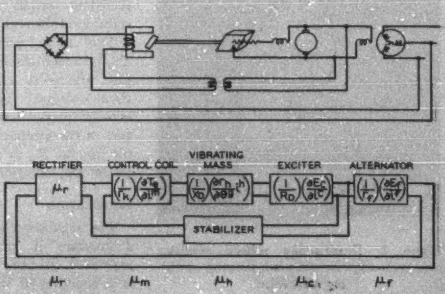


Figure 6 (left). Illustration of X_D, regulator stability factor

X_D = slope of spring torque curve minus slope of electrical torque curve

Figure 7. Stages of amplification and over-all amplification factor

$$\mu_o = \frac{\mu_r\left(\frac{\partial T_e}{\partial i^m}\right)\left(\frac{\partial r_h}{\partial \theta^e} i^h\right)\left(\frac{\partial E_c}{\partial i^c}\right)\left(\frac{\partial E_f}{\partial i^f}\right)}{r_k X_D R_D r_f}$$


RECTIFIER
CONTROL COIL
VIBRATING
MASS
EXCITER
ALTERNATOR
μ_r
$\left(\frac{1}{r_k}\right)\left(\frac{\partial T_e}{\partial i^m}\right)$
$\left(\frac{1}{X_D}\right)\left(\frac{\partial r_h}{\partial \theta^e} i^h\right)$
$\left(\frac{1}{R_D}\right)\left(\frac{\partial E_c}{\partial i^c}\right)$
$\left(\frac{1}{r_f}\right)\left(\frac{\partial E_f}{\partial i^f}\right)$
STABILIZER
μ_r
μ_m
μ_h
μ_c
μ_f

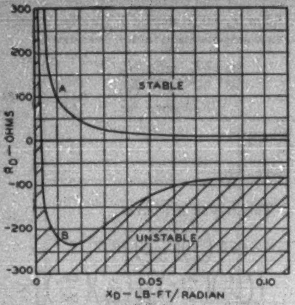


Figure 8. Excitation-system stability characteristics showing effect of stabilizer. Regulator moment of inertia neglected (assumed equal to zero)

Alternator rating 37.5 kva, 110 volts, 60 cycles

A—No stabilizer *B*—With stabilizer

3. Exciter armature resistance is negligible.
4. Exciter armature inductance is negligible.

The point at which these additional assumptions are introduced is indicated in the analysis.

Nomenclature

Particular values of the following quantities are identified by appropriate superscripts.

i—current
θ—angle of displacement from a given position

Particular values of the following quantities are identified by appropriate subscripts.

m—moment of inertia
e—voltage
r—resistance
ψ—flux linkages
$L = \partial\psi/\partial i$—incremental self-inductance
$M = \partial\psi/\partial i$—incremental mutual inductance
E—generated voltage due to rotation relative to flux in line with field poles
μ—amplification factor of an element of the regulating system

The following symbols are used as subscripts or superscripts.

m—regulator coil
h—regulator rheostat
b—regulator armature
g—rheostat operating arm
c—exciter field
a—exciter armature
f—alternator field
d—alternator direct axis[3]
q—alternator quadrature axis[3]
r—rectifier
p—primary of stabilizer (connected to exciter armature)
s—secondary of stabilizer (connected into regulator circuit)
k—regulator-coil circuit (rectifier, stabilizer secondary, and regulator coil) under steady-state conditions
T—regulator-coil circuit of the interconnected system under oscillatory conditions

When not identified by a subscript:

$m = m_b + m_g$ = total moment of inertia of movable parts of the regulator

The following quantities also appear in the analysis:

T_e—electrical torque on regulator armature
T_Σ—spring torque on regulator armature
μ_o—over-all amplification factor
n—ratio of $\dfrac{\text{primary turns}}{\text{secondary turns}}$ of stabilizer

It should be noted that resistances, inductances, and flux linkages of the stabilizer are referred to the primary.

$$R_D = -\frac{\partial E_c}{\partial i^c} + r_h + r_c + \frac{\partial r_h}{\partial i^h} i^c + r_a,$$

the exciter stability factor.

$$X_D = \frac{\partial T_\Sigma}{\partial \theta^b} - \frac{\partial T_e}{\partial \theta^b},$$

the regulator stability factor.

In the tensor notation, each unit vector is designated in bold-face type by the same symbol which is used as a superscript for the current (or angle) of the circuit to which the unit vector applies.

A prime (′) identifies terms applying to the system after interconnection.

A double prime (″) identifies terms applying to the interconnected system after introduction of the assumption that stabilizer magnetizing current is negligible.

Derivation of Equations

The following presents the development of the equations from which the performance of the system (figure 13) was determined. Using the tensor method of attack,[3] the first step was to set down the equations for the individual parts of the system shown in figure 12; second, by means of the connection tensor, the equations applying to the interconnected system were

Figure 9. Excitation-system stability characteristics showing effect of change in regulator moment of inertia. Effect of stabilizer included

Alternator rating 37.5 kva, 110 volts, 60 cycles

A—$m = 0$
B—$m = 7.37 \times 10^{-6}$ pound-feet-seconds²/radian
C—$m = 73.7 \times 10^{-6}$ pound-feet-seconds²/radian

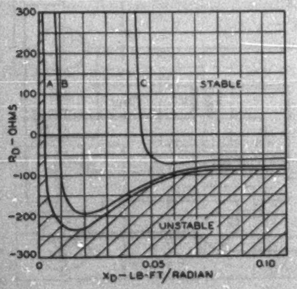


Figure 10. Excitation-system stability characteristics showing effect of change in the rate of change of regulator rheostat resistance with regulator displacement

Alternator rating 37.5 kva, 110 volts, 60 cycles

A—$\dfrac{\partial r_h}{\partial \theta^g} i^c = 40.7$ volts per radian

B—$\dfrac{\partial r_h}{\partial \theta^g} i^c = 162.5$ volts per radian

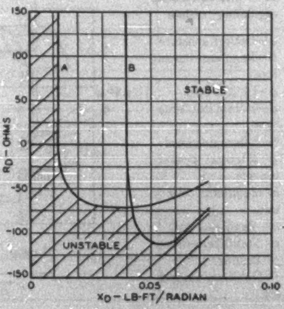
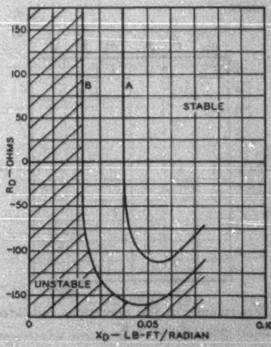


Figure 11. Excitation-system stability characteristics showing effect of change in exciter field inductance

Alternator rating 37.5 kva, 110 volts, 60 cycles

A—$L_c = 30$ henries *B*—$L_c = 60$ henries



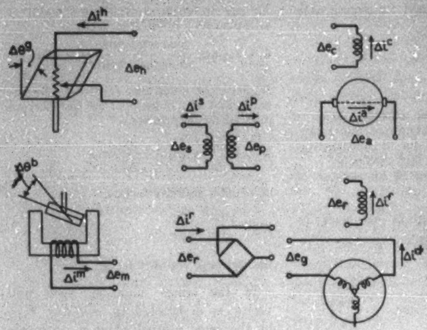


Figure 12. Component parts of regulating system before interconnection

a—Exciter armature
b—Regulator armature
c—Exciter field
d—Alternator armature
f—Alternator field
g—Rheostat operating arm
h—Regulator rheostat
m—Regulator coil
p—Stabilizer primary
r—Rectifier
s—Stabilizer secondary

found; third, the determinant of the equations for the currents and angular displacements was found; and fourth, Routh's criterion[1] was applied in order to determine the necessary conditions for stable operation. Routh's criterion is an algebraic rule, the application of which is discussed later in this analysis.

1. *General Equations Before the Interconnection*

The equations for the component parts may be summarized by the equation of small changes

$$\Delta e = z \cdot \Delta i \tag{1}$$

where the impedance tensor, z, of the component parts is given in equation 2.

2. *Equations for the Connected System*

When the parts are connected together as shown in figure 13, the effect of the interconnection may be represented by transformation of the tensor, z, according to the equation

$$z' = C_t \cdot z \cdot C \tag{3}$$

where

$$C = \begin{array}{c|cccccc|} & c' & f' & p' & m' & b' & d' \\ \hline m & & & & 1 & & \\ b & & & & & 1 & \\ h & 1 & & & & & \\ g & & & & & 1 & \\ c & 1 & & & & & \\ a & 1 & 1 & 1 & & & \\ f & & 1 & & & & \\ r & & & & 1 & & \\ d & & & & & & 1 \\ p & & & 1 & & & \\ s & & & & -1 & & \\ \hline \end{array} \tag{4}$$

Performance of the operations indicated in (3) yields equation 5.

3. *Regulation*

If the regulator were inoperative, a change, Δe_q, in alternator terminal voltage would not result in any change, Δi^f, in alternator field current or corresponding change $(\partial E_f/\partial i^f)\ \Delta i^f$, in internal generated voltage. The effectiveness of the regulator may be expressed in terms of its ability to cause a change $(\partial E_f/\partial i^f)\ \Delta i^f$, in response to a change, Δe_q.

At steady state ($p = 0$), solution of equation 5 yields

$$\frac{\partial E_f}{\partial i^f}\Delta i^f = \mu_o \Delta_{eq} \tag{6}$$

where the steady-state amplification factor,

$$\mu_o = \frac{-\mu_r \dfrac{\partial E_f}{\partial i^f}\cdot\dfrac{\partial T_e}{\partial i^m}\left(\dfrac{\partial E_c}{\partial i^c} - r_a\right)\dfrac{\partial r_h}{\partial \theta^g}i^c}{r_m + r_r + \dfrac{r_s}{n^2}\Big)X_D\left[R_D + r_a\left(1+\dfrac{r_f}{r_p}\right)\dfrac{\dfrac{\partial E_c}{\partial i^c} - r_a}{r_f + r_a\left(1+\dfrac{r_f}{r_p}\right)}\right]\left[r_f + r_a\left(1+\dfrac{r_f}{r_p}\right)\right]} \tag{7}$$

For $r_a = 0$, equation 7 for the steady-state amplification factor, becomes

$$\mu_o = -\frac{\dfrac{\partial r_h}{\partial \theta^g}i^c\dfrac{\partial T_e}{\partial i^m}\mu_r\dfrac{\partial E_f}{\partial i^f}\dfrac{\partial E_c}{\partial i^c}}{R_D X_D r_f r_k} \tag{8}$$

where

$$r_k = r_m + r_r + \frac{r_s}{n^2}$$

This may be written as

$$\mu_o = -[\mu_r]\left[\left(\frac{1}{r_k}\right)\left(\frac{\partial T_e}{\partial i^m}\right)\right]\times \left[\left(\frac{1}{X_D}\right)\left(\frac{\partial r_h}{\partial \theta^g}i^c\right)\right]\left[\left(\frac{1}{R_D}\right)\left(\frac{\partial E_c}{\partial i^c}\right)\right]\times \left[\left(\frac{1}{r_f}\right)\left(\frac{\partial E_f}{\partial i^f}\right)\right] \tag{9}$$

$$= -\mu_r\mu_m\mu_h\mu_c\mu_f$$

where the μ's are the amplification factors of the various stages (see figure 7).

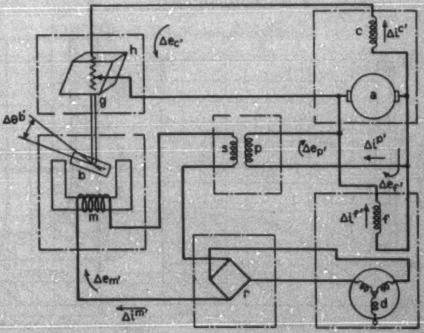
It may be seen that, in general, each amplification factor consists of a self-admittance times a mutual impedance. The effect on regulation of changes in any of the system constants can thus readily be visualized in terms of their effect on these admittances and impedances.

4. *Stability*

Equation 5 defines the system of differential equations which apply to the departures, Δi, from steady-state values of the currents (and angle) of the connected system after any slight disturbance. The solutions of this system

Figure 13. Diagram of interconnected regulating system

a—Exciter armature
b—Regulator armature
c—Exciter field
d—Alternator armature
f—Alternator field
g—Rheostat operating arm
h—Regulator rheostat
m—Regulator coil
p—Stabilizer primary
r—Rectifier
s—Stabilizer secondary



$$\begin{array}{c|cc|cc|cc|ccc|cc}
 & m & b & h & g & c & a & f & r & d & p & s \\
\hline
m & r_m+L_m p & \frac{\partial\psi_m}{\partial\theta^b}p & & & & & & & & & \\
b & \frac{\partial T_e}{\partial i^m} & \frac{\partial T_e}{\partial\theta^b}-\frac{\partial T_Z}{\partial\theta^b}-m_b p^2 & & & & & & & & & \\
\hline
h & & & r_h+\frac{\partial r_h}{\partial i^h}i^h & \frac{\partial r_h}{\partial\theta^g}i^h & & & & & & & \\
g & & & 0 & -m_g p^2 & & & & & & & \\
\hline
c & & & & & r_c+L_c p & 0 & & & & & \\
s=a & & & & & -\frac{\partial E_c}{\partial i^c} & r_a+L_a p & & & & & \\
\hline
f & & & & & & & r_f+L_f p & 0 & M_{fd}p & & \\
r & & & & & & & -\mu_r\frac{\partial E_f}{\partial i^f} & r_r & -\mu_r\frac{\partial E_f}{\partial i^d} & & \\
d & & & & & & & -\frac{\partial E_f}{\partial i^f} & 0 & -\frac{\partial E_f}{\partial i^d} & & \\
\hline
p & & & & & & & & & & r_p+L_p p & \frac{1}{n}M_{sp}p \\
s & & & & & & & & & & \frac{1}{n}M_{sp}p & \frac{1}{n^2}r_s+\frac{1}{n^2}L_s p
\end{array} \qquad (2)$$

of equations express the values of these departures as functions of time. These functions of time consist of the real parts of a series of terms of the form

$$Ae^{\alpha t} \qquad (10)$$

where A is an arbitrary constant and α is in general a complex number. α is always a root of the algebraic equation in powers of p formed by setting equal to zero the determinant of the system of equations defined by equation 5. This determinant is

$$D=a_6p^6+a_5p^5+a_4p^4+a_3p^3+a_2p^2+a_1p+a_0 \qquad (11)$$

where the a's are determined by the constants of the system.

If the real part of the complex number, α, is positive, the term, $Ae^{\alpha t}$, increases with increasing time. This means that after a slight disturbance the currents do not tend to return to their steady-state values, but increase until the departures, Δi, are so large that they become limited by saturation or some other change in the system constants. Hence, the system is defined as being unstable if the real part of any root, α, of the determinant (11) is positive.

The existence of a root having a positive real part may be detected by the use of Routh's criterion without actual solution for the roots of the determinant (11). Routh's criterion depends upon the relative values of the a's of (11) and, in general, requires the substitution of numerical values for the system constants which determine the values of the a's. The effect of varying system constants may be found by trial.

USE OF SIMPLIFYING ASSUMPTIONS

It has been found possible to simplify the analysis of stability by making certain assumptions which appear reasonable.

(*a*). The alternator load current, i^d, may be assumed to be zero, since experience has shown that in general the system is more stable when the generator is loaded than at no load.

(*b*). Calculations on stability of typical systems have indicated that it is justifiable

$$\begin{array}{c|c|c|c|c|c|c}
 & c' & f' & p' & m' & b' & d' \\
\hline
c' & r_h+\frac{\partial r_h}{\partial i^h}i^h+r_c+r_a-\frac{\partial E_c}{\partial i^c}+(L_a+L_c)p & r_a+L_a p & r_a+L_a p & 0 & \frac{\partial r_h}{\partial\theta^g}i^c & 0 \\
\hline
f' & r_a+L_a p-\frac{\partial E_c}{\partial i^c} & r_a+r_f+(L_a+L_f)p & r_a+L_a p & 0 & 0 & M_{fd}p \\
\hline
s'=p' & r_a+L_a p-\frac{\partial E_c}{\partial i^c} & r_a+L_a p & r_a+r_p+(L_a+L_p)p & -\frac{1}{n}M_{sp}p & 0 & 0 \\
\hline
m' & 0 & -\mu_r\frac{\partial E_f}{\partial i^f} & -\frac{1}{n}M_{sp}p & r_m+r_r+\frac{1}{n^2}r_s+\left(L_m+\frac{1}{n^2}L_s\right)p & \frac{\partial\psi_m}{\partial\theta^b}p & -\mu_r\frac{\partial E_f}{\partial i^d} \\
\hline
b' & 0 & 0 & 0 & \frac{\partial T_e}{\partial i^m} & \frac{\partial T_e}{\partial\theta^b}-\frac{\partial T_Z}{\partial\theta^b}-(m_b+m_g)p^2 & 0 \\
\hline
d' & 0 & -\frac{\partial E_f}{\partial i^f} & 0 & 0 & 0 & -\frac{\partial E_f}{\partial i^d}
\end{array} \qquad (5)$$

to neglect stabilizer magnetizing current. The effect of this assumption may be represented by the equation

$$z'' = C_t' \cdot z' \cdot C' \tag{12}$$

where

$$C' = \begin{array}{c|cccc} & c'' & f'' & m'' & b'' \\ \hline c' & 1 & & & \\ f' & & 1 & & \\ p' & & & 1/n & \\ m' & & & 1 & \\ b' & & & & 1 \end{array} \tag{13}$$

(*c*). Calculations for typical systems have indicated that it is justifiable to make the further simplifying assumptions that exciter armature resistance, r_a, and exciter armature inductance, L_a, are zero.

On the basis of the assumptions of (*a*), (*b*), and (*c*), equation 5 becomes

$$z'' = \begin{array}{c|cccc} & c'' & f'' & m'' & b'' \\ \hline c'' & R_D+L_cp & 0 & 0 & \frac{\partial r_h}{\partial \theta^b} i^c \\ f'' & -\frac{\partial E_c}{\partial i^c} & r_f+L_fp & 0 & 0 \\ m'' & -\frac{1}{n}\frac{\partial E_c}{\partial i^c} & -\mu_r\frac{\partial E_f}{\partial i^f} & r_T+L_Tp & \frac{\partial \psi_m}{\partial \theta^b} p \\ b'' & 0 & 0 & \frac{\partial T_e}{\partial i^m} & -X_D-mp^2 \end{array} \tag{14}$$

where

$$r_T = \frac{r_p+r_s}{n^2} + r_m + r_r$$

$$L_T = \frac{\partial \psi_m}{\partial i^m} + \frac{1}{n^2}(L_a+L_s+L_p-2M_{sp})$$

The determinant of equation 14 is

$$\text{Det.} = b_5p^5+b_4p^4+b_3p^3+b_2p^2+b_1p+b_0 \tag{15}$$

where the *b*'s are functions of the resistances, inductances, and other physical constants of the system.

If the physical constants of a system are known, stability may be studied by applying Routh's criterion to the *b*'s of (15) in the same manner in which it was applied to the *a*'s of (11). For the special case, *m* is zero, $b_5=0$, and $b_4=0$.

From calculations of this sort the stability characteristics shown in figures 8, 9, 10, and 11 were determined.

Summary

The major factors involved in the design and application of the direct-acting regulator and their relation to each other have been discussed. An outline has been presented of the mathematical method by which these relations were derived. Finally it has been shown that the modern direct-acting regulator, when properly designed for the system in which it must function, satisfies all the fundamental requirements of a regulating system.

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The Application of Tensors to the Analysis of Rotating Electrical Machinery, Parts I–XVI (a book), Gabriel Kron. *General Electric Review*, 1938.

Discussion

F. E. Crever (General Electric Company, Schenectady, N. Y.): The system analyzed in this paper is by far the most usual in which the direct-acting regulator is employed and hence the very thorough analysis made is of particular interest. The use of the direct-acting regulator is of course not limited to such a system. A great many of the direct-acting-regulator applications have been made to regulate not only voltage but current, speed, torque, tension, and other quantities both in the central-station field and in industrial plants. The outstanding problem in such applications is to obtain stability and good regulation together. As the paper points out, a great many of the factors that favorably influence regulation have a tendency to decrease the stability. In general the method of analysis employed by the authors can be applied to any regulating system. The tensor method is particularly applicable in that it allows the equations of the component parts of the system to be set up and then to be readily combined to represent a complex system. The characteristic denominator of the system of differential equations is usually of a high degree so that simplifying assumptions are necessary to obtain a ready solution. The authors have been very careful in the assumptions made, which accounts for the good agreement between the analysis and practical results.

C. Concordia (General Electric Company, Schenectady, N. Y.): There are two points in this paper which I should like to amplify.

First we note that it is stated that the exciter-field inductance should be increased to improve stability. Since some investigators have concluded that all time lags in the amplification stages should be as small as possible for stability, it is of interest to comment on this conclusion. Study of the performance equations shows that the reason a certain amount of lag may be desirable is the presence of mutual terms (that is, mutual reactions between various stages) in addition to the components of the amplification factor, or the presence of second order lags (for example, the regulator-armature inertia). The mutual reactions may arise either from the stabilizer or from inherent mutual effects.

These considerations bring out the importance of studying the actual system equations, as was done in this paper, rather than an idealized system in which some of the mutual effects may be left out.

A second point to be commented on is the effect of friction in the regulator armature. It is evident that friction must be small if the regulator stability factor X_D is to be made very small. Moreover, calculations show that such friction (treated as viscous damping) is detrimental to stability and thus indicate an additional advantage of minimizing the regulator friction. Figure 4 shows one well-known mounting for accomplishing this result. Regulator friction was introduced into a system having nearly the constants of figure 9 of the paper, with results as follows:

D	M	R_D	X_D
0	0	0	0.003
0.0022	0	0	0.026
0	0.0000737	0	0.028
0.0022	0.0000737	0	0.031

where

D	is the damping coefficient and $D = 0.0022$ is about one-half the value required for critical damping with $X_D = 0.09$.
M	is the regulator-armature inertia.
R_D	is the exciter stability factor, taken as zero in this case.
X_D	is the minimum value of regulator stability factor required for stable operation.

C. E. Valentine (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have developed an interesting analysis of a regulating system. Except for the mathematical treatment by means of the tensor method, which appears more complicated than simple to many engineers, the assumptions made and conclusions reached are, in general, quite in line with operating experience.

Of particular interest is one of the general conclusions (2*b*) that in order to improve stability, the moment of inertia of all the movable parts of the regulator should be reduced. This has been recognized for some time as a fundamental feature of a direct or indirect-acting type of regulator. In the "Silverstat" regulator described in AIEE Paper 39-91 (AIEE Transactions, volume 58, 1939, pages 838–44) a lightweight quick-acting moving arm was used even in the earliest design.

This same feature has been used consistently on all later designs which have also achieved an enviable record of performance both on single generators and generators in parallel.

The adoption of the crossed-leaf type spring mounting (also described in the earlier paper) for the moving element and the extension of this idea in the form of a spring for actuating the variable-resistance member, as shown schematically in figure 4, is a desirable improvement over the bearings and linkage previously used. The crossed-spring type of hinge has a background of most satisfactory performance as the supporting means for the moving arm on the Silverstat generator voltage regulators where it has been used from the start.

K. A. Oplinger (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have pointed out the possibility of using a nonlinear resistance characteristic in a regulator as a means of improving regulation. It would appear difficult to make this nonlinear characteristic such that the slope of the exciter-field-circuit resistance line always approaches the slope of the exciter saturation curve. I would like to ask if it has been practical to do this for all types of exciters, and if so, what actual improvement in regulation is secured taking into account that R_D will change appreciably with exciter saturation and field temperature?

The general conclusions of the authors under (2*c*) state that the exciter field inductance should be increased in order to improve stability. Since this conclusion is not in agreement with experimental results, could not this conclusion be in error due to assumptions made regarding the size of the stabilizer?

C. R. Hanna (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The accuracy of a voltage regulator is determined by the over-all amplification factor of the system which is the ratio of the change in generated voltage to the deviation in terminal voltage. The authors give the equation for the amplification of their regulator at the bottom of figure 7 of their paper. It would have been more instructive if data on system stability had been given in terms of the amplification instead of just the X_D and R_D factors which as indicated in their equation, only partially determine the amplification.

The curves given show only the limiting conditions for stable operation. No computations are made of damping conditions corresponding to points in the stable range. It is necessary to have large decrements of any system oscillations so that disturbances due to load changes, etc., will not cause continuous swinging of the voltage. Points in the stable range may not be selected indiscriminately because the damping may still be too low for practical operation.

In the interests of simplifying a problem as involved as that of voltage regulation, many variables have to be eliminated or assumed to have a small influence. The authors make one assumption, however, which is difficult to justify; namely, that the stabilizer magnetizing current is zero. This means infinite self- and mutual inductance and, therefore, a transformer of infinite size. Such an assumption completely ignores the problem of determining just how large the stabilizer should be for a good practical result.

The mathematical processes used in the paper could have been employed to yield much more design information than is given. One is somewhat disappointed that the numerical results do not include such pertinent information as rates of decay of oscillations or methods of predicting transformer size for a given system. As shown in the recent paper by the writer and his collaborators on "Recent Developments in Voltage Regulation" (AIEE TRANSACTIONS, volume 58, 1939, pages 838–44) these can be computed by less involved processes than the method of tensors as used by the authors.

W. K. Boice, S. B. Crary, Gabriel Kron, and L. W. Thompson: Mr. Hanna suggests that simpler methods could be used which would yield more easily the damping factors. The equations in the paper can be simplified to almost any extent by making assumptions or neglecting the effect of certain factors so that they will yield the damping factors directly, or with little numerical work. This is usually accomplished at a sacrifice of accuracy and, what is more important, at a sacrifice of the ability to determine the effect of factors which influence the regulator and excitation-system design. In many cases, it is advisable to make simplifying assumptions only after a more general approach has been made. In this way the simplifying assumptions may be tested for validity.

Mr. Hanna questions the validity of neglecting stabilizer magnetizing current. The calculations which we have made for a typical system have indicated that this could be done with small error. However, the influence of stabilizer magnetizing current was studied and can be further studied by using the corresponding more general equations presented in the paper.

Mr. Oplinger questions the value of the nonlinear stack-resistance characteristic in that it would be difficult in practice to have the exciter characteristic slope always correspond to the field circuit resistance line. It is not necessary and is practically impossible that these characteristics correspond for all loads. However, the nonlinear stack resistance for any given load condition results in better regulation than a linear-characteristic regulator-stack resistance. It is in the direction to improve the regulation and is accordingly desirable from this standpoint.

For the typical system which we have studied, the analysis showed that an increase in field inductance improved stability. This would not necessarily be true for any system, particularly one whose constants or connections deviated materially from those chosen for study. However, experience with direct-acting regulators over a period of years has indicated for a wide range of typical systems that increased exciter field inductance is in the direction to improve stability.

Mr. Concordia's comments and contributions on regulator friction are appreciated and can be considered a valuable addition to the material presented in the paper.

We are pleased to learn from Mr. Valentine that he has also found the crossed-spring type of hinge and low moment of inertia to be most satisfactory. This type of design has definite advantages.

We wish to thank Mr. Crever for emphasizing that the method presented in the paper may be used for other types of regulating systems.

Signal System, Interlocking Plants, and Automatic Train Control on the San Francisco-Oakland Bay Bridge Railway

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Synopsis: This paper briefly describes the nature and purpose of the various groups of railway signal and interlocking equipment operated in connection with railway service over the San Francisco-Oakland Bay Bridge. Following this description is an explanation of the method used in determining the lengths of signal blocks and calculating safe braking distances throughout the project. It is hoped that the presentation of this paper will lead to further discussion and research concerning the braking of trains of this type.

THIS eight-mile-long double-track railway with its "train-a-minute" operation called for the most complete and modern system of block signalling and train control, and required the maxmium facility for operating the switches and wayside signals at the San Francisco terminal, at the Oakland yard, and at the junction of the two railways. The means by which these extreme requirements were met, the choice throughout of electrical equipment in preference to nonelectrical mechanical devices, the design of an electromechanical "brain" consisting of 3,000 relays with their interlocking circuits, the certain and safe solution of new and unique circuit problems, comprise the history of the installation. Signal blocks are here, but no signals are visible along the tracks. Instead, there is a four-speed signal in the cab directly before the eyes of the motorman. Should the train exceed the speed indicated, an automatic brake application brings the train to a stop. The method used for the calculation of safe braking distances and the determination of suitable block lengths may interest those who are confronted with similar problems.

An interlocking control machine at which the operator merely pushes two buttons on a track diagram to set up a route, position the switches, and clear the signals, automatically and infallibly avoids the authorizing of any conflicting or unsafe move. If, for any reason, the customary or preferred route between the two points is not available, the machine will unerringly select and set up another route, if any be available, between the two points. On these same control boards are the operating buttons and indication lights of a system for the transmission, reception, and storage of the identities of trains of the 15 lines of the Oakland and Berkeley network. In each tower, one in San Francisco and one in Oakland, the operator has at all times a visible picture of the trains approaching on the bridge, and the identity of the nearest three is always shown.

Cab-Signal and Train Control System

The system is a true automatic block system with double-rail track circuits, using reactance or impedance bonds at the insulated joints to provide a path for the negative return of the power current, but having no visible block signals whatsoever. Instead of semaphore arms automatically taking various angles, the 100-cycle signal current in the rails is uniformly interrupted, the rate of interruption in each block depending on its distance behind the occupied block. A clear track ahead is denoted by 180 interruptions per minute, occupancy less than "clear track" distance by 120 interruptions per minute, occupancy in the second block ahead by 75 per minute. Occupancy in the first block ahead or in the immediate block is denoted by a steady current or by no current. Receivers at the front end of the train pick up the signal current by induction, filtering and amplifying it, and the interruption rate picks up the corresponding rate relay on the train, thereby selecting the cab-signal light and the set of governor contacts which will remain in control so long as that code continues to be picked up.

Whenever the train reaches the authorized speed as indicated by the cab signal, a white light is lighted, and remains lighted as long as the speed equals or exceeds that indicated. Should the speed be increased until it is one mile per hour greater than that authorized, a warning signal sounds and continues sounding until the speed is reduced below that point. Should the train continue to accelerate and reach a speed two miles in excess of that authorized, an immediate emergency brake application will automatically result and the train is brought to a full stop. When the cab-signal aspect changes to indicate a lower speed, and the train is not already down to this new authorized speed, the motorman is allowed $2^{1}/_{2}$ seconds in which to initiate a service-brake application which he must hold until the train is down to the authorized speed. Any delay beyond that period will cause an automatic emergency application of the brakes and the train will be brought to a full stop. When the aspect changes to the lowest-speed indication, the motorman, in addition, must step on an "acknowledging contact" pedal for, though he may be traveling below that speed and consequently not be required to apply the brakes, he must indicate that he is alert and prepared to stop in case the block ahead is occupied.

The designed arrangement of codes and controls is given in table I.

An additional code of 240 interruptions per minute, used at points where trains leave cab-signaled tracks, cancels the cab-signal and train-control equipment on the train, lights a violet light in the cab signal showing the letters "NS", and permits the train to proceed without speed restrictions until a control code is again encountered.

In such an automatic block system the controls are set up by the occupancy or nonoccupancy of a track block. The 100-cycle current is supplied to the track circuit through a low-voltage transformer whose leads are connected to the two rails at the exit end of the block. At the entrance end the track relay leads are connected in a similar manner and current-limiting resistances connected in series at either or both ends. When the signal current is shunted through a train axle in the block, the track relay drops and thereby initiates the resulting changes in the controlling circuits.

Signal Power Supply

The 100-cycle a-c energy for the signal system is supplied by duplicate motor

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generator sets in the mole substation. One set provides ample power for the entire bridge railway, the other serving as a standby. The two sets are controlled by equipment which functions automatically to start and cut into service the standby in the event of any failure of the operating set.

Distribution of the 100-cycle energy is made at 2,300 volts with line transformers at frequent intervals stepping down the potential to 110 volts for service to the track transformers and code transmitters and to the rectifier transformers supplying the control circuits and their d-c relays.

The two-conductor 2,300-volt distribution cable is in duplicate, or looped, from end to end of the project. Oil switches on either side of each line transformer enable a transformer, or a section of cable between transformers, to be cut out without de-energizing other portions of the installation.

Interlocking Plants

In the Oakland yard, the interlocking plant includes the main tracks, the eight set-out tracks, the connections and switches between such tracks, switches for entering or leaving these tracks, and all signals governing routes through these interlocked switches. There are 36 interlocked switches and 62 color-light signals of the searchlight type in this plant. Main-track signals for the normal direction of traffic are one-unit or two-unit high signals. All others are single-unit dwarf signals. At the eastbound divergence from joint track, the signal carries two white marker lights to indicate to the approaching train whether the switch is set for the Key System or the Interurban Electric connection. To give the approaching train the earliest possible warning, or assurance, as the case may be, the marker light is repeated at two preceding high signals.

Table I

Code Interruptions Per Minute	Cab Signal Aspect		Nominal Authorized Speed (MPH)	Speed Above Which White Light Is Lighted (MPH)	Speed Above Which Warning Signal Is Sounding (MPH)	Speed at Which Emergency Brakes Are Automatically Applied (MPH)
180	Green	35	35	35	36	37
120	Yellow and green	25	25	25	26	27
75	Yellow	17	17	16½	17½	18½
None	Red	11	11	10¾	11¾	12¾

In the San Francisco terminal loop, all switches and signals are interlocked. There are 36 switches and 40 dwarf signals.

Cab-signal blocks with code in the normal direction of traffic are carried continuously through both interlocking plants. The wayside signals are therefore primarily route signals but are arranged to operate in a manner consistent with track conditions. A yellow clear indicates a "no code" condition in the immediate block; that is, the motorman must expect that upon passing a yellow wayside signal, a "Red 11" cab signal will be received. With the normal control, a cleared signal will return to stop when the head of the train passes it; that is, the signal is a "stick" signal responsive to the occupancy of the immediate block. With a special control, however, the signal is made "non stick", will not be returned to stop by a passing train, and may be cleared even when the immediate block is occupied. Signals at entrances to cab-signal territory such as those leading from storage tracks to set-out tracks in the Oakland yard are known as "TC" signals. When a "TC" signal is cleared with the immediate block occupied, the aspect is a flashing yellow instead of steady yellow. With this aspect showing, the train comes to a stop before passing the signal. The choice of clear aspect, whether green, steady yellow, or flashing yellow, is automatic and entirely dependent on track conditions and not at all dependent on the choice between normal and special control.

Interlocking circuits are so arranged that a signal may not be cleared unless all switches in the route governed are properly positioned. Opposing or conflicting signals may not be clear at the same time. A switch may not be thrown while its block is occupied. No switch may be moved unless all signals governing through that switch are at stop. If a signal is thrown to stop in the face of an approaching train, switches in the route

Figure 1. Typical time-distance chart

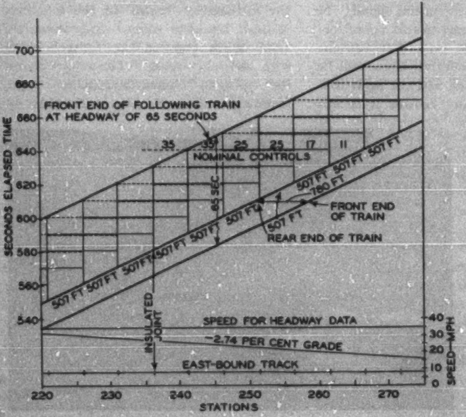
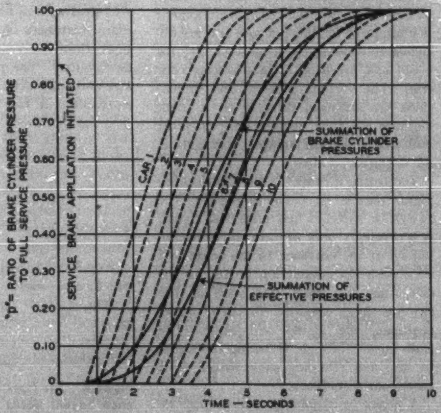


Figure 2. Initial rise in brake pressures



will remain locked until a safe time interval has elapsed.

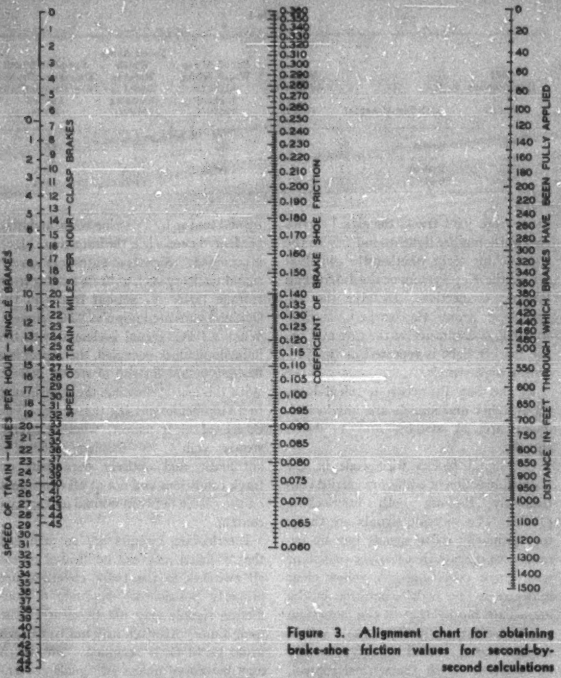


Figure 3. Alignment chart for obtaining brake-shoe friction values for second-by-second calculations

NX Control Machines

The control machines at the two interlocking plants are the latest development in equipment for this purpose and are designed to provide the utmost facility for speed and precision of interlocking control. The various controls and indications are arranged directly on a track diagram so that memorizing of lever numbers and their related functions is not required.

To "set up a route" in the interlocking plant, that is, properly to position the switch or switches and clear the signal, it is necessary only to push the initiation button at the entrance to the route and then push the completion button at the exit from the route. As soon as the switches have operated to their proper positions, the signal will clear. The train passes through, restoring the signal to stop and releasing each switch as it passes out of its block.

A series of adjoining routes may be operated as one, this being known as end-to-end operation. It is necessary only to press the initiation button of the first route and the exit button of the last route and all intervening switches will properly position and intermediate signals will clear as well as the initial signal. In many instances of end-to-end operation, more than one route exists between the two points. The preferred route will be automatically selected and set up if it is available. If for any reason it is not available, the machine will automatically select and set up the optional route. One end-to-end location in San Francisco has four possible routes which are automatically selected as available in their prescribed order of preference.

When initiating a route, the initiating button may be operated in either of two ways. It may be pushed or it may be rotated 90 degrees. If the route is set up by pushing the initiating button, any occupancy of the block immediately beyond the signal will return the signal to stop; therefore, the route may not be set up by this means if this immediate block is occupied. If the route is set up by rotating the initiating button, occupancy of the immediate block will not return the signal to stop and the route will remain set up for any following train or trains. A route that is set up may be cancelled by pulling the initiating button, if it had been pushed, or by rotating it back to the normal position, if it had been rotated.

On the San Francisco control board, in addition to the indications of signals, switches, and occupancy, a green light is provided on each of the six lines representing the six tracks in the train shed. When a train is ready to leave, the conductor presses a button on the platform. The corresponding green light on the board is lighted and remains lighted until the dwarf signal is cleared, authorizing the train to leave the shed.

The *NX* control machine contains the least possible quantity of the necessary control equipment; the push button and miniature switch contacts, the various indicators, and the thousand-odd terminal screws. The relays with their interconnections, the equipment which constitutes the automatic "brain" of the machine, is in a separate room. Approximately 1,000 wires connect the control board with the relay room. The relays are of the plug-in quick-detachable type, mounted on panels two feet wide by

Figure 4. San Francisco control board

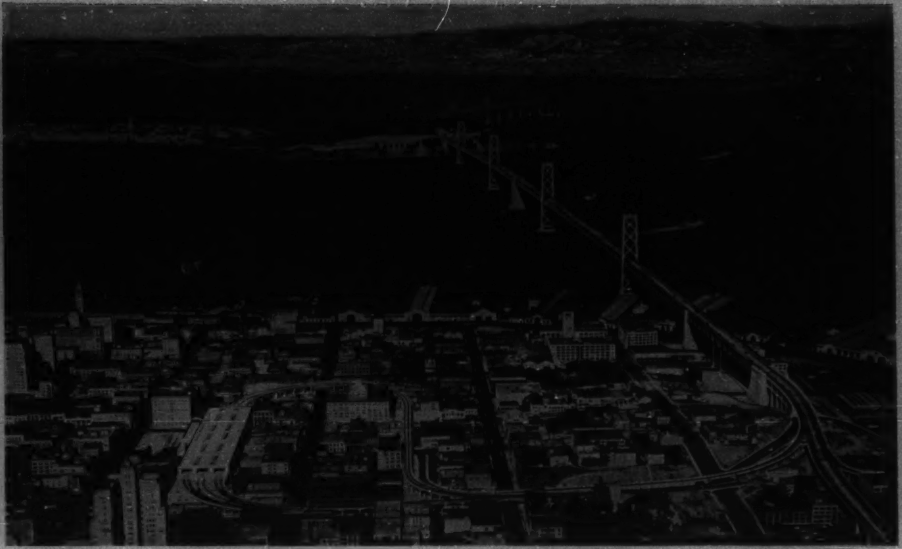


Figure 5. Air view showing the tracks of the San Francisco terminal loop

eight feet high. In San Francisco 22 of these panels, mounted back to back with a passageway between, contain 865 such relays. In Oakland the interlocking plant is naturally divided into eight groups of interlocked switches and signals. At each group remote from the tower is a "bungalow" containing the relays and the protective interlocking of circuits for that particular group. Only the necessary remote control and indication wires are carried from the bungalow to the control room. There are six such bungalows in the Oakland yard, the tower serving the purpose directly for two of the groups, and the number of relays is 1,328. In housings along the bridge between the two interlocking plants are 500 additional relays.

Train Describer Equipment

On each control machine at the right-hand end of the board is a special group of buttons and a group of lights which constitute the visible controls of the train describer equipment. These buttons and lights are lettered and numbered in accordance with the established identities of the various trains. When a train is given its outgoing route from the San Francisco plant, the operation is, as usual, the pushing of the entrance and the exit buttons. But in this case, instead of operating the regular exit button, the control-board operator is required to press one of this special group, thus setting up the identity of the outgoing train, as well as setting up and clearing the route. When the train accepts the signal the identity is transmitted to the Oakland control machine where it is eventually displayed in the group of lights on that board.

This system so functions that the nearest train approaching the plant is identified by the proper light in the number 1 group of 15 lights, the next train by a light in the second group and the third train by a light in the third group. The presence of additional approaching trains is shown by a single light for each. When a train passes the first signal of the interlocking plant, its identity light in number 1 bank is cancelled. The train identity shown in bank number 2 then jumps into number 1, that shown in number 3 moves into number 2, and the indication of the train shown by the single light of number 4 passes into bank number 3 and becomes identified. By this means the operator at the control machine may always see the identity of the next three trains to enter the plant and can plan accordingly. The train identities to the number of 15 are transmitted in either direction over a single pair of wires by means of variations in the polarities of four successive d-c impulses. The equipment includes 170 train describer relays at San Francisco and 102 at the Oakland control station.

Automatic Sorting of Trains

Although designed primarily for informing the tower of the identities of approaching trains, an additional and unique use is made of the train-describer system in controlling the entrance switch to the San Francisco interlocking. The identities of Interurban Electric trains are shown by numbers and those of the Key System by letters. Tracks 1, 2, and 3 in the terminal are assigned to the Interurban Electric Railway, and tracks 4, 5, and 6 to the Key System. At the entrance switch the preferred route for the Interurban Electric is therefore over the right-hand track and for the Key System over the left hand. In place of pushing the buttons to set up the proper route for each approaching train, the operator switches on "automatic sorting". The identification of each train is held by a bank of four relays. Number 1 of this bank is down for the numbered descriptions and up for the lettered descriptions. A front or a back contact of this number l relay is used to select the route and completion is automatic as soon as the switch is released by the passing train. The operator is thus enabled to forget the entrance switch entirely and all Interurban Electric trains are automatically routed over the right-hand track and all Key System trains over the left.

Train Describer Repeaters

The San Francisco control machine includes, in addition to the regular train-describer equipment, two groups of indication lights known as train-describer repeaters. These are mounted at the left end of the board and carry the descriptions after they are automatically cancelled at the entering switch until the trains pass the point from whence they are normally routed, each to its individual track in the station. Either of the two incoming tracks may be occupied by five short trains at one time and each repeater therefore is equipped with five groups of description lights. As the train passes through the entering switch its description is cancelled from the main describer, and enters the *N* repeater or the *R* repeater in accordance as the switch was in the normal or the reverse position.

Determination of Block Lengths

The extremely close headway between trains on the Bridge Railway, together with the heavy grades and frequent changes in grade, necessitated an unusually careful and elaborate investigation to determine the correct layout of signal blocks throughout the length of the project. It is necessary that these blocks and their controls not only permit the desired headway, but also provide safe braking distances at all points and under all conditions. The investigation included tests of the equipment actually operated over the bridge, as well as studies of existing data.

To verify the headway the signal blocks were laid out in the usual manner on time-distance charts. Figure 1 shows a typical portion of one of these charts where the grade is descending 2.74 per cent and the speed is constant at 35 miles per hour. The line designated as "front end of train" is first calculated and drawn. Where the speed is not constant a step-by-step method is used for the calculation. Scaling the train length of 780 feet to the left of this line, a second line is drawn parallel, representing the rear end of the same train at any instant. Still parallel to the first line, but scaling 65 seconds above it, a third line is drawn representing the front end of the following train.

A horizontal line drawn across these three lines will correctly represent the relative positions of the two trains at a given instant in time and, if the blocks are projected vertically from the track plan to this horizontal line, the arrangement of signal blocks between the two trains is shown. By drawing the hori-

Table II. Interurban Electric Railway Ten-Car Train, Test Condition, Braking From 30 Miles Per Hour, Level Grade

t	V	p	f	$R_b = 16.4pf$	$R_t = \frac{V+45}{450}$	$R = R_b + R_t$	V_a	D	SD	d	Sd	Sd_a
0	30.00											
1	29.83	0		0	0.17	0.17	29.92	43.9	43.9	0		
2	29.64	0.01	0.150	0.02	0.17	0.19	29.74	43.6	87.5	0.4	0.4	0.2
3	29.27	0.08	0.150	0.20	0.17	0.37	29.46	43.2	130.7	3.5	3.9	2.1
4	28.52	0.24	0.150	0.59	0.16	0.75	28.90	42.4	173.1	10.2	14.1	9.0
5	27.24	0.46	0.149	1.12	0.16	1.28	27.88	40.9	214.0	18.8	32.9	23.5
6	25.44	0.68	0.147	1.64	0.16	1.80	26.34	38.6	252.6	26.2	59.1	46.0
7	23.26	0.85	0.146	2.03	0.15	2.18	24.35	35.7	288.3	30.3	89.4	74.2
8	20.86	0.94	0.146	2.25	0.15	2.40	22.06	32.4	320.7	30.4	119.8	104.6
9	18.36	0.98	0.147	2.36	0.14	2.50	19.61	28.8	349.5	28.2	148.0	133.9
10	15.80	1.00	0.149	2.42	0.14	2.56	17.08	25.0	374.5	25.0	173.0	160.5
11	13.16		0.153	2.51	0.13	2.64	14.48	21.2	395.7	21.2	194.2	183.6
12	10.41		0.160	2.62	0.13	2.75	11.79	17.3	413.0	17.3	211.5	202.8
13	7.52		0.169	2.77	0.12	2.89	8.97	13.2	426.2	13.2	224.7	218.1
14	4.46		0.180	2.95	0.11	3.06	5.99	8.8	435.0	8.8	233.5	229.1
15	1.12		0.197	3.23	0.11	3.34	2.79	4.1	439.1	4.1	237.6	235.5
15.2	0		0.210	3.42	0.10	3.52	0.56	0.3	439.4	0.3	237.9	

Note: Two tests made under the above conditions resulted in measured distances of 429 feet and 431 feet, respectively.

Table III. Interurban Electric Railway Ten-Car Train, Braking From 37.74 Miles Per Hour, Minus Three Per Cent Grade

t	V	p	f	$R_b = 16pf$	$R_t = \frac{V+45}{450}$	$R = R_b + R_t - 0.61$	V_a	D	SD	d	Sd	Sd_a
0	37.74											
1	38.17	0		0	0.18	−0.43	37.95	55.6	55.6	0		
2	38.57	0.01	0.132	0.02	0.19	−0.40	38.37	56.2	111.8	0.6	0.6	0.3
3	38.82	0.08	0.130	0.17	0.19	−0.25	38.70	56.8	168.6	4.5	5.1	2.8
4	38.75	0.24	0.128	0.49	0.19	+0.07	38.79	56.9	225.5	13.6	18.7	11.9
5	38.26	0.46	0.124	0.91	0.19	+0.49	38.50	56.5	282.0	26.0	44.7	31.7
6	37.40	0.68	0.119	1.29	0.18	0.86	37.83	55.5	337.5	37.7	82.4	63.5
7	36.29	0.85	0.113	1.54	0.18	1.11	36.85	54.0	391.5	45.9	128.3	105.3
8	35.10	0.94	0.108	1.62	0.18	1.19	35.70	52.3	443.8	49.2	177.5	152.9
9	33.91	0.98	0.103	1.62	0.18	1.19	34.50	50.6	494.4	49.5	227.0	202.2
10	32.75	1.00	0.100	1.60	0.17	1.16	33.33	48.9	543.3	48.9	275.9	251.4
11	31.64		0.097	1.55	0.17	1.11	32.20	47.2	590.5	47.2	323.1	299.5
12	30.58		0.094	1.50	0.17	1.06	31.11	45.6	636.1	45.6	368.7	345.9
13	29.55		0.092	1.47	0.17	1.03	30.07	44.1	680.2	44.1	412.8	390.7
14	28.55		0.091	1.45	0.16	1.00	29.05	42.6	722.8	42.6	455.4	434.1
15	27.56		0.090	1.44	0.16	0.99	28.06	41.2	764.0	41.2	496.6	476.0
16	26.59		0.089	1.42	0.16	0.97	27.08	39.7	803.7	39.7	536.3	516.4
17	25.63		0.088	1.41	0.16	0.96	26.11	38.3	842.0	38.3	574.6	555.4
18	24.69		0.087	1.39	0.16	0.94	25.16	36.9	878.9	36.9	611.5	593.0
19	23.76		0.087	1.39	0.15	0.93	24.23	35.5	914.4	35.5	647.0	629.2
20	22.83		0.087	1.39	0.15	0.93	23.30	34.2	948.6	34.2	681.2	664.1
21	21.90		0.087	1.39	0.15	0.93	22.37	32.8	981.4	32.8	714.0	697.6
22	20.97		0.087	1.39	0.15	0.93	21.44	31.5	1012.9	31.5	745.5	729.7
23	20.04		0.087	1.39	0.15	0.93	20.50	30.1	1043.0	30.1	775.6	760.5
24	19.10		0.088	1.41	0.14	0.94	19.57	28.7	1071.7	28.7	804.3	790.0
25	18.16		0.088	1.41	0.14	0.94	18.63	27.3	1099.0	27.3	831.6	818.0
26	17.21		0.089	1.42	0.14	0.95	17.69	25.9	1124.9	25.9	857.5	844.6
27	16.26		0.089	1.42	0.14	0.95	16.74	24.6	1149.5	24.6	882.1	869.8
28	15.30		0.090	1.44	0.13	0.96	15.78	23.1	1172.6	23.1	905.2	893.7
29	14.31		0.092	1.47	0.13	0.99	14.80	21.7	1194.3	21.7	926.9	916.0
30	13.30		0.093	1.49	0.13	1.01	13.80	20.2	1214.5	20.2	947.1	937.0
31	12.28		0.094	1.50	0.13	1.02	12.79	18.8	1233.3	18.8	965.9	956.5
32	11.22		0.096	1.54	0.13	1.06	11.75	17.2	1250.5	17.2	983.1	974.5
33	10.14		0.098	1.57	0.12	1.08	10.68	15.7	1266.2	15.7	998.8	991.0
34	9.03		0.100	1.60	0.12	1.11	9.59	14.1	1280.3	14.1	1012.9	1005.8
35	7.89		0.102	1.63	0.12	1.14	8.46	12.4	1292.7	12.4	1025.3	1019.1
36	6.70		0.105	1.68	0.12	1.19	7.30	10.7	1303.4	10.7	1036.0	1030.6
37	5.47		0.108	1.73	0.11	1.23	6.09	8.9	1312.3	8.9	1044.9	1040.4
38	4.18		0.112	1.79	0.11	1.29	4.83	7.1	1319.4	7.1	1052.0	1048.4
39	2.82		0.116	1.86	0.11	1.36	3.50	5.1	1324.5	5.1	1057.1	1054.5
40	1.39		0.121	1.94	0.10	1.43	2.10	3.1	1327.6	3.1	1060.2	1058.6
40.9	0		0.127	2.03	0.10	1.52	0.70	0.9	1328.5	0.9		1060.6

zontal lines at such points that the rear end of the train is shown as just having passed an insulated joint, the restrictive distance behind the train is at its minimum length. There must then be one full unrestricted block ahead of the following train, so that it may proceed through that block on a clear signal during the time that will elapse before the rear end of the leading train will pass beyond another insulated joint and thereby step the restrictive conditions another block forward.

In checking braking distances, the leading train is considered stationary with its rear axle immediately ahead of an insulated joint. When closing up on a standing train a restrictive block must be encountered at such a distance behind that insulated joint as will enable the motorman to bring the train from its maximum possible speed to a safe stop. This required distance includes five items as follows:

1. Travel during time required for operation of signal equipment (2 1/2 seconds).
2. Travel during allowed time for motorman's reaction (2 1/2 seconds).
3. Travel from initiation of service-brake application until train is stopped.
4. Safety factor for defective brakes or slippery rails, 25 per cent of item 3.
5. Overhang of two trains beyond end axles (20 feet).

Items 1 and 2 are taken together as the travel at the maximum possible speed for five seconds. The maximum possible speed under any control is the speed which would result in an automatic brake application, plus two per cent error for governor maladjustment. For the maximum fully loaded ten-car Interurban Electric train on level track the items are as follows:

Items 1 and 2, $\frac{5\times37.74\times5280}{3600}=$	277 feet
Item 3, level grade, from 37.74 miles per hour	1,329 feet
Item 4, 25 per cent of item 3	332 feet
Item 5	20 feet
Total	1,958 feet

It will be seen that only items 3 and 4 will vary with the grade and only item 3 requires extensive investigation.

The chief factors influencing the effect of brakes upon a railway train may be listed as follows:

1. The nominal pressure of the brake shoes against the wheels when the brake cylinders are at their full operating pressure.
2. The efficiency of the brake rigging in transmitting this pressure.
3. The coefficient of friction which enables this pressure to effect a tangential force on the wheel rims.

Table IV. Interurban Electric Railway Ten-Car Train, Braking From 18.87 Miles Per Hour, Minus Three Per Cent Grade

t	V	p	f	$R_b = 16\,pf$	$R_t = \frac{V+45}{450}$	$R = R_b+R_t -61$	V_a	D	ΣD	d	Σd	Σd_a
0	18.87											
1	19.24	0		0	0.14	−0.47	19.10	28.0	28.0			
2	19.78	0.01	0.183	0.03	0.14	−0.44	19.56	28.7	56.7	0.3	0.3	0.2
3	20.02	0.08	0.180	0.23	0.14	−0.24	19.90	29.2	85.9	2.3	2.6	1.5
4	19.81	0.24	0.178	0.68	0.14	+0.21	19.92	29.2	115.1	7.0	9.6	6.1
5	18.98	0.46	0.177	1.30	0.14	+0.83	19.40	28.5	143.6	13.1	22.7	16.1
6	17.54	0.68	0.176	1.91	0.14	1.44	18.26	26.8	170.4	18.2	40.9	31.5
7	15.61	0.85	0.177	2.40	0.14	1.93	16.58	24.3	194.7	20.7	61.6	51.2
8	13.42	0.94	0.178	2.67	0.13	2.19	14.52	21.3	216.0	20.0	81.6	71.6
9	11.08	0.98	0.183	2.87	0.13	2.39	12.28	18.0	234.0	17.6	99.2	90.4
10	8.48	1.00	0.190	3.04	0.12	2.55	9.76	14.3	248.3	14.3	113.5	106.3
11	5.77		0.200	3.20	0.12	2.71	7.13	10.4	258.7	10.4	123.9	118.7
12	2.87		0.213	3.40	0.11	2.90	4.32	6.3	265.0	6.3	130.2	127.0
12.9	0		0.232	3.71	0.10	3.20	1.43	1.9	266.9	1.9		131.0

Five seconds at 18.87 miles per hour	= 138 feet
Calculated braking distance	= 267
25 per cent of braking distance	= 67
Overhang	= 20
Minimum length of one block	492 feet
Actual block on minus 3 per cent grade	507 feet

4. The variation in effective brake pressure during the period of initial application before all brake cylinders are at their full operating pressures.
5. The weight of the train, including its load.
6. The inertia effects of rotating parts, such as wheels and axles and parts such as motor armatures which are geared to the wheels and axles.
7. The train resistance or those components of mechanical friction which oppose the motion of the train at a constant speed on tangent level track in still air.
8. The grade over which the train is moving.
9. Curvature of the track over which the train is moving.

These factors as they entered into our own particular problem were as follows:

1. Total nominal brake-shoe pressure (pounds):

Interurban Electric ten-car train, six motor cars, four trailers, with equalization at 50 pounds	942,000
With equalization at 60 pounds	1,130,400
Key System seven-unit train with single brakes	1,181,900
With clasp brakes	1,057,700

2. Brake rigging efficiency, taken in all cases as 85 per cent.
3. The most extensive records of the effect of railway brakes are of the very elaborate and lengthy series of experiments made by Captain Douglas Galton in 1878 on the Brighton Railway in England. As is true of most braking studies made since that date for speeds under 60 miles per hour, Galton's observations have been the basis for our studies and, together with the tests made with Key System and Interurban Electric equipment, comprise the data from which our friction formulas have been derived.

For use in our braking calculations the coefficient of brake-shoe friction is taken as continuously varying with the speed and with the distance through which the brakes have been applied. For clasp brakes our formula is

$$f_1=\frac{36}{100+3V}$$

in which V is the speed in miles per hour. This gives a curve which approaches the maximum extremes observed by Galton without exceeding his maximum observation for any speed. Galton's tests were made with clasp brakes. For single brakes we have used values somewhat less than the mean of his observations, having found that such values agreed with our tests. The formula used for single brakes is

$$f_1=\frac{9}{30+V}$$

The reduction factor for distance, F, is taken as equal to the ratio,

$$\frac{10{,}000+5d}{10{,}000+24d}$$

in which d is the distance in feet through which the brakes have been fully applied. The final coefficient is then given by the formula,

$$f=Ff_1$$

and the nomograph, figure 3, is designed for conveniently obtaining the resulting values.

The coefficient of friction at low partial pressures is assumed to vary as the square root of the pressure and this variation is included in the curve of summation of effective pressures in figure 2.

4. When a service-brake application is made, the reduction in air pressure, followed by the operation of the triple valve, is first effective in the head car and then successively in the following cars of the train. Further, the normal pressure of the brake shoes on the wheels is not applied instantly, but rises gradually from zero to full pressure. For use in making braking calculations, standing tests were made of

the rate of rise in brake cylinder pressures of an Interurban Electric ten-car train. These pressure rises were charted and, after consideration was given to the effect of reduced pressures on brake rigging efficiency and on coefficients of friction, a curve of summation of effective pressures was added to the chart (figure 2). Similar data were obtained for a Key System train of seven articulated units. The effective pressure factors for the first ten seconds of a service brake application as used in our calculations are as follows:

Second	Factor for Interurban Electric Railway	Factor for Key System
1	0.0	0.0
2	0.01	0.0
3	0.08	0.08
4	0.24	0.43
5	0.46	0.78
6	0.68	0.97
7	0.85	1.00
8	0.94	1.00
9	0.98	1.00
10	1.00	1.00

5. The weights in pounds of the various trains involved were as follows:

Interurban Electric ten-car train	970,000
Maximum passenger load	250,000
Total weight	1,220,000
Key System train with clasp brakes	952,000
Maximum passenger load	296,000
Total weight	1,248,000
Key System train with single brakes	910,000
Maximum passenger load	296,000
Total weight	1,206,000

6. We have found that the inertia of the rotating parts of either of our maximum trains is equivalent to the addition of approximately 100,000 pounds to the weight. This is the weight equivalent of the rotating parts and, when this is added to the total weight of the train, the sum is the total weight-equivalent of the train.

7. Train-resistance data were obtained by drift tests on level track. From these tests formulas were derived as follows:

For Interurban Electric, $R_t=\dfrac{V+45}{450}$

For Key System, $R_t=\dfrac{V+40}{1{,}000}$

in which R_t is the retardation in miles per hour per second caused by train resistance and V is the speed in miles per hour. The low train resistance of Key System equipment is a result of the installation of roller-bearing journals in all trucks.

8. Grades on the bridge railway vary from three per cent descending to four per cent ascending and lengths of blocks vary accordingly.

9. Resistance caused by track curvature was a factor in the calculation of our speed-time-distance charts, but its effect on braking was not of great importance and will not be discussed here.

To calculate the distance traveled from the initiation of a service brake application until the train is stopped, a second-by-second method is used. The retardation of the speed of a train, caused by the application of the brakes during a period of one second, is obtained by the formula

$$R_b=G\cdot r\cdot e\cdot p\cdot f$$

in which

G is the constant of acceleration caused by gravity in miles per hour per second, taken as 21.93

r is the nominal braking ratio, the ratio of the total nominal brake shoe pressures to the total weight-equivalent of the train

For the Interurban Electric fully-loaded ten-car train

$$r=\frac{1{,}130{,}400}{1{,}220{,}000+100{,}000}=0.856$$

For the ten-car light train of the preliminary tests

$$r=\frac{942{,}000}{970{,}000+100{,}000}=0.880$$

For the Key System fully loaded train with clasp brakes

$$r=\frac{1{,}057{,}700}{1{,}248{,}000+100{,}000}=0.785$$

and with single brakes

$$r=\frac{1{,}181{,}900}{1{,}206{,}000+100{,}000}=0.905$$

For the Key System light train with clasp brakes

$$r=\frac{1{,}057{,}700}{952{,}000+100{,}000}=1.006$$

with single brakes

$$r=\frac{1{,}181{,}900}{910{,}000+100{,}000}=1.170$$

e is the brake rigging efficiency taken as 0.85

p is the ratio of total effective pressure to total full pressure

f is the coefficient of friction for the second involved and is obtained by means of the nomograph, figure 3, after the average speed for that second and the average distance through which the brakes have been applied have been estimated

In the formula for R_b, G, r, and e are constant for a given train condition. For the fully loaded Interurban Electric train

$$G\cdot r\cdot e=21.93\times 0.856\times 0.85=16.0$$

and the formula for this train may be simplified to

$$R_b=16\cdot p\cdot f$$

The total retardation per second is equal to the retardation caused by braking, plus that caused by train resistance, plus that caused by grade, or

$$R=R_b+R_t+R_g$$

R_g is positive or negative in accordance as the grade is ascending or descending. The formula is

$$R_g=s\cdot G\cdot\frac{W}{W_e}$$

in which s is the grade, G is the gravity acceleration constant, W is the total weight, and W_e the total weight-equivalent of the train. For the loaded ten-car train on a three-per-cent descending grade

$$R_g=-0.03\times 21.93\times\frac{1{,}220{,}000}{1{,}320{,}000}=-0.61$$

miles per hour per second.

In the typical calculations shown, symbols which have not been fully discussed are as follows:

V, speed, miles per hour, at the end of each second
V_a, average speed during the second
D, distance in feet traveled during the second
SD, total distance traveled since brake handle was placed in service position
d, equivalent distance ($D\cdot p$) brakes were fully applied during the second
Sd, total equivalent distance brakes have been fully applied to end of second
Sd_m, total equivalent distance brakes have been fully applied to middle of second

After the final braking distance for a given speed and grade has been calculated and the required over-all length of the restrictive blocks obtained, the minimum length of a single block is found. On descending grades we have four restrictive blocks in the rear of the occupied block and three restrictive blocks where the grade is level or ascending. Our standard rail length is 39 feet and where practicable the block length is a multiple of 39 feet or of one-half of 39 feet.

In order to check the length of two blocks for braking from the 25 control, and of one block for the 17 control, calculations must also be made for braking from 27.54 and 18.87 miles per hour. For the project under discussion, braking distances were calculated for level grade, one, two, and three per cent descending, and one, two, and three per cent ascending grades.

In the typical tabulations shown, all calculations were made with slide rule. The formulas given herein for various functions of speed are not considered applicable to train speeds greater than 50 miles per hour.

A New Technique for Lead Cable Sheathing

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Synopsis: A new technique used in the production of lead sheaths for electric power cables is introduced. The various phases of the problem and the difficulties encountered heretofore are described. It has been found that it is necessary to consider the problem both as a mechanical and a chemical one. A method is described in which both factors are used. This consists in pretreating lead as received with a sodium-lead alloy to remove all deleterious impurities and the use of a special standpipe procedure for filling the lead-press extrusion cylinder to obtain a complete union of successive charges, resulting in sound lead sheaths throughout the entire cable length. Test data show that the physical and corrosion properties of lead are not adversely affected by the sodium treatment.

THE HISTORY of the cable-making art shows some very interesting facts. Twenty years, and more, ago, while the use of electric power in the United States was increasing rapidly and the electric power utilities were trying to meet the demand for the transmission of power with the installation of cables operating at higher voltage than heretofore used, they seemed to be held back because of the poor operating record of the cables furnished. A study of the situation showed it to be due to the nature of materials (paper and oil) used for insulation and the lack of proper drying, degasifying, and impregnation methods. In time, this situation was overcome and the rate of cable failures due to defective insulation decreased. However, with the improvement in the technique of insulation manufacture a new problem came to plague both the manufacturer and the cable user.

About 1931 an increase in cable failures due to the lead sheath was noted and this seemed to continue for several years, until today it is found that the major portion of inherent cable failures as reported by the Association of Edison Illuminating Companies and the Edison Electric Institute are due to defects in the lead sheath.

Recognition of the lead sheath as a source of trouble led to an intense investigation of the physical properties of lead as well as manufacturing methods of making lead pipe. As a result, we find some valuable papers written by Moore and his associates, of the University of Illinois,[1] McKeown,[2] and Waterhouse.[3]

The research staffs of the public utilities, such as Commonwealth Edison Company, Philadelphia Electric Company, and Detroit Edison Company, have reported at technical meetings from time to time on their studies. At the same time the cable manufacturers were spending large sums of money on the improvement of manufacturing methods as well as researches on the physical properties of lead as used in cable sheathing. Some of these have been published through technical organizations, such as Bassett and Snyder,[4] Phillips,[5] Dunsheath,[6] and others privately, such as Sherman.[7] All authors were interested in the physical properties of lead and have contributed to the knowledge of the action of lead under physical stress, yet it cannot be said that they advanced the solution of the lead-sheath problem as rapidly as desired. The influence of internal stresses on the expansion of the sheaths was widely studied but we still had the lead-sheath problem as distinct from lead in strip form.

The cable manufacturers gave serious study to the improvement in manufacture of lead sheaths. Various methods, mostly of a mechanical nature and referred to as "improved foundry practice," were tried out. Under this slogan we find the vacuum press,[8] in which the lead-press cylinder is filled with molten metal under conditions in which most of the air is eliminated; hydrogen flame,[9] where the metal is poured in the presence of burning hydrogen which reacts with the oxygen in the vicinity of the molten lead and prevents further surface oxidation of the molten metal; electric arc and oxyacetylene flame which melt the oxidized surface of the residual charge so as to assist in the union of the two charges; high density inert gas,[10] which is used to maintain a nonoxidizing gas blanket over the molten lead in the melting pot and to replace the normal atmosphere or air as far as possible during the time of pouring; continuous extrusion machines,[11] which have been developed in Europe in which the lead is never in contact with air. Mechanical difficulties have been encountered which have delayed the extensive use of this type of machine and at the present time it cannot be considered as a commercial lead press free of shortcomings. Our company studied the claims made for each of the above lead-press processes hoping to find a solution to the problem of elimination of flow lines and laminations in lead sheaths. Some of them appeared to have merit, others were fair, and some of no value. We were able to try all but the vacuum press and the continuous extrusion machine sufficiently long to make us feel that from our viewpoint they were not able to accomplish what we were seeking, namely, a continuously uniform cable sheath no matter how many cylinder charges were required which did not have critical regions, charge welds, or laminations.

A careful study of lead-pipe manufacture indicated that there are two distinct problems. One of these problems pertains to the method of filling the lead-press cylinder with new molten lead and the other deals with the chemical stability of the lead itself. These two problems are closely related and by solving both we have a means to obtain the desired end.

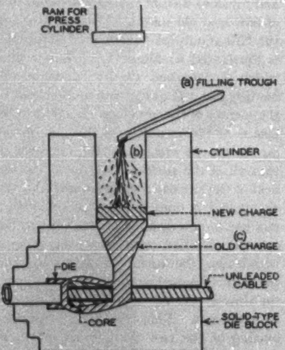

Figure 1. Trough filling of cylinder

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This accomplishment has taken several years to bring to a successful conclusion and required the combined efforts of our production and technical staffs. Among the latter who co-operated in this development were H. L. Beede, technical manager; F. H. Gooding, research engineer; and E. Critchley, assistant chemist.

[1] For all numbered references, see list at end of paper.

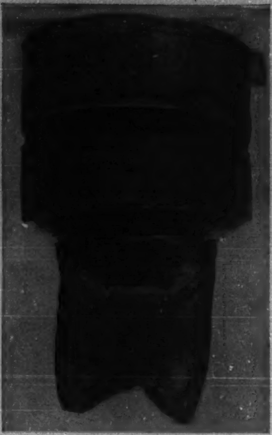


Figure 2 (left). Nonunion of charges

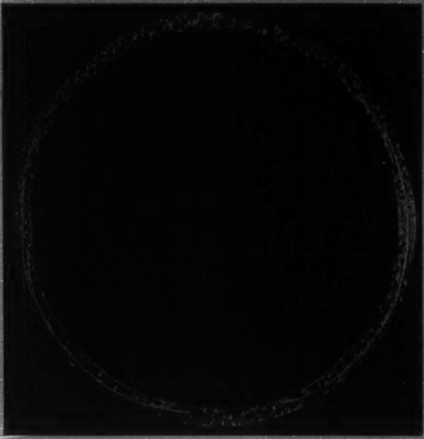


Figure 3. Cable sheath resulting from trough filling

Let us discuss first what happens when the cylinder of a lead press is filled. Figure 1 is a sketch showing the pouring of molten lead from the melting pot into the cylinder. The lead flows along a trough (*a*) to the cylinder and then splashes itself into a broken stream (*b*) as it fills the cylinder. A considerable amount of air becomes occluded and further oxidation of the hot lead takes place so that the cylinder of lead contains gas and lead oxides. Moreover, the previous charge (*c*) is solid and has a surface temperature of about 250 degrees Fahrenheit compared with that of 700 degrees Fahrenheit for the new lead. The incoming molten lead is chilled as it strikes the surface of the old lead, resulting in the condition shown in figure 2. The two charges are easily separated yet they are forced down through the lead block, around the core to form a lead sheath. Such a pipe is not a sound lead sheath, but contains flow lines, laminations, and poor welds, particularly in the so-called critical regions where the lead at the union between the two charges is extruded. If rings are cut along the pipe and etched, we find crystal structures, such as shown in figure 3, which clearly indicate flow lines full of inclusions. Such a pipe is none too strong and in time under stress will produce leaks. The electric arc, oxyacetylene flame, and continuous extrusion machines attempt to correct the nonuniting of charges. This is difficult to accomplish by the first two methods as the molten surface of the residual charge has already started to chill before the new lead reaches it. The continuous extrusion machine may have great promise but it is still in the development stage and many problems pertaining to the control of temperature in its various parts and the overcoming of severe oxidation within it if allowed to cool have yet to be solved.

Recognizing the importance of getting successive charges of lead to unite, we adopted the method developed by our associate company in England, Callender's Cable and Construction Company, Ltd., of bottom filling in which the molten lead is directed by means of a pipe to the surface of the previous charge. This eliminates the splashing that usually takes place, and therefore, the entrapping of air, at the same time causes the new hot lead to melt the surface of the previous charge while the cylinder is being filled and replaces the old charge with a mixture of new and old metal. This is known as the Hill standpipe[14] method of filling and is illustrated in figure 4. (*a*) is a pipe which is attached to the spout of the melting pot and guides the molten lead to the surface of the previous charge (*b*); by means of the handles (*c*) the end of the pipe is moved around by the operator so that all of the surface is subjected to the heat of the stream of the new lead and the old charge is completely replaced with a mixture of new and old metal. When the cylinder is filled with lead, the pipe is withdrawn, the surface is skimmed, and the ram is pressed against the lead during the time it takes to solidify the lead prior to extrusion. This results in a perfect uniting of the two charges, as shown in figure 5, which shows a half section of a slug removed from the press and a perfect joining of the lead. The remains of the old charge can be identified by the small-size grain structure resulting from the previous pressure cycles. Slabs may be cut from the other half and bent into a U shape without showing any splitting where the charges unite.

Although a better lead sheath than formerly was obtained, it was noted that there were oxides and other deleterious impurities still present in the lead as inclusions. Since inclusions are known to cause defects in cable sheaths, it is obvious that their complete elimination is desirable if it is intended to improve the quality of the lead pipe. We therefore studied various ways which have been pro-

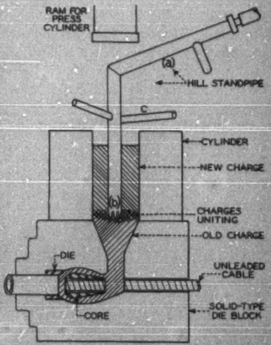


Figure 4. Hill standpipe method of filling cylinder

posed to remove entrapped impurities in lead. While the vacuum press will remove free gases during the filling of the cylinder and thus eliminate oxidation in this respect, it cannot reduce entangled oxides, decompose sulfides, or eliminate other deleterious impurities already present in the lead in the melting pot; such impurities are to some extent soluble in and occluded by the molten metal.[11-12] The impurities segregate themselves between the lead crystals and thus weakness and corrosion along crystal boundaries results. When cooling is rapid as in the case of the molten metal while being transferred from the melting pot into the lead-press cylinder, the impurities have no time to separate, and are therefore interspersed throughout the mass and not merely at the so-called critical region. Furthermore, the deleterious impurities are not only detrimental in themselves, but they also bring about a condition whereby small amounts of oxidation products act as catalysts and thus there is a possible progressive acceleration of oxidation and decomposition taking place in the melting pot.

When commercial lead is melted, the dross removed, and then remelted, the tendency for dross formation in the molten state will be as before. D. M. Smith[13] has shown that on melting lead in a vacuum of the order of 0.025–0.015 millimeter mercury pressure, dross appeared on the surface and increased in quantity on agitation of the molten metal. Two or three purifications under vacuum were necessary to practically prevent continued dross formation.

As stated above, the use of a hydrogen flame to reduce the oxides as the lead is poured into the cylinder is not completely effective. The flame contacts only the surface of the stream of molten metal, and, therefore, merely prevents further oxidation without helping the situation. This method alone does not bring about complete union of charges.

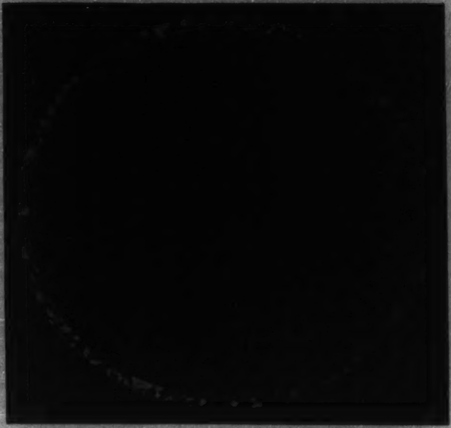


Figure 6. Cable sheath resulting from Hill standpipe filling

While the surface of the residual charge can be melted by the electric arc or oxy-acetylene flame, the oxides originally present will only be disturbed but not removed. When pouring the new charge of lead at the usual pouring temperature, which is around 750–775 degrees Fahrenheit, there will not be sufficient fluidity or residual heat to allow those oxides, sulphides, and other deleterious impurities which are insoluble in molten lead to rise to the surface and be skimmed off.

The use of inert or reducing gases in the lead-press cylinder is ineffective as the release of the gas under pressure has a cooling effect on the molten metal entering the cylinder. The lowered temperature and marked sluggishness of the molten lead prevents proper welding between charges. Moreover, the oxides in solution which are always present in molten metal are unaffected. Besides, foaming of the molten metal and occlusion of some of the gas takes place, thus bringing about blistering, gas pockets, and sponginess in the extruded sheath. Even when inert gas is used with a positive pressure over the melting pot, dross with an appreciable amount of red lead accumulates. The other methods referred to all suffer from similar difficulties.

Apart from the many efforts to bring about some sort of stability to the molten lead as it enters the lead-press cylinder, various means have been employed to reduce as far as possible surface oxidation of the molten lead in the melting pot. Such ingredients as burnt clay, sand, powdered mica, charcoal, graphite, fluxes, various salts, and mechanical contrivances have been used. Stabilization of lead has also been attempted under different circumstances, using chlorine, phosphorus, boron trichloride, titanium tetrachloride, vanadium trioxide, lithium, and a host of other reducing and deoxidizing substances. In all such cases the reagents failed to bring about the desired results.

Recognizing the shortcomings of all the

Figure 5. Complete union of charges

methods in use for the stabilization of lead, we proceeded to experiment with various deoxidizing and reacting substances so that the treated metal may not only be free from occluded oxygen and other gases, but also free of entangled oxides, sulphides, and other deleterious impurities; moreover, that after the elimination of all such impurities, the treated metal should be immune from further oxidation and contamination in process of extrusion.

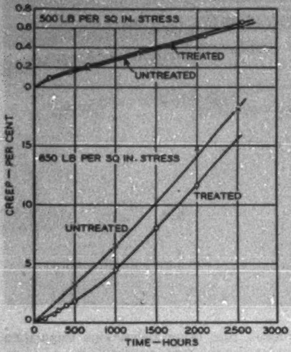


Figure 7. Creep tests on lead strips cut from cable sheathing

Temperature 15 degrees centigrade

After conducting a research extending over a period of several years, it was found that the desired end could be accomplished by the chemical stabilization of lead and its alloys. The active reagent that brings about this stabilization is metallic sodium, a member of the alkali metals in the periodic group, preceded by lithium and followed by potassium. Sodium, when used within critical limits and under prescribed conditions, stabilizes lead without at the same time changing the basic physical characteristics or affecting adversely the corrosion-resistant properties of the treated metal. It can be obtained as a relatively stable alloy of lead and is commercially available at a reasonable price. Since metallic sodium is quite reactive and dangerous to handle under atmospheric conditions, apart from the fact that uniformity of treatment is difficult, it is necessary to alloy it under controlled conditions with lead and this alloy is then introduced into the lead to be stabilized.

The amount of master alloy to use depends upon the kind of lead to be treated and the impurities present in the metal as received from the refinery. For example, as little as 0.005 per cent of metallic sodium cleanses, and as a result imparts a silvery color to electrolytic lead. This type of lead, which is known to dross more than any other commercial lead on the market, may then be heated at elevated temperatures for long periods of time in an open pot without contamination.

The critical amount of sodium used in this treatment of lead ranges from 0.005 per cent to 0.05 per cent by weight of the amount of lead being treated. Upon the addition of the sodium-lead alloy to the molten lead, the alloy is broken down into its component parts at a temperature of 850–900 degrees Fahrenheit. The sodium thus released combines with any free or dissolved oxygen, reduces the various oxides, and decomposes as well as throws out of solution such deleterious compounds as sulphides which are usually present in commercial lead. The deleterious impurities thus released float to the top of the molten mass of metal from which they are readily skimmed off.

This method of treatment results in a trace of sodium of the order of 0.001 per cent remaining in the lead. This small amount of residual sodium has a stabilizing effect upon the treated lead in that the latter may be allowed to solidify, then remelted without the necessity of again treating with sodium-lead alloy. The remelted lead not only does not dross, but retains the same silvery appearance and physical characteristics as the lead resulting from the first treatment.

It is possible to keep the treated lead, due to its stability, at temperatures as high as 850–900 degrees Fahrenheit in the melting pot for long periods of time without fear of dross formation and without affecting the physical characteristics of the extruded metal. This treatment overcomes the natural sluggishness of lead and its alloys at any given temperature below red heat. The increase in fluidity of the molten metal together with the higher pouring temperature is a distinct advantage, as it not only helps to float to the surface any foreign matter which might find its way into the cylinder, but also facilitates the fusion and welding of the old with the new charge.

For lead alloys, the lead is first pretreated and then the alloying metal, as

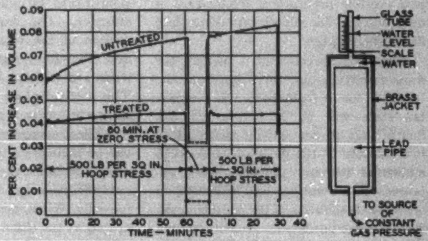


Figure 8. Increase in pipe volume due to steady internal pressure of 62 pounds per square inch (500 pounds per square inch hoop stress)

Temperature 25 degrees centigrade

Table I. Physical Tests

	Tensile Strength* (Pounds per Square Inch)	Elongation (Per Cent in One Inch)	Rockwell Hardness Number **	90-Deg Bends Original IPCEA† Machine
Electrolytic				
Untreated	1,980	75	62	88
Treated	2,010	83–87	65–67	72–84
St. Joseph (0.06 per cent copper)				
Untreated	2,350	66	72.5	50–66
Treated	2,280	78–78.5	72.5	58–66
Electrolytic and 0.01 per cent lithium				
Untreated	2,630	58	92.6	27
Treated	2,890	65	88	27
St. Joseph and 2 per cent tin				
Untreated	3,140	62.5	85.2	24
Treated	2,980	72	75	22

* Speed of machine one inch per minute.

** Model *R H*, one-half-inch ball penetrator, 30-kilogram load. Anvil $^3/_8$-inch flat spot. Time factor 30 seconds.

† Insulated Power Cable Engineers Association.

Table II. Bend Tests

Type of Lead	Time of Test	Single 90-Deg Bends, IPCEA Machine Average	Maximum	Minimum
Electrolytic	Immediately	50	72	40
	One week later	78	100	56
	Two years later	92	106	78
St. Joseph	Immediately	52	78	42
	One week later	76	90	60
	Two years later	89	100	78

such, or in the form of a master alloy, is added. Here also, it is possible to maintain a high temperature in the melting pot. The extruded alloy sheath shows uniform dispersion of the alloying metal and is free from inclusions or laminations.

Although our method of treatment consists in taking a mixture of lead and sodium, which we call the master alloy, and dissolving it in the molten lead to be treated, we do not get as a result an alloy of lead in the final product.

To determine what influence the treatment has on the physical and corrosion-resistance properties of lead, experiments were conducted on both types for comparison. Table I shows typical physical properties for several types of lead and lead alloys.

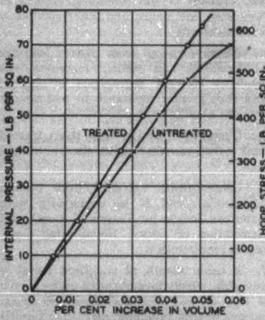


Figure 9. Increase in pipe volume due to rising internal pressure

Pressure increased at ten pounds per square inch per ten seconds

Temperature 25 degrees centigrade

Taking bend tests[8] as an indication of aging, table II shows the change in two kinds of treated lead immediately, one week, and two years after extrusion. It is to be noted that recrystallization or self-annealing has taken place and the range between maximum and minimum bends are closer after two years than when first tested, which is typical of commercial lead.

Creep tests on treated and untreated lead strips, and expansion tests on pipe have indicated that the treated lead is somewhat superior in resisting distortion due to stress even when the standpipe method of filling the cylinder is used in both instances.

Figure 7 shows the results of creep tests made at 500 and at 850 pounds per square inch. At 500 pounds per square inch there is practically no difference, but at 850 pounds per square inch the untreated lead stretches faster than the treated metal. This might be expected because of the improved crystal bond that is characteristic of the treated lead, and which reduces that part of the flow which is associated with the crystal boundaries.

Expansion tests were made on samples of lead pipe, sealed at the ends, and subjected to internal pressure. The increase in volume of the pipe was measured by the water displacement method shown at the top of figure 8. The initial creep and creep rate and the elastic recovery when stress is removed are shown for a 62-pound-per-square-inch (500 pounds per square inch hoop stress) pressure cycle, lasting one hour. This is followed by a one-hour zero-pressure period and a further one-half hour stress period. The initial creep and creep rate are greater for the untreated lead. The recrystallization during the one-hour rest period was negligible in both cases.

Stress-strain curves, made on similar samples of lead pipe, exhibited the same tendencies, see figure 9. The pressure was raised by 10-pound steps, and the increase in volume noted at each step. At 50 pounds per square inch the untreated pipe began to expand rapidly, and had reached the short-time yield point. The treated pipe went to 70 pounds per square inch before this point was reached.

The combination of the Hill standpipe method of filling the lead cylinder and the use of treated lead has resulted in an improved quality of lead sheath. Several years ago, while making the study of the various methods proposed for obtaining better sheaths, it was realized that to appreciate the full value of any improvement in lead-press technique it was necessary to test full-charge lengths of pipe, extruded under normal production conditions, instead of short specimens. Therefore, a time-pressure test was set up by taking full-charge lengths having about 2.80 inches outside diameter and 0.141 inch wall. Both ends are sealed and an internal pressure of 90 pounds per square inch of air (805 pounds per square inch circumferential stress) is applied for ten days and then the pressure increased 10 pounds per square inch internal pressure every five days until rupture occurs. The early tests with former process resulted in many of the samples failing on the first step and usually on a charge weld, or close to a die weld, where the tongues of the flow lines were very pronounced, resulting in blunt fractures. With the new technique the pressure reached on the average the 130 pounds per square inch step internal pressure (1,160 pounds per square inch circumferential stress) before rupturing which normally occurs at the thinnest part of the sheath and with a clean knife-edge fracture. Figures 10 and 11 show the rupture of lead pipes made by the old and new methods. Monthly checks are made on full-charge lengths from regular factory production runs and

Figure 10. Pressure-test rupture of pipe made by old method

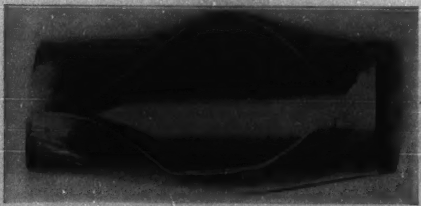
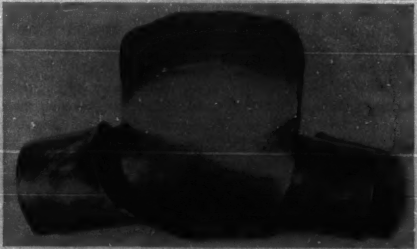


Figure 11. Pressure-test rupture of pipe made by new technique



after bursting the fracture is examined for location along the pipe, the position with respect to the welds, the nature of the fracture, and the uniformity of crystal size. In addition, a slug of lead from each press, following the extrusion of the pipe, is cut in sections and etched as shown in figure 5 as a check on the technique of cylinder filling.

This technique of filling the cylinder completely eliminates the so-called critical region from a pipe. Figure 6 shows an etching of a ring taken about 30 feet from the beginning of a pipe and in the zone of what is normally considered the critical region. When compared to figure 3 one readily notes an improvement.

Table III. Chemical Corrosion

Time in Hours in Boiling Distilled Water	Type of Lead Sheath, Specimen Approximately 150 Grams	Milligrams $Pb(OH)_2$ per Gram of Lead
25	St. Joseph Untreated	0.049
	Treated	0.027
70	St. Joseph Untreated	0.064
	Treated	0.042
112	St. Joseph Untreated	0.089
	Treated	0.068

In view of the intense chemical activity of metallic sodium in the presence of moisture or chemical solutions which are known to attack lead, comparative corrosion tests were conducted on treated and untreated metal. Since corrosion is largely of an intercrystalline nature the extent of corrosion will, in a measure, depend upon the purity and general characteristics of the intercrystalline material, or we may say the cementing medium. According to Dunsheath,[8] this cementing medium is generally made up both of amorphous and crystalline metal, with the former predominating. It is this boundary material which is readily susceptible to corrosive conditions, particularly when the metal is under stress.[8] Since sodium treatment brings about stabilization not only to the crystals but also to the intercrystalline cement as well, it is expected that better bonding between crystals, greater stability, and less susceptibility to corrosion should result.

A series of corrosion tests were made to compare the rate and degree of corrosion on treated and untreated lead. Both types of lead were exposed to live steam at 105 degrees centigrade for 100 hours. The treated lead disclosed a white deposit of lead hydrate, $Pb(OH)_2$, which could be readily rubbed off. The untreated lead had a brownish white deposit on the surface which was hydrated lead monoxide $PbO.Pb(OH)_2$. The latter was quite pronounced, embedded in the surface, and required a sharp tool to remove it.

In another experiment 2-inch squares of lead sheath, approximately 150 grams in weight, after being thoroughly sandpapered, washed with distilled water, and wiped dry with a soft cloth, were placed in 500-cubic-centimeter porcelain casseroles, submerged in 300 cubic centimeters of distilled water, and boiled for varying periods of time, ranging from 25 to 112 hours. At the end of the respective periods the solutions were tested for alkalinity by titrating with a one-tenth normal solution of HCl, using phenolphthalein as an indicator, and calculated to milligrams of $Pb(OH)_2$ per gram of lead. The results are shown in table III.

It is to be noted that in all cases the water for the untreated lead showed a greater alkalinity than the treated lead. Moreover, when the samples were removed from the casseroles and allowed to dry, the deposit formed on the surface of the untreated lead samples was more pronounced and was whiter in color than on the treated samples.

In addition, corrosion tests were made on samples of both types of lead by differential aeration, using various kinds of chemical solutions which are known to be met in actual service. Nine samples of lead pipe, 1.75 inches over-all diameter and 6 inches long, with the solutions were placed in one-quart glass jars and the pipes were partially submerged by setting the jars in an inclined position. The results of these corrosion experiments as indicated by the amount of acid necessary to neutralize the resulting alkalinity of the solutions are shown in table IV.

Table IV. Corrosion Due to Differential Aeration

Description of Solution	Cubic Centimeters of 0.066 n HCl Used to Neutralize Ten Cubic Centimeters of Solution	
	Untreated Lead	Treated Lead
Manhole water	0.3	0.3
Manhole water plus CO_2	0.9	0.55
Tap water plus CO_2	0.3	0.3
Tap water plus CO_2 plus one per cent acetic acid	47.5	45.0
n/10 sodium bicarbonate	15.4	15.2
n/10 sodium chloride	1.3	0.3
n/10 calcium sulphate	1.9	0.8
n/10 ammonium hydroxide	7.7	6.7
One per cent lime water	8.0	7.6

It is to be noted that when comparing both types of lead that the treated lead does not show any greater degree of corrosion than the untreated lead.

Considering the results of the experiments described above, it is our opinion that the corrosion resistance of treated lead is at least equal to, and somewhat better than, that of untreated lead.

Summary and Conclusions

The following conclusions may be listed as to what has been accomplished by the combination of the method of filling which we are using and the stabilization of the lead.

1. All types of deleterious impurities are removed in process of treating commercial lead with sodium within the critical range of 0.005 to 0.05 per cent sodium. Such treated lead will retain approximately 0.001 per cent residual sodium which has a stabilizing effect, when such metal is remelted, processed, and used in service.

2. In processing no special equipment is necessary, no extraordinary ingenuity is required, and no hazard in treating lead is involved.

3. With proper lead-press procedure, cable sheaths extruded from treated lead disclose a bright appearance, a distinct, uniform, and brilliant crystal structure, absence of flow lines, inclusions, and other commonly known defects.

4. Alloys prepared from treated lead are homogeneous and stable; moreover, the tendency toward segregation is reduced and thus greater resistance to corrosion is brought about.

5. Chemical corrosion tests show treated lead to be somewhat better than untreated lead.

6. Cable sheath extruded from treated commercial lead is in conformity with American Society for Testing Materials and Association of Edison Illuminating Companies requirements.

7. A cable sheath of stabilized metal is available, which throughout its entire length is free of occluded and dissolved gases, various oxides, sulphides, and other foreign matter.

8. A stabilized cable sheath has been developed whose metal can be remelted and maintained at elevated temperatures, such as 900 degrees Fahrenheit, in an open pot for long periods of time without drossing or affecting its chemical or physical characteristics.

9. By means of treated lead and bottom filling as carried out with the Hill standpipe complete welding of charges is assured.

10. The appreciably greater fluidity and higher temperature of treated metal is a distinct advantage as it not only helps to float to the top of the lead-press cylinder any foreign matter, but makes it much easier to fuse the old and new charges and thus facilitates the welding operation.

11. In the long-time pressure test on full-charge lengths, treated lead sheaths require invariably a higher pressure and disclose a knife-edge break at time of rupture.

12. It cannot be overemphasized that the lead-sheath problem is both a mechanical and chemical one. If improved quality of lead sheaths is to be expected, then our method, or some other method that will bring about the same result, is necessary.

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Discussion

Lyall Zickrick (General Electric Company, Schenectady, N. Y.): I have studied with quite some interest the preprint copy of the paper "A New Technique for Lead Cable Sheathing" by Reinitz and Wiseman.

The authors are to be commended for their efforts and endeavor in attempting new improvements for the cable manufacturing industry. There are contained in the paper, however, several items which it would be well to have the authors further clarify. For convenience, these items may be listed as follows and taken up in detail:

(*a*). References to the work of other investigators.
(*b*). Comments on the method of filling.
(*c*). Regarding quality of lead and effect of sodium.

(*a*). In the authors' references to the work of other investigators it appears that considerable detail has been omitted from the report which would justify their conclusions. Inasmuch as these references to the work of others are quite critical, substantiating data would seem desirable. I know in our work with hydrogen there are both effective and ineffective ways in which it may be applied. There is no doubt about the ability of hydrogen to reduce lead oxide when applied correctly.

(*b*). In reading the description of their method of filling, I have been wondering if the authors experience any difficulty in getting a good well-fused area between the new and old charge. We were successful with some of our early methods in obtaining a well-fused area in the central portion, but not clear to the edge. We now obtain an excellent bond clear to the edge and for about three to four inches into the old charge.

Table I

	Per Cent		
	(A) Southeast Missouri Desilverized Lead	(B) Very Pure Lead	(C) Southeast Missouri Undesilverized Lead
Silver	0.0004	0.00000	0.0068
Arsenic	0.0001	0.0000	0.0000
Antimony	0.0012	0.00024	0.0010
Bismuth	0.0012	0.0000	0.0005
Copper	0.0003	0.000057	0.062
Iron	0.0008	0.000193	0.0003
Zinc	0.0007	0.000080	0.0002
Manganese	0.0000	0.0000	0.0000
Nickel and cobalt	0.0000	0.0000	0.0048
Total of above impurities	0.0047	0.000570	0.0756
Lead by difference	99.9953	99.99943	99.9244

(*c*). Throughout the text there are a great many references made to the quality of pig lead which appear to be quite alarming. One gains the impression that the so-called good brands of lead we cable manufacturers use are full of, to quote: "entangled oxides, sulphides, and other deleterious impurities", and again "such deleterious compounds as sulphides which are usually present in commercial lead", and still again I encounter the phrase, "the oxides in solution."

Table II. Hardness and Tension Tests of Cast Metal

	(A) Southeast Missouri Desilverized (7)	(B) Very Pure Lead 3712(5)	(C) Chemical Lead (10)
Yield point (0.5 per cent elongation) (pounds per square inch)	861	714	1,643
Ultimate tensile strength (pounds per square inch)	1,966	1,609	2,765
Elongation (per cent)	64.2	68.6	50.2
Reduction in area (per cent)	92.4	100.0	87.9
Brinell hardness number one hour after casting	4.2	2.9	5.5
Brinell hardness number ten days after casting	4.1	3.0	5.4

I feel it would be especially desirable for additional enlightenment on this subject, for if such is the quality of lead we use, the industry should be so informed.

For my own part, I have never found any evidence of any solubility of oxides in lead, either in the literature or elsewhere, and the same is true as well for sulphides. If these so-called impurities are soluble, I would not expect them to segregate, especially to the grain boundaries as stated, at least until they had precipitated from solution.

Again in the text an implication has been made which suggests an excessive amount of dross in pig lead. Reference 13 to the work of D. M. Smith has been quoted. Smith's work gives no information whatsoever as to the source, type, or chemical composition of the lead he used, but merely states that upon melting in vacuum he obtained a quantity of dross, which to quote directly from his work: "Spectrographic analysis showed that this dross consisted mainly of lead, small quantities of tin, calcium, aluminum, magnesium, cadmium, and carbon also being detected." It does not appear reasonable to classify the above as being in any way representative of our pig lead of the present day, produced by reliable refineries. I believe the lead we use is of far better quality than is herein intimated.

I am somewhat confused as to the part or metallurgical function the sodium addition plays, especially the residual sodium.

In general, I believe it is accepted that the purer the lead is the lower the tensile properties, and also to a degree the larger will be the grain size and the lower the resistance to fatigue. For illustration, the data in tables I and II of this discussion are pertinent ("Pure and Low-Alloy Lead", a paper by G. O. Hiers presented at an AIME symposium on pure metals, February 1939).

In table II you will note the low physical properties for lead 99.9994 per cent pure. If such is the case, the action of this small amount of residual sodium scarcely seems to be in the direction of greater purity.

Again I read "untreated lead stretches faster than the treated metal because of the improved crystal bond that is characteristic of the treated lead, which reduces that part of the flow which is associated with the crystal boundaries."

In metallurgy the nature of crystal boundaries and their characteristics has for years been a subject for controversial argument and discussion. It seems the subject can hardly be so easily and lightly dispensed with. The inference is that lead flows along crystal boundaries at least to a degree, and because of this treatment such flow is retarded. It is common metallurgical knowledge today that the precipitation of a phase from solution causes such a stiffening of crystal boundaries, so here again a higher degree of lead purity is not indicated. Precipitation may occur throughout the crystal, but time and aging tend to agglomerate the precipitated phase in the grain boundaries. Could not this strengthening effect be due to precipitation of a sodium-lead phase originating from the residual sodium? Long-time aging tests (two years or more) taken on sheaths containing varying amounts of residual sodium should give important data covering the effect of this constituent on fatigue, corrosion resistance, and other physical properties.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): For a given kind of lead or lead alloy, the two main desirable characteristics of the sheath from the standpoint of the user are (*a*) soundness of structure and (*b*) circumferential uniformity of thickness. The authors' paper relates to the first characteristic.

As indicated in my discussion on expansion of sheaths on underground power cables in service in the University of Illinois Bulletin 306 mentioned in reference 1 of the authors' paper, imperfect welds and nonmetallic inclusions usually constitute the initial set of causes for a large number of openings of sheaths at defects in service. After about five years of operation, openings have occurred in Chicago at a high rate also in abnormally thin portions of sheaths. Most of these unfortunate occurrences in Chicago obtained for 66-kv solid-type single-conductor cable, the number of defective lengths of this type of cable involved having been close to 200 to date. Over 4,000 lengths of such cable were installed, principally during the period of 1926–31, inclusive.

Sheath troubles due to inherent defects have also occurred to a lesser extent with, for example, 4- and 12-kv cables which have relatively less insulation. Also, such cables have joints filled with a heavy viscous compound whereas the 66-kv cable had joints filled with a thin oil under a small pressure. The oil migrated into the cable and resulted in the maintenance of relatively high internal pressures during the periods of daily maximum loads. The resulting rate of sheath expansion has been excessive (about six mils in diameter per year) and so investigations are being conducted to devise a scheme to reduce sharply this rate in order that a reasonable cable life may be obtained.

In view of all our trouble, it is obviously very gratifying to have learned of the improvements described in the paper. Cable with sheath treated as outlined in the paper has been furnished to Commonwealth Edison Company and no structural defects have been found in such sheaths.

The authors refer to tests to rupture and to creep tests at 500 and 850 pounds per square inch. Such data are of general interest, but more pertinent data are those obtained at lower stresses which apply for most operation. Obviously, a sheath that expands unduly is quite undesirable and the cable may develop trouble long before rupture of the sheath occurs. The authors, I believe, appreciate this point because the manufacturer has co-operated with me by furnishing samples for long-time creep tests to be made at the University of Illinois under the sponsorship of the Utilities Research Commission. In these tests of treated lead, the samples of ASTM grade II lead and of ASTM grade III lead had creep rates within the range found for similar materials having no treatment.

Perhaps a word should be said also for some of the other developments on cable sheath. We have had a large operating experience and have obtained a large amount of test data on samples of such sheaths furnished by other manufacturers. The progress made by the various manufacturers toward always furnishing sheaths that are structurally sound has varied, but nowadays it appears that very good sheaths are being produced by some of the methods that the authors tried and found wanting.

Any of the methods used by the manufacturers must be accompanied by continuous good workmanship to assure uniformly perfect sheath structure, and the cable manufacturers are aware of this fact.

I. T. Faucett (General Cable Corporation, New York, N. Y.): The authors cover a very broad subject on which the industry has made large expenditures in research and development during the past decade. Essentially, the method which they recommend for obtaining uniformity of lead structure is to offset the effect of one impurity by the introduction of another. It would seem that the preferred approach to the problem is to exclude the impurities in the first instance, as is done, for example, in the vacuum press.

Among the recognized causes of field troubles with lead sheaths is the oxidation of ram lubricant. This is not mentioned in the paper, and it would be very interesting to know how this dangerous impurity is eliminated in the authors' process.

Another cause of trouble with lead sheath, common to most methods, is the chilling of the lead ram, molten lead, and cylinder walls during the charging of the press, as well as the oxidation incident thereto. As the ram moves out of the cylinder, air is allowed to enter and this not only chills the ram and cylinder walls but also chills and oxidizes the surface of the lead remaining from the previous charge. The Hill standpipe method, used by the authors, is subject to the above condition. The vacuum press, on the other hand, eliminates this source of trouble by providing a closed evacuated system into which the lead is introduced through an electrically heated pipe. In the vacuum press, oxide is not given a chance to form, whereas the authors' method depends upon the reduction by means of sodium of the oxides which must inevitably form and the subsequent skimming of the sodium oxide, which is not positive.

The authors refer to the vacuum press and state that:

> ". . . . while the vacuum press will remove free gases during the filling of the cylinder and thus eliminate oxidation in this respect, it cannot reduce entangled oxides, decompose sulphides, or eliminate other deleterious impurities already present in the lead in the melting pot. Such impurities are to some extent soluble in and occluded by the molten metal."

The authors state they have not investigated the vacuum press and apparently they are unaware of the method used in the melting of the lead in the lead kettle and the drawing of the melted lead into the lead cylinder. In the vacuum press the lead is melted in a basket located one-quarter of the height of the kettle from the top and the lead is drawn out at the center line of the kettle one-quarter of its height from the bottom. This procedure prevents any entangled oxides and other insoluble impurities from being carried toward the bottom of the kettle, because such impurities, due to their lower density, will float and further, because the melting is done at the top of the kettle. With this arrangement only pure bright metal can go over into the lead-press cylinder.

With further reference to entangled oxides and sulphides, it should be noted that the alkalies used in standard refining methods are as effective in removing sulphur as is the sodium treatment. This, together with information from precise tests, shows that sulphides are not part of the impurities unless incompletely refined or contaminated lead is used. As for the presence of oxygen, some of the leading metallurgists contend that it is not present in significant quantities. However, even granting that the sodium reduces or removes in the melting pot dissolved or occluded oxides, it would seem from the authors' description of their process that the molten lead must again be pretty thoroughly exposed to the action of the air and with a new opportunity to entrain or occlude oxides. How is this prevented unless there is present in the final metal considerable controlled amounts of sodium? Is 0.001 per cent of sodium supposed to be adequate for this, and, if it acts in this way, does it continue to exist in that quantity, regardless of detail of procedure?

The authors state that although sodium is present in the final product, it does not become alloyed with the lead. This statement is open to question, as metallurgical literature indicates that sodium may be in solid solution in lead in quantities comparable to those used by the authors. However, whether the sodium is present as an alloy or as a mixture, a much more careful and complete study of physical and chemical properties than is indicated by the authors seems warranted. Concerning the chemical properties, it is interesting to note that the authors conclude that their corrosion tests are favorable. In the long run, extended field experience will be the only sound criterion, particularly as there are so many factors influencing corrosion, other than those investigated by the authors. In particular, there must be assurance that the corrosion data obtained are with samples containing the maximum amount of sodium which may ever be present in actual commercial production.

The authors' test results are a case in point. It is noted that the treated lead had a white deposit which was readily rubbed off, whereas the deposit on the untreated lead had to be removed with a sharp tool. Contrary to accepted belief, this is suggested as an advantage of the treated lead. Lead owes its corrosion resistance to the formation on its surface of an adherent nonsoluble protective film. Chemical corrosion of lead in service occurs only where the conditions are such that the film is either soluble or nonadherent. It would be of interest if the authors would offer some explanation concerning their apparent belief as to the advantage over a strongly adherent film of one which could readily be rubbed off.

Data are given for tensile strength, elongation, hardness, bending tests, corrosion, and creep. Most of the differences shown are within the degree of experimental error found in such tests and are not therefore of any importance other than to show that in these samples the sodium has not impaired the physical characteristics of the lead. As with other metals, the physical properties (ultimate state of equilibrium) of lead are affected by previous physical and thermal history and therefore some variation in these properties in different samples must be discounted unless the treatment of the lead is adequately controlled. On the other hand, if definite changes in the properties of the lead were found, may not these be due to the effect of the sodium as an alloy? If the change is due to the presence of the sodium, how do the properties depend upon the amount of this alloying constituent?

The condition shown in figure 10 is, we believe, not pertinent to present-day practice. With the condition of the nonunion of charges shown in figure 2 and the microstructure of extruded pipe shown in figure 3, it is natural to expect the rupture in the weld shown in figure 10. However, although these conditions may have existed five or six years ago, they are not representative of present-day practice and are definitely not those found in pipe made by many of the methods discussed by the authors.

The authors in their conclusions 3 and 4 state in effect that the structure of lead sheaths produced by their method is uniform and free from flow lines, also that the tendency toward segregation is reduced. It should be borne in mind that structural uniformity and freedom from flow lines are entirely relative, that is, some extrusion processes will result in less prominent flow lines than others, but in all cases flow lines and welds in the charge-weld region can be detected by proper etching methods. Flow lines and welds do not mean that the sheath is defective and tests prove that unless foreign inclusions or dimensional deficiencies are present, the weld usually will be as strong as the metal outside the weld region.

Bearing on etching methods, some experience which we have had in examination of lead sheets containing small quantities of sodium may be mentioned. So far as concerns ordinary behavior in the laboratory, this lead seems to tarnish no more and no less than similar lead without sodium. However, attempts to produce photographs of etched surfaces of lead containing sodium have met with great difficulty due to the tendency of the etched surface to tarnish much more rapidly than with any other lead or lead alloy with which we have worked. Because of this tarnishing or oxidation, photographs of the etched surfaces could be obtained only by a more refined etching technique than required for other lead surfaces.

A word on the control of the process is in order. Definite and simple control is of paramount importance in order to insure perfection in any product. With the vacuum lead press, control is very simple, since a vacuum gauge tells the whole story. If the vacuum is good, the results must be good. In the authors' method, however, the matter of control seems to offer great complications. In this connection, the following points may be considered:

(*a*). The authors indicate that a range of 0.005 to 0.05 per cent of sodium by weight (0.06 to 0.6 per cent by volume) may be used depending upon the impurities of the lead to be treated. Has any precise method been worked out to predetermine the amount required for lead from different sources and containing different impurities?

(*b*). What is the effect on the stabilizing properties if the sodium as poured into the press, varies from the normal amount?

(*c*). Have the studies of the physical and corrosion properties of the extruded lead been correlated with the final amounts of contained sodium or have these studies shown that the control of the amount of sodium used is unimportant?

(*d*). Figure 4 indicates that no means are provided to prevent air from entering the standpipe and to keep the pipe full of lead. Are such means provided? If not, is there not thorough mixing of the lead stream with air during the flow? Will not oxides formed be carried down to the bottom of the charge where they may be caught on the cylinder walls before rising to the top? Does the standpipe remain clean and bright, or does dross from the surface of the poured lead cling to it as it is removed?

D. M. Simmons (General Cable Corporation, New York, N. Y.): I like this Reinitz-Wiseman paper because it is such good publicity for the vacuum press which has been developed and is used by our company in all our paper cable plants. Moreover, the processes described in this paper actually do result in fairly good lead sheathing—though not, we are confident, as good as is obtained with the vacuum press. Consequently, in criticizing parts of this paper, the criticism is made not with the thought of unfavorable comment upon the resultant product, but rather is the criticism directed toward some of the theory and analysis, and toward some of the criticisms of other methods.

These authors have contrasted their present results with the worst results obtained in the past, obtained by methods now generally abandoned, and they would lead the reader to understand that only with their method can good results be obtained. The analysis by which they attempt, in the absence of experimental information, to establish this conclusion is the object of our criticism. Several different aspects of their analyses will be considered.

They describe the use of sodium in connection with lead which they describe as being very bad in respect to dross formation. They have apparently not recognized that excessive dross formation on lead is likely to be a result of contamination by certain metallic impurities present in exceedingly small amounts, such as antimony, etc. The sodium treatment, in addition to reducing the lead oxide formed on the outside of the pigs, happens also to be an effective means of eliminating these troublesome metallic contaminations. The rate of dross formation is so greatly reduced that, by contrast, the metal may, now, seem to retain a silvery surface. Also there may be enough sodium hydroxide formed to contribute a colorless slag still further helping to maintain a bright surface. The "stabilization" of the lead in the melting pot is thus very simply explained.

If these authors were using a lead with such bad drossing characteristics and were using this under conditions such as were common in lead covering some years ago, it is not surprising that there was excessive dross formation and excessive oxide inclusions in the sheath which was produced. The Hill standpipe method would not eliminate the formation of dross as the lead was passed into the cylinder and the excessive dross formed in the melting kettle itself may have found its way into the lead press. Accordingly, there was an exceedingly bad condition which undoubtedly was relieved considerably by the refining operation which this particular lead demanded.

Also, in reference to the appearance of the pigs of remelted lead, it should be noted that commercial lead is normally stored outdoors and subject to considerable surface oxidation and that any properly remelted lead stored inside will be much brighter and more silvery in appearance and will remain much brighter than commercially handled pigs as long as this lead is protected indoors.

Published data indicating the presence of appreciable amounts of oxygen in lead are not accepted by some of the best authorities on the metallurgy of lead. Also, we have authoritative assurance from one large refiner of lead that sulphur does not exist in any of their brands of lead even to the fourth decimal place. In view of the perfectly rational explanation of the experiences with dross formation on the basis of accepted causes (metallic impurities) the authors' explanation of the effect of the sodium is not very plausible.

It is noted that they found the Hill standpipe method of filling inadequate until combined with the sodium refining of the lead. In view of their comment about the bad drossing characteristics of the lead which they were using and in view of certain fundamental difficulties with the standpipe method, it is readily seen why there should be difficulties with drossing. Oxides formed on the surface of the high-temperature molten lead would rise partly to the surface of the molten lead but other portions of the dross would attach themselves to the sides of the lead cylinder. Oxide would also be found on the inner and outer walls of the pipe, especially if this was kept at a high temperature so that the initial pouring would be at high enough temperature to melt the surface of the previous charge. Altogether, there would be plenty of reason to expect trouble with oxide inclusions in spite of any gain which might have been obtained as a result of the surface melting of the previous charge.

Along with the vacuum process of filling the lead cylinder, we have combined other refinements of operation as described earlier by Mr. Faucett in his discussion and resulting in the filling of the cylinder with dross-free lead under vacuum conditions not conducive to the formation of any additional dross.

Howard S. Phelps (Philadelphia Electric Company, Philadelphia, Pa.): This paper is one of many evidences of the excellent work done by the cable manufacturers in recent years in response to the demands of cable users for improved lead cable sheathing. The technique described is undoubtedly capable of producing sheathing of a quality superior to that generally available a few years ago. However, it does not deal with the entire problem. Accordingly, there is reason to doubt if the results from it really are superior to those of several other improved techniques already in use.

In considering the problem of obtaining satisfactory lead cable sheathing, it must be realized that, except in the use of European continuous-extrusion machines, there are two distinct weld areas that are potential sources of trouble. These are: (1) the weld between charges, which gives rise to flow lines; and (2) the die weld, or welds, formed as the two streams of lead meet after passing over the bridge of the die block. The paper by Messrs. Reinitz and Wiseman deals exclusively with the former and ignores the latter.

Analyzing the reasons that support the advantages of uniting the successive charges without welds reveals they are equally important in respect to the die welds. That portion of the description of the new technique concerning the uniting of successive charges stresses the importance of melting the surface of the old charge to facilitate its complete union with the new one. The procedure of introducing the lead of the new charge at a higher temperature, and of directing its flow over the entire surface of the preceding charge, undoubtedly helps accomplish this.

However, the new technique makes no provision for improving the union between the two streams of solidifying lead after passing over the bridge of the die block. Accordingly, the die weld probably remains as weak in the new as in the old technique.

It is fair to conclude that even if one of the two sources of trouble is completely removed, the over-all improvement in quality of the product ceases when the charge weld area has been made as strong as the die weld area. Therefore, it would seem that any technique accomplishing this objective is substantially as good as another.

The foregoing observations apply in particular to the relative merits of the Hill standpipe method of filling the press, rather than to the advantages claimed for the chemically stabilized lead. The latter advantages are not readily apparent from the test data reported.

From table I, the tensile strength of both "electro" lead and "electro" lead plus 0.01 per cent lithium is greater for the sodium treated than for the untreated. However, the reverse relation is true for St. Joseph lead and St. Joseph lead plus 2 per cent tin.

Further inconclusiveness appears in the creep data of figure 7 for the strip specimen at 500 pounds per square inch longitudinal stress and figure 8 for the pipe specimen at 500 pounds per square inch hoop stress. In the former case the treated lead has a greater creep rate while in the latter case it expands less.

These inconsistencies seem to indicate that the differences noted in test data may be a function of the type of lead tested or method of making the test, as well as the process of treating the lead. Certainly the published test data do not convincingly establish the superiority of sodium-treated lead.

The strongest apparent case in favor of the sodium-treated lead is the evidence reported about the type of burst. However, comparing this evidence with that of other independent tests, the reported results are still inconclusive.

For several years, the Philadelphia Electric Company has been studying types of burst in lead cable sheathing, as disclosed by stress-to-failure tests. The results have indicated that extreme caution must be used in drawing conclusions from short-time tests.

It has been found that short-time tests at relatively high stresses result in wedge-shaped fractures that develop from gradual necking down of the lead. On the other hand, for long-time tests at much lower stresses, it can be stated generally that when either die welds or flow lines are involved, the sheaths fail by fractures that develop abruptly without necking down; apparently from a separation of the lead crystals at a weld. For tests at either short time and high stress, or long time and reduced stress, the exact type or character of sheath failures appears to depend on such factors as temperature of sample during test, unit stress in the material, and the composition of the lead being tested, in addition to the success of the weld.

These observations have been found true regardless of the technique for recharging the lead press, or method of purifying the lead. It remains to be seen from actual tests under carefully controlled conditions whether or not the characteristics under discussion will have been improved in the case of sodium lead.

W. H. Bassett, Jr., and C. J. Snyder (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): Messrs. Reinitz and Wiseman have presented a very interesting paper describing the developments which they have undertaken in the Okonite laboratories. In evaluating the results presented, it is suggested reference be made to our paper "Occurrence of Irregularities in Lead Cable Sheathing and Their Relation to Failures," published in the *Transactions* of the American Institute of Mining and Metallurgical Engineers, 1933, volume 104, which is not included in the bibliography in the paper. In this paper we described how uniform cable sheath can be produced by good foundry practice. A number of micrographs of cable sheathing were shown similar to the authors' illustrations, figures 3 and 6. We fail to see any appreciable difference in the structure of the cable sheath resulting from Hill standpipe filling, the authors' figure 6, and the structure of cable sheath produced by our good foundry practice, figures 1, 2, and 3 of this discussion.

We have investigated bottom pouring of lead and have found that it is advantageous in the production of satisfactory calcium-lead-alloy cable sheath or other types of alloys which oxidize rapidly. We have likewise investigated the fusing of the remainder of the old charge to the new charge in the lead cylinder. (For details of movement of lead during extrusion cycle see figure 4 of this discussion.) We have found

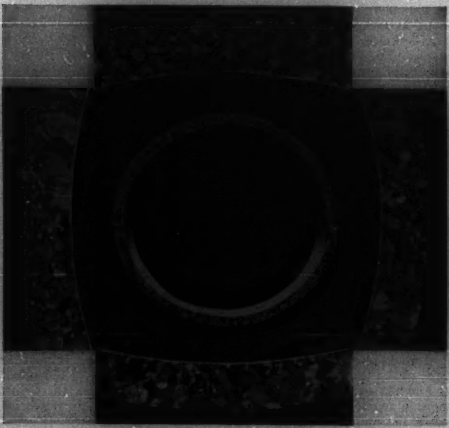


Figure 1. Structure of section of carefully prepared lead tube 21 feet from start of extrusion cycle

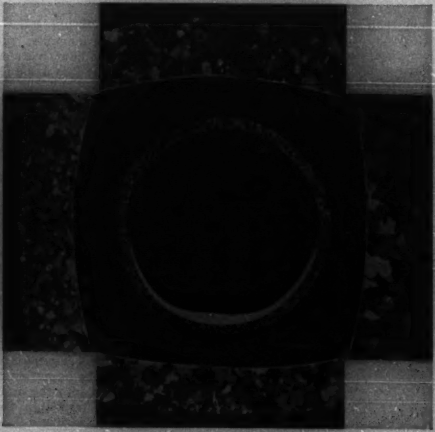


Figure 2. Structure of section of carefully prepared lead tube 28 feet from start of extrusion cycle

that this fusing is not necessary nor essential, nor is the bottom pouring required when producing common or copper-bearing lead cable sheath. Bottom pouring of castings is an old and useful foundry practice, but cannot be used universally. We have found that there are some grades of lead which cannot be handled by the bottom-pouring method as this practice produces at one location in each extrusion cycle a structural condition in the lead sheath which results in abnormally low tensile strength.

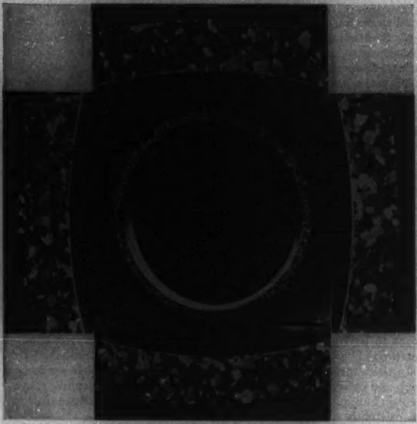


Figure 3. Structure of section of carefully prepared lead tube 77 feet from start of extrusion cycle

The quality of a product can be judged only by the results of tests on the finished material which naturally must be free from weak spots. If, as the authors point out, the two charges do not weld together under the heat, pressure, movement of the lead through the cylinder, and the subsequent hot working in the die block, then laminated and blistered sheath would be produced. With the proper technique at the lead press, optimum results are obtained. The term "good foundry practice" as used by the writers refers to the selection of the lead, the melting conditions, the control of the pouring temperature of the lead, bottom pouring, if required, the skimming or cropping operation, the pressure on the lead during the cooling cycle, and the speed and temperature used during the extrusion cycle. We also see that the lead is not contaminated with oil or grease, air or moisture, and, in other words, that all conditions are correct for the casting of a sound billet in the cylinder so that homogeneous lead sheath is produced.

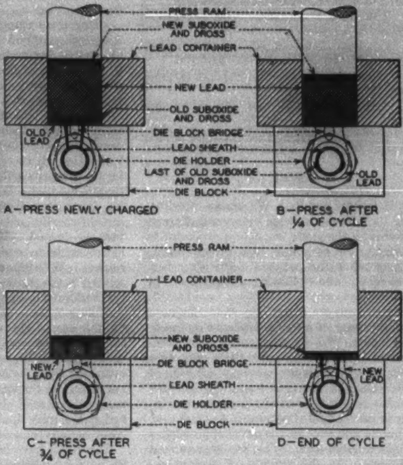
We are particularly interested in the authors' results of internal pressure tests on full-charge lengths of pipe extruded under normal production conditions. In these tests the sample is subjected to 90 pounds per square inch (805 pounds per square inch hoop stress) for ten days, then the pressure is raised ten pounds per square inch every five days until failure occurs. They state that many failures occurred in less than ten days when testing samples produced by their former extrusion practice.

We have made many full-charge bursting tests in the Anaconda laboratories. It has been our experience that all samples break with a knife edge fracture when tested above 100 pounds (1,000 pounds per square inch hoop stress) gauge pressure unless a definitely defective sample was used. We would therefore like to ask the authors what results they would have obtained if they had allowed the tests on their full-charge tests to continue at the original 90 pounds gauge pressure.

In our laboratories, where 2.75-inch outside diameter by 0.133-inch thick full-charge lengths have been tested at normal room temperature (65 to 85 degrees Fahrenheit) under a load of 85 pounds gauge pressure (850 pounds per square inch hoop stress), we have never experienced failures in less than 60 days. It is not unusual to require 90 days in order to reach a failure on a reel of ordinary copper-bearing lead.

We feel that the full-charge bursting tests should be carried to destruction at one pressure, because of the normal characteristic difference in appearance of lead failures at 90 pounds and 130 pounds per square inch pressure (900–1,300 pounds per square inch hoop stress). We have made tests where the pressure was increased in steps and have always obtained knife-edge fractures. It is possible therefore that the authors' test method may give misleading results.

The *Journal* of the Institute of Metals shows that sodium has been used in treating lead for nearly 20 years. In 1920 Thieme[1] reported the hardening of lead by the addition of sodium. In the same year Jones[2] reported good corrosion resistance in a chemical plant by the addition of sodium to lead. He attributes the good effect of sodium to the removal of oxide. Calingeart and Boesch[3] in 1923 report a correction to the lead sodium equilibrium diagrams published by Mathewson[4,5] in 1905 and 1906. A recent patent[6] issued to Magill of Du Pont covers the addition of sodium to lead in order to produce an alloy which is more rapidly corroded than pure lead. The application of this alloy is in the manufacture of white lead or any other operation where the lead is to be dissolved by some corroding agent.



A—PRESS NEWLY CHARGED

B—PRESS AFTER ¼ OF CYCLE

C—PRESS AFTER ¾ OF CYCLE

D—END OF CYCLE

Figure 4. Probable movement of lead oxide and dross as indicated by following sections from extrusion cycle

In our early studies of the production of lead cable sheath, we recognized that certain brands of lead made more dross on remelting than others. This subject was discussed with the Anaconda refinery, and, in 1932, they developed a special grade of lead which gave a bright surface on remelting. The refining experts report that the importance of sodium is in the removal of impurities from the lead. On remelting lead which has a relatively low antimony content, we have found that a mirror surface can be obtained. For example the difference in behavior of lead pigs *A* and *B* illustrated below can be readily noted:

Pigs Marked	Per Cent				
	Lead	Copper	Bismuth	Antimony	Silver
A	99.850	0.067	0.072	0.0096	0.0013
B	99.870	0.061	0.065	0.0007	0.0029

In working with calcium lead in 1935, we found that the dross-forming elements were removed from the lead and that on remelting the lead surface remained bright in the same manner as reported by the authors with respect to sodium.

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5. Mathewson, *Z. anorg. Chem.*, volume 50, 1906, page 171.

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B. B. Reinitz and R. J. Wiseman: Reviewing the comments made by several of those discussing the paper, we note a similarity in points raised as well as lack of understanding of what the paper actually describes. Therefore, the following restatement of several facts may help to correct a misinterpretation of the paper and thus eliminate the necessity of making duplicate replies to comments. We do not consider the addition of sodium in the quantities used as a refining or purification process or alloying of the lead. In the first place, the amount used is very small and the quantity remaining in the lead for sheathing is so small that it has no influence on the physical properties of the original lead. This has been proved by tests. As the amount of sodium remaining in the lead is of the order of 0.001 per cent to 0.002 per cent and far less than that of other foreign metals which the lead may contain in the brands of commercial lead used at present for cable sheathing, it is hard for us to understand why it is thought we are alloying when, as we state above, our process does not change to any extent the physical properties of the lead. In other words, we do not contaminate the lead, we do not endeavor to make the lead harder, nor do we increase its vulnerability to corrosion. As the cable specifications allow a total of 0.15 per cent of metallic impurities in the lead for sheathing purposes, and we normally are well within this value, the process does not cause the chemical composition to be materially changed from a commercial viewpoint. We do not try to improve the chemical composition, that is, by the addition of sodium, no attempt is made to reduce the quantity of other metals present in lead as some refiners have done in the past when they added as high as three to seven per cent of sodium and using entirely different processing methods. We purposely use only sufficient sodium to cause the oxides and sulphides which are already present, or the oxides formed during heating, to decompose or to be thrown out of solution. We call these the deleterious impurities, as distinguished from the metallic impurities. Our treatment is quite limited in this respect as otherwise we would get into the same difficulties of corrosion that others have experienced where larger quantities of sodium are used. It was the discovery that it is the extremely small amount of sodium which prevents oxidation and removes the oxides and sulphides which makes this process of value. Metallurgically, purification and refining consists in reducing the amount of metallic impurities. We do not consider our process as such, but rather look upon it as a cleansing and stabilization, what might be called a scavenging of the nonmetallic impurities which, heretofore, have not been properly recognized as one of the major causes of the defects in lead sheathing.

Using this improved lead in combination with the method described for uniting charges, a better lead sheath is obtained. Our tests and customers inspection reports extending over a period of years have all indicated this. We believe, and, as stated in the paper, "that this method or some other method which brings about the same results, namely elimination of flow lines and laminations, is necessary," if we are to hope for lead sheaths which will give a longer life than in the past. Some of the methods used by other manufacturers only accomplish certain specific ends instead of everything that is desired. The vacuum press and hydrogen-flame treatment are notable in this respect. With the treating of the lead described in the paper not only is oxidation of the lead both in the melting pot and during the filling of the cylinder prevented but also, by the use of the standpipe, we actually cause the successive charges to be 100 per cent united. This has been proved by many tests over several years. In these tests, the full charge in the lead block and cylinder is removed as a solid block, cut in half, planed, and etched with the lead consistently giving cross sections such as shown in figure 5.

Comments have been made that perhaps we have been using inferior brands of lead and that something must be done about it. Such is not the case. Like other manufacturers we purchase the standard commercial brands of lead required to meet ASTM specifications. For the purpose of this paper, in order to emphasize what can be accomplished by such treating of the lead, we took (1) St. Joseph lead which everyone acknowledges is one of the highest grades of commercial lead, and which drosses to the same degree as other brands, and (2) electrolytic lead which is of a higher commercial metallic purity but oxidizes more readily. This shows that this treatment greatly improves all kinds of commercial lead from the standpoint of oxidation and at the same time does not adversely upset the other properties of the lead.

It has been suggested that a more elaborate list of references be given. As with most papers, more could be given if one wishes to take the space for them. We included those which we felt would cover the subject matter sufficiently for those who cared to go into it further. We wish to acknowledge the paper by Mr. Bassett in the *Transactions* of the AIME which reviews very well the lead problems which have confronted cable manufacturers in recent years.

We agree with Mr. Halperin that imperfect welds and nonmetallic inclusions usually constitute the initial set of causes for sheath failures. We remove the nonmetallic inclusions by the stabilization of the lead and eliminate the imperfect weld by our method of filling. What is at times considered as a defective die weld might be a poor charge weld where the laminations resulting from the nonunion of charges abutt the die weld. At times the charge weld is only about 1/64-inch from the die weld and cannot be seen except by magnification after etching. In view of the above it is many times mistaken for a die weld.

It is recognized that more or less sound cable sheaths are extruded now by some cable manufacturers. However, it is the consistency of good foundry practice throughout the cable length that is essential, and not at the ends or scattered along the reel lengths. By the method described in the paper we are able to control, at all times, both the condition of the lead and the elimination of flow lines and laminations.

We are not able to agree with Mr. Bassett that the fusing of the successive charges is not necessary nor with his statement that some grades of lead cannot be handled by this method. Our studies have clearly shown complete fusing is essential if it is desired to eliminate flow lines. Careful examination of figure 1 which Mr. Bassett submits will disclose a flow line in the so-called critical region where metal from the vicinity of the contact of the two charges in the cylinder extrudes as pipe. We have used different grades of lead and tests have not shown low tensile strength.

In a manufacturer's laboratory it is necessary to obtain results in a reasonably short time, though long enough to be able to produce results comparable to service conditions. We believe this is accomplished by the pressure-time step test which we make on full-charge lengths of empty pipe, as, early in our studies, we obtained small slits in the pipe such as occur in service. The time taken for test is empirical and as the same method is used all the time we are enabled to compare the results. Answering Mr. Bassett on what would have been obtained if we had allowed our tests to continue at the original 90 pounds gauge pressure, we would expect it to continue for months before rupturing. Leaving samples under test so long does not help in completing a research in a reasonable length of time, nor is it an aid to learning whether improvements are being made. After a test has been completed and microscopic examination of the lead in the vicinity of the rupture has been made one can tell whether there were any defects in the pipe to have caused an earlier rupture than would otherwise occur.

Our review of the literature on the use of sodium in the treatment of lead shows that it has been used for about 50 years. However, the amount, form, and method have been distinctly different from the procedure described in the paper. Moreover, purification of the lead from foreign metals was attempted rather than the cleansing or stabilization from nonmetallic inclusions. Refining experts were only interested in removing metallic and not nonmetallic impurities, and, therefore, failed to appreciate how small quantities of sodium could remove oxides and sulphides, equally as objectionable as metallic impurities.

We do not agree with Doctor Simmons regarding the effect of metallic impurities on dross formation. The amount of sodium that we use does not eliminate metallic contamination nor is sodium hydroxide formed to contribute a colorless slag.

Where cable manufacturers may be criticized, is for their failure in the past to recognize that all brands of lead will dross. It seems to have been accepted as a natural evil of little importance (the drossing is hastened in the melting pot and according to good foundry practice all manufacturers periodically cleaned out the red oxide) instead of finding means for the removal of this dross or oxides contained in the lead, as received, and seeing that further oxidation was prevented. Our process has accomplished this and it seems evident that no other process does the same. There have been improvements by some manufacturers in the lead-press operation, but not enough, and with no such treatment of the lead as the paper discusses.

The storing of lead outdoors and surface oxidation is not the whole story. Allow remelted lead to stand around and it will soon lose its luster, but this is merely a surface condition as there are still oxides dispersed through the lead.

The table herewith may help to show that sodium in the amounts that we use does not throw out other foreign metals from lead, that is, it is not a purification process. A quantity of antimony (0.07 per cent) was added to lead which was then heated to 850 and 1,200 degrees Fahrenheit, held at these temperatures for 30 minutes, and then checked for antimony content. The experiment was repeated for lead which had been treated with 0.02 per cent sodium. The results are as shown in Table III.

The time of treatment in the factory is much less than 30 minutes. It is the high temperature of 1,200 degrees Fahrenheit and not the sodium that reduces the antimony content.

We have relied too much on the statements of metallurgists on what is good or bad in lead. They confine themselves to the metallic phase of lead rather than realizing that the nonmetallic phase of lead may also be important. This was clearly shown by a refiner when he was told that we found sulphides in his lead. He first disagreed but after checking a sample confirmed our findings. Upon request that the sulphides be eliminated we were informed that commercially it would be too costly. That is, the refinement would not be justifiable in view of the small quantity of lead used for cable sheathing purposes as compared to general trade consumption. This seemed a logical reason and we thereupon set up our premelting pot to eliminate the oxides and sulphides.

We do not say that the Hill standpipe method of filling is inadequate unless its use is combined with the sodium treatment of lead. Rather, the paper shows there is a distinct improvement in the mechanical properties of a cable sheath through use of the standpipe but that it is desirable to go further by eliminating the oxides and sulphides in the lead, thus accomplishing a thorough job instead of a partial one.

We are in accord with Mr. Faucett that to overcome troubles it is necessary to eliminate the source of the trouble. The nonunion or nonfusing of successive charges is the cause of flow lines and laminations. These are eliminated by the use of the Hill standpipe method of filling the cylinder. The presence of oxides and sulphides in the lead are the source of weakness in crystal structure and our treatment of the lead overcomes this trouble. We think Mr. Faucett did not consider that the amount of sodium retained in the lead is far less than the amount of bismuth, antimony, arsenic, and copper which may be present and therefore the resulting lead composition is about the same as before it was treated.

There was no need of mentioning ram lubrication in the paper. We have not experienced the difficulties some others have had with it, even before the development of our new processes.

Chilling of the lead ram and cylinder walls is not of importance nor does chilling of the surface of the previous charge cause oxidation. Heat is the cause of oxidation, particularly if the lead is in a molten state. Our treatment prevents the latter from occurring while the lead is being poured into the cylinder and that is why it is so effective. Fundamentally, it is desired to have the successive charges to unite, heating of the ram or cylinder walls will not accomplish it. The surface of the residual charge must be melted and what better and easier way to do this than with the new charge so that the two charges are united as one. We question that the vacuum-press method unites charges and would be interested in seeing slugs similar to those shown in figure 5, to prove that it does.

As we view it, if an alloy of lead is formed by the addition of sodium, we should expect some physical or chemical change in the lead. It has been shown that we do not change the physical properties and, as far as we can tell, it is not in chemical combination with lead. The composition of the resulting mixture of the lead is still well within that expected in commercial lead, which may contain as much as 0.06 per cent copper, yet we do not consider this lead as an alloy in the commercial sense.

The amount of physical data which has been given on stabilized lead in the paper represents only a part of the tests already made and we consider these data sufficient to show that the process does not adversely change the properties of the lead. Regarding corrosion, we are fully cognizant that there are many factors which influence corrosion. It is also shown that the sodium treatment of lead does not intensify the corrosion rate of lead in solutions which are representative of those that occur in service. If unduly large quantities of sodium were used in lead, corrosion would then be very severe. The amount of sodium in the samples used for corrosion tests was the maximum which will be present in actual commercial production. We have had samples of pipe under study similar to field conditions for about three years and so far there has not been a single case where trouble occurred.

Referring to Mr. Faucett's comments on the type of deposit on the lead when subjected to steam and the solubility of the corrosion film: it is our understanding that where severe corrosion is encountered, neither a soluble or insoluble, adherent or nonadherent film will protect a lead sheath. It is necessary to use protective coverings such as rubber or synthetic plastics. At the same time, where the corrosion films are of the nonsoluble, adherent type, they are generally embedded not as uniform films but as segregated incrustations, and thus pronounced pitting takes place which hastens failure.

We agree with Mr. Faucett that the data on physical properties are within the degree of experimental error and that is why it is claimed that the properties of the treated lead are not changed. We are not trying, for example, to obtain higher tensile strength but are cleansing the lead before it is used for sheathing in order that the resulting pipe will be more uniform in its characteristics throughout its entire length than is possible with lead not so treated. We are able to control very easily the treatment of the lead and none of the difficulties anticipated by Mr. Faucett has been experienced.

We do not agree with Mr. Faucett that the microstructure of extruded pipe shown in figure 3 is not representative of present day practice as a whole. Some cable manufacturers have improved their previous practice but we have been able to examine sheaths occasionally and have found flow lines even in pipe from the vacuum press.

We wish to correct Mr. Faucett's statement that we have not investigated the vacuum press. We do not use the vacuum press but we have thoroughly investigated it and studied it in operation. We decided that it did not accomplish what we wanted, namely, uniting of the charges. The ram and cylinder operation is complicated and the melting pot, although good, does not remove sulphides and other deleterious impurities, such as allotropes of lead formed in the initial stages of oxidation. These have specific gravities close to that of lead and are dispersed through the lead.

We wish that the alkalies used in standard refining methods were effective in removing sulphur as that would save the expense of using a separate treating operation.

It is agreed that oxygen is not present in significant quantities and the paper does not make this statement. If oxygen were present, it would immediately react with molten lead to form lead oxides. It is pointed out in the paper that, after the treatment of the lead with sodium, there remains about 0.001 per cent sodium in the lead. This prevents further oxidation while the lead is in the melting pot and while being poured into the cylinder, even though it is exposed to the air.

No difficulty has been experienced in etching or taking photographs of our stabilized lead with the same etching solutions as used for nontreated lead. Rather than tarnishing more readily, the surface of the treated lead remains brighter for a much longer time.

As before stated, we use the commercial brands of lead, and, like others, did not ap-

Table III

Per Cent					
Antimony Added	Sodium Added	Antimony Retained	Sodium Retained	Heated to Deg F	Duration of Heating (Minutes)
0.07		0.07		850	30
0.07		0.035		1,200	30
0.07	0.02	0.07	0.001	850	30
0.07	0.02	0.035		1,200	30

preciate that the oxides and sulphides present were in sufficient quantities to be objectionable although actually of small magnitude.

Through our method of filling no difficulty is encountered in obtaining a completely fused area. A permanent bond, clear to the edge of the cylinder and for about four to six inches into the old charge, results from this procedure, as is shown in figure 5, and by the numerous etched slugs on exhibition in our laboratory.

References 11, 12, and 13 cited in the paper clearly show that certain oxides are to some extent soluble in molten lead. Naturally, it is obvious that it is only on solidification that segregation along boundary lines will take place.

It is well known that the presence of metallic impurities in lead will decrease the tendency to form dross. For instance, lead containing small quantities of copper, tin, or antimony will definitely form less dross for a given time and temperature than lead free from metallic impurities as reported by G. O. Hiers. Thus, if we are to accept Mr. Zickrick's statement that the lead used by D. M. Smith contained more metallic impurities than in use at present by the cable manufacturers, then the tendency to dross, especially under reduced pressure, should have been eliminated at least after the first heat and vacuum treatment.

It is true that the purer the lead the lower will be the tensile properties and the larger will be the grain size and the lower the resistance to fatigue. However, by our technique of sodium treatment, metallic impurities are not removed. The process is a stabilizing one and not a metallurgical refining process. Therefore, the tensile strength of sodium-treated lead is the same as for untreated lead while the resistance to creep is somewhat greater owing to the more uniform and better bonding of the crystals.

The tensile-strength figures given for treated and untreated lead in table I are all very similar and within five per cent, except for the electro plus 0.01 per cent lithium. Tensile tests on strips cut from lead pipes cannot be expected to check any closer than 5 per cent.

The treated electro plus 0.01 per cent lithium lead has a higher tensile strength than the untreated, probably due to the formation of a binary alloy of sodium and lithium.

Regarding figure 7, it is well known that when the creep rate of two leads is compared, the stress at which it is tested must be taken into account. Thus the creep rate of noncopper-bearing lead is much greater than that of copper-bearing lead when tested at 850 pounds per square inch, but the creep rates are equal at some lower stress.

The 500-pound-per-square-inch creep tests of figure 7 and figure 8 are not strictly comparable, one being made on strip and the other on pipe. In the pipe tests the lead is stressed in at least two directions, but in only one direction on the strip tests. The simple pipe formula was used for calculating hoop stress and not the Philadelphia Electric Company formula.

We agree with Mr. Phelps that different results may be obtained when making long-time and short-time tests. We do not consider our tests as short-time tests because, even with the pressure steps used, they have extended over periods of a month or more.

Instruments and Methods of Measuring Radio Noise

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Synopsis: This paper embodies the relevant agreed recommendations of the Joint Co-ordination Committee on Radio Reception of EEI, NEMA, and RMA, as to the nature, essential characteristics, and performance of an instrument for the measurement of radio-noise voltages. It further gives detailed descriptions of the recommended practices for measuring radio noise directly from low- and high-voltage apparatus, for making noise measurements along overhead lines, for determining broadcast field-strength levels, and methods of collecting data for the establishment of radio-noise standards.

RADIO-NOISE effects are produced by extraneous electrical fields associated with transient conditions in an electric circuit. In order that apparatus producing these effects can be treated and described in precise terms and the most satisfactory method of radio-noise suppression employed in a given case, it is necessary that there be some means of measuring the radio-noise voltages that are significant in relation to radio reception. It is also desirable that there shall be a national understanding and agreement as to the method of measurement.

The noise voltage produced by electrical apparatus on a given system depends upon the high-frequency voltage generated, the internal impedance of the apparatus, and the character and impedance of the load. This voltage is propagated by conduction, induction, radiation, or a combination of these. All metallic materials, whether used normally for electrical systems or for other purposes, may conduct high-frequency energy.

The radio-noise effect of electrical apparatus on a receiver antenna is influenced by most of the above factors. No definite method has been developed that will permit the calculation of voltage on a receiver antenna when the noise voltage produced by electrical apparatus is known.

Through the co-operative efforts of the Edison Electric Institute, National Electrical Manufacturers Association, and Radio Manufacturers Association, the measurement of radio noise has been placed on an engineering basis. In 1932 this committee adopted specifications for a radio-noise meter and methods of measurement. These were published in NELA Publication No. 32 in 1933. These specifications were revised and published in EEI Publication C9, NEMA Publication 102, and RMA Engineering Bulletin No. 13 in 1935. Due to the advances in the radio art and the increased use of the radio-frequency spectrum, it has been necessary to extend and bring up to date these specifications. All the field data in this paper were taken with a meter built according to the old specifications. However, similar data taken with an instrument according to the new specifications would not be sufficiently different to change the interpretation of the results.

The development of these specifications has resulted in the application of engineering principles to the reduction of radio noise produced by certain types of electrical apparatus. The continuation of this work shows promise of producing more beneficial results in the future.

Radio-Noise Meters

A meter for measuring radio noise should give results which are comparable with the effect of the noise as heard in the loudspeaker of a radio receiver, and at the same time should be accurate and rapid in operation.

Radio noise from different types of apparatus differs in character; some types of noise have high amplitude but are of short duration, whereas others are more nearly sinusoidal in form. Furthermore, the characteristics of the radio receiver upon which the noise impinges will have an effect on the noise characteristics, so that the wave shape of the noise pulse at the output of the receiver may

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differ materially from that at the input.

A noise meter designed to measure the characteristics of the noise pulse as it exists at the input to a radio receiver would be a complex and cumbersome piece of equipment because it would be required to measure noise potentials of the order of microvolts and would have to be capable of amplifying all types of pulse shapes without distortion. Such a device would not be suitable for field measurements and furthermore the results would require correlation with observations of the interfering propensities of the many types of noise.

The most practical type of noise meter is one which is essentially similar to a radio receiver, with indicating means in the output. Such a device may be made convenient to operate, portable, and reliable. The indications of noise intensity for various types of noise on this type of noise meter depend upon the design constants chosen, but it is preferable to have a simple indication such as this type of noise meter provides and then to classify types of noise-making apparatus if necessary, rather than to use a complex noise meter which does not alter the noise pulse, as in the latter case classification of noise types is replaced by correlation of pulse shapes.

INDICATING MEANS

There are three general types of output indicators which may be used:

1. **Oscilloscopes**
2. **Wave-form analyzers**
3. **Indicating meters**

Oscilloscopes and wave-form analyzers are useful for theoretical investigation of noise, but do not provide ready numerical means of expressing results, and are more difficult to use than an indicating meter. Indicating meters are convenient and reliable so are to be preferred for general use.

FREQUENCY RANGE

The noise meter should cover the entire radio spectrum of interest for reception, which is from approximately 150 kilocycles to at least 100 megacycles. However, the design problems for frequencies above 20 megacycles differ materially from those for frequencies below that value so that it is more practical to design two separate instruments, one for frequencies below 20 megacycles and one for frequencies above 20 megacycles. This discussion will be confined primarily to the meter for frequencies of 150 to 350 kilocycles and 540 to 20,000 kilocycles. The wide frequency range dictates the use of the superheterodyne circuit, and because the intermediate frequency thereof will probably be the RMA standard value of 455 kilocycles, there will be a range on either side of this frequency which will not be covered, namely 350 to 540 kilocycles.

DETECTORS AND INDICATING METERS

Since the noise is transmitted through the instrument at radio frequencies, detection is necessary before applying the resultant audio-frequency pulse to the indicating meter. The indicating meter may be arranged to read root-mean-square, average, or peak value of the wave. It is generally recognized that on noise pulses of short duration, meters reading average or root-mean-square do not indicate values as high as those disclosed by listening tests. A peak or quasi-peak indicating device has been found to give meter readings more nearly proportional to the auditory interference experienced. Since a peak-reading meter is desired, the meter may be in the detector circuit, thereby eliminating audio amplification. The use of a meter in the detector circuit has the further advantage that calibration of the device can be made by application of an unmodulated carrier-frequency voltage, thus eliminating the necessity of modulation thereon, and eliminating the necessity of determining the modulation factor.

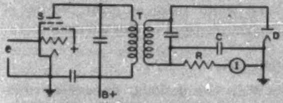

Figure 1. Detector circuit

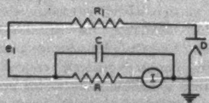

Figure 2. Equivalent detector circuit

A detector for noise measurement should meet special requirements, but with due regard for practical design limitations. A typical diode detector, which is the most satisfactory and reliable type detector, is shown in figure 1. The radio-frequency voltage, *e*, noise or signal voltage as the case may be, is applied by preceding amplifier circuits to the input of the final intermediate-frequency amplifier tube *S*. Tube *S* in turn impresses the voltage on diode *D* through the intermediate-frequency transformer *T*. In the diode circuit are a resistance *R*, a capacitor *C*, and the current-indicating meter *I*. Tube *S* and transformer *T* can be replaced by an equivalent voltage e_1 and resistance R_1 as shown in figure 2. Capacitor *C* is charged by e_1, through R_1 and discharges through *R*. The time constant on charge is then R_1C and on discharge *RC*.

If voltage e_1 is a suddenly applied potential, capacitor *C* will charge to 63 per cent $(1-1/\epsilon)$ of e_1 in time R_1C. On discharge the voltage of *C* will drop to 37 per cent $(1/\epsilon)$ of its initial voltage in time *RC*. Now if the discharge time constant *RC* is long in comparison with the charge time constant R_1C, the voltage of *C* will build up to very nearly the peak value of the applied voltage. In considering the mechanism of build-up of voltage to the peak value, it should be borne in mind that noise voltage consists of a series of impulses, the measured voltage reaching essentially the peak value after the first few impulses. In the very rare case of noise consisting of a single pulse, or pulses with a repetition time longer than the discharge time constant of the metering circuit, a value considerably less than the peak would be indicated. However, experience would tend to show that the disturbing effect of such noises on the listener is less than would be indicated by their peak amplitude.

In determining the time constants of the metering circuit, consideration must be given to meter characteristics, noise characteristics, and to circuit design limitations. An indicating meter which is too rapid in action is expensive and is difficult to read, whereas a meter with too long a time constant makes circuit design difficult. A meter with a time constant of 200 to 400 milliseconds is suitable, the time constant of the meter for this use being considered as the time required for the meter to deflect from zero to an equilibrium position upon the application of a steady current equal to about two-thirds full-scale value. This corresponds to a meter with a natural period of 0.5 to 0.7 second with a damping factor of 10 to 100 determined according to American Standards Association standard methods.

It is desirable that the time constant of the noise meter be determined by the circuit values rather than by indicating-meter constants, because the former are more readily determined and may be held more closely. By making the circuit charge time short and discharge time long in comparison with the indicating-meter time constant, this may be accomplished.

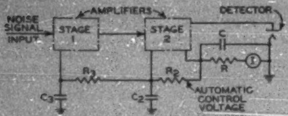

Figure 3. Logarithmic amplifier

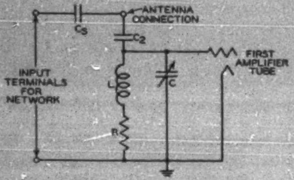

Figure 4. Input circuit

It is difficult to secure an equivalent resistance on charge (R_1 of figure 2) of less than 20,000 to 30,000 ohms with receiving-type vacuum tubes, whereas fixed resistors for discharge (R of figure 2) become difficult to determine accurately and are not stable in value above approximately 5 megohms. This, therefore, in conjunction with the indicating-meter constants determines the permissible range of values for C and for charge and discharge time constants. It should also be borne in mind that the peak voltage is given by IR so that for any value of voltage, the lower we make R, the greater I will become, thus permitting use of a less sensitive indicating meter. In some cases where it is desirable to use a relatively insensitive meter and a high value of R, a d-c amplifier may be used. From the above considerations, a charge time constant of the order of 10 milliseconds and a discharge time constant of the order of 600 milliseconds are indicated.

The following tabulation shows several possible combinations of circuit values which result in these time constants for charge and discharge.

R (Megohms)	C (Microfarads)	R_1 (Ohms)
5	0.12	83,000
4	0.15	67,000
3	0.2	50,000
2	0.3	33,000
1	0.6	17,000

If it is desired to measure the signal intensity of a broadcast-station carrier or to use the audio-frequency noise output for listening or other type of indication, the time constant RC should be of the order of 0.1 millisecond, which may be done by appropriate reduction in the value of C when such measurements are required.

SELECTIVITY

Steep-wave-front disturbances involve a broad band of frequencies for their transmission and when transmitted through a selective amplifier undergo a change in shape. The energy content of a high amplitude pulse of short duration is unchanged by a selective amplifier, but its maximum amplitude is decreased and its duration correspondingly increased. Since this shape alteration of sharp pulses takes place in radio receivers in proportion to their selectivity, in order to correlate noise-meter indications with radio reception, it is desirable to have comparable selectivity. The over-all pass band of radio receivers, from antenna to loudspeaker, varies widely, from about 1,500 cycles in the lowest-priced receivers to 6,000 or 8,000 cycles in the case of receivers of high fidelity. Since good-quality receivers merit additional consideration, the noise meter should have somewhat greater pass band than that of the average receiver. The high-frequency end of the pass band is of prime interest, frequencies lower than 60 cycles seldom being of interest in noise studies. The noise meter should then transmit frequencies of 4,000 to 5,000 cycles, which means the selective circuits should have a band width twice as great because of double-side-band considerations.

SENSITIVITY

While it is desirable to have signal intensities of five to ten millivolts per meter from broadcasting stations for good reception, there are many localities where no signal over 500 microvolts per meter is available. A signal-to-noise ratio of 30 decibels for average program to average noise is generally conceded to constitute a minimum ratio for acceptable reception so that the noise meter must be capable of measuring noise voltages of the order of ten microvolts. It is seldom that noise voltages of more than 100 millivolts are of interest, which determines the upper limit of noise-meter calibration. However, such voltages should not undergo any limiting action by the noise meter, nor should such voltages cause generation of spurious responses, so the amplifiers of the noise meter should be capable of handling peak values up to about ten volts input.

METER SCALES

A scale on the indicating meter which is logarithmic in character has several advantages over a linear scale. In the first place it fulfills the psychological conditions of Weber's law and in addition makes the use of fewer attenuator taps possible, with resultant increase in simplicity of construction and use.

The logarithmic characteristic is best secured by electrical means. The deflection law of the meter itself is linear, the angular deflection of the pointer being proportional to the current through the meter.

The automatic-volume-control system as used on radio receivers provides a ready means of obtaining such a characteristic. By the use of remote-cutoff vacuum tubes in the noise-meter amplifier stages, a sufficient amount of control can be applied to each stage to obtain a useful input range of about 40 decibels on each meter scale range without amplifier overload or distortion.

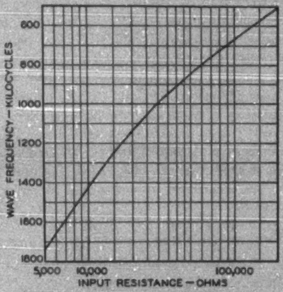

Figure 5. Noise-meter input resistance with circuit of figure 4

In figure 3 is shown the method of obtaining logarithmic indication. The direct current developed by the detector is filtered by R_2C_2 and R_3C_3 to eliminate any intermediate-frequency or noise component and is then applied to bias the amplifier stages. The time constant of the filter should be of the order of 200 milliseconds so that it will not influence the meter indication, the time constant for noise measurement being that of the detector circuit only.

The meter scale is calibrated logarithmically in microvolts or linearly in decibels above one microvolt and the value of noise input in microvolts is thus read directly on the indicating meter.

The meter scale is useful over a ratio of inputs of about 100 to 1 by virtue of the logarithmic system used, but in order to cover the entire desired range of 10 to 100,000 microvolts, multipliers of 10 and 100 should be used. These multipliers are in the form of attenuators for the noise input, and in order that none of the tubes be overloaded, must precede the first tube. The attenuator may be of the resistance type, or may be of the capacitance or mutual inductance type, any one of which can be made to have the desired attenuation independent of frequency.

INPUT CIRCUITS

The input circuits should be suitable for use either with a vertical-rod antenna for measuring noise or signal field intensities or as a voltmeter. In either case the impedance of the input system should be high, so that the voltage existing on the antenna or across a standard coupling network will not be affected by connection of the noise meter. One type of input circuit which fulfills these requirements is shown in figure 4.

The antenna should be a relatively short vertical rod with an effective height of one-half to one meter which means a physical height of approximately twice that value. Such an antenna has an inherent capacitance of 10 to 20 micromicrofarads, and C_s should have the same capacitance so that the input circuit will be tuned correctly when the meter is used either with the antenna connection or as a voltmeter.

The impedance of this type input system is a pure resistance when tuned for maximum response as the *LRC* circuit under such condition becomes an equivalent resistance and inductance in series, the value of the inductive reactance being equal to the capacitive reactance of C_s and C_2 in series. In order to secure a suitable high impedance, a small capacitance C_2 is placed in series with the circuit. The capacitance of C_s and C_2 in series should be of the order of three micromicrofarads. If we assume this to be three micromicrofarads and L to be 166 microhenries, and the power factor of L to be one per cent, at broadcast frequencies, the equivalent resistance of the input circuit as a function of frequency will be as shown in figure 5.

CALIBRATION SOURCE

The noise meter should be battery operated for field use, which means that the gain will change with battery usage as well as due to aging of tubes and effects of atmospheric humidity. In order to make the instrument readings accurate, the gain must be standardized each time the instrument is used. The gain standardization requires the incorporation of a self-contained calibrating source. The primary calibration should be performed by means of a standard signal generator but a comparison standard must also be included in the noise meter itself.

There are three possible types of calibration sources which might be used, an internal radio-frequency oscillator with a meter to indicate its output, the inherent thermal agitation noise voltage of the noise-meter input circuit, or the shot noise from a saturated diode. The latter two are simple to incorporate and depend only upon constancy of resistance of the input circuit. For an input circuit resistance of 10,000 ohms and a band width of 6,000 cycles, the thermal agitation voltage is 1 microvolt at a temperature of 20 degrees centigrade, whereas the shot noise for a current of one milliampere is 14 microvolts, under the same conditions, so is easier to apply.

The shot noise voltage is given by

$$V_s^2 = 31.8 \times 10^{-20} i R^2 F$$

at a temperature of 20 degrees centigrade where

V_s is shot noise voltage
i is current in amperes
R is input circuit parallel equivalent resistance in ohms
F is frequency pass band in cycles

The circuit of figure 6 may be used for the calibration source. *LC* is the input tuned circuit and R is a resistance shunted across this circuit during calibration so that the total circuit resistance will be more uniform with frequency. B is a battery of sufficient voltage so that the diode draws saturation current. R_1 is a resistor which varies the diode filament temperature until the space current as read by meter I reaches the calibration value. Meter I may be the output indicator of the noise meter switched to the position shown in figure 6 for calibrating purposes. The amplifier gain is then adjusted to standard value by variation of screen or initial bias potential.

In the design of such instruments, due consideration must also be given to shielding, image-frequency response, and intermediate-frequency response ratios, so that the noise indicated is due only to that existing at the frequency to which the meter is tuned.

Methods for the Measurement of Radio-Noise-Influence Voltage Produced by Electrical Apparatus

The term "noise-influence voltage" will be used hereafter to describe the radio-noise voltage measured at the terminals of electrical apparatus when connected to a coupling network. This term is used in order to make a definite distinction between this voltage and the radio-noise voltage that may appear at the input terminals of a radio receiver. The noise voltage that appears between the antenna and ground will hereafter be referred to as the "noise voltage."

NOISE-INFLUENCE VOLTAGE OF LOW-VOLTAGE DEVICES

The noise voltages produced by low-voltage electrical apparatus are generally caused by the transmission of high-frequency currents along the wires or conductors connected to the apparatus. These currents are due to the electromotive forces produced by the apparatus at its terminals. As such apparatus is generally grounded or has a substantial capacity to ground, the currents will not be confined to the conductors connected to it but will in addition be propagated via ground. It is, therefore, necessary to consider the disturbing currents as consisting of two components, one corresponding to the electromotive force acting between the various conductors, hereafter referred to as the line-to-line noise-influence voltage (E_L) and the other

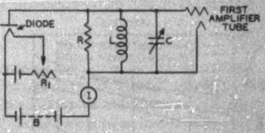

Figure 6. Calibration method

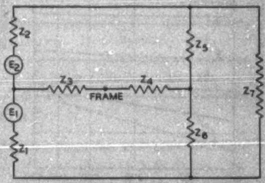

Figure 7. Equivalent circuit showing the various impedances involved when a universal motor is connected to a line

Z_1, Z_2—Impedance of field windings
Z_3—Capacity of field windings and armature to frame
Z_4—Capacity of frame to ground
Z_5, Z_6—Impedance from line to ground
Z_7—Impedance from line to line
E_1, E_2—High-frequency voltage developed at brushes

corresponding to the electromotive force acting between the ground as one pole and the conductors jointly as the other, this being hereafter referred to as the line-to-ground (E_0) noise-influence voltage.

Effect of Variation in Termination on the Noise-Influence Voltage

The amplitude of the noise-influence voltage produced by electrical apparatus is determined by the external terminating impedance as well as its internal impedance. This may be seen by referring to figure 7 which is a simplified circuit for the various impedances involved when a universal motor is connected to a supply line. It will be noted that the line impedance network is represented as a delta system. In service conditions, the voltage induced upon a receiver antenna is associated with the vector sum of the voltages across Z_5 and Z_6. Consequently, any network that is developed should be so designed that the line-to-ground component will be taken vectorially. This can be accomplished readily by a star network as shown by figure 11.

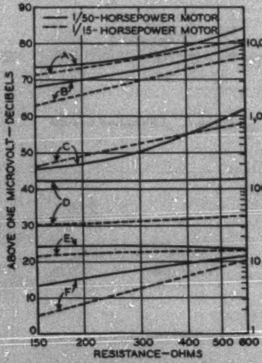

Figure 8. Noise-influence voltage from 1/50- and 1/15-horsepower universal motors with various terminating impedances

A—Line to ground, frame grounded

B—Line to line, frame grounded or ungrounded

C—Line to ground, frame ungrounded

D—Line to line, frame grounded or ungrounded—filtered

E—Line to ground, frame grounded—filtered

F—Line to ground, frame ungrounded—filtered

The noise-influence voltages produced by $^1/_{50}$- and $^1/_{15}$-horsepower motors with terminating impedances of 600–300 and 150 ohms are shown by figure 8. It will be seen that the measured average noise-influence voltages are seven decibels higher with 600 ohms than with 300 ohms and five decibels higher with 300 ohms than 150 ohms in the following conditions:

1. Line-to-line, grounded or ungrounded (not filtered)
2. Line-to-ground, grounded and ungrounded (not filtered)
3. Line-to-ground ungrounded (filtered)

Where filters are applied to the motors all three impedances give identical line-to-line and line-to-ground (frame grounded) voltages.

Some field tests have indicated that the variation in the impedances of supply lines in homes may vary from 10 to 2,000 ohms with an average around 150 to 300 ohms. Other investigators have measured an average impedance of 80 ohms. The supply-line impedances vary considerably from location to location and are not pure resistances but contain reactive terms. Some of the supply lines that have been measured were resonant in the broadcast band. Lines with impedances that contain reactive terms may increase or decrease the noise voltage from electrical apparatus, depending upon their phase relation with respect to the internal impedance of the apparatus. The impedance of supply lines may vary from day to day. Turning on a lamp may change the line impedance characteristics so as to cause the noise voltage from apparatus to increase or decrease a considerable amount.

Field tests indicate that the impedance of each wire to ground is different, thereby causing a high-frequency unbalanced termination for the apparatus. Laboratory tests have indicated that the minimum noise voltage from electrical apparatus will be obtained when the terminating impedances are balanced with respect to ground. A mathematical analysis of figure 7 will also show this.

This effect was determined by using a circuit as shown by figure 9 where *A*, *B*, and *C* were the terminating resistors and the other network utilized to measure the vector sum of the line-to-ground voltage. When *A*, *B*, and *C* were 200, 300, and 300 ohms respectively, providing a resultant resistance of 150 ohms between lines and between each line and ground, the line-to-ground voltage was 240 microvolts. When *B* was short-circuited, *C* made equal to 600 ohms, and *A* equal to 200 ohms, providing a line-to-line resistance of 150 ohms and 150 ohms from one line to ground, the line-to-ground voltage increased to 1,920 microvolts. This impedance unbalance resulted in a line-to-ground voltage that was three times as great as the line-to-ground voltage obtained with a balanced 600-ohm network. This test probably produced a greater unbalanced condition than will exist in practice. However, it does show the necessity of considering unbalanced systems in the field.

Field measurements on unfiltered devices also indicate that, because of the impedance unbalances of supply systems, the networks used for measuring the noise-influence factor should have impedances greater than the average line impedances measured in the field. Figure 10 shows the ratio between the noise-influence voltage produced by a motor when terminated with a 600-ohm network and as measured on various supply lines. In this figure the ordinates are the average of the voltage measurements taken at three frequencies in 20 residences. It may be seen by this curve that 50 per cent of the cases had 25 per cent or more of the voltage measured on a 600-ohm network. Twenty per cent of the cases had 60 per cent or more of the voltage measured on the 600-ohm network. If this motor had been measured on a 150-ohm network, 50 per cent of the cases would have had a voltage on their supply line that would have been 100 per cent or more of the voltage measured when using a network.

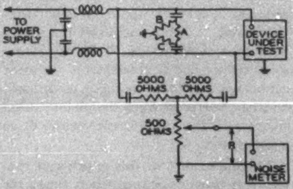

Figure 9. Circuit used to determine the effect of unbalancing the terminating impedance

Circuits Used for the Measurement of Radio-Noise-Influence Voltage at the Terminals of Apparatus Normally Connected to Single-Phase Domestic Supply Lines

The auxiliary devices and detailed methods of measurement contained in this paper have been developed primarily for the 550–1,500-kilocycle broadcast band. It is probable, however, that the same methods can be used for measurements at frequencies higher than this, but since the problem of measurement at these higher frequencies is now in the developmental stage, the scope of this paper, so far as detailed measurement procedure is concerned, is limited to frequencies below 1,600 kilocycles.

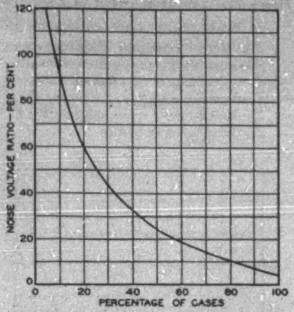

Figure 10. Per cent of noise-influence voltage existing on supply lines

The measurement of noise-influence voltage at the terminals of single-phase low-voltage apparatus such as motors, appliances, etc., is accomplished by a circuit arrangement as shown in figure 11.

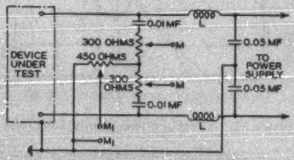

Figure 11. Circuit for the measurement of noise-influence voltage produced at the terminals of single-phase low-voltage devices

M-M—Terminals for line-to-line measurements
M_1-M_1—Terminals for line-to-ground measurements
L—Radio-frequency choke coils

Measurement of the line-to-line voltage (E_L) is made by connecting the radio-noise meter across the terminals *M-M*. This voltage may be obtained by measuring the voltage across a known portion (R) of the 600-ohm resistance between lines. This measured voltage is multiplied by $600/R$ to obtain E_L.

Measurement of the line-to-ground voltage (E_G) is made by connecting the radio-noise meter across the terminals M_1-M_1. This voltage may be obtained by measuring the voltage across a known portion (R) of the 450-ohm resistance. The measured voltage is multiplied by $600/R$ to obtain E_G.

This network is constructed with resistors that are noninductive at the radio frequencies employed in the measurements. These resistors may be of the metallized filament or carbon type commonly used in radio receivers. They should preferably be mounted with the coupling capacitors in a nonferrous metallic case. The resistance values should not differ from the specified values by more than five per cent plus or minus. Furthermore, the resistance between one line terminal and the ground terminal should not differ by more than one per cent from the resistance between the other line terminal and the ground terminal. Taps for suitable ratios can be secured by proper selection of resistor units.

It is necessary that the radio noise already existing in the supply circuit independently of the apparatus under test should not be included in the measurements and that the normal impedances existing between the supply lines and ground should not modify, in any essential respect, the characteristics of the measuring circuit. These two results are achieved by inserting in each supply lead between the measuring circuit and the terminals of the supply line a choke having a radio-frequency impedance of not less than 5,000 ohms at the frequency of the test. The chokes must be so designed that any drop in the supply voltage due to the load current of the apparatus does not cause the terminal voltage to fall below the normal rating of the apparatus.

Method of Making Measurements

The frame of the apparatus is normally grounded.

The final reading is the mean of the values measured during a period of not less than ten seconds.

In the case of apparatus having noise-influence voltages of the nature of impulses with long intervals between them, the reading is the mean of the values of ten impulses.

The network should be connected to the supply circuit by a twisted pair of leads 24 inches long and to the noise meter by leads not exceeding 4 inches.

It has been the experience to date that it is necessary to ground the device under test if comparable results in various locations are to be obtained. However, for apparatus that is normally ungrounded in service the grounding of the apparatus may increase considerably its apparent ability to produce noise. Because of the lack of information on this subject it is desirable to include in this paper the precautions that must be taken when measuring ungrounded apparatus.

In order that the capacitance to ground of apparatus tested ungrounded shall be standardized for the purpose of measurement, the apparatus under test should be placed 15 inches above the metallic floor of the test room in an insulated position. The distance to the walls of the room should not be less than 15 inches. The radio-noise meter, networks, and the person in charge of the test should be at a distance greater than 15 inches from the apparatus. If a shielded room is not available, the apparatus should be placed at a height of 15 inches over a grounded metal plate which is not less than seven by seven feet.

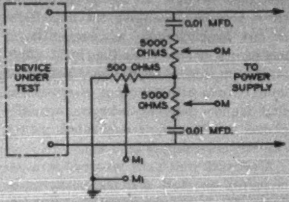

Figure 12. Circuit for the measurement of noise-influence voltage on equipment in the field

M-M—Terminals for line-to-line measurements
M_1-M_1—Terminals for line-to-ground measurements

Circuit Used for the Measurement of Noise-Influence Voltage at the Terminals of Low-Voltage Three-Phase Equipment

A network similar to that used on single-phase apparatus may be used for three-phase systems. The network consists of three 300-ohm resistors connected in Y with a 500-ohm resistor connected from the neutral to ground. This arrangement provides a network with 600 ohms between phases, and 600 ohms between ground and all phases in parallel. The line-to-line voltage (E_L) between any two phases may be obtained by measuring the voltage across a known portion (R) of the 600-ohm resistance between the lines. This measured voltage is multiplied by $600/R$ to obtain E_L.

The line-to-ground voltage (E_G) may be obtained by measuring the voltage across a known portion (R) of the 500-

ohm resistance, and multiplying the measured voltage by 600/*R* to obtain E_G.

MEASUREMENT OF RADIO-NOISE-INFLUENCE VOLTAGE ON EQUIPMENT IN NORMAL SERVICE

In normal service the noise-influence voltage of apparatus cannot be measured accurately by the networks previously described because of the possibility of these networks changing the impedance of the load circuit. Consequently, a measuring network having little effect on the impedance of the load should be used. Among those types of apparatus that require different networks are telephone equipment, complicated power switchboards, supply lines, etc.

A network which can be used for measurements on such systems is shown in figure 12. It is to be noted that this circuit is fundamentally the same as the network used for single-phase measurements, differing only in the change of circuit constants and omission of filter chokes.

By connecting the radio-noise meter to terminals *M-M*, the voltage across a known portion (*R*) of the 10,000-ohm resistance between lines can be measured. This measured voltage when multiplied by the conversion factor 10,000/*R* gives the noise-influence voltage between lines.

The line-to-ground noise-influence voltage is obtained by connecting the radio-noise meter at points M_1-M_1 across a known portion (*R*) of the 500-ohm resistor. When the lines are considered jointly, their resistance to ground is 3,000 ohms; thus the conversion factor for the line-to-ground measurement is 3,000/*R*.

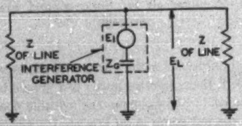

Figure 13. Schematic circuit of a typical high-voltage interference generator

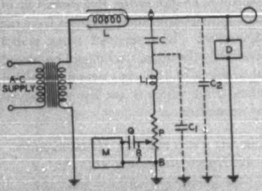

Figure 14. Circuit for the measurement of the noise-influence voltage produced by high-voltage devices

T—Testing transformer
L—Radio-frequency choke, not less than 20,000 ohms at frequency at which measurement is made
M—Radio-noise meter
C—Coupling capacitor, not less than 0.0025 microfarad
G—Dummy antenna
D—Device under test
P—Potentiometer or tapped resistor, 600 ohms resistance, nonreactive
L_1—Inductance between capacitor and potentiometer, ten microhenries, approximately
C_1—Stray capacitance on potentiometer side, not over 50 micromicrofarads
C_2—Stray capacitance on bus side, not over 60 micromicrofarads

RADIO-NOISE-INFLUENCE VOLTAGE OF HIGH-VOLTAGE APPARATUS

The radio-noise voltage produced by high-voltage apparatus is influenced by the same means as the low-voltage devices. It is propagated in the same fashion and the difficulties of determining the radio-noise effect on receivers are as great if not greater than that from low-voltage devices. Consequently, it is necessary, as it is on the low voltage, to measure the noise-influence voltage from this type of apparatus on a standard network.

The noise-influence voltage produced by high-voltage equipment depends upon the internal impedance of the apparatus and the line impedance. An interference generator and its load impedance is shown by figure 13. All high-voltage apparatus generally has an impedance to ground at broadcast frequencies that is larger than the line impedance to ground. Because of this condition, the line impedance affects the transmission line-to-ground voltage E_G. For open-wire lines the impedance *Z* is between 400 and 600 ohms unless the line is shorter than 10 or 12 wave lengths.

A circuit similar to figure 14 has been used by a number of manufacturers to determine the noise-influence voltage of line apparatus, such as insulators, bushings, and fuse cutouts.

Because of the effect of the distributed capacitances of the high-voltage conductor and coupling capacitor it is necessary to limit these distributed capacitances to the values shown in figure 14 in order that the error be kept under ten per cent at 1,000 kilocycles, where the majority of the measurements are made.

A simplified circuit to show the effect of these distributed capacitances on the voltage measured is shown by figure 15. It is the same as figure 14 except the impedance of the choke coil and transformer has been neglected. If the choke coil in series with the transformer has the impedance specified, this part of the system can be neglected.

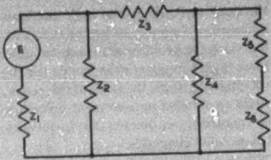

Figure 15. Equivalent circuit of figure 14

E—Voltage produced by device
Z_1—Internal impedance of device
Z_2—Impedance of stray capacitance C_2
Z_3—Impedance of coupling capacitor
Z_4—Impedance ofs tray capacitance C_1
Z_5—Impedance of series inductance
Z_6—Terminating resistance

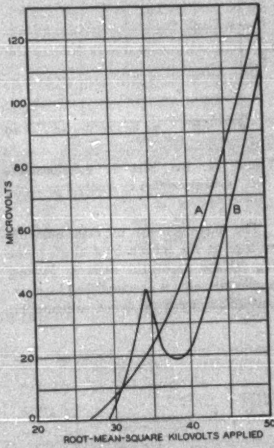

Figure 16. Effect of power-supply wave form

A—Corrected wave form
B—Distorted wave form

The voltage appearing across the resistance *R* figure 14 (Z_6 figure 15) is:

$$E_{Z_6}=E\frac{Z_3Z_4Z_6}{Z_{11}Z_{22}Z_{33}-Z_{11}Z_4^2-Z_{33}Z_3^2}$$

where:

$$Z_{11}=Z_1+Z_2$$
$$Z_{22}=Z_2+Z_3+Z_4$$
$$Z_{33}=Z_4+Z_5+Z_6$$

From the foregoing, it will be noted that stray capacitances will introduce errors, which must be given consideration in the design of a circuit such as shown by figure 14. If it is assumed that the voltage that would be measured with a pure resistance of 600 ohms across the device is E_R, then the error in per cent introduced by the system as shown by figure 14 is $\frac{E_R - E_{R_1}}{E_R}100$.

Effect of Power-Supply Wave Form on the Noise-Influence Voltage

Tests in the laboratory have shown that supplying a distorted 60-cycle voltage wave to the testing transformer will produce erroneous measurements. Curve *A* of figure 16 shows the noise influence produced by a treated pin-type insulator when the testing transformer was supplied by a system that contained a high percentage of harmonics. The source of voltage for this test was such as to result in a change in both the phase relationship and per cent magnitude of the various harmonic components for various values of root-mean-square voltage readings.

Correcting curve *A* to agree with the measured crest voltage readings did not materially improve the appearance of this curve.

The testing transformer was then supplied from a motor generator set and the curve *B* as shown by figure 16 was obtained.

Field Applications

In the preceding paragraphs radio-noise meters and methods of measurement have been described in detail. With the instruments available, and with the development of a suitable measuring technique, definite progress can be made by those engaged in the investigation and mitigation of noise phenomena.

Power companies and other agencies which have used noise-measuring equipment in the field have found it to be valuable in a number of ways, some of which are enumerated as follows:

1. Investigation and analysis of complicated noise problems on transmission and distribution systems.
2. Quantitative determination of noise characteristics of electrical apparatus and appliances for the purpose of determining their suitability from a noise standpoint.
3. Analyses of situations where special devices have been applied for mitigating noise effects.
4. Accumulation of data for establishing acceptable noise levels. Examples of the foregoing applications are given in the following paragraphs:

Measurement of Noise Along Transmission Lines

Figures 17 and 18 show, in graphical form, measurements made along 66-kv pin-type transmission lines where asphalt emulsion was applied to the heads of the insulators so as to close all voids between the surface of the insulator and the energized conductor and tie wires. Figure 17 covers a situation where measurements were made over a 5-mile section of the line before and after the asphalt application. A total of approximately 13 miles of line was treated between the generating station marked *A*, and the substation marked *B*. At a point 8.4 miles from the generating station there is a switching structure from which point a tap line extends for approximately 20 miles to another generating station. Except for approximately one-quarter of a mile from the tap structure, this line was not treated with asphalt at the time the measurements were made.

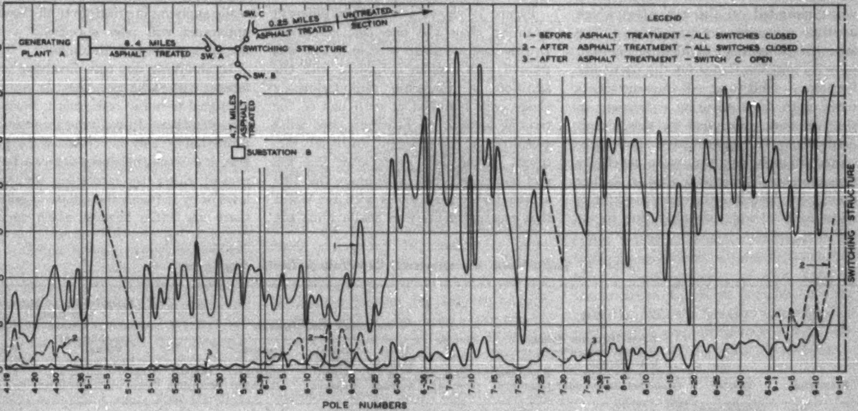

Figure 17. Noise measurements along a transmission line—case I

The explanation of the higher noise level in the sixth to ninth miles lies in the fact that this particular pole line carries both 66- and 11-kv circuits arranged in vertical configuration with the 66-kv circuit occupying one side of the pole and the 11-kv circuit the other side. Measurements were taken with the noise meter placed in a car with the antenna rod mounted on the side window. As a result, the 11-kv circuit was nearest the car up to pole 6-27 and probably shielded the higher-voltage circuit to some extent, while beyond this point the conditions were reversed. In making the measurements the car was always headed in a direction such that the rod would be on the side toward the pole line. Measurements were taken at each pole at an approximate distance of 20 feet transverse to the center of the line, and care was exercised to maintain this separation at the various pole locations.

Since the installation of the noise meter in the car would be expected to affect the measurements as compared to operating the noise meter with a standard rod as a unit on the ground, an antenna

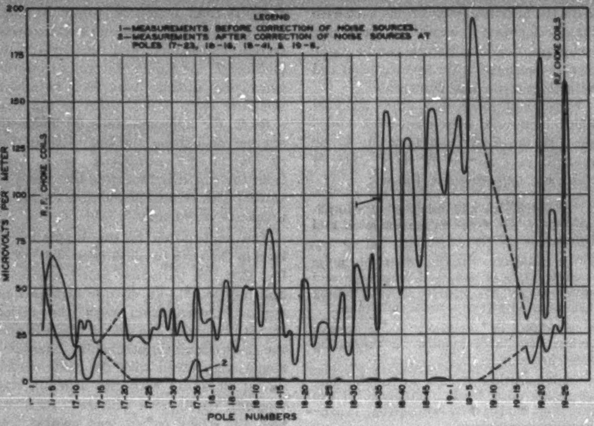

Figure 18. Noise measurements along a transmission line—case II

factor was secured in the following manner: A pole location was selected and a measurement made with the equipment installed in the car. A mark was placed on the road directly under the rod antenna, after which the instrument was removed and the car run ahead approximately 50 feet. The instrument was then placed on the ground in a position so that the rod was in the same vertical line as when mounted on the car, after which another measurement was taken. The second measurement divided by the first, gave an antenna factor for the particular frequency used and all measurements made with the measuring equipment in the car were multiplied by this factor. Aside from the quantitative results obtained in reducing the noise level the measurements indicated that a higher noise level existed in the eighth and ninth miles and beyond, probably due to inadequate asphalt coatings, and that the closing of switch *C* on the untreated tap line, raised the noise level all along the asphalted section, thus showing the results of noise propagation along the line from a distant source.

Figure 18 shows a somewhat different situation than the one just described. In this case a smaller section of line was treated with asphalt emulsion, with the addition of line-type radio-frequency choke coils installed at each end of the treated section, to attenuate the noise generated in the adjacent untreated sections. About two years after this job was completed, the noise along this line attained very high levels with the result that measurements were made along the line as shown on curve 1. These measurements indicated a source of noise at pole 19-6 with less severe disturbances originating at other points. Due consideration, however, had to be given to the possibility of reflections from the choke coils. It developed that an insulator at pole 19-6 was found to have cracks in two of the shells which gave rise to severe noise. Incidental cases were found also at poles 17-23, 18-16, and 18-41. The correction of these conditions resulted in curve 2, which indicates a very low noise level for this type of line. In making these measurements the same precautions were taken with respect to lateral distance from the line and the determination of an antenna factor as outlined in the discussion of figure 17.

MEASUREMENTS OF LOW-VOLTAGE APPLIANCES

In the measurement of radio noise produced by low-voltage electrical appliances, it is desirable to utilize the information in such a way that it will be possible to rate these devices according to their radio-noise effects when operated in a home or place of business. This feature applies also to noise levels of high-voltage equipment and their relation to reception in the home, but the data on this phase of the subject, at present, are somewhat meager. This discussion, consequently, while applying in principle to all classes of apparatus, will refer specifically to the use of appliances in the home.

In analyzing any case involving interference with radio reception, the question of signal-to-noise ratio must be given consideration. In applying the reasoning prompted by this conception of the problem, it follows that reception can be improved either by increasing the signal level or by reducing the noise. Obviously there are limits beyond which each of

Table I. Radio-Noise Measurements, Low-Voltage Motor Appliance

		Noise-Influence Voltage*			Residence Measurements					Noise Voltage on Antenna (Per Cent)		
		Line-Ground								Noise-Influence Voltage Line-Ground		
Kilocycles	Station	Grounded	Ungrounded	Line-Line Grounded	Line-Ground†	Noise Voltage on Antenna	Signal Voltage on Antenna	Signal-to-Noise Ratio (Decibels)	Quality Reception	Grounded	Ungrounded	House Wiring, Line-Ground
610	*M*	1,200	160	208	585	107	396	11.4	*E*	8.9	66.6	18.3
660	*N*	1,280	170	188	612	97	140	3.0	*F*	7.6	58.2	15.8
710	*O*	1,310	184	166	610	79	860	20.8	*C*	6.0	42.9	12.7
760	*P*	1,200	207	145	370	30	1,240	32.3	*A*	2.5	14.5	8.1
830	*Q*	1,120	223	123	153	13	290	27.0	*B*	1.2	5.8	8.5
860	*R*	1,120	220	117	125	10	350	30.8	*A*	0.9	4.6	8.0
1,020	*S*	1,270	240	84	93	6	352	36.2	*A*	0.5	2.5	6.4
1,170	*T*	1,370	305	84	40	9	435	34.2	*A*	0.7	3.0	22.5
1,200	*U*	1,380	318	85	25	9	198	27.4	*B*	0.7	2.8	36.0
1,440	*V*	1,500	255	130			12,300		*A*			
Average		1,275	228	133	290	40				3.1	17.5	13.8

* In accordance with figure 11. † In accordance with figure 12.

these factors cannot go, these being dictated by geographical location, local conditions, the status of the art, and other considerations. While opinions differ to some extent, a number of measurements have indicated that where the signal-to-noise ratio is 30 decibels or greater, satisfactory reception is obtained. With decreasing values of signal-to-noise ratio, reception becomes increasingly unsatisfactory, finally reaching a point where all program reception is impossible when the signal-to-noise ratio is zero decibels or lower.

Table I shows a tabulation of a number of measurements made under various conditions on a motor-driven electrical appliance. The data include measurements made from line to ground (E_G) and from line to line (E_L) in accordance with the circuits shown in figures 11 and 12. All measurements of noise in the residence were made at frequencies adjacent to the broadcast-station channels, the noise at the latter frequencies being obtained by interpolation. Other measurements include the noise voltage from the customer's house wiring to ground, noise on the customer's antenna, signal on the antenna, signal-to-noise ratio in decibels, and quality of reception. The latter item is intended to indicate the reaction of the listener to various signal-to-noise ratios, and it has been found convenient to express these aural effects as follows:

A—Entirely satisfactory

B—Very good, background unobtrusive

C—Fairly satisfactory, background plainly evident

D—Background very evident, but speech easily understood

E—Speech understandable only with severe concentration

F—Speech unintelligible

By referring to table I it will be noted that a definite relation can be established between the noise-influence voltage and the results in a residence for a particular appliance. It has been found that even with the same appliance, results will vary in other residences. The data collected by the Joint Co-ordination Committee on Radio Reception of the EEI, NEMA, and RMA show that the relationship between noise measured at the factory and on the customer's antenna varies over a very wide range. This is due to such factors as the coupling between the antenna and supply system, the electrical characteristics of the house wiring system, the susceptibility of the radio receiver to noise pickup by means other than the antenna connection, and finally, the field intensity of the radio station being received. Through the accumulation and analysis of a considerable amount of data, eventually it will be possible to determine generally acceptable noise-influence voltages for appliances and other electrical apparatus. In making a study of this character it must be recognized that it is not economically feasible, and in many cases practically impossible, to select limiting noise-influence voltages which will satisfy all conditions and locations. As a result, limits that may be acceptable generally will have to be recognized as taking care of a certain percentage of cases rather than all cases.

MEASUREMENTS ON ANTENNA SYSTEMS

In the foregoing paragraphs, reference has been made to the signal-to-noise ratio and to the coupling between the antenna and house wiring system. One method of increasing the signal-to-noise ratio is by reducing this coupling by means of a noise-reducing antenna system of suitable design and proper installation. Quantitative data to compare one antenna arrangement with another may be obtained by measuring both signal and noise separately on the two antenna systems and determining the signal-to-noise ratios for each antenna arrangement. Since the proper functioning of most noise-reducing antenna systems requires that the pickup section of the antenna be located beyond the intense portion of the noise field, the noise meter can be used to advantage in determining the best location for the antenna. This may be accomplished by making a survey at various points on the premises, using the noise meter and its associated antenna rod. The final results can then be checked after the antenna is installed.

An example of the data which may be secured and the results which can be obtained, is shown in table II. In this installation, the conventional antenna was an inverted L type with a 60-foot flat top and a lead-in located 40 feet from the overhead line carrying the disturbance. The noise-reducing antenna consisted of a vertical wire 37 feet long located 100 feet from the overhead line carrying the disturbance. A coupling transformer was mounted near the ground level at the lower end of the vertical antenna with a 12-inch lead to a ground pipe. From this point a twisted-pair transmission line, laid on the ground for the test, ran to the radio set in the house where another coupling transformer was installed directly at the antenna and ground terminals of the set. The conventional antenna was removed during the period of making measurements on the noise-reducing antenna. As in the case of table I, noise measurements were made at other frequencies and interpolated for the broadcast channels.

It will be noted that the signal-to-noise ratio was increased in varying degrees from 3.4 to 19.6 decibels when using the noise-reducing antenna and that on the basis of approximately 30 decibels signal-to-noise ratio, seven out of the nine stations involved were received satisfactorily with the noise-reducing antenna, while only one could be placed in that category with the conventional antenna arrangement. These data are given to indicate the application of the noise meter to a given situation rather than to show the maximum attainment possible with a noise-reducing antenna system. Under more favorable conditions with respect to antenna location and other factors, a much greater increase in signal-to-noise ratio would be a likely possibility.

Table II. Measurements of Signal and Noise Voltage on Antenna Systems

		Signal		Noise		Signal-to-Noise Ratio (Decibels)		Improvement, Noise Reducing Over Conventional
Kilocycles	Station	Conventional	Noise Reducing	Conventional	Noise Reducing	Conventional	Noise Reducing	
610	*M*	336	450	35	15.5	19.5	29.1	9.6
660	*N*	310	143	43	12	17.1	21.5	3.4
710	*O*	930	695	51	9.5	26.1	37.1	11.0
760	*P*	1,790	1,230	63	8	29.2	43.5	14.3
830	*Q*	530	570	89	7	15.5	38.0	12.5
860	*R*	450	336	106	8	12.5	32.1	19.6
1020	*S*	470	2,400	90	59.5	14.1	32.0	17.9
1170	*T*	560	1,570	102	33.5	14.9	33.5	18.6
1440	*V*	260	120	126	34.5	6.0	11.0	5.0

Discussion

J. J. Smith (General Electric Company, Schenectady, N. Y.): The specifications for an instrument for measuring radio noise and the description of the methods of measurement given by the authors in this paper should be of considerable help to those who have to study the problem of radio interference. It is hoped that this work done under the Joint Co-ordination Committee on Radio Reception of EEI, NEMA, and RMA will tend to produce a uniformity in the methods used by different workers and encourage measurements along the standard procedure.

In many cases of investigation of complaints of radio interference due to power equipment the only type of measurement made is to listen with a radio set. The data thus obtained, however, are not of great assistance since they tell only the relative value of the interference and the radio signal and do not give the actual value of either.

The signal field strength available for radio reception varies considerably from place to place. Thus, as far as radio reception is concerned, in a locality where the field strength is of the order of thousands of microvolts a given piece of power equipment might be used without affecting radio reception. However, when the same piece of equipment is used in a locality where the field strength of the radio signal is of the order of a few microvolts it might make reception difficult. Thus it is evident that the field strength at the point of reception is an important part of the radio-interference problem, and it is only by having a definite measure of its magnitude and also a knowledge of the magnitude of the noise-influence voltage produced by the device that the best solution can be obtained.

Another advantage of this new meter is that it is expected it will be available at a lower cost than the meters previously used. This should result in its being more widely used and thus assist in obtaining data on both the signal strengths at the point of reception and the noise-influence voltage of the electrical apparatus in the same location together with the resulting radio reception.

Similar work on studies of the problem of radio interference has been going on in Europe and a committee (CISPR) of the International Electrotechnical Commission has developed standards for a radio-noise meter quite similar to the one described in this paper. However, there are certain differences and I should like to ask the authors what these differences are and the reasons why it did not appear desirable to make the two meters the same.

Charles M. Burrill (nonmember; RCA Manufacturing Company, Inc., Camden, N. J.): The "quality of reception" ratings listed in table I are based on a subjective scale of values devised by the writer in 1934 for use in an unpublished research on broadcast reception at ultrahigh frequencies. This scale has been found useful within the RCA organization in a number of radio-noise and interference investigations. However, the figures in table I are the first to be published comparing ratings on this subjective scale with signal-to-noise ratios determined objectively with a radio-noise meter.

In 1936 we made a few tests to correlate the readings of a quasi-peak noise meter with our "quality of reception" ratings. The results of these tests were communicated to the Joint Co-ordination Committee on Radio Reception of RMA, NEMA, and EEI, but it was not possible to make at that time a sufficient number of the time-consuming listening tests to warrant the publication of definite conclusions. It is gratifying to note the agreement between our figures and those given in table I, despite the fact that different noise meters and different noises were involved.

We found average quality ratings corresponding to the signal-to-noise ratios given in the following table:

Signal-to-noise ratio (decibels)		+28	+8	+3
Quality		*B*	*B*	*B*
Signal-to-noise ratio (decibels)	−10	−20	−34	−47
Quality	*C*	*C*	*D*	*E*

All but the first three ratings in table I are entirely consistent with the above values. The first three ratings correspond to considerably poorer quality of reception than would be expected from our data. I believe the explanation of this is, that we used a noise of a more impulsive nature than the noise of an average commutator motor, and that our noise meter may have given to brief high peaks of noise relatively more weight than the instrument used in obtaining the data of table I.

The above partial agreement and partial disagreement, and the necessity for explanations are typical of experience when quantitative studies of interference are attempted. They illustrate why standardization of method is the greatest need in this field. The work reported in the present paper is a splendid and important contribution toward filling this need.

AUTOMATIC-GAIN-CONTROL TIME CONSTANTS

In the radio-noise meter described in the paper, in which a logarithmic scale is obtained by "automatic gain control" action, the function of the indicating meter is twofold; first, to measure a change in output, and second, to measure a change in gain which is produced by the change in output through feedback. The input corresponding to a given indication depends on both of these. If now the time constants determining the transient response of the gain-changing circuit are not the same as those determining the transient response of the indicating meter, the actual response of the instrument to noise will be a complicated function of *both* sets of time constants.

For example, if in figure 3 of the paper the time constants R_2C_2 and R_3C_3 are made 200 milliseconds as suggested in the paper, and if the charging time of C is ten milliseconds, then the response to a single isolated noise peak will be substantially as though the automatic-gain-control circuit did not exist, for C will be charged before the gain control has time to act. If this is the performance desired, then at least the time constants of the automatic-gain-control circuits should be as carefully standardized as those determining the indicating-meter action. It should be mentioned, however, that with a slow automatic-gain-control circuit there is serious danger of overloading the intermediate-frequency amplifier on impulsive noise peaks.

I believe that the time constants of the automatic-gain-control circuits should be made the same as those determining the response of the indicating meter, so that the instrument will have a truly logarithmic scale for both impulsive and sustained noises. This is accomplished by making the time constants R_2C_2 and R_3C_3 small compared with the charging time constant of C, or ten milliseconds. Some tests have indicated that this is a practical possibility.

INTERNAL CALIBRATING SOURCE

There is another possible internal calibrating source not mentioned in the paper, which I believe has received too scant consideration. In fact such a source, a relaxation oscillator, was used in a commercial radio-noise meter built in accordance with the 1932 specifications of the Joint Coordination Committee ("Radio Noise Meter and its Application," C. R. Barhydt, *General Electric Review*, volume 36, 1933, page 201). The method of calibration there used may not have been very satisfactory. However, I believe that the relaxation oscillator, properly designed, will be found at least as satisfactory as the shot-noise generator, and perhaps preferable to it.

There is an advantage, not mentioned in the paper, in using for a comparison standard of calibration a typical noise wave form rather than a sine wave. By so doing, changes in selectivity of the instrument are compensated for, exactly for noise wave forms of the type used, partially for other wave forms. The compensation cannot be obtained exactly for all forms of noise, because at one extreme, the peak value with noise pulses which completely overlap is proportional to the square root of the band width, whereas at the other extreme, the peak value with nonoverlapping noise pulses is directly proportional to the band width ("A Study of the Characteristics of Noise," V. D. Landon, IRE *Proceedings*, volume 24, 1936, page 1514).

Shot noise has a wave form at the one extreme, corresponding to complete overlapping of the noise pulses in the receiver, whereas a relaxation oscillator with a low repetition frequency produces a noise wave form at the other extreme, that of no overlapping. By increasing its repetition frequency the relaxation oscillator may be made to give an intermediate type of wave form, if desired.

In a large number of cases, actual noise wave forms consist of pulses repeated at twice the commercial power-line frequency (120 cycles per second), a rate too slow to cause overlapping in most receivers. Thus, a noise wave form with a repetition frequency of 120 cycles per second such as can be obtained with a relaxation oscillator, would seem more typical of radio noise than hiss such as shot noise, and therefore preferable for use in calibration.

The shot noise of a tube is very small, so that a radio-noise meter must be very sensitive if shot noise is to be used for its calibration without additional complications. This may prove a rather severe limitation to the use of shot noise for calibration, particularly for low-priced instruments.

The shot noise generated by a saturated tungsten-filament diode may be determined very accurately by measuring the average space current, although generally a correction for the finite internal resistance of the diode must be applied unless the external resistance is low. Unfortunately, a tungsten-filament tube cannot be operated from batteries suitable for a portable instrument. Thoriated-tungsten or oxide-coated-filament tubes, which must be used instead, are much more uncertain as to saturation and noise. It seems doubtful to me whether the shot noise from such a tube can be determined any more reliably by measuring its space current than the noise generated by a properly designed relaxation oscillator can be determined by measuring its supply voltage.

The International Special Committee on Radio Interference (CISPR), working in Europe, has been investigating the design of a standard radio noise generator, and a paper describing some of their results with a relaxation oscillator used for this purpose has recently been published ("Étude et Applications d'un Générateur Stable de Tensions Perturbatrices Radiophoniques. Perturbateur Type," G. Coffin and G. Marchal, *L'Onde Électrique,* volume 17, number 204, December 1938, pages 562–74).

J. L. Clarke (Bell Telephone Company of Canada, Montreal, Que.): The authors have not mentioned whether any means have been provided in their measuring set to make the noise field audible. It has been our experience when measuring noise including several discrete and readily identifiable sounds that it is very helpful to be able to sort out the observations when one or another of the more prominent sounds is present. We have found that the addition of an audio stage and loudspeaker to the measuring set is very helpful in this connection.

W. F. Grimes (Radio Interference Engineering Bureau, Inc., Los Angeles, Calif.): The paper by C. V. Aggers and his associates presents a very complete description of instruments and methods of measurement available for the laboratory study of radio noise. Some results of comparative laboratory and field tests are also given.

It appears that the radio and electrical industries are confronted with two general problems involving the reduction of radio noise. First, the manufacturer of electrical apparatus, as is shown by the paper, has recognized the necessity for the development of radio-noise-free products. Much work has been done to devise methods which permit reproduction of laboratory results in the measurement of radio-noise-influence voltages. Second, as pointed out in the paper, measurements now made in the laboratory cannot be satisfactorily reproduced in the field due to the electrical constants of supply circuits and other such factors.

The routine investigation of radio noise in the field is in existence today on a very limited basis. So far as is known, little or no quantitative field work is being done. Funds which have been made available for field investigations, with few exceptions, have been appropriated for the immediate benefit of broadcast and electric-utility customers. This has meant the establishing of a service free to the radio listener through which improvement in radio-receiving conditions could be accomplished.

In conclusion, it would seem that closer co-operation is necessary between the manufacturers and the field agencies serving the radio listener.

When instruments and methods of measurement are devised which will permit the interpretation of laboratory results in terms of expected radio-listener satisfaction, apparatus will be produced and purchased for the direct improvement of radio reception.

W. N. Goodwin, Jr. (Weston Electrical Instrument Corporation, Newark, N. J.): In the paper reference is made, somewhat briefly, to the characteristics of the indicating instrument. It is thought that a more complete discussion of these characteristics and why the particular constants were adopted might possibly be of general interest.

In noise measurements and also in indicating sound volume, such as broadcasting-program material, the indicating-instrument pointer is continuously fluctuating, and measurements are made by estimating averages of the pointer indication, or by noting peak readings.

In view of this condition, the first question considered by the committee was whether the instrument should be overdamped, critically damped, or underdamped.

This was studied by computing the deflection-time characteristics for specific cases of the three types of motions, from the following equations of motion. These were developed and the interesting properties of the quantity n discovered by the present writer in the early years of his career as an instrument engineer.

$$\frac{\theta}{\phi}=1-\frac{\epsilon^{-\frac{2\pi n t}{T_0}}}{\sqrt{n^2-1}}\sinh\left[\frac{2\pi t}{T_0}\sqrt{n^2-1}+\sinh^{-1}\sqrt{n^2-1}\right] \quad (1)$$

$$\frac{\theta}{\phi}=1-\left[\frac{2\pi t}{T_0}+1\right]\epsilon^{-\frac{2\pi t}{T_0}} \quad (2)$$

$$\frac{\theta}{\phi}=1-\frac{\epsilon^{-\frac{2\pi t}{T_0}}}{\sqrt{1-n^2}}\sin\left[\frac{2\pi t}{T_0}\sqrt{1-n^2}+\sin^{-1}\sqrt{1-n^2}\right] \quad (3)$$

where θ = deflection at any time t, resulting from the application of a constant electromotive force to the instrument circuit, which will produce a final deflection of an angle ϕ; T_0 is the undamped period of the instrument; and n the ratio of the actual to the critical damping coefficients, which the writer has designated "specific damping coefficient."

Equation 1 applies to the aperiodic or overdamped condition, equation 2 to the critically aperiodic condition, and equation 3 to the periodic, or underdamped condition.

Equation 1 may be considered the general equation from which equations 2 and 3 may be derived by making $n=1$ or $n<1$ respectively. When this substitution is made in equation 2 it becomes indeterminate and is evaluated in the usual manner by differentiation. Equation 3 becomes imaginary and is transformed by the relations, $\sinh^{-1} j\alpha = j\sin^{-1}\alpha$ and $\sinh jx = j\sin x$.

The three curves derived from the equations are shown in figure 1 of this discussion. Curve 1 shows the overdamped condition where the specific damping coefficient $n=1.5$, curve 2 shows the critically damped condition where $n=1$, and curve 3 the underdamped condition where $n=0.825$, and the damping factor is 100, resulting in an overswing of one per cent.

The curves were computed upon the basis that the deflections of corresponding instruments reached to within one per cent of their steady-state deflection in the same time.

It will be noted that the velocity of the slightly underdamped instrument is much more uniform from zero to full-scale deflection than for the other two conditions.

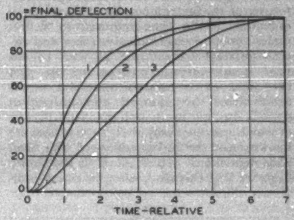

Figure 1

In the latter, the velocity is high in the lower part of the scale and diminishes rapidly in the upper part.

It is quite evident that an instrument, having a movable system giving a uniform velocity throughout its motion for a given current, would result in more correct estimates of averages and peaks than one varying in velocity. This has been proved by actual experience for a number of years in instruments for similar purposes, namely, for volume control in broadcasting. Furthermore, it results in far less eye fatigue to the reader.

For these reasons the committee adopted the slightly underdamped instrument for the noise meter.

The next question considered was that of speed of response. If the pointer action is too rapid, the eye cannot follow it; and if too slow it does not adequately follow the fluctuations in the current and gives erroneous indications. By actual tests it was found that an instrument having an undamped period of not less than 0.5 and not greater than 0.7 second, slightly underdamped, gave very satisfactory results, especially when connected into the noise-meter circuit which itself has definite time constants.

The committee thought it desirable, in order to keep the cost of the instrument within reasonable limits, to permit considerable tolerance in its constants, and for this reason specified that the undamped period may be from 0.5 to 0.7 second, and its damping factor from 10 to 100. Consideration was given to the question as to the best method of specifying response time based upon the constants just referred to. An instrument having moment of inertia, damping, and spring control does not have a time constant similar to that possessed by a resistance-capacity circuit, or to a body heated at a constant rate, namely, the time which will reduce the exponent of ϵ in the exponential function to unity, equivalent to the time required for the quantity to reach about 63 per cent of its final value. The reason for this is that the deflection of the instrument is not only an exponential function with time but also a trigonometric or hyperbolic function, as shown in the equations given above. It would be possible, of course, to assume the time constant to be the time required to produce 63.2 per cent of final deflection as in the heat and electrical problems, but as it has no fundamental basis as it has in the latter cases, it has no especial virtue.

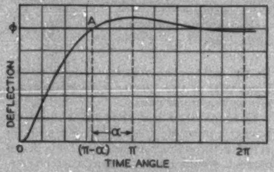

Figure 2

The time quantity which seemed to be the most practical for specifying the characteristic of the instrument, in addition to the damping, is the time required to deflect under constant electromotive force and circuit resistance from rest at zero to the final steady-current position on its first excursion. This value of time is a simple function of the damped and undamped periods and of the damping constants, any of which may be computed if the others are known, and is independent of the final scale deflection. These relations are shown in figure 2 of this discussion, in which ordinates are angles of deflection, and abscissas are angles proportional to time; 2π being the angle for a complete period; and π for the half period. The angle ϕ is the steady deflection and the motion is represented by the line OA. The angle α is the phase angle of the system, equal to $\sin^{-1}\sqrt{1-n^2}$ given in equation 3. The time to complete a full period, that is through the time angle 2π, is T, where T is the damped period, and $T=T_0/\sqrt{1-n^2}$. The time angle for the deflection to A is therefore $(\pi-\alpha)$, and the time to reach A from zero is then

$$t_0=\left(\frac{\pi-\alpha}{2\pi}\right)T=\left(\frac{\pi-\alpha}{2\pi}\right)\frac{T_0}{\sqrt{1-n^2}}$$

which is the value of time by which the indicating instrument used in the noise meter is specified.

This time value for any completed instrument may be measured in several ways, for example, (1) by direct observation, using a high-speed electric stop watch; (2) by taking a motion picture of the motions of the pointer from which the time can be computed; (3) by applying an interrupted direct current, having the off and on times equal, and varying the speed of interruption until a maximum deflection of the pointer results. Then the time of a complete period, off and on, is equal to the damped period T, from which the specified time can be calculated from the equations given above.

Table I

Undamped Period T_0 (Seconds)	Damped Period T (Seconds)	Damping Factor k	n	Deflection Time (Milliseconds)
0.5	0.62	10	0.592	217
0.5	0.885	100	0.825	357
0.7	0.87	10	0.592	304
0.7	1.24	100	0.825	500

Table I of this discussion gives the various constants of instruments having undamped periods from 0.5 to 0.7 second and damping factors from 10 to 100, computed from the above equations.

C. W. Frick (General Electric Company, Schenectady, N. Y.): The measuring circuits described in the paper have been in use for a number of years with radio-noise meters according to the old specifications. In the testing of low-voltage devices both the line-to-line and the line-to-ground radio-influence voltages are measured. The following test illustrates relations which may be found between these quantities, depending on the conditions. It also shows reasons for making the two measurements. A commutator motor was set up with choke coils in the lines, each coil having 1,700 ohms reactance at 1,000 kilocycles. The frame of the motor was grounded. First the motor was tested on the standard circuit of figure 11 of the paper. The noise-influence voltage was 2,500 microvolts line-to-line (V_L) and 4,000 microvolts line-to-ground (V_G). Referring to the equivalent circuit in figure 7 of the paper, it appears that the internal voltages E_1 and E_2 taken with respect to ground are approximately equal in magnitude and in the same direction but not in phase, since when the line impedances are equal V_L is proportional to the vector difference of E_1 and E_2 and V_G is proportional to one-half their vector sum. A later test indicates the assumption of equal magnitudes for E_1 and E_2 to be reasonable.

Next the device was connected in a circuit similar to figure 9 of the paper to try the effect of equal and unequal impedances between supply lines and ground. A delta network of 200 ohms between lines and 300 ohms from each line to ground, as in the paper, was used for the first condition. Noise voltage between lines was measured directly and noise voltage from line to ground was measured on the network of figure 12 connected as in figure 9. This gave 620 microvolts line-to-line and 1,080 microvolts line-to-ground. These values are approximately one-fourth of those measured according to figure 11, this ratio being the same as the ratio of the resistances of the networks which were 600 and 150 ohms respectively. This shows that the radio-frequency currents were controlled by the line chokes.

The next measurement was made with unequal impedances between lines and ground. One side of the line was connected to ground and a 150-ohm resistor was connected across the line. The measuring network of figure 12 was left in place. The noise voltages then became 1,260 microvolts line-to-line and 630 microvolts line-to-ground. It may be noted that the line-to-ground voltage is half the line-to-line voltage which it should be in this test because the network of figure 12 measures half the vector sum of the voltages between lines and ground and here the voltage on the grounded side is zero.

It appears from these results that the junction between E_1 and E_2 in figure 7 is practically grounded, probably through capacitances and the grounded frame. Then when one side is grounded it eliminates the voltage on that side from the circuit. The voltage across the line was the same with the ground on either side which would indicate that E_1 and E_2 are nearly equal in magnitude. In the case of equal impedances the line-to-ground voltage was proportional to half the vector sum of E_1 and E_2 or approximately proportional to either E_1 or E_2. In the case of one side grounded only one of these was acting and the voltage to ground was only half as large.

If the phases of E_1 and E_2 had been different the result would have been different. If they had been equal and opposite in direction instead of in the same direction, they would have tended to cancel each other out in the line-to-ground voltage when the impedances from line-to-ground were equal. However, when one side was grounded, eliminating either E_1 or E_2 from the circuit, the cancellation effect would not exist and the line-to-ground voltage would be about the same as in the test. Thus if E_1 and E_2 were opposite in phase the line-to-ground voltage would increase as it did in the test described in the paper. In the test described here, however, it decreased.

If E_1 and E_2 are opposite in phase, their effect is additive between lines. In such a case consideration of the test results indicates that under conditions of unequal impedances between lines and ground the line-to-ground voltage would not exceed half the value measured between lines provided the impedance of the line is not higher than the impedance of the network. The two quantities measured are useful for analyzing effects such as these.

In table I of the paper which gives an example of radio-noise data taken on a device and data taken in a home, the figures in the last three columns appear to be ratios between these two sets of measurements, but it is not clear what they are. It would be desirable for the authors to add an explanation of these figures and to show their use.

Further explanation would also be desirable in the case of figure 16. Can the shape of the curve for distorted voltage be explained? If the authors consider it necessary to take precautions to avoid errors attributed to voltage distortion, it would be desirable to state what these precautions should be.

Charles J. Miller, Jr. (The Ohio Brass Company, Barberton): The radio-noise meter described by the authors certainly has several desirable characteristics over those formerly available. The use of a logarithmic output meter with a range of 100 to 1 will certainly reduce the amount of work necessary for measuring radio-influence voltages on all types of devices. The new meter will be direct reading, a decided improvement over the substitution method of measurement.

In connection with the proposed detector circuit and indicating meter, a tabulation of suitable combinations of R, C, and R_1 is given. The lowest value of R given is one megohm. Since this resistance is in series with the indicating meter, the sensitivity of this meter will have to be very high. The most sensitive portable meter with which the writer is familiar has a full scale deflection for 20 microamperes flowing through it. If used with the one-megohm resistor, the voltage across the capacitor C would have to be 20 volts to produce a full-scale reading. What is the maximum voltage that can be satisfactorily applied to the capacitor C, considering all the limitations imposed by vacuum tubes available, and what indicating-meter sensitivity is practical for this application?

The coupling circuit for use with high-voltage devices imposes three new limitations over the previous circuit. These are the upper limits placed upon stray capacitances on the bus side and potentiometer side of the circuit and the lower limit of 2,500 micromicrofarads for the coupling capacitor. A formula is given whereby one may calculate from the constants of the circuit, Ez_4, the voltage appearing across the resistance potentiometer. A second formula is given whereby the per cent error may be calculated if the load impedance into which the device is looking is different from a pure resistance. It is possible to determine all the constants of an existing circuit by measurements. While it is fully recognized that it is desirable to use a circuit meeting all the requirements, is it not practical to compute the error introduced by an existing circuit, whose parameters are known, and then to use this factor to correct all readings taken on that circuit in order to make them comparable with values that would be obtained on an ideal circuit?

E. T. Hughes (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In 1930 the Joint Co-ordination Committee on Radio Reception of EEI, NEMA, and RMA was appointed to study methods for improving radio reception and in particular to consider the steps necessary to co-ordinate this newer use of electricity with some of its other uses for light, heat, power transmission, and communication.

A major problem has been in the development of radio-noise test circuits for the laboratory measurement of radio-noise-influence voltages. A test circuit which could determine directly the radio-noise voltage to be expected on any receiver antenna from any electrical apparatus on test would be desirable. However, no definite method has been developed that will permit the calculation of the radio noise on a receiver antenna when the radio-frequency voltage produced by electrical apparatus is known. Therefore the solution to the problem has been to develop measuring circuits which will permit measuring the radio-noise-voltage output of electrical apparatus with a radio-frequency load simulating the load impedance of the power system. At the present state of the art the radio noise produced by any equipment can only be expressed in units measured, utilizing the coupling networks recommended by the joint co-ordination committee.

The two basic circuits which have resulted from these considerations have proved to be well adapted for laboratory work because it has been possible for investigators in different laboratories to obtain comparable results in tests on the same apparatus.

By the use of the high-voltage test circuit it has been possible for the NEMA members to recommend certain levels for various high-voltage equipments. These levels have been obtained by making measurements on new equipment and on equipment which was returned from the field. The establishment of permissible levels of radio-noise-influence voltage for all classes of apparatus will require a somewhat different approach to the problem through field tests on different systems throughout the country.

Field tests are now being conducted in the Pittsburgh area to aid in the establishment of levels for low-voltage appliances. An appliance will be tested for radio-noise-influence voltages in the laboratory, then installed in several different homes where the noise voltage on the antenna will be measured. Knowing the effective height of the installed antenna, the signal-to-noise ratio desired, and broadcast station field strength, then the tolerable noise-influence voltage will be:

$$V=\frac{Se}{R}$$

where

V = tolerable noise influence voltage
S = antenna noise factor
e = broadcast station field strength level
R = signal to noise ratio

It is expected that from these tests the antenna-noise factor for an average home may be determined.

A second set of tests has been planned on a 6,900-volt distribution system. These tests are intended to determine the attenuation, effect of reflection, the amount of radiated field, and the field intensity perpendicular to the line in terms of the actual high-frequency voltage on the transmission line. The attenuation of the noise voltage through distribution transformers will also be studied.

A comparison between the work on this subject here and abroad may be of interest to some. The noise meter conforming to the new committee specifications is very similar to the one adopted by England, France, Germany, Italy, and Belgium. The low-voltage coupling networks used in the United States are different in that we have adopted a star network compared to a delta circuit used abroad. Both networks, however, give comparable results when the impedances are the same. The joint co-ordination committee has adopted 600-ohm networks whereas the European network is 150 ohms. The reasons for adopting 600 ohms have been outlined in the paper.

The continuation of the work of the joint co-ordination committee shows promise of producing more beneficial results in the future and by universal adoption and use of this instrument and method of measurement, radio-noise levels for all types of equipment can be established.

C. V. Aggers, D. E. Foster, and C. S. Young: The supplementary information contained in the various discussions is a valuable contribution to the problem, making the paper a comprehensive description of the art of measuring radio noise.

J. J. Smith has raised the question as to why the instrument described in the paper differs from the specifications developed by the CISPR. The CISPR specifications call for a time constant on charge of 1 millisecond and on discharge of 160 milliseconds. The other differences between the instrument in the paper and the CISPR specifications are of a minor nature and will in general cause no difference in performance between the two instruments. In developing the specifications for detector time constants, consideration was given to the CISPR specifications. However, as pointed out in our paper, it is difficult to secure a charge time constant of 1 millisecond with available tubes and difficult to hold this value in production, whereas it is relatively easy to secure a 10-millisecond charge time constant and a time constant of such value may be more readily held in production of noise meters. The discharge time constant specified by the CISPR is of the same order as that of the indicating meter, and hence the indicating-meter time constant will have a large effect on the results obtained. By the use of the long time constant of 600 milliseconds suggested in our paper, a much wider permissible range of time constants for the indicating meter is provided, and indicating meters of more desirable characteristics may be secured. Furthermore, as pointed out in the paper, by largely eliminating the influence of the indicating meter, the effective discharge time constant becomes that of the circuit, and this may be more readily duplicated in production than the time constant of the meter. The ratio of charge to discharge in the CISPR specifications is larger than that of the meter described in our paper, and therefore the CISPR meter will give a higher indication on wave forms of the sharply peaked type. On wave forms approaching the sinusoid, the difference in indication between the CISPR and the joint co-ordination committee specifications will be of the order of one per cent, a relatively minor factor considering the other possible variations.

We feel that the time constants suggested in our paper will give more constant, reliable results and permit more uniformity in production of noise meters than those adopted by the CISPR.

C. M. Burrill points out that with the automatic-volume-control time constant suggested in our paper, the response on a single-pulse type of noise will depend in some degree on the time constants of the automatic-volume-control circuit. The response of the instrument will not be the true peak value on a single pulse or widely separated repetitive pulses, not only because of automatic-volume-control time constants, but also because of the detector time constants chosen. As pointed out in the reply to Doctor Smith's discussion, the detector time constants were chosen to provide reliable and consistent results with practical tubes and circuits, rather than to provide idealized response to all types of noise. Furthermore, as pointed out in our paper, the disturbing effect of pulse types of noise appears to be less than would be indicated by their peak amplitude. It is felt that the correlation of the meter indication on such types of noises with their disturbing effects will be satisfactory with the time constants described and may in fact be better than when the time constants are chosen to give indications nearer the actual peak values. Mr. Burrill's suggestion of short automatic-volume-control time constants would be satisfactory provided adequate attenuation is provided by the automatic-volume-control filter having the short time constants he suggests. The automatic-volume-control filter must be such as to prevent the audio-frequency component of the noise present in the detector circuit from being applied to the amplifiers and there producing a modulation effect. An automatic-volume-control filter of short time constant with adequate attenuation of the audio-frequency component is considerably more complex, that is, must include more filter stages for the same attenuation, than one with a longer time constant.

Relaxation oscillators were considered in drawing up specifications on the noise meter, but experience to date with this type of oscillator was, in the opinion of the majority of the committee, such as to indicate that relaxation oscillators are not as reliable or uniform nor as readily checked in instrument use. The determination of the output of the relaxation oscillator by measurement of the supply voltage is an indirect method, and therefore not as accurate as measuring the current in a saturated diode. The advantage of a noise calibration source of a type to eliminate the selectivity influence in calibration mentioned by Mr. Burrill was also a factor influencing the committee's choice of a saturating diode as a noise source. Mr. Burrill is correct in stating that the theoretical shot noise from oxide-coated-filament tubes, such as are practical for portable noise meters, is not as readily determined as that from tungsten-filament tubes. However, in the noise meter described, oxide-coated-filament tubes are satisfactory, since they are used as a transfer standard, the initial calibration being in terms of an unmodulated sine-wave voltage. When so calibrated, it is felt that the oxide-coated-filament diode is a practical and uniform transfer standard readily determined by measurement of the space current. The difference between tungsten and oxide-coated, or thoriated-tungsten-filament tubes is that the latter two types do not have a sharp, definite saturation characteristic but saturate more gradually. Under such circumstances, the impedance of the diode must be taken into account. This impedance need not be specifically determined for the use described in our paper, since it is implicitly included in the initial calibration process.

C. J. Miller's comment emphasizes the point mentioned in our paper that a d-c amplifier may be employed to permit the use of a less sensitive meter. The use of a d-c amplifier and relatively insensitive meter is a more practical design combination than an attempt to secure a charge time constant of ten milliseconds with a resistor lower than one megohm. Battery-type tubes such as are required for portable noise meters have a rather high effective resistance when used to drive a diode and therefore consideration was not given to resistors of less than one megohm. All through the design of the instrument practical tube and circuit considerations were kept in mind in an endeavor to provide an instrument to give readily reproducible results.

The influence of capacitances in the measurement of high-voltage devices is perhaps more readily taken into account than might be indicated by Mr. Miller's comments. Such circuit capacitances are very important in television and have been studied by many investigators of late. The principles developed for television may be used in connection with other circuits where the stray capacitances are important, such as in this case the noise-measurement circuits. Among the several technical papers dealing with this problem, are the following:

"Analysis and Design of Video Amplifiers," S. W. Seeley and C. N. Kimball, *RCA Review*, January 1939.

"Transient Response of Multistage Video Frequency Amplifiers," A. V. Bedford and G. L. Friedendall, IRE *Proceedings*, April 1939.

These papers describe the principles and methods involved when uniform response characteristics are necessary over a range of the order of four megacycles, which is even greater than is required in the case of noise measurements.

J. J. Clarke inquires regarding means of making the noise audible. The provision for audible indication of the noise is a decided help in identifying the type of noise or in identifying broadcast stations when the equipment is used for field-intensity measurements. As pointed out in our paper, when the audio-frequency noise output is used for listening, the detector time constant should be of the order of 0.1 millisecond, which may be accomplished through the use of a switch reducing the value of the capacitor by a factor of 100 for listening purposes.

The specifications for the 150 to 18,000-kilocycle noise meter include a paragraph which states that a switch shall be provided to substitute for the weighting detector circuit a conventional second-detector circuit for carrier field-intensity measurements or to supply audio-frequency output which can be used for monitoring analysis with other apparatus.

Mr. Grimes pointed out the need for closer co-operation between the manufacturers and the field agencies. This has been one of the crying needs since the advent of radio but these two groups cannot co-ordinate their activities until standards on instruments and methods of measuring radio noise have been established. It will be only through the co-operation of these groups that sufficient data will be collected which will permit the interpretation of laboratory results in terms of expected radio noise.

It was pointed out by Mr. Hughes in his discussion that a series of field tests are contemplated in an attempt to obtain some conception of the correlation factor between laboratory measurements and field measurements. It is encouraging to note that such a program is under consideration. A number of such programs should permit the establishment of satisfactory radio noise levels for all types of apparatus.

The data included in Mr. Frick's discussion emphasizes the necessity for making both the line-to-line and line-to-ground measurements on low-voltage apparatus as well as choosing some definite terminating impedances for measuring the radio-noise-influence voltage.

The phase relation of E_1 and E_2 in figure 7 varies a great deal between types of machines and somewhat on even the same design of machines. The data shown in the paper is representative of what may be expected from a definite type of machine.

Mr. Frick stated that his tests indicated the junction between E_1 and E_2 in figure 7 is practically grounded. In practically all small commutating machines, the capacity of the windings to the frame is relatively small. There is also the impedance of the field winding between the interference generator and ground so that it would be practically impossible that the junction between E_1 and E_2 be at ground potential.

We wonder if Mr. Frick has not misinterpreted his results because if the measurement is made by figure 12 and one side of the line is grounded, the V_G could be only half of the line-to-line voltage. In other words, the line-to-line measurement is actually the line-to-ground measurement because one side of the line is grounded. The value of 1,250 microvolts appears to be correct because in the previous measurement he obtained 1,080 microvolts line-to-ground. The grounding of one side should increase this value of 1,080 as was obtained by Mr. Frick.

Mr. Frick questions table I. The caption of the last three columns was omitted in the preprint. These columns give the noise voltage on the antenna in per cent of the noise-influence voltage line-to-ground and house wiring. The collection of this type of data by various investigators will permit the establishment of noise levels for such apparatus, for instance, at 610 kilocycles (table I) the noise voltage on the antenna is 8.9 per cent of the noise-influence voltage line-to-ground with the frame grounded. If, for example, it could be determined that an average of ten per cent of the noise-influence voltage produced by low-voltage devices would be obtained at the input to the radio receiver, then it would be possible to determine the permissible radio-noise-influence voltage of a device which would permit satisfactory radio reception with any available broadcasting field-strength level.

The distorted wave shape shown by figure 16 was due to drawing leading current through the induction regulator. In the case discussed, it was necessary to eliminate the induction regulator and energize the transformer from a motor generator set. From tests to date it appears as if it will be desirable to limit the total distortion of the voltage wave to five per cent.

Mr. Goodwin's discussion is a very valuable contribution on this subject as he has covered thoroughly many of the factors given consideration while preparing the specifications of the indicating meter for the radio-noise meter.

Methods of Controlling Radio Interference

C. V. AGGERS
ASSOCIATE AIEE

Synopsis: This paper summarizes the recognized methods for reducing at the source man-made radio noises. The effectiveness of the different controlling methods is discussed in the light of efficiency and utility. The use of shunt-type filters and choke coils is so broad that this paper is confined to the more important applications of these controlling devices. Filter methods are so important that specific applications are discussed in detail. The application of these devices to other apparatus is governed by the type, location, and use of the apparatus.

THE PROBLEM of providing satisfactory radio reception to all areas of the United States requires the co-operation and co-ordination of the various industries involved. Improvements in radio service and reduction of radio noise have been made on many fronts. The solution of the problem lies in the mutual co-operation of interested parties, that is, the broadcasters in providing adequate field strength, the radio set manufacturer in supplying equipment and means for installations with minimum practicable susceptibility to unwanted signals, the radio listener in the proper installation and operation of the set, and the manufacturer and user of electrical equipment to see that it is designed and operated so as to produce the minimum of unwanted stray fields. This paper is limited to the latter subject and gives a few of the experiences of manufacturers in the reduction of radio noise from their electrical apparatus.

Radio receivers are susceptible to two kinds of noise, natural and man-made. Atmospheric noise, more generally called natural static, creates a problem that rests with the makers of the receiver and to some extent with the number, power, location, and type of broadcasting stations. Man-made radio noise, emanating from rotating electrical machines and stationary electrical apparatus, presents not only a problem to the manufacturers but also to those that apply and use the equipment, as the electrical circuit to which the apparatus is connected may have as much influence on the radio noise as the application of a suitable filter or a change in the design of the apparatus. Fortunately, noise from this type of equipment, unlike natural static, is measurable (although not always easily), and is fairly constant in intensity, for a specific piece of equipment.

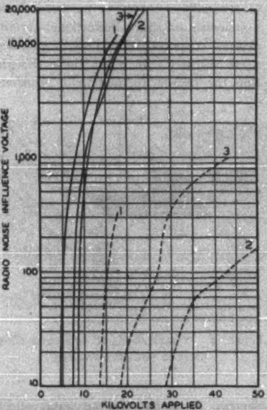

Figure 1. Noise-influence voltage of untreated and treated pin-type insulators

Solid curves—Untreated insulators
Dashed curves—Treated insulators

1—Line voltage 6.9 kv, one piece
2—Line voltage 33.0 kv, one piece
3—Line voltage 33.0 kv, multipart

Both rotating and stationary apparatus are capable of producing radio-noise voltages. The term "radio-noise voltage" will be used hereafter to describe the radio-noise voltage which appears across the input of the radio receiver. This term is used to make a definite distinction between this voltage and the high-frequency voltage measured at the terminals of electrical apparatus when connected to a coupling network. The high-frequency voltage measured at the terminals of electrical apparatus shall hereafter be referred to as the "radio-noise-influence voltage". Commutator and interrupter-type inductor motors are among the rotating apparatus that may produce radio noise while high-voltage line insulators, and to some extent low-voltage porcelain insulators are contributors among nonrotating apparatus.

Methods of Reducing or Eliminating Radio Noise at the Source

Three basic methods of reducing or eliminating apparatus-produced radio noise are recognized and applied with success. They are:

1. Consideration in design
2. Application of low-impedance shunt filters
3. Application of high-impedance series filters

All three methods are not equally effective for a particular device. The method must be suited to the specific source of noise.

Consideration in Design

The possibility of reducing or eliminating the radio-noise-influence voltages in the design of all apparatus is being studied intensively by a large number of manufacturers. All classes of apparatus cannot be economically designed to be noise free but continuous

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improvements are being made in many lines. In certain cases the consideration of the noise-influence voltage has resulted in an improvement of the apparatus.

PIN-TYPE INSULATORS

In many areas, pin-type insulators on 33- and 66-kv systems are a major source of radio noise on well-designed transmission systems properly constructed and maintained. Radio noise is produced by pin-type insulators as a result of the high voltage gradients across various sections of the insulator. These high voltage gradients are usually across air and other insulation in series. If the gradients are sufficiently high, the air is ionized, allowing a discharge to pass through the air. This discharge is a glow or an arc. In either case, the discharge produces a change in the distributed parameters of the insulator, resulting in the generation of damped transients. It is believed that both ionization and deionization set up damped transients, which appear in connected circuits as noise voltages.

On untreated pin insulators, the principal location of the high voltage gradient is between the line or tie wire and the surface of the insulator. There may also be high voltage gradients across the cement joints between shells, between the pin and the pin hole surface of the porcelain, as well as tangential gradients along the porcelain surfaces of the sheds.

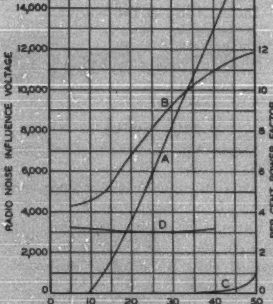


Figure 2. Noise-influence voltage and power-factor of untreated and treated pin-type insulators

A—Noise-influence voltage, untreated insulator
B—Power factor, untreated insulator
C—Noise-influence voltage, treated insulator
D—Power factor, treated insulator

By the proper application of a semiconducting glaze, it is possible to redistribute the voltage to eliminate or reduce the air discharges. The insulators thereupon become free or relatively free of radio-noise voltage as figure 1 shows.

The application of a semiconducting glaze to some pin-type insulators not only reduces the noise-influence voltages but improves the power factor, that is, decreases power loss, as well. The relation between the power factor and the noise-influence voltage of a pin-type insulator is shown by figure 2. The loss, on one of the higher-voltage pin-type insulators, at 40 kv to ground was reduced from 0.98 to 0.38 watt by the application of semiconducting glaze.

OTHER PORCELAIN STRUCTURES

Studies on a large number of apparatus bushings show that most porcelain structures are free or relatively free of radio noise, at their operating voltage. Occasionally, because of mechanical requirements or space limitations, certain designs of porcelain insulators produce noise voltages. It is on these designs that the proper application of semiconducting glaze can be advantageously applied.

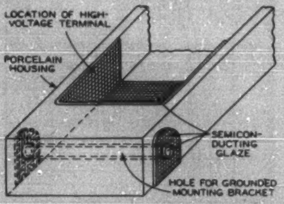

LOCATION OF HIGH-VOLTAGE TERMINAL
PORCELAIN HOUSING
SEMICONDUCTING GLAZE
HOLE FOR GROUNDED MOUNTING BRACKET

Figure 3. Application of semiconducting glaze to insulating housing

The semiconducting glaze has also been experimentally applied to small porcelain housings, separating outside mounting clamps and inside high-voltage terminals. The proximity of the inside and outside mounting clamps had resulted in high voltage gradients, which produced air discharges at the edges of the clamps along the porcelain surface and between the clamps and the porcelain surface. Upon application of semiconducting glaze to the areas shown in figure 3, the voltage at which noise was detected was increased 100 per cent.

Careful study of insulating structures has indicated that noise voltage can be generated at either the high-voltage or ground terminals or at any intermediate points where the air is overstressed. Partial breakdowns in insulating structures are in general a particularly prolific source of radio noise and can usually be identified by the high values of radio noise accompanying the partial failure.

Figure 4A. Visible corona on a ten-inch untreated suspension insulator at 27 kv

RELATION BETWEEN CORONA AND NOISE-INFLUENCE VOLTAGE

Some power companies are now including in their specifications the requirement that apparatus shall produce no visual corona at operating voltage, with the thought that such a requirement defines the radio-noise characteristics. Tests in the laboratory on a number of types of apparatus has indicated that visual corona is not necessarily a true criterion of the noise-influence voltage, because the method used to decrease the stresses in the visible area may, if the proper precautions are not taken, increase the stresses at other locations where the corona cannot be seen.

This can easily be demonstrated by using asphalt emulsion or similar means to reduce the visual corona between the edge of the cap and the porcelain on a suspension-type insulator. The amount of visual corona produced by a ten-inch suspension insulator at 27 kv to ground before and after the insulator had been treated is shown by figures 4*A* and 4*B*. The noise-influence voltages produced by these two insulators were practically the same.

ELECTROSTATIC SHIELDING

Shielding methods are used on some types of equipment. One of the more common applications of this method outside the radio field is the use of

Figure 4B. The ten-inch treated suspension insulator shows no corona at 27 kv

shielding in ignition transformers for oil burners where the spark discharge on the secondary side produces a radio-noise voltage on the primary supply line through the mutual coupling between the primary and secondary windings. The application of shielding does not provide as great a reduction in noise as an external capacitor filter. However, with this type of transformer the application of shielding is more economical than a capacitor filter.

Shielding cannot be applied satisfactorily to all types of transformers because a low-impedance high-frequency ground is difficult to obtain in the general application of this type of equipment.

Experiments and limited application indicate that shielding will find application in eliminating unwanted radiation from various other equipment.

Low-Impedance Shunt Filters

Some apparatus is so constructed that it is impossible to reduce the noise-influence voltage to a satisfactory level through design changes. The application and location of such apparatus will determine whether a filter will be required. The low-impedance shunt filter is generally found to be the most satisfactory for low-voltage applications. Low-impedance shunt filters are simply capacitors of a proper size and rating, suitably connected to reduce the line-to-line and line-to-ground noise-influence voltages. This type of filter finds wide application on commutating machines, and make-and-break devices of various kinds.

The commutating motor produces a noise-influence voltage that has two components that must be separately considered from an interference standpoint. One voltage component is produced between the terminals of the machine, and is commonly referred to as the line-to-line noise-influence voltage. The other voltage component appears between the supply lines jointly as one terminal and ground as the other, and is called the line-to-ground noise-influence voltage.

The noise-influence voltage produced by a commutating machine, is propagated along the supply lines and induces a noise in the radio-receiver antenna by mutual coupling. In general, very little radio-noise voltage is induced in the radio antenna by direct radiation because the field strength around a motor decreases rapidly with increased distance from the machine. The field strength ten feet from a one-fourth-horsepower motor will usually be less than one microvolt per meter.

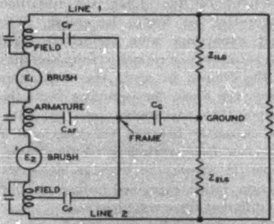


Figure 5. Equivalent circuit of a commutating machine as an interference generator

C_F—Capacity of field to frame
C_{AF}—Capacity of armature to frame
C_G—Capacity of frame to ground
Z_{1LG} and Z_{2LG}—Line-to-ground impedances
Z_{LL}—Line-to-line impedance

An analysis of the equivalent circuit of a commutating machine as an interference generator, figure 5, shows that the voltage from each line to ground, the voltage between lines, and the voltage from frame to ground will be zero if the impedance of each line to the frame of the machine is made zero.

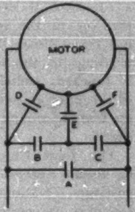
If the impedance of the lines to ground are balanced ($Z_{1LG}=Z_{2LG}$) the voltage from line to ground and frame to ground will not be greatly affected if the line-to-line impedance is made zero. Usually this impedance is not balanced, and some reduction in the line-to-ground voltage is obtained when the line-to-line impedance is decreased. This decrease is insufficient to give adequate noise reduction in most cases. The frame-to-ground voltage is practically unaffected by a reduction in line-to-line impedance under unbalanced line-to-ground impedance conditions.

The closest approach to the ideal condition of zero line-to-frame impedance is obtained with a properly designed shunt capacitance filter. These capacitance filters can be designed to give a minimum impedance over a definite frequency band. Such a filter, designed for one wave band will be less effective in other bands. If maximum protection is desired over a wide frequency band, a number of capacitance filters must be used.

In the United States the filters are nominally designed to give the maximum noise reduction over the broadcast band, which extends from 550 to 1,500 kilocycles. Some apparatus for use elsewhere, however, must be designed to give maximum noise reduction also over the 250 to 400 kilocycle band. Suitable noise reduction in this low-frequency range usually requires larger capacitors for proper filtering.

Commutating motors can be operated either with the frame grounded or ungrounded. When the frame is grounded, maximum noise reduction is obtained by placing capacitors from each line to the frame. The reduction of the noise-influence voltage from a $^1/_{15}$-horsepower motor using two 2.0-, 0.5-, and 0.1- microfarad capacitors over the frequency range 150 to 2,500 kilocycles is shown in table I. The capacitors were connected at *D* and *F*, figure 6, with a total lead length of five inches. The 2.0-microfarad capacitor gives maximum protection in the lower frequency band. The 0.5- and 0.1-microfarad capacitors produce as great a reduction of noise at 1,000 kilocycles and above as does the 2.0-microfarad capacitor. The length of leads used on capacitance filters is important because the lead inductance will tune the ca-

Figure 6. Composite figure showing connection of filter capacitors in vee, wye, and delta



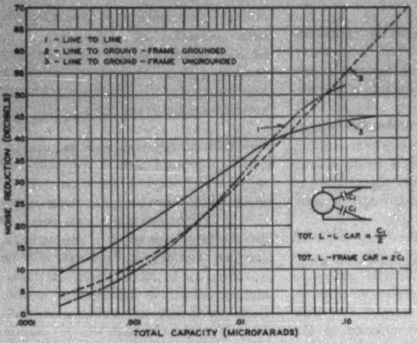


Figure 7. Noise-reduction ratios as a function of total filter capacitance

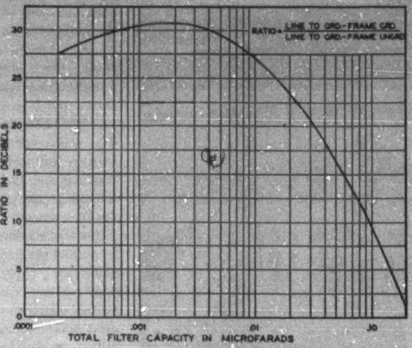


Figure 8. Ratio of line-to-ground noise with the motor frame grounded to the line-to-ground noise with the frame ungrounded as a function of the total filter capacitance

pacitance to series resonance and provide minimum impedance and maximum noise reduction at a definite frequency.

FILTERS FOR UNGROUNDED DEVICE

High-capacitance filters can be used only on grounded-frame machines. Low-capacitance filters to reduce the radio-influence voltage of ungrounded devices such as vacuum sweepers and food mixers must be used to prevent unpleasant shock when they are touched.

If the frame of an unfiltered motor is ungrounded the frame will float at a potential somewhat above ground, depending on the capacity of the windings to the frame and the frame to ground. If the frame is touched no shock will be noticed because of the small capacitance of the windings to the frame. However, when the same motor is filtered, a capacitor is usually connected from the line to the frame. This capacitor has many times the capacitance of the windings to the frame so if the frame is touched, sufficient charging current of the filter capacitor may flow through the body to ground to cause an unpleasant shock.

The Joint Co-ordination Committee on Radio Reception of Edison Electric Institute, National Electrical Manufacturers Association, and Radio Manufacturers Association has recommended that the current to frame of capacitance-type filters be held below 0.3 milliampere. Therefore a filter for an ungrounded device must be designed to give maximum reduction of noise-influence voltage with a limited amount of capacitance.

To show the effects of various size capacitors in filters used for suppressing noise-influence voltages, tests were made on a $^1/_{15}$-horsepower commutating motor. Measurements were made in accordance with the recommended practices of the Joint Co-ordination Committee on Radio Reception. The filter capacitors were connected three ways; in vee, one capacitor from each line to the frame; in wye, two capacitors across the lines, one capacitor from the midpoint to the frame; and in delta, one capacitor between the lines and a capacitor from each line to the frame. Table II shows the average noise reduction ratios, expressed in decibels, for the different filter combinations.

The relation between total capacitance and noise reduction expressed in decibels is shown in figure 7. The abscissa values are in terms of total capacitance line-to-frame or line-to-line and are used with the proper noise-reduction curves. As an example, assume a filter combination of a 0.01-microfarad capacitor connected from each line to the frame. The line-to-line capacitance would be 0.01/2 or 0.005 microfarad and the line-to-frame capacitance would be 2×0.01 or 0.02 microfarad. The 0.005 figure would be used in referring to the line-to-line curve and the 0.02 figure to the line-to-frame curve.

These curves show that with the motor frame ungrounded, little improvement in line-to-ground noise is gained by increasing the capacitance of the line-to-frame capacitor beyond 0.02 microfarad. In general this value will vary with the stray capacitance of the motor to ground as well as the size and type. For the line-to-line and line-to-ground condition with the frame grounded, the improvement increases with increase in capacitor size.

The ratio of line-to-ground noise with the motor frame grounded and ungrounded for various sizes of capacitors is shown in figure 8. To reduce the number of measurements only the vee filter connection was used. The data

Table I. Effect of Capacitor Size in Reducing Radio Noise From $^1/_{15}$-Horsepower Motor

Type of Measurement	Capacitor C1 (Microfarads)	Reduction of Radio-Noise Output (Decibels)*						
		200 Kc	340 Kc	600 Kc	1,000 Kc	1,400 Kc	2,000 Kc	2,500 Kc
Line-line	0.1	29.7	33.9	40.5	45.9	41.4	46.7	41.7
	0.5	43.6	49.6	64.8	50.7	39.2	41.8	40.0
	2.0	60.1	54.8	54.8	48.6	38.7	41.2	39.2
Line-ground, frame ungrounded	0.1	36.8	44.0	41.6	37.9	27.2	38.1	37.4
	0.5	41.2	46.7	41.0	37.9	30.4	37.6	37.4
	2.0	40.6	45.2	42.1	39.1	33.5	39.9	39.1
Line-ground, frame grounded	0.1	40.5	47.0	53.0	55.0	44.8	44.8	39.8
	0.5	56.0	62.0	60.6	44.6	43.4	53.4	40.9
	2.0	65.7	77.2	73.7	49.8	49.8	49.2	49.3

$$* = 20 \log \frac{\text{(radio-noise voltage unfiltered)}}{\text{(radio-noise voltage filtered)}}$$

Table II

Test	A	B	C	D	E	F	Maximum Filter Current at 120 Volts, 60 Cycles (Milliamperes)	Average Noise Ratio in Decibels, Unfiltered to Filtered: Line-Ground (Grounded)	Line-Ground (Ungrounded)	Line-Line*
1	0.05						0	0	0	46.9
2				0.0001		0.0001	0.005	4.6	9.8	0
3				0.0005		0.0005	0.023	11.4	19.4	3
4				0.001		0.001	0.045	15.7	24	6.7
5				0.003		0.003	0.135	24.4	31.2	12.7
6				0.006		0.006	0.27	32.6	36.2	16.8
7				0.01		0.01	0.45	37	40	24.2
8				0.05		0.05	2.25	55.7	44	43
9				0.10		0.10	4.5	63	45	48.5
10				0.2		0.2	9	69.1	40	52.2
11	0.05			0.003		0.003	0.27	26	31.2	46
12	0.05			0.006		0.006	0.54	32	37.1	46.8
13	0.05			0.01		0.01	0.91	36.5	40	46.8
14	0.01			0.003		0.003	0.27	25.7	31.2	33.3
15	0.01			0.006		0.006	0.54	31.7	37.1	33.3
16		0.01	0.01		0.006		0.21	23	29.4	26.7
17		0.01	0.01		0.01		0.30	27.7	32.7	26.4
18		0.10	0.10		0.003		0.13	17.8	25.4	46.2
19		0.10	0.10		0.006		0.26	24.8	31.2	46.2
20		0.10	0.10		0.01		0.43	51.4	41.5	46.2

* There was no substantial change in the line-line noise with frame grounded or ungrounded.

shows that for the smaller sizes of filter capacitors the radio-noise voltage to ground is increased by grounding the frame while for the larger sizes of capacitors, the noise voltage is decreased by grounding the frame.

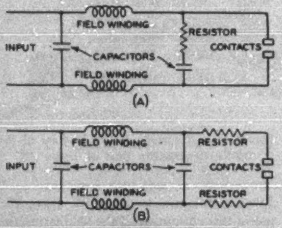


Figure 9. Filter circuits for interrupter-type inductor motors

It should be noted that these data indicate results on a particular motor and while it is indicative of the performance of commutating motors, the conclusions should not be generally applied to all apparatus.

Make-and-Break Contacts

Apparatus in which electrical contact is made and broken frequently is the source of an annoying form of interference. Make-and-break circuits are usually filtered with a resistance and capacitance combination. The amount of capacitance across the contacts is important from the standpoint of life of the contacts and the reduction of noise-influence voltage. The resistance properly placed in the circuit, performs two functions; it limits the short-circuit current of the capacitor, and it acts as an internal impedance in the noise-voltage circuit. A combination of capacitance and resistance can usually be chosen to achieve the desired noise reduction.

The interrupter-type motor is an example of this form of noise generator. This motor depends for its operation on a properly synchronized make and break of the current flowing in the field coils.

Capacitance is usually connected across the make and break contacts of the motor to reduce arcing and heating of the contacts. This capacitance must be kept sufficiently low that the contacts will not weld and cause sticking when the capacitance is short-circuited by the contacts. The capacitance that is generally used for this purpose is not sufficient to reduce the noise value to a satisfactory level. The application of larger capacitance requires the addition of resistance to reduce the capacitor discharge when the contacts close. Two resistance and capacitance combinations which are satisfactory for this type of application are shown by figure 9. The combination shown by figure 9*B* has been used to reduce the noise voltage of an electric razor to less than 0.5 per cent of its original value.

The circuit shown by figure 9*A* does not give the reduction of figure 9*B* on this type of make-and-break circuit, but is applicable where resistance cannot be placed in series with the line.

High-Impedance Filters

Radio-Frequency Choke Coils

When high-voltage apparatus is considered, the cost of shunt-type filters becomes uneconomical and high-impedance series filters must be applied. These filters take the form of tuned choke coils and find application on high-voltage transmission lines, telephone lines, and distribution networks. They are not usually used to reduce radio

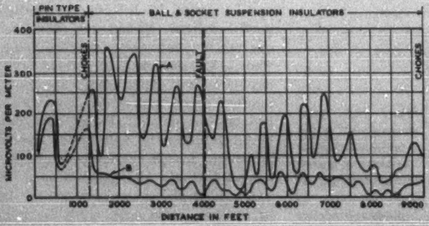


Figure 10. Noise measurement along a 66-kv transmission line showing the effect of reflections

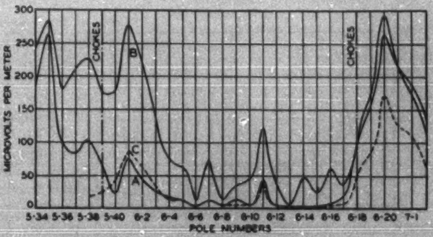


Figure 11. Noise measurements along a 66-kv transmission line showing the effect of parallel lines

A—Line normal
B—Telephone choke short-circuited
C—Line dead at pole 5-39

Table III

Test Number	Description of Tests	Noise Starting Voltage (Kilovolts)
1	Standard 6.9-kv insulator on wood cross arm with wood pin	19.8
2	Standard 6.9-kv insulator on wood cross arm with metal pin. Pin not grounded	7.7
3	Standard 6.9-kv insulator on wood cross arm with metal pin. Pin grounded	4.9
4	Standard 6.9-kv insulator on metal pole-top pin. Pin not grounded. Guy-wire hook and lag bolt close to pin grounded	5.6
5	Standard 6.9-kv insulator on metal pole-top pin. Pin not grounded. Grounded guy-wire hook and lag bolt removed	5.75
6	Standard 6.9-kv insulator on metal pole-top pin. Pin grounded	4.9
7	Treated 6.9-kv insulator on pole-top pin. Pin not grounded. Guy-wire hook and lag bolt close to pin grounded	21.3
8	Treated 6.9-kv insulator on wood cross arm with metal pin. Pin not grounded	13.8
9	Treated 6.9-kv insulator on wood cross arm with metal pin. Pin grounded	20.25
10	Treated 6.9-kv insulator on wood cross arm with cross arm brace grounded. Pin not grounded	10.75

noise at the source but are used to attenuate the transmission of noise from specific sections of the circuit.

Field experience has demonstrated the radio-frequency choke coils can be applied to transmission lines and give satisfactory performance provided they are applied properly. One of the most common mistakes made in their application is attempting to sectionalize a part of a line without completely eliminating the radio noise in the section. When noise is generated in the sectionalized part of the line, the radio-frequency noise is reflected at the choke coils. This noise may be amplified a number of times, depending upon the distance between the chokes.

An example of such a situation is illustrated by figure 10 curve *B* indicates the noise field strength measured along a reinsulated section of a 66-kv pin-type transmission line. The section was reinsulated with suspension insulators. Choke coils were installed at each end of the reinsulated section.

After the line had been in service for several years, a line fault occurred which resulted in numerous radio complaints. The line was again surveyed and field strengths as shown by curve *A*, figure 10, were obtained. Normally a high noise level is obtained at the pole where the fault occurs, however, it may be seen by referring to curve *A*, that noise peaks occurred all along the major portion of the reinsulated section. Several unsuccessful attempts were made in an effort to locate the fault. As a last resort, the chokes were short-circuited and the fault was readily located at the point indicated on figure 10. After the fault had been corrected, the choke coils were again inserted in the line and the noise returned to its original level.

It should be noted that the noise peaks occur approximately every 450 feet, which is a half wave length of 1,090 kilocycles. This is exactly the frequency of measurement indicating reflections from the choke coils.

This same effect is obtained when a reinsulated section is not entirely free of radio noise. A number of radio-frequency choke-coil installations have been declared unsuccessful due to insufficient reduction of noise. These unsatisfactory choke-coil installations have in general been caused by the lack of proper reinsulation in the desired sections. If reflections are present in an apparently unsuccessful choke-coil application, a complete inspection of the reinsulated section should be made.

The proper application of radio-frequency choke coils requires that they be inserted in all parallel lines. The radio-noise field strength produced by a 66-kv line with a paralleling telephone line on the same poles, after it had been reinsulated and choke coils inserted in both the 66-kv line and telephone line, is shown by curve *A* of figure 11.

During a storm one of the telephone choke coils was destroyed by lightning and a resurvey of the line indicated a noise level as shown by curve *B*. After the insertion of a new telephone choke coil, the noise level again was reduced to that shown by curve *A*. The noise level of this line when the line was open at pole 5-39 and energized from the other end is shown by curve *C*.

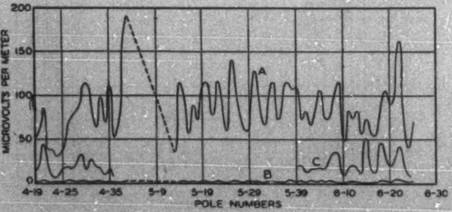


Figure 12. Noise measurements along a 66-kv transmission line showing the effect of applying asphalt emulsion to pin-type insulators

Field Investigations of Noise From Transmission-Line Equipment

In certain areas an important source of radio interference produced by electric-power-system apparatus is from high-voltage transmission lines. In general its importance arises not so much from its severity as from the fact that it may be continuously present. The situation is aggravated by the fact that such equipment is located in areas of low broadcasting field strengths.

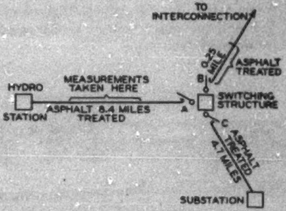


Figure 13. Schematic switch diagram of the 66-kv transmission line of figure 12

Less complaints would arise from the use of this equipment if the field strengths that are available during evening and night hours, were available during the day. Usually the daytime field strengths are from one to ten per cent of the evening and night field strengths, and as a result complaints are received even with low levels of radio-noise voltage.

Radio noise from high-voltage systems can be caused by defective apparatus; poorly spliced, nicked, or small-size conductors; improperly designed hardware; hardware installed without sufficient clearance; arcing horns; improperly inserted cotter pins, and many other less obvious details. Radio noise can be produced by the high-voltage system apparatus in the performance of its normal duty and must be eliminated by design after the cause is detected. At substations, radio noise is usually pro-

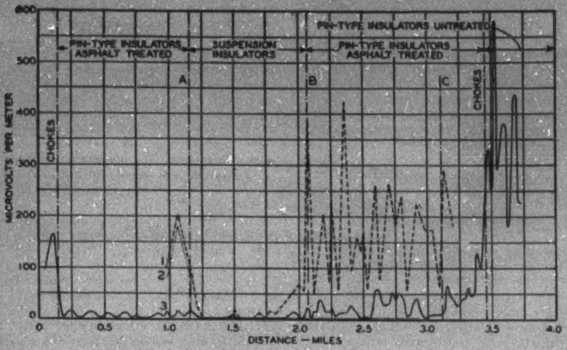


Figure 14. Noise measurements along a 66-kv transmission line with different types of reinsulation

duced by certain types of apparatus whose limitations in this respect must be recognized and allowed for.

Rural Lines

It is generally believed that insulators on 11.8-kv rural transmission lines do not produce radio interference. Where wood pins are used this is usually true. However, if metal pins are used with standard insulators, there is considerable possibility of radio interference. A number of such cases have been investigated and one in particular where a power company had changed from a cross-arm construction to a pole-top construction. After the change had been completed, this line, previously interference free, became the cause of many radio complaints.

To check this condition, a pole which had been standing in the open for several years, was brought into the laboratory and arranged so that the cross arm, pole-top pin, guy-wire hook, and lag bolts were mounted exactly according to the public-utilities specifications. The results of these tests are shown in table III.

The insulator on the wooden pin did not create any noise below 19.8 kv. This same insulator with a grounded metal pin started to create noise at 4.9 kv, as measured by the circuit adopted by the joint co-ordination committee of EEI, NEMA, and RMA on radio reception. This insulator mounted on the pole-top pin started to produce noise at 5.6 kv. With the guy-wire hook and lag bolt removed from the pole, this insulator still started to produce noise at 5.75 kv.

Tests 8, 9, and 10 are interesting because they show that under actual field conditions the starting noise voltage can be lower when the pin is ungrounded, because of the leakage current flowing from the metal pin to the cross arm or from the cross arm to a grounded cross-arm brace.

Test 7, indicated that it would be necessary to use radio-interference-proof insulator on the pole-top pin if interference was to be eliminated. This procedure was adopted by one power company. Another company transferred to a wooden pin mounted on a metal bracket.

Application of Asphalt Emulsion

Asphalt emulsion has been used successfully in many applications to reduce the radio noise produced by high-voltage pin-type insulators. The effect of such an application on an actual line is shown by figure 12. The treated line, switching stations, and the point where the measurements were made are schematically shown in figure 13. Curve *A* of figure 12 shows the noise level of a section of this line before application of the asphalt emulsion with all switches indicated in figure 13 closed. Curve *B*, figure 12, shows the noise level of the same section after the application of asphalt emulsion and switch *C*, figure 13, open. Curve *C*, figure 12 shows the noise level along parts of this section after the application of asphalt emulsion and switch *C*, figure 13, closed. Considerable radio noise is shown coming from the untreated section. Also, figure 12 shows that the noise is not attenuated as rapidly as is frequently assumed because the measurement made at pole 4-19 is approximately five miles from switch *C*. Choke coils should be inserted between switch *C* and the untreated section for maximum effectiveness.

The reduction in radio noise from another line with the application of asphalt emulsion is shown in figure 14. This line was a 66-kv pin-type transmission line mounted on wood poles.

The first attempt at reducing the radio noise along a section of this line consisted of reinsulating the section between *A* and *B* with suspension insulators, treating the pin-type insulators from point *B* to point *C* with asphalt emulsion, and inserting radio-frequency choke coils at *A* and *B*. The noise along this line with this construction is shown by curve 1, figure 14.

This installation did not protect sufficient area and tests were conducted to determine the noise level of asphalt-treated section by cutting the line at point *C* and energizing the line from the other end. The noise level under the asphalt-treated section (*B* to *C*) then became very low, as indicated by curve 2, figure 14.

The line was reinsulated as shown by figure 14, and the choke coils moved to their final position as shown. The noise level over the entire area is shown by curve 3, figure 16.

The results shown by these curves demonstrate that:

1. Asphalt emulsion, when properly applied, is fairly effective in reducing inherent pin-type insulator noise.
2. Suspension insulators provide the lowest noise level.
3. Radio noise travels for a considerable distance along the line conductors making the use of radio-frequency choke coils necessary in order to avoid the replacement or treatment of an excessive number of insulators.

Discussion

Charles J. Miller, Jr. (The Ohio Brass Company, Barberton): Mr. Aggers has summarized quite clearly some of the principles that may be applied to control radio interference.

In connection with the application of a semiconducting glaze to pin-type insulators he states that its use improves the power factor of the insulator. He gives no explanation as to why the losses are reduced. In the case of the ordinary insulator, the corona discharge at the tie wire and conductor, and at other parts of the insulator if it exists, requires a small but definite amount of energy to produce it. This represents an energy loss in excess of that occurring within the porcelain itself. With the application of a semiconducting glaze which effectively suppresses the formation of corona, the total losses of the insulator are reduced and accordingly the power factor is improved.

The electrostatic capacitance of the treated unit is usually somewhat greater than that of an untreated unit, and if the inherent losses in the porcelain are equal in each insulator, the power factor of the treated unit will be smaller than that of the untreated unit. However, it is probably the suppression of the corona discharge that contributes the greater share to the lower power factor of the treated insulator.

The comments on the relation between corona and noise-influence voltage are very interesting and confirm many similar observations made at the Barberton laboratory of the Ohio Brass Company. For example, tests on three-unit strings of suspension insulators have shown wide variations between the visual corona point and the radio-influence voltages. On two similar strings the lowest visual corona point detectable corresponded to a radio influence voltage of 5 microvolts in one case and 50 microvolts in the other.

The use of radio-frequency choke coils in connection with high-voltage transmission lines to isolate or block out the interference from a given protected area is a well-known principle. The use of tuned choke coils in series with every conductor at the point of installation will provide a very effective blockade for a given band of frequencies centered on the resonant frequency of the choke. It has been customary to tune these coils at a frequency somewhere near the middle of the broadcast band. In special cases the coils may be tuned to suppress any particular band of frequencies. However, the use of all-wave receivers now makes it desirable to provide wide-band suppression. A statement of how wide a band of frequencies may be satisfactorily suppressed by the use of tuned choke coils and what provisions could be made for wide-band suppression would be appreciated.

The application of asphalt emulsion for the suppression of radio noise from pin-type insulators has been used for a number of years. However, it is generally conceded that the treatment is not permanent as it tends to deteriorate after a few years. It would be interesting to know the life of the asphalt treatment on the transmission lines discussed by Mr. Aggers in this paper.

W. F. Grimes (Radio Interference Engineering Bureau, Inc., Los Angeles, Calif.): C. V. Aggers in his paper presents a number of methods for controlling radio noise originating under specified conditions. The suppression methods described have all demonstrated their efficiency under actual operating conditions and they are in everyday use. The title of the paper has been well chosen, suggesting as it does additions to existing equipment for the sole purpose of improving radio reception.

It may be of interest to consider, briefly, the conditions under which controlling methods must be applied and the relation of these conditions to other circumstances under which the radio listener has been found to be not satisfied with radio reception. The data to be considered are based on the experience of the co-operative radio-interference investigating agency which is operating in and near the city of Los Angeles, in an area of comparatively high broadcast field strengths. The investigations made by this agency may be divided into four groups which represent separate responsibilities for investigation. Forty-eight per cent of the investigations are traceable to one or more defects in the listener's installation or receiver; 24 per cent, either too intermittent to trace or cleared before the source determined; 20 per cent, electrical apparatus or appliances, exclusive of electric-utility apparatus; 8 per cent, electric-utility apparatus.

Those sources of interference to which controlling methods should or may be applied represent less than 20 per cent of the total investigated. Of this number a majority require servicing or replacement rather than the addition of filters or other suppression equipment. Worn and burned contacts, loose connections, broken bushings, and similar sources of interference should be repaired or replaced.

Nearly 50 per cent of the radio listeners who report interference do so because they lack understanding of the problem or have not been properly advised. These individuals believe their trouble is due to interference when actually it is due to a defective receiver or lack of correct receiver installation.

These remarks are intended only to point out that in order to develop maximum use of radio-receiving equipment it is necessary to consider all of the factors disturbing to the listener. Methods of controlling radio interference must eventually be applied to apparatus, when required, at the point of manufacture. The radio and electrical industries are urged to consider the general problems of the listener and to initiate an active co-operative campaign designed to develop listener interest and understanding.

A. M. Lane (representing General Electric Supply Corporation and Locke Insulator Corporation, San Francisco, Calif.): Mr. Aggers has presented a very interesting paper. These remarks are with reference to that part of his paper dealing with radio-frequency choke coils and are somewhat supplementary. Possibly a brief description of radio-frequency attenuation coils for wire lines would be of interest.

Radio noise caused at any point on a power system travels along the wires with very little attenuation, thus producing interference for many miles. To prevent all interference at a certain location, it has been necessary in the past to clear up all noise sources for many miles in all directions along the wires. Experience has shown this to be extremely difficult and expensive, and the job is never finished because new sources of noise are continually forming.

Fortunately, radio noise is objectionable only at certain sections of most power lines, such as in thickly settled districts or near radio-receiving stations. In the course of our investigations we found that the Radio Interference Specialty Company had developed a practical method which prevents radio noise of all frequencies from passing a given point in a power line. Thus, a section of line where noise is objectionable can be isolated from the rest of the power system with regard to radio-frequency noise and at the same time not interfere with the power service, or the transmission of carrier frequencies. These isolated sections can then be economically freed from noise sources, and the noise problem is thus eliminated permanently.

The isolation of sections of power lines from radio noise is accomplished by the use of very small inductance coils properly spaced along the line at each end of the section to be isolated. Two of these coils in series in each power wire constitute an attenuation section and reduce the noise level approximately 25 decibels. Two sections of attenuation consist of three coils in series, the center coil being larger than the end coils. This arrangement gives approximately 50 decibels noise attenuation. The 60-cycle loss for two such sections is approximately equal to 1,200 feet of line.

In practice, it has been found that the reflections at the coils are so low that sources of local trouble within the protected area may be readily located.

The spacing between coils is important and depends upon the electrical constants of the power line and the lowest frequency it is desired to stop. However, the spacing can always be arranged so that the coils can be mounted on existing poles. The coils should always be installed where there are the least number of paralleling wires such as telephone lines and secondaries, as these lines pick up and carry the radio noise. All coils are fitted with spark gaps as a protection against lightning and have been tested with artificial lightning of 2,000,000 volts.

Attenuation coils meeting the requirements of the telephone and telegraph companies have been developed for installation on open-wire lines, even though such lines must accommodate telephone and telegraph carrier channels.

One of the earliest of the many existing installations was made for the protection of the radio-receiving station of the Mackay Radio Telegraph Company located about 40 miles south of San Francisco near the ocean. In this installation coils were placed in the 11-kv power wires and the telephone toll wires. This installation has thoroughly protected all frequencies for the radio company's world-wide receivers since the first part of 1933.

Measurements of the amount of reduction in interference have been made by the telephone company and several power companies. These measurements show that a reduction of 98 per cent in the radio noise may be obtained over the frequency band from 500 kilocycles to 14 megacycles. It is probable that the protection extends to much higher frequencies, although no such measurements have yet been made.

R. J. Sullivan (Commonwealth and Southern Corporation, Jackson, Mich.): This paper is a valuable contribution to the literature on the subject of radio interference. This is a field of investigation in which much remains to be accomplished, and in which a comparatively small amount of published information is available. No doubt many independent investigations have been carried on, the results of which have not been published, perhaps partly because more or less makeshift apparatus was used which did not permit the results to be published in the quantitative form of curves and tabulations which are usually considered to be requisites of successful technical papers. This situation will be improved if the standard test circuit recommended by the Joint Co-ordination Committee on Radio Reception becomes widely used in such investigations.

The section of the paper dealing with radio interference produced by electric-power-system apparatus invites further discussion. The statement that radio noise produced by high-voltage system apparatus "... must be eliminated by design after the cause is detected", is part of the story; and the statement that "radio noise from high-voltage systems can be caused by defective apparatus," is another part. Improvements in design will not eliminate individual cases of interference due to defective materials or poor workmanship. In either case if the noise-radiating equipment is allowed to get into service it may cause considerable irritation and expense to its purchaser before being identified as a source of radio interference. This paper offers useful information on dealing with equipment already in service, but it would be very interesting to know what is being done by the manufacturers in the way of production testing of equipment for radio interference before it leaves the factory. This would be considered as an important part of the test procedure by those members of the operating organizations who have the job of investigating radio-interference complaints.

It is not clear what is meant by "high-voltage systems". Severe interference can be caused by defective distribution apparatus of voltages at least as low as 6,900 volts. Interference has been found to originate in distribution transformers in the high-voltage bushings, high-voltage leads and taps which are not spaced sufficiently, defective insulation which would withstand the usual high-potential insulation tests, and other defects, such as carbonized terminal blocks. One investigation disclosed a batch of new 7,200-volt distribution transformers which caused a very high level of radio interference due to the fact that they had been overbaked during the impregnation process at the factory which raised blisters in the insulation between the high-voltage and low-voltage windings. The potential gradient across the insulation produced a glow discharge in these voids which started at less than half rated voltage. The overbaking also left the insulation in a brittle condition, but the defect was only detected by the radio-interference test, which could have served the same purpose during a factory test.

A considerable range of starting voltages for radio interference has been found for transformer bushings filled with asphalt compounds, above 7,200 volts, due to voids in the compound. This characteristic is subject to control in the manufacturing process if it is taken into consideration. Many of the offending bushings are old types, of which large numbers are still in service and still being manufactured for replacements.

In connection with the treatment of insulators to reduce radio noise, a discussion of the following points should be of interest:

> What variations can be expected in the radio-interference starting voltages of a number of semiconducting glaze insulators of the same design with respect to rated voltage?
>
> What effect does dirt have on the radio-interference qualities of treated insulators?
>
> How long can asphalt emulsion be expected to act as a radio-noise-reducing agent when exposed to normal weather conditions?

C. V. Aggers: Mr. Lane has presented a very interesting discussion on the radio-frequency attenuation coils which have been used to isolate sections of telephone and power lines. These coils are apparently designed so as to provide a single- or two-section T-type low-pass filter in which the capacity of the conductors to ground is used as the capacitance element of the filter.

This method of isolation should, and apparently does provide adequate protection over a very wide frequency range provided the precautions enumerated by Mr. Lane are adhered to.

Mr. Miller has explained why the losses are reduced by the application of semiconducting glazes. However, if the proper precautions are not taken, it is possible to increase the inherent loss of an insulator to such a value that these losses may exceed the reduction obtained by the elimination of the corona discharges.

Mr. Miller pointed out that the use of all-wave receivers now makes it desirable to provide wide-band suppression. In general, the present type of chokes are designed to provide protection in the broadcast band, 550 to 1,500 kilocycles. Choke coils to provide protection over a wider band have not, in general, been found necessary up to the present time. If and when wider-band-spread protection is desired, the method discussed by Mr. Lane will probably offer the most economical solution.

Chokes for other applications have been designed for wider band spreads. One application required a coil that had a minimum impedance of 10,000 ohms from 300 to 3,000 kilocycles.

Mr. Miller requested the life of the asphalt treatment on the transmission lines discussed. These lines were inspected by the power company after approximately five years of service and it was felt, at that time, that they could reasonably expect a life of ten years from this treatment. The life of this treatment, of course, varies with the precautions taken during its application as well as the climatic conditions.

Mr. Sullivan desires to know what is being done by manufacturers in the production testing of equipment radio interference before it leaves the factory. At least one company is testing all treated insulators before shipment. Other companies are considering the same procedure. In general, radio interference is considered in the development of the certain type of equipment and tests conducted on the first production lots. If these units are satisfactory then sample lots are checked at various intervals.

It is realized that severe interference can be produced by 6,900-volt equipment. In general, the term high-voltage systems has been applied to voltages above 1,000 volts in order to distinguish this equipment for the equipment that operates from the distribution secondaries.

Referring to Mr. Sullivan's question regarding the variation that can be expected in the ratio-interference starting voltage of a number of semiconducting-glaze insulators of the same design, no one has been able definitely to define starting voltage. We have not attacked the problem from this angle because some insulators produce a measurable amount of noise voltage at what you might call its starting voltage. On other insulators the voltage may vary five to ten kilovolts between the point of first detection of noise by a headset and the point where the unit produces a measurable value (in the order of 10 or 20 microvolts). However, if a definite number of microvolts is used to define the starting voltage, there may be considerable variation in the starting voltage. This variation will depend upon the type of treatment used on the insulator and the method of applying the test voltage.

The general method of attack has been to measure the noise at various voltages and plot an envelope curve. This curve gives the variation which can be expected in any one design and the maximum value must be below any noise voltage which may be considered for standardization.

The type of data described above is for use only by the design engineer as the operating engineer is only interested in the noise voltage at the operating voltage which after all is the criterion as to whether an insulator will be a cause of radio-interference complaint.

The insulator manufacturers in general are attempting to establish a definite number of microvolts for a particular voltage classification. The values which they are considering are sufficiently low to prohibit any possibility of a noise complaint from this equipment.

Experience in the field to date has indicated that dirt has little or no effect on the radio-interference qualities of a treated unit. Experience in the laboratories has indicated the characteristics are changed slightly; these changes have generally indicated an improvement of its characteristics but not sufficient to warrant any noticeable change in line noise.

Information in regard to the expected life of asphalt emulsion is covered in my reply to Mr. Miller.

Mr. Grimes pointed out in his discussion some of the other factors that must be considered and remedied before satisfactory radio reception can be obtained by all listeners. As pointed out in the paper, the ultimate solution of this problem requires the co-operative efforts of the radio listeners as well as all the involved industries.

"Magne-Blast" Air Circuit Breaker for 5,000-Volt Service

E. W. BOEHNE
MEMBER AIEE

L. J. LINDE
ASSOCIATE AIEE

Synopsis: Fundamental principles of magnetic action and thermal reaction have been united in an original manner to create a new circuit interrupter, the "Magne-Blast" air circuit breaker. Intended for general applications in switching, controlling, and protecting 2,300- to 5,000-volt circuits, this breaker, throughout a comprehensive test program, has proved satisfactory in every requirement. An open presentation of test results shows how an efficient arc chute functions thermally to decay and extinguish the arc with a minimum of voltage disturbance. The breaker is immune to circuit recovery characteristics.

Principles of performance are discussed using the familiar terms of current, voltage, resistance, and reactance in showing their effect on the instantaneous values of phase angle, current reduction, and the attendant rates of rise of recovery voltage. The co-ordinated electrical and mechanical design is described in some detail and a typical arrangement is shown as used in a metal-clad equipment.

Background

OIL circuit breakers have a history beginning with the early application of alternating currents in the electrical power industry. Throughout this period, oil breakers have maintained a satisfactory record of performance. Failures have been rare, maintenance low, and it has been possible to build circuit breakers of all ratings. Although oil, by its presence alone, constitutes a hazard from leakage or fire, operating engineers will seldom sacrifice any of the proved advantages of modern oil circuit breakers to eliminate the rather remote hazard of fire. But, when freedom from oil is added to all the good points, an oilless breaker becomes attractive. The magnetic air breaker, though long used on d-c systems, has been of minor importance in the intermediate-voltage circuits of the utilities.

To be completely successful in competing with an oil circuit breaker, an air circuit breaker cannot afford to be its inferior in any respect. Available field experience permits a ready review of conventional air breaker performance, for manufacturers have long succeeded in maintaining the position of such breakers in the railway and industrial fields. This experience has proved these magnetic breakers handicapped in their appeal to the utilities only because of their:

1. Poor space factor.
2. Exposed flame, demanding careful segregation.
3. Visual and audible disturbance.

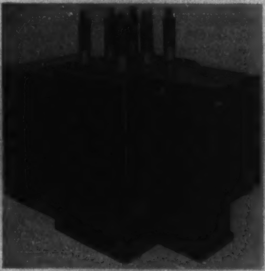


Figure 1. A left-front view of a 1,200-ampere Magne-Blast air circuit breaker for use on 2,300- to 5,000-volt circuits

Insulated for 7.5 kv, rated interrupting capacity 100,000 kva. This breaker is arranged for use in a vertical-lift metal-clad equipment

These same breakers, however, provide an argument to their favor in an acknowledged record for:

1. Positive circuit control.
2. Little deterioration of materials during interruption.
3. Economical operating life.
4. Simple contacts and arc-interrupting details.

Though boasting of the obvious advantages here listed, magnetic air breakers have been forced to a secondary position by the problems of size, flame, and disturbance. The "Magne-Blast" circuit breaker offers a solution to these problems while maintaining every advantage of previous breakers.

Application

Unless custom-built to meet the requirements of a particular application, the value of a power circuit breaker is enhanced by the variety of applications it will satisfactorily meet. The breadth of this field is responsible for the variety of characteristics that are today demanded of breakers. Fortunately these characteristics may be grouped into three classes: switching, protection, and control; each making its individual demands on equipment.

Listed below are the most important characteristics of a good breaker:

1. Reliable circuit interruption.
2. High insulation level.
3. Long and economical operating life.
4. Little hazard and disturbance.
5. Small size.
6. High operating speed.

The air circuit breaker here described meets every objective listed.

Under the heading of performance will

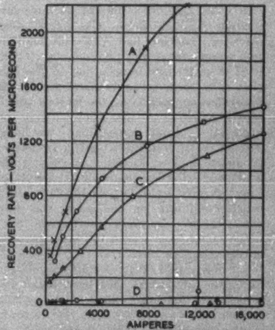


Figure 2. The influence of the Magne-Blast air circuit breaker on the voltage-recovery characteristics of typical circuits

A, B, and *C*—Characteristic rates of rise of recovery voltage for three test circuits

D—Rates of rise of recovery voltage of the same circuits during Magne-Blast circuit-breaker tests

These data were recorded with a cathode-ray oscillograph

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Reliance has been placed on the co-operative efforts of many individuals during the successful development of the Magne-Blast air circuit breaker. Appreciation is expressed for the valuable contributions from the staffs of the circuit-breaker test laboratory and the research laboratory of the General Electric Company. The Philadelphia Electric Company and the Aluminum Company of America are credited with the original applications of this breaker to their systems, thus providing initial service experience.

be found a rather thorough presentation of the interrupting ability of the breaker. The device exhibits no fire throughout the limits of its interrupting rating. This, together with desirable space factors permitted by an efficient arrangement of materials, has enabled the breaker to be applied in standard vertical-lift metal-clad gear. Here the two enhance each other, the gear providing all its many advantages of safety, installation costs, and appearance; the breaker providing safe and reliable circuit control.

The long operating life of the air device makes it particularly attractive in services where duty demands frequent operation. In fact, the air breaker makes certain industrial functional operations less complicated. For example, present - day furnace operators have learned by experience that if the breaker current is reduced before the breaker is operated, oil deterioration is reduced. This process is recommended at the sacrifice of the time to raise the furnace electrodes. The air device simplifies these industrial functions.

An exceedingly attractive feature of the new breaker is its immunity to circuit recovery characteristics. This desirable characteristic is achieved by the "Magne-Blast" arc-resistance characteristic which, in spite of the inductive properties of the circuit, holds the recovery rate and peak recovery voltage to a value which is only a weak counterpart of the circuit characteristic. These features are described in figure 2 and will be discussed later in detail. In brief, this characteristic of the breaker broadens its field of application, particularly where sudden switching surges endanger associated equipment.

Taken as a whole, this oilless device, incorporating all the sturdiness and reliability of the oil breaker, together with the attendant ease of circuit interruption, space considerations, and maintenance, offers to the industry a new interrupter which should find a wide field of practical applications.

Performance

Of all power equipment, the breaker has to meet the most unusual conditions. It must function unerringly to divorce the energy source from the circuit in distress and still function as the energy valve under all normal conditions. In addition to its protective values, the breaker may be utilized as a switching device accumulating many thousands of operations throughout the year. These, and other requirements, demand that the power breaker perform as a compact, sturdy, and co-ordinated application of mechanical and electrical principles.

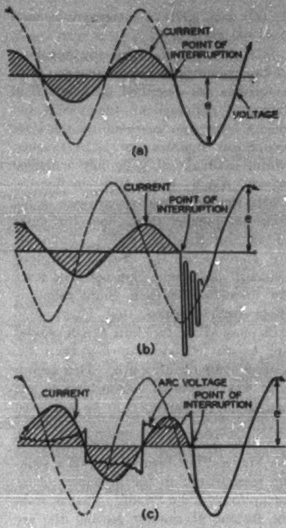

Figure 3

(a)—A typical interruption on a unity-power-factor circuit where the current and voltage zeros coincide

(b)—A typical interruption on a zero-power-factor circuit. Interruption occurs at a current zero with recovery voltage at a peak, thereby encouraging high-frequency transients and a high recovery rate

(c)—A Magne-Blast interruption of a zero-power-factor circuit illustrating the effect of arc resistance in shifting the current zero toward voltage zeros to reduce the rate of voltage recovery

Table I. Interrupting Tests on 600-Ampere Indoor-Type Magne-Blast Air Circuit Breaker

Interrupting Rating—100,000 Kva at 5,000 Volts, Three Phase

			Current (Amperes)				
Volts	Operation†	Test Frequency	Maximum Peak	Initial in Arc (RMS)	Three Phase Kva	Arc Duration (Cycles)	Interrupting Time* (Cycles)
2,300	O	60	690	330	1,320	5.5	9.5
2,300	O	60	1,230	660	2,600	3.0	7.5
2,300	O	60	2,600	1,150	4,600	2.0	6.8
2,300	O	60	4,000	1,870	7,500	1.7	6.3
2,300	O	60	14,500	6,200	25,000	1.0	6.5
2,300	O	60	25,000	9,600	38,000	0.8	5.0
2,300	O	60	45,000	16,800	67,000	0.7	4.8
2,300	O	60	71,000	25,000	100,000	1.0	4.3
2,300	CO	60	700	340	1,350	4.5	9.3
2,300	CO	60	1,310	710	2,800	3.3	8.0
2,300	CO	60	2,500	1,090	4,400	2.0	6.5
2,300	CO	60	4,900	2,700	10,800	1.6	6.3
2,300	CO	60	8,500	4,000	16,000	1.3	6.5
2,300	CO	60	13,300	6,700	27,000	0.9	6.0
2,300	CO	60	23,000	9,200	37,000	0.7	4.8
2,300	CO	60	45,000	19,300	77,000	0.7	5.5
2,500	CO	60	72,000	24,000	103,000	0.7	5.0
3,000	O	60	79,000	33,000	171,000	0.6	3.8
4,500	O	60	1,280	690	5,400	3.3	7.3
4,500	O	60	2,600	1,270	9,900	2.6	6.8
4,500	O	60	4,300	2,100	16,400	1.8	6.0
4,500	O	60	8,800	3,900	30,000	1.3	5.5
4,500	O	60	13,700	6,200	48,000	1.1	5.3
4,500	O	60	23,000	9,800	76,000	0.9	5.0
4,500	O	60	24,000	10,400	81,000	0.8	4.8
4,500	O	60	39,000	14,200	111,000	0.8	5.0
4,500	CO	60	1,150	510	4,000	3.5	7.3
4,500	CO	60	2,300	1,130	8,800	2.5	6.8
4,500	CO	60	72,00	3,300	26,000	1.5	5.8
4,500	CO	60	23,000	9,600	75,000	1.1	5.0
4,500	CO	60	32,000	12,200	95,000	1.1	5.0
4,500	CO**	60	30,000	11,600	90,000	0.9	5.0
4,500	CO**	60	31,000	12,900	100,000	1.0	5.0
4,500	CO**	60	41,000	15,000	117,000	0.8	4.8
4,500	CO**	60	43,000	16,000	125,000	0.6	4.5

* Times for last two phases to clear.

** Duty cycle CO plus 15 seconds CO.

† O = opening, CO = closing opening.

Interrupting Performance

Table I presents a group of consecutive interrupting tests made on one three-phase breaker, rated at 5,000 volts 100,000 kva. The table presents 4,500-volt and 2,300-volt tests, showing both opening and closing-opening performance on a three-phase circuit. At the end of this series of tests the breaker had made some 870 operations, of which over 600 were made on a power life test at 750 amperes, 65 interruptions were over 50,000 kva, 40 interruptions were over

75,000 kva, and 25 interruptions were over 100,000 kva.

The above tests were conducted on circuit connections whose recovery characteristics are described by curve *B* of figure 2. Throughout these tests frequent inspection revealed no need for mandatory maintenance. The breaker, during interruption, gave only a muffled report which was quite inoffensive. There was no visible disturbance or fire from the breaker throughout the limits of its interrupting rating. Following the above series the breaker withstood standard one-minute high-potential tests. An inspection of each chute following these tests revealed that interruption took place within the chute at a developed arc length considerably less than the longest available arc path. This knowledge was helpful in establishing a safe margin of performance to be discussed later.

A close analysis of the performance of the Magne-Blast breaker reveals a striking resemblance to the principles of interruption of a d-c circuit. That is, from the instant of contact parting the arc resistance increases. In both the a-c and d-c circuit this defines a reduction of circuit current. The a-c circuit, however, is blessed with current zeros which afford natural points of interruption. In contrast, the creation of an increasing arc resistance within an a-c air interrupting device accomplishes two major functions which co-operate to bring the current to an early interruption. These are: (1) rapid increase of energy absorption by the arc chute, interruption being forecast when the rate of absorbing the arc energy exceeds its rate of liberation, and (2) a change in the phase angle of the circuit from a zero power factor or phase angle of 90 degrees to a phase angle around 40 degrees to 50 degrees at interruption. With this changing phase angle the circuit is rapidly approaching a unity-power-factor condition where, in the limit, the current is in phase with the voltage and both approach zero together. It is common experience that the interruption of such a unity-power-factor circuit is quite easy because of the extremely low voltage following current-zero interruption. Conversely the arc of an oil breaker introduces little resistance with practically no improvement in phase angle. A breaker using oil as a dielectric withstands the full shock of the circuit recovery characteristic. Because of this fact, the true importance and significance of recovery characteristics were first revealed in connection with the oil device. This knowledge now goes forward to aid the air device. It follows, therefore, that with the decreasing phase angle and reducing arc current in the Magne-Blast breaker the recovery characteristics are improving with each successive current zero. It is of importance to note that the breaker clears when the phase angle is in the neighborhood of 40 degrees or less. The arc chute, to be described later, is uniquely designed to incorporate sufficient arc resistance to bring the circuit to within a few degrees of unity power factor had the current persisted beyond a phase angle of 40 degrees. The phase angle at interruption being approximately 40 degrees defines an ample margin in performance. These features are sketched in figure 3 showing the relationship between the recovery characteristics of a unity- and zero-power-factor interruption together with the Magne-Blast performance. The extremely high damping associated with what high-frequency transients remain in an interruption is brought about by the unique configuration which aids the gaseous products immediately following interruption to form a short-lived leakage path across the breaker. Some form of transitory leakage path has always been one of Mother Nature's methods of protecting the air device. This phenomenon, working together with the large swing in phase angle exhibited by the breaker during interruption, accounts for the enormous reduction in recovery rate and peak recovery voltage immediately following interruption. This reduction in recovery rate is shown in figure 2 and forms an important link in the interruption sequence. Here, regardless of the station recovery characteristics (curves *A*, *B*, or *C*), the breaker responds with the recovery rate characteristic *D* shown at the bottom of the figure. This immunity to circuit characteristics has an important bearing on breaker application, as mentioned previously.

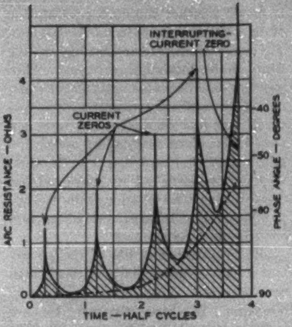

Figure 5. A curve of instantaneous arc resistance for time in half cycles

The increase of arc resistance to a circuit of constant reactance decreases the phase angle as shown. This curve is plotted from figure 3 and indicates the effect of an offset current wave

Figure 4. An oscillogram of a 3,600-volt 1,150-ampere single-phase interruption

A—Cathode - ray-oscillograph trip indication
B—Breaker trip-coil current
C—Breaker travel record
D—Arc voltage across breaker terminals
E—Current through breaker
F—Generator voltage

Figure 4 is a typical oscillogram showing a Magne-Blast breaker interruption of a single-phase fault of 1,150 amperes. The instantaneous arc resistance is defined as the ratio of the increasing arc voltage to the decreasing arc current. This instantaneous resistance is plotted in figure 5 for the test of figure 4. During any half cycle of current the minimum resistance occurs, as would be expected, near the current crest. However, this minimum resistance increases for each successive half cycle until resistance on the last half cycle of arc is such a value that the phase angle of the circuit is close to or less than 60 degrees. At the next current zero the current is invariably extinguished.

Figure 6 shows a master plot of the minimum resistance of the last half cycle of arc for the tests of table I. This characteristic is shown in both linear and

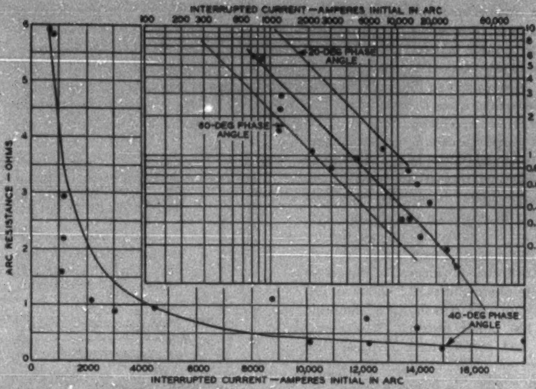

Figure 6. Shows the interrupted current, initial amperes in arc plotted against the minimum resistance introduced in the last half cycle of current

The points are plotted on logarithmic and linear scales showing the curves of the resistances necessary to give a 40-degree phase angle on both scales and the 60-degree and 20-degree phase angles on the logarithmic scale. The tests plotted on this curve are all single phase

logarithmic form. The latter shows the remarkable fact of a straight line at −45 degrees slope which, in turn, defines a constant-power-factor characteristic. The phase angles of the lines shown are indicated on the curves.

The effect of arc resistance for the test of figure 4 is shown in quite different form in the static vector diagram of figure 7. Here the arc voltages for the four half cycles of the arc are shown by the horizontal vectors marked 1, 2, 3, and 4, respectively. Each half cycle of arc current is shown in instantaneous magnitude and phase-angle relationship to the circuit voltage. Figure 6 gives a graphical interpretation of the several simultaneous changes which are taking place within the circuit, namely, the current reduction, the increase in arc voltage, and the reduction of phase angle. It will be noted that the phase angle, as the current approaches its final zero, is somewhat closer to the voltage than indicated by the phase angle at the peak of the last current loop, the latter being shown in general form in figure 4. This characteristic is true of each of the current loops giving a sort of hysteresis effect. This phenomena shows that a very important cooling effect starts near the current peak continuing up to current zero. This rapid cooling as the current approaches zero plays a predominant part in determining the final clearing. It might be said that the interrupting process starts during the last quarter cycle of arc. These features will take added significance when the novel chute design, presented later, is studied from an electrical and thermal aspect.

Figure 7. Polar diagram of a low-current interruption test

The schematic oscillogram (copies from figure 4) illustrates the current and voltage traces. The polar diagram illustrates the instantaneous values of current, voltage, and phase angle for each half cycle as arc resistance increases in value

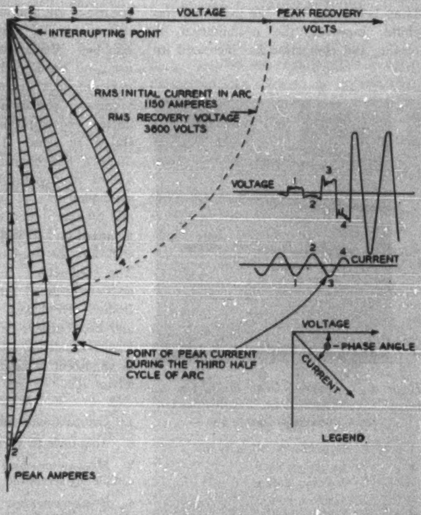
1
2
3
4
VOLTAGE
PEAK RECOVERY VOLTS
INTERRUPTING POINT
RMS INITIAL CURRENT IN ARC 1150 AMPERES
RMS RECOVERY VOLTAGE 3600 VOLTS
VOLTAGE
1
2
3
4
CURRENT
2
4
1
3
4
3
POINT OF PEAK CURRENT DURING THE THIRD HALF CYCLE OF ARC
VOLTAGE
φ-PHASE ANGLE
CURRENT
LEGEND
2
1
PEAK AMPERES

Figure 8 presents the clearing of two poles of a three-phase ungrounded fault of 125,000 kva at 4,500 volts. The upper trace shows the first pole to clear, while the lower is typical of the last pole to clear. Following the first phase to clear, the remaining phases clear in series. Because of the compounding of the series arc resistances of the two remaining phases, together with the fact that they clear later, the final clearing usually takes place at an extremely low phase angle.

Before starting an extended series of interruption tests the breaker was tested to determine the effect of rapid duty. A normal operating load was established and interrupted once every 30 seconds until more than 600 operations were recorded. The breaker required no cooling as it continued its operations on this reactive circuit of 750 amperes at 4,500 volts. A series of 140 interruption tests were then completed. Of this number 65 interruptions exceeded 50,000 kva and 25 were of more than 100,000 kva. Standard one-minute high-potential tests showed no reduction in insulation values. After this complete series the breaker was disassembled and inspected. Of all details, the arcing tips alone required maintenance or replacement.

Description

The performance of the Magne-Blast air circuit breaker emphasizes the importance of a controlled increase in arc resistance. This resistance can be effectively increased at a controlled rate by cooling and lengthening the arc stream during circuit interruption. The interleaving or serpentine arc chute as used on this breaker, figure 9, is a simple and reliable method for cooling and lengthening the arc in a manner consistent with circuit requirements.

INTERRUPTER

As evidenced by its name, the interleaving chute is formed by long tapered fins alternating from either arc-chute wall, extending from the throat and arc runner to the mouth of the arc chute. Placed parallel to each other and spaced to leave room for the arc to fold in and out between opposite fins, their gradual taper away from the side walls permits the arc to form in a straight line without obstruction. As the arc stream is driven deeper into the chute, however, it is gradually forced to depart from its straight line as the fins cross deeper and deeper into the space between fins of the opposing side. The arc encounters an ever-increasing cooling effect as its path becomes longer and more tortuous. Long before reaching the mouth of the chute, however, the combination of cooling and elongation has increased its resistance to a critical value and the arc is extinguished. Though a sufficient untraveled path remains to provide a greater arc resistance and a further decrease in phase angle, tests show this is but a safety factor.

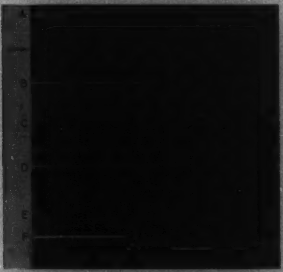

Figure 8. A representative oscillogram of two poles during a three-phase close-open test at 4,500 volts, 125,000 kva

A—Breaker-trip-coil current
B—Current, pole 2
C—Voltage, pole 2
D—Current, pole 3
E—Voltage, pole 3
F—Breaker travel

The interleaving arc chute of the Magne-Blast breaker has proved a simple way to provide ample arc cooling between closely spaced nonmetallic barriers. Arc elongation demands no compromise as the same barriers nicely fold the arc into a serpentine pattern.

The design permits moulding an arc-chute side and its fins in one piece using an especially treated arc-resistant insulation material. Continued operations show no ill effects on the chutes. Performance is unaffected, though the intense heat forms a glassy and pebbly surface on the fins. Figure 10 illustrates two sets of arc chutes. *A* shows an unused chute. At *B* is shown a chute side after 140 power tests to 130,000 kva. Color and texture have changed, but thermal and electrical properties show no adverse effects. *C*, *D*, and *E* picture the throat end, the side, and the mouth of completed arc chutes ready for mounting in a breaker.

BREAKER ARRANGEMENT

Early agreement encouraged a geometric arrangement that would permit the use of the Magne-Blast air breaker in vertical-lift metal-clad switchgear assemblies. Experience with such gear using oil breakers has long proved the value of such arrangements. No compromise in performance or cost was permitted in completing the design illustrated in figure 11. Terminals, conductors, and operating structures show a close relationship to oil-circuit-breaker arrangements. A conventional oil-circuit-breaker operating mechanism is mounted on and isolated by a steel plate enclosing the front of the breaker frame. The conductors, contacts, and current coils are enclosed within the rigid breaker frame. Arc chutes, arc runners, mufflers, and box barriers extend back from the frame to define the boundaries. The arrangement is desirable from the listed advantages:

1. Compact and efficient in space.
2. Adaptable to vertical-lift metal-clad.
3. Mechanism and control accessories completely accessible.
4. Breaker completely isolated from mechanism and operating side.
5. All breaker parts are exposed by the ready removal of the insulated-box barrier.

Figure 11 illustrates the features of this arrangement in some detail. The complete interrupter and contact structure is mounted on and between the two insulated breaker bushings. These bushings are rigidly connected to the frame and to each other by a heavy insulated barrier *J*. Arc runners and blowout coils are mounted on and insulated from bushing *G*. A copper casting extending from this bushing carries the primary contact fingers *A*. These fingers complete the current circuit to the contact arm through solid-silver inlays. The inverted arrangement of these fingers provides an increase in contacting pressure from the magnetic forces at high currents. From the movable contact arm the current enters bushing *H* at the pivot. A connection between this bushing and the lower blowout coil of the arc chute carries current during interruption and provides an additional support for the arc chute and muffler. The arc-chute assembly can be easily removed to expose the complete assembly of contacts and arc runners.

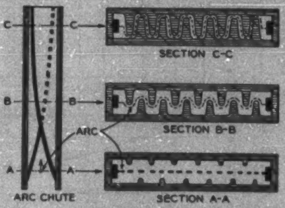

Figure 9. A schematic diagram showing the treatment of an arc in the Magne-Blast interrupter

At section *A-A* the arc is struck in a line between arc runners, as it is forced into the chute the fins constrict the arc and force it into a serpentine path *B-B*. Section *C-C* shows its final shape under extreme conditions

CURRENT TRANSFER

To follow the treatment given the current during breaker opening, reference is made to figure 12. As the blade starts down, current is transferred from the primary contact *A* to the intermediate contacts *B*. Low-voltage arcing, as the blowout coil *E* is first shunted in, is taken on the parting of the intermediate contacts, to the benefit of the more sensitive primaries. As these intermediate contacts part, all current is shunted through the blowout coil and magnetic flux is created to force the arc on its way. Finally, the silver-tungsten arc contacts part *C*, the resultant arc is forcibly driven from the contacts adding additional blowout coils to the circuit as the arc travels along the runners *D*. For heavy currents this travel is so rapid that the arc is

Figure 10. A finished arc chute for the Magne-Blast air circuit breaker

A—Shows one-half of an unused arc chute

B—Shows a similar half after 140 power interruptions to 130,000 kva. The discoloration of *B* indicates a surface change that has no effect on the thermal or electrical properties of the arc chute

C—Is taken from the throat or contact side of the arc chute

D—Shows the side of the arc chute

E—Illustrates the serpentine pattern at the mouth of the arc chute with muffler removed

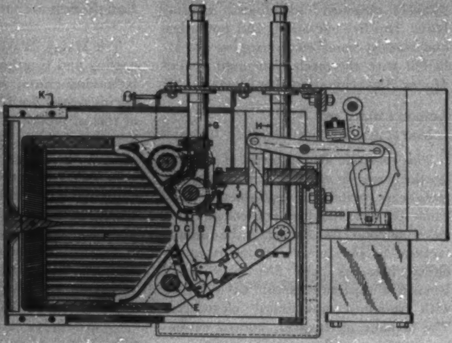

Figure 11. A sectional drawing of the 5,000-volt Magne-Blast air circuit breaker for 100,000-kva interrupting capacity

A—Primary contact fingers and movable contacts
B—Intermediate contacts
C—Arcing contacts and lower arc runner tip
D—Arc runners
E—Blowout core and coils
F—Inside of arc chute
G—Rear insulated bushing
H—Front insulated bushing
J—Horizontal tie barrier of insulation
K—Removable box barrier and vents

extinguished at a minimum break distance of one inch. As the arc terminus travels up along the upper arc runner it jumps an insulated gap and injects a second multiturn coil to maintain high flux densities in spite of current reduction. At the same time, the arc terminus has shifted to the lower runner *D* and has shunted in a third multiturn coil *E* that assists in arc travel in the lower portion of the chute. The travel of arc roots in divergent directions lengthens the arc, and, assisted by the driving force of the flux, forces it into the serpentine passage *F* on its way to extinction.

Heavy-current interruptions require throttling for the powerful magnetic blowouts at such currents may drive the arc through the chute before resistance and cooling are permitted to complete their functions. A muffler mounted on the end of the chute, pockets and retards air flow and directs all arc products into the streamlined deflectors forming the end of the box barrier. Before the gases reach this area all arc, arc flame, and gas has been cooled to a harmless value and final gases flow from the grilled openings at the top and bottom of the box barrier *K*, negligible in temperature, velocity, and potential. Maximum power interruptions are evidenced only by the small trickle of smoke from these openings and the dull thud that accompanies the formation of the initial arc.

INSULATION

Bushings are designed and manufactured in accordance with oil-circuit-breaker standards. Because each pole is mounted on these bushings, the breaker is easily isolated into three phases. The extended barriers between phases and from phase to ground offer ample insulation and are flame resistant. Arc runners and blowout coils are separated from the side walls of the arc chute by sheets of inorganically bonded mica. This material, high in dielectric and mechanical strength, suffers no damage when exposed to power arcs. Operating rods of insulation are a strong link between the mechanism and the contact arms.

OPERATING MECHANISM

Oil-circuit-breaker practices provide a valuable background when considering the requirements of an operating mechanism. Step-by-step evolution has brought the present-day solenoid mechanism to a high degree of perfection. Because this breaker so closely parallels modern oil circuit breakers in mechanical operation, it has been quite practical to use a standard operating mechanism. A modern oil-breaker solenoid mechanism contributes to performance when applied to this air circuit breaker. The mechanism requires but minor modifications when used as described. The features of an efficient

Figure 12. Contact structure and blowout coils. Half of arc chute assembly is removed

A—Primary contacts	*D*—Arc runners
B—Intermediate contacts	*E*—Blowout coils
C—Arcing contacts	*F*—Arc chute

solenoid structure; sturdiness, adaptability, trip-free operation, and potential- and current-transformer tripping are inherent with the mechanism used on the Magne-Blast circuit breaker (figure 1).

Arrangement in Metal-Clad Equipments

The compact arrangement of this air breaker loses no advantage when used in metal-clad switchgear equipments. Because the breaker does not exhaust hot gases beyond its barriers, the manufacturer of switchgear can use it as oil circuit breakers have long been used. Breaker compartments can be of metal with standard mechanical clearances. The breaker mechanism, control accessories, and secondary couplings are conveniently accessible from the front of the metal-clad equipment. Inspection and adjustment of these accessories can be completed without removing the breaker from its operating compartment.

Figure 13 illustrates the close relationship between such metal-clad equipments. *A* shows a metal clad using a 100,000-kva oil circuit breaker insulated for 7.5 kv. *B* shows, to a similar scale, a metal-clad equipment with a 100,000-kva "Magne-Blast" air breaker insulated for 7.5 kv.

Conclusion

The Magne-Blast air circuit breaker, by cooling and lengthening the arc in an interleaving chute, adds resistance to the circuit as the arc is extinguished. Rates of rise of recovery voltage are held to a harmless value. The breaker has demonstrated the characteristics of long and economical life, reliable circuit control, and a reduction of hot gases and disturbances when interrupting high currents. The physical arrangement permits its use in vertical-lift metal-clad gear where a compact arrangement is possible without infringing on the accessibility of the mechanism or control accessories.

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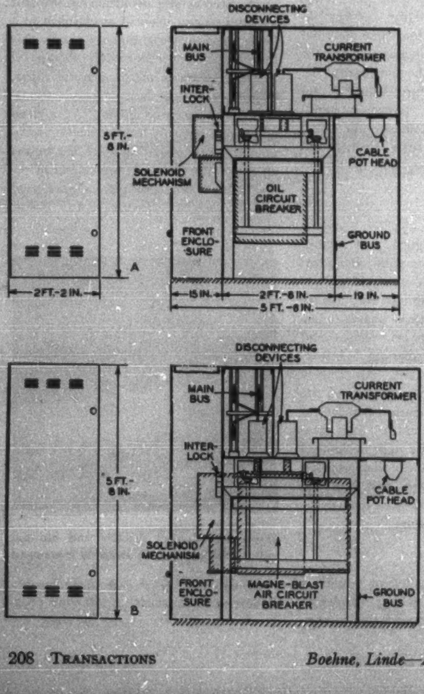

Figure 13. Breaker arrangement in a metal-clad equipment

A—An oil circuit breaker rated at 100,000 kva insulated for 7.5 kv in typical vertical-lift metal-clad equipment

B—A Magne-Blast air circuit breaker rated at 100,000 kva with 7.5 kv insulation used in a comparable metal-clad equipment

Discussion

Elias S. Cornell (Delta-Star Electric Company, Chicago, Ill.): This paper discloses an interesting development in oilless-circuit-breaker design. The mechanical and electrical complexities which seem to characterize this entire class of circuit interrupters, do not however appear to be materially reduced.

Elimination of oil or other liquids will be favorably regarded from the standpoint of maintenance. However, there appears to be a greater number of active parts such as increased number of arcing contacts, the addition of large arcing chutes, and blowout coils. Whether or not the net result will be an improved maintenance condition, only operators can tell—and time for experience should be allowed before definite conclusions can be drawn.

It is particularly interesting that this paper introduces the first oilless circuit breakers adaptable for vertical-lift type of metal-clad switchgear. Designers and operators have recognized that the vertical-lift construction possesses many electrical, mechanical, and space preserving advantages over the draw-out or truck type design.

On the other hand, modern oil circuit breakers are of clean-cut design with few working parts. Experience with this type has been so generally good that before the oilless can supplant present modern oil circuit breakers, a unit more simple mechanically and electrically must emerge from the various designs now suggested.

I shall look forward to further developments of the oilless circuit breaker of which this paper is the latest contribution.

C. G. Suits (nonmember; General Electric Company, Schenectady, N. Y.): Messrs. Boehne and Linde are to be congratulated for the excellent application which they have made of thermal principles of arc interruption. Although these principles are not entirely novel, they have been carried in the present instance to new limits which bespeak the authors' fine appreciation of the underlying arc phenomena.

The means of interrupting an arc by cooling has been employed to an accidental extent in many circuit-breaking devices in the past, but it has only been with recent advances in our knowledge of the fundamentals of this process that engineers have employed it as a design principle.

It may be of interest to review some of the fundamentals involved in the interruption principle of this circuit breaker as illustrative of the influence of thermal factors on the electrical properties of arcs.

The most important underlying fact is this: In a high-pressure discharge electrons are in thermal equilibrium with the neutral gas, a concept first proposed by K. T. Compton.[1] The electrons acquire kinetic energy by virtue of their acceleration in the field between the electrodes, and lose a small fraction of this energy (determined by the ratio of masses) to the gas molecules in each collision. At some high gas pressure and correspondingly high collision frequency it will be impossible for the electron energies to exceed the gas energies. This implied condition for thermal equilibrium can be defined more rigorously by stating that the energy gained from the field by the electron per mean free path must be small compared to the thermal energy; thus

$$\Delta Ve << \frac{3}{2}kT$$

where ΔV is the average potential difference through which the electron of charge e falls per collision. Here k is the Boltzmann constant and T the mean gas temperature. It is clear that this relationship is best satisfied at high pressure, and since the electric field in the arc column decreases with increasing current, at high current. The results of numerous investigations[2] show that even in low-current arcs at atmospheric pressure in air the equilibrium between electrons and gas is established.

This fact has important consequences in circuit interruption, the first of which is: the *ionization density* is fixed by the *gas temperature* (identical with the electron temperature), and neither of these two quantities can be changed without altering the other. When a volume of arc gas is cooled, its ionization density is reduced thereby. Moreover, the relation between ionization density n and temperature T is a quantitative one,[3] being given by the equation

$$\log_{10}\left(\frac{n^2}{N}\right) = -\frac{5{,}040V}{T}+\frac{3}{2}\log_{10}T+15.38$$

where V is the ionization potential of the neutral gas of molecular density N. It is interesting to note the sensitivity of ionization density to temperature, particularly at the temperatures around 2,000–3,000 degrees Kelvin which have been observed around the current zeros of some a-c arcs.[4] This is shown by table I of this discussion, where ΔT is the temperature difference required to double the ionization density at the mean gas temperature T. Thus a change of only 30 degrees will double the ionization density at 2,000 degrees Kelvin. It is not difficult to understand, therefore, how removal of heat by the circuit-breaker chute structure is effective in reducing the conductivity of the arc gas at the current zero.

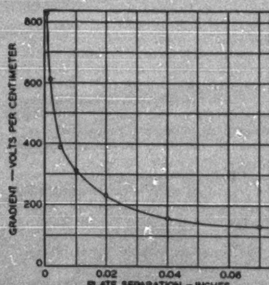

Figure 1. Electric gradient for an arc between flat insulating plates at different separations

A second consequence of the thermal equilibrium in the high-pressure arc is this: It is impossible to change the direction of momentum of the conducting electrons, as, for example, by a magnetic field, without also acting upon the neutral gas. This is a consequence of the high rate of energy transfer at high pressure. Thus we do not sweep out the electron cloud as a separable quantity with the magnetic field. We move the electrons and the gas; in short, the arc column. For that reason we find that when a d-c arc is wiggled by an a-c field perpendicular to its axis, its maximum translational velocity is a very small fraction of the velocity which it reaches in a d-c field of the same magnitude. In some unpublished calculations, Doctor H. Poritsky found that to account satisfactorily for the velocity of the arc moved by a d-c magnetic field he had to use a value of mobility corresponding not to that of the electrons, but to that of a relatively immobile body, such as a positive ion. This is further evidence that the electron gas cannot move without dragging with it the neutral gas. It has, of course, long been known that because of the space charge it is impossible to separate the electrons from the positive ions in a plasma.

Considerations such as the above lead us to the application of heat transfer data to the arc column.[5] We can move an arc magnetically past an insulating plate and in cooling it thereby reduce its ionization density. This rather striking effect can be illustrated by a simple experiment in which the electric gradient in the arc column is measured for an arc between insulating plates with various spacings, as shown by the curve of figure 1 of this discussion. These measurements were taken for a steady-state d-c arc of 15 amperes, but they are believed to be illustrative of a general property of the high-pressure arc, whether it be d-c or a-c, high or low current. Similarly, we can investigate the effect of cooling on the current zero in a transient arc. This is illustrated by figure 2, where the cooling or "deionization" time τ is plotted as a function of plate separation for an arc burned between parallel insulating plates. In this experiment the d-c arc current of 15 amperes is suddenly reduced to zero for an adjustable interval and then voltage is suddenly reapplied. The time τ is the minimum interval which will prevent reestablishment of current. It decreases with smaller plate spacing as a result of the more effective cooling.

Table I. Temperature Difference ΔT to Double Ionization Density at Mean Temperature T

T (Deg K)	ΔT
7,000	400
6,000	300
5,000	210
4,000	130
3,000	78
2,000	30

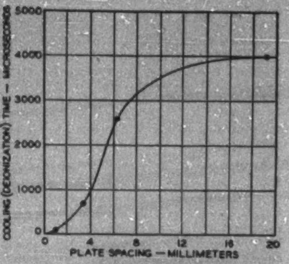

Figure 2. Cooling time of arcs between insulating plates, for different spacings

In applying heat-transfer principles to their circuit breaker, the authors have arrived at a form of cooling structure which in a unique way satisfies many diverse practical requirements in addition to the prime function of extracting heat. Thus the desirable lengthening of the arc column brings with it the advantage of a longer parallel insulation path at the time of the current zero. This particular insulation path, because of its higher temperature, is most vulnerable to surface flashover. The folding of the arc column is accomplished in the plane perpendicular to the plane of the arc motion. This is important because folding in any other plane will bring with it some components of magnetic field (due to the current upon itself) which oppose the blowout field.

It is interesting to point out that in this breaker we observe a real current-limiting action, analogous in many ways to the phenomena of the current-limiting fuse.[6] As a result of recent work we now know beyond question that this fuse is a real arc interrupting device wherein a high gradient is developed in an arc in a tube of fused quartz. This fact apparently escaped the original investigators.[7,8] These data will be reported more completely elsewhere.

That the principle of arc interruption by cooling will have wider development and that heat transfer engineering will be further applied to circuit breaker design seem certain by the remarkable performance of the authors' Magne-Blast breaker.

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R. C. Dickinson (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Since the announcement over ten years ago of the pioneer in the field of high-voltage high-capacity modern oilless circuit breakers, the "De-ion" air circuit breaker, several types of oilless circuit breakers have made their appearance. This latest device, presented by Messrs. Boehne and Linde, draws an arc and increases its voltage to such an extent that the circuit potential cannot maintain it. Thus, principles heretofore applied only to low-voltage interrupting devices, have been extended to a higher-voltage class. This is accomplished in approximately the space of competitive breakers by refinement in the design in the interrupting element.

It is now recognized that a good principal of circuit breaker design is to keep the arc energy of the interrupting element as low as possible, thereby minimizing distress on the breaker in general. The arc energy is a function of the integrated arc voltage with respect to time and, therefore, it has always appeared good practice either to keep the arc voltage low except near the current zero, or to keep the interrupting time very short. In view of this it comes somewhat of a surprise to hear high arc voltage in a circuit interrupting device proclaimed as an advantage.

Fundamentally, the device described seems to operate on the long established principal of stretching the arc out to great length, thereby developing sufficiently high arc voltage to prevent the circuit voltage from maintaining it. The resulting arc voltage is sufficient to cause a shifting of the point of interrupting from the crest of the a-c wave (for a zero-power-factor circuit) toward the zero point, which thereby reduces the magnitude of the resulting transient in proportion to the system-frequency phase shift. Any other reduction in the transient peak must evidently be caused by leakage. The author states that such leakage does occur in this device and that this leakage is through a path formed by gaseous products. We would expect this leakage effect to vary considerably over the interrupted current range. That is, it would be more pronounced at high current values. This would make the breaker largely dependent purely upon the development of arc voltage at the low current values. We would also expect the development of arc voltage to be decreased at low current values due to the weaker blowout effect and to the decreasing effect of the restriction of the arc path formed by the narrow serpentine slot. Therefore, we would be interested to know how this breaker performs on low current values such as magnetizing currents.

In discussing the authors' explanation of the operation of this breaker, we call attention to a statement by D. C. Prince in his discussion of "Arc Extinction Phenomena in High-Voltage Circuit Breakers Studied With the Cathode-Ray Oscillograph" by Messrs. Van Sickle and Berkey:[1] "The modification produced in the circuit recovery rate by these (oil) breakers is due to their attempting to clear the circuit at times other than normal current zero or to conduction of small currents without a complete breakdown of the dielectric. These characteristics tend to produce distress in the circuit breaker, as it is required to absorb energy normally stored in the electric circuit." We would thus conclude that this is a high-energy device and is, therefore, subject to considerable stress in interrupting short-circuit current. Thus, it is primarily dependent upon a characteristic making it necessary for it to absorb relatively high energy in order to interrupt the circuit.

In the De-ion air circuit breaker, we have a shifting of the point of interruption on the 60-cycle voltage wave which is most pronounced at high current value. As shown by the oscillogram of figure 10*A*, in the paper on De-ion air breakers for 2,500 and 5,000 volts, in AIEE TRANSACTIONS for November 1938, this phase shift is of about the same order as shown by Messrs. Boehne and Linde. However, the interrupting principles of the De-ion air circuit breaker include a very definite voltage interrupting characteristic produced by the rapid growth of dielectric in a multiplicity of cathode layers.[2] Its interrupting characteristic is therefore independent of line current and, for all practical purposes is independent of the circuit recovery characteristics. The reduction in transient peak caused by the phase shift at high current in the De-ion air breaker is not relied upon for circuit interruption and is merely incidental in its performance. Also, in the De-ion air breaker, no critical damping of the recovery transient due to gaseous leakage paths has been observed.

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V. L. Cox (General Electric Company, Philadelphia, Pa.): This new design of oilless circuit interrupter will find many fields of application especially where frequent operations are necessary for motor starting, jogging, reversing, furnace control, and reclosing feeder service.

In designing this breaker, the design engineers kept in close touch with the engineers responsible for switchgear assemblies, and the result is a breaker that can be fitted into present oil-circuit-breaker equipment designs with practically no change in arrangement of apparatus and over-all dimensions. It was early recognized that the contour of the air breaker should conform as closely as possible to that of the oil breaker in order to conserve space in the usual switching structure. It has long been recognized that the most economical arrangement, for the combination of busses, breaker, current transformers, potential transformers, and the take away of primary cables (up or down) can be accomplished only with this particular contour.

The ideal structure for mounting the new breaker is metal-clad switchgear. It has been possible to retain the vertical lift, easily removable circuit-breaker feature which has been found so useful in oil-circuit-breaker metal-clad switchgear. The advantage is the continuance of a design which has proved its utility in the field; no new station designs are necessary nor need any conceptions of this type of equipment be changed.

The use of an oilless circuit interrupter in metal-clad switchgear puts the metal-clad type of switching structure even higher up the pinnacle of design attainment for factory-built shipped-assembled units. The safety to personnel, protection to circuits, co-ordinated design, ease of installation and maintenance, high level of insulation, and unit construction employed in metal-clad switchgear make this type of switching structure the best. This holds true for the oil or oilless breakers, but due to the absence of inflammable liquid, the installation of the new breakers can be made in locations not best for oil circuit breakers.

Tracing the history of metal-clad switchgear back to its inception, both in Europe and in this country, we find that there has been a definite trend from the compound and oil-filled structure insulating medium toward the air-filled type. The high-voltage air-breaker development is another step in this direction in its now complete elimination of this liquid.

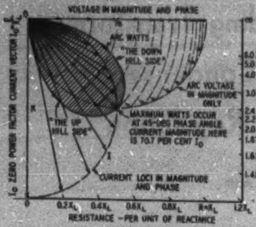
D. C. Prince (General Electric Company, Philadelphia, Pa.): In domestic refrigerators we have gone through the stage of units separate, units above, and units below the box. By trying all combinations the most generally satisfactory has emerged. Likewise in the automobile, we have tried engines beneath the seats, engines in front, and engines in the rear. Each arrangement has its advantages, but, in general, the logic of the situation has begun to clarify itself.

Until recently circuit breakers were more or less alike. Then a perfect rash of variations broke out in Europe. A person fully familiar with our standard tank-type of circuit breakers might not even recognize one of the current European productions for what it is.

The use of oil in a tank determines to a certain extent the configuration of the parts of a circuit breaker. In a circuit breaker such as the Magne-Blast, this criterion is absent and in its absence the situation regarding the whole arrangement problem should be reconsidered.

In Europe the threat of war and scarcity of various raw materials may effect evolution; also, different maintenance practices and different service standards may put a premium on different elements of design. We must, therefore, go through our own evolutionary process, as we have done in connection with the refrigerators and automobiles, to see what physical arrangement, in the absence of the limitations of an oil vessel, lends itself best to the industry's needs.

This question cannot be satisfactorily answered in the laboratory. The next few years will, therefore, see many excursions away from past conventional circuit-breaker practices. In the end the most suitable designs will evolve. For the sake of the


VOLTAGE IN MAGNITUDE AND PHASE
ARC WATTS
"THE DOWN HILL SIDE"
ARC VOLTAGE IN MAGNITUDE ONLY
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CURRENT LOCI IN MAGNITUDE AND PHASE
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0
0.2X_L
0.4X_L
0.6X_L
0.8X_L
R=X_L
1.2X_L
RESISTANCE – PER UNIT OF REACTANCE
∞
6.0X_L
5.0X_L
4.0X_L
3.0X_L
2.4X_L
2.2X_L
2.0X_L
1.8X_L
1.6X_L
1.4X_L

Figure 3. Polar diagram showing the inseparable relations between phase shifting, current reduction, and watts as resistance is inserted into an inductive circuit

Note: Maximum watts occur at 45-degree phase angle when the current is only 70.7 per cent of its zero-power-factor value

industry, let us hope that such evolution can be brought about with a minimum of false starts and costs for all concerned. However, nature is a severe taskmaster. We must evolve to meet changing conditions or join the dinosaur.

E. W. Boehne and L. J. Linde: The development of the Magne-Blast breaker is the natural growth of the General Electric Company's oilless devices of high interrupting capacity pioneered by our company's transportation department and presented in excellent papers in 1922 ["Air-Break Magnetic Blowouts (For Contactors and Circuit Breakers Both A-C and D-C)," J. F. Tritle, AIEE TRANSACTIONS, 1922, page 262]. The interleaving chute has enabled appropriate rating to be literally folded into sizes and arrangements suitable for standard vertical-lift metal-clad gear. The development embodies the contributions of the past, the contributions of a fuller knowledge of recovery rates as offered by the oil device; the contributions of our research studies of arcs in various environments, together with novel arrangements in mechanical design.

The authors are greatly indebted to Doctor Suits for his discussion of the problems of circuit interruption in air. His investigations of arc phenomena have considerably reduced the mysteries of such arcs and have shown us the importance of the thermal as well as the electrical factors of this general problem. The interleaving arc chute meets all thermal demands as well as the electrical requirements of an interrupting device.

Mr. Cornell points to the clean-cut design and few working parts of modern oil circuit breakers as contrasted to the apparent complexity of any oilless circuit breaker design. Blowout coils, arc chutes, and insulations do add to the complex appearance of an oilless breaker. Apparent complexity, however, can only be evaluated as it influences performance and maintenance expense. We are fortunate in having a complete historical background covering the performance and life expenses of magnetic-blowout-type air-circuit-breaker constructions. During many years the type *JR* high-voltage air circuit breaker has maintained an excellent reputation for performance and life in the industrial and transportation fields. The Magne-Blast breaker incorporates a similar blowout-coil construction.

As a contribution to easy inspection and maintenance, the enclosing box barrier may be readily removed to expose the arc chutes and contact structure. Arc chutes themselves are then easily removed when a more detailed inspection is being made, for then all arc runners, contacts, and blowout coils are accessible.

Figure 5. Oscillogram showing two phases of a three-phase CO interruption of 14,000 amperes at 4,500 volts by a Magne-Blast air circuit breaker

Note the low arc drop at current crest and the high arc drop at interruption

A—Trip-coil current
B—Phase *A* current
C—Voltage across phase *A*
D—Phase *B* current
E—Voltage across phase *B*
F—Travel record

Figure 4. Single-phase interruption of 22,000 amperes at 2,600 volts by a Magne-Blast breaker

Note current reduction to 46 per cent of previous loop and low phase angle at interruption

A—Trip-coil current
B—Bus voltage
C—Short-circuit current
D—Arc voltage
E—Cathode-ray trip

We are in agreement with Mr. Cornell and Mr. Cox in their discussion of vertical-lift arrangements. This particular breaker arrangement is a direct result of the operators' and designers' recognition of the many advantages of the vertical-lift design over draw-out or truck designs for metal-clad switchgear.

To carry through with the points raised in Mr. Dickinson's discussion requires a clarification of the principle of arc interruption as demonstrated by the Magne-Blast breaker. The basic consideration is one of resistance. When resistance is uniformly inserted into an otherwise zero-power-factor circuit, the current is reduced, the power factor swings toward unity, and energy is dissipated in the resistance. These three changes are inseparable as clearly shown in the circle diagram of figure 3 of this discussion. Energy dissipation becomes maximum when the resistance in ohms equals the reactance in ohms and occurs, as shown in figure 3, at a phase angle of 45 degrees, simultaneous with a current reduction of only 30 per cent. The Magne-Blast breaker magnetically blasts the power arc into an efficient arc trap where the resistance characteristics are uniformly developed in the arc itself, and so effectively that energy dissipation is thrown over the hump of maximum energy dissipation and, as the decaying arc proceeds in the chute, it is literally on the "downhill side" toward interruption. See figure 3.

With the above fundamental relations in mind, it is a little difficult to unravel the apparent contradictions in Mr. Dickinson's discussion to the effect that the De-ion breaker exhibits comparable phase-angle changes, marked arc-current reduction, while paragraph 2 of his discussion teaches the merits of keeping the arc energy of the interrupting element as low as possible.

Arc energy has been no problem in the Magne-Blast breaker, chiefly due to the controlled absence of all metal vapor from the main arc stream together with the exposure of the rapidly moving arc to increasing amounts of cool insulating surfaces. In the Magne-Blast breaker the arc duration of higher currents seldom exceeds one-half cycle. In this short interval the heat penetration into the surface walls of the interleaving chute is less than eight mils. That component of heat which is retained is partly absorbed into the fins of the chute, the larger share being carried off by convection currents immediately following interruption. These conclusions are necessary to account for the fact that following an exhaustive series of tests the breaker will be found as "cool as the proverbial cucumber."

This brings us to Mr. Dickinson's recommendation of high arc voltage near current zero. We heartily agree and refer to the two oscillograms in our paper as well as the two shown in this discussion, the latter of which bears considerable study. Note the low arc voltage at peak current in relation to the high arc voltage at interruption. This is the first time that this desirable characteristic has been observed in air breakers of this type. It is the characteristic result of a pure expanding arc, confined and cooled in the absence of metal vapor.

Recovery rate control by the Magne-Blast breaker is a natural refinement of one of the inherent properties of air interrupters employing magnetic-blowout action. It has only been through the evolution of oil breakers that the importance of recovery rates has been recognized. Low-gradient leakage paths available during the first 100 microseconds following interruption afford an excellent way to absorb the high-frequency components of recovery characteristics. This leakage path is through the gas confined to the chute during a brief recovery period following interruption and has only been detected by the absence of the high-frequency component of the recovery characteristic. The extreme length of this path deep within the chute together with its freedom from metal vapor accounts for this desirable shock-absorbing feature. The major factor in recovery-rate control, as previously demonstrated, is the large phase shift up to the point of interruption.

Low-current performance of the Magne-Blast breaker follows the same characteristics as the higher-current performance, namely, a continually increasing arc voltage, phase shifting, current reduction, followed by a positive interruption. These processes are naturally slower because of the slower movement of the arc along the runners.

The Magne-Blast breaker has been tested throughout the complete current range, that is, from 0.2 amperes upward, zero-power-magnetizing currents as well as similar currents on a leading-power-factor basis. These tests have been entirely satisfactory and are strengthened by the fact that, with this publication, breakers have been in service over a year performing satisfactorily where interruptions of regulator magnetizing currents are required daily.

The Use of Multiwinding Transformers With Synchronous Condensers for System Voltage Regulation

H. P. ST. CLAIR
MEMBER AIEE

THREE-WINDING transformer banks offer a convenient and economical means, and are frequently used, for operating synchronous condensers at step-down substations, particularly where the secondary voltage is too high for direct operation of the condenser. The usual function of the condenser in such cases is to regulate the secondary voltage, and the condenser regulator is controlled from that voltage rather than from the tertiary winding, or condenser terminal voltage. Furthermore, in many three-winding transformer designs the effect is nearly the same as though the condenser were actually operating on the secondary winding.

There are, however, many applications in which it is desirable to vary the transformer design and with it the condenser performance in order to accomplish a particular regulating and power factor correcting job which cannot be done with the usual three-winding design. A number of such applications, involving not only three-winding transformers but in some cases four-winding transformers as well, have been worked out and placed in operation with excellent results on the system of the American Gas and Electric Company.

While descriptions and analyses of the conventional three-winding transformer design have been presented and discussed in the technical literature (see bibliography), there is believed to have been little, if any, discussion of the possibilities of manipulating the equivalent reactance network of three-winding as well as four-winding transformers in order to co-ordinate or combine the main regulating function of the condenser with other more-or-less secondary regulating effects which may be desired. It is the purpose of this paper, therefore, to describe and classify the several types of transformer designs and regulating combinations that can be used in applying synchronous condensers with multiwinding transformers and to illustrate each type with examples of actual installations.

Theory and Application of Three-Winding Transformers

It is recognized, as mentioned above, that the equivalent reactance network of three-winding transformers derived by means of the three-point formula has been amply published before, but for convenient reference in this paper it will be repeated here. The reactance network as shown in figure 1 takes the form of a star in which the legs A, B, and C are found by solving the three equations:

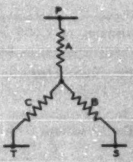

Figure 1. Equivalent-reactance network of three-winding transformer

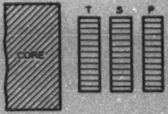

Figure 2. Probable arrangement of windings for type I transformers

$$A+B=X_{ps}$$

$$A+C=X_{pt}$$

$$B+C=X_{st}$$

The terms X_{ps}, X_{pt}, and X_{st} are the reactances from primary to secondary, primary to tertiary, and secondary to tertiary, respectively, expressed in per

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cent or per unit values and of course all referred to the same kilovolt-ampere base. The solution of the above equations gives the following expressions for the three legs of the star:

$$\text{Leg } A = {}^1/_2(X_{ps} + X_{pt} - X_{st})$$

$$\text{Leg } B = {}^1/_2(X_{ps} + X_{st} - X_{pt})$$

$$\text{Leg } C = {}^1/_2(X_{pt} + X_{st} - X_{ps})$$

In transformers having the three windings placed successively on the core without interleaving, as shown in figure 2, one of the three legs of the star will have a relatively small value for the reason that the reactance between the two windings farthest apart will be roughly equal to the sum of the reactances between adjacent windings. For example, if in figure 2, $X_{pt} = X_{ps} + X_{st}$, then leg B in figure 1 will be zero. With interleaved windings, however, whether on the shell-type or core-type design, the straight-line reactance circuit no longer holds true and it is generally possible to obtain nearly any desired relationship between the values of A, B, and C in figure 1.

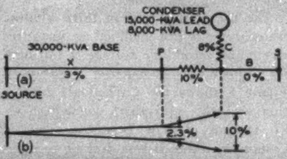

Figure 3. Typical application of type I three-winding transformer and condenser for ten-per cent no-load voltage range

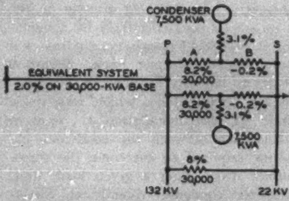

Figure 4. Type I transformer application, equivalent setup for Sunnyside substation, Canton, Ohio

When a synchronous condenser is connected to leg C of figure 1, obviously the relative values of A and B will directly affect the regulating performance and the range of voltage control obtainable with a given size of machine. The particular problem which will be dealt with in the discussion of the various types of installations presented in this paper will be the selection of these relative values, A and B, in the equivalent reactance network. The value of C affects the range of terminal voltage over which the condenser is required to operate, but for this discussion it will be assumed, as is generally true, that it offers no serious problem except the necessity of selecting the proper voltage ratings for condenser and transformer.

For convenience, the three-winding transformer designs discussed in this paper will be classified in three types according to the relative location of the synchronous condenser in the equivalent reactance network, that is, according to relative values of A and B in figure 1. The reason for this classification will appear in the following descriptions.

TYPE I—CONVENTIONAL THREE-WINDING DESIGNS WITH CONDENSER REGULATING SECONDARY VOLTAGE

In this type the equivalent reactance network of the transformer places the condenser electrically on or close to the secondary bus. A typical elementary layout for such a transformer and condenser combination is shown in figure 3a where the equivalent circuit legs A, B, and C correspond to the equivalent circuit shown in figure 1 and the transformer design would probably correspond to figure 2. To illustrate the actual performance of such a setup, typical values have been assigned as follows:

Transformer bank = 30,000 kva

$$\text{Condenser rating} = \begin{cases} 15{,}000 \text{ kva lead} \\ 8{,}000 \text{ kva lag} \end{cases}$$

Equivalent reactance network of transformers:

A = 10 per cent
B = 0 per cent
C = 8 per cent

Equivalent system reactance to high side of transformer = 3 per cent on 30,000 kva

The proper system reactance to use in this elementary diagram is not the total reactance back to the source, including generator reactances, etc., but only that part of the system reactance between the point of installation being considered and the nearest point or combination of points at which system voltage is normally maintained at a constant level. This value of reactance will usually be much smaller than the total system reactance ordinarily used to determine short-circuit current at the particular location. This reduced value of reactance is used in all the elementary diagrams covering the actual installations described below.

Fig. 3b shows graphically the voltage range over which the condenser by itself with no load on the system would be able to swing the secondary voltage, in this case a total of ten per cent. This means, in other words, that the condenser would be capable of maintaining flat regulation on the secondary bus if the natural regulation due to load variation amounted to ten per cent without correction.

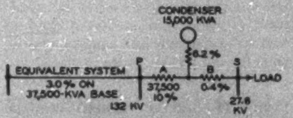

Figure 5. Type I transformer application, equivalent setup for Riverside substation, Benton Harbor, Mich.

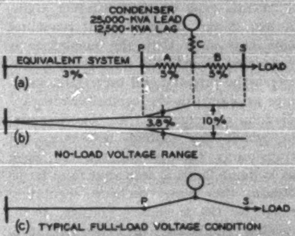

Figure 6. Typical application of type II three-winding transformer and condenser for ten-per cent no-load voltage range

Typical three-winding transformer installations of the type I design on the American Gas and Electric Company system are in service at Sunnyside substation, Canton, Ohio, and Riverside substation, Benton Harbor, Mich. Equivalent reactance diagrams for each of these installations are shown in figures 4 and 5, respectively. In these installations as in a number of others of the same type, the condenser or condensers are arranged to regulate the secondary bus voltage and, as far as electrical performance is concerned, the condensers might just as well have been connected directly to the secondary bus. The only purpose of the three-winding transformer is to avoid the necessity of a second transformer bank and associated switching equipment which would otherwise be required to operate the synchronous condensers.

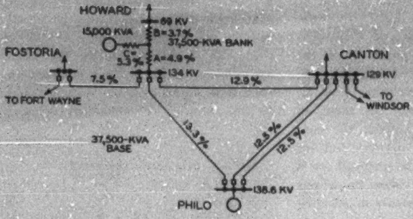

Figure 7. Type II transformer application; equivalent setup for Howard substation, near Shelby, Ohio

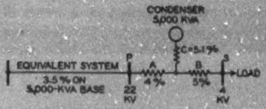

Figure 8. Type II transformer application; equivalent setup for Carbondale substation, Carbondale, Pa.

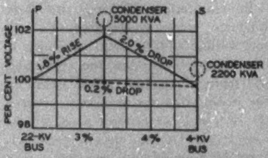

Figure 9. Comparison of condenser capacity required with three-winding and two-winding transformers, for Carbondale substation, Carbondale, Pa.

TYPE II—SPECIAL THREE-WINDING TRANSFORMER DESIGNS WITH CONDENSER REGULATING SECONDARY VOLTAGE BUT WITH INCREASED EFFECT ON PRIMARY VOLTAGE

In this type of three-winding transformer design the equivalent reactance circuit is such that the condenser is connected electrically somewhere in between the primary and secondary windings as shown in the typical elementary diagram of figure 6*a*. In this diagram, in which the system reactance and transformer bank rating are the same as in figure 3*a*, it is seen that in order to obtain the same no-load regulating range for the secondary voltage, as indicated graphically in figure 6*b*, it is necessary to use a synchronous condenser of nearly double the size. Figure 6*c* shows graphically a typical full-load condition for this setup, in which it will be observed that the condenser must be larger, not only because the total reactance *A* plus the system reactance, on which the condenser operates is smaller than in figure 3*a*, but also because of the necessity of overcoming the uncorrected voltage drop in leg *B* produced by the secondary load.

The purpose of such a transformer design is to provide regulation, primarily for the secondary bus voltage, but at the same time to utilize *more* synchronous condenser capacity than would be required with the type I transformer design, thereby giving greater correction to the high-voltage system. If the daily load curve for the local load supplied from the secondary bus is somewhat similar to the high-voltage-system load curve, then it is possible with this scheme to obtain fairly flat regulation on the high-voltage bus at the same time that the secondary bus is being held constant. To approach this result it is a fairly simple matter to determine the relative values for *A* and *B* and the condenser capacity required, taking into account of course the system load and impedance characteristics and the local secondary load.

Obviously, the larger leg *B* (figure 6*a*) is made with respect to *A*, the greater the condenser capacity required to hold the secondary voltage constant. Often the condenser size is limited by the size of the transformer bank, which in turn is fixed by the amount of secondary load to be handled. In such cases the problem becomes one of selecting relative values for legs *A* and *B* which will require a given condenser to work through a reasonably full range while regulating the secondary bus voltage. In this way the high-voltage system is given the benefit of as much correction as the capacity of the particular condenser will permit, but without sacrificing the main function of secondary bus regulation.

One of the first examples of this type of application was that installed at the Howard substation as shown in figure 7. Here the condenser rating of 15,000 kva was chosen by bank capacity and for other reasons, and it was desired to give the 132-kv system the full benefit of the condenser output while regulating the 66-kv bus. This was particularly important in this case in order to help supply the flow of reactive kilovolt-amperes required between Howard and Canton, the latter being a low-voltage point on the system because of previously installed transformers rated at 120 kv. Here, the division between *A* and *B* is in the proportion of 57 per cent to 43 per cent of the total reactance from primary to secondary which falls within the manufacturer's tolerance on a specification calling for a 60 per cent and 40 per cent division.

The operating results with this setup have been satisfactory and the condenser is still holding good voltage on the 66-kv system, although with the present heavier 132-kv system power flow, which was not altogether foreseen at the time of the installation, it would have been better to have placed the condenser a little closer to the 66-kv bus. On the other hand, this defect has been fully compensated by a subsequent installation of a much larger synchronous condenser on the 132-kv system at an adjacent station (Fostoria), which will be described later under four-winding transformers.

The use of this type of three-winding transformer is not confined to instances where the secondary voltage is too high for synchronous-condenser operation. Figure 8 shows the Carbondale substation and the equivalent connected 22-kv system of The Scranton Electric Company, where it was desired to regulate the 4-kv bus and at the same time to provide as much correction as possible for the 22-kv system. Here a 5,000-kva synchronous condenser, which is much larger than would have been necessary for regulating directly on the 4-kv bus, is used effectively by connecting it to the 4-kv tertiary winding of a 3-winding transformer bank. Figure 9 shows an analysis, confined to the transformer circuit alone, which was made prior to this installation to show a comparison between the condenser capacity required to overcome the transformer drop under a given load, first, with the condenser directly on the 4-kv bus and, second, with the condenser on the tertiary winding of a three-winding bank of the design shown in figure 10. Of course, the comparison shown here was not quite complete because of the omission of the system impedance back of the 22-kv bus. Actually, the difference between the condensers required in the two setups would be somewhat less due to the effect of this difference in condenser capacity on the system impedance, as brought out in the discussion of figure 1.

This installation has been in operation for some time and approaches flat regulation for both 4-kv and 22-kv bus voltages.

It is possible in certain cases to obtain the advantages of the type II three-winding transformer design by adding an external reactor to an existing two- or three-winding transformer bank. In the Suburban plant setup, shown in elementary form in figure 11, it was desired to install a synchronous condenser to regulate the 4-kv distribution bus, but at the same time to furnish the maximum amount of much needed reactive kilovolt-ampere correction to the 22-kv and 66-kv systems. This result was made possible by installing the 10.8 per cent reactor, shown as leg *B* in figure 11, and connecting the synchronous condenser to an auxiliary 4-kv bus connected directly to the terminals of the transformer bank. This scheme not only made it possible to deliver practically double the amount of reactive correction to the 66-kv system but also reduced the short-circuit problem on the 4-kv distribution bus. To make the scheme adjustable, both for present as well as future requirements, taps were provided in the reactor.

Other notable installations of type II three-winding transformers with synchronous condensers are now under construction and will be placed in operation during the present year at Torrey substation at Canton, Ohio, and at New Carlisle substation near South Bend, Indiana. The former is a 30,000-kva transformer-bank installation, and the latter, 45,000 kva.

TYPE III—THREE-WINDING TRANSFORMER DESIGN WITH CONDENSER REGULATING PRIMARY VOLTAGE

In this type of three-winding transformer design, the equivalent reactance network is such that the condenser is connected electrically close to the high-voltage winding and therefore is capable of effectively regulating the high-voltage bus only. In other words, leg *A* in the equivalent network is small with respect to *B*. A typical elementary diagram is shown in figure 12 where the values used correspond to those of figures 3 and 6. Obviously, such a setup does not provide a practical means of regulating the secondary bus voltage since large swings in primary voltage and a very large condenser would be required in order to compensate the drop through leg *B* of the transformer. It might be used, however, where it is desired to maintain constant voltage on the high-voltage bus, and by providing separate means, such as automatic tap changing under load, for regulating the secondary bus.

No setups of this type involving three-winding transformers and synchronous condensers are in operation on the American Gas and Electric Company system, although the four-winding transformer installations described below are similar in performance and purpose to this type.

Special Four-Winding Transformers With Synchronous Condensers or Generators for Regulating More Than One Bus Voltage

In 1935 a proposed synchronous-condenser application together with a new transformer bank to be installed at the Fostoria substation presented a somewhat unusual regulating problem. An existing installation of a 20,000-kva three-winding transformer bank, stepping down from 132 kv to 66 kv with a tertiary winding operating two 5,000-kva synchronous condensers, was totally ineffective for regulating the 66-kv bus because the transformer design was type III instead of type I. The existing transformer bank was fortunately quite suitable for use at another location where additional capacity without synchronous condensers was needed. In replacing this bank it was desired to provide for the operation of a large synchronous condenser to maintain bus voltage on the 132-kv system, and at the same time it was necessary to provide some means of regulation for the 66-kv bus.

The first plan proposed for this job was to use a type III three-winding transformer bank with load-ratio-control in the 66-kv winding, and to transfer the two 5,000-kva condensers to some other location. The plan finally worked out, however, made use of a four-winding transformer, designed along the lines shown in figures 13 and 14. The two 5,000-kva synchronous condensers were connected to a fourth winding so placed with respect to the other windings that effective regulation of the 66-kv bus could be obtained at the same time that the large condenser was regulating the 132-kv bus. As shown in figure 14, which represents an approximation of the equivalent reactance network of the four-winding transformer bank, the electrical effect is simply as though the large condenser were connected directly on the 132-kv bus and the two small condensers directly on the 66-kv bus. While the rigorous representation of the reactance network of a four-winding transformer bank involves a complicated solution such as that worked out by F. M. Starr,[8] the actual diagram did not differ greatly from the simple straight-line equivalent circuit shown in figure 14 and produced by an arrangement of windings on the transformer core as shown in figure 13. The transformers were specified, designed, and purchased to meet approximately the equivalent-reactance circuit of figure 14.

For regulating the 132-kv bus, there was purchased a synchronous condenser having a continuous rating of 36,000 kva at 15 pounds hydrogen pressure, and provided with sufficient excitation voltage and capacity to give a short-time ceiling overload rating of 75,000 kva.

Since the two 5,000-kva condensers were built for 4,000-volt operation, whereas a rating of 11,000 volts was chosen for the new condenser, a con-

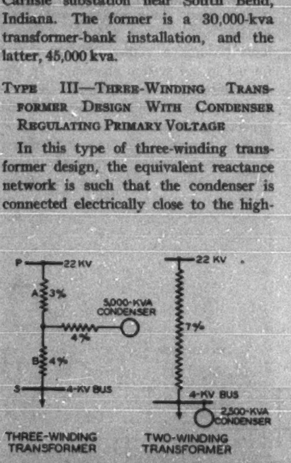

Figure 10. Equivalent networks of three-winding and two-winding transformers used for comparison in figure 9

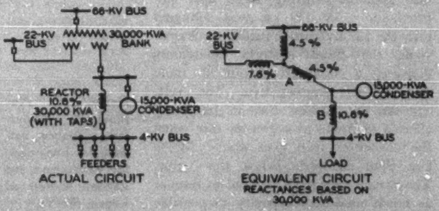

Figure 11. Simulation of type II transformers by adding reactor between load and condenser—Suburban plant, Scranton, Pa.

venient incidental feature of the four-winding transformer design was the use of the 4,000-volt condenser winding for starting the large machine, thus eliminating the necessity for compensator or starting taps. Besides furnishing about the right voltage for starting purposes, this arrangement, with the two 5,000-kva condensers running while starting the large machine, offered the additional advantage of supplying a large part of the starting kilovolt-amperes directly, thereby reducing the disturbance on the system.

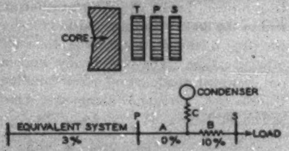

Figure 12. Probable arrangement of windings and typical application of type III three-winding transformer with condenser regulating high-voltage system only

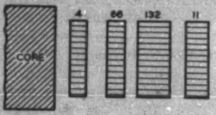

Figure 13. Winding arrangement for four-winding transformers, Fostoria, Ohio

The performance of this installation has been eminently satisfactory, both from the standpoint of the 132-kv system regulation and the 66-kv bus regulation. One 5,000-kva condenser has been sufficient much of the time for maintaining 66-kv bus voltage. Some time after this installation was completed there was staged a test to determine the ability of the 132-kv system, together with this condenser installation, to handle heavy emergency power flow into Indiana. During these tests approximately 150,000 kw was delivered over the 132-kv system into Fort Wayne while the three Fostoria condensers delivered a combined output of more than 60,000 reactive kva. The voltage on the 132-kv bus is purposely allowed to droop at a greater rate when the condenser load goes above 36,000 kva than at loads below this value. The reason for this is to hold in reserve as much condenser capacity as possible and for as long a time as possible before the output of the machine is automatically cut back because of excessive field temperature. The inherent droop in the electronic regulator is approximately one per cent from zero to 30,000 kva on the condenser, with an additional droop of between three per cent and four per cent in going from 30,000 to 75,000 kva.

A more recent and somewhat larger four-winding transformer bank, having some of the same characteristics as that at Fostoria, was designed and installed for use with the high-pressure superposition turbine at Logan, W. Va. The elementary diagram showing the approximate reactance relationships between the four windings of this bank and the adjacent system connections is shown in figure 16. The relative arrangement of windings is shown in figure 15. It will be noted that the 50,000-kva generator is connected in effect close to the 132-kv bus and is arranged to maintain voltage on that bus. The two 18,750-kva generators are connected directly through step-up transformers to the 44-kv bus, and the three small machines on the 4-kv winding are coupled fairly close to the 44-kv winding but remote from the 132-kv and 11-kv windings. In this setup the 44-kv bus voltage is controlled by means of the two 18,750-kva generators, while the three small machines are expected to operate without regulation and with a fixed field setting near unity power factor to obtain maximum kilowatt output from the generators. The 50,000-kva machine regulates voltage on the 132-kv bus through the use of a resistance-reactance compensator in conjunction with the generator terminal voltage and the load delivered to the transformer.

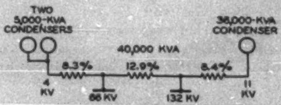

Figure 14. Approximate equivalent reactance network for Fostoria four-winding transformers

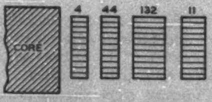

Figure 15. Winding arrangement for four-winding transformer, Logan, W. Va.

This installation has worked out entirely satisfactorily in performing the regulating functions for which it was designed. As in the case of the Fostoria transformer bank, the rigorous equivalent reactance network of the transformer bank would be somewhat more complicated but the coil arrangement and approximate reactances specified and obtained for the bank gave very nearly the straight-line reactance diagram shown in figure 16. A photograph of one unit of this transformer bank is shown in figure 17.

Conclusions

It will be seen from the above descriptions of actual installations on the American Gas and Electric Company system that three-winding and four-winding transformers properly designed have become a rather important tool for use in our system planning. In any large interconnected network such as this, having many important load and generating points, with a wide variation both in direction and amount of power flow, it is becoming increasingly necessary to operate to a large extent at fixed voltage levels throughout the system. Of course, a certain amount

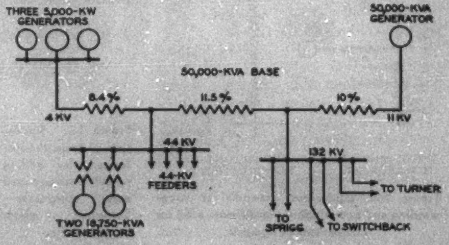

Figure 16. Approximate equivalent reactance network for four-winding transformers and station layout at Logan, W. Va.

of voltage gradient is always necessary to keep within economic limits of synchronous-condenser capacity but these voltage gradients are not allowed to become extreme. Important generating stations maintain a voltage of approximately five per cent above the nominal 132-kv level, while the low points on the system, with one or two exceptions, do not fall below 132 kv. Under these conditions, heavy power flow in any given direction for any considerable distance necessarily involves leading power factor, that is, a reverse flow of reactive kilovolt-amperes, most of which must be supplied by synchronous condensers. The special designs of three-winding and four-winding transformers described herein have contributed very substantially to the solution of the problem of maintaining reasonably constant voltage levels under these conditions of operation on the 132-kv system. In addition a number of regulating problems on lower-voltage systems have been efficiently handled by their use.

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Figure 17. One unit of 100,000-kva four-winding transformer bank at Logan, W. Va.

Discussion

Wm. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The concept of an equivalent system reactance against which a change in reactive kilovolt-amperes on a synchronous machine operates is almost indispensable for intelligent planning or even for best operation of interconnected synchronous equipment. Many have unquestionably used it in some form without a thorough understanding of the principles which have been so clearly illustrated by the author. He is to be congratulated on the clearness with which he has reduced the handling of reactive kilovolt-amperes to simple terms, and for securing valuable system characteristics at little or no increase in capital expenditure through proper advance planning.

Naturally the concept of a change in reactive kilovolt-amperes at one point of a system operating upon its equivalent reactance is based on the assumption that the resulting redistribution of reactive kilovolt-amperes between various machines will not cause any of them to reach their operating limits. This will usually be true in practical problems, but the incremental reactive-kilovolt-ampere values which are readily given by a calculating board or analytical study (necessary to determine the equivalent system reactance) will provide a simple check. In case the ability of any machine is exceeded, its connection on the calculating board may be removed from the supply bus (or common bus when considering tap changes). Results will include the variation in voltage on the "regulated" bus where the limits of machine abilities are reached.

A useful corollary of this principle is that a change in reactive kilovolt-amperes or a transformer tap change on an interconnected system will not cause any incremental flow of reactive kilovolt-amperes over any line or impedance branch which directly joins two regulated busses (assuming the machine abilities are not exceeded). Accordingly, such branches may be omitted in arriving at equivalent system reactance.

L. H. Hill (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): One of the noticeable trends in transformer design and application during the past few years has been the increased number of multiwinding transformers. Not so many years ago a three-winding transformer was considered comparatively unusual, but today three-winding transformers are comparatively common. The number of four-winding transformers that have been actually used so far has been relatively small, but Mr. St. Clair has ably indicated some of the applications for this type of transformer.

It might be interesting to point out from the manufacturing point of view that the construction of a four-winding transformer introduces no great technical difficulties. As far as the electrical design of the transformer is concerned, the principal problem consists in arranging the windings to obtain the desired reactances. In the case of core-type transformers generally used in the smaller sizes, the number of possibilities is limited due to the fact that it is usually desirable to use concentric windings. In the larger sizes, however, where the shell type of construction is frequently used, the windings can be grouped so that almost any combination of reactances may be obtained without sacrificing other desirable characteristics in the transformer. Having made an electrical design to suit the reactances desired, the only remaining added complications to the ordinary transformer are the greater number of bushings required and the greater number of terminal boards needed. In smaller sizes of transformers it may be difficult to obtain the necessary clearances for terminal boards and bushings without resorting to a tank larger than necessary, but in the larger sizes the necessary space is usually available.

Figure 1

Mr. St. Clair describes an installation of four-winding transformers at the Fostoria substation, and figure 1 of this discussion illustrates three of the transformers before the spare unit had been installed. The complete rating is as follows:

High voltage 13,333 kva, 138 kv
Second voltage 6,667 kva, 69 kv
Third voltage 12,500 kva, 11 kv
Fourth voltage 4,000 kva, 4 kv
} single phase, 60 cycles

H. P. St. Clair: Mr. Hill mentioned some of the principal problems which have to be met and solved in the design, construction, and assembly of multiwinding transformers.

In regard to the problem of arranging the windings to obtain the reactances desired, we have made it a point to co-operate closely with the designers by making available to them as much flexibility as possible without sacrificing the main regulating function which is desired in a particular application. In this way, we have tried to avoid penalizing the transformer design from the standpoint of cost because of the reactances requested. In a very few cases a certain amount of penalty in cost may have been unavoidable, but, if so, the improved over-all operating results obtained were worth the small additional cost. In general, however, it is possible to work out a satisfactory design to accomplish the desired results without any increase in cost.

Wave Shape of 30- and 60-Phase Rectifier Groups

O. K. MARTI
FELLOW AIEE

T. A. TAYLOR
MEMBER AIEE

As pointed out in a number of recent papers,[1,2] a mercury-arc rectifier draws a distorted current from the a-c supply system, even though power to the system is supplied by a sine-wave source. The harmonics in the current wave react on the supply system to cause distortion in the voltage wave, the degree of distortion being a function of the rating of the rectifier, the number of phases in which power is supplied to the anodes, the impedance at harmonic frequencies of the supply system, and the rectifier load. It is well known that, in theory at least, reductions in the wave-shape distortion may be effected by increasing the number of rectifier phases and thus reducing the number of harmonic components present in the supply-circuit current.[1,2] Up to 1937, however, because of practical difficulties, largely in the design of the complex transformer arrangement required, no attempts were made to produce rectifiers commercially having more than 12 phases.

Recently the Aluminum Company of America installed rectifiers having a total capacity of 82,500 kilowatts for conversion of alternating to direct current in its plants at Alcoa, Tenn., and Massena, N. Y.[3] These are the largest installations of rectifiers supplied from overhead transmission systems in this country at the present time. The power supply at both Alcoa and Massena may be interconnected with several high-voltage networks, which in turn supply several power distribution systems involved in exposures with open-wire telephone lines. Co-operative studies were made to determine the effects of these rectifiers on the wave shape of the a-c supply systems and the telephone-circuit noise. These studies were carried on during 1937 and 1938, and included preliminary estimates of the harmonic components arising in the rectifiers and their effect on the power-system wave shape. The estimates indicated that the operation of the rectifiers could be expected to increase the inductive influence of the power-supply networks over the area affected, with resultant increases in the telephone circuit noise. Tests made after half of the rectifiers were in operation at Alcoa confirmed the results of the estimates and indicated the necessity for a reduction in the rectifier harmonics.

Because of the size of these installations, and the complexity of the supply systems, it appeared impracticable to limit the rectifier harmonics by the use of frequency-selective devices, which have been successfully applied to certain smaller installations.[1] However, theoretical considerations and tests on a miniature rectifier set simulating the electrical characteristics of the converters under consideration, indicated that substantial reductions in the wave-shape distortion might be effected through the use of a relatively simple installation of phase-shifting transformers. Following the successful application of this idea in the case of the miniature rectifiers, a set of full-sized phase-shifting transformers was built and applied to each group of five 5,500-kw rectifiers at Alcoa. This arrangement reduced the important harmonic components on the power-supply systems to relatively small values, and restored the wave shape and noise conditions practically to normal. This paper describes briefly the voltage and current relations, the preliminary tests on the small-scale rectifier equipment, the phase-shifting transformers and their application to this particular situation, and presents data to indicate their effectiveness.

Harmonics in Power Supply to Multiphase Rectifiers

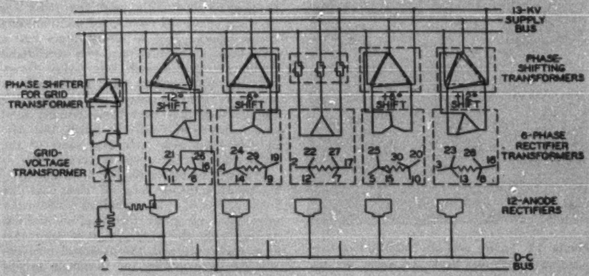
A number of different transformer connections[4] are used to secure six-phase operation of a rectifier when supplied from a three-phase source. For all of these connections there is a 60-degree phase relation between the fundamental-frequency voltages applied to anodes which fire consecutively, and the lowest-order harmonic component appearing in the d-c voltage wave is the sixth (360 cycles for a 60-cycle supply system). The corresponding components in the a-c supply are the fifth and seventh harmonics (300 and 420 cycles). All harmonics of order ($6a \pm 1$), where a is any positive integer, appear in the a-c supply.

In order to increase the number of phases beyond six, it is necessary to ar-

Figure 1. Schematic diagram of transformer connections in 30-phase rectifier station

Notes:

1. Connections shown for only one six-phase secondary on each rectifier transformer. Actually in each transformer there are four groups of secondaries operated in parallel supplying a total of 24 anodes in two rectifier tanks
2. Numbers on transformer secondaries indicate firing order of anodes in the five-unit station (30-phase operation)
3. Dotted Δ of phase-shifting transformers indicates supply-bus voltage; heavy lines refer to voltages applied to rectifier transformer primaries



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1. For all numbered references, see list at end of paper.

Table I. Theoretical Relationships in Multiphase Rectifiers

Equivalent Number of Rectifier Phases	Phase Relation Between Fundamental-Frequency Voltages Applied to Anodes Which Fire Consecutively (Deg)	Lowest Order Harmonic in D-C Output	Lowest Order Harmonic in A-C Supply
6	60	6	5, 7
12	30	12	11, 13
18	20	18	17, 19
24	15	24	23, 25
30	12	30	29, 31
36	10	36	35, 37
48	7.5	48	47, 49
60	6	60	59, 61

range the rectifier transformer connections so that the phase angle between the fundamental-frequency voltages applied to two anodes which fire consecutively is less than 60 degrees. One method of accomplishing this is by multiple connection of secondary windings associated with a single primary. The delta zig-zag connection for 12-phase rectifiers is one example of this method. Twelve-phase operation may also be secured by operating two 6-phase rectifiers in parallel, with the primary of one transformer connected delta and the second in wye. In either case the phase relation between the fundamental-frequency voltages applied to corresponding anodes of the two units is 30 degrees. Theoretically, the lowest-order harmonic in the d-c circuit voltage is then the 12th (720 cycles) appearing as 660 and 780 cycles on the a-c side. Similarly, if it is possible to operate a larger number of 6-phase rectifiers in parallel, and to provide the proper phase displacement between the fundamental-frequency voltages applied to the corresponding anodes of the different rectifiers, the number of phases can be increased, together with the order of the lowest harmonic appearing in the d-c output and a-c supply to the station, as shown in table I.

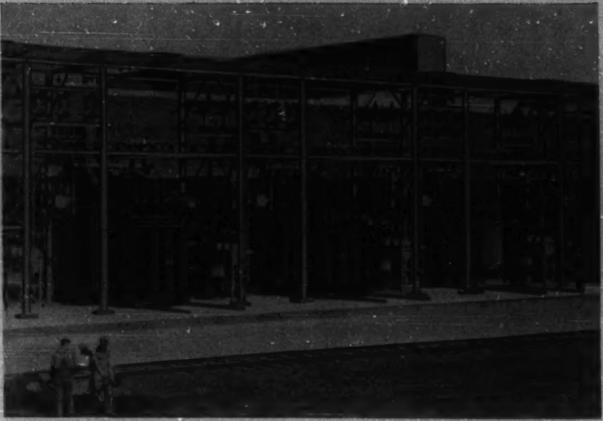


Figure 3. Rectifier transformers, phase-shifting transformers, and reactor

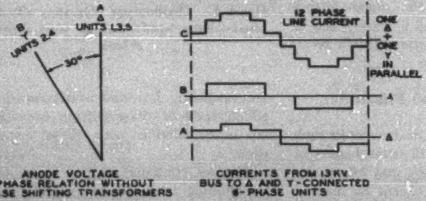
Thirty-Phase Operation at Alcoa and Massena

The rectifiers installed at Alcoa are divided into two stations, each having a total d-c output of 27,500 kw. Each of these stations includes five rectifier transformers, and each transformer, having two double six-phase secondaries, supplies two 12-anode rectifier tanks. All secondaries are connected in double wye with interphase transformers. Each unit (one transformer and two rectifier tanks) is rated at 5,500 kw. Referring to figure 1, in the first station three of the five rectifier transformers have delta-connected primaries, while the primaries of the other two transformers are wye-connected. The second station at Alcoa, as well as the station at Massena, has three units with wye-connected primaries and two with delta-connected primaries. In all three stations four of the units are connected to the supply bus through phase-shifting transformers and one through a reactor.

In figure 1, the connections of the grid-control circuit for regulating the firing of the anodes, and as a consequence the d-c circuit voltage, are shown only for unit 1.

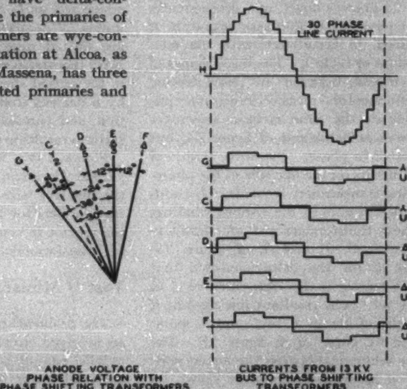
The vector diagram of figure 2*A* shows the 30-degree phase relation between the fundamental-frequency voltages applied to corresponding anodes of a wye-connected and delta-connected unit, and the adjoining curves illustrate the wave forms of the currents between the supply bus and these units without the phase-shifting transformers. The resultant current

Figure 2. Phase relation between fundamental-frequency voltages applied to corresponding anodes of five units of figure 1 and currents from 13-kv bus to individual units



A
Δ
UNITS 1,3,5
B
Y
UNITS 2,4
30°
12 PHASE
LINE CURRENT
ONE Δ + ONE Y IN PARALLEL
C
B
A
Y
Δ
ANODE VOLTAGE PHASE RELATION WITHOUT PHASE SHIFTING TRANSFORMERS
CURRENTS FROM 13 KV BUS TO Δ AND Y-CONNECTED 6-PHASE UNITS

(A)



G Y 4
C Y 2
D Δ 5
E Δ 3
F Δ 1
-12°
+12°
-24°
-36°
-30°
30 PHASE
LINE CURRENT
H
G
C
D
E
F
Δ(-36°) UNIT 4
Δ(-24°) UNIT 2
Δ(-12°) UNIT 5
Δ(-0°) UNIT 3
Δ(+12°) UNIT 1
ANODE VOLTAGE PHASE RELATION WITH PHASE SHIFTING TRANSFORMERS
CURRENTS FROM 13 KV BUS TO PHASE SHIFTING TRANSFORMERS

(B)

Figure 4. Miniature test set simulating 30-phase system

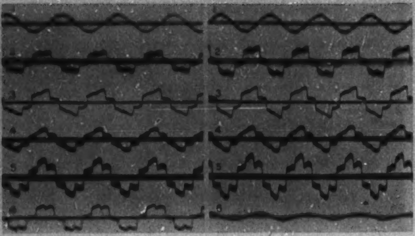


Figure 5. Oscillograms illustrating effect of phase-shifting transformers on currents to each unit in miniature tests

1—Resultant line current
2—Wye unit (−6 degrees shift)
3—Wye unit (+6 degrees shift)
4—Delta unit (−12 degrees shift)
5—Delta unit (no shift)
6—Delta unit (+12 degrees shift)

A—Five units in service
B—Four units in service

flowing in the supply circuit, to which these two rectifier units are directly connected, and in case no other converting device is connected to that circuit, is the sum of the currents represented by curves *A* and *B* as shown in curve *C*. (As will be shown later, the wave shape of these currents is not as simple as indicated in this diagram due to other factors, such as resistance, inductance, arc drop, exciting currents of transformers, wave shape of the supply-bus voltage, etc.) The phase-shifting transformers are connected as autotransformers ahead of four of the rectifier transformers, as shown in figure 1. A positive phase shift of 12 degrees is consequently introduced ahead of one of the delta-connected units, with a corresponding negative shift ahead of a second delta unit. Positive and negative phase shifts of six degrees are introduced ahead of the two wye units. As shown in the vector diagram, figure 2*B*, the fundamental-frequency voltages applied to corresponding anodes of the five units are now 12 degrees apart, and consequently the equivalent of 30-phase operation is obtained, in accordance with the relationships of table I. The wave shapes of the currents between the phase-shifting transformers and rectifier-transformer primaries are the same as those shown for the wye or delta unit of figure 2*A*, but displaced in phase. The wave shapes of the currents on the line side of the phase-shifting transformers are derived by adding the currents in the various windings of these transformers and are shown in curves *C*, *D*, *F*, and *G*, of figure 2*B*. Curve *E* for the delta-connected unit with a reactor is the same as curve *A* of figure 2*A*. The resultant line current is the sum of curves *C* to *G* and is represented by curve *H* in figure 2*B*. The phase relations, between the anode voltages of the various units are shown by the direction of the secondary windings in figure 1, together with the firing order. The dotted lines of the phase-shifting transformer vectors indicate the supply-bus voltage, and the heavy lines give the relative phases of the voltages applied to the rectifier-transformer primaries.

Different types and sizes of phase-shifting transformers are supplied for 6- and 12-degree phase shift. The transformers for positive and negative phase shift are identical except for some of their internal connections. Both sizes of transformer are designed to have equal reactances of approximately one per cent based on the rating of the rectifier transformer. A three-phase reactor is connected ahead of the unit requiring no phase shift to compensate for the drop in the other units. This reactor was introduced in order to avoid all possible sources of harmonics from unbalances, due to the angle between voltages not being exactly 30 degrees, or the magnitudes of the voltages and the firing period of the anodes not being equal. In the case of the other stations at Alcoa and Massena, where there are three wye-connected and two delta-connected units, the same arrangement is used, except that two wye-connected units are shifted by plus and minus 12 degrees, while the delta units are shifted plus and minus 6 degrees. With this arrangement the anode voltages in the second station at Alcoa are shifted by 6 degrees from those in the first station, and parallel connection of the two stations results in 60-phase operation.

Figure 3 is a view of the transformer yard at Alcoa and shows from left to right the relative size of the phase-shifting transformers for 12 and 6 degrees and of the reactor in comparison with the rectifier transformers.

Test of Miniature 30-Phase Set

The problems to be dealt with in such an involved circuit with several units in parallel cannot be easily and effectively analyzed theoretically except by making many assumptions and neglecting some of the factors. Such an analysis requires a great deal of time and the results are subject to an element of uncertainty, especially in view of the fact that the voltages and currents involved are not of sinusoidal wave shape. For this reason a model 30-phase rectifier test set (see figure 4) was built in order to obtain as quickly as possible the necessary design and operating data and to determine the effectiveness of phase-shifting transformers in connection with these installations.

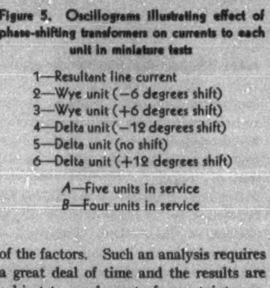


Figure 6. Oscillograms illustrating effect of phase-shifting transformers on wave shape at Alcoa

The set consisted of five units, each unit having a rectifier and a phase-shifting transformer with six grid-controlled tubes. In other words, each unit of six tubes was comparable to a six-anode rectifier, whereas in the field 12-anode rectifiers are used with two anodes operating in parallel. A polarization voltage simulating the back electromotive force due to the electrolytic cells was provided on the d-c side. The constants or characteristics of the transformers and reactors, although small-scale models were used, were such that representative conditions as to the circulating currents between wye and delta units, resistance and reactance drop, etc., were obtained.

Since very little power was required, the study of the wave shapes with oscillograph and wave-shape analyzer for different system and load characteristics could be carried out very easily and with the greatest convenience. Some of the results obtained with this set are shown in the oscillograms of figure 5*A* and *B*. The lower curves of figure 5*A* show the primary currents in one phase of each of the five units (three delta and two wye), and the top curve is the resultant current of the same phase of the 30-phase system. It will be noted that the latter approaches much more closely a smooth sine wave. A comparison may be made with the curves of figure 2*B* which are arranged in the same order as the oscillographic traces, and the effect of the phase-shifting transformers can be easily analyzed. The trace of the primary current of the last unit is somewhat distorted due to the presence of an abnormally high reactance in the circuit.

Figure 5*B* shows the result of the same setup but with one unit taken out of service. The resultant of the primary current of the four units is somewhat distorted under this condition when compared to the top curve of figure 5*A*. However, the four units still shared the load very satisfactorily. The last trace of figure 5*B* shows the magnetizing current in the primary of the transformer, the cathode of this unit being disconnected. Many other circuit arrangements were studied, taking several units out of service and adding a sixth unit with delta or wye connection to the setup. Furthermore, the influence of delaying the firing of the anodes by means of grid control was investigated and its effect on the shape of the resultant supply bus current observed.

Effectiveness of Phase-Shifting Transformers

According to the relationships of table I, the 30-phase connection should eliminate all harmonics in the a-c supply to the rectifier station below the 29th (1,740 cycles). Complete elimination of these harmonics is not realized in practice, due to slight inequalities in the loads on the rectifier units and in the impedance of the rectifier transformers and associated bus structure, and also on account of difficulties in securing exactly the proper phase relationship between the various anode voltages. However, as indicated in table II, the measured reductions in harmonic current for 30-phase operation compared with straight 6-phase operation, range as high as 90 to 1, with the greatest reductions at the lower frequencies, which are present in the largest magnitudes for 6-phase operation. The *I·T* product,[5] which is an index to the weighted harmonic content of the alternating current, is reduced by as much as 18 to 1 through the use of the phase-shifting transformers.

The improvement in 13-kv line-current and bus-voltage wave shape resulting from the use of the phase shifters is graphically illustrated by the oscillograms of figure 6. The first two traces were obtained immediately after the first rectifier station at Alcoa was placed in operation and before the installation of the phase-shifting transformers. The two lower traces were obtained with the

Table II. Effectiveness of 30-Phase Connection in Reducing Harmonic Currents From 27,500-Kw Rectifier Station Showing Effect of Grid Control†

		"Equivalent 6-Phase Operation" Arithmetic Sum of Harmonic Line Currents in Rectifier Units 1, 2, 3, 4, and 5 (Amperes)			"Equivalent 30-Phase Operation" Vector Sum of Harmonic Line Currents (Total Current to Station) (Amperes)			Effectiveness of 30-Phase Connection as Compared With 6-Phase Operation**		
Harmonic n	Frequency	No Grid Control	Balanced Loads*	5 Per Cent Control	No Grid Control	Balanced Loads*	5 Per Cent Control	No Grid Control	Balanced Loads*	5 Per Cent Control
5	300	266.0	262.0		8.0	5.0		33.0	52.0	
7	420	105.0	101.0		7.9	8.5		13.0	13.0	
11	660	70.5	72.0	78.7	0.79	1.08	2.32	89.0	66.0	34.0
13	780	49.8	47.5	47.5	0.555	0.51	1.7	90.0	94.0	28.0
17	1,020	25.9	26.9	42.5	0.39	0.93	1.9	66.0	29.0	23.0
19	1,140	21.3	23.8	27.1	0.31	0.85	1.4	69.0	28.0	20.0
23	1,380	8.5	9.6	24.7	0.97	0.33	2.6	8.8	29.0	9.4
25	1,500	8.3	10.0	18.2	0.96	1.21	1.4	8.6	8.2	13.0
29‡	1,740	4.96	4.8	13.8	4.85	4.45	13.7	1.02	1.08	1.01
31‡	1,860	4.71	3.9	10.9	4.6	3.6	10.9	1.03	1.08	1.0
35	2,100	4.83			0.57			8.6		
37	2,220	5.03			0.88			5.7		
41	2,460	3.56			0.18			19.7		
43	2,580	3.58			0.17			20.9		
47	2,820	2.29			0.15			15.1		
49	2,940	2.35			0.16			14.4		
53	3,180	1.68			0.41			4.1		
55	3,300	1.65			0.30			5.6		
59‡	3,540	1.29			1.21			1.07		
61‡	3,660	1.23			1.08			1.14		
65	3,900	0.95			0.34			2.85		
67	4,020	1.19			0.45			2.65		
I·T§		480,000	505,500	659,000	29,600	28,100	78,000	16.2	18.0	8.4
13-kv rectifier bus voltage TIF§					45	38	112			
154-kv bus voltage TIF§					8	10	13			
D-c bus voltage		600	605	575						
Total d-c output current (amperes)		40,000	39,200	35,000						

* Small amount of grid control on certain units to balance d-c output currents.

** Expressed as ratio of arithmetic sum of harmonic currents in individual rectifiers to vector sum of corresponding current.

† These frequencies not suppressed by 30-phase operation.

‡ Measured at Alcoa on station with three delta and two wye units.

§ See reference 5.

same rectifier loads and supply system arrangement after the installation of the phase shifters.

The phase-to-neutral impedances of the 13-kv supply system, looked at from the primaries of the phase-shifting transformers, as calculated from the harmonic voltages and currents for the system arrangement under which the data of table II were obtained, are as follows:

Frequency	Phase-to-Neutral Impedance (Ohms)
660	5.6
780	6.7
1,020	4.2
1,140	8.1
1,380	7.8
1,500	8.3
1,740	13.6
1,860	11.1

The commutating reactance of the rectifier transformers is approximately six per cent.

Effect of Grid Control on 30-Phase Operation

Included in table II are the results of tests to determine the effect on the a-c wave shape of reducing the d-c bus voltage by the use of grid control.[2,6] The use of only a small amount of control on certain units to balance the d-c load among the units makes no substantial change in the over-all effectiveness of the phase-shifting transformers. However, with grid control used to reduce the d-c bus voltage in the order of five per cent, the over-all effectiveness of the 30-phase arrangement is somewhat impaired, as indicated by an increase of about 3 to 1 in the $I \cdot T$ product. Even under this condition, the 30-phase arrangement is about eight times better than straight six-phase operation with the same amount of grid control, and the reductions at four important frequencies (660, 780, 1,020, and 1,140 cycles) are 20 to 1 or more.

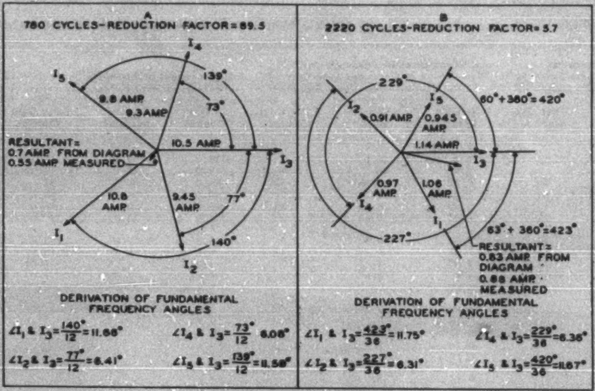

A
780 CYCLES-REDUCTION FACTOR=89.5
I4
I5
I3
I1
I2
9.8 AMP
9.3 AMP
10.5 AMP
10.8 AMP
9.45 AMP
139°
73°
77°
140°
RESULTANT= 0.7 AMP. FROM DIAGRAM 0.55 AMP. MEASURED
DERIVATION OF FUNDAMENTAL FREQUENCY ANGLES
∠I1 & I3 = 140°/12 = 11.66°
∠I4 & I3 = 73°/12 6.08°
∠I2 & I3 = 77°/12 = 6.41°
∠I5 & I3 = 139°/12 = 11.58°
B
2220 CYCLES-REDUCTION FACTOR=5.7
229°
I5
I2
I3
I4
I1
0.91 AMP
0.945 AMP
1.14 AMP
0.97 AMP
1.06 AMP
60°+360°=420°
63°+360°=423°
227°
RESULTANT= 0.83 AMP. FROM DIAGRAM 0.88 AMP. MEASURED
DERIVATION OF FUNDAMENTAL FREQUENCY ANGLES
∠I1 & I3 = 423°/36 = 11.75°
∠I4 & I3 = 229°/36 = 6.36°
∠I2 & I3 = 227°/36 = 6.31°
∠I5 & I3 = 420°/36 = 11.67°

Figure 7. Measured vector relations between harmonic line currents to five rectifiers equipped with phase-shifting transformers for 30-phase operation as shown in figure 1

Vector Relations Between Harmonic Line Currents

The effectiveness of the 30-phase connection in reducing the harmonics out of the rectifier station depends upon the balance of five nearly equal currents in the proper phase relation. This is illustrated in figure 7*A* which shows the measured vector relations between the 780-cycle line currents to the five rectifier units of figure 1. These currents were measured on the line side of the phase-shifting transformers. The phase angles were obtained from measurements of sums and differences of the harmonic voltage drops across inductive shunts in the secondaries of the current transformers associated with each unit. It is evident from the diagram that the five vectors are nearly equal and approximately 72 degrees apart. The resultant of the five vectors is also shown. The reduction factor of 89.5 is the ratio of the arithmetic sum of the five currents to the vector sum or the resultant current.

Table III. Effect of "Balanced" and "Unbalanced" Rectifier Operation†

Ratio of Harmonic Currents With 6-Phase Operation to Those Measured With 30-Phase Operation

Frequency	Five Units in Parallel "Balanced Rectifiers"*	Four Units in Parallel*
300	51.5	5.9
420	27.0	4.5
660	43.7	3.7
780	35.7	3.8
1,020	29.0	3.0
1,140	42.0	3.9
1,380	22.9	2.2
1,500	28.2	2.7
1,740‡	0.95	1.01
1,860‡	0.92	1.05
2,100	6.2	
2,220	5.8	
2,460	23.5	
2,580	38.2	
2,820	24.0	
2,940	16.2	
3,180	6.3	
3,300	3.9	
3,540‡	1.12	
3,660‡	1.19	
3,900	3.5	
4,020	3.8	
$I \cdot T$ (ratio)	15.9	3.2
$I \cdot T$ (6 phase) magnitude	449,000	330,000
$I \cdot T$ (30 phase) magnitude	28,300	104,000
Voltage TIF (13-kv rectifier bus)	29.0	69.0

† Measured at Massena on 27,500-kw station with three wye and two delta units.

‡ These frequencies not suppressed by 30-phase operation.

* Same total d-c load in each case.

Since 780 cycles (13th harmonic) appears in the a-c supply as a result of the 12th harmonic in the d-c wave, the phase shift of the various 780-cycle currents from that in unit 3 (taken as reference) should be 12 times the fundamental-frequency shift introduced by the phase-shifting transformers. The derivation of the fundamental-frequency angles from the measured angles is shown in figure 7*A*. The derived values are fairly close to the theoretical 6- and 12-degree shifts.

A similar diagram for the 37th harmonic is shown in figure 7*B*. In this case the phase shift is 36 times that at the fundamental frequency. The fundamental-frequency shifts derived from the measured angles are about the same as those derived from the 780-cycle diagram. However, the effects of the small deviations from the correct shifts of 12 and 6 degrees tend to become greater for the higher-order harmonics, and the angles between the five vectors in figure 7*B* no longer closely approximate 72 degrees. The reduction factor is only 5.7 compared with 89.5 at 780 cycles.

Table IV. Effect of Phase-Shifting Transformers on Supply-System Voltage Wave Shape and Telephone Circuit Noise (for 27,500-Kw Station in Operation at Alcoa)

	Rectifiers Off	Five Units* No Phase Shifters	Five Units Phase Shifters**
Power System A		**Voltage TIF**	
Alcoa 13-kv rectifier bus	10	176	40
Alcoa 154-kv bus	10	77	15
Location 1 on 154 kv	9	64	7
2 on 154 kv	12	118	13
3 on 154 kv	20	480	34
4 on 154 kv	16	84	14
		Maximum Noise on Exposed Toll Circuits	
Decibels above reference noise	37	57	37
Noise units	500	5,000	500
Power System B		**Voltage TIF**	
Alcoa 13-kv rectifier bus	10	79	14
Alcoa 120-kv bus	10	51	10
Location 5 on 120 kv	15	56	21
6 on 120 kv	9	23	12
7 on 66 kv	14	33	24
		Maximum Noise on Exposed Toll Circuits	
Decibels above reference noise	32	44	35
Noise units	280	1,100	400
Power System C		**Voltage TIF**	
Alcoa 13-kv rectifier bus	10	137	
Alcoa 154-kv bus	14	233	16
		Maximum Noise on Aluminum Company Telephone Circuits	
Decibels above reference noise	42	73.5	42
Noise units	900	33,000	900

* Equivalent to one 12-phase rectifier of 22,000 kw and one 6-phase rectifier of 5,500 kw in parallel.

** "Balanced operation."

"Balanced" and "Unbalanced" Operation

From the vector diagrams of figure 7 it may be seen that the effectiveness of the 30-phase method of operation is impaired if one of the units is shut down. If the load on the four remaining units is unchanged, the resultant harmonic current at the suppressed frequencies will be equal to the current which was supplied by the unit which was shut down, or the reduction from straight six-phase operation will be four to one. This reduction is approximately realized in practice as shown by the ratios in the last column of table III. The reduction factor for the $I \cdot T$ product is about three to one.

A comparison of the data for "balanced" operation in tables II and III shows that the phase-shifting transformers are about equally effective whether applied to a station comprising three delta and two wye units, or three wye and two delta units.

Supply-System Wave Shape and Telephone-Circuit Noise

The previous comparisons have been made on the basis of the improvement effected by 30-phase operation of the rectifiers compared with straight 6-phase operation. Actually, before the installation of the phase-shifting transformers, four of the units in the first station at Alcoa made up two 12-phase rectifiers, with one extra 6-phase unit. This method of operation made substantial reductions (as compared to straight 6-phase operation) in the harmonic line currents at 300, 420, 1,020, 1,140 cycles, etc., while accomplishing no reduction at 660, 780, 1,380, 1,500 cycles, etc. The wave-shape and noise tests which indicated the necessity for further reductions in the harmonics were made with this arrangement, and under a variety of supply-system conditions presenting different system impedances as viewed from the rectifiers. To indicate the improvement in wave shape and noise conditions accomplished by the installation of the phase-shifting transformers, the summary of table IV is included. The maximum noise values on exposed telephone toll circuits are included merely to indicate the order of improvement effected on a large number of circuits when the phase shifters were installed.

Table V. Effect of 30-Phase Rectifier Operation on Voltage TIF of Supply Systems to Massena 27,500-Kw Rectifier

Circuit	Rectifiers Off	Balanced Rectifiers	One Unit (No. 3) Out of Service
Power condition (a)			
Massena rectifier 13-kv bus	12.1	28.8	69
Massena 110-kv bus	14.8	15.8	21
Location 1—13-kv bus	20.7	20.6	22.8
Location 2	27*	24*	32*
Power condition (b)			
Massena rectifier 13-kv bus	9.1	48	75
Massena 110-kv bus	14.1	14.7	15
Location 2	28.8*	32.5*	35.7*

* 1,500-cycle component from 450-horsepower synchronous motor on motor generator set controlled the voltage TIF at location 2. TIF observed with motor off, but with balanced loads on the rectifiers, was 11.

Tests made at Massena following the installation of the 27,500-kw (five-unit) station equipped with phase-shifting transformers indicated that the rectifiers had practically no effect on the wave shape of the 110-kv supply system under the "balanced" operating condition, and a relatively small effect when one unit was out of service, as indicated in table V.

Final tests were made at Alcoa after the completion of the second station and with phase-shifting transformers installed ahead of all ten units to provide 60-phase operation. The results of these tests under three power conditions are shown in table VI and indicate that voltage-wave-shape conditions under balanced operation compare favorably with those existing before the rectifier station was installed. The effects of grid control and of "unbalanced" operation with one unit out of service are also shown in table VI.

Conclusion

As demonstrated in the case of these large rectifier installations, the introduction of autotransformers as phase shifters offers a very attractive means of reducing harmonic components which otherwise would be outstanding where multiunit rectifiers are involved. In situations of

Table VI. Power-System Influence Measured With 55,000-Kw 60-Phase Rectifier Operating at Alcoa

	Voltage TIF 13-Kv Rectifier Bus	Voltage TIF 154-Kv Bus	Voltage TIF 120-Kv Bus	Total 13-Kv I·T Product
Power condition a-2				
Rectifiers off	10.0	9.0	10.0	
Balanced (no control)	14.3	11.0	4.5	37,000
Balanced (5 per cent control)	14.5	10.2		48,000
Unbalanced* (no control)	37.0	40.5	12.6	154,000
Power condition a-1				
Rectifiers off	10.0	9.0		
Balanced (no control)	28.2	12.8		42,000
Balanced (2 per cent control)	31.5	19.2		45,000
Unbalanced* (no control)	67.0	37.0		126,000
Power condition a-3				
Rectifiers off	10.0	14.0		
Balanced (no control)	15.2	11.9		43,500
Balanced (5 per cent control)	17.4	11.6		65,000
Unbalanced* (no control)	40.3	41.5		153,000

* One unit shut down—nine units operating.

this character, this method of improving wave-shape conditions has proved not only effective but also simpler and more economical than the application of filtering devices, usually consisting of a combination of reactors and capacitors, which have been used to some extent with satisfactory results in smaller rectifier installations.

The application of phase shifters is by no means limited to the present arrangement, but can be adapted to a variety of conditions. For instance, in case four units (with four or eight tanks), instead of five units as in the above stations, would suffice to meet the required load, a 24-phase system could be arranged by using one phase shifter, giving a relative phase displacement of 15 degrees, in connection with two rectifier groups, each consisting of a transformer with a wye and with a delta connected primary. The same result could be secured by using two identical phase shifters for plus and minus 7.5 degrees. In case three units (with three or six tanks) would take care of the required load, or if larger rectifier units could be built to meet the load of the stations described above, an arrangement could be used comprising three 12-phase rectifier units, one shifted plus 10 degrees and another minus 10 degrees, resulting in 36-phase operation. Such an arrangement would appear to offer some advantage over the present 30-phase system, as it would require only one type of rectifier transformer, although of a more complicated construction, and one design of phase-shifting transformer. The 12-phase system could be obtained with one wye and one delta 6-phase transformer as one unit, or the conventional 12-phase with quadruple secondary. This would simplify the problems of design, installation, and eventual replacement, and should result in an even better wave shape than the 30-phase arrangement. However, the flexibility of the station would be somewhat impaired due to the smaller number of units. Other arrangements may, of course, be used to suit the particular requirements of each case. When the supply-system impedance conditions are such that the influence is materially affected by those harmonic components which are unsuppressed with the particular arrangement employed, it should be relatively simple to reduce these components by means of selective devices.

The solution of the wave shape and noise problems presented by these large rectifier installations is an illustration of the results which can be achieved by cooperation between the electrical manufacturer, the purchaser of electrical equipment, the power-supply companies, and the communication companies. It is expected that the experience gained during this study will be valuable in the design and installation of future large rectifier stations.

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Discussion

F. O. Stebbins (General Electric Company, Schenectady, N. Y.): This paper presents an entirely adequate solution of this particular communication-circuit noise-level problem and because of its complete form and the factual data contained therein is a valuable contribution to the literature on this subject.

Apparently the noise level at rated kilowatt load, with all units operating (at approximately equal kilowatt load per unit) is well below the minimum acceptable values. It may be assumed that with the same kilowatt load imposed on four units instead of five or nine tanks instead of ten that the noise levels in the high-voltage bus materially affecting communication circuits were still within acceptable limits.

In studying this problem was it possible to (1) establish some minimum acceptable noise level as an objective; (2) determine whether the 13-kv or the high-voltage (110 or 154 kv) lines would govern permissible noise levels?

Due to the very nature of electrochemical operations all equipment must be capable of carrying rated load 24 hours per day, 365 days per year. Hence the determination of an acceptable minimum noise level under the least favorable operating conditions (such as normal kilowatt load imposed on four instead of five units or nine instead of ten tanks) is most important, and may have a material influence on station layouts, provisions for spare capacity, etc.

Local conditions, at the point of installation, such as (*a*) the capacity and extent of a-c power supply, (*b*) interconnection of this system with other systems, (*c*) exposure of open-wire main-trunk-line communication systems to the power supply, (*d*) the number of high-voltage transformations in the complete system have an important effect on the solution of this noise-level problem.

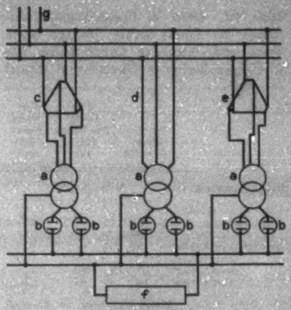


Figure 1. Large rectifier plant with 36-phase connection

a—12-phase transformers of identical construction; output 8,000 kw each
b—Two 5,000-ampere 800-volt rectifiers per transformer
c—Phase shift transformer for plus ten degrees
d—Without phase-shift transformer
e—Phase-shift transformer for minus ten degrees
f—Load
g—Supply lines

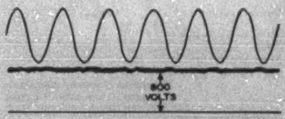


Figure 2. 36-phase connection obtained by parallel operation of three 12-phase transformers, each for 10,000 amperes, 800 volts

Common primary current of the three rectifier transformers

As examples the 60,000-kw station of the Consolidated Mining and Smelting Company at Trail, B. C., and the 54,000-kw station of the Aluminum Company of Canada do not at present require equipment to improve the wave shape of the power supply. All of these factors may be studied prior to installation and an approximate solution attained.

The harmonic reductions the authors obtained with the 30-phase group seem to approach maximum values approximately halfway between the frequencies that the 30-phase group does not suppress. That is, these reductions increase to maximum values at 13th and 43rd harmonic frequencies, but are zero at 29th, 31st, 59th and 61st harmonic frequencies, etc. Did the authors find that this same characteristic applied also to the 60-phase group, and the reductions in the harmonics approach a maximum at approximately 29th harmonic frequencies? Also how did the harmonic reductions of the 60-phase group compare with those obtained with the 30-phase group?

We note that the authors built and tested a miniature 30-phase rectifier in their factory and made a study of the effect of unbalanced 30-phase operation by shutting down one unit in a 30-phase group. The poorest wave shapes obtained with unbalanced operation, which are shown on the oscillogram taken in the factory, are made evident later in the rectifier installations at Alcoa and Massena, where unbalanced operation increased the voltage TIF on the 13-kv station bus, approximately two to one.

We would like to know whether the authors' tests showed that the large harmonic reductions obtained on the a-c side of the 30-phase rectifier also were found to exist on the d-c side of the rectifier. Did these reductions increase to maximum values at frequencies halfway between the 30th and 60th harmonic frequencies, as they did on the a-c side of the rectifier?

It has been found that odd harmonics in the a-c supply voltage wave to a rectifier come through to the d-c side of the rectifier as even harmonics of approximately the same percentage value. Did the authors learn whether a 30-phase or a 60-phase group will tend to reduce the percentage values of the harmonics in the d-c output voltage, produced by harmonics in the a-c supply voltage?

Have the authors reached any conclusions regarding the economic limit of filtering equipment, that is the maximum kilowatt rating of a station at which the filter is a more economical solution to the wave-shape problem than the phase-shifting autotransformer?

E. V. DeBlieux (General Electric Company, Pittsfield, Mass.): The authors of the paper have made a valuable contribution to rectifier operating experience in reporting the results of tests of the Massena and Alcoa installations. These tests demonstrate that wave-shape distortion of rectifier installations can be reduced to acceptable values by phase multiplication.

The required kilowatt output of the station, purchaser's specifications covering the number of operating units, and operating requirements as to spare capacity, will determine the number of units (transformer and associated rectifiers) in a given station. At the present time the units must operate either in 6-phase or 12-phase relationship to the supply system. These factors will determine what can be done by way of phase multiplication to minimize TIF. I believe that there is little advantage in going to more than 36, or possibly 48 phases, because the higher harmonics are a small item in the TIF, extreme accuracy of phase shift is required to eliminate these higher harmonics (see figure 7*b* of the paper), and also unless the output is very large it is not economical to divide the installation into the large number of units required.

The authors state that due to various factors, complete elimination of certain harmonics was not realized. Were any tests made to determine whether or not it would be possible to improve the harmonic elimination by unequally dividing the load or by altering the phase shift between units? Figures 1 and 2 of this discussion are a connection diagram and an oscillogram of a German installation that consisted of several 24,000-kw 36-phase sets using a connection similar to that at Alcoa. The oscillogram shows the same excellent line-current wave shape as figure 6 of the subject paper. In the German installation reactors were connected in the rectifier cathode lines to limit the harmonic currents that circulate between rectifiers and transformer secondaries as a result of the phase displacement between units. Did the authors of the paper take oscillograms of anode current or other measurements to determine the magnitude of the circulating harmonic currents in their installation?

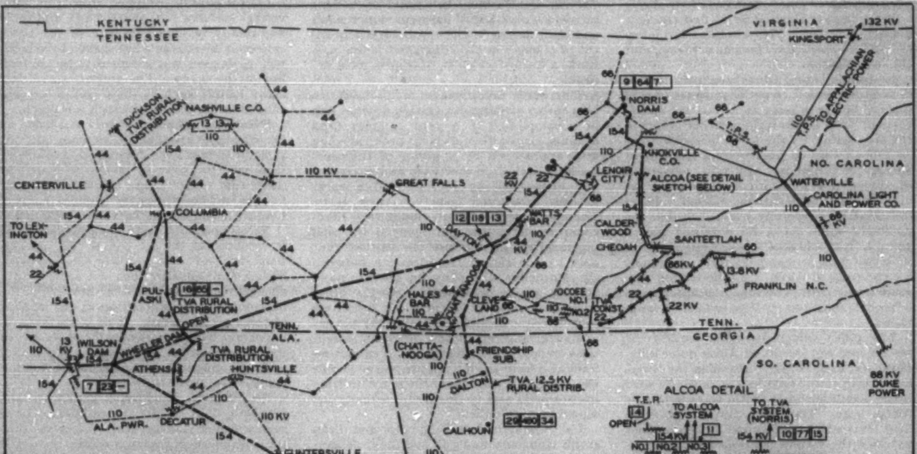
S. A. Flemister (Southern Bell Telephone and Telegraph Company, Atlanta, Ga.): The paper by Doctor Marti and Mr. Taylor contains much interesting information regarding the many important electrical features of a large mercury-arc-rectifier installation such as that at the plant of the Aluminum Company of America at Alcoa, Tenn. Of most interest and concern to telephone engineers are, of course, those features relating to the harmonics impressed by the rectifiers on the connected a-c power-supply network, since it is through the propagation of these harmonics over the connected high-voltage transmission lines and thence over the urban and rural distribution systems connected therewith that the noise-influence problem may become important.

As it was my good fortune to participate in the tests and analyses made in connection with the Alcoa installation, the paper under discussion is most interesting to me. While the paper covers rather completely

Figure 3. Voltage telephone influence factors measured at various points

27,000 kw of rectifiers supplied from TVA only

The value in the left-hand side of the box at a given location shows the voltage telephone influence factor without rectifiers on system. The value in the center of box shows the voltage TIF with rectifiers and without phase-shifting transformers. The value in the right-hand side of box shows the voltage TIF with rectifiers and with phase-shifting transformers


KENTUCKY
TENNESSEE
VIRGINIA
132 KV
KINGSPORT
110 T.P.S. TO APPALACHIAN ELECTRIC POWER
9 | 64 | 7
NORRIS DAM
66
154
110
KNOXVILLE C.O.
T.R.S.
NO. CAROLINA
LENOIR CITY
ALCOA (SEE DETAIL SKETCH BELOW)
WATERVILLE
CAROLINA LIGHT AND POWER CO.
66 KV
22 KV
WATTS BAR
12 | 118 | 13
DAYTON
CALDERWOOD
CHEOAH
SANTEETLAH
66 KV
13.8 KV
FRANKLIN N.C.
CHATTANOOGA
HALES BAR
CLEVELAND
OCOEE NO.1
NO.2
TVA CONST.
22 KV
TENN.
GEORGIA
SO. CAROLINA
88 KV DUKE POWER
FRIENDSHIP SUB.
DALTON
TVA 12.5 KV RURAL DISTRIB.
29 | 480 | 34
CALHOUN
(CHATTA-NOOGA)
TO GA. PWR. CO.
GUNTERSVILLE
110 KV
154
DECATUR
ALA. PWR.
HUNTSVILLE
TVA RURAL DISTRIBUTION
ATHENS
WHEELER DAM
OPEN
WILSON DAM
13 KV
7 | 23 | –
TENN.
ALA.
PULASKI
16 | 60 | –
TVA RURAL DISTRIBUTION
COLUMBIA
CENTERVILLE
TO LEXINGTON
DICKSON TVA RURAL DISTRIBUTION
NASHVILLE C.O.
13 | 13
110 KV
GREAT FALLS
44
ALCOA DETAIL
T.E.R.
14
OPEN
TO ALCOA SYSTEM
11
154 KV
NO.1 NO.2 NO.3
13 KV
ROTARY BUS NO.1
ROTARY BUS NO.2
TO TVA SYSTEM (NORRIS)
154 KV
10 | 77 | 15
BANK NO.4
10 | 176 | 40
13 KV
5 UNITS
27,000-KW RECTIFIERS
LINES OF TVA SYSTEM
COMMONWEALTH AND SOUTHERN SYSTEM
LINES OF THE ALUMINUM COMPANY

the technical aspects of the problem, I believe it will also be of interest to describe the scope of the problem and the co-operative arrangements which so facilitated the testing work and the ultimate solution.

To illustrate the scope of the problem from the operating standpoint, figure 3 of this discussion has been prepared to show the wide geographical area and the extensive connected transmission-line network involved. By reference to the figure, it may be seen that the area included large parts of the states of North Carolina, Georgia, Alabama, and Tennessee, which are served by a part or all of the transmission-line systems of the Carolina Power and Light Company, the Nantahala Power Company (owned by the Aluminum Company), the Tennessee Public Service Company, the Tennessee Valley Authority, the Tennessee Electric Power Company, the Alabama Power Company, and the Aluminum Company of America. As can be appreciated, in addition to the network of transmission lines shown, there were extensive connected urban and rural distribution networks, many of which were involved in inductive exposures to the circuits of the telephone plant.

Based on experience with other (smaller) rectifier installations and on the studies made by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell Telephone System, it seemed quite probable from the beginning, that a rectifier installation of the magnitude contemplated would result in widespread and important increases in power-system influence and in the noise on telephone systems exposed to the circuits of the connected power network. In view of this, the problem was discussed at an early date with all of the power organizations whose systems might be affected as well as with the Aluminum Company and the Allis-Chalmers Company. All of these organizations were very interested and expressed their desire to do whatever they could to find the best possible solution. This wholehearted interest and co-operation continued throughout the investigation.

In deciding upon a test program and procedure, it was felt that the program should include not only tests at Alcoa, but simultaneous tests at strategic outlying points in the area served by the power networks connected directly or indirectly to the bus at Alcoa. Accordingly, single-line diagrams of the power systems involved were studied and the field-test locations selected. These locations were carefully selected so that the pertinent data could be obtained and a suitable test schedule worked out to fit in with switching arrangements, etc., necessary for the tests at Alcoa. As the testing program progressed, it became desirable to add a few field locations to those originally designated.

The paper under discussion covers the testing and test results at Alcoa quite fully. However, little is said concerning the correlated tests at the field locations which were made simultaneously with certain of the Alcoa tests. The chief purpose of these field tests was to determine by the manner in which the various harmonics were propagated from Alcoa, the magnitude and extent of the changes in the power-line influence with the rectifiers connected to the different power systems under normal and probable emergency power operating conditions. Tests at these locations were all made with instruments of the indicating type and consisted of voltage-wave analyses, noise tests on selected representative telephone circuits, and measurements of the telephone influence factor (TIF).

As an indication of the extent of propagation of the harmonic voltages arising in the rectifiers, the measured TIF values at several locations are shown in the figure for one of the power operating arrangements tested. This is the condition of the TVA system only supplying 27,000 kw of rectifiers, which represented half the station capacity. Three values of measured TIF are shown in boxes at the selected locations for the TVA system. In the box to the left is shown the reference value of TIF measured with no rectifier load connected to the power system. The value in the middle shows the measured TIF with the rectifiers connected to the system but with the phase-shifting transformers out. In the right of the box is shown the TIF value with the rectifiers supplied through the phase-shifting transformers.

The final solution adopted was, as discussed in the paper, accomplished by the provision of specially designed phase-shifting transformers. These particular transformers are, as indicated by the data shown on the figure, amazingly effective in reducing the harmonics which would otherwise be impressed on the connected power network. A satisfactory reduction might have been accomplished through other means, such as the use of filters, etc. However, had filters been employed, they probably would have been more costly and their effectiveness would have been dependent upon keeping the power-system impedance nearly the same as that assumed in the design of the filters. This restriction would, of course, have been very undesirable from the power operating viewpoint. The use of the phase-shifting transformers has provided a very satisfactory method of harmonic suppression and their action in this respect is independent of changes in the connected power network, a feature which is, of course, most desirable.

This whole investigation seems to me to be a rather striking illustration of what can be accomplished in a very complex (and at times almost discouraging) problem when it is approached by a group of engineers working in close co-operation and interested only in determining the facts and in working out a solution based on these facts. There is no need and not enough time to recount in detail the part played by each organization because any one can see that only by the closest co-ordination between the Aluminum Company on the one hand and the power organizations on the other could it be possible to carry out tests over such a wide area and under so many power-system arrangements and operating conditions smoothly and efficiently. I cannot refrain also from paying tribute to the people who conceived the idea of the phase-shifting transformers and to those who actually designed and built them.

The Southern Bell Telephone and Telegraph Company and the long-lines department of the American Telephone and Telegraph Company were assisted in the conduct of the field tests and in the analysis of the results by engineers from the headquarters organization of the American Telephone and Telegraph Company and Bell Telephone Laboratories in New York. The Western Union Telegraph Company and the Southern Railway, which operate communication facilities in the area, were kept in close touch with the study.

O. K. Marti and T. A. Taylor: Mr. Flemister's discussion supplements the data in the paper and emphasizes the scope of the co-ordination problem and the co-operative arrangements employed in its solution.

In answer to the question of Mr. De-Blieux, no tests were made to determine the effect on harmonic reduction of unequally dividing the load or altering the phase shift between units. An effort was made to keep the loads balanced and no provision was available on the phase-shifting transformer for changing the phase shift. While some improvement might be made by these changes, neither change seems practicable in commercial operation. No measurements were made of the magnitudes of the circulating harmonic currents during the joint study. However, some data were secured by the Allis-Chalmers Company and will probably be included in another paper covering additional aspects of this installation.

In answer to Mr. Stebbins' question, there is, of course, no practical method of setting up definite limits for noise or power-system influence. In this particular case the power-system influence and the noise levels on exposed telephone circuits before the rectifiers were placed in operation were taken as the primary objective in determining the desirable reductions in the harmonics. This objective was fully met under conditions of balanced operation of the station, although some increases in influence and noise were noted with unbalanced operation. Since the principal exposures were to power distribution circuits supplied from the high-voltage systems (110 and 154 kv), and there was relatively little exposure to the 13-kv system at Alcoa, the wave shape of the high-voltage system was assumed to be controlling in the study. The authors agree that many factors such as those mentioned will enter into the solution of such a problem.

Detailed measurements were not made on the 60-phase installation, so data are not at hand to make the comparison requested. However, the over-all measurements indicated that the 60-phase connection was at least as satisfactory as the 30-phase connection with respect to harmonic reductions.

No co-operative tests were made on the wave shape of the d-c end of this installation, since no noise problem was involved. Theoretical considerations indicate that substantial reductions would be expected in the harmonics on the d-c output as the number of phases is increased. This is particularly important from the standpoint of the use of the larger number of phases in connection with d-c transmission and merits further study.

No definite conclusions regarding the economic limit of filtering equipment have been reached. In general, it appears that the multiphase connection will be restricted to the larger installations, where all units are operated in parallel at constant load, while the various filter arrangements may be more economically applied to the smaller installations.

Lightning Investigation on Transmission Lines—VII

W. W. LEWIS
FELLOW AIEE

C. M. FOUST
MEMBER AIEE

THE lightning investigation reported in previous papers of this series was continued in 1937 and 1938, with the following power companies participating:

Pennsylvania Power and Light Company
Ebasco Services, Inc.
Appalachian Electric Power Company
American Gas and Electric Company
Consumers Power Company

The following companies also co-operated by furnishing pertinent information:

Pennsylvania Water and Power Company
Philadelphia Electric Company
Public Service Electric and Gas Company

Work was confined mainly to a study of magnitude and distribution of current in tower members, overhead ground wires, conductors and counterpoise wires. All readings were taken by means of magnetic links.[1] The present paper constitutes a progress report on certain features of the investigation.

Current in Towers and Strokes

In the immediately preceding paper of this series,[2] cumulative curves were shown of tower and probable stroke current, involving 358 strokes on four systems, during the years 1933, 1934, and 1935.

Figure 1 of the present paper shows similar curves for 734 strokes, on seven lines ranging from 66 to 220 kv, during the four years from 1933 to 1936 inclusive. The shapes of these curves do not differ greatly from the previous curves.

In working up the data for figure 1, tower currents were found by adding the currents in the four legs and neglecting any currents that might flow in the diagonal members or braces. Stroke currents were found by adding currents in all towers affected by the stroke. As ordinates the average of each 10,000-ampere range has been plotted.

In 1937 and 1938 a number of installations were made in which magnetic links were placed on cross members or braces in addition to the corner legs. It was found that the cross members carried from 30 to 100 per cent as much current as the legs, depending on how symmetrical the counterpoise arrangement was with respect to the tower legs. However, no currents were measured which indicated tower or stroke currents in excess of those shown on figure 1.

Occasional questions have been raised as to the validity of the method of obtaining stroke current by adding currents in adjacent towers which appeared to be affected by the same lightning discharge. Accordingly measurements obtained near the tower top and before current subdivision had taken place have been analyzed to show direct-stroke currents. These records were obtained on tower-top lightning rods and overhead ground wires and are summarized in table I.

This table includes some 80 records obtained over several years operation and on four different lines. The percentage figures from table I have been plotted on figure 2 against the average of each 10,000-ampere range. For comparison the stroke-current curve from figure 1 has been reproduced on figure 2.

The direct measurement in rods and ground wires shows a higher percentage of strokes in the low-current range and a lower percentage of strokes in the high-current range. The higher percentage of low-current strokes is not due to current sensitivity of the measurement stations because considering division of current, the rod and ground wire stations recorded down to about the same current level as the tower stations. Possibly in interpreting stroke currents by addition, some low-current records which were considered portions from another stroke should have been classified as direct strokes. The lower percentage of high-current strokes obtained through rod and ground-wire measurements results from two circumstances: A number of currents extended above the upper limit of range of the measurement station. Also, it is quite reasonable that in adding up tower currents to obtain stroke currents a few excessively high records were obtained in assigning adjacent tower currents to a single stroke when in fact more than one stroke occurred. The agreement between the different methods of evaluating the stroke current is fairly good. For practical purposes, an upper limit of current somewhere between the two curves, say 150,000 amperes, may be chosen.

Lightning Currents in Line Structures

Comparing surge currents and surge-current summations in various parts of the line structure is frequently disappointing unless attention is given to a number of possible current combinations. An analysis has been made of 42 sets of records wherein currents flowing up the tower structure could be compared with corresponding currents leaving the tower top through overhead ground wires and lightning rods.

A direct comparison of "inward" with "outward" flowing currents is possible if the tower top is considered as the summa-

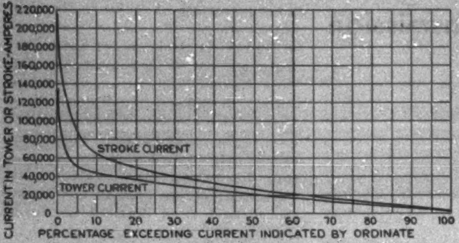
Table I. Range of Direct-Stroke Currents

Range of Current (Amperes)	Lightning Rods: Number Strokes in Range	Lightning Rods: Per Cent of Number Exceeding Current Range	Overhead Ground Wires: Number Strokes in Range	Overhead Ground Wires: Per Cent of Number Exceeding Current Range
Below 10,000	0	100	6	88
10,001– 20,000	4	87	4	80
20,001– 30,000	3	77	6	68
30,001– 40,000	7	53	7	54
40,001– 50,000	9	23	13	28
50,001– 60,000	3	13	4	20
60,001– 70,000	1	10	4	12
70,001– 80,000	3	0	1	10
80,001– 90,000			1	8
90,001–100,000			2	4
100,001–110,000			2	0
Total	30		50	

Paper 39-122, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939. Manuscript submitted April 18, 1939; made available for preprinting May 8, 1939; released for final publication August 23, 1939.

W. W. Lewis is in the engineering division of the central-station department and C. M. Foust is in the general engineering laboratory, General Electric Company, Schenectady, N. Y.

[1] For all numbered references, see list at end of paper.



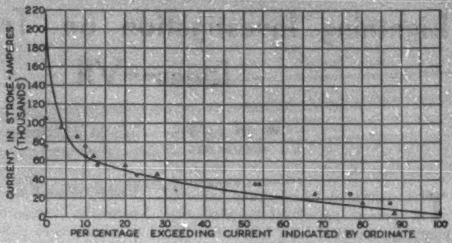


Figure 1. Cumulative curves of tower and probable stroke currents from 734 strokes occurring during the years 1933 to 1936 inclusive on the Wallenpaupack-Siegfried, Glen-lyn-Roanoke, Safe Harbor-Westport-Takoma, Philadelphia-Delaware state line, Safe Harbor-Perryville, Holtwood-Coatesville and Holtwood-York transmission lines, ranging in voltage from 66 to 230 kv

Figure 2. Stroke-current curve of figure 1 reproduced. For comparison, currents from lightning-rod measurements (circles) and overhead-ground-wire measurements (triangles) are indicated

tion point, and current polarities or current direction as indicated by the magnetic links are noted.

If currents are combined by simple algebraic addition a wide range of results is obtained. "Inward" and "outward" currents agree in about 25 per cent of the 42 cases, "inward" currents are in excess of "outward" in about 50 per cent and "outward" in excess of "inward" in about 25 per cent of all cases.

In table II are shown specimen records wherein "inward" and "outward" current values agree within plus or minus ten per cent.

In all of the cases of table II, current flowing along the ground wire or wires was joined by current flowing up the tower, and in each case current flowed into a stroke to a ground wire.

The most common case is one in which upward-flowing tower current exceeds the outward flowing current at the tower top. A number of specimens of such records is given in table III.

The first two cases are associated together because the towers are adjacent and the stroke was to a ground wire between them. Records indicate another low-current stroke to have occurred two tower spans away during the same period of exposure of these magnetic links.

For the third item where the tower-cage current was 69,630 amperes, the parallel counterpoise wire fed some 30,700 amperes into the tower base. As this counterpoise wire was connected only on one side of the tower the cross-members' currents were very high, exceeding the leg currents in magnitude. In this case, that of a two-ground-wire line, ground-wire currents flowed past the tower top in one direction on one overhead ground wire and past the tower top in the opposite direction on the other ground wire. Indications are that two strokes were present, one to the structure itself and one nearby to one of the overhead ground wires.

The fourth case is again one in which the upward current in the tower when increased by the inward flowing current in the ground wire, exceeded the outward-flowing ground-wire current. Here, however, the 70,000-ampere outward-flowing current saturated the magnetic link in the inner position leaving only the outer link available for measurement purposes. Some degree of oscillation in this current or a low magnitude reversed current would readily reduce an 82,000-ampere single-link indication to 70,000 amperes.

The fifth item, a 57,000-ampere stroke to a lightning rod, included three ground-wire currents flowing inward toward the tower top. The reading on the fourth ground wire was wiped out by a stroke to this ground wire in the adjacent tower span. However, an inward-flowing current in this wire would still further unbalance the tower-top current summation.

For the sixth item a lightning-rod stroke of 62,000 amperes gave inward-flowing currents on all four ground wires. A continuous counterpoise wire carried 37,700 amperes inward toward the tower base.

Specimen cases in which the tower-cage current when combined with inward-flowing tower-top current was less than outward tower-top current are given in table IV.

The first item of this group was a direct stroke to a tower-top lightning rod with four overhead ground wires all carrying current inward to be combined with 11,870 amperes flowing up the tower. The counterpoise wires at this tower carried a combined value of 16,470 amperes into the tower base. No other strokes reached the line nearby.

The second item pertains to a stroke to the ground wire near the tower top concerned, in which two ground wires carried current inward to the tower top and two outward. Crow-foot counterpoise wires carried inward to the tower base only 3,760 amperes as compared to 9,460 upward in the tower cage.

In the third item records were obtained when a stroke occurred to the ground wire adjacent to the tower concerned. No other strokes occurred to the line in this vicinity. In the fourth record, ground-wire currents flowed outward in both directions. Adjacent strokes occurred on each side of this tower.

These specimen records show clearly the difficulties involved in simple addition of surge-crest-ammeter records. In many instances it is possible upon careful consideration of all data to recognize difficulties in the way of good checks.

Table II. Comparison of Inward and Outward Currents at Tower Top, Showing Good Agreement

Item	Combined Leg and Brace Amperes	Ground-Wire Amperes		Combined Amperes	
		In	Out	Inward	Outward
1	11,870	8,150	18,400	20,020	18,400
2	13,350	10,200	23,000	23,550	23,000
3	15,600	2,800	17,700	18,400	17,700
4	11,250	9,160	19,800	20,410	19,800

Some of the circumstances which militate against good checks in addition of records are included in the following summary:

1. Nearby strokes causing overlapping surges.
2. High currents in nearby structure members other than the one to which the measurement station is attached.
3. Insufficient magnetic-link records to measure crest currents when oscillations or reversed currents are present.
4. Crest-current values in the several members under consideration not being simultaneous.

The fourth of these items is concerned with the nature of the distribution of cloud and ground charge. That the complete charge participating in a stroke is spread over a considerable area is commonly accepted. Also, such oscillograms of lightning-current wave shapes as have been recorded have shown complex wave shapes. Repetitive strokes are now generally recognized and it is presumed by many that each discharge taps a somewhat different cloud charge position. Continuing from these considerations it is reasonable that the several currents in different overhead ground wire and structure members will not all reach crest at the same time, which of course is the assumption on which addition of member currents is based.

Overhead-Ground-Wire Current

In view of the difficulties involved in the problem of summation of currents in line structures it is very desirable to have field records for examination, from which a reasonable criterion of comparison of measurements from station to station can be made. Fortunately, in many cases the two stations at each end of the ground wire within the tower span are carrying the same current if no stroke occurs within the span.

Some 230 records of this type in which the sending and receiving ends were carrying the same current have been accumulated during the last three years. These records ranged from 1,000 to 28,000 amperes and are of three kinds: first, records in which two magnetic links recorded intensities within the link magnetization range; second, records in which currents were low and only the inner magnetic link recorded; and third, those cases of high currents in which the inner link was magnetically saturated and only the outer-link record was available for current interpretation. All 230 records have been summarized in table V.

In this table the average of each pair of sending and receiving end stations is accepted as a reference and the percentage of deviation of the actual recorded current from it taken. In column 2 are given the number of individual records within each per cent deviation and in column three the percentage of the total records in each deviation range.

The magnetic links are selected to record crest surge currents within an accuracy of plus or minus ten per cent. On this basis some 70 per cent of the sending and receiving end stations checked each other accurately. Several of the known circumstances working against a check and quite likely responsible for the deviations greater than plus or minus 10 per cent, are as follows:

1. In a great number of cases one station will provide two link readings from which oscillatory calibrations can be used, while the corresponding station at the adjacent tower will provide only a single link reading subject to the unidirectional calibration. If some degree of oscillation is present the two-link station will indicate a higher crest current. This circumstance accounts for the greater number of excessive differences particularly of the higher percentages.
2. Possible streamer and stroke disturbances occurring between the two stations.
3. Cases where link magnetization is disturbed prior to calibration.

Table III. Comparison of Inward and Outward Currents at Tower Top, Inward Greater Than Outward

Item	Combined Leg and Brace Amperes	Lightning-Rod Amperes	Ground-Wire Amperes		Combined Amperes	
			In	Out	Inward	Outward
1	24,820		22,100	32,500	46,900	22,100
2	21,290		21,650	32,180	42,940	32,180
3	69,630		20,350	70,000	89,980	70,000
4	58,650		23,500	70,000+	82,150	70,000+
5	42,080	57,000	40,960	None	83,040	57,000
6	58,460	62,000	26,130	None	84,590	62,000

Table IV. Comparison of Inward and Outward Currents in Tower Top, Outward Greater Than Inward

Item	Combined Leg and Brace Amperes	Lightning-Rod Amperes	Ground-Wire Amperes		Combined Amperes	
			In	Out	Inward	Outward
1	11,870	33,000	8,820	None	20,690	33,000
2	9,460	None	5,200	23,500	14,660	23,500
3	15,900	None	3,800	28,500	19,700	28,500
4	14,250	None	None	32,200	14,250	32,200

Polarities of Strokes to Transmission Lines

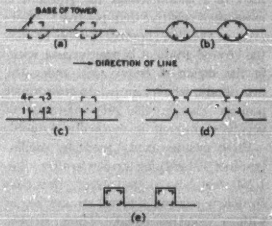


Figure 3. Typical arrangements of tower-to-tower counterpoise wires

Lightning strokes to transmission lines are predominantly negative in polarity, that is, current flows from positive earth to negative cloud. In figure 1 are summarized a total of 734 strokes accumulated from 1933 to 1936 inclusive. Of these, 685 or 93 per cent were negative and 49 or 7 per cent were positive. This total undoubtedly omits many strokes below 5,000 amperes and omits all below 2,000 amperes. That a considerable number of such low-current strokes do occur, at least under the conditions of projections of considerable height above the earth, has been definitely determined.[3] The indication is that these low-current strokes are generally of the continuing type and associated with upward-stepped leaders.

As regards cloud polarities a very interesting study has recently been reported[4] in which in addition to ground-surface gradients, soundings were made at various elevations up through the cloud by small free balloons. The results of this investigation suggest that "the main body of a thundercloud is negatively charged and that the upper part is generally charged positively." Also that "there is good evidence of the frequent occurrence of a local concentration of positive charge in the base of the cloud

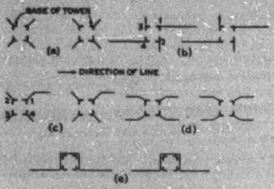


Figure 4. Typical arrangements of radial or noncontinuous counterpoise wires

usually just in front of the heavy rain center, but sometimes in other parts of the base."

Regarding the field at the ground, after combining records of some 20 storms, the authors[4] conclude that under the main body of a thundercloud the gradient is more often negative than positive. However, as the cloud approaches and recedes the ground gradient is positive and within the region of heavy rain under the main body, which rain brings down a net positive charge, the preponderance of negative gradient is markedly reduced.

From measurements[3] made by oscillograph of 55 lightning strokes to a building 1,250 feet above the street, the conclusion is reached that all strokes began with a negative cloud. Three strokes changed to positive at or near the end of the stroke and only three per cent of the total charge measured was associated with the positive portion of the strokes. In one case a continuing low current of only several hundred amperes was followed later by a negative discharge having a crest of 1,500 amperes, which was followed by a positive discharge of 2,000 amperes. These data show that mixed polarities are occasionally present in a single lightning stroke. Such a mixed polarity stroke or a sequence of mixed polarity strokes, when measured with magnetic links, will indicate an oscillatory surge and many such records have been obtained.

All magnetic-link records indicating oscillatory polarities have been interpreted through the use of an oscillatory calibration chart,[5] which was obtained in the laboratory using oscillatory calibrating surges of constant logarithmic decrement. It is obvious, therefore, that where mixed polarity surges are present and especially when the positive and negative magnitudes are of the same order, the interpretation will be correct only when the wave shape is that of a damped oscillatory capacitor discharge of constant logarithmic decrement. However, the use of such a calibration will invariably, it is believed, permit a closer approximation of the true current than would be obtained from the unidirectional polarity calibrations.

Counterpoise Wires

Counterpoise wires are in use on various systems ranging from 66 kv to 287 kv. The material used is stranded copper conductor, stranded steel conductor, stranded copperweld conductor, stranded iron wire, flat steel strip, and copper rod. The stranded copper was 1/0 and 2/0, the galvanized steel and copperweld varied from $^3/_8$ inch to $^5/_8$ inch in diameter, the copper rod was $^1/_4$ inch in diameter and the steel strip $^1/_{16}$ by 1.5 to 2 inches.

There is insufficient data to say that any of these materials or sizes is better than any other. Length of life and freedom from theft has played a large part in the choice of material.

One of the main objects of our investigation was to learn how the counterpoise functions and if possible the best arrangement of counterpoise wires. Natural lightning is very intermittent and erratic, so that an investigation of this sort is bound to progress very slowly.

Figure 3 illustrates some of the arrangements of continuous (parallel) counterpoise wires that are in use, and figure 4 shows some of the radial or discontinuous arrangements.

Distribution of Current Between Continuous Counterpoise Wires and Tower Footings

With an arrangement such as shown in figure 3*c*, readings of current in tower legs 1 and 2 (including diagonals) and in counterpoise wires were recorded in 22 cases at nine towers, and by subtraction the tower-footing current was evaluated. The summation of the readings gave distribution of current as follows:

Tower legs 1 and 2	100 per cent
Continuous counterpoise	69 per cent
Footings, legs 1 and 2	31 per cent

In five occurrences at four towers readings were obtained of current in the counterpoise wire and in all four legs of the tower, figure 3*c*, and by subtraction the footing current was obtained. The average distribution of current was approximately as follows:

Tower legs (four legs)	100 per cent
Continuous counterpoise	58 per cent
Footings (four legs)	42 per cent

With an average tower-footing resistance as measured by a Megger ground tester of 69 ohms, this would indicate an apparent counterpoise resistance of about 50 ohms.

Distribution of Current Between Radial Counterpoise Wires and Tower Footings

In an arrangement such as shown in figure 4*a*, in which each limb of the counterpoise system is 50 feet long, readings on one occasion were taken in all four tower legs and counterpoise wires, and by subtraction the footing current was found. The division of current was as follows:

Tower legs (four legs)	100 per cent
Radial counterpoise wires (four legs)	42 per cent
Footings (four legs)	58 per cent

The tower had a resistance of 47 ohms by Megger ground tester, which indicates an apparent resistance of approximately 65 ohms for the radial counterpoise system.

Table V. Ground-Wire Sending and Receiving End Comparisons

Per Cent Deviation From Average of Sending and Receiving Stations	Number of Records Within the Per Cent Deviation of Column 1	Number of Records in Per Cent Within the Per Cent Deviation in Column 1
0	41	17.7
0 to 5	80	35.0
5.1 to 10	40	17.4
10.1 to 15	23	10.0
15.1 to 20	11	4.9
20.1 to 25	8	3.5
25.1 to 30	7	3.1
30.1 to 35	4	1.7
35.1 to 40	6	2.6
40.1 to 45	4	1.7
45.1 to 50	2	0.8
50.1 to 55	1	0.4
55.1 to 60	1	0.4
60.1 to 65	1	0.4
65.1 to 70	1	0.4
Totals	230	100

In an arrangement such as shown on figure 4*c*, in which the one long wire was 250 feet long and the three short wires 50 feet long, on two occasions at the same tower readings were taken of current in the four legs and in the four counterpoise wires and the footing current was found by subtraction. The average distribution of current was as follows:

Tower legs (four legs)	100 per cent
Radial counterpoise wires (four legs)	70 per cent
Footings (four legs)	30 per cent

The tower-footing resistance by Megger ground tester was 91 ohms, giving an indicated resistance for the radial counterpoise system of 39 ohms. Of the current carried by the counterpoise wires, the 250-foot wire with $62^{1}/_{2}$ per cent of the total length carried 68 per cent of the total counterpoise current.

In an arrangement such as shown in figure 4*c* readings were taken in the long counterpoise (250-foot) wire only and in the connecting tower leg number 1, in five cases at four towers, and by subtraction the footing current was found. The distribution was as follows:

Tower leg 1	100 per cent
250-foot radial counterpoise only	72 per cent
Footings (leg 1)	28 per cent

In 16 cases involving 12 towers readings were taken simultaneously in one long (150-foot) counterpoise wire and the corresponding tower leg 4 and in one short (40-foot) counterpoise and the corresponding leg 3 (figure 4*b*). By subtraction the footing current was found. The division of current was:

Tower legs 3 and 4	100 per cent
Radial counterpoise wires (legs 3 and 4)	78 per cent
Footings (legs 3 and 4)	22 per cent

The average footing resistance of the 12 towers was 129 ohms by Megger ground tester, and assuming legs 1 and 2 and connected counterpoise wires to have the same amount and distribution of current as legs 3 and 4, an apparent resistance of 36 ohms for the radial counterpoise system would be indicated. The calculated resistance of the radial counterpoise system from actual measurement by Megger ground tester of the individual wires when separated from the towers was approximately 50 ohms.

Distribution of Current Between Tower Footings and Combination of Continuous and Discontinuous Counterpoise Wires

A section of line is equipped with one parallel counterpoise wire, which is continuous if the distance between towers is 1,000 feet or less (figure 3*e*), and broken at 500 feet from the tower if the distance between towers is greater than 1,000 feet (figure 4*e*). In three cases involving three towers in this section readings were taken simultaneously in the tower legs, tower footings, and continuous or broken counterpoise arrangement. In all three cases the wire was continuous on one side of the tower and broken on the other. Following was the division of current:

Tower current	100 per cent
Continuous counterpoise	33 per cent
Broken counterpoise	27 per cent
Tower footings	40 per cent

The tower-footing resistance by Megger ground tester averaged 135 ohms, which would indicate an average apparent resistance of 164 ohms for the continuous counterpoise and 200 ohms for the broken counterpoise. The average actual resistance measured by Megger ground tester of the continuous counterpoise wires when disconnected from the towers was 65 ohms and of the broken counterpoise wires 74 ohms.

Experimental Combinations of Continuous and Radial Counterpoise Wires and Driven Rods

An experimental installation of continuous (parallel) counterpoise wires, combined with two radial counterpoise wires extending at right angles, one in each direction (figure 5*a*), was made in a sandy high-resistance territory. The continuous counterpoise wire was connected to one corner of a three-legged tower and the radial wires to the other two legs. The length between towers was about 500 feet, so that it might be considered that 250 feet of continuous counterpoise was associated with each tower in each of two directions. Each right-angle radial counterpoise was 250 feet long, or 500 feet total per tower.

In 1937 there was a stroke to the overhead ground wire between two towers. By means of magnetic links on the counterpoise wires and the tower legs, the current was evaluated and the pickup was found to be approximately as follows (the tower footing pickup being found by subtraction):

Total current	100 per cent
Continuous counterpoise pickup	38 per cent
Right-angle counterpoise pickup	43 per cent
Footing pickup	19 per cent

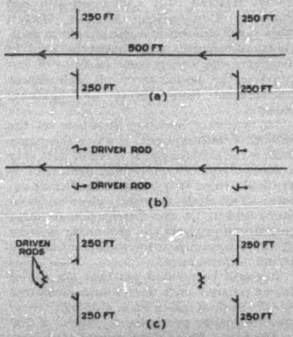


Figure 5. Experimental installations of tower-to-tower right-angle counterpoise wires and driven rods

Another experimental installation consisted of a continuous counterpoise connected to one corner of a three-cornered tower, supplemented by one or two deep-driven rods connected to the other corners, figure 5*b*, and still a third installation consisted of right-angle counterpoise wires 250 feet long extending from two corners of the tower and one or two deep-driven rods from the third corner, figure 5*c*.

In 1938 a stroke occurred to the overhead ground wire between two towers, to the left of which was the arrangement of counterpoises shown in figure 5*b* and to the right the arrangement shown in figure 5 *c*. Current was measured in the counterpoise, tower legs, and driven rods and found by subtraction in the tower footings. The distribution of current between the sections arranged as in figure 5*b* and figure 5*c* was as follows:

Total stroke current	100 per cent
Pickup in section 5*b*	74 per cent
Pickup in section 5*c*	22 per cent
Pickup in footings in both sections	4 per cent

Four towers were involved in the section arranged as in figure 5*b*. The footing resistance of these towers varied from 540 to 1,400 ohms, with an average of 831 ohms. The combined resistance of the footings and the driven rods varied from 9 to 23 ohms, with an average of 13 ohms. The apparent counterpoise resistance calculated from the distribution of current was about 61 ohms. The distribution of current between the counterpoise, driven rods, and footings was approximately as follows:

Total current picked up in section 5*b*	100 per cent
Picked up by driven rods	81 per cent
Picked up by continuous counterpoise	18 per cent
Picked up by footings	1 per cent

Three towers were involved in the section arranged as in figure 5*c*. The footing resistance of these towers varied from 665 to 910 ohms, with an average of 808 ohms. The combined resistance of the footings and the driven rods was about 13 ohms for all towers. The apparent right-angle-counterpoise resistance calculated from the distribution of current was about 46 ohms. The distribution of current between the right-angle counterpoise, driven rods, and footings was approximately as follows:

Total current picked up in section 5*c*	100 per cent
Picked up by driven rods	77 per cent
Picked up by right-angle counterpoise	22 per cent
Picked up by footings	1 per cent

It is probable that the driven rods penetrated through the sand to underlying clay or loam, while the counterpoise wires were buried in sand at very high resistance. The counterpoise wires in this experiment were equally effective whether continuous and parallel to the line or at right angles to the line. It is also apparent that in this particular territory, the deep-driven rods are more effective than the counterpoise wire.

Conclusions

1. The frequency of occurrence of lightning strokes of various current amplitudes is about the same whether measurements are obtained directly in lightning rods and overhead ground wires or amplitude values are obtained by addition of tower member and adjacent tower currents.

2. The average lightning stroke current is about 30,000 amperes. The upper level of strokes is about 150,000 amperes and possibly less than one per cent of all strokes reach this value. Stroke currents as measured range down to 2,000 amperes. Other observers using oscillographic means report strokes as low as several hundred amperes crest.[3] Some 93 per cent of all lightning strokes reaching transmission lines are of negative polarity, that is, the cloud is negative and the ground positive.

3. A summation of currents at the tower top, considered as a center for tower, overhead-ground-wire, and lightning-rod currents, shows inward and outward flowing currents equal in 25 per cent of all cases. In 50 per cent of the cases inward flowing currents are larger than outward, and in the remaining 25 per cent outward currents are larger than inward currents.

4. Seventy per cent of all records of currents flowing in overhead ground wires at adjacent stations at the ends of the same span show the sending and receiving ends to agree within plus or minus ten per cent.

5. All arrangements of counterpoise wires that have been tried have given improvement in the flashover performance of the lines, some arrangements more than others. The counterpoise wire reduces the resistance at the tower footing, reduces the earth potentials, and holds down the tower potential, thereby preventing insulator flashover.[3]

6. The data show that counterpoises of various arrangements pick up from 40 to 80 per cent of the current, as compared with the tower footings which pick up from 60 to 20 per cent. This relative amount of pickup depends on the length and arrangement of the counterpoise wires, the tower-footing resistance, and possibly other factors.

7. Counterpoise resistance calculated from the division of current between counterpoise and tower footing, agrees with the resistance measured by Megger ground tester and current divides substantially in accordance with the resistance. An exception is that of the continuous and long discontinuous or broken counterpoises, which have a low measured resistance but an actual apparent resistance to lightning current much higher and of the same order as the usual radial counterpoise. This indicates that the entire length of the continuous counterpoise is not effective in reducing the resistance at the foot of the tower.

8. In territory in which a considerable depth of sand lies on the surface, producing a very high resistance condition, driven rods offer a very effective means of reducing the tower resistance. Tests in which driven rods were compared with the continuous and right-angle counterpoises of about equal effective length, showed about 80 per cent of the current picked up by the driven rods and 20 per cent by the counterpoise wires lying in the high-resistance sand.

9. Magnetic links have proved to be very valuable in obtaining the data on current necessary for the analysis of the counterpoise problem.

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Discussion

F. W. Hartzell (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper presents in a valuable form additional information that might be used in attacking the ever-present problem of improving the performance of high-voltage transmission lines during the lightning seasons.

In reviewing previous literature on the subject of counterpoises, of which this paper deals quite extensively, it is found that only a limited amount of actual field data is available, some of it being of conflicting nature. This study, however, gives an interesting picture of the distribution of surge currents throughout the grounding systems of transmission line towers as determined by actual measurements.

It might be pointed out that the use of driven ground rods is sometimes limited by the depth of soil to rock, although driven rods frequently penetrate to depths of 100 to 150 feet. These deep ground rods compare in length to some of the counterpoises that have been under investigation, so some interesting light might be thrown on the subject if these ground rods were treated as vertical counterpoises as compared to the parallel and radial counterpoises described in this paper.

It also becomes evident that individual towers, or groups of towers, should be considered separately according to ground characteristics when prescribing lightning-resistant features to any transmission line. A careful survey of the conditions to be encountered along a transmission line is therefore imperative before recommendations are made for lightning protection.

K. B. McEachron (General Electric Company, Pittsfield, Mass.): It is not enough to discover the maximum current in lightning strokes, it is also necessary to determine how often currents of any given magnitude may occur, and Lewis and Foust have established curves, as given in figure 1 of the paper, which for several years back have been used in determining the probability of strokes of any given magnitude. These data have been of much value in helping to determine just how far one is justified in proceeding in an effort to protect against direct strokes. These data could be of still more value if the authors could give some data with reference to the number of strokes per mile of line per year which they have experienced. Information is needed with respect to stroke severity in different localities, but this is also a statistical problem which requires much time to secure a satisfactory answer.

In connection with the discussion of polarities of strokes, I would like to point out that, in reference 3 of the paper, I presented data showing that the continuing type of stroke occurred not only to the Empire State Building but was found in many strokes to substantially flat terrain. However, the evidence, thus far available, indicates that upward-stepped leaders occur from structures several hundred feet in height, and occur in a smaller percentage of the strokes as the structure height becomes lower. Thus far, I have no evidence that upward leader strokes occur to transmission lines of the usual height. Continuing strokes with downward initial leaders are common, and undoubtedly occur to transmission and distribution circuits. The continuing stroke is like a d-c arc between cloud and ground with superimposed current peaks. The arc current is often quite small, but may have a magnitude of several hundred amperes with a duration of a fractional part of a second.

It is of importance to recall that direct strokes from negative clouds release a positive bound charge on conductors in the cloud field, which may be of importance on low-voltage circuits when the rate of field change is sufficiently rapid. Only the current peaks will have such an effect, so that in an oscillogram of induced current in such a conductor the continuing part would be absent.

Although the magnetic links frequently show reversal of polarity of current in the inducing conductor, yet there is no evidence that lightning oscillates. It may change polarity, and the available data indicate that it does reverse. However, there is no apparent relation between the magnitude of the initial current and the subsequent reversal. This seems quite reasonable, if the cloud is considered as made up of groups of

Table I. Lightning-Current Measurements, 1938 Data

Area Is for Section In Which Magnetic Links Are Installed

Item No.	Test No.	Tower No.	Main Members			Lesser Members			Totals			Ratios—Total to Main		
			I_m	A_m	D_m	I_l	A_l	D_l	I_t	A_t	D_t	R_i	R_a	R_d
1	1	503	10,400	20.24	514	8,900	6.90	1,290	19,300	27.14	713	1.85	1.34	1.39
2	1	538	37,000	12.16	3,062	49,100	9.30	5,280	86,100	21.46	4,000	2.33	1.76	1.30
3	1	539	40,100	16.72	2,400	66,900	8.94	7,480	107,000	25.66	4,175	2.67	1.53	1.74
*4	1	550	10,000	23.00	436	2,000	7.12	281	12,000	30.12	398	1.20	1.31	0.91
5	1	626	27,500	23.00	1,195	14,500	7.12	2,040	42,000	30.12	1,395	1.53	1.31	1.16
*6	2	540	6,800	16.72	407	0	9.30	0	6,800	26.02	261	1.00	1.56	0.64
7	2	551	15,500	36.44	425	17,800	8.42	2,120	33,300	44.86	955	2.15	1.23	2.25
8	2	625	54,200	16.72	3,240	88,500	9.30	9,500	142,700	26.02	5,490	2.64	1.56	1.69
*9	2	626	9,000	23.00	391	0	7.12	0	9,000	30.12	298	1.00	1.31	0.76
10	3	503	9,600	20.24	474	7,800	6.90	1,130	17,400	27.14	642	1.82	1.34	1.35
*11	3	504	7,700	12.16	633	3,800	6.90	550	11,500	19.06	604	1.50	1.57	0.95
12	3	541	30,000	25.30	1,185	20,100	12.34	1,630	50,100	37.64	1,330	1.67	1.49	1.12
13	3	542	23,900	12.16	1,965	18,000	9.30	1,935	41,900	21.46	1,953	1.75	1.76	1.00
14	3	543	32,300	23.00	1,400	27,300	7.12	3,830	59,600	30.12	1,980	1.85	1.31	1.41
15	3	544	27,300	16.72	1,633	23,100	9.30	2,480	50,400	26.02	1,935	1.85	1.56	1.18
16	3	549	26,400	16.72	1,472	18,900	9.30	2,030	45,300	26.02	1,740	1.72	1.56	1.18
17	3	552	37,100	16.72	2,220	31,800	9.30	3,410	68,900	26.02	2,650	1.85	1.56	1.19
18	3	622	21,600	23.00	940	13,400	7.12	1,885	35,000	30.12	1,163	1.62	1.31	1.24
*19	3	623	3,200	16.72	185	0	9.30	0	3,200	26.02	123	1.00	1.56	0.66
*20	3	624	7,700	16.72	451	1,200	9.30	129	8,900	26.02	342	1.15	1.56	0.76
*21	3	627	4,600	12.16	379	0	8.94	0	4,600	21.10	218	1.00	1.73	0.57
22	4	540	12,200	16.72	732	10,000	9.30	1,075	22,200	26.02	855	1.82	1.56	1.17
23	4	549	29,200	16.72	1,745	16,900	9.30	1,815	46,100	26.02	1,770	1.58	1.56	1.02
24	4	550	30,400	23.00	1,320	18,700	7.12	2,630	49,100	30.12	1,630	1.62	1.31	1.23

* Due to low value of lightning current it is probable that more current than is recorded was present in lesser members. Such being the case, the true ratios would be nearer other ratios recorded.

I_m, I_l—Total current in amperes in main and lesser members, respectively
D_m, D_l—Current density in amperes in main and lesser members, respectively
I_t—Total current in amperes—all members
A_t—Total cross section in square inches—all members
D_t—Current density in amperes—all members
R_i—Ratio: total current to current in main members
R_a—Ratio: total cross section to cross section in main members
R_d—Ratio: total current density to current density in main members
A_m, A_l—Total cross section in square inches in main and lesser members, respectively

charged insulated water drops which may group themselves in unpredictable configurations, both as to size of the group and location. In the laboratory, the discharge across 30 feet of air may oscillate if the series resistance is low enough, but in nature the cloud is not a conductor, and the availability of charge at any given point of discharge is not great, and thus time factors are introduced which are not normally present in the laboratory generator. Since calibration of the magnetic-link installation had to be made in some systematic manner, the use of the laboratory oscillatory wave was the best compromise.

It is indeed fortunate that the measurement of stroke current in the lightning rod at the top of the tower so often checked the summation of currents in the tower legs, since these data have been so widely used. In view of conclusion 2 of the paper, do the authors now feel it desirable to revise slightly downward the curve of figure 1?

R. M. Schahfer (nonmember) and **W. H. Knutz** (both of Northern Indiana Public Service Company, Hammond): The recent report of Messrs. Lewis and Foust provides additional data needed in studying and applying protective measures against lightning. However, by neglecting the current carried by the lesser tower members or by assuming any fixed ratio of this current to the current carried by the main tower members, it is entirely possible that the conclusion as to the total structure current may be in error.

With the foregoing thought in mind, we installed magnetic links on 29 132,000-volt double-circuit steel towers. These installations were made just before the 1938 lightning season. The magnetic links are installed about 15 feet above the ground on all tower members which provide a lightning current path to ground. From 14 to 18 brackets and 28 to 36 magnetic links are used per tower.

During the 1938 lightning season 24 towers provided lightning-current readings. Current values, steel-member areas, current densities, together with pertinent ratios are shown in table I of this discussion.

Summaries of current and current-density ratios taken from the table are:

	Total Current to Current in Main Members	Total Current Density to Current Density in Main Members
Average	1.67	1.16
Maximum	2.67	2.25
Minimum	1.00	0.57

Eliminating the probable low values designated by an asterisk(*) these ratios are:

	Total Current to Current in Main Members	Total Current Density toCurrent Density in Main Members
Average	1.90	1.33
Maximum	2.67	2.25
Minimum	1.53	1.00

From the above figures, the conclusion would be drawn that the ratio of total tower current to current in the main members has a variable ratio which is greater than heretofore suspected, and that the total structure current may therefore be in excess of values previously obtained by applying a general factor to the current of the main members.

Our total tower current values will, if plotted as a cumulative graph, form a curve somewhat above curves shown by Messrs. Lewis and Foust in figure 1.

The lightning investigation work from which these data were obtained is being continued in 1939.

W. W. Lewis and **C. M. Foust:** Doctor McEachron suggests that the authors give some data with reference to the number of strokes per mile of line per year. The authors have such data on the same lines covered by the curves of figure 1, and for the same years. The figures vary widely on account of actual differences in the number of storms and strokes and perhaps partly for the reason that some lines are well covered with magnets throughout their length, thus giving a fair average per mile, while other lines have magnets only for short lengths, probably selected for the known exposure to lightning. Naturally, the strokes per mile are high at these latter classes of installation. For one line, completely equipped with magnetic links, the strokes per mile varied from 0.22 to 0.87 for different years. On another line, only six per cent of its length equipped with links, the strokes per mile varied from 15.4 to 39.2. The data for the other lines fell in between these extremes.

Doctor McEachron asks in view of con-

Arc-Furnace Loads on Long Transmission Lines

T. G. LeCLAIR
MEMBER AIEE

LARGE electric arc furnaces are loads which frequently introduce special technical problems when served from a central-station system. The fluctuations in the load taken by the furnaces are violent, and this characteristic combined with the inherently low power factor of this type of load results in a serious flicker problem. The low power factor also may require power-factor correction, in order to maintain a reasonable voltage level and reduce transmission losses. A further complication is that the arc is an excellent source of higher-harmonic currents and voltages. Where the load or the supply lines are close to telephone circuits, this may necessitate special measures to prevent telephone interference.

Long transmission lines accentuate the problem mentioned. The longer the line, the greater will be the extent of the voltage flicker suffered by other customers on the same transmission system. On a system which operates near its stability limit, the fluctuations in line load caused by the furnaces may result in a lower allowable loading of the system. In extreme cases, load fluctuations may be timed in such a manner as to cause instability, even though the initial loading is well below the normal stability limit of the system.

Large furnace loads on transmission lines are not common. However, this type of load is increasing in importance and means should be found to handle these loads at a reasonable cost. This paper describes the solution of one particularly difficult problem, the details of which should be of value to other companies when considering the supply to proposed intermittent loads.

Nature of Load

The Northwestern Steel and Wire Company at Sterling, Ill., has a wire mill and small steel mill, which are equipped with the usual complement of lighting and power load. Within the past few years, two three-phase electric arc furnaces with a nominal rating of 7,500 kva each have been installed as a source of steel supply. These furnaces melt a wide variety of scrap iron and steel. The furnaces are first charged with cold metal, and when this initial charge is melted, additional cold scrap is added before the metal is poured. Both the initial charge and the back-charging result in violent load changes. Figure 1 shows a photograph of one of these furnaces in use.

Supply System

The load at Sterling is supplied through a bank of 20,000-kva transformers, 132/13.8-kv, which are connected to a 132-kv line from Dixon, Ill. As shown on the diagram, figure 2, the location of Dixon is almost at the center of a 220-mile single-circuit transmission system, connecting the Powerton and Waukegan generating stations. There are two other independent 132-kv transmission circuits from Powerton to Chicago, each of which operates on a single generating unit at Powerton.

Connected to the distribution system fed from Dixon are several hydroelectric generators. However, the average running capacity of all these generators is less than 4,000 kw. There is also a steam generating station at Dixon, but this station is normally operated only for a short time during the peak period of the year. Therefore, practically the entire load distributed from Dixon is dependent upon the 132-kv transmission system for its supply. The Dixon distribution system serves most of the territory of the Illinois Northern Utilities Company, in an area of several thousand square miles.

Figure 1. Pouring metal from a 7,500-kva electric arc furnace

Load Characteristics

The Northwestern Steel and Wire Company load has a half-hour integrated maximum demand of about 17,000 kw at about 85 per cent power factor. The indicated demands occasionally exceed 25,000 kw, or 30,000 kva, most of this demand being the load of the two electric furnaces. The demand of the general power load is about 3,000 kw, not including the wire mill. The lighting load of 75 kw, served at 13,800 volts, was connected to the city distribution system which is supplied from the Dixon 33-kv bus. This was because the voltage fluctuations caused by the electric furnaces were too great for the operation of mercury-vapor lamps.

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The author wishes to thank S. H. Mortensen for the design characteristics of the condenser and R. E. Ayers and C. F. Andrews of the Illinois Northern Utilities Company and A. J. Krupy and E. L. Michelson of the Commonwealth Edison Company for their assistance in the preparation of the paper.

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clusion 2 of the paper if the authors feel it desirable to revise slightly downward the curve of figure 1 for stroke currents. The curve of figure 1 is the result of 734 strokes and was obtained by adding current in all towers affected by a stroke. Some question has been raised as to the validity of this method. The circles and triangles of figure 2 are based on only 80 strokes, but the data were obtained in each case close to the stroke. In the case of the lightning rods, the data involved only one reading for each stroke and in the case of the ground wires involved the addition of two readings. For this reason the authors proposed a compromise upper limit of 150,000 amperes, subject to revision on the accumulation of further data.

Messrs. Schahfer and Knutz present some valuable data as to the distribution of current in the main and lesser members of the tower structure. The average ratio of total current to current in main members of approximately 1.7 to 1.9 agrees with ratios found on other systems. As indicated in the paper this ratio depends to some extent on the symmetry of the counterpoise arrangement with respect to the tower legs, as well as on the actual ratios of cross sections of lesser and main members.

The sudden changes in arc resistance and the frequent short-circuits between electrodes during the process of melting down scrap, result in a violently fluctuating characteristic for the furnace load. The extent of these load fluctuations can best be illustrated by reference to figure 3, a chart from the graphic wattmeter connected to the steel and wire company total load. The section of the chart is a typical example of the period from the initial charging to the pouring of the arc furnaces. As indicated on the chart, the load may frequently go from zero to over 20,000 kw within a period of one to five minutes, or drop off at the same rate. In spite of the violent fluctuations, the average load throughout a 24-hour period is about 60 per cent of the half-hour integrated maximum demand.

Even a close inspection of graphic meter charts does not tell a complete story on the nature of load fluctuations. The load, which apparently swings quickly from zero to 20,000 kw, does not actually make this change in one step, but rather increases by a series of steps. The maximum instantaneous step was determined by oscillograms and found to be about 10,000 kva. These instantaneous changes take place within a period of a hundredth to a tenth of a second. Figure 5 shows one such load swing with a maximum change of 7,000-kva in one cycle. These random fluctuations may come as frequently as six to ten times per second, and are very disturbing to lighting service.

Methods of Voltage Control

Voltage control was first attempted by means of automatic tap changers on the 20,000-kva transformer bank supplying the 13.8-kv bus. The adoption of this scheme was based on an estimated maximum load of 10,000 kw and on incomplete information about the arc-furnace characteristics. The tap changer was partially successful in following the slower load fluctuations of the type indicated on the wattmeter chart. In this service the tap-changer mechanism was subjected to as many as 900 operations per day. This number of operations was reduced by equipping the voltage regulator with a time delay. The tap changer, of course, had practically no effect on the Dixon 132-kv bus, as indicated in the sample chart (figure 4). This voltage chart does not reflect the real extent of the flicker, because it is from an instrument with a very slow response, and shows no record of the more rapid instantaneous voltage changes.

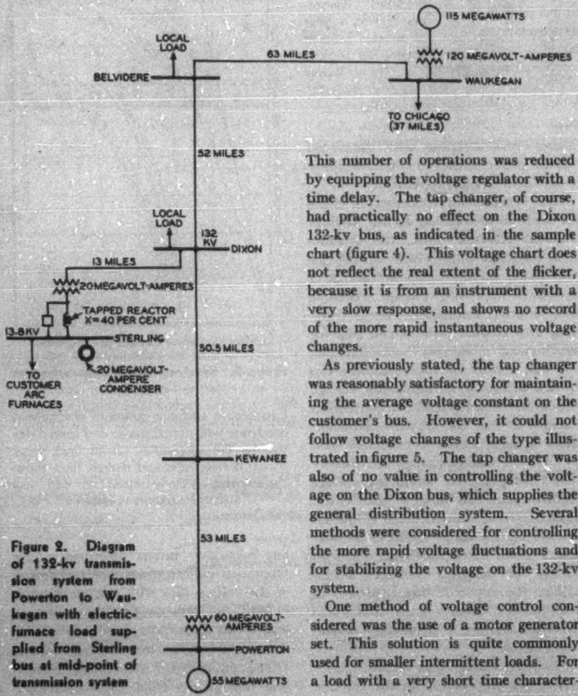


Figure 2. Diagram of 132-kv transmission system from Powerton to Waukegan with electric-furnace load supplied from Sterling bus at mid-point of transmission system

As previously stated, the tap changer was reasonably satisfactory for maintaining the average voltage constant on the customer's bus. However, it could not follow voltage changes of the type illustrated in figure 5. The tap changer was also of no value in controlling the voltage on the Dixon bus, which supplies the general distribution system. Several methods were considered for controlling the more rapid voltage fluctuations and for stabilizing the voltage on the 132-kv system.

One method of voltage control considered was the use of a motor generator set. This solution is quite commonly used for smaller intermittent loads. For a load with a very short time characteristic, such as a welding machine, a motor generator equipped with a flywheel and using an induction motor is very satisfactory, as the flywheel can smooth out the sudden demands. For a furnace with a load factor of 50 per cent or more, the flywheel, of course, is of no benefit. To supply the load under consideration, a motor generator set would need to be at least 20,000 kva in capacity, which would be quite expensive. In addition to its high cost, the voltage regulation on the bus supplying the furnace would be very unsatisfactory. It would certainly be less satisfactory than that obtained with the transformer connected to the high-voltage system.

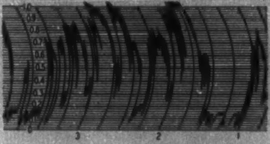


Figure 3. Arc-furnace load curve

Typical kilowatt load cycle of Northwestern Steel and Wire Company load from charging of furnaces to pouring. Full scale = 20,000 kw

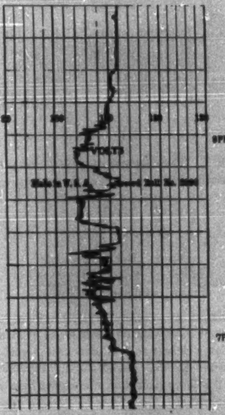


Figure 4. Effect of furnace load on Dixon 132-kv bus voltage—condition before installation of condenser at Sterling

Meter does not respond to momentary voltage changes. Multiplier: 1,200

A series booster, connected as shown in figure 6, was considered as an inexpensive solution. This series booster would have its exciting winding in series with the supply to the furnace load; its booster winding would be between the 13.8-kv bus and the supply to the mill and lighting load. This would improve the voltage for the general service, but would make the furnace regulation poorer and would not improve the voltage for the other Illinois Northern Utilities Company customers.

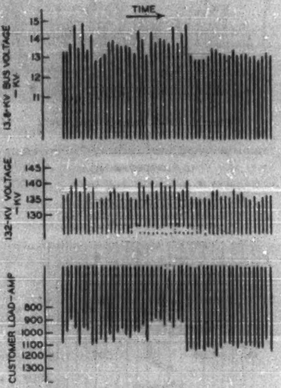


Figure 5. Oscillograms of typical changes in arc-furnace load and effect on bus voltage—no reactor, no condenser

C-phase records taken at Sterling (see figure 2)

Top—13.8-kv bus voltage
Center—132-kv voltage
Bottom—customer load current

To obtain an accurate measurement of the variations in voltage with variations in load a very large deflection was used on the voltage elements of the oscillograph. However, with this large deflection the waves from the various elements would interfere on the record and a special technique was used so that only the top of each oscillograph wave was recorded, the balance being blocked off. This accounts for the unusual appearance of the oscillograms. Space did not permit a more detailed explanation of the technique for obtaining partial oscillograph waves

Another scheme considered was that of series capacitors. These could be connected in the supply transformer secondary leads or in the 132-kv line, or divided in two parts and connected in both places. A series capacitor will neutralize a part of the reactive load due to the line and transformer reactance. The capacitor could be made large enough to maintain practically constant voltage on the furnace bus, but it would not improve regulation on the Dixon 132-kv bus. Since the electric furnaces are subject to frequent short-circuits the series capacitors would require suitable protection against damage from this source. There was also a possibility of telphone interference, as series capacitors will tend to accentuate rather than reduce the effect of harmonic current.

Condenser Installation

The solution which promised to solve the regulation problem for this customer and the general system and the one finally adopted was the installation of a 20,000-kva synchronous condenser connected to the 13,800-volt bus at Sterling which supplies the furnace load. This condenser was designed with somewhat special characteristics to obtain the maximum benefit for this application.

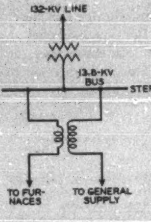


Figure 6. Series booster

Proposed connection of series booster to maintain constant voltage on general supply

The synchronous condenser is useful for such an installation as this in three different respects. First, the synchronous-condenser output improves the system power factor, which in turn is of benefit in improving voltage regulation and reducing transmission losses. Second, with gradual variations in load the synchronous condenser can maintain constant voltage on the customer's bus by the action of an automatic voltage regulator. Third, with instantaneous changes in load the inherent characteristics of the synchronous condenser tend to limit the voltage change and thereby reduce lamp flicker, before the voltage regulator has had time to take effect. This is accomplished because of the instantaneous change in output of the condenser which takes place with a sudden change in terminal voltage. Fortunately, the action of the condenser in maintaining constant system voltage also tends to maintain a satisfactory system power factor.

Since the voltage dips on the 132-kv system are proportional to the reactive kilovolt-amperes supplied from this system to the furnace load, it was possible to decrease the flicker on this system by transferring the greater part of the sudden changes in reactive load to the synchronous condenser. This was accomplished by the insertion of a special buffer reactor between the 13.8-kv secondary winding of the 132-kv transformer bank and the bus to which the synchronous condenser and the furnace load are connected. This reactor was equipped with taps so that the amount of reactance could be varied after installation in order to use the value which would transfer the maximum reactive kilovolt-ampere load fluctuation to the synchronous condenser without causing trouble from surging of the condenser. A short-circuiting breaker was installed to take the reactor out of service at any time that the condenser or furnaces are not operating.

Table I shows a comparison of the calculated benefits obtained by the various corrective means described. This tabulation indicates not only the extent of the voltage dips with a given change in load, but also the resultant reactive load which the system must supply with the corrective device in service.

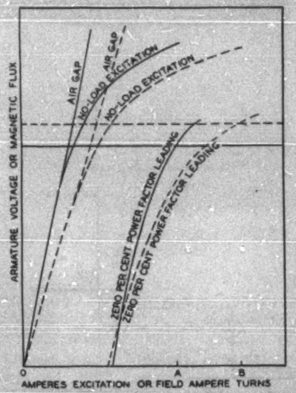


Figure 7. Synchronous-condenser saturation curves

Comparative saturation curves of synchronous condenser with normal design and special design for this application. Solid lines show normal characteristic and dashed lines show characteristic with increased air gap and higher flux density in armature

The actual flicker without any correction was greater than that calculated.

Table I. Methods of Correcting Voltage Flicker

Calculated Effect of Various Means Proposed to Reduce the Voltage Flicker Caused by Instantaneous Load Changes on Electric Arc Furnaces. Balanced Three-Phase Load Assumed Although Most Instantaneous Swings Are Unbalanced

Corrective Equipment	Voltage Fluctuation in Per Cent for Instantaneous Swing of 10,000 Kva at 75 Per Cent Power Factor: Sterling 13.8 Kv	Voltage Fluctuation in Per Cent for Instantaneous Swing of 10,000 Kva at 75 Per Cent Power Factor: Sterling 132 Kv	132-Kv Reactive Load on System for Steady Customer Load of 20,000 Kw at 80 Per Cent Power Factor (Kilovolt-Amperes)
No corrective means	8.2	3.6	19,300
10,000-kva condenser, no reactor	5.3	2.5	7,900
20,000-kva condenser, no reactor	4.2	2.0	2,100 leading
10,000-kva condenser, 40 per cent reactor (20,000 base)	10.1	1.8	16,300
20,000-kva condenser, 40 per cent reactor	6.7	1.2	6,300
20,000-kva condenser, 20 per cent reactor	5.8	1.5	2,100
20,000-kva motor generator set	9.9		
10 per cent series capacitor (20,000 base)	4.6	3.5	15,400
Booster transformer	0* 9.0**	3.7	20,000

* General power supply. ** Supply to arc furnaces.

These differences are due primarily to the fact that the increment loads involved in the sudden changes in load are at very low power factors. Also, the sudden changes are usually single-phase changes, while the calculations are based on a three-phase load. By observation of lamp flicker, which is the final measure, the improvement due to the condenser is appreciably better than expected from calculations.

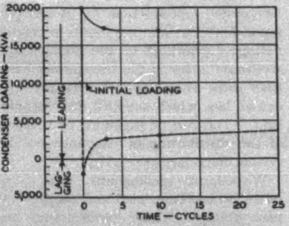


Figure 8. Condenser performance curve

Calculated performance of 20,000-kva synchronous condenser, excited for 10,000 kva at normal voltage. The solid line shows the change in output when the terminal voltage is suddenly dropped ten per cent. The dashed line shows the change when the terminal voltage is suddenly increased ten per cent. Both curves are based on constant exciter voltage

Condenser Design

The synchronous condenser was designed especially to meet the demands of this rapidly fluctuating load. It is necessary for the condenser to produce a large change in reactive kilovolt-amperes for a given variation in voltage. This may be accomplished by designing the machine so that the effective saturated values of subtransient and transient reactances are low. For an application of this kind, the amount of saturation that is effective is not the usual "rated voltage" value of reactance (that is, the reactance effective on short circuit from rated voltage and no load) but is a considerably lower value because the internal voltage is appreciably above normal and consequently the saturation is greater than at normal voltage. The manufacturer's tests and calculations for this machine indicate that the "rated voltage" saturation factor for transient reactance is about 0.91, while the saturation factor effective at full load during small voltage changes is about 0.67. The low reactances and saturation factors in this case were produced by working the machine at fairly high magnetic densities, increasing the saturation of the various parts, and increasing the air gap.

Figure 7 shows characteristic curves for a condenser of normal design compared with one having these special features. In this figure, the solid lines show the no-load and full-load saturation curves for a normal condenser design and the dashed lines are the characteristics for this condenser. It should be noted that the increased gap requires greater excitation for a given terminal voltage or armature flux density. Furthermore, the machine works at an increased armature flux density and this results in further increased excitation for the full-load point. *OA* represents the field excitation for full load and normal voltage on a standard machine and *OB* is the excitation on this machine for the same condition. Obviously, this increased field will result in better voltage regulation.

The result of these changes was a machine with an unsaturated subtransient reactance of 22 per cent, an unsaturated transient reactance of 30 per cent, and a synchronous reactance of 124 per cent. These subtransient and transient values were checked by an oscillograph test after installation.

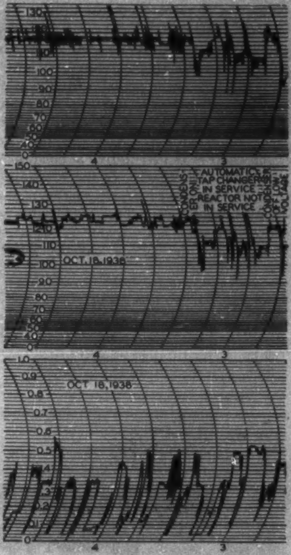


Figure 9. Effect of synchronous condenser on voltage regulation

Sections of three simultaneous graphic charts at Sterling to show benefit of condenser. No condenser and no reactor from 2:45 to 3:25 p.m. Condenser in service with 20 per cent reactor after 3:25 p.m.

Top—13.8-kv bus voltage × 120
Center—Transformer secondary voltage × 120
Bottom—Customer kilowatt demand × 39,000

Figure 8 was drawn to illustrate the calculated performance of this synchronous condenser for a sudden change in terminal voltage due to a sudden change in the customer's reactive load. This curve shows the variation in condenser loading with respect to time, on the assumption that a ten per cent change in terminal voltage is applied while the condenser is excited for 10,000 reactive kva loading at normal voltage. The instantaneous change in output for a ten per cent change in voltage is at least 10,000 kva.

Results Obtained

The improvement in regulation is well illustrated in the charts of figure 9. These charts are a short section taken under a typical load condition. The charts illustrate a brief period during which the condenser was out of service and the reactor short-circuited; followed immediately by a period with the condenser and reactor in service. The comparison is therefore under directly similar load conditions. The beginning of each chart is representative of conditions before the installation of the condenser, and the remainder representative of the present conditions. The graphic charts, of course, are not capable of showing either load or voltage changes which take place in a small fraction of a second. Such sudden changes are shown in the oscillogram of figure 10, which was also taken with the condenser in operation, and with a 20 per cent reactance between the condenser bus and the 13.8-kv secondary of the main transformer bank. The complete oscillograms contain records of 12 elements, showing the currents and voltages on all phases. The records from the other elements were less satisfactory and were not reproduced because they do not add to the clarity of the picture. The bottom record on the oscillogram is good for comparative purposes only. It was obtained from a single-phase watt element in the oscillograph. This element records the product of current in one phase, and a voltage which is 30 degrees out of phase with this current. Its record serves to show the nature of the power swings of the condenser.

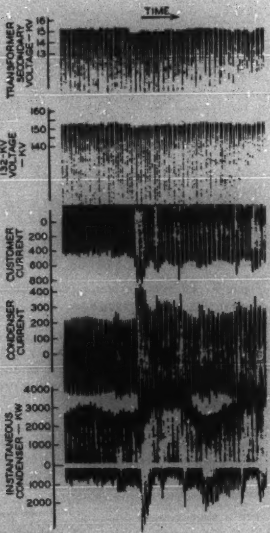


Figure 10. Oscillogram of condenser operation with 20 per cent reactor—effect of sudden change in load current on bus voltage and condenser loading

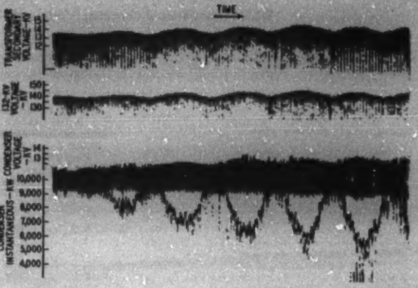
Reading from top to bottom, records are from elements at following points at Sterling (see figure 2)

1—Transformer secondary voltage
2—132-kv system voltage
3—Amperes in supply to customer
4—Amperes in synchronous-condenser leads
5—Instantaneous watts, product of condenser phase current and voltage between phases

An attempt was made to get the maximum benefit of the condenser in smoothing out the system voltage by using a 40 per cent reactance between the transformer and the condenser bus. This increased reactance was effective in further reducing the voltage flicker caused by the load changes, but resulted in hunting of the condenser with respect to the remainder of the system. A series of rapid load changes may be timed in such a manner as to have a cumulative effect if the reactance between the condenser and the system is too high. In a few instances, with the 40 per cent reactor, this cumulative effect was sufficient to cause the condenser to pull out of step and trip off the system.

The oscillogram of figure 11 illustrates a typical case of condenser hunting. This shows the steadily increasing swing in the loading of the condenser. The single-phase watt element of the oscillograph is again connected in such a way as to measure the product of the amperes in one phase of the condenser and a voltage 30 degrees out of phase with this current. Only the top half of this record is reproduced, because the bottom half on the original film was not clear enough for printed reproduction. It is interesting to note that hunting results in greater voltage fluctuations on the primary and secondary sides of the transformer bank than those which took place on the condenser bus, which supplies the customer load. This can be explained by a study of the distribution of reactance on the system diagram.

Figure 11. Oscillogram of condenser operation with 40 per cent reactor—voltage oscillations on high-voltage system caused by condenser swinging



Reading from top to bottom, records are from oscillograph elements at following points at Sterling (see figure 2):

1—Transformer secondary voltage
2—132-kv system voltage
3—Sterling bus voltage
4—Instantaneous watts, product of condenser phase current and voltage between phases

Wave-form oscillograms were also taken on customer load and on the system voltage. These oscillograms are not reproduced here, but they show that the original wave form was bad, and that the synchronous condenser was effective in improving this wave form to the extent that it should cause no telephone interference. It is also interesting to note that the lighting load was reconnected to the main 13.8-kv supply on October 27, 1938, after the condenser was placed in service and that satisfactory operation of the sensitive mercury-vapor lamps is now obtained from that source. It had been impossible to operate these lamps satisfactorily from the 13.8-kv source before the installation of the condenser.

Cost and Benefits

The installation of this synchronous condenser was not particularly cheap. However, the total cost was under $10 per kilowatt of integrated maximum demand. This cost is considered reasonable when it makes possible the supply to such a load without noticeable disturbance to other customers. In addition to its primary function of correcting the voltage flicker, the condenser is desirable for the following reasons:

1. Excessive maintenance of the transformer tap-changers is eliminated.
2. The load power factor is corrected to approximately unity.
3. Transmission losses are reduced.
4. The stability limit of the 132-kv system is raised.
5. The Dixon load can be carried from Powerton or Waukegan alone, without running Dixon steam generators, if one 132-kv line is out.

Conclusion

The installation of a suitable synchronous condenser proved to be an excellent remedy for voltage flicker caused by severely fluctuating load on a 132-kv overhead transmission line. The cost of the remedy at $10 per kilowatt of arc-furnace load was more than justified because it resulted in better system operation, in addition to making the supply to this class of business possible, without disturbance to other customers. This type of load, while it has wide instantaneous swings, has a very satisfactory load factor throughout the 24-hour period. Such loads should be encouraged by power companies.

Discussion

W. B. Wallis (nonmember; Pittsburgh Lectromelt Furnace Corporation, Pittsburgh, Pa.): It is noted with interest that the furnace load in question consisting of two high-power rolling-mill-type electric furnaces rated at six tons each are connected to the center of a 220-mile 132,000-volt 60-cycle transmission line, along with various power and lighting loads and the lighting voltage flicker has been neutralized.

As the author states, arc-melting-furnace load of this character is one of increasing importance as a matter of large revenue to the power companies and is interesting in that it shows an economical solution of voltage fluctuation due to the results of high peak loads on a very long transmission system serving many small lighting communities.

These furnaces while rated at 7,500 kva each, are habitually overloaded and are frequently run with a load of 16,000 kva each. While they were rated at 6 tons they regularly make heats ranging from 22 to 24 tons. The installation of the furnaces in this plant is a distinct commercial success, making high-grade steel ingots from ordinary country scrap at a price well below the established market for billets of similar size. Indeed the demand for the steel has been such that the furnaces have necessarily been enormously overloaded to get out the necessary production.

The power bills for this customer's 18,000 to 19,000-kw load average $40,000 to $44,000 per month which constitutes a very attractive source of revenue to the power system.

The more than 100 per cent furnace overload naturally imposed conditions on the power company that were not originally considered in the power service calculations. The installation of the 20,000-kva synchronous condenser with its voltage regulator is an ingenious and interesting installation, and the fact that this together with the switchgear and reactors has resulted in an entirely satisfactory solution of the difficulties originally encountered is a subject for congratulation to the engineers who conceived and carried out the work. This is especially true in view of the fact that the cost was very moderate and the remedy so complete and all under very unusual conditions caused by the extra long length of transmission line.

The commercial success of this plant will no doubt result in the installation of a number of other similar arc-furnace rolling-mill plants and the solution will unquestionably be of interest to prospective suppliers of such large load and revenue.

It might also be noted that if a separate line had been run from Dixon to Sterling to carry the lighting load that perhaps no very great objection would have been found to the furnace load, even though much larger than originally considered due to the unusually large overloads imposed.

The power-factor conditions are quite satisfactory for the load in question, as is also the load factor, all of which will no doubt encourage other installations of a similar character.

Sterling Beckwith (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): Mr. LeClair's paper is very interesting and shows the logical solution of a difficult problem. As a machine designer I would like to emphasize the utilization of saturation in the condenser to improve its characteristics for this application. When saturation of transient reactance is ordinarily thought of, the saturated value, or what has more recently been called the rated voltage value, is taken as the effective value for a dead short circuit from rated voltage. This rated voltage value has developed for use in studies of system short-circuit currents. However, for a case such as the one discussed by Mr. LeClair, the value of transient reactance that should be used to calculate the transient voltage dips is a value which is more saturated than the rated-voltage value used in studies of short-circuit currents. The reason is that the machine is initially operating at half to full load, so that the internal voltage of the machine is greater than unity.

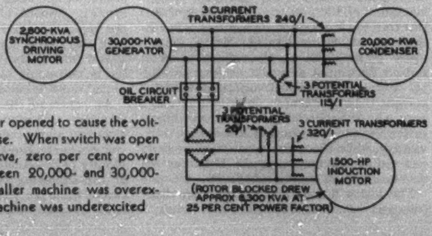
Mr. LeClair gives the rated voltage saturation factor as 0.91, and the saturation factor effective at full load during small voltage changes as 0.67, which shows the pronounced gain from saturation. It also shows that ordinary characteristics of an alternator, such as short-circuit ratio, synchronous impedance, or even rated-current or rated-voltage values of transient reactance will not give a direct measure of the stability of a machine for service such as that described by Mr. LeClair.

It would be interesting to know the per unit resistance of the system plus transformers plus reactors, since this resistance is one of the important factors contributing to the hunting that was observed when 40 per cent reactance was used. It might also be interesting to know the relation between the peak load at the Sterling bus and the steady-state power limit at this bus, both with and without the 20 per cent and the 40 per cent reactors.

Figure 1 accompanying this discussion shows the connections used to test the voltage dip of a large condenser under sudden load changes starting from an initial condition of full load. Curves taken from oscillograms made during such a test are shown in figure 2 of this discussion.

Curve *A* is the per unit voltage, and curve *B* is the per unit current. It should be noted that both contain a subtransient portion lasting about 0.05 second and a transient portion which lasts beyond the end of the oscillogram. The third curve of this figure shows the ratio of the change in voltage to the change in current, or in other words the reactance effective during the voltage change (since $R = E/I$). The subtransient and transient effects are even more pronounced in this curve than in the others. For purposes of comparison, the test values of saturated and unsaturated reactances are shown by arrows. These test values were obtained by sudden short-circuit from no load, and the saturated values correspond to short-circuit at an initial voltage exactly equal to the internal voltage (= voltage behind Potier reactance) existing in curve *A* prior to the sudden change in load. The change from a subtransient to a transient to a saturated synchronous reactance is so striking and clear that further comment is unnecessary. Also the agreement with the actual numerical value of the reactances as deter-

Figure 1. System on which tests were made


2,800-KVA SYNCHRONOUS DRIVING MOTOR
30,000-KVA GENERATOR
3 CURRENT TRANSFORMERS 240/1
20,000-KVA CONDENSER
OIL CIRCUIT BREAKER
3 POTENTIAL TRANSFORMERS 115/1
3 POTENTIAL TRANSFORMERS 20/1
3 CURRENT TRANSFORMERS 320/1
1500-HP INDUCTION MOTOR
(ROTOR BLOCKED DREW APPROX 6,300 KVA AT 25 PER CENT POWER FACTOR)

Oil switch was closed or opened to cause the voltage to suddenly fall or rise. When switch was open approximately 10,000 kva, zero per cent power factor, circulated between 20,000- and 30,000-kva machines. The smaller machine was overexcited, and the larger machine was underexcited

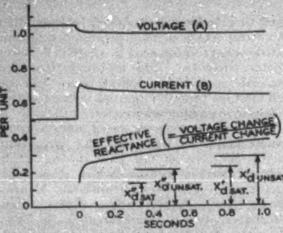


Figure 2. Results of voltage-dip tests

mined by usual methods and here shown by the arrows is very good.

B. M. Jones and **V. E. Hill** (nonmember; both of Duquesne Light Company, Pittsburgh, Pa.): We electric utility people had just as well recognize that large electric furnaces are part of our business—some of our bread and butter—and we must make up our minds to learn how to handle them so that we can live more peacefully.

As we all know, the difficulty in serving large electric arc furnaces is that the large load swings at low power factor may cause objectionable voltage fluctuations to other customers in the same area, and expensive corrective measures may have to be taken.

Obviously, the correct engineering approach is to determine in advance what the power swing and resultant flicker will be, and to provide a suitable system connection and corrective equipment, if necessary, to keep the flicker within tolerable limits. However, this is not always so easy, for in some cases it is very difficult, if not impossible, to find out just what the electric performance of the furnace will be, due, we believe, to the fact that the manufacturers of the furnace cannot determine themselves in advance the exact electrical performance and characteristics of the complete furnace installation.

In a recent installation, certain system changes and switching during the meltdown period were necessary after the furnace installation was made, to prevent the flicker from being reflected into parts of our system where it would be objectionable. This furnace caused much larger swings than had been anticipated as a result of our experience with other furnaces and from what we had been led to believe would exist as shown by literature on this subject. Tests of these swings, both by recording meters and oscillograph, indicated frequent single-phase swings of 300 amperes, with a maximum of 400 amperes, which is equivalent to 8,500 kva or, for such currents in all three phases, is equal to 14,000 kva, three phase. The 22-kv supply voltage at this station fluctuated as much as four to six per cent, with occasional variations as high as eight to ten per cent. These fluctuations were also experienced, of course, at other customers and distribution stations in this vicinity, while there was a two to three per cent voltage fluctuation on the 22-kv bus of the transmission supply substation.

The higher fluctuations on the 4-kv distribution substations within the vicinity of this customer gave rise to a few flickering-light complaints, in the order of four or five a month, which is remarkably small in view of the magnitude of the swings and the thousands of customers exposed. However, another objection to these voltage fluctuations from a company standpoint was the excessive number of regulator operations caused thereby. Counters placed on some of the regulators in nearby distribution substations indicated that, whereas only 4 to 6 operations per hour were experienced when the furnace was not running, from 200 to 300 operations per hour occurred when the furnace was on, that is, during the melting-down period. A maximum of 1,000 operations per hour was noted on one occasion; this based on only a 15-minute period.

Other distribution substations fed from this same transmission substation, but not by the same lines feeding the furnace, also experienced an increase in regulator operations, but not nearly so many. Checks on one regulator indicated 50 to 150 operations per hour with the furnace on, whereas the normal number with the furnace off was generally less than 30 per hour. Obviously, such an increase in regulator operations would enormously increase the maintenance on them.

Even though the number of complaints was relatively few, it was evident that something had to be done. One or more new 22-kv lines into this area were considered, but needless to say, the cost would have been entirely unjustified, as would also have been the installation of a synchronous condenser. Since the voltage fluctuations back at the transmission substation were tolerable, the obvious solution would have been a separate line to serve this furnace only, but this, too, would have been very expensive. However, the same objective was accomplished by another means. Since this customer was the first load fed from his direct line, supervisory equipment was installed so that the breaker at his station could be opened by remote control from the transmission substation, thus leaving him fed radially on this one line. This move was accomplished at a cost of only $4,400.

It is true that the opening of this breaker reduces the normal capacity to this area, or rather the number of feeds to this district, since the customer's load utilizes a good portion of the normal line capacity. The breaker is only open a very small percentage of the time, because the furnace is not operated continuously—generally, only 15 or 20 heats a month and not more than one a day. Furthermore, the breaker can be instantly closed and the line returned to service in this district during any emergency periods, such as arises during lightning storms or for failure of other lines in this district. Should this customer ever operate the furnace continuously, one or two shifts each day, or if the load in this district should materially increase, then another line or two would be needed and could then probably be justified.

As concerns the enormous number of regulator operations, the breaker opening described above reduces the severity of the voltage fluctuations of the stations in the vicinity of this customer to that experienced at the supplying transmission substation, two to three per cent. Even these fluctuations were objectionable and so our substations department developed a time-delay device to attach to the regulators. These devices permit of the normal functioning of the regulators to adjust for bus-voltage variations, load changes, and the like, but prevent the regulators from attempting to follow the rapid fluctuation caused by the furnace.

We have conducted negotiations from time to time on other large furnace installations, up to 10,000 and 12,000 kva, in which we considered service at 66 kv, and also the use of corrective equipment, such as referred to in Mr. LeClair's paper.

It has also been our experience that the size of the power swings caused by electric arc furnaces is subject to appreciable control by the furnace operator, through the choice of the voltage tap used, the length of arc, and general skill developed from experience.

The electric utilities should, we believe, protect themselves from these objectionable voltage flickers when they interfere with the service to other customers, probably by charging against the furnace job the expense of major system rearrangement, new lines, or corrective equipment, all of which may be necessary to hold fluctuations within tolerable limits. In this connection, it is highly desirable and has been our experience, that it is to the mutual benefit of all concerned for the utility, customer, and furnace manufacturer to co-operate and work together in the initial design and in the operation of the furnace.

L. W. Clark (The Detroit Edison Company, Detroit, Mich.): Most power companies at some time or another have encountered similar service problems involving electric arc furnaces as that described and thoroughly analyzed by Mr. LeClair. There are always several ways in which the load can be handled and it is usually a matter of economics as to which method is finally selected. This installation is of particular interest in that the analysis shows a synchronous-condenser installation to be the most satisfactory solution.

In general, our experience has indicated that a synchronous condenser would not improve a flicker condition sufficiently to warrant the installation from the standpoint of flicker elimination alone. There usually had to be some other need for the condenser, such as general voltage control of that part of the system before its installation could be justified. No doubt these other advantages were deciding factors in the selection of a condenser for this particular installation. The original flicker condition was not beyond the range of correction afforded by the condenser operation as shown by the reduction of flicker from 3.6 per cent to 1.5 per cent.

Where the voltage fluctuation or flicker must be corrected to a greater extent, for instance, in the range of eight to one, it is doubtful if a condenser installation would prove economical as compared with a motor generator set. As an example, an installation of two small single-phase arc furnaces at Vassar, Mich., as shown in figure 1, illustrates both the motor-generator-set and the synchronous-condenser method of solution. The smaller furnace rated 250 kw, 360 kva, has maximum instantaneous load fluctuations of about 500 kva, and was served from the 4,800-volt synchronous-condenser bus. Calculated voltage fluctuations on the 4,800-volt bus due to the furnace operation are 4.8 per cent with the condenser shut down, and 3.2 per cent with the condenser operating. This is a standard design of synchronous condenser with a subtransient reactance of 24 per cent and a synchronous reactance of 164 per cent. Taking into account that there is no buffer reactor to force the condenser to absorb more of the reactive kilovolt-amperes, the reduction in voltage fluctuation is of about the same order as that of the Sterling installation described by Mr. LeClair. As long as there was no general lighting load served from this bus, the voltage conditions were satisfactory as the flicker in the main 24-kv bus was not objectionable under normal operating conditions with both 24-kv lines in service. However, it later became necessary to transfer lighting load to the 4,800-volt bus and then, even with the condenser in operation, the flicker proved annoying and caused the furnace load to be transferred to a motor generator set on the other bus.

The motor generator set consisting of a 1,750-kva 80-per-cent-power-factor synchronous motor driving a 600-kw generator was originally installed to handle a 600-kw 875-kva single-phase furnace which has maximum load fluctuations of about 1,300 kva, or about $2^{1}/_{3}$ times those of the smaller furnace, and would cause a flicker of about 12.5 per cent on the bus if corrective measures were not applied.

It is obvious that a condenser installation of sufficient size to reduce the 12.5 per cent flicker to something in the order of 1.5 per cent to 2 per cent, would not only be expensive but it would also be of much greater size than could ever be used or needed for general voltage control of that portion of the transmission system. However, the motor generator set, which has a slightly lower installed cost than the 3,000-kva condenser on the opposite bus, does reduce the flicker to less than 2 per cent, and tests show the set to be about $5^{1}/_{2}$ times as effective in flicker reduction as the more costly 3,000-kva condenser.

The paper mentions the fact that for a load with a very short-time characteristic, such as a resistance-welding machine, a motor generator equipped with a flywheel and using an induction motor is satisfactory but that for a furnace load with the greater load factor, the flywheel would be of no benefit. It is our opinion that we might go a step farther and state that a flywheel set need only be considered on loads with a high fluctuating kilowatt demand, such as rolling-mill loads, but that for either welder loads or arc-furnace loads in which the predominating source of trouble is the rapid change in reactive rather than kilowatt load, the flywheel type of set is not generally needed. A straight, synchronous set will do a very good job where the desired reduction in magnitude of voltage fluctuation is not greater than five or ten to one.

It is interesting to note that the furnaces apparently operated for some time prior to the condenser installation and, consequently, imposed annoying flickers on the 132-kv system which would affect the local lighting load at Dixon. While these flickers, no doubt, only occasionally reached the maximum value of 3.6 per cent, there probably were frequent flickers of sufficient magnitude to affect the lighting, and we would be interested to know if many customer complaints were received during this period.

H. P. St. Clair (American Gas and Electric Service Corporation, New York, N. Y.): Mr. LeClair has described in his paper a truly difficult problem of voltage regulation. Problems of this type are encountered now and then, probably with increasing frequency as time goes on, and Mr. LeClair's paper describing the solution in his particular case will undoubtedly be of value to many other engineers faced with the necessity of working out voltage regulation problems of a similar nature in the future.

I should like to describe briefly a somewhat similar problem which was encountered on the system with which I am associated and the solution which was used to take care of it.

At St. Joseph, Mich., the Auto Specialties Manufacturing Company, operators of a foundry and a machine shop, had installed several electric furnaces, one of which was nominally rated at 5,000 kva, but actually used at a much higher rating. This customer's load, including the large furnace, was supplied over a separate double-circuit 27-kv line from the Riverside substation which in turn was supplied by a 37,500-kva transformer bank, stepping down from a double-circuit 132-kv line 38 miles in length from the Twin Branch generating plant and substation. This transformer bank was equipped with a tertiary winding to which was connected a 15,000-kva transformer condenser arranged to regulate voltage on the 27-kv bus. In addition to the Auto Specialties Manufacturing Company's circuits, this substation also supplied a 27,000-volt system, including the cities of Benton Harbor and St. Joseph.

This arrangement was expected to be entirely adequate to handle the fluctuating industrial load in this area without difficulty and as a matter of fact for several years voltage fluctuations did not become critical. However, as the Auto Specialties Manufacturing Company's operations increased, accompanied by more intensive use of the large electric furnace, severe voltage fluctuations were encountered in the adjacent area supplied from the Riverside substation 27-kv bus. Observations at this time indicated that the electric-furnace load was imposing on the system swings as high as 15,000 kva, the increments in these swings being largely reactive kilovolt-amperes. Visible fluctuations of four to five volts were observed on the Riverside substation 27-kv bus, with nearly two volts visible on the 132-kv bus. Obviously, the synchronous condenser offered very little help since the power swings were entirely too fast to be followed by any condenser and regulation scheme.

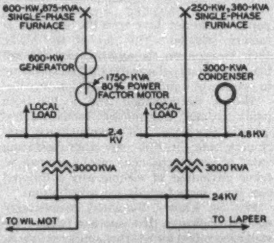


Figure 3. Vasser substation

Since the power and reactive kilovolt-ampere swings were actually producing voltage fluctuations in excess of five volts, it seemed almost obvious at the outset if we were to continue to serve this customer's load, a different arrangement would be necessary. Calculations indicated that a separate transformer, stepping down from 132 kv to 27 kv to supply this customer's lines alone would reduce fluctuations on the 132-kv bus to less than two volts; and with the synchronous condenser operating on the 27-kv bus from which the remainder of the system load was supplied, the regulation on that bus would be still better than on the 132-kv bus. Consequently, a new 15,000-kva transformer bank was installed and the heavy swings of this particular customer were as you might say "quarantined" in this manner, so that service could be supplied to the remaining area without intolerable voltage fluctuations.

Comparing this setup with that described by Mr. LeClair and illustrated in figure 2, of his paper, it might be pointed out that Mr. LeClair was fortunate in having the local distribution load at Dixon supplied directly from the 132-kv system, so that he was not faced with the problem of supplying any other load except the customer's load from the low side of the transformer bank at Sterling. In effect, the solution which we adopted was to change over to a system more nearly like that shown in figure 2 with the local distribution system separated from the furnace load.

G. B. Schneeberger (Cleveland Electric Illuminating Company, Cleveland, Ohio): Mr. LeClair's paper is a very interesting and detailed description of the problems arising in connection with the operation of an arc furnace especially when the furnace is used to melt cold metal.

This type of load is inherently very erratic and demands careful consideration before it is connected to any system, or overworked regulators and flicker complaints will result.

The great improvement in refractories has enabled operators to push their furnaces far beyond their original capacities. This has resulted in an insistent demand for more power. The nominal rating of a furnace is on a par with the nominal rating of welders. It is not uncommon to encounter furnaces which have doubled their original transformer capacity. In one case on our system, a furnace whose original transformer equipment was 375 kva is now operating with 1,200 kva.

This emphasizes the necessity of allowing a liberal margin when this type of load is under consideration.

It is interesting to note the application of the synchronous condenser in handling the unusual *lagging* reactive kilovolt-amperes. With the advent of the modern strip mill, due to the liberal use of synchronous motors, troublesome *leading* reactive kilovolt-amperes was encountered. The synchronous condenser was also used to advantage to correct this condition. Most of the more modern mills are now provided with regulators which control the reactive condition at its source.

E. S. Fields (Cincinnati Gas and Electric Company, Cincinnati, Ohio): The AIEE paper entitled "Arc-Furnace Loads on Long Transmission Lines" has been read with a great deal of interest. The various solutions considered and the one finally adopted along with the special characteristics of the synchronous condenser are to my mind distinct contributions in the field of electric-system engineering.

From my knowledge of the rate per kilowatt-hour to make electric melting attractive and the large amount of system investment to serve this customer, it is quite apparent that the customer must have a very high annual load factor to make the service mutually attractive financially. I imagine that some of the five items of benefits listed in the paper were substantial, particularly item 5 which represents a release of transmission capacity, and that the evaluation of these items went a long way toward justification of the expenditure.

While we have no such problem, as the one you describe, on our system, we have one that has some of the same elements in which you may be interested. It is a large steel rolling mill located about 40 miles from our power stations and served from 66-kv lines which also supply the usual types of system load at the same locations. The variations in the steel-mill load are as much as 25,000 kw in a short interval of time and, since a great part of this load consists of synchronous-motor-driven motor generator sets, automatic field control has been applied to these motors to vary the field strength with the load. The application has given entire satisfaction.

T. G. LeClair: Mr. Wallis has offered some valuable additional information on the characteristics of these particular electric furnaces. The characteristic which he mentions of severe overloading has been found to apply to not only this furnace installation, but also to a large number of others. The statement made by Mr. Wallis that there would have been no objection to the furnace load if the lighting load had been on a separate line is not quite correct. He has evidently been misinformed, because the lighting load at Sterling was connected through separate lines to the Dixon 33-kv bus during the time of greatest disturbance. The original disturbance was, in fact, noticeable throughout the territory served from the Dixon substation.

The comments by Mr. Beckwith are an important contribution from the designer's viewpoint. They show that with a reasonable foreknowledge of the problem much can be done to adjust a synchronous condenser to be more beneficial in handling of disturbances caused by fluctuating loads. In answer to his question, the resistance of the system to the condenser bus is 1.3 per cent on a 20,000-kva base with either a 20 per cent or a 40 per cent reactor. The system's reactance to the condenser bus is 42 per cent and 62 per cent with the 20 per cent and 40 per cent reactors, respectively.

Mr. Beckwith's question concerning the steady-state power limit at the Sterling bus is quite pertinent. The calculated pull-out limit is 32,000 kw with 40 per cent buffer reactor, 47,000 kw with 20 per cent buffer reactor, and 90,000 kw with no buffer reactor.

The discussions by Mr. Clark and by Messrs. Jones and Hill show radically different solutions for the intermittent-load problem. In both of these cases the synchronous condenser described in the paper would not have been the most satisfactory solution. The size of the loads, their location on the system, and the annual load factor all apparently had a bearing in coming to a different conclusion.

Messrs. Schneeberger and Fields both point out that intermittent loads are frequently associated with synchronous machinery and in this case the regulating value of the synchronous machinery can be taken advantage of without any additional cost for a special condenser installation.

Mr. St. Clair's discussion, surprisingly enough, indicates that another furnace installation called for a rather similar solution, even though the problem was approached from the opposite angle of having the condenser already in service and installing the line to make the condenser more useful.

The variety of solutions for the problem of intermittent loads which have been mentioned in the discussions accentuate the note on which I would like to close this discussion. That is that every intermittent load appears to have certain special characteristics and is applied to a power supply system with different limitations. Therefore, there appears to be no general answer to the question of what is the best method of supplying these intermittent loads.

Intermittent equipment is a growing essential tool to the industrial user. Unquestionably, the power companies should stand ready to supply these intermittent loads. The greatest benefit can be obtained by whole-hearted co-operation between the equipment designer, the equipment user, and the utility company *before* the order is placed. Adjustments in the design of the equipment or the proposed methods of operating may frequently result in lessening the possible disturbances to the point that the intermittent equipment can be installed without expensive special facilities to be paid for either in the rate or as a separate charge.

Mercury-Arc Rectifiers in the Coal-Mining Industry

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ALTHOUGH mercury-arc rectifiers at present constitute only a small part of the total installed capacity of a-c to d-c conversion equipment in coal mines, in the past three years 21 rectifiers of a total rated capacity of 8,100 kw have been placed in service in 15 coal mines. Considered alone, this would not be important, but, since these rectifiers exceed in number of units and capacity other forms of new conversion equipment installed in the same period, a definite trend toward the use of rectifiers is indicated. It is the purpose of this paper to discuss the causes for this trend and to record, in so far as it is possible, the results which have been obtained with rectifiers in mining service.

Two developments are responsible for this interest in rectifiers. First is the increasingly rapid change from hand loading methods to concentrated operation with machine loading. If the large investments in mining machines required for mechanical mining are to be profitable, these investments must be backed up by a power supply which is efficient and which is adequate to supply an uninterrupted flow of power to the mining machines.

The second is the development of the ignitron rectifier which can be used advantageously for 275-volt service and for higher voltages. Prior to 1936, only the tank-type or multianode rectifier was available for which the principal field of application is at 500 volts and higher. Since, by far, most of the coal is produced by 275-volt mines, it was not until the ignitron was developed for large capacity that rectifiers really became available to the coal-mining industry as a whole.

The beginning of the modern trend to rectifiers is marked by the installation of a 600-kw 600-volt tank-type rectifier by Pittsburgh Coal Company in August 1936. This was discussed by A. Lee

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D. E. Renshaw is engineer in the mining section, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

Barrett at an AIEE District meeting in Akron, Ohio, in October 1937. Briefly, Mr. Barrett stated that this unit is saving 26.5 per cent of its cost per year due to increased efficiency. The rectifier serves a mine working three shifts per day and 600 shifts or more per year.

The year 1937 saw two installations which produced several "firsts". In February, the Union Collieries Company of Pittsburgh placed a 300-kw 275-volt d-c, 2,300-volt three-phase 60-cycle ignitron equipment in an underground station, approximately two miles from the shaft bottom. This equipment may be briefly described as consisting of an automatic control, an Inerteen-filled transformer, a six-tube ignitron rectifier, and a water-to-air heat exchanger for cooling the rectifier. Because of clearance conditions in the main haulage way, the maximum height of any part is 55 inches, and the maximum width 62 inches.

In October 1937, the Weirton Coal Company installed three portable ignitron rectifiers to be the first mine:

1. To be served solely by rectifiers.
2. To use portable rectifiers.
3. To make all their substations portable.
4. To place all their conversion equipment underground.

This equipment is essentially the same as the Union Collieries Company equipment, except that it is mounted on three trucks. The transformer and a-c switchgear are on one truck, the rectifier and its auxiliaries and the heat exchanger on the second, and the d-c switchgear on the third.

Installations in 1938 followed essentially the pattern established in 1937.

In 1939, two noteworthy advances have been made. Three 600-volt ignitron rectifiers have been placed in service, all of these being of the nonportable type in surface substations.

The second innovation, which may be the more important, is the use of permanently evacuated, permanently sealed ignitrons. One 200-kw 275-volt equipment and three 300-kw 275-volt equipments have been placed in service at the date of reading this paper.

The principal advantages of the rectifier are, of course, high momentary overload capacity and high efficiency, and these advantages have, in many cases, combined to make the rectifier the most economical conversion unit.

In a typical example of concentrated mechanical mining, a substation is located adjacent to the load center and serves several operating units, each unit consisting of a cutter, a loader, and a locomotive. These machines are powered by intermittent or short-time-rated motors, which have continuous ratings averaging about one-third of the nominal ratings. A substation to serve this group of machines should have sufficient continuous capacity to furnish all the power that the machines can safely absorb on an all-day basis. The substation should also have sufficient momentary capacity to carry over the normal peak load without damage to the conversion unit or interruption of the supply of d-c power. Experience has indicated that this peak frequently approaches the sum of the combined rated currents of all the machines served.

For example, a substation serving six units, having a total of approximately 960 horsepower, should be capable of carrying 900 kw momentarily and 300 kw continuous. Interpreted in terms of conversion units, this means a 300-kw rectifier will operate with fewer outages than a 400-kw rotating machine, since the 300-kw rectifier unit can carry more load momentarily than the 400-kw rotating unit.

At 275 volts, there will be but a minor difference in the cost of the substation including building, foundation, and installation, whether a rotating machine or a rectifier is used, and at 600 volts, the rectifier will definitely be less expensive. Any saving in operating cost obtained by the use of the rectifier will be a net gain.

Basing a power-cost comparison on 275-volt equipment only, the losses of a 300-kw rectifier will average 5 kilowatt-hours per hour less than the losses of a rotary converter over the usual load cycle, and will average 20 kilowatt-hours per hour less than the losses of a synchronous motor generator.

In a mine in which power must be kept on the trolley continuously to operate pumps and other auxiliary services, the annual saving in comparison with a rotary converter will be roughly 40,000 kilowatt-hours, which at a rate of one cent per kilowatt-hour amounts to $400. The comparison with a motor generator shows a saving of 160,000 kilowatt-hours or $1,600 per year.

The savings in 600-volt service are more impressive, since, on the higher voltage, the rectifier efficiency is increased approximately three per cent, while the efficiency of 275- and 600-volt rotating machines are approximately the same.

As of this date, six ignitron rectifiers have each had more than one year of service, the maximum in this group being 32 months and the average 22 months. Nine additional ignitron rectifiers have had less than one year each, the maximum in this group being 9 months and the average 6 months.

In the six equipments of the first group, there are 36 ignitron tanks, 36 excitation thyratrons, 36 ignitor crystals, 6 vacuum pumping systems, and 6 cooling systems. These are the parts which were really on trial in the earlier installations.

No delays have been caused by leaks in the vacuum system and only one shutdown has been caused by any type of failure of the vacuum system. This was caused by improper operation, contrary to instructions. When the mercury was replenished in the mercury-vapor pump and attention was called to the proper method of operation, the trouble was cured permanently.

Four ignitor crystals have been replaced in the field. Two of these were defective crystals which failed very shortly after installation. Two others, which were replaced after approximately one year of service, were not returned to the factory for inspection, so the exact condition of these crystals at the time of reported failure is unknown.

The minimum life of firing tubes has been slightly more than 12 months. Some firing tubes have been in service two years, but the average is at least 15 to 18 months.

Only four of the ignitron tanks have been opened, all for replacement of firing crystals. No other internal parts have been replaced or repaired.

There have been no recent failures of the cooling system. In several of the earlier installations, it was necessary to modify the circulating-water pumps to prevent loss of water and to improve bearings. These modifications have, apparently, eliminated the trouble. Under present conditions, the amount of water required to replenish losses is negligible.

It seems evident, therefore, that ignitron rectifiers, at the present stage of their development, are highly reliable, comparing favorably with rotating machines. The cost of maintenance is extremely low except for the cost of replacing firing tubes, which, judging from existing experience, should average less than $150 per year. In comparison with rotating conversion machines, the over-all cost of maintenance of rectifiers will usually be higher, but the difference should not exceed $100 per year on the smaller ratings. On the larger ratings, maintenance costs will be more nearly equal.

In an analysis of power conditions at a mine, due consideration should be given to the effect of added equipment on the power factor of the system. A rectifier, with its transformer, operates at an average power factor of approximately 93 per cent. A rotary converter is usually adjusted to operate at unity power factor at the average load, but as an all-day average, will usually operate at something less than unity, lagging. Standard mining motor generator sets are capable of producing a large corrective effect to compensate for inductive equipment on the system. A power-factor analysis can be made only with a full knowledge of local conditions and any further attempt to make a definite statement on this subject would be useless.

As a result of three years' experience in the application and operation of rectifiers in coal-mining service, combined with many years of experience in other industries, we can summarize as follows:

1. Rectifiers are at least equal to other types of conversion equipment in reliability.

2. For equipments of equal service capacity for mining conditions, the installed cost of a rectifier will be approximately equal to or less than the cost of rotating conversion equipment.

3. The operating cost of a rectifier, including maintenance and cost of power, will be less than the operating cost of rotating machines, except where power-factor correction is of considerable importance.

4. Where additional power-factor corrective equipment is necessary, a motor generator, or a rectifier with capacitors may be the preferred type of equipment.

5. An analysis of local conditions, taking into account required service capacity, hours of service per year, power cost rates, and power factor, is necessary to establish which type of conversion equipment is most advantageous.

Discussion

F. L. Kaestle (nonmember; General Electric Company, Schenectady, N. Y.): This paper on "Mercury Arc Rectifiers in the Coal-Mining Industry" is indeed very timely, and is of interest to the entire electrical field, as well as the coal-mining industry.

Mr. Renshaw has very well pointed out that the rapidly expanding mechanization of coal mining has led to an increased interest in rectifiers, because their characteristics are so well adapted to the type of load encountered in this service.

The increased average power load of the mechanized mine has required the installation of more conversion equipment. The fact that the load center is highly concentrated in a relatively small working area, and that this area shifts quite rapidly as the coal is worked out, has increased the need for readily movable underground substation equipment. The development of the ignitron has made it feasible to build portable rectifiers to meet this important shifting-load condition.

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The important characteristic of the mine load which makes rectifier equipment particularly adaptable, is that the load fluctuates widely. The cutting and loading machines and the haulage locomotives produce alternately high peaks and light loads during the working shift. Also the trolley usually must be kept energized continuously between shifts, with only the very light load of maintenance and inspection work, and for pumps and lights. The high percentage of very light load periods emphasizes the most outstanding advantage of the rectifier, that of low no-load losses which account for its high efficiency at light loads. As an example, the no-load loss of a 300-kw ignitron rectifier equipment is approximately 4 kw, that of a rotary equipment 12 kw, and of a motor generator 26 kw.

Mr. Renshaw has demonstrated the savings effected by these lower rectifier-equipment losses for a mechanized mine operating 600 shifts per year. This number of working shifts is quite high as an average, and even here the power savings are substantial. For mines working 200 to 300 shifts per year, and there are a large number which do so, the savings are even greater.

High overload capacity must be considered in the design of any equipment for mining conversion, but the means by which it is accomplished are decidedly in favor of the rectifier substation. The continuous rating and hence, to some extent, the physical size of any conversion equipment to carry a high-peak, low-average load can be kept to a minimum if that equipment has a high momentary peak capacity. Even with a rectifier substation, it must be remembered that the rectifier unit itself must still be selected primarily on the basis of the maximum load to be carried. However, the transformers, cooling equipment, and switchgear may be selected primarily on the basis of the continuous rating, and these parts make up a large part of the total equipment. Hence, the high overload capacity can be obtained in the rectifier equipment without the large increase in copper and iron required to obtain it in rotating machines.

One of the principal considerations in the development of the ignitron was to improve the efficiency for application in the 250–275 volt field. In order to reduce the arc drop it was necessary to place each anode immediately above and very close to the cathode. This was accomplished by enclosing each anode with its individual mercury-pool cathode in a separate vacuum tank or tube. These single-anode units are assembled as a group to form a rectifier unit. With such an arrangement, it is necessary for practical design reasons, to initiate the arc each half cycle by means of an ignitor, rather than maintain it continuously, as is done in the multianode rectifier. Thus the real operating difference in the two types lies mainly in the ignition systems used, for the basic rectifying principles of the two are the same.

The mechanical construction of the ignitron has served to simplify the rectifier. The low height and light weight of the individual units now make it practical to build portable and underground equipments. The mercury pool does not have to be insulated from the vacuum tank, in the ignitron, and so a sturdier design results. This simplification of the ignitron, together with its improved efficiency, has expanded the use of the rectifier into many new low-voltage fields, including this important coal-mining industry.

Another progressive step, mentioned by Mr. Renshaw, which has permitted the further simplification of the ignitron, is the use of permanently evacuated, sealed tubes. With these tubes, no vacuum-pumping system and no auxiliary equipment to indicate or control vacuum is required. The reliability of the sealed tube, from the standpoint of continuity of service, has been well established by tube equipments for welding machines, synchronous-condenser exciters, and building lighting, as well as by three coal-mining equipments which are now in service.

The lower installed cost of the sealed-tube equipment and the lower maintenance, due to the elimination of vacuum equipment, must be balanced economically against tube renewal costs, and this resolves itself into a question of tube life. Since the tube is constructed of corrosion-resisting materials, its life is dependent on vacuum and ignitor life. Many tubes in welding service have been operating for over four years and are still in service. Although it is too early to know what tube life is in this service, on the basis of very encouraging results so far, it is confidently expected that the tubes will last from three to five years or longer. When their life has been demonstrated, the sealed-tube rectifier will be the most attractive equipment available in ratings of 300 kw and less, where the cost of the pumping equipment is a relatively large item.

Rectifiers have been very successfully applied in 500-volt services for many years and now, with the development of the ignitron, the many operating advantages of the rectifier are available in the 250-volt field. The low light-load losses of the rectifier are its strongest point. The ignitron is simple, sturdy, and reliable. It is easily made portable, and is well suited to underground operation. Rectifier substations are easily provided with high momentary overload capacities. Because of the absence of rotating machines, the rectifier substation equipment is practically immune to damage from lightning.

The sealed-tube ignitron not only retains all the other advantages of the pumped equipment, but also offers a means of reducing the installed cost and maintenance for the ratings to which it is suited.

With the present increase in coal production, the mercury-arc rectifier should play an ever more important role in the reduction of mine operating costs.

D. E. Renshaw: I am very glad to have heard Mr. Kaestle's contribution on this subject, particularly his discussion regarding the permanently sealed ignitrons.

It is quite evident, because of increasing mechanization of mines, additional conversion equipment will be required. It is also undoubtedly true that there are many conversion units still in service which, because of age and long use, are expensive to maintain and are wasteful of power. For this added and replacement conversion equipment, the ignitron rectifier will, in many cases, provide a better source of d-c power at a saving in over-all cost.

New Type of D-C to A-C Vibrator Inverter

O. KILTIE
ASSOCIATE AIEE

VARIOUS SYSTEMS have been devised to operate from direct current to supply alternating current at specified frequencies for specific loads. However, there have been the usual limitations to prevent individual extensive application of each type.

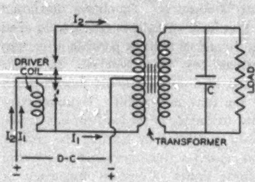
Motor generator and dynamotor type machines do not fill all the requirements for universal needs. Special provisions must be made to obtain good frequency regulation and high efficiency.

Grid-controlled vacuum-tube inverters have been successful for many purposes, but for loads in the lower ranges, the high cost and low efficiency are objectionable. With supply voltages in the order of 6 or 12 volts direct current the normal arc or plate drop of the tube itself may be as much as the supply voltage leaving nothing for the load. Practically instantaneous attainment of rated frequency during the starting period would be an advantage for this type.

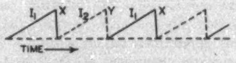
Vibrator-type inverters have been extensively used during the last few years, especially in the automobile-radio field where load requirements have not been severe and long contact life has not been a fundamental requirement. Their use has increased considerably of late for various purposes such as for electric clocks, small motors, musical instruments, and various electrical control equipment.

It is the purpose of this paper to present to the industry a form of vibrator inverter for the conversion of direct current to alternating current, which has been accepted to have many improvements and advantages over other forms and types of inverters including the conventional vibrator system shown in figure 1*A*. It is not the intention in the following paragraphs to criticize the present conventional vibrator system, but merely to establish a basis of comparison for what has been considered to be a distinct improvement.

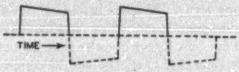
The circuits used for nearly all vibrator applications in the past have been basically similar to the circuit of figure 1*A*. Modifications are found in various methods for starting and driving the vibrating reed. By referring to figure 1*B* it is evident that during the "closed" period of each cycle the line current comprised of I_1 and I_2 continues to increase until the circuit is opened at points X and Y by the vibrator contacts. Alternate current impulses occur for respective paths of the battery circuit as shown. The general wave shape of the output voltage is acceptable for many applications, however, the fact that the contacts are compelled to break an increasing current at a maximum value proves disastrous for appreciable load currents. The resulting spark produces considerable heat and transfer of contact materials. Extremely high frequency components occur at the "make" and "break" from the resulting spark, to cause objectionable radio disturbance. Suppressors must always be added to avoid such interference. If for any reason, action of the vibratory reed should stop while either set of contacts is closed, the battery or supply-line current would increase to high values only limited by the d-c resistance of the circuit and the protective device, if used. Adequate capacitors are required and are connected either across the output load or across each set of vibrator contacts with correct associated resistance to decrease sparking to a minimum.



A—Conventional vibrator circuit



B—Contact current in circuit of *A*



C—Output voltage from circuit of *A*

Figure 1

New Type Vibrator Circuit

A vibrator-type inverter satisfies the requirements for simplicity, low cost, high efficiency, and very few moving parts. Its serious enemy, as described previously, is the damaging spark during the opening of the contacts. Frequency regulation is excellent. Small size is a desirable feature. To develop a new inverter the decision was made in favor of the vibrator type with the fundamental objective being to reduce the circuit current to zero before allowing the vibrator contacts to open. It was apparent if such a condition could be made possible, the contact life should be very long and result in a most reliable type of inverter. Such was the process of development and the following paragraphs explain the theory followed.

If a reactor L, capacitor C, and resistor R, be connected in series across a source of direct voltage through a switch as shown by the solid lines of figure 2*A*, an approximate sine-wave half-cycle current can be obtained. The extent of continued oscillation will depend upon the damping of the circuit resistance R. Assuming the capacitor to be completely discharged when switch 1 is closed, the current will increase to a maximum and reduce to zero as the capacitor becomes fully charged. If switch 1 is opened at point X in the current wave I_3 of figure 2*B*, there will be no spark at the switch since the current at that moment is zero and the capacitor will be charged to a potential equal to line voltage. When closing switch 2, capacitor C will begin to discharge and force a current I_4 to duplicate the charging current I_3 in all respects except that it will flow in the reverse direction. Again no spark will occur at switch 2 if it is opened at point Y. The capacitor and reactor have definite reactance constants, therefore currents I_3 and I_4 individually acquire the same definite resonant frequency. Amplitude of the two waves will be equal providing the

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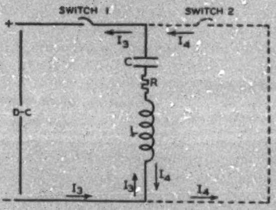

Figure 2A. Circuit to obtain one cycle of charge and discharge alternating current

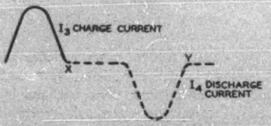

Figure 2B. Charge and discharge current waves

resistances of the two paths are equal Alternating current can thus be obtained if the switching operations occur alternately and uniformly. For switching purposes the conventional type vibrator may be substituted most ideally if the respective contacts be made to open the circuit corresponding to the points X and Y in the current waves of figure 2*B*. Resistance R represents the load which should be of the proper value to obtain correct damping of the load current. By omitting the reactor the current will increase at a very rapid rate equivalent to a high frequency and the current will decrease, in that same half cycle, at a very slow rate which would be equivalent to a low frequency, so that wave shapes of this type would be of little value for power purposes. A new inverter circuit as shown in figure 3 is thus made possible. The circuit is similar to the charge-discharge circuit of figure 2*A* except that the output transformer has been added to couple the load resistance with the exciting source. Certain type loads may be supplied directly from a single low-reactance transformer excluding the use of a tuning reactor L. Such is possible only where a lagging-power-factor load provides the necessary inductive reactance to reflect in series with the capacitor C to provide the correct resonant frequency. In other applications as described later, for neon-tube purposes, the tuning reactance may be combined in a single output transformer by providing sufficient leakage reactance between the two windings. General applications usually require the inductance be provided as a separate unit and that the output transformer be of the conventional low-reactance type with minimum leakage reactance between windings.

Since the load current can be made to reduce to zero, an ideal time in the cycle is available immediately after each half cycle for the contacts to open the circuit to switch for the alternate half cycle. During the moment of no current flow the contacts may be opened with absolutely no spark or damage whatsoever, and the primary objective of the development has been achieved.

It is essential to choose circuit constants such that the load current will increase to a maximum and decrease to zero during a single closed period of one pair of contacts. To obtain maximum output load watts, it is desirable to open the circuit as soon as possible after zero current has been reached. In other words, load current should flow as much of the time as possible. Uniform resonant frequency of the load current can be easily controlled by holding the reactor and capacitor reactance constants within definite limits. Constant frequency of the vibrator is made available at little extra expense, since the vibratory reed has that inherent characteristic. Since many applications require a specific frequency the frequency of the vibrator must be chosen to agree. The resonant frequency of the load current must then be designed to a value approximately ten per cent higher in order that the contact current is at zero before the contacts open. Absence of current at opening of contacts makes it impossible for destructive sparking to occur. The contacts are required to withstand only mechanical wear and resistance heating.

Line current from the d-c supply consists of charging current only, so the effect is a series of d-c pulsations in exactly the same shape and amplitude as the positive half cycles of the alternating current in the oscillogram of figure 4*A*. Since line current from the battery flows only half the time, a d-c ammeter measurement will be slightly less than half the a-c measurement of current in the capacitor leg.

Perhaps the features and advantages of the new inverter are best proved and most evident by referring to the oscillogram of figure 4*A*, which shows the alternating current I_5 of circuit per figure 3. It consists of both the charge and discharge half-cycle currents in alternate arrangement. The oscillograms of figures 4*A*, 4*B*, and 4*C* were taken with an inverter connected according to figure 3 and connected to a 96-per-cent lagging-power-factor load. The following test data will show actual relative measurements and are true for the conditions of the various oscillograms.

E_1—Line volts, direct current	110
I_6—D-c line current	1.4 amperes
E_2—Load volts, alternating current	107 (figure 4*B*)
I_8—Load current, alternating	1.18 amperes
Load watts	122
I_5—Capacitor current	3.68 amperes (figure 4*A*)
Capacitor C	124 microfarads
Reactor L	0.0241 henry

Phase relation and comparison may be made of a-c output volts E_2 with capacitor current I_5 by referring to figure 4*C*. It is apparent that wave shapes are quite satisfactory for most purposes. From close inspection of the oscillograms it can be seen exactly where the vibrator contacts close and open.

There are various methods to start the vibrator in motion, the most simple of which is to connect the driver coil of the vibrator directly across the load contacts on the battery side as shown in figure 3. When closing the d-c supply switch an initial current will flow through transformer primary, capacitor, and driver coil. Even though this current is small it establishes a directional flux to start operation of the vibratory reed in the usual manner. During the initial swing of the reed the corresponding contacts close and allow for the first half cycle of charging component of the load current. Simultaneously, the driver coil becomes short-circuited to remove the magnetic pull upon the reed, which allows the reed to swing back across the center position to close the other pair of vibrator

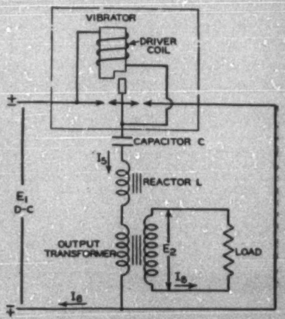

Figure 3. New type of d-c to a-c vibrator inverter

contacts. During this period the discharge half cycle occurs. Since the short circuit has been removed from the driver coil, the reed is again pulled in the opposite direction to repeat the continuous cycle. The natural frequency of the reed determines the a-c frequency.

This development has created new requirements for capacitors. The volt-ampere output depends directly upon the value of capacity, so for appreciable loads from low direct voltages especially, the number of microfarads reaches a large figure. The a-c electrolytic type capacitor has proved most suitable for such requirements, chiefly because of its small size and low cost. Power factor and losses should be low. Inductive effects in the capacitor and in the lead connections between the terminals and foil create a surge impedance which may reject a capacitor satisfactory in other respects.

The a-c output can be synchronized with an isolated a-c power source by providing the driver coil of the vibrator with a suitable low voltage obtained directly from the synchronized voltage.

An important feature is that which allows the d-c supply current to reduce to zero if the vibratory reed should stop in any position of its normal operating range while the d-c line switch remains closed. Since the capacitor becomes charged and remains charged, no current can flow. The battery or d-c circuit is opened if the discharge contacts remain closed.

From this development it appears the new inverter will be ideal for many specific applications. At the present time much hope is held for it to enable the use of standard 110-volt a-c television receivers in d-c districts, its chief advantage being in constant frequency and lack of disturbing interference. Radio receivers on police cars are required to operate almost continuously. The plate-supply direct voltage can be supplied from a vibrator of this type with considerable assurance of reliable operation.

Application for Neon Signs

The original purpose of this development was to find a new cheap method to supply suitable high voltage for neon signs on automotive vehicles. For such purposes the inverter must operate on any condition from short-circuit to open-circuit loads. A circuit the same as figure 3 may be used except that the output transformer requires sufficient leakage reactance between windings to hold the tuning reactance within reasonably close limits to prevent contact sparking for the various load conditions. An inverter suitable for most neon sign applications for 6.0-volt d-c service supplies 7,500 volts open circuit which is suitable for ten feet of nine-millimeter-diameter red neon tubing. Battery drain is almost constant at 3.5 amperes for any load condition. An electrolytic capacitor of 6,500 microfarads is necessary for this purpose. Wave shapes are so favorable that smoothness and appearance of the tubing while operating is comparable with that from any commercial a-c supply.

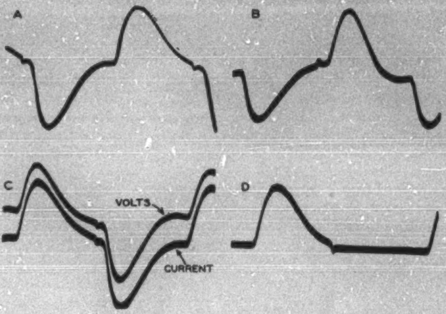

Figure 4. Oscillograms of inverter current and voltage of figure 3

A—Alternating current I_s

B—Output alternating voltage E_2

C—Relation of current I_s and output voltage E_2

D—D-c line current I_s

Application to Fluorescent Mazda Lighting on Railway Cars

The problem of supplying suitable a-c power to a group of the new fluorescent Mazda lamps from 32- or 64-volt batteries seems to be very well satisfied by this type of inverter. An efficiency of 75 per cent is maintained throughout the normal variation of the battery voltage. Best operation is obtained with a load power factor near unity although variations, either leading or lagging, may be tolerated.

Several railway cars[1,2] have now been equipped with fluorescent lights and vibrator inverters of this type.[3] With no changes necessary to the batteries and generators of existing equipment in service, it is possible to increase the average illumination on the reading level from approximately three foot-candles with tungsten lamps to approximately eleven foot-candles with fluorescent lamps. The losses of the inverter and lamp ballast auxiliaries have been considered in this comparison.

The constant frequency of the inverter power supply becomes a valuable feature since the lamp auxiliary is of the reactance type. Even with battery voltage variations from 28 to 43 volts the vibrator has been made to hold almost constant amplitude of the vibratory reed and to provide almost a constant frequency of 60 cycles. Therefore, with a constant frequency of the power supply the voltage may be allowed to vary to some extent without causing damage to the auxiliaries or to the fluorescent lamps. It then makes it possible to eliminate the lamp regulator which has been necessary for tungsten lamps on railway cars.

Present rated outputs from 32-volt sources have not yet much exceeded 250 watts, so for loads of 1,000 and 1,500 watts, panels of four sections and six sections respectively are used. Each 250-watt section is connected to a group of from 12 to 16 lamps as required. Progress of development for the actual installations in service has been conservative on the safe side. It is evident that the present output can be increased, although it is believed that standardization of ratings may establish the 250-watt size as close to the preferred rating.

This entire development is so new and recent that further research and development will naturally bring many improvements.

References

1. *Railway Electrical Engineer*, March 1939, page 41.

2. *Railway Electrical Engineer*, October 1938, page 212.

3. *General Electric Review*, January 1939, page 32.

Discussion

B. D. Bedford (General Electric Company, Schenectady, N. Y.): Mr. Kiltie's paper is of great value in that it introduces a vibrating-switch inverter employing an

Variable-Voltage Equipment for Rotary Drilling Rigs

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THE PURPOSE of this paper is to tell why variable-voltage equipment should be considered for oil-well drilling rigs, to explain the operating characteristics of a typical equipment, and to compare the over-all performance with that of an equivalent mechanical drive.

The variable-voltage drilling rig is associated with internal-combustion engines. Steam turbines have been suggested for prime movers, but have not yet been used. A-c motors can be used to advantage, and have been proposed, but to our knowledge never actually used.

Let us review briefly some of the disadvantages of steam equipment as compared to the internal-combustion engine. A safe and adequate water supply for steam boilers is sometimes difficult and expensive to obtain. The water supply for cooling internal-combustion engines is, comparatively, a small amount. Steam boilers are bulky and heavy units. There are usually not less than three per rig, each weighing approximately 30,000 pounds or as much as the two large-capacity Diesel engines used on a comparable heavy-duty mechanical drilling rig. The boilers are also much bulkier than the engines. Inspection and maintenance is a more serious item with boilers than with internal combustion engines. Steam offers the possibility of boiler explosion which represents a hazard to life and property. The open fire under the boiler presents a real hazard in case of a gassing well. When gas is not available, as in the case of "wildcatting," the transportation of other fuels in quantities required for steam generation entails considerable expense. Assuming liquid fuel, the quantity required for internal combustion engines will be considerably less than that for boilers.

The development of reliable high-speed internal-combustion engines of adequate rating, brought a real competitor of steam as a source of power for drilling oil wells. While steam rigs still predominate by a wide margin, the internal-combustion-engine drive is being used in increasing numbers. Improvements in steam equipment such as the use of higher steam pressures (500 to 800 pounds per square inch) and superheat, will undoubtedly be matched by improvements in internal-combustion-engine equipments such as the development of larger-capacity and higher-speed engines. Variable-voltage transmission with its many advantages will help in further increasing the use of the internal combustion engine.

Difficulties Encountered With a Mechanical Transmission

As the internal-combustion engine cannot pick up any appreciable load from standstill, a clutch must be provided. This clutch must slip while the load is being accelerated to a speed corresponding to the engine speed, and at the same time transmit a torque as required by the load. This means clutch wear. The manipulation of the clutch results in engine stalling or near stalling, sudden changes in engine speed, and shock loads on the engine and mechanical transmission. Such factors mean high maintenance of the engines and of the entire mechanical transmission. Also, it is practically impossible to pick up a heavy load with the smoothness desirable when doing a fishing job. The mechanical transmission is inflexible as to arrangement because the engines must be close to the draw works and the mud pump.

What Is Meant by a Variable-Voltage Equipment

Before considering the advantages of a variable-voltage drilling equipment let us outline briefly what such an equipment consists of. Such an equipment consists of d-c generators a portion of whose fields are supplied by separate excitation, and whose voltages are varied and reversed by controlling their fields; motors with shunt fields separately excited, so their direction of rotation and speed are changed as the polarity and value of the generator terminal voltages are changed; constant voltage exciters to provide for separate excitation of generators and motors, and a source of auxiliary power;

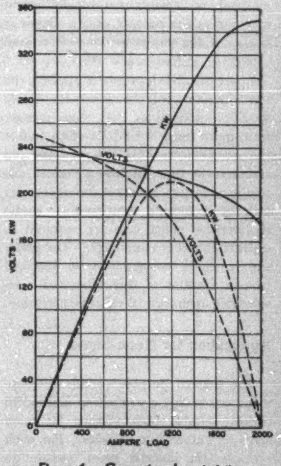

Figure 1. Generator characteristics

Solid curves—With separately excited and self-excited shunt fields

Dashed curves—With separately excited and self-excited shunt fields and also series differential field

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improved method of commutation. This improvement makes this type inverter capable of handling much larger loads, with reduced contact wear. The new design greatly expands the number of applications in which vibrating-switch inverters may be used.

Practically all small independent electric power systems, such as those found on automobiles, railway cars, airplanes, small boats, and isolated farms, obtain continuity of service by storing electric energy in batteries. At present, the manufacturer must design and the consumer must buy many special lamps, motors, and other appliances for operation on these low-voltage d-c circuits. The vibrating-switch inverter will have an important place in the electrical field, for it is now practical to convert the battery energy of these independent systems to alternating-current so that standard a-c apparatus may be used.

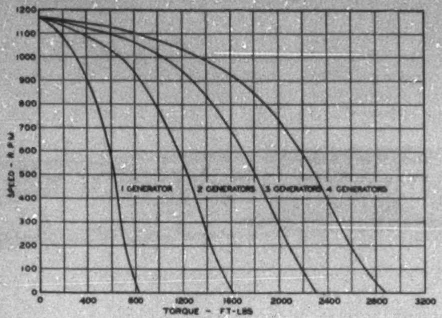


Figure 3 (left). Motor speed-torque characteristic with generators in parallel

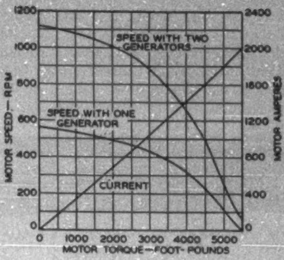


Figure 4 (right). Motor speed-torque characteristic with generators in series

a control equipment which gives full control of power application to all main motors for drilling or hoisting operations from the driller's position.

The generators as here used are more than simply variable-voltage machines. In addition, they have a shape of voltage curve which droops to zero at some predetermined current value. Such a curve establishes two desirable limits:

1. The peak generator output and consequently the peak demand on the engine can be limited to the maximum desirable output of the engine. Once established the protection to the engine is automatic, regardless of pump, table, or hook load. The electric motors can even be stalled without danger of stalling or even slowing down the engines.

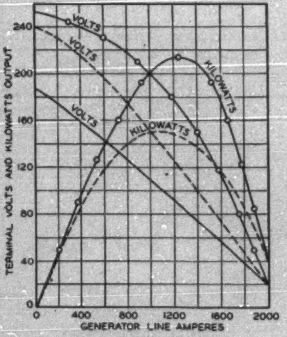


Figure 2. Generator voltage and kilowatt characteristics

Circles—Three-field generator with series differential field

Solid curve—Three-field generator with self-excitation eliminated

Dashed curves—Two-field generator with series differential field

2. The maximum current that can be circulated when a motor is stalled can be limited to that value which is considered a safe maximum from the standpoint of both the mechanical and electrical equipment. Such a limitation is particularly desirable for mud-pump and table drives.

How these generator characteristics are obtained will be discussed in detail later in this paper.

Separate shunt excitation of the motors provides two principal advantages. (1) The motors are reversed as the applied armature voltage is reversed. Motor reversing is therefore accomplished by means of only small generator-field-circuit contactors. With series or compound fields, large main-circuit contactors would be required. (2) The use of a separately excited field assures that the torque exerted by the motor will be always proportional to the armature current. This is important where high momentary torques are required, as when pulling on a stuck pipe.

The exciters, or auxiliary generators, are constant-voltage machines. They are suitable as exciters for the fields of the generators and motors, and as a source of auxiliary power for lighting, shale-shaker motor, raw-water motor, engine-cooling-tower circulating-pump motor, and any other auxiliary load.

The control is relatively simple. Since motor speed control and motor reversing are accomplished by the manipulation of only small generator field currents, the necessary circuits can be handled directly on a drum controller. It is therefore easy and economical to provide a large number of speed points. Means are provided for transferring available generator capacity to any motor circuit in case of emergency. Derrick-floor control units, such as the master controllers, drill-hoist selector switch, and emergency stop push button, are of the oil-immersed or explosion-proof type. Master controllers provide for 20 points of speed control and are operated from the driller's position.

Generator Characteristics

Let us consider how the generator characteristics referred to above are obtained.

Figure 1 illustrates the voltage and kilowatt characteristics of a generator equipped with only shunt fields, including both self- and separate excitation, and the same generator equipped with self- and separately excited shunt fields and also differential series fields. It is apparent that the machine with only shunt fields has a power demand characteristic which would be unsuited to internal-combustion-engine drive on a drilling rig. The demand on the engine with such a generator characteristic increases approximately in proportion to the current or motor torque required. It would require careful attention of the operator to avoid overloading and stalling of the engine. Such close attention is not desirable.

The curves for the generator arranged with shunt fields and differential series fields indicate that the maximum demand on the engine can be limited. The demand on the engine actually decreases as the armature current increases beyond that value corresponding to the peak generator kilowatts. This means that the engine is actually working more easily when excessive motor torque or hook pull is being exerted than when normal torque or pull is required.

Either of these generators could be of the variable-voltage type. In the first case the operator must give attention to engine loading, while in the other case the engine is automatically protected against overloading.

A drooping generator characteristic can be obtained by using only a separately excited shunt field and a differential field, referred to as a two-field design, or by using a separately excited shunt field, a self-excited shunt field, and a differential series field, referred to as a three-field design. The three-field design has the advantages that for a given frame size a higher no-load voltage, a greater continuous kilowatt rating, and a greater peak kilowatt output are obtainable than are possible with a two-field design. These results are due to the following reasons.

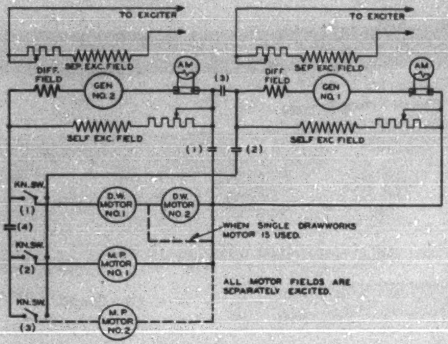


Figure 5 (left). Schematic wiring diagram

Two generators arranged for series operation during hoisting

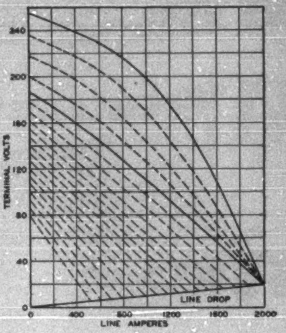


Figure 6 (right). Voltage characteristic of typical 200-kw generator illustrating voltage curves to give 20 points of motor speed control

In either case, at the maximum desired armature current the differential series field must be approximately equal in strength to the separately excited shunt field. If the shunt excitation is made up entirely of separate excitation, the series field will occupy a greater percentage of the field space than it would if the separate excitation were only one-half of the total shunt excitation. Therefore the space for shunt, or positive excitation is less in the two-field generator than in the three-field machine. Also the self-excited shunt field provides an automatic increase in excitation with increased voltage, within the limits of the saturation curve of the machine.

Figure 2 illustrates typical voltage and kilowatt output characteristics of the two different field windings on a given frame size of generator. The no-load voltage of the two-field generator is 241 volts compared to 253 volts for the three-field machine. The voltage of the two-field machine drops quite rapidly with increase of load from no load, while it is relatively well sustained in case of the three-field design. This means higher operating speeds for light loads. Assuming that each machine is to rate 1,000 amperes, it will be noted that the voltages at this current for the two- and three-field generators are 150 volts and 200 volts respectively. Therefore the rating of these machines would be 150 kw and 200 kw respectively. It will be noted further that the peak output of the two-field machine is very little greater than the rated value, while the three-field machine gives a peak output seven per cent higher than its rating. Therefore, the assumed frame with three fields can transmit a peak engine horsepower corresponding to a generator output of 214 kw, while the two-field machine can transmit an engine horsepower corresponding to a peak generator output of only 152 kw.

Types of Equipment

The equipment may be "full electric" or "semielectric." With a full-electric equipment the draw works, table, and mud pump are all driven electrically. The table may be driven by the drawworks motor through the draw works, or by a separate table motor. With a semielectric equipment the mud pump is driven mechanically.

Generators may be connected in series or in parallel. Figure 3 illustrates the speed-torque characteristics of a motor operating on one, two, three, or four generators in parallel. It is evident that the loss of a generator reduces almost proportionately the motor torque available. This is a disadvantage of this system. Paralleled generators have the advantage however that standard-voltage machines may be used, regardless of the number.

Figure 4 illustrates the speed-torque characteristic of a motor operating on one or two generators in series. It is evident that the loss of one generator means only a negligible reduction in maximum motor torque. There is however a proportionate reduction in motor speed. There is a practical limit to the number of generators which can be used in series. From the standpoint of safety and from the standpoint of standard industrial insulation limits the maximum generator voltage should not exceed 600 or possibly 750 volts. It is evident then that if a large number of generators are used the voltage rating will be low with consequently high ampere rating. In extreme cases this results in expensive generators.

Advantages of Variable-Voltage Electrical Transmission

In view of the above characteristics of a variable-voltage d-c equipment for

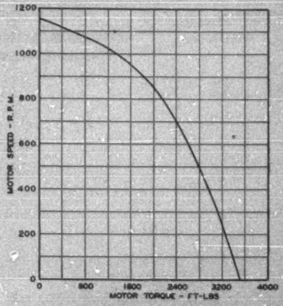


Figure 7. Speed-torque characteristics of 300-horsepower mud-pump motor operating on 250-kw generator

Table I. Data on Weights, Speed Ratios, Mechanical Efficiencies, and Times Allowed for Making and Breaking Joints

Maximum depth of hole (feet)	6,480
Weight of traveling block, hook, and elevators (pounds)	15,000
Weight of 6,300 feet of $6^{5}/_{8}$-inch 31.9-pounds-per-foot drill pipe (pounds)	200,000
Weight of reamers, drill collars, and bit (pounds)	15,000
Total effective weight of string (pounds)	230,000
Number of lines used	6 or 8*
Governed engine speed—either drive (rpm)	600
Speed ratios—electric drive:	
Drilling gear—internal	3.2:1
Drilling gear output shaft to draw-works jack shaft	1:1
Over-all—motor shaft to draw-works drum shaft:	
High-high	2.85:1
Low-high	4.74:1
High-low	10.50:1
Low-low	17.50:1
Speed ratios—mechanical drive:	
Engine through reverse gear to draw-works jack shaft	2.8:1
Over-all—engine to draw-works drum shaft:	
High-high	2.49:1
Low-high	4.14:1
High-low	9.18:1
Low-high	15.3:1
Mechanical efficiencies (per cent):	
Electric drive:	
From engine flywheel to generator pulley	95
From draw-works motor shaft to hook	74
Mechanical drive:	
From engine flywheel to reverse gear	92.7
Through reverse gear to hook	73.3
Over-all efficiencies from engine flywheel to hook, at maximum output:	
Electric drive	56.8
Mechanical drive	67.8
Effective drum diameter—second layer $1^{1}/_{8}$-inch line (inches)	29
Coming out, either drive, six or eight lines:	
Seconds allowed to break joint, set stand aside, and drop hook (per stand)	35
Going in, either drive, six or eight lines:	
Seconds allowed to pick up stand, make joint, and drop string (per stand)	50

* Assume buoyancy counteracted by friction during hoisting.

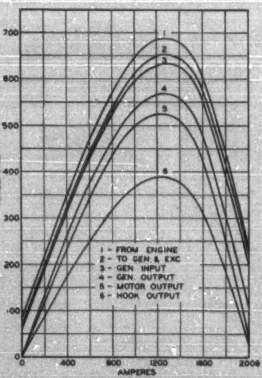


Figure 8. Power absorption between engines and hook with electric transmission

oil-well drilling, let us consider the advantages offered by this type of power transmission as compared to a mechanical transmission.

1. Engine-clutch manipulation in connection with load pickup is eliminated. The clutches are used only when starting up the generator set, therefore delays and expense due to clutch maintenance are negligible. Shocks to engine and mechanical equipment incident to clutch manipulation when picking up loads are eliminated, and consequently maintenance of mechanical equipment and delays caused thereby are reduced.

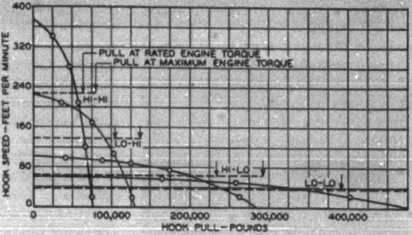


Figure 9. Hook speed versus hook pulls for various draw-works combinations using eight lines

Solid curves—Electrical drive

Dashed curves—Mechanical drive

2. The engine cannot be overloaded or stalled. This eliminates delays in operation. The engine operates at constant speed regardless of load, a condition much easier on the engine than when speeds are varied widely and suddenly.

3. Loads are picked up smoothly. The inherent characteristics of the electrical equipment, including the control, prevent shock loads on the engine or the mechanical equipment.

4. A wide range of motor speeds and torques is available. Voltages at light loads, and consequently operating speeds with such loads, are 15 to 25 per cent above rated speeds; full-load torques may be handled at many speeds ranging from zero to full speed; torques ranging from 40 per cent to 200 per cent of full-load torque may be exerted at zero speed; the maximum torque to be exerted at zero speed can be predetermined.

5. Operating records are easily obtained. Engine performance may be checked by measuring generator output; energy consumption for various operations may be determined; charts showing changes in operating conditions may be obtained.

6. Equipment may be located to best advantage. The power plant and mud pit may be placed at any convenient location.

7. Flexibility as to prime movers and total rig capacity are provided. Engines of the Diesel, gas, or gasoline type or a-c motors may be used as prime movers; one or a few large engines, or many small engines may be used; two separate equipments may be used on one rig to double the power available.

8. Loss of an engine does not reduce the maximum hook pull available when generators are connected in series.

A Typical Equipment

A typical full-electric equipment with two generators arranged for operating in series during hoisting would consist of the following apparatus:

1. One complete generator unit consisting of the following:

(*a*). One 250-kw 200-volt 1,200-rpm generator

(*b*). One 200-kw 200-volt 1,200-rpm generator

(*c*). Two 20-kw 115-volt 1,200-rpm exciters

(*d*). Equipment assembled and wired on rigid skid-type bedplate

2. One 200/400-horsepower 200/400-volt 450/900-rpm draw-works motor

3. One 300-horsepower 200-volt 900-rpm mud-pump motor

4. One 200-horsepower 200-volt 900-rpm auxiliary mud-pump motor

Note: All ratings are continuous based on 40 degrees centigrade rise. This allows for overloads and high ambient temperatures encountered in drilling oil wells. Forced ventilation of motors is used so that (1) motor ventilation will not vary with motor speed, thus assuring full ventilation with overloads, and (2) to make it possible to provide safe air to the motor in case of gaseous atmosphere.

5. One complete variable-voltage control equipment which would include the following items:

(*a*). One main control cabinet containing contactors, relays, knife switches, exciter meters and rheostats, etc.

(*b*). One derrick-floor resistor rack, containing generator field resistors, and with two 20-point reversing oil-immersed master controllers mounted thereon

(*c*). One draw-works post control mechanism, with two wheels arranged so that generators may be controlled independently during drilling, or simultaneously during hoisting

(*d*). One oil-immersed selector switch by means of which the circuits are set up for drilling or for hoisting

(*e*). One explosion-proof emergency stop push button

(*f*). One weatherproof meter cabinet with an ammeter for each generator circuit

Typical Main Circuit Diagram

Figure 5 shows a typical main circuit diagram for a two-generator full-electric equipment arranged for independent use of generators during drilling and series-connected generators during hoisting.

Performance of Typical Equipment

During drilling the 200-kw generator supplies power to the draw-works motor for table drive, or to the separate table motor if used. The 250-kw generator supplies power to the 300-horsepower mud-pump motor. The voltage characteristic of the 250-kw generator is similar to that shown by figure 6 for the 200-kw generator except that for this machine the curve drops to 20 volts at approximately 2,500 amperes, and produces rated voltage at 1,250 amperes. This figure also illustrates the speed control provided by the 20-point controllers. The speed-torque curve of the mud-pump motor operating on the 250-kw generator is illustrated by figure 7. It may be pointed out here that the engine capacity need not be sufficient to carry a total generator output of 450 kw. Let us assume two 350-horsepower engines are used. The two engines drive the generator set and are connected by a coupling between the generators. They therefore divide their output between the generators as required. During drilling the power demand for table drive is usually considerably less than 200 kw, and therefore adequate engine capacity is delivered to the 250-kw generator so the mud-pump motor can operate at full capacity.

During hoisting the characteristic of the 250-kw generator is automatically changed to match that of the 200-kw generator, so that the combined total rated output of the two generators is 400 kw. The 400-horsepower draw-works motor takes the full output of the two generators during hoisting. While the motor is overloaded at times, this is permissible as this motor was previously only partially loaded and because the effective value of the load may still be within a safe value from the standpoint of final motor temperature since the hoisting job is done in a relatively short time.

Transmission Efficiencies

Figure 8 illustrates distribution of engine power during hoisting, from the engine flywheel to the hook, exclusive of that power required for auxiliary circuits. Curve 1 of this figure illustrates the maximum demand which can be made upon the engine for any indicated ampere load on the two generators in series. The maximum possible demand is 687 horsepower, which would be the required output of the two engines combined. This peak power demand occurs at approximately 1,300 amperes. The power demand decreases for current values greater or less than this value. It is therefore evident that as the demand for hook pull becomes excessive, the load on the engine actually decreases, thereby eliminating any possibility of stalling the engine.

The difference between curves 1 and 2 represents the power absorbed in the mechanical transmission between the engine and the generator set pulleys. The difference between curves 2 and 3 represents the power required for electrical excitation and control, and for the necessary motor blowers. The difference between curves 3 and 4 represents the losses in the generators, and the difference between curves 4 and 5 represents the losses in the cable and the draw-works motor.

Curve 5 indicates the power available at the motor shaft, with a peak value of 526 horsepower corresponding to the 687-horsepower engine output value. This represents a transmission efficiency from engine flywheel to motor shaft of 76.8 per cent. Curve 6 indicates the power available at the hook. With 687 horsepower demanded from the engine, the output at the hook is 390 horsepower. The over-all efficiency from engine to hook is therefore 56.8 per cent.

Similar calculations for the mechanical transmission result in an output at the hook of 466 horsepower, with an engine output of 687 horsepower. This

Figure 12. Time for coming out of and going into 4,680-foot hole using six lines

Solid curves—Electrical drive

Dashed curves—Mechanical drive

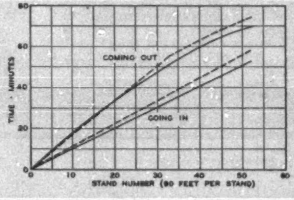


Figure 10. Hook speed versus hook pulls for various draw-works combinations using six lines

Solid curves—Electrical drive

Dashed curves—Mechanical drive

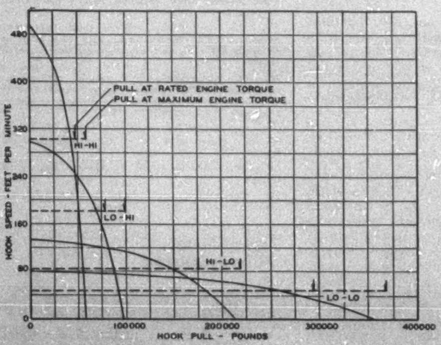
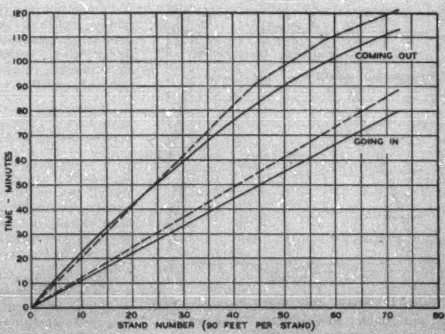


Figure 11 (below). Time for coming out of and going into 6,480-foot hole using eight lines

Solid curves—Electrical drive

Dashed curves—Mechanical drive



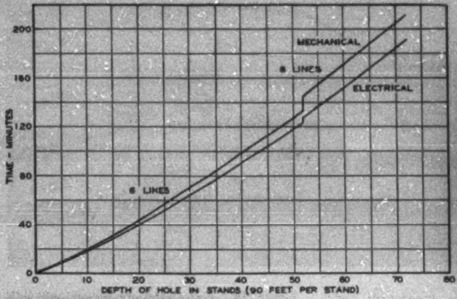


Figure 13. Time required for making round trip for any depth hole up to 6,480 feet, 72 stands, using six lines to 52 stands, eight lines 53 to 72 stands

Identical engines used with both drives

Figure 14. A 200-horsepower mud-pump motor

Figure 15. A 400-horsepower draw-works motor

Figure 16. A 75-horsepower mill-type table motor

represents an over-all efficiency of 67.8 per cent for the mechanical transmission as compared to 56.8 per cent for the electric. The efficiencies used for the various mechanical portions of the drives between the engine and the hook are given in table I.

Comparative Performance When Hoisting

Figure 9 illustrates the motor characteristics with two generators, of figure 4 converted into hook pull and hook speed, with a four-speed draw works and with eight lines on the blocks. Figure 10 illustrates the same characteristics with six lines on the blocks. Table I gives the data used for these conversions.

Figures 9 and 10 also show the hook pull-speed characteristics possible with the same engines but with mechanical transmission. The same draw-works unit is used for both drives.

The rated horsepower available to either drive is 650 horsepower. It is assumed that the engines have a maximum overload capacity of 25 per cent. The electric drive uses some of this margin when developing its peak output when hoisting, while the mechanical drive uses a corresponding portion when accelerating loads at the time draw-works combinations are changed. It will be noted that the maximum hook pull available for each draw-works speed combination is approximately the same for both drives. The equipments are therefore on a comparable basis from the standpoint of over-all speed ratios and use of engine power.

For each draw-works combination the hook speed with electric transmission becomes considerably higher than is possible with the mechanical transmission. This is because the electric motors increase in speed as the load decreases. No such increase in speed is possible with mechanical transmission because the engine is held to a definite maximum desirable speed by the governor. At heavy pulls the hook speed with mechanical transmission is higher than with the electric transmission. It should be noted, however, that the increase in hook speed with reduction in hook load is relatively rapid with the electric transmission. It may also be observed from these curves that the electric drive follows more closely the curve of constant horsepower than does the mechanical transmission.

An attempt has been made to calculate the hoisting performance of each of these equipments. The depth of hole, the weight of equipment, and other pertinent data on which these calculations were based are given in table I.

Figure 11 shows the time required for coming out and going into a 6,480-foot hole, for both equipments with eight lines on the blocks. The curves for coming out illustrate the higher speed of the mechanical equipment at very heavy loads, and the higher speeds of the electric drive at lighter loads. Thus, at about the 24th stand the accumulated time for the mechanical rig becomes greater than for the electric rig. When going into the hole the time is always less with the electric drive. This is due to the higher free-hook speed with this drive.

The total saving in round trip time with the electric rig as indicated by figure 11 is approximately 18 minutes. This is a saving of 8.6 per cent of the time required with the mechanical transmission, which is approximately 210 minutes.

Figure 12 shows comparative performance of the two transmissions with six lines on the blocks when coming out of and going into a hole 4,680 feet (52 stands) deep. The saving in time per round trip with the electric transmission

Figure 17. Semielectric-rig power unit

Figure 18. Full-electric-rig power unit with belted generators

is 9.5 minutes or seven per cent of the 132 minutes required with the mechanical drive.

Figure 13 shows the time required to make a round trip to any indicated depth for each type of transmission. It is assumed six lines will be used until a depth of 52 stands is reached after which eight lines will be used. From this curve can be calculated the total comparative time for making all round trips during the drilling of a well. If we assume an average bit life of 90 feet of hole, after drilling to 450 feet, the number of round trips to drill to 6,480 feet would be 66. The time for making each round trip to renew the bit can be taken from figure 13.

The total time required with a mechanical transmission would be 114 hours, as compared to 103 hours with the electric drive. This means a total saving of 11 hours or 9.6 per cent with the electric transmission as compared to the mechanical transmission.

This saving in time takes into account only the comparative speed-pull characteristics of the equipments and does not include savings in time that are realized with the electric transmission as a result of easier and more flexible control manipulation by the operator, elimination of delays incident to overloading or stalling the engine, and eliminating of delays incident to failure or maintenance of the mechanical transmission as a result of shock loads occuring therewith. The savings shown are obtained in spite of the lower overall efficiency of the electric drive. They are due to the higher speeds at light loads and to the inherent characteristic of the electric transmission which more closely follows the curve of constant horsepower.

Summary

Compared to a mechanical drive, the d-c variable-voltage transmission has several important advantages. These are:

1. Loads may be picked up smoothly and handled smoothly over a wide range of controlled speeds.

Figure 19 (above). Power unit with two cross-connected direct-connected generators

Figure 20. Four close-coupled engine-generator sets mounted on trailer

Figure 21 (below). Twelve-generator power plant made up of two six-generator equipments

2. Flexibility as to: location of equipment; number, type, and capacity of prime movers; and combining of two equipments.

3. Lower maintenance of power plant and transmission due to elimination of abuses incident to clutch manipulation.

4. More continuous drilling due to elimination of delays incident to clutch manipulation, to inherent operating characteristics, and to less time out for repairs.

In view of such advantages drilling costs should be less.

Discussion

T. M. Keiller, H. C. Le Vois (nonmember), and **E. L. Robinson** (nonmember; all of Gulf States Utilities Company, Beaumont, Tex.): In the Gulf Coast region, electricity has been extensively used for all the various power requirements of drilling, oil production, transportation, and refining.

The wildcat or exploration well is often quite a distance from sources of electrical energy, but we have numerous instances of temporary power lines built to locations that, in some cases, prove producing fields. Humble Oil and Refining Company has just built 7$^1/_4$ miles of 33-kv line across coastal marsh and running 1,600 feet out into the Gulf of Mexico. On this rig, wound-rotor motors are used, but the advantages of variable-voltage control could be realized by the use of an a-c motor generator set.

This system, more nearly approximating the steam rig in drilling characteristics, has in addition an advantage over straight a-c motors in the possible use of comparatively high-voltage synchronous motors with high power factor and attendant reduction in voltage regulation, often a factor in service to remote oil fields.

Should central-station service not be available on an economic basis, considering the possible duration of the requirements, it may be desirable to install a central power plant, distributing energy to several rigs, in which case the a-c motor generator set would be desirable.

From the point of view of the supplier of energy, the definite maximum demand, the absence of severe peaks, and the high power factor attainable, makes the a-c motor generator set with variable-voltage equipment a more desirable load than a straight a-c rig.

Another decided advantage that can be gained with the use of variable-voltage control with purchased electric power through the use of motor generator set is a saving in weight and space over Diesel electric. A large amount of activity along the Texas and Louisiana Gulf Coast is being carried on in salt marshes, open salt-water bays, and in the open Gulf, where the problem of adequate foundation is of prime consideration and great expense. The use of purchased electric power would permit lighter foundations, due to less equipment weight, therefore, greater foundation economy.

In addition, practically all fresh-water requirements would be eliminated. The cooling systems of engines used for generator drives require several barrels of make-up water per day. The hauling of fresh water is an expensive item and can be practically eliminated.

A safety feature in deep drilling is that variable-voltage control prohibits the placing of excessive strain on lines and derrick, thereby eliminating the possibility of pulling in the derrick.

Another advantage is that, if space and weight saving is desired but variable voltage is required on the draw works, a motor-generator set of sufficient capacity could be used for the draw-works motor only. The synchronous motor on the motor generator set could have an excitation range of sufficient capacity to correct power factor for a-c motors on the mud pumps. This would sacrifice the advantage of using variable-voltage control on the mud-pump motors, which provides against excessive duty on the pumps when washing a bridge-in well or stuck drill stem.

The driller would profit either in the purchase of power or generation in the central station from the reduction in demand, increase in power factor, and higher load factor possible. With more than one rig, diversity of peak demand would reduce the aggregate demand on the power source.

Once drilling crews become accustomed to the use of electric power rigs, they appreciate the ease of installation and increased speed of drilling operations. The variable-voltage system gives all these and the added advantages of the smooth high-torque variable-torque speed operation of the d-c motor.

P. L. Savage (nonmember; General Electric Company, Los Angeles, Calif.): I am sure we all are indebted to Mr. Lamberger for a very excellent paper on a subject which should be of great interest to the petroleum industry in particular and electrical engineers in general. In the writer's opinion, d-c equipment using variable-voltage control offers many advantages which are covered in considerable detail by Mr. Lamberger and should find more general use in the oil fields.

As suggested by Mr. Lamberger early in his paper, variable-voltage drilling is usually associated with internal-combustion engines. He also mentions that steam turbines and a-c motors have been suggested as prime movers but up to the present time, have not been used. A-c motors for generator drives, instead of engines, in particular, seem to offer a number of advantages which should be given serious consideration by the purchasers of oil-well-drilling equipment.

It must be admitted at the start that some locations where drilling operations are carried on, do not have a-c power available and in these locations, prime movers are to be favored. On the other hand, substantial percentage of oil-well drilling is done in locations where central-station power is available at reasonable cost. In such cases, the use of motor generator sets consisting of an a-c motor, of either the induction or synchronous type, driving the d-c generators, should be given careful scrutiny. Available central-station power lines are usually more than ample in capacity for the electric power requirements in drilling the deepest oil wells.

In this discussion, I would like to point out briefly the advantages of a-c motors when used for driving d-c generators.

It is generally admitted that the a-c motor has an advantage over the reciprocating engine on the basis of maintenance costs.

Ease of installation and portability are other factors which favor a-c motors to drive the generators, due to smaller size, lighter weight, and fewer auxiliaries.

Other considerations in favor of motor generator sets are:

1. No water for cooling is necessary.
2. There is no fuel problem.
3. The utility factor is practically 100 per cent.
4. More power per pound weight and per square foot of floor area occupied.
5. Lower first cost.

Many operating companies find it necessary to move drilling rigs from one field to another and in some cases, these new locations may not have a-c power available. This presents no serious problem since an a-c motor might be replaced with an internal-combustion engine for such locations. In the original design the d-c generators can be chosen so that the operating speeds are suitable for either motor or engine drive.

Mr. Lamberger has very clearly pointed out the advantages in the use of variable-voltage d-c power for drilling and has compared this operation with engines and mechanical transmissions.

In the past, where alternating current is available, it has been the practice of some operating companies to use a-c drilling equipment which consists of wound-rotor induction motors for furnishing the required power directly to the draw works and mud pumps.

It should be remembered that the a-c drilling motors obtain their speed variation by means of losses in the secondary or rotor circuit; therefore, a motor which has a full-load efficiency at full speed of approximately 90 per cent, drops immediately to something below 50 per cent at half speed, assuming that the torque remains constant. As the speed goes down, the efficiency drops in almost direct proportion. In addition to low efficiency at reduced speed, a considerable problem is encountered in maintenance, for the controller is required to carry heavy current. Also motor heating becomes a problem.

If d-c equipment were used with power supplied by a motor generator set, all of the advantages outlined by Mr. Lamberger would be available and the over-all efficiency would be expected to be much higher. Although we have interposed a motor generator set and must supply the losses to it, still the efficiency of the motor generator and d-c driving motor are maintained relatively high throughout the load range of the machine.

For instance, the efficiency of a motor generator set of suitable rating for oil-well drilling would be approximately 83 per cent which takes into account the combined efficiency of motor and generator. At half load, the efficiency has dropped to only approximately 80 per cent, which is very high as compared to the 50 per cent efficiency of a wound-rotor induction motor operating at a similar load.

Bearing out Mr. Lamberger's statement that electrical maintenance costs are low when variable-voltage d-c equipment is used, it may be stated that there are two such engine-driven drilling rigs being operated by a major oil company in California at the present time. Although they were designed for drilling holes approximately 3,500 feet in depth, they have given an excellent account of themselves drilling much deeper holes and during the period since 1931, the expense of maintenance and repair parts for the electrical equipment has been negligible.

W. G. Taylor (General Electric Company, Schenectady, N. Y.): First let me congratulate Mr. Lamberger on his very fine paper which has been carefully prepared and presents the subject in a very interesting manner from a different angle from anything previously published on this subject. I feel sure that this paper will serve to clearly inform all engineers of the interesting and important phases of this application of electric drive with generator voltage control, even though they may not be familiar with the details of operation of oil-well rotary drilling rigs. Some points, which Mr. Lamberger may have touched on only lightly or not at all, have occurred to me as worth adding to round out his excellent presentation.

Essentially there are three major operations on a rotary drilling rig. The first is that of revolving the drill pipe and bit to drill the hole; the second, which occurs simultaneously with the first, is that of circulating mud down through the drill pipe and up outside of it to carry away the cuttings, plaster up the side of the hole to minimize caving, and provide a hydrostatic head to prevent blowouts where high gas pressures are encountered, this mud circulation being handled by reciprocating pumps; and the third operation is that of hoisting the drill pipe out of the hole to replace worn bits, and also putting the pipe back into the ground again, and likewise handling the casing which lines the hole after it is drilled. I mention this to bring out the point that the motor driving the rotary table to revolve the bit and the motor driving the mud pump must be able to operate with entirely independent speed characteristics and both run at the same time, and so a separate generator must be supplied for each because the speed regulation is by generator voltage control. Therefore, on a fully electrified rig, where all of these major operations are performed by motor drives, at least two generators must be used as Mr. Lamberger has stated.

A good many installations have been made where the pump is driven directly by the engines, which also drive a generator to supply power to the drilling motor, thus making what is known as a semielectric rig. As there is only one main motor in such a case and no mud-pump motors, only one generator is necessary.

In pointing out the advantages of a full-electric rig, Mr. Lamberger has mentioned that in some locations it is decidedly advantageous where suitable space near the well is at a premium, such as on steep hillsides. It might be added that another highly desirable location for this type of drive is in marine drilling, where the power equipment must be located on a barge, and where it is therefore not practical to use engines with direct mechanical drives to the rig, unless the piling used for supporting the derrick is greatly extended at considerable expense to provide a foundation for such a drive.

In explaining the relative characteristics of two-field and three-field d-c generators for this service, Mr. Lamberger has pointed out that in a given generator frame the three-field machine will have the advantage of higher free-hook speed resulting from the higher voltage obtained at light current load. High free-hook speed is considered essential in modern deep-well drilling because it shortens the time required to hoist the free hook from derrick floor to the top of the derrick when putting the pipe back into the hole length by length, and so reduces the nonproductive time when the rig is not making a hole. The speed obtainable is dependent not alone on the generator characteristics, but also on the gear and sprocket ratios of the drive from the motor to the hoisting drum, and on the amount of load on the motor which lifting the free hook involves. This load on a medium or heavy rig will amount to lifting a dead weight of from 10,000 to 15,000 pounds at a speed as high as 415 feet per minute in a 92-foot derrick. It is, however, hoisted on the upper part of the volt-ampere curve of the generator, and therefore the relation which the resulting hoisting speed of the free hook bears to the speed of hoisting loads of pipe depends upon the shape of that part of the generator curve. On a two-field generator this part of the volt-ampere curve is steeper than on a three-field design. Not so many years ago when the hoisting speeds for heavy loads were more moderate than they are now, the steeper curve of the two-field machine better fitted the conditions to be met, but since then speeds have been increased for heavy loads, making the flatter curve of the three-field machine better adapted for the purpose. The limit of speed at which the free hook can be hoisted is determined by the human element as represented by the ability of the drilling crew to handle the cycle of operations without a mistake and consequent loss of time which would offset any higher speed used.

Mr. Lamberger has described the difference in motor hoisting characteristics when running from generators in parallel as compared with generators in series. It may be noted, however, that normal hoisting of drill pipe with a properly applied d-c drive of either type is never carried on at the high-torque slow-speed end of the curves, shown in figures 3, 4, and 12, as the speed is too low to get the pipe out fast enough to suit any operator. A drive properly applied both as to horsepower capacity and gear and sprocket ratios should handle a heavy normal hoisting load on that portion of the horsepower-output curve of the motor or kilowatt-output curve of the generator which is at or near the peak output. Under such conditions it has been found, in the cases which have been checked, that the loss of one of two generators operating in parallel to supply the hoisting power will still leave enough torque available from the remaining generator to hoist the pipe at very low speed.

Another point to the advantage of the scheme of running the generators in parallel is that, on the modern four-speed draw works or hoist, only the three higher speeds are used for normal hoisting work, and the lowest speed is therefore available for increasing the rope pull at a given motor torque in an emergency.

There are many equipments in service, some of them designed for the generators to run in series and others designed for them to run in parallel for hoisting, and as far as I know all have given an excellent account of themselves and have met emergency conditions successfully.

A. Naeter (Oklahoma Agricultural and Mechanical College, Stillwater): As an educator in a region where many electrical-engineering graduates enter various phases of electrical work in the oil industry, the writer feels that the author has made a valuable contribution to the available literature on variable-voltage rotary drilling rigs.

During the last few years the writer has been watching with interest a large drilling company in the southwest with regards to its drilling personnel. This particular company uses variable-voltage equipment driven by internal-combustion engines, and direct Diesel drive as well as conventional steam equipment. A few years ago this company hired an electrical-engineering graduate, with oil-field experience, and competent as electrician and machinist, to work as a motorman on a variable-voltage drilling rig. At first this employee faced considerable opposition from the practical men working on the rig. He soon "sold himself" to both employer and employees of the company to a degree that several additional electrical graduates were employed because the employer had become convinced that the electrical graduates had been able both to reduce maintenance costs and to speed up drilling operations. In time motormen became drillers, and then advanced to positions of greater responsibility. No practically trained employees were dropped, but electrical-engineering graduates were employed to fill vacancies, appearing through normal turnover, as well as new positions. Now the company no longer insists on oil-field background, and electrician and machinist skills, but trains its young engineers after employing them. This particular company is following an employment program that in time will place at least one graduate on each tour of every rig.

To an engineer in the educational field, it is encouraging to see young engineering graduates prove themselves in operating variable-voltage rotary drilling rigs at a time when many of the conventional fields of employment of the predepression days offer relatively few opportunities.

E. M. Gerry (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): In the second paragraph of Mr. Lamberger's paper he states that "*A-c motors* can be used to advantage [as prime movers] and have been proposed, but to our knowledge never actually used."

A well was drilled in Oklahoma several years ago, using d-c variable-voltage equipment, driven by a squirrel-cage induction motor. The final depth of the hole was over 10,000 feet; 7,500 feet of the total depth was drilled with special heavy $6^5/_8$-inch drill stem.

Some Recent Developments in Impulse-Voltage Testing

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Synopsis: Impulse-voltage testing of electrical apparatus and circuits is a steadily growing practice and constant attention is therefore being given to improvements in testing methods. Accordingly, as progress is made it is necessary to describe recent changes. This paper presents a brief discussion of the present situation, describes some improvements in technique of surge analysis of apparatus and circuits, and in equipment for the generation and measurement of test surges.

EQUIPMENT and methods for impulse-voltage testing of apparatus and circuits have been added to and improved considerably within recent years. In comparison with the older methods, the newest technique provides for several important advantages. A decidedly better understanding of the surge mechanism of the circuit and an improved control of shape and timing of generated surges is now realized. Impulse-voltage tests and investigations are conducted with greater speed and reduced uncertainties.

The Twofold Nature of the Surge-Strength Problem

Impulse-voltage testing in its earliest period consisted in applying a surge voltage, measured for amplitude by the sphere gap, to the apparatus or circuit under investigation and watching for visible evidences of failure. The introduction of the cathode-ray oscillograph,[1] which provided for the recording of voltage wave shapes, opened the way to a more precise understanding of surge performance of circuits and strength of insulation.

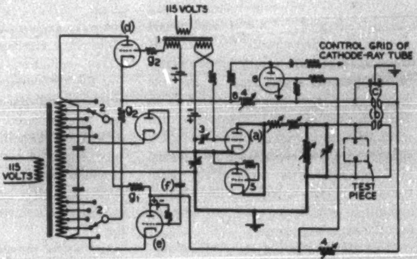

Figure 1. Oscillograph electric-transient analyzer

At present a complete experimental analysis of the electric impulse or surge characteristic of an electrical machine or circuit will involve two general considerations. These are the surge mechanism of the circuit and the electrical strength of the circuit. Stated in a different way, any experimental analysis of the surge characteristics of a circuit, whether it is a machine winding or a connecting network, must, to be at all complete, answer two questions:

1. **What is the nature of the current and voltage variations in the several parts of the circuit under surge conditions?**

2. **What is the strength of the circuit (insulation particularly) under surge conditions?**

As an example, take the case of the rotating-machine winding where entering surges are subject to varied reflections and attenuations. Depending upon such factors as winding length, coil turn distribution, and terminal connections, voltage wave shapes and amplitudes inside the winding will vary widely particularly when the applied surge voltages are relatively short in time duration compared to the electrical length of the winding. To apply an impulse voltage, therefore, in such a case, to test for insulation strength, without an understanding of the surge mechanism involved, may frequently lead to incorrect results. The case of the rotating machine, however, is but one example of the many cases where in an experimental analysis both the surge mechanism and electrical strength of the circuit must be considered.

Oscillograph Electric-Transient Analyzer

Recognizing the importance of providing a measuring instrument for surge analysis which eliminated the delays and difficulties involved in the use of the single-discharge surge generator and high-speed cathode-ray oscillograph, the oscillograph electric-transient analyzer[2] was developed. Several years' use of this instrument has demonstrated its wide applications and has resulted in improvements in the device.

The oscillograph electric-transient analyzer generates a succession of low-voltage impulses of controlled wave shape for application to the circuit or apparatus to be analyzed for surge mechanism. The time of occurrence of the impulse waves is co-ordinated exactly with the time axis of a cathode-ray oscillograph and there is, therefore, an exact retracing of the wave image on the oscillograph screen. By connecting the deflection plate terminals to a selected point of the circuit under study the wave shape at this point is observed as a standing wave. By moving

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[1] For all numbered references, see list at end of paper.

the deflection plate terminal rapidly through succeeding points on the test circuit, the travel of voltage wave shape along the circuit is made visible.

Figure 1 is the connection diagram for the latest analyzer. The circuit changes recently made are noted by numbers as follows:

1. Initiating transformer
2. Time-axis position control
3. Preliminary zero-line control
4. Balanced voltage time-axis circuit
5. Reverse-current tube
6. Cathode-beam control

The low-voltage surge-generator circuit shown in heavy lines produces a surge of adjusted shape for each cycle of the 60-cycle supply voltage. The surge is initiated by the firing of the thyratron tube *a* which in this circuit replaces the sphere gap of the high-voltage surge generator. The surge voltages are applied to the test piece and to the vertical deflection plates of the cathode-ray oscillograph *b*. Now the time axis of the oscillograph which is provided by the horizontal deflection plates *c* must have a correct and fixed time relation to the surges generated. This it was found could best be accomplished by firing the time-axis thyratron *d* from one secondary of the peaking transformer 1 and the surge tube *a* from the other secondary of the same transformer. Tube *d* in turn instantly fires the other time axis tube *e* through coupling capacitor *f*. By means of a capacitor 3 the firing of the surge thyratron *a* may be delayed any desired amount to provide for some zero line before the surge starts.

Most effective use of the cathode-ray-tube screen requires that the time axis begin at a point near the left edge of the screen and sweep to the right. To provide for the beam starting at the left a definite deflection voltage is required. Obtaining this from batteries or fixed-pole magnets has disadvantages. For the analyzer circuit, therefore, a portion of the 60-cycle voltage of the main transformer was utilized. Tap switches at 2 were found convenient for selecting the required beam bias voltage so that at the instant of beam initiation the bias would be correct. Resistors g_1 and g_2 absorb the transformer voltage when thyratron tubes *e* and *d* are tripped.

In the original analyzer the time-axis voltage was supplied through a single tube to one time-axis deflection plate, the other plate being grounded. This was found to have two disadvantages. It prevented good focusing of the cathode beam throughout its travel and it gave a converging deflection sensitivity (direct-voltage lines were not parallel). By using the balanced circuit as shown in figure 1 comprising principally tubes *d* and *e* and in which the time-axis plates have at each instant the same potential but of opposite polarity, these two disadvantages have been eliminated.

For the unidirectional polarity surges a single discharge tube at *a* was shown in the early analyzer circuit to be adequate. However, a number of instances occurred where oscillatory surges were required. For the low voltages utilized in the generator circuit the substitution of a gap was objectionable because of timing difficulties. In addition another discharge tube connected oppositely, as shown at 5, provided for the conduction of the reversed polarity currents accompanying oscillatory discharges of the main capacitor.

In view of preventing undesirable illumination on the cathode-tube screen and back-sweeping of the beam, a good arrangement for beam initiation and cutoff is required. This has been realized by taking off the time-axis circuit at the point marked 6 a positive-polarity voltage which is in part applied to the control grid of the cathode tube as shown in figure 2 and discussed in connection with the cathode-ray oscillograph in a subsequent section.

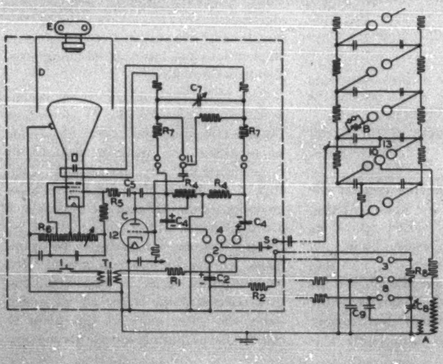

Figure 2. Impulse-generator and oscillograph control circuits

While the duration of the beam could be controlled by varying the time constant of the initiating circuit, better results are obtained by making the time constant longer than the slowest time axis and cutting off the initiating potential with a thyratron 6. The grid of this thyratron is connected to both sides of the time-axis circuit by resistors of such value that the grid potential becomes positive and fires the tube at the end of the time axis.

Surge Test of Insulation Strength

Surge-voltage and impulse tests of apparatus and conductor insulation are made with a surge generator, cathode-ray oscillograph and voltage divider, and sphere gap. As regards the generator and oscillograph, recent changes require some explanations.

CATHODE-RAY OSCILLOGRAPH

Some two years ago[3] there was described for the first time a practical high-speed oscillograph which dispensed with vacuum-pumping equipment and permitted simultaneous visual observation and recording of short-time surges. Complete circuit diagrams were presented. Continuous use of this oscillograph on a wide variety of work since then has resulted in many valuable refinements in equipment and methods. The following paragraphs describe the major changes adopted.

The diagram of figure 2 shows the latest arrangement of the control and timing parts and connections for the impulse generator-cathode ray oscillograph circuit. Apparatus and circuit changes are summarized as follows.

A. Substitution of an impulse transformer for an intermediate surge-generator unit.

B. Introduction of a special wave-front circuit giving extra timing precision.

C. More precise beam-duration control.

D. Introduction of extra light shielding for camera compartment.

E. Utilization of higher-speed camera.

In all high-speed cathode-ray-oscillograph work and particularly with nonrecurring transients the problem of properly timing the oscillograph operation with the event to be recorded is an important one. In general impulse testing

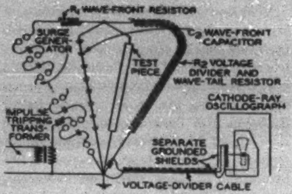

Figure 3. Schematic arrangement of surge-generator circuits

the total duration of commonly used time axes ranges from 1 to 100 microseconds. For full-time flashover on a standard wave such as th e$1^1/_2$x40 microsecond suitable time axes range from 50 to 100 microseconds and for the 1x5 wave from 5 to 10 microseconds. For some volt-time flashover curves and for wave-front work, time axes of total duration as low as 1 microsecond are sometimes required. For best results a short-time zero-deflection axis is required prior to the beginning of the recorded transient. For full-time flashover work with a 50-microsecond time axis, this zero-deflection time axis will be several microseconds long. Adequate time co-ordination between the time axis and the transient will be within one or two microseconds. However, for wave-front testing the zero-deflection time axis will be less than a microsecond and adequate time co-ordination will require a precision within tenths of a microsecond. For this reason a particular connection arrangement is used for full-time flashover work and a second connection for wave-front work. Changing from one connection to the other is accomplished by reversing the switch *S* on figure 2.

For full-time flashover tests *S* is turned down. The initiating switch 1 when closed provides for a power frequency voltage on the right sphere of gap 2 of the initiating surge circuit. As the left sphere already has on it a continuous voltage of positive polarity, this gap breaks down and is immediately followed by the breakdown of gap 3. Gaps 2 and 3 are in series and really constitute a three-electrode gap. They have been separated as shown so that the ultraviolet radiation from gap 2 will irradiate gap 4 and gap 3 will irradiate gap 8. Gaps 3 and 8 in turn are placed so as to irradiate the main-generator trip gap 10. Such irradiation produces ionization in the gaps so that they spark-over without delay when the potential on them reaches the spark-over value.

The voltage of capacitor C_3 is now applied across resistor R_3 through the switch *S* to the midsphere of the time-axis-circuit gap 4. Capacitor voltages C_4 are applied across resistors R_4 and a square-wave positive-polarity voltage is applied to the biasing grid of the cathode tube through R_5. This positive voltage is sufficient to reduce the negative blocking grid bias obtained from the main cathode excitation resistor R_6. The cathode beam is initiated and begins to deflect immediately in accordance with the time-axis voltage of capacitor C_7 which is charging from capacitors C_4 through resistors R_7.

Meanwhile the voltage across resistor R_3 is also across resistor R_8 and capacitor C_8. When the voltage C_8 rises to a predetermined value gaps 8 break down throwing capacitor voltage C_9 on the primary of the impulse transformer *A*. The resistance-capacitance product R_8C_8 is adjusted to give the required time interval between the beginning of the time axis and the transient excitation of the impulse transformer. The voltage wave on the secondary of the transformer is applied to the mid sphere of the three-electrode gap of the surge generator 10 and precipitates this gap breakdown which is immediately followed by breakdown of the remaining gaps of the Marx circuit and the production of the required surge voltage.

The impulse transformer is an air-core transformer of the type commonly used on damped-wave oscillators. Such a transformer has been found by experience to provide a simple, economical intermediate unit to replace the complete small-scale rectifying-type surge generator commonly used for this purpose. In view of the limited energy output of the transformer, best results are obtained if it is installed at the base of the surge generator and capacitance values are kept low to provide as square a voltage wave as possible. Returning now to the cathode-beam control, a circuit connected to and controlled by the sweep voltage is used to cut off the cathode beam after it has moved off the tube screen. Gap 11 is set to break down when the sweep voltage reaches a value high enough to complete the time axis and in so doing energizes the grid of thyratron 12 positively, causing it to fire and to short circuit the positive cathode-tube grid bias from the sweep circuit used to initiate and hold the cathode beam on. The cathode-tube grid immediately goes back to its negative polarity bias obtained from R_6 and the sequence of events is completed.

As the above described time-co-ordination mechanism is not adapted to split microsecond timing precision, a different technique is used for time axes of very short duration.

When switch *S* is turned up pressing control button S_1 breaks down gaps 2, 3, and 8 in order and finally gap 10 through the impulse transformer. As yet the oscillograph has not started. Experimentally it has been determined that the insertion of a low resistance at *B* in the surge generator circuit will provide sufficient delay in the breakdown of the Marx circuit gaps to permit the tripping of the sweep circuit gap 4 and the completion of the entire mechanism of cathode-beam initiation and initial zero-deflection sweep through a connection at 13 on the second stage of the surge generator. This arrangement provides for all the precision required in timing, succeeding surges occurring within 0.1 microsecond on the time-axis scale. The resistance *B* is bridged by a gap which breaks down to carry the main discharge current of the surge generator.

Surge-Generation Circuits

Recent increasing use of wave-front tests wherein time elements to be measured range as low as 0.1 microsecond at high voltage and 0.02 microsecond at low voltage have necessitated that more attention be given to the arrangement of surge-generator discharge circuits to eliminate spurious oscillations. Experience has gradually shown that the general considerations for reasonably smooth waves are as follows:

1. The ground point of the surge generator, oscillograph, test piece, and resistance voltage divider to converge as nearly as possible to one point as shown on figure 3.
2. The cable from the resistance divider to the oscillograph to be out of the generator field.
3. The oscillograph to be housed in shielded room and this room to be as well out of the field of the generator as possible.
4. The ground point to be as directly connected to good ground earth as possible, a few feet (less than ten) if possible.
5. Both ends of the divider cable to be terminated by a resistance equal to the surge impedance.
6. Several hundred ohms damping resistance in the deflection leads.

In figure 3 are illustrated several of these points. The diagram is not intended to show relative positions of apparatus as these are invariably peculiar to each installation. The practice of converging all ground connections at one point has been found to eliminate stray potentials in any one circuit due to the ground connection of that circuit carrying ground current for another circuit. This has been shown to be important particularly in connection with the measurement circuit to the oscillograph.

A frequent source of disturbance in the measurement circuit is an oscillation resulting from the stray-capacitance coupling of high-voltage elements with the divider cable sheath. This can be eliminated by a shield as shown in figure 3 or by running the cable under the ground-floor level. If the oscillograph housing is located in the field of the high-voltage elements, local oscillations may occur in it for each surge-generator discharge. A separately grounded shield over the housing is therefore beneficial.

Figure 4. Projector for reproduction and analysis of cathode-ray-oscillograph film

A most important feature is a good short ground connection between the common point and ground. In fact, for high-voltage unbalanced discharge circuits, whether or not satisfactory short-time tests can be obtained will depend upon grounding conditions.

The resistance equal to the surge impedance at the oscillograph cable end provides for the elimination of reflections. This resistance should be carefully adjusted. However, experience has been that complete elimination of reflections is difficult. The resistor at the generator end of the cable is therefore advantageous as it provides against repeated reflections appearing at the oscillograph end.

Film Records

The high-vacuum type of oscillograph permits records of any size desirable depending upon the camera used. Experience however, has been that standard 35-millimeter records are most convenient and accurate. As each wave is observed visually, as it is recorded photographically, qualitative results are known before films are developed. With the small film records which can be dried in less than ten minutes after washing, inserted in a standard projector as shown in figure 4, the wave shape is projected on the lower table surface which is provided with cross-section lines. Direct calibrations are first made by projecting the calibration oscillogram. This provides a ratio of kilovolts per millimeter deflection which can be used repeatedly for the measurement of test oscillograms.

When film reproductions are required, and these usually necessitate some marking such as identifying numbers or scale division values, the wave image from the projector is transmitted through a transparent surface just above the table level. Such markings as necessary are made on this surface which a quick-drying ink. An unexposed film is inserted under this transparent surface and the image of the wave shapes and markings recorded usually at five times the size of the original negative. As this enlargement is really a positive film, prints from it will be as desired—black traces on a white background. Any number of paper-print reproductions are now made from this positive film without hazard to the original record.

Detection and Location of Faults

Because impulse tests involve voltage applications of only a few microseconds time duration, it is often difficult to know when failure has occurred or damage has been done to the test specimen. Especially is it important to know this immediately on application of a surge so that testing may be discontinued until the location or nature of the failure is determined. The most common and practical evidences of failure or damage are as follows:

1. Visible external arcs and corona streamers.
2. Excessive noise in the discharge.
3. Chopping or distortion of the oscillogram.
4. In oil-immersed apparatus, rising gas or smoke bubbles.
5. Indications of excessive current in the test leads.

Visible external arcs and corona streamers are present in most cases of insulator flashover such as porcelain insulators when tested for full-time flashover, or when volt-time curves are being taken. Corona streamers are visible at voltages below flashover and beginning usually on the rising front of the wave continue in extension as the voltage increases. The formation of these streamers, however, apparently lags behind the applied voltage and their extension continues for a time along with the dropping voltage of the wave tail. If the level of voltage of the wave is sufficiently high, these streamers bridge across the insulation and culminate in flashover. The critical

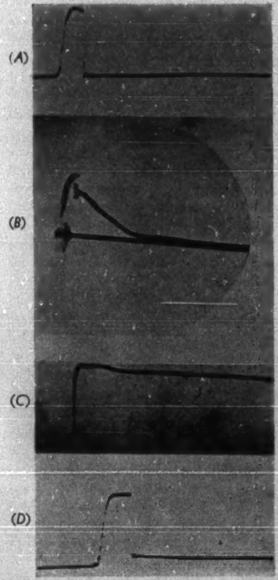

Figure 5. Sample oscillograms of insulation failure

A—Insulation failure on a wave tail showing the complete chopping obtained in a low-resistance breakdown

B—Insulation failure on a wave crest showing partial chopping obtained in a high-resistance breakdown

C—Partial insulation failure of a high-voltage cable as indicated by wave distortion on the crest

D—Complete insulation failure of a high-voltage cable on the next application following C flashover is that of the lowest voltage level (crest voltage) which will complete flashover for about half the impulse applications. No difficulty is experienced in detecting flashover in such cases. Both visible flashover arcs and excessive noise give ample evidence of it. The flashover path shows the failure channel if the observer is watching the test piece carefully.

On wave-front tests, however, and for some cases of chopped waves additional difficulties are encountered. The chopping of the wave adds a volume of noise which renders this method of no value as an indicator, and distorts the test wave so that the oscillogram of the chopped wave cannot be used with any degree of certainty to indicate test-piece failure. It is always necessary to follow up the chopped wave with a full wave of the same voltage level. The full wave of a specified voltage level has a crest voltage 15 per cent lower than in chopped waves.

Chopping or distortion of oscillograms in full-wave tests are the most practical and certain indications of damage to the insulation under test. A sharp drop in the wave such as shown in figure 5*A* definitely shows that failure has occurred. This is the record of a failure in an air-insulated instrument transformer which could not have been detected by outside evidences or by excessive noise. This failure occurred from line-to-ground and took the form of a very low impedance failure path. Figure 5*B* shows a distorted wave indicating partial failure.

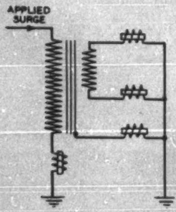

Figure 6. Arrangement of magnetic links for location of failure points

Figure 5*C* is an unusual and interesting oscillogram of a cable failure showing a wave distortion due to partial failure on the surge preceding the one causing final breakdown, which is shown in figure 5*D*. Such records are not frequently obtained and their occasional occurrence stimulates confidence in the oscillograph.

Observation of the oil surface in oil-immersed apparatus sometimes is helpful in detecting and locating a failure, but in general is a very unsatisfactory method because small failures which can be detected from the oscillogram often do not result in rising bubbles, which can be seen on the surface of the oil.

In all these records the chopping and distortion of applied full waves are immediately observed on the screen of the high-vacuum oscillograph.[1] Although not infallible the oscillograph is the best evidence of damage. Simultaneous visual and photographic recording of the voltage wave shape across the test piece insures the detection of failures immediately. Time lost in testing and resulting confusion when surges are applied after the first failure can be avoided when each test-piece voltage wave can be observed as it is applied.

In many cases of impulse-voltage testing the test piece is made up of a number of parts such as several windings and possibly a core and a case. The most common practice is to apply the impulse to one winding with other windings and core and case grounded. It is always important to know when failure occurs just which insulation has been broken down. Particularly in connection with instrument transformers the authors have made many successful uses of the surge-crest-ammeter magnetic-link technique[4] commonly used for lightning measurements.

If as shown in figure 6 the connecting leads to the several parts of the test piece are brought through magnetic-link stations, the links can be used to indicate where breakdown occurs. As soon as the oscillograph indicates breakdown by chopping or distortion of the wave, the magnetic links are measured for residual magnetization in the surge-crest ammeter. If properly adjusted the magnetic links in the path of the failure current will receive greater magnetization.

Small coils such as shown in the figure have been used most commonly for this application. Two, three, six, or ten turns are used. The calibration curves for unidirectional surges are shown in figure 7. Depending upon the characteristics of the surge-generator circuit and the shape of the wave applied, the coils will carry normal capacitance current which increases in amplitude with applied voltage. Therefore, the magnetic link coils must be chosen to leave no link magnetization for these charging currents, but to provide sufficient turns to give measurable residual for the short-circuit currents.

Oscillographic Recording of Voltages Between Tap Points

While the greater portion of impulse testing is concerned with voltages between high-potential tap points and ground in very many cases direct tap-to-tap voltage increments are required. This requirement calls for the measurement of low-voltage increments at high-voltage levels. With the deflection plates of the oscillograph either directly connected or through resistance or capacitance dividers, both plates become of the same voltage polarity. When the voltage between plates is sufficiently high for a suitable deflection, the absolute potentials of the plate will be excessive for good oscillograph operation, that is, absolute deflection-plate voltage will introduce a cathode-beam distortion which results in beam defocus and unsatisfactory records.

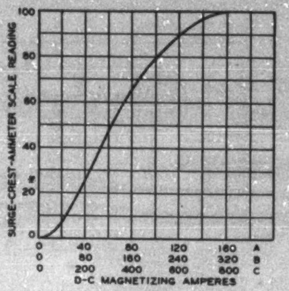

Figure 7. Surge-crest-ammeter magnetic-link calibration

A—Ten-turn coil
B—Five-turn coil
C—Two-turn coil

Until recently such tap-to-tap voltages have been obtained by subtracting tap-to-ground records or by accepting usually with considerable uncertainty such tap-to-tap records as could be obtained with minimum defects. The method of subtracting tap-to-ground measurements is unsatisfactory because of possible uncertainty in the timing of the two records, a point on which the actual values of tap-to-tap voltages depend.

Through the work of the authors marked progress has been recently made on this problem. A circuit has been built and tested which automatically does the subtracting of the two tap-point waves. Figure 8 is schematic drawing of the connections used for the subtraction divider.

Assuming a requirement of tap-to-tap measurements between T_1 and T_2 at the high-voltage end of the winding *W*, the applied surges are entering at *S*. These may be single-surge applications from high-voltage surge generator or a timed train of surges from the oscillograph electric-transient analyzer.

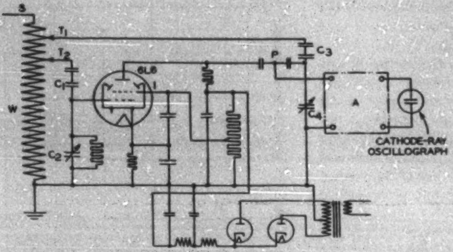

Figure 8. Subtractive voltage-divider circuit for tap-to-tap records

Capacitor combinations C_1, C_2, and C_3, C_4 are similar voltage dividers which must be sufficiently low in capacitance to give no appreciable wave distortion at the intermediate tap point. This is an important and exacting requirement.

Unit I is an amplifying inverter and A an amplifier. At point P the voltage waves from the left and right divider superimpose and are of opposite voltage polarities. The oscillograph through amplifier A, therefore, records the voltage between P and ground which is equal to the instantaneous differences between the voltages at P from the left and right dividers.

Preliminary adjustment of this circuit is accomplished by connecting the tap point terminals T_1 and T_2 together at T_1 and adjusting C_3 and C_4 for no deflection on the oscillograph. This proves a correct balance of the two waves arriving at P. Moving the left capacitor lead to intermediate tap point T_2 now gives the required tap-to-tap voltage difference.

Particular precautions are necessary in setting up such a circuit to provide for accurate results. The capacitance divider should be permanently built in a very symmetrical arrangement to provide for a minimum of unbalanced voltage interference effects, and when in use should be so adjusted that the amplifiers are not carried beyond their saturation points. The inverter must be built to include a minimum of time lag between the incoming grid voltage from the capacitor C_1C_2 and the output plate voltage to point P. The amplifier at A must have a wide-range frequency response and a high input impedance. In balancing the whole arrangement it has been found possible to add sufficient lead length at T_1 to balance against some lag in I which cannot be eliminated otherwise.

Conclusions

Recent rapid progress in impulse testing has emphasized the importance of an understanding of the surge mechanism of the circuit under test and analyzer equipment has been developed which provides for direct demonstrations of this mechanism.

For the recording of test waves in impulse-voltage testing precision methods of timing have been made available which provide for improved accuracy of recording with the cathode-ray oscillograph, especially for wave-front testing.

The necessity of careful attention to surge-generator grounding conditions has been increasingly recognized and practices have developed wherein disturbing oscillations particularly in the measurement circuit are considerably relieved.

A circuit which permits the recording by cathode-ray oscillograph of voltage increments between tap points at high absolute voltage levels has been designed and tested.

The cathode-ray oscillograph is the best indicator of damage in impulse testing, because it provides for both simultaneous visual observation and permanent film records of test wave distortions and choppings.

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Discussion

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): Messrs. Foust and Rohats are to be complimented on the thoroughness with which they have reported their progress in this rather specialized art of impulse testing. Their contribution will aid greatly in reducing the intangibles and increasing the real understanding of the behavior of surges in laboratory and field apparatus.

One question is raised by the authors which might bear even more consideration, namely, keeping the potentials of the deflection plates symmetrical with respect to ground. This is discussed in regard to the timing-axis plates for the transient analyzer, and has been provided for similarly in the circuit for the oscillograph, figure 2, where the ground potential lies midway between the time-axis deflection plates. However, would it not be equally important to maintain symmetrical potentials on the vertical (surge) deflection plates? Usual circuits with the resistance divider inherently ground one of these plates, in which case this ideal may not always be practicable. To be consistent, then, the calibration voltage must also be applied with the same plate grounded so that any nonuniformity in sensitivity will apply equally in both cases. Do the authors know of any way of obtaining a phantom ground for these deflection plates?

The practice recommended by the authors of bringing ground points of all parts of the surge generator and measuring circuits together is most heartily subscribed to. As we approach the faster wave fronts comparable with television frequencies, it may be helpful to refer to the parallel experiences of television engineers, whose similar difficulties with stray effects are being remedied by the same procedure.

P. H. McAuley (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In reviewing the section on "Surge Generation Circuits", it is interesting to note the different practices that exist in different laboratories. Concerning grounding and shielding, laboratory techniques are very similar. On the other hand, we have seldom used a resistor at the generator end of the divider cable as the authors advocate. It has never appeared necessary, even for short-time measurements, and constitutes another item requiring occasional checking for maintenance of accuracy in results. Similarly, we have avoided damping resistors in the oscillograph deflecting plate leads, always trying to control spurious oscillations at their source in the circuit, rather than damping them out in the measuring circuit. Of course, with capacity dividers it has been found necessary to use damping resistors.

The surge-generator circuit of figure 3 of the paper shows the series resistance R_1, lumped at the generator terminal and a capacitor C_2 connected in parallel with the test piece. With R_1 and C_2 variable, control of the wave front is obtained. Our practice differs in that the resistance R_1 is distributed throughout the main capacitor bank and use is thus made of the natural capacity to space of all parts at high voltage to control the wave front. Particularly for tests with the standard 1.5x40-microsecond wave this method has several decided advantages:

1. Better use is made of the generator voltage rating whereas the addition of terminal capacity definitely derates the generator in voltage. Slightly less series resistance can be used in the latter case but the net effect is appreciable loss in maximum output voltage when a shunt capacitor is used.

2. Making use of the stray capacity of the generator by distributing the series resistance is more logical than adding an expensive capacitor, which is poor economics in most cases.

3. Because of the unavoidable stray capacity, the suppression of oscillations is much more difficult when R_1 is lumped at the terminal. Mathematically and practically it can be shown that R_1 is more effective when distributed along the capacitor bank. This is where oscillations should be controlled rather than in the measuring circuit.

For these reasons, even when a shunt capacitor may be desirable for wave-front control over wide ranges, it is better to have the series resistance distributed.

One point not stressed by the authors is the effect of lead lengths in the discharge and measuring circuits. In 1938 some tests were made at the Trafford laboratory with a 10-inch rod gap in parallel with a 150-centimeter sphere gap on steep-front voltage waves. With the circuits through each gap about 40 feet in length, both gaps flashed on each impulse with the sphere spacing varied between 5.55 and 16.2 inches. At shorter or longer spacings one of the gaps only flashed. This is almost a three-to-one ratio in the sphere gap spacing. When an additional 70 feet was added to the lead to the 10-inch rod gap, 50-50 sparkover between the gaps was obtained for 22.3 inches spacing on the sphere gap and was quite critical. These results illustrate the importance of leads in the testing circuits and their possible effect on measurements. The performance of a rod gap judged by its characteristics as determined at its terminals may be greatly influenced by the addition of a few feet of lead. Conversely, in measuring the characteristics of apparatus on steep-front waves, precautions must be taken to obtain a true picture of the performance.

Morris Newman (University of Minnesota, Minneapolis): Basing our opinion on our own experience we feel in complete accord with the developments in impulse-voltage testing given in the paper by C. M. Foust and N. Rohats. We should like to add the suggestion that the cathode-ray oscillographs with which most laboratories are equipped at present could be simply converted to exterior photography of the fluorescent screen trace by the use of a miniature 35-millimeter film camera and the recently developed high-speed films. We have taken, for example, using a redesigned cold-cathode-ray high-voltage oscillograph, about 70 oscillograms on a small standard film roll, in test work, just as fast as the surge generator could be recharged, developed the film ready for inspection in five minutes and, after a half-hour of fixing, washing, and quick drying, projected it onto a large eight-foot by eight-foot screen for inspection of minute details. The oscillograph remained ready for instant use in rechecking or for further tests since it was not necessary to disturb the vacuum. We therefore very heartily concur with Mr. Foust in recommending the advantages of exterior photography.

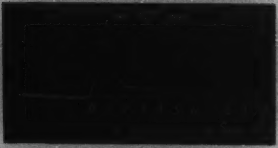


Figure 1. Arrester on 1,000 kv per microsecond

Also, while agreeing with the desirability of using symmetrical voltages on the oscillograph sweep plates when possible, we feel that such a measure minimizes the effect of nonuniform fringing field beyond the deflection plates but does not remove the cause completely, although it may do so for most practical purposes. We should like to suggest from experience that linear and constant deflections in both the timing and phenomenon voltage sweeps are obtainable by shaping the field beyond the plates, with a system of shielding surfaces and properly shaped passages, so that the beam electrons emerge through the same field conditions independently of their position in a perpendicular direction to the direction of the deflecting field. With care in controlling the fringing field we have found it possible not only to obtain constant deflection over the full sweep with voltage to just one of the plates, which is generally far more convenient, but we could then move the beam electrostatically instead of electromagnetically into any desired location by application of an easily controllable d-c biasing voltage to the opposite deflection plate.

Finally, we too have found an initiating "impulse transformer", which in our work was a Tesla coil, a useful economical substitute for a separate initiating impulse generator for synchronizing longer-time phenomena; and parallel to our experience we note that Messrs. Foust and Rohats have been quite aware, and have pointed out, that such a scheme would not be suitable for fast fractional-microsecond timing precision. But we do differ on the advisability of obtaining the desired delays for getting the oscillograph into operation through the insertion of the delaying resistance *B* in figure 2 of the paper under discussion. Such a delay in the impulse-generator switching process will, in fast front-of-wave work show up as a jog in the lower part of the voltage-wave-rise trace. As a matter of fact such delays in the first few stages of the impulse generator often occur even without insertion of resistance and we find it indeed desirable to eliminate these delays entirely by simultaneous initiation of the first three or four of the intermediate gaps rather than of only one, in order to obtain a more smooth rate of voltage rise output of the generator (figure 1 of this discussion). Therefore we should recommend a separate initiating surge-generator circuit covering the entire range of synchronization times in preference to the dual range "impulse transformer" and "impulse generator initiation delay" combination scheme if very fast front-of-wave work, such as in the testing of low-voltage equipment, is involved.

The last suggestions of this discussion are offered not in criticism, but as specialized refinements which some laboratories may possibly find will lead to further development of impulse voltage research technique. We feel that C. M. Foust and N. Rohats have done an admirable job of presenting impulse-testing developments which should go far toward eliminating discrepancies still unfortunately to be found in surge testing.

W. F. Skeats (General Electric Company Philadelphia, Pa.): In a recently installed 3,000,000-volt impulse generator, careful attention was given to many of the points mentioned in the paper by Messrs. Foust and Rohats. The general form of this generator is quite similar to that made familiar by the New York World's Fair generator, except that for the lower voltage only 30 capacitors are used and these are arranged in one unit of four columns, as shown by figure 2 of this discussion. A plan of the layout is shown in figure 3.

Figure 2. 3,000,000-volt impulse generator

Of greatest interest at the moment are the precautions taken in connection with the ground circuit for two purposes:

1. To eliminate spurious oscillations in the cathode-ray-oscillograph record, and

2. To obviate local potential differences in the ground circuit which might be dangerous to personnel or equipment.

The general principles followed in this connection were:

1. Concentration of all grounds at a single point and connection of this point by a short heavy conductor to a good earth ground.

2. Avoidance of all parallel ground connections except as required in laying more than one conduit between two points; in such cases all conduits were kept within a few inches of each other throughout their length.

3. Enclosure of all control-power wiring in metal conduit which served as the ground connection between the various parts of the apparatus.

4. Introduction of control-power wiring to the system only at the main ground point.

5. Connections on three sides from the main ground point to the building steel.

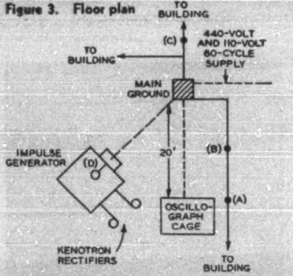
6. Enclosure of the control desk and cathode-ray oscillograph in a metal cage large enough to accommodate the operator and one or two observers. This cage used wide mesh screen on three sides and metal sheet on top, bottom, and the fourth side, all solidly connected electrically and grounded by conduit to the main ground. This is in addition to the sheet-metal housing of the oscillograph itself.

7. The deflection lead is a well-insulated concentric cable passing from the main ground to the control cage in a copper pipe from which the cable sheath is insulated except for the connection at the ground end. The control cage is connected to the

copper pipe but the oscillograph housing proper is connected to the cable sheath.

Enclosure of the control wiring in metal conduit gives it good protection from any harmful induced voltage. Electromagnetic induction may cause a potential gradient along the conductor, but except for minute differences due to displacement of the conductor from the center of the conduit or to local irregularities in the magnetic field, the same potential gradient is induced along the conduit. Thus unless there is a conducting path along which current may flow to relieve the gradient in one or the other, there is no resulting potential difference between them. Such a path has been carefully guarded against. Electrostatic induction may cause a local disturbance of potential, but again conduit and inside conductor suffer the same charge, and by skin effect the current which flows on account of this potential disturbance flows entirely in the conduit. The inductive potential gradient due to this current flow is again equal in both conduit and conductor, leaving only the resistance drop as a source of potential difference between the two. In the oscillograph deflection lead even this resistance drop is eliminated, by the copper pipe outside the sheath.

Figure 3. Floor plan



Solid lines—Ground conductors on test floor
Dashed lines—Ground conductors below test floor

From main ground, 1 / -inch copper pipe passes through floor, basement, and basement floor to a multiple driven ground

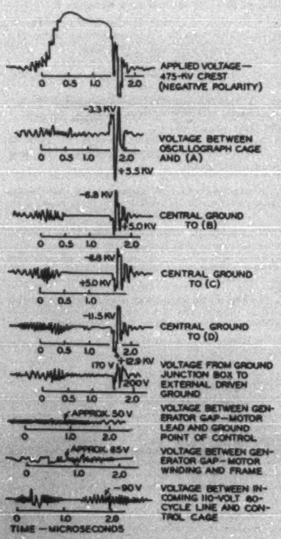


Figure 4. Voltage measurements

Thus the potential difference between wiring and the ground system may vary very little throughout the system. In order to keep this potential difference low, however, one further precaution is necessary: the wiring must enter the ground system at a point where it is subject to but little variation from true earth potential. For that reason, all wiring enters this system at the "central ground point".

In order to check the arrangement, a steep-front wave reaching a crest of 475 kv in about half a microsecond and chopped at about 15 microseconds by a 20-inch rod gap was applied and various voltage measurements made throughout the circuit. The results are shown in figure 4 of this discussion.

These results may be summarized as follows:

1. The most severe voltages occurred at the time of chopping of the wave.

2. Voltages from one to three per cent of the crest value of the applied impulse were measured over comparatively short runs of heavy conductors in the ground system. Most spectacular of these is the voltage of 5.5 kv measured between the control cage and a ground strap running less than a foot away from it and connected over a loop of about 50 feet of heavy conductor. This voltage was checked approximately by means of a sphere gap and found to be correct.

3. The potential difference between the "central ground point" and a driven ground outside the building was limited to about 300 volts—0.06 per cent of the applied wave.

4. The potential difference between frame and winding of the motor for controlling the generator gap spacing, which is located at the base of the impulse generator; the potential difference between leads to this motor and the control cage ground; and the potential difference between the incoming 60-cycle leads and the control cage ground—all were less than 100 volts—0.02 per cent of the applied wave.

Thus voltages within the system are controlled to a satisfactory extent. The relatively high voltage measured on the ground straps connecting with the building steel together with the much lower voltage between the "central ground" and outside earth seems to indicate that the major stray voltages are associated with charges induced on the building steel.

C. M. Foust and N. Rohats: Mr. Siegfried discussed the use of balanced voltages in sweep circuits which are necessary to give uniform voltage sensitivity throughout the time axis. With balanced sweeps, d-c calibration lines are parallel, with unbalanced sweeps d-c lines converge. Having a balanced sweep, a balanced deflection voltage would be desirable only from the standpoint of improving the focus. The authors have thought of two ways to obtain symmetrical deflection potentials but have not tried them as both would detract somewhat from accuracy. One way was to place the main ground point at the midpoint of the portion of the resistance divider connected across the deflection plates. In this case the current flowing in the part of the divider between ground and the surge generator would be different from that between ground and the divider because of the addition of charging currents returning from the surrounding building structure. The other way was to leave the main ground at the end of the deflection resistance and connect the oscillograph ground to its midpoint. In this case the oscillograph would be raised to one-half of the deflection voltage, thereby drawing some charging current from the divider. It is possible, however, that this would be negligible especially with a low-resistance divider.

The surge-testing equipment at Philadelphia described by W. F. Skeats incorporates ground-connection arrangements designed with great attention to detail, in view of giving a minimum of circuit oscillations and measurement circuit disturbances. The authors wish to thank Mr. Skeats for his contribution to this discussion.

Morris Newman's comments on outside photography bring to attention a number of interesting considerations. Since the fast cameras required have a very shallow depth of focus the screen would have to be essentially parallel to the film. In this position it could hardly be perpendicular to the central position of the cathode beam and this would mean some correction of the record. Another way would be to have a mirror somewhere between the camera and the screen.

The authors are heartily in agreement with the suggestion of shielded deflection plates, but a tube with such plates is not at present commercially available.

In regard to the use of a delay resistance within the surge generator for wave-front work, the authors' experiences have been that serious oscillations are not obtained. Another simple circuit arrangement has been worked out by J. W. Beatty of Philadelphia. This involves using the initiating impulse transformer the same as for slower work but having several microseconds delay in the oscillograph sweep circuit to counterbalance the delay in the tripping of the surge generator. This delay is obtained by coupling the mid sphere of the sweep trip gap to the initiating circuit through resistance instead of capacitance. The delay may be varied by adjusting the gap setting. This scheme in addition to doing all that the previous method could do has the added advantage of being able to retard the sweep so much that instead of spreading out the front of a wave on the screen some later section of the wave, such as the flashover part, may be brought out in detail. To minimize delays in the spark-over of the first several gaps of a surge generator, they should be arranged in line so that the ultraviolet radiation from the first gap will fall upon and ionize all of the remaining gaps.

The authors appreciate the comments of P. H. McAuley pertaining to the measurement and discharge circuits. The importance of surge impedance at both ends of a resistance-divider cable varies directly as the length of the cable. With cables of about 50 feet or less only the oscillograph end need have surge impedance across it. With cables of 20 feet or less, the value of the resistance across the cable becomes of no importance. The 200 ohms damping resistance shown in the deflection plate leads are too small to damp any oscillations of such frequency as may arise in the generator circuit, but are enough to suppress such high-frequency local oscillations as may arise in the deflection plate circuit.

With reference to the location of the series resistance in the surge-generator discharge circuit, the authors have found in practice that about 200 ohms distributed within the generator and the remainder R_1 placed externally give the best results. While the economic question of surge-generator efficiency is important, it is secondary to that of good waves and consistent results. On this basis the amount of resistance R required for control of oscillations generated within the capacitor bank was determined experimentally and this resistance was distributed in 20 resistors, one on each side of the ten interstage gaps in use. From this point on, the important considerations are the generation of the required wave shape, its maintenance at all voltage levels, and consistency of test results. Here the authors' experience has been that the value of capacitance across the test piece is quite significant especially for short-time tests and that extra load capacitance C_2 ranging up to ten per cent of the generator capacitance for most work and on special occasions up to 25 per cent is of decided value and justifiable despite some loss in generator efficiency. With values of generator capacitance C_1 and load capacitance C_2 in mind, the oscillograph electric-transient analyzer is used to determine the series resistance $(R+R_1)$ and parallel resistance R_2 for the wave required. With R_1 outside the generator bank, it can be conveniently changed for any wave required.

The efficiency of a generator is dependent upon wave-shape requirements. If the load capacitance C_2 consists only of generator stray capacitance, the series resistance $(R+R_1)$ must be high and efficiency suffers just as much as if C_2 were high and $(R+R_1)$ were low. With a high C_2 made up of extra load capacitance in parallel with the test sample, the wave shape will be more rigidly maintained as both high capacitance and low resistance make possible higher currents into the test piece for corona and flashover streamers. In view of the high energy rates involved in disruptive discharges, it is reasonable to expect the more rigid circuit to give values nearer that required and certainly more satisfactory for comparison purposes.

In considering comparative results it is important to bear in mind that two circumstances are involved, one having to do with comparative results on different apparatus and involving the basic question of insulation co-ordination and the other having to do with comparative results between laboratories on the same apparatus and involving testing equipment and measurements. Results are comparable only in so far as applied waves are similar at all voltage levels.

Load-Rating Theory for Multichannel Amplifiers

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Synopsis: The amplifiers of multichannel telephone systems must be so designed with regard to output capacity that interchannel interference caused by amplifier overloading will not be serious. Probability theory is applied to this problem to determine the maximum single-frequency output power which a multichannel amplifier should be designed to transmit as a function of N, the number of channels in the system. The theory is developed to include the effects of statistical variations in the number of simultaneous talkers, in the talking volumes, and in the instantaneous voltages from speech at constant volume.

IN a perfect multichannel carrier telephone system, each channel would be entirely free from interference produced by the energy present in the other channels. Since all the channels are amplified by the same repeaters, which as a practical matter cannot have perfectly linear characteristics, this is an ideal that may be approached but not completely realized. The interchannel interference must be kept down to a value which will be satisfactory for the grade of transmission concerned, further reduction being uneconomic. To do this the repeaters must meet definite load-capacity requirements and modulation (nonlinearity) requirements. The load-capacity requirement is most conveniently specified in terms of the maximum single-frequency sine-wave power which a multichannel amplifier must transmit without appreciable overloading. The modulation requirement pertains to the performance of the amplifier for impressed loads equal to or smaller than the load capacity, and specifies the allowable power in the modulation products resulting from such loads. Because of the numerous factors which affect these requirements, their determination is a rather complicated matter and the present discussion will be restricted solely to a determination of the load-capacity requirement. The object is to determine this quantity as a function of N, the number of channels in the system.

The criteria ordinarily used for determining the load capacity of single-channel amplifiers are of little use here because of two fundamental differences between single-channel and multichannel systems. In the first place, the modulation produced in a single-channel amplifier depends only upon the input to that channel and occurs only when the channel is energized. In addition, the most important frequencies resulting from modulation fall directly back upon frequencies already impressed and the net effect appears as a distortion of the original input, rather than as noise. The situation is entirely different in a multichannel system. In this case, the modulation products falling into one particular channel are in the main unrelated either to the impressed frequencies or to the volume of impressed speech in that channel. Thus it is no longer possible to think of the interference as distortion; the effect must rather be considered as that of a particular kind of noise whose level depends upon the load on the other channels of the system. For a given grade of service, the ratio of signal to noise must be much larger than the ratio of signal to modulation products resulting in distortion; thus it is to be expected that the nonlinearity requirements will be more stringent for multichannel operation than for single-channel operation.

The second fundamental difference between single-channel and multichannel systems arises from the character of the load which each system must be designed to handle. A single-channel amplifier must be capable of handling one channel at the maximum volume normally expected. Inasmuch as the amplifier will be loaded only about one-fourth of the time, even in the busiest hour, and as the averaged impressed volume will be some 15 decibels below the maximum that must

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Many members of the Bell Telephone Laboratories, in addition to those mentioned in the text, have contributed to various phases of this work. The authors take this opportunity to acknowledge their indebtedness to these colleagues, and in particular to Doctor G. R. Stibitz, who first developed the theoretical approach here used.

be provided for, the ratio of maximum to average load of such an amplifier is inherently very high. In a multichannel system, however, the several channels will very rarely be heavily loaded simultaneously. There is thus a favorable diversity factor, increasing with the number of channels, and multichannel amplifiers may accordingly be worked successfully at lower ratios of maximum to average load.

Occasionally, of course, there will be short periods of excessive loading during which the interchannel interference in multichannel systems will rise above the value normally permitted. This sort of thing often occurs when it is desired to make economical use of facilities of any kind in common. In machine-switching systems, for example, it is common practice to associate a large number of lines with a smaller number of switches and trunks. The number of switches and trunks provided is sufficient to ensure a satisfactory service, with a very small probability of requiring more facilities than are available. The multichannel amplifier problem presents a situation identical in principle, though the methods of solution are necessarily very different. The application of probability theory is evidently indicated as the method of attack.

Those characteristics of multichannel amplifiers which are important to the problem will be described first. Then a description will be given of the variables which must be taken into account in computing load capacity. Finally, the combined effects of these variables will be determined on a statistical basis to establish the required load capacity as a function of the number of channels in the system.

Characteristics of the Multichannel Amplifier

At the present time, multichannel systems of primary interest employ single-side-band transmission; the carrier frequencies are largely suppressed and different amplifiers are used for the two directions of transmission. For such systems negative-feedback amplifiers have outstanding advantages, particularly with respect to stability of gain and reduction of modulation effects, and are thus being used almost exclusively in present-day multichannel systems. The following discussion is related particularly to such systems, although many of the calculations are also applicable to less common types.

At light loads the principal modulation products in a negative-feedback amplifier increase approximately as the square or the cube of the fundamental output power. Beyond a certain critical point, however, the modulation increases very rapidly and the total output of the amplifier soon becomes practically worthless for communication purposes. This critical point will be called the "overload" point. For most tube circuits it is either the point at which grid current begins to flow, or that at which plate-current cutoff occurs. This point obviously defines the instantaneous load capacity.

Below the overload point the higher-order modulation products are negligible in comparison with second- and third-order products, and the interference may be regarded as due to the latter sources alone. Beyond the overload point, however, the higher-order products become important very rapidly and the resultant disturbances appear in most, if not all, of the channels. With given tubes, the interference below the overload point may be altered by changing the amount of feedback. The interference above the overload point, however, may be little changed in this way because of the rapid loss of feedback as the amplifier overloads. Accordingly, in designing an amplifier, the necessary load capacity may be determined solely by insuring that the output will rarely rise above the overload point, afterward adjusting the amount of feedback so that the interference below the overload point will be tolerable. There are thus two problems which may be handled separately, at least for negative-feedback amplifiers, it being understood that the results are combined in the final design. As previously stated, only the load-capacity problem will be considered in detail here but many of the methods used have been applied successfully to the interchannel-modulation problem.

The Load on a Single Channel

The total load applied to a multichannel amplifier varies rapidly between widely separated limits. A complete knowledge of the variations in the load applied to a single channel is necessary first; these variations arise from several recognizable causes which may be discussed separately.

Number of Active Channels

First of all, a single channel at a given instant may or may not be carrying speech; if not, it contributes nothing to the multichannel load. A channel will be called "active" whenever continuous speech is being introduced into it; that is, a channel is active during the time it is actually carrying speech power, and also during the short pauses that occur between words and syllables of ordinary connected speech. A channel is said to be "busy" when it is not available to the operator for completing a new call. Busy time is by no means all active time, for a busy channel is inactive during much of the time the connection is being completed, during pauses in the conversation, and finally during the time the other party is talking. The fraction of time during the busiest hour that a channel may be busy depends on the size of the group of circuits of which it is a member and on the methods of traffic operation. Measurements on circuits in large groups, made by M. S. Burgess, indicate that the largest fraction of the busiest hour that a channel may be active is about one-fourth. For channels in small circuit groups, this figure may become considerably smaller but it is unlikely that

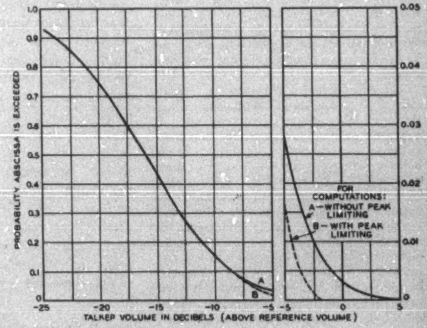

Figure 1. Talker volume distribution

any probable increase in group size or improvement in operating practices will increase it appreciably. This figure, which will be represented by τ, may accordingly be taken as a conservative estimate of the limiting probability that a channel will be active in the busiest hour.

The number of channels that are active at a given instant in an N-channel system may be anything from zero to N. Inasmuch as the channels are independent, it is possible to write down at once the probability that exactly n of them are simultaneously active. This probability is

$$p(n) = \frac{N!}{n!(N-n)!}\tau^n(1-\tau)^{N-n} \qquad (1)$$

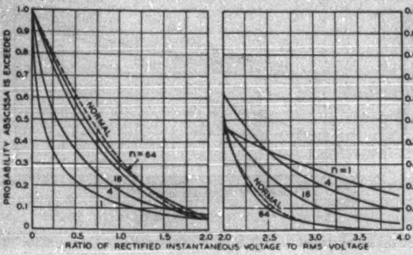

Figure 2. Instantaneous voltage distributions for n talkers

TALKING VOLUMES

A second source of variation in the load on a given channel is that the impressed volume may have any value within rather wide limits when a channel is active. By "volume" is meant the reading of a volume indicator of a standard type. Its importance in the present problem arises from the fact that the volume is an approximate measure of the average speech power being introduced into the channel. Although some other instrument might give a better measurement of the latter quantity, only the volume indicator has been used sufficiently widely in the plant to give data on the distribution of average speech power per call under commercial conditions. The average speech power is dependent on the type of instruments, the character of the speech, and the time interval over which the average is determined. From an analysis of phonograph records of continuous speech it is found that the average speech power of a reference volume talker may be taken as 1.66 milliwatts, and the relationship between volume* and average power may be expressed by the following equation:

$$\text{Volume (decibels)} = 10\log_{10}\frac{\text{average speech power in milliwatts}}{1.66} \qquad (2)$$

This equation is based on the long-average speech power. It will be understood that for purposes other than load-rating computations, a different relation might be found more suitable.

The use of equation 2 to relate volume to average speech power is applicable to speech in a single channel. It is convenient to refer to a quantity related in the same way to the total average power contributed by a number of channels as the "equivalent volume".

The single-channel volumes on commercial circuits are conveniently measured at the transmitting toll test board, which will be taken as a point of "zero transmission level". Henceforth it is assumed that there is no gain or loss between this point and the output of the amplifier, so that the latter is also a point of zero transmission level. While this will seldom be the case in an actual system, the necessary change in the load capacity is easily computed. The volumes at this point are found to be distributed approximately according to the "normal" law; that is, the probability that the volume will be between V and $V+dV$ is given by

$$p(V)dV = \frac{1}{\sqrt{2\pi}\sigma}e^{-\frac{(V-V_0)^2}{2\sigma^2}}dV \qquad (3)$$

For calls on typical toll circuits, the best present values for the parameters are $V_0=-16.0$ decibels and $\sigma=5.8$ decibels. These parameters depend, of course, upon the character of the local plant and upon the habits of telephone users, and changes in either will affect their values. Curve A of figure 1 shows this distribution of talker volumes at a point of zero transmission level. Curve B of figure 1 is the talker-volume distribution used for load-rating computations when a particular amount of peak amplitude limiting occurs in the terminal equipment. This will be discussed later. Although the mean volume is V_0, the volume corresponding to the mean power of the distribution (3) is equal to $V_0+0.115\sigma^2$, as may be seen by converting the volume scale of the distribution to power ratios, averaging, and reconverting the average to volume in decibels. For the values of the parameters given above, $V_0+0.115\sigma^2=-12.1$ decibels.

INSTANTANEOUS VOLTAGE DISTRIBUTION

Finally, the voltage in an active channel fluctuates widely even at constant volume. Not only the differences between successive syllables and the differences between vowel sounds and consonants, but also the fine structure of single sounds, are important in this connection. The total voltage impressed on the amplifier is the quantity which determines whether or not it will overload, and the phases as well as the amplitudes of the frequency components in the several channels must be considered in determining this. It is most convenient for analysis to work directly with instantaneous voltages of speech, the frequency of occurrence of the magnitudes being expressed in the form of a distribution function.

This distribution function has been measured by Doctor H. K. Dunn, using apparatus which measures 4 samples per second of the instantaneous voltage out of a commercial subset and typical loop. By operating the apparatus until about 1,000 successive samples have been measured, usable distribution curves of instantaneous voltage are obtained; this is readily checked by making repeated runs comprising the same number of samples on speech recorded on high-quality phonograph records. It is, of course, known that commercial transmitters have considerable asymmetry as regards positive and negative voltages but the poling referred to the toll board is expected to be random. As the measurements were considerably simplified by doing so, it appeared desirable to average out this asymmetry by arranging a linear rectifier ahead of the sampling apparatus to obtain equal samples of positive and negative voltages.

Such measurements have been made for a number of different talkers, different commercial subsets, and different volumes, with the speech input held at substantially constant volume in each test. The various subsets now in commercial use all give essentially the same distribution curve. The resulting distributions, if they are considered as functions of the ratio of instantaneous to rms voltage, are also nearly independent of the speech volume at the subset. Specifically, the only important effect of volume is that which may be ascribed to amplitude limiting in the transmitter; that is, to the fact that the transmitter itself has a limited load capacity. However, this effect does not appear until the volume is ten decibels or more above the mean of the volume distribution curve, and is only of importance for talkers at still higher volumes. For all lower volume talkers, the instantaneous voltage distribution may be considered as the same for all volumes when expressed as a ratio of instantaneous to rms voltage. The cumulative distribution curve of the quantity E/U, where E is the rectified instantaneous voltage and U the rms voltage, is shown by the curve $n=1$ of figure 2.

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* Subsequent to the preparation of this paper, a new volume indicator was standardized for use in the Bell System. With the new volume indicator, volume is expressed in *vu*, +8*vu* being approximately equal to reference volume (0 decibels) as used herein.

VOLTAGE LIMITING

While this curve of figure 2 is accurate for the bulk of the talkers, it changes for the high-volume talkers who overload the subset transmitters. It is also the custom to provide a certain amount of amplitude limiting in each channel by suitable circuit design of the channel terminal equipment. This limiting alters the shape of the instantaneous voltage distribution curve for a range of voltages below the maximum, the extent of the modification depending on the talker volume and the characteristics of the limiting device. Its effect on the load capacity will be considered later.

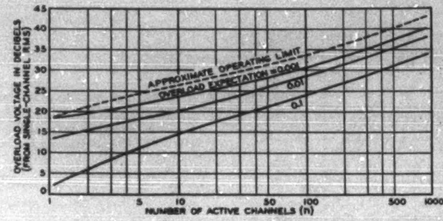
Multichannel Instantaneous Voltage Distributions

The number of variables with which it is necessary to deal makes the general load-capacity problem rather a complicated one. The analysis will be easier to follow if the effects of the different variables are taken up one at a time, thus building up a complete theory in successive steps. To do this, it is advantageous to start with a case so simplified that it rarely, if ever, occurs in ordinary practice; that is, that in which the volumes in all the channels are regulated to a common constant value, and in which the number of *active* channels is also kept constant. For this condition, it is necessary to consider only the effects of the distribution of instantaneous voltages in each channel. This distribution curve is the same for all of the channels, since all are at the same volume, but the voltage in any channel at a particular instant is entirely independent of the condition of the other channels.

OVERLOAD EXPECTATION

The total voltage impressed on the amplifier by a number of channels at a given instant is the sum of the instantaneous voltages in the separate channels. Since disturbances will be produced in many of the channels when the applied voltage goes beyond the overload point, it will be useful to know the fraction of the time that this may be expected to occur; this fraction will be called the overload expectation and denoted by ϵ. It is important to notice that this quantity ϵ is not necessarily the fraction of the time during which the performance of the amplifier will be unsatisfactory. This might perhaps be the case for a device having an instantaneous cutoff characteristic, but for an ordinary amplifier the time constants (among other things) affect the results of overloading. The interpretation of the overload expectation will be discussed further later; consideration must be given first to how it is obtained.

Figure 3. Overload voltage for n active channels



THE n-CHANNEL VOLTAGE DISTRIBUTION

The load in each channel is applied at voice frequency to the input side of a modulator, the voice-frequency instantaneous voltage distribution being as shown by the curve $n=1$ of figure 2. The overloading of the amplifier is determined, however, by the distribution of the sum of n such voltages after each has been shifted by the modulator to the appropriate carrier frequency, one sideband being suppressed. It may be shown that if the phases of the various components of the voice-frequency input were random, the distribution of instantaneous voltage at side-band frequency would be identical with that measured at voice frequency. It is known, however, that the phases at voice frequency are not entirely random, and there may thus be differences between the two distributions. The results of a number of tests bearing upon this point indicate that any error resulting from the use of the distribution measured at voice frequency will be small for systems of few channels, and will rapidly disappear as the number of channels is increased.

Theoretically, the resultant n-channel voltage distribution can be derived from the single-channel distribution by straightforward analytical methods; in the present case, however, expression of the result in useful form is very difficult because of the form of the single-channel curve. This difficulty might be resolved by using graphical or numerical methods, as applied later to the volume-distribution curves; fortunately, the fact that the voice-frequency voltage distributions may be used throughout permitted the resultant n-channel distributions to be obtained much more easily. Since the addition of voltages from the several carrier channels does not depend materially upon the frequencies at which the channels appear in the system, the addition of n channels at voice frequency will give the desired n-channel distribution directly. M. E. Campbell effected this addition by the use of phonograph records, the n-channel distributions being determined by means of the instantaneous-voltage sampling apparatus previously mentioned.

As material for this process, 16 high-quality phonograph records were made of the outputs of commercial subsets through representative subscriber loops. Both male and female voices were used. The speech was furnished by reading magazine stories containing considerable conversational material, due precautions being taken that the volume on each record was substantially constant throughout. A calibrating tone was cut on each record to enable it to be played at any desired volume and most of the volumes recorded were well below the point at which the transmitter began to act as a voltage limiter.

These individual records were then combined in groups of four, with all records adjusted to the same volume by means of the calibrating tones, and rerecorded. Several such 4-voice records were made; by combining them again in the same way, 16-voice records and finally 64-voice records were obtained. The instantaneous voltage distributions were measured before and after each rerecording to insure that the recording process introduced no errors. A few minor discrepancies were found, but all were small enough to be disregarded. Each single-voice record appeared several times in a 64-voice record, but since the phases of its different appearances were random, this had no appreciable effect on the resultant voltage distribution. This was verified by comparing the voltage distributions of the various possible 16-voice combinations. By this process n-channel voltage distributions were obtained for $n=1$, 4, 16, and 64.

These distributions, together with a normal curve, are shown in figure 2 in cumulative form. To show the curves conveniently to the same scale, it has been necessary to plot for each case not the distribution of E, the rectified instantaneous voltage, but that of E/U, where U is the rms voltage. The rms voltage, it will be remembered, is directly related to the equivalent volume by equation 2. The figure shows clearly the gradual transition from the single-channel distribution to the normal one for large n, and also indicates that for 64 active channels the curve is normal within the precision of the measuring apparatus. Hence, the normal distribution may justifiably be used for any value of $n>64$.

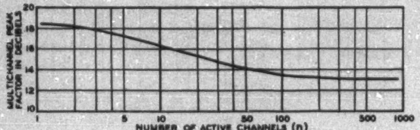


Figure 4. Multichannel peak factor for n active channels

Further significance is accorded the above data by plotting the ratio of the voltage exceeded a fraction ϵ of the time to the single-channel rms voltage, as a function of the number n of active channels, for several fixed values of ϵ. From the data given, points on such curves can be obtained for $n=1$, 4, 16, 64; furthermore, the fact that the distribution for $n>64$ is normal permits drawing the asymptote for large values of n. The points read from figure 2 and replotted in this way give the full lines shown in figure 3.

In order to make practical use of these curves, it is necessary to know what value of ϵ corresponds to satisfactory performance of the amplifier. Experiments have been conducted on a number of different multichannel amplifiers, each loaded by various numbers of active channels all at the same volume. It has been found that for low enough values of ϵ, no audible disturbance is produced but that as ϵ is increased by increasing the load on the amplifier, the disturbance falling into a channel not energized increases rapidly to a large value. Two different amplifiers having the same computed load capacity may show noticeable differences in performance in this respect when subject to identical fixed loads of the type being considered, thus indicating the influence of circuit design on the value of ϵ. In general, however, the allowable values of ϵ measured for all of the amplifiers that have been tested lie in a relatively narrow band on either side of the curve for $\epsilon=0.001$. The broken curve of figure 3 represents the approximate upper limit of the observations, extrapolated parallel to the $\epsilon=0.001$ curve above $n=14$. It is possible that some amplifiers would overload even if operated in accordance with this curve, but for the great majority of amplifiers of types thus far tested the operation would be satisfactory, with perhaps a small margin.

MULTICHANNEL PEAK FACTOR

It is useful at this point to introduce the concept of "multichannel peak factor," which is defined as the limiting ratio of the overload voltage to the rms voltage for a given number of active channels at constant volume. The ratio of the overload voltage for n active channels to the rms voltage of one active channel is given directly by the broken curve of figure 3, and the rms voltage for n channels is simply $\sqrt{n}$ times that for one channel. A simple computation then gives the multichannel peak factor. This is plotted in figure 4 as a function of the number of active channels n. The reduction in multichannel peak factor as the number of active channels increases reflects the transition from the single-channel distribution curve to the normal curve, as depicted in figure 2.

The Distribution of Equivalent Volume

The multichannel peak factor deals only with the effects of changes in the instantaneous voltages of the channels, all other variables being fixed. It is next necessary to extend the treatment to include the effects of the other load variations that occur in practice—those in number of active channels and in channel volumes. It is important, first of all, to notice that the instantaneous-voltage variations occur very rapidly, while changes in the other two quantities are, in comparison, very slow. In the experiments described above, the loads were so fixed that the equivalent volumes could be changed only by changing the operating transmission level of the amplifier; in practical cases the amplifier transmission level is kept fixed, but the equivalent volume is constantly changing because of changes in number of active channels and in channel volumes.

The amplifier is thus loaded with a constantly changing equivalent volume but because of the great difference in the time-scales of the two classes of variations the load may be regarded as a succession of equivalent volumes, each constant for a small interval of time that nevertheless is long enough to include a representative sample of the resultant instantaneous voltage distribution. If the distribution function for equivalent volume is computed, and then corrected by the multichannel peak factor, the fraction of such intervals during which the amplifier will be unsatisfactory from the standpoint of overloading may be determined. For a particular amplifier, the operating transmission level must be so chosen that this fraction will be small enough to make any adverse effects on transmission unimportant. For systems of very many channels the proper value of this fraction is probably about one per cent. During the busiest hour, this corresponds to 36 seconds during which audible interference *may* occur and as this will be broken up into many very short intervals, the total effect should be slight. For systems of very few channels, the equivalent volume may reach objectionably high values during these intervals and it might be necessary to make this fraction smaller than one per cent to secure good performance. For illustrative purposes, the one per cent figure will be used in what follows without implying that it may not need alteration in some cases. The methods used are applicable no matter what value is chosen for the fraction of time overloading is permitted.

CONTROLLED VOLUMES

As the simplest case to which the above procedure may be applied, and one that may occasionally be of practical interest, consider a commercial system with all the channels controlled to the same volume. If there are N channels in the system, the probability that exactly n channels will be active at any given time is given by equation 1, with $\tau = 0.25$. By computing the value of $p(n)$ for all values of n, and taking the cumulative sum, the value of n which makes the sum 0.99 (or the next greater n) is readily determined. This determines the number n of active channels that is exceeded one per cent of the time. A plot of these values of n is given by the curve of figure 5, as a function of N, the number of channels in the system. For small values of N this curve has been drawn in a manner to smooth out the steps introduced because n must of necessity be an integer and when the value of n read from the curve is not an integer, the next higher integral value is to be used. It is of interest to compare this curve with the two straight lines of the figure. The lower straight line represents the asymptote for sufficiently large N and the upper straight line is for the condition where all channels are active simultaneously ($n = N$).

The average power for n channels is n times that of one channel, and the equivalent volume expressed in decibels is 10 $\log_{10} n$ above that in one channel. The equivalent volume may thus be computed as a function of n, and by means of figure 5, as a function of N. Curve A of figure 6 shows the values of equivalent volume so determined as a function of N, the number of channels in the system; it applies specifically to the case where the volume of each of the active channels is controlled so as to be 12.1 decibels below reference volume. The choice of this particular volume is purely arbitrary, but it corresponds to the average power of the single-talker volume distribution.

The equivalent volumes given by curve A of figure 6 are a measure of the average power of the N channels, as computed by means of equation 2. To determine the required instantaneous load capacity of the system, the average power must be corrected by the multichannel peak factor which is read directly from figure 4, using for the number of active channels the values read from figure 5.

For design purposes, it is more convenient to use the rms power of the single-frequency test tone whose peak value represents the instantaneous load capacity. As the ratio of the peak to rms power of a single-frequency tone is three decibels, this test power is obtained by subtracting three decibels from the instantaneous load capacity. This required test-tone capacity is plotted as a function of N in curve A of figure 7, which gives the output capacity required for an N-channel system with volume control as specified above.

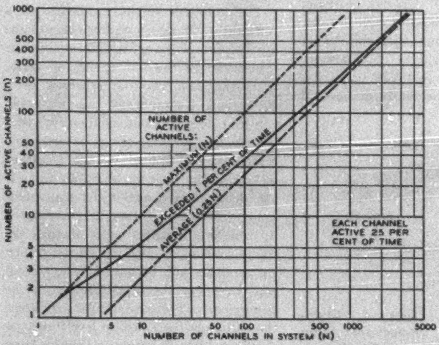
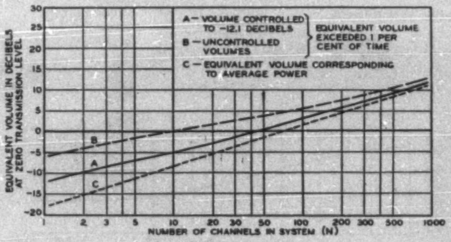


Figure 5 (left). Number of active channels as a function of the number of channels in the system

Figure 6 (below). Equivalent volume for systems of N channels



UNCONTROLLED VOLUMES

For systems in which volume control is not used, the application of this procedure becomes more involved. To study this more general case, it is convenient first to interchange the conditions of the preceding section, letting the number of active channels be fixed at any value n and examining how the distribution curve of equivalent volume may be obtained for this fixed number of channels. The relation between volume and average speech power given in equation 2 may be rewritten for this case in the form

$$V_i = 10 \log_{10} \frac{W_i}{W_0} \text{ decibels}$$

where $W_0 = 1.66$ milliwatts, W_i is the average speech power in milliwatts, and V_i is the volume in decibels for any one of the active channels, all at a point of zero transmission level.

Likewise, the relation between equivalent volume and average speech power for n active channels is given by the expression

$$V = n\text{-channel equivalent volume} = 10 \log_{10} \frac{\sum_1^n W_i}{W_0} \text{ decibels}$$

Since the distribution of the channel volumes V_i is known and the volumes of the various channels are independent, the straightforward procedure to obtain the distribution of the n-channel equivalent volume V would involve the following steps: (1) the obtaining of the distribution function of W_i by a transformation of that of V_i; (2) the calculation of the distribution function for the quantity $Y(n) = \sum_1^n W_i$; (3) the transformation of the $Y(n)$ distribution to that of V by inverting the process used in step 1.

The difficulties in this process are all in the second step, where, having given $p_1(W)$, the distribution of average powers for a single channel, it is required to obtain $p_n(Y)$, the distribution for n active channels, with Y defined in terms of W by the relation given immediately above. The formal solution requires the evaluation of integrals of the following type:

$$p_n(Y)=\int_0^Y p_{n-k}(W)p_k(Y-W)dW$$

By successive calculation of such integrals for $n=2, 4, 8 \ldots$, taking k each time equal to $n/2$, the required distributions may be obtained for the necessary range of values of n.

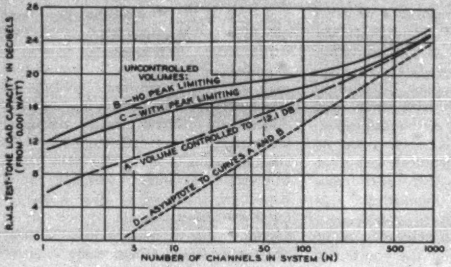


Figure 7. Load capacity for systems of N channels

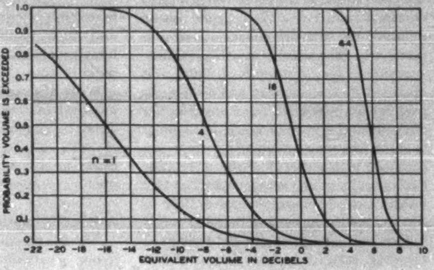


Figure 8. Equivalent volume distributions for n active channels

As in the case of the instantaneous-voltage distributions, it has not proved feasible to perform the integrations analytically. It was necessary to resort to numerical evaluation of these integrals; by combining the transformations in steps 1 and 3 with the process of evaluating the integral, the process was somewhat shortened. In this way equivalent volume distributions have been obtained for $n=2, 4, 8, 16 \ldots$; needed points on the distribution curves for intermediate values of n are obtainable by interpolation.

The accuracy of such a process depends upon the number of division points used in the numerical integration and this as a practical matter must be kept fairly small. When the process must be repeated many times, the errors introduced at each step may accumulate and lead to inaccuracies for large n. It is thus desirable to have some control over the accuracy other than by repeating the calculation with a larger number of division points. This is provided by calculating the moments of $p_n(Y)$ from those of $p_1(W)$ without the use of numerical integration.

The moments S_k of $p_1(W)$ are defined by

$$S_k=\int^{\infty} W^k p_1(W)dW$$

and the moments $T_k^{(n)}$ of $p_n(Y)$ similarly. By the use of the semi-invariants of Thiele,* it may be shown that

$$T_1^{(n)}=nS_1$$

$$T_2^{(n)}=nS_2+n(n-1)S_1^2, \text{etc.}$$

By comparing the moments of the distributions obtained by numerical integration with those calculated in this way, and making occasional minor alterations in the curves to bring the first and second moments into agreement, assurance was obtained that all the distributions used are reasonably accurate, with no accumulation of error as n becomes large.

Examples of the cumulative distribution curves of equivalent volume for 1, 4, 16, and 64 active channels are given in figure 8. The decrease in standard deviation which occurs as n increases is of interest for it indicates how the fluctuations in load due to talker volume variations are reduced by combining a large number of channels in one system.

Having now n-channel equivalent volume curves for a range of values of n, the resultant equivalent volume curves may be calculated when the restriction to a fixed number n of active channels is removed. Let $p_n(V)$ denote the probability that, with n channels active, the equivalent volume lies between V and $V+dV$ and let $p(n)$ denote the probability that just n channels *will* be active. Then the total probability that the equivalent volume will be between V and $V+dV$ is given, for an N-channel system, by

$$p(V)=p(1)p_1(V)+p(2)p_2(V)+\ldots.+p(N)p_N(V)$$

The $p_n(V)$ are given by equivalent volume curves such as those in figure 8 and the $p(n)$ by equation 1. Examples of curves thus computed are given in figure 9, which shows the equivalent volume distributions at a point of zero transmission level for 3, 12, and 240 channel systems. The equivalent volume that is exceeded one per cent of the time, read from such curves, is plotted as curve B of figure 6.

This curve gives, for any number of channels having uncontrolled volumes, the equivalent volume which will be exceeded just one per cent of the busy hour. To obtain the necessary load capacity, this must be corrected for the multichannel peak factor. In the controlled-volume case, for a given number N of channels in the system, there was no difficulty in deciding the value of n, the number of simultaneously active channels, for which the multichannel peak factor should be taken. Now, however, there is no unique relation between equivalent volume and the number n; in addition, the multichannel peak factors were measured with all n channels at the same volume, which represents a condition rarely holding on a system without volume control. It is apparent, however, that in the majority of cases in which the equivalent volume approaches values on curve B of figure 6, the number of simultaneously active channels will be greater than the average number $N\tau$ of active channels. Since the multichannel peak factor decreases as n increases, the peak factors for $n=N\tau$ active channels may be safely used. A more detailed analysis, feasible only for very small systems but avoiding the use of this approximation, shows that its effect is small and tends to give load capacities slightly higher than actually required, but the difference diminishes rapidly as the size of the system is increased.

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* T. N. Thiele, "The Theory of Observations," 1903; reprinted in *Annals of Mathematical Statistics*, volume 2, 1931. See especially sections 22, 29.

For the uncontrolled volume condition, therefore, the multichannel peak factors are read from figure 5 for values of $n=N_T$. They are added to the equivalent volumes obtained from curve *B* of figure 6, and reduced to single-frequency power as previously described for the volume-controlled case. Curve *B* of figure 7 is obtained in this manner and shows the load capacity required in an amplifier for an *N*-channel system in which the volumes of each channel are distributed in accordance with curve *A* of figure 1. The load capacity which is approached asymptotically as *N* increases indefinitely is represented by curve *D* of figure 7.

The load capacities given by figure 7 are valid only for systems for which the basic single-channel data apply. As these may not hold in specific cases, and may be subject to modification in the future, estimates of the effects of small changes in these data are useful. These effects cannot be described simply for moderate numbers of channels but for large numbers of channels the effects are readily estimated from the change in the location of the asymptote shown on figure 7. The equation of this asymptote is as follows:

$$L=10\log_{10} N_T+(V_0+0.115\sigma^2)+MPF+P_0-3 \text{ decibels}$$

where

L = test-tone load capacity
MPF = asymptotic multichannel peak factor
P_0 = long average power of a reference volume talker in decibels above 0.001 watt

The other quantities are as defined before.

Peak-Voltage Limiting

The curves referred to in the preceding discussion have so far neglected the effects of peak-voltage limiting in the transmitters and in the channel terminal equipment. Fundamentally, the effect of such limiting is to modify the distribution of instantaneous voltages in the individual channels. The extent of the modification, however, depends on the volume. For single-channel systems it is obvious that the improvement in load capacity due to limiting will be substantially equal to the reduction in the maximum peak voltage. For a large number of channels the improvement will approach the reduction in the rms voltage per channel. An approximate method of accounting for these complicated reactions is to consider that peak-voltage limiting modifies the upper end of the single-channel volume distribution. Strictly the amount of such modification is a function of the number of channels as well as of the characteristics of the limiters. Curve *B* of figure 1 represents a compromise between the different effects which is believed to give reasonably accurate results for both small and large numbers of channels for the limiting characteristic of present terminals.

With the talker volume distribution modified in accordance with curve *B* of figure 1, computations of the load capacity with voltage limiting present may be made in a manner identical with that previously described. Curve *C* of figure 7 shows the results obtained for this amount of limiting.

All of the load capacity curves of figure 7 are based on the equivalent volume which would be exceeded one per cent of the time, irrespective of the number of channels in the system. Where voltage limiting is used, it appears reasonable to consider this percentage as fixed because the action of the limiters serves to restrict the range of voltages above the overload point, thus reducing the severity of any overloading effects. When there is no limiting, and particularly for a small number of channels, the range of overloading voltage is not so restricted and overloading effects may become undesirably severe during the one per cent of time when the overload voltage is exceeded. If voltage limiting is not provided in some form, it may be important to reduce the percentage of time during which overloading may occur for small numbers of channels. This is a matter to be determined by experience and, if necessary, would require modification of curve *B* of figure 7 in the direction of requiring more load capacity for a small number of channels, thus increasing the spread between curves *B* and *C*.

Operating Margins, Etc.

The curves which have been given for output capacity versus number of channels apply to a single amplifier, or to a system in which all amplifiers are identical and work at the same output level without appreciable impairment of over-all performance. In practice, the number of amplifiers in tandem n a long system may be very large and problems of equalization and regulation may make it difficult to maintain exactly the same level conditions at all amplifiers. In addition, aging of tubes, and other effects will introduce some impairment. It is important, therefore, to allow a margin for these effects in the design of an amplifier for a multichannel system. The proper margin is essentially a matter of system design and it is often economical to build a liberal margin into the amplifiers in order to allow greater latitude and economy in the design of equalizing and regulating arrangements.

In addition to the speech loads, there are also impressed on the amplifiers various signaling and pilot frequencies, carrier leaks, etc. It is not always possible in practice to make these negligibly small and the load-capacity requirements must be corrected to allow for their presence. Multichannel telephone systems are also required to transmit other types of communication circuits, such as program channels and voice-frequency telegraph systems, superposed on one or more telephone channels. Modifications of the methods applied to speech loads may readily be made to determine the effect of these on the amplifier load capacity.

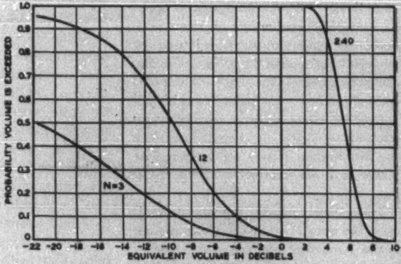

Figure 9. Equivalent volume distributions for systems of N channels

An Unstable Nonlinear Circuit

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ASSOCIATE AIEE

An iron-core reactor in parallel with a capacitor in a series circuit with some kind of impedance will exhibit unstable characteristics under certain conditions. A change in one or more of the circuit parameters will cause the current or voltage associated with some element of the circuit to change suddenly from one stable value to another. Between these two conditions no value of current or voltage will satisfy all of the elements of the circuit simultaneously.

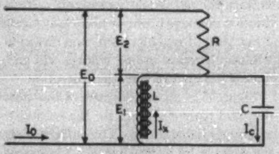

Figure 1. The parallel type of nonlinear circuit

The instability of certain types of circuits has been encountered in the past, especially on three-phase metering circuits. In general this is an undesirable condition, but there are several methods by which some practical use may be made of the unstable characteristics of the parallel reactor and capacitor circuit.

An accurate mathematical analysis of the circuit is very difficult because the exact equations of magnetic phenomena, and of the current and voltage wave forms, are quite complicated. Only a simplified theory is considered, and the discussion is based largely on the data taken on typical test circuits.

Circuit Characteristics

A circuit similar to figure 1 where the series impedance consists of pure resistance is the most simple form of the circuit. The measured characteristics of the reactor and capacitor as well as their combined volt-ampere curve are shown in figure 2. As the series resistance varies from a maximum to a minimum and returned to its highest value, at a constant line voltage, the voltage common to the reactor and capacitor follows the curve in figure 3. With decreasing values of resistance the voltage E_1 follows the curve VAX to the critical point X. A further incremental decrease in resistance causes the voltage suddenly to change from 49 volts to 102 volts and the line current likewise suddenly changes from 0.60 amperes to 0.14 amperes. If the resistance is further decreased, the voltage will continue along the curve YN, but if the resistance is increased the voltage will proceed along the curve NYZ to a second critical point Z where there is again a sudden readjustment of voltage—this time a collapse from 96 volts to 34 volts, and the current changes suddenly from 0.21 to 0.50 ampere.

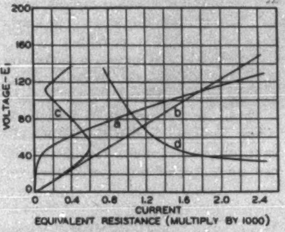

Figure 2. Characteristic curves of parallel branch of the nonlinear circuit, obtained from measurements

a—Reactor volt-ampere curve
b—Capacitor volt-ampere curve
c—Volt-ampere curve of combined reactor and capacitor
d—Equivalent resistance of reactor

Referring again to figure 3, the stable conditions along that portion of the curve VAX is called a substable state and is characterized by relatively low flux density in the reactor, good wave forms of current and voltage in all elements of the circuit, and by the line current leading the reactor voltage. After the circuit passes through the unstable period, stable conditions again prevail along the curve NYZ, which is called a hyperstable state. It has associated with it a relatively high flux density in the reactor, distorted wave forms, and in general the line current lags the reactor voltage.

Thus, there is a hysteresis-loop effect where the two critical points do not coincide. It has been found that the area within this hysteresis loop can be varied by changing one or more of several factors such as line voltage, the relation between the reactor and capacitor volt-ampere curves, or the equivalent resistance of the parallel circuit. The effect of varying the line voltage is shown in the group of curves in figure 4.

It has been observed that the unstable phenomena may be produced under certain conditions by a variation in any one or more of the quantities such as reactance, capacitance, series impedance, line voltages, or frequency. Thus, in the circuit considered above, figure 4 shows that instability will be encountered between 115 and 125 volts if the series resistance is fixed at 170 ohms. However, figure 4 also shows that the circuit will not become unstable for a variation in resistance or line voltage providing the latter never exceeds 100 volts. It is evident, therefore, that there are some factors that govern the instability of the circuit, and these can be understood more easily from a simple graphical analysis of the above circuit.

Circuit Theory

Figure 2 gives sufficient data to permit an approximate calculation of the instability curves as illustrated by an example in the appendix. The calculated curve in figure 5 indicates that the curve of series impedance has a maximum and minimum point, and that these agree reasonably well with the critical points of figure 3 for a line voltage of 125 volts. No correction was made for the distorted

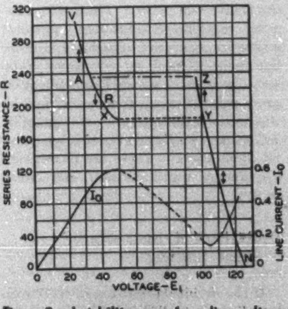

Figure 3. Instability curve for a line voltage of 125 volts

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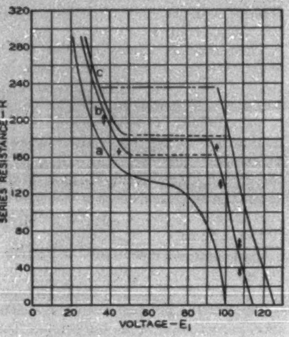

Figure 4. Effect of line voltage on instability

a—Line voltage 100 volts
b—Line voltage 115 volts
c—Line voltage 125 volts

waves associated with the hyperstable condition, hence the greater discrepancy between the maximum and the critical point *Z*. The impedance curve for a line voltage of 100 volts does not contain maximum and minimum points; thus it may be concluded that the primary requisite for instability is that there must be such a relation between the various elements of the circuit that there will be a maximum and minimum point in the curve of series impedance plotted against the voltage common to the reactor and capacitor. Mathematically, the first derivative of an equation of *R* in terms of E_1 will show the presence of instability and the location of the critical points. If a different relation between the reactor and capacitor volt-ampere curves is chosen, the maximum and minimum points will occur at different values of series resistance and a different value of line voltage must be maintained before instability will occur. Thus, the reactance, capacitance, and their equivalent resistance, the series impedance, and the line voltage must be correlated if instability is to be avoided or deliberately produced. There are three probable fields where the characteristics of instability deliberately produced may have some useful application. These are phase shifting, energy transformation, and circuit control.

Utilization of Instability—Phase Shifting

If the circuit represented by the curves in figure 2 is applied to a line voltage 160 volts or higher, the highest value of E_1 in the substable range is about 55 volts. The capacitor current, curve *b*, is substantially greater than the reactor current at this point hence the line current, curve *c*, is leading the line voltage. The lowest value of E_1 in the hyperstable state may be 120 volts where current in the reactor is now greater than that in the capacitor and the line current lags the line voltage.

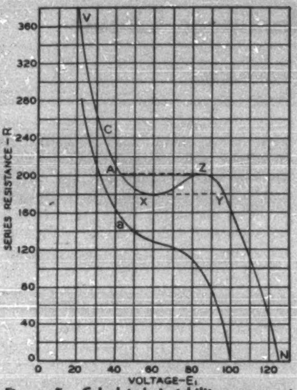

Figure 5. Calculated instability curves at line voltages of 100 volts (a) and 125 volts (c)

Thus, the line current in a nonlinear circuit, designed so that the hyperstable state will be well above the intersection of the reactor and capacitor volt-ampere curves, will shift from a large leading angle to a large lagging angle when passing into the hyperstable state. A reversing motor may be made by inserting this nonlinear circuit in series with one winding of a split-phase motor as in figure 6. A change in line voltage, or any other factor which will cause a shift from one stable state to the other, will cause a reversal of rotation. In fact, if the impedance of the series winding changes sufficiently with speed, the motor will continuously oscillate, first in one direction, then in the other. The number of revolutions in each direction depends on the value of line voltage, and at one value of voltage the net revolutions is zero. A reduction of one per cent or more may cause a net gain of revolutions in one direction while an increase in line voltage will produce a net gain in the other direction.

Figure 7 is an oscillogram of line current and reactor voltage for the substable and hyperstable states. The current leads the voltage by approximately 75 degrees and three cycles later it lags by about 60 degrees.

Energy Conversion

A nonlinear circuit of a little different construction from those previously mentioned is shown in figure 8. The reactor not only acts as an autotransformer, but it acts as a solenoid to raise a plunger. Let the line voltage, frequency, series impedance, and capacitance remain con-

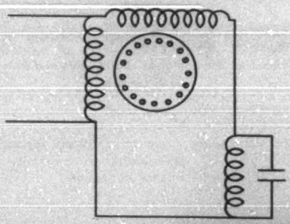

Figure 6. (above). Split-phase motor with unstable circuit

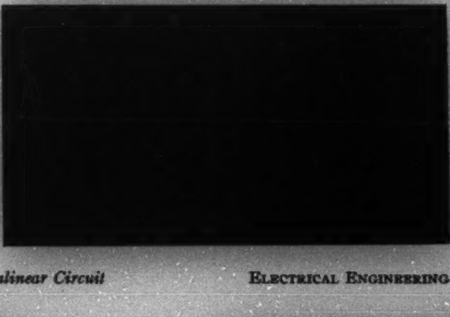

Figure 7. Oscillogram of transition from substable state (on left) to hyperstable state (on right)

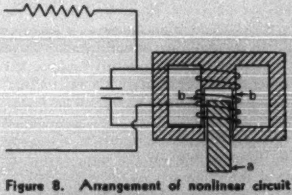

Figure 8. Arrangement of nonlinear circuit to produce reciprocating motion

a—Movable core b—Saturated section

stant and assume that the reactor operates in the substable state when the plunger is in the solenoid. The flux density in the reactor is low and the majority of the flux passes through the saturated section of steel, *b*. The small amount of flux passing through the plunger does not create a sufficient magnetic pull on the plunger to hold it against gravity or some other external force. Consequently, an external force will pull the plunger out of the solenoid and in doing so, the equivalent reactance of the circuit is changed sufficiently to cause the circuit to pass into the hyperstable state. The high voltage across the reactor then produces a sufficiently high flux to overcome the external force and the plunger moves into the solenoid. The forward motion of the plunger, however, causes the circuit to return to the substable condition, the attraction is released, and the external force again removes the plunger. Reciprocating motion is thereby produced and maintained as long as the motion of the plunger is capable of causing a transfer from one stable state to the other. The action is more easily understood from the hysteresis-loop characteristic curves in figure 9 where plunger displacement is plotted against magnetizing current in the reactor. This curve indicates a large difference between the attractive force for the inward and outward stroke.

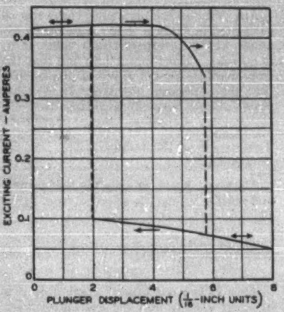

Figure 9 (left). Unidirectional pull on plunger of unstable circuit

Plunger moving into solenoid ⟶

Plunger moving out of solenoid ⟵

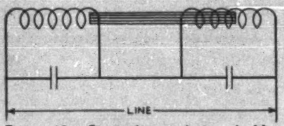

Figure 10. Circuit for producing double-acting reciprocating motion

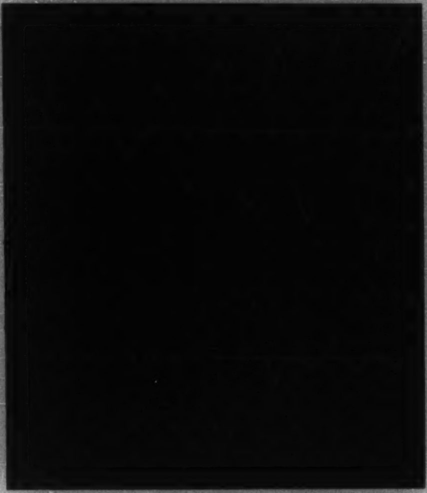

Figure 11. Oscillogram showing change in exciting current during motion of plunger

Top—Plunger moving out of solenoid

Bottom—Plunger moving into solenoid

E_0 = line voltage, E_1 = reactor voltage, I_x = reactor current

If two similar reactors are connected by a common core as in figure 10, a continuously reciprocating system is obtained with power delivered during each stroke. In this circuit each reactor and capacitor group acts as a series impedance for the other. A reciprocating air compressor with a laminated iron core mounted on each end of the piston was inserted between two units. A pressure of 75 pounds per square inch was established in approximately two minutes in a container having a volume of 0.1 cubic foot, with the circuit oscillating at 240 strokes per minute. When the maximum pressure was obtained, the reciprocating motion ceased because the back pressure on the piston was equal to the magnetic attraction on the plunger. The power consumed under the stalled condition, was about one-fourth of its value when the unit was in motion. When the air pressure was reduced to a value sufficiently low for the attractive force to overcome this pressure and the static friction, the pump automatically started.

The frequency of the supply circuit affects the operation of reciprocating motion to some extent because it tends to change the period of oscillation of the plunger, and the area of hysteresis loop. For the circuit on which the tests were made, however, the frequency could be varied 20 cycles or more and the line voltage could be varied over a range of 100 per cent without causing the reciprocating motion to stop. The frequency of reciprocating motion depends not only upon the mechanical inertia but upon the electrical constants of the circuit. A change in capacitance or equivalent resistance in parallel with a nonlinear reactor will change the period of reciprocation within certain limits.

Oscillograms in figure 11 show the rapid rate change of exciting current in the reactor when the core is suddenly withdrawn, and inserted.

A circuit similar to figure 8 may be used for electric bells, gongs, or signals without electrical contacts. The transformer *A* shown in series with the reactor and capacitor *B* in figure 12 is used instead of the ordinary bell transformer. The internal impedance of this transformer on open circuit is so high that instability cannot occur in the nonlinear circuit. When the secondary of the transformer is closed, however, the series impedance is reduced to a value which establishes reciprocating or vibrating mechanical motion.

Circuit Control

The nonlinear circuit may find many applications in controlling circuits but only a temperature control will be mentioned as an illustration.

The circuit shown in figure 8 may be a relay circuit in which a nonlinear reactor is used as the relay itself, that is, the plunger may carry the relay or circuit-breaker contacts. If the area of the plunger and saturated section are properly proportioned a continuous attractive force may be maintained instead of the reciprocating action. In other words, the motion of the core would not cause the system to change from one stable state into the other; but instability could be controlled by a variation in line voltage, series impedance, or some other suitable factor. A thermostat placed across the series resistance of the nonlinear reactor could control a power circuit without intermediate contactors or amplifiers. The important factor is that a small variation in the series impedance is amplified to produce a large and sudden variation in the voltage across the reactor which in turn influences the pull on the plunger.

A temperature-control circuit may be arranged as in figure 13 in which two series resistors have equal and opposite temperature coefficients of resistance. Tests on constant line voltage and frequency indicate that an increase of 0.4 per cent in one resistor and a corresponding decrease in the other resistor will cause the plunger to shift from the one reactor into the other. Thus, a temperature-control system could be operated without a thermostat or contact and would be relatively sensitive to temperature changes providing the line voltage and frequency remained constant. Two metals having practically equal and opposite temperature coefficients of resistance are titanium and zinc. The temperature sensitivity at constant line voltage and frequency in such a system would depend upon the value of temperature coefficient necessary to produce the required change in resistance. The 0.4 per cent change mentioned above would require a temperature change of less than two degrees centigrade to shift the plunger from one coil to the other if the series resistors were made of titanium and zinc. The effect of the line voltage and frequency depends upon the actual change occurring in series impedance. For example, if the temperature control system is set up as in figure 13 with a thermostat added as shown by the dotted lines, which short-circuits as much as 20 per cent of one resistor or the other; the effect of line voltage and frequency becomes negligible. An actual circuit operated satisfactorily over a 10-cycle change in frequency and a 20-volt variation in line voltage.

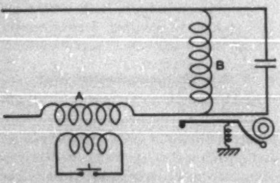

Figure 12. Unstable circuit for an electric bell without contacts

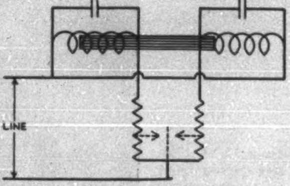

Figure 13. Temperature control circuit

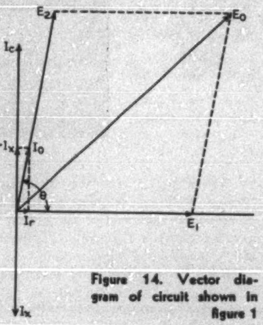

Figure 14. Vector diagram of circuit shown in figure 1

Summary

An instability curve of series impedance versus voltage common to the parallel branch of the circuit as in figure 1 can be calculated approximately. If this curve contains maximum and minimum points, the circuit will have two states of stability with an intermediate unstable range. Some practical use of this phenomenon has been suggested for circuit control and energy conversion. The circuit which produces reciprocating motion has not been completely analyzed, hence the optimum operating conditions cannot be stated until further knowledge is obtained.

Appendix

Any point on the instability curve can be calculated in figure 5 as in the following example which corresponds to a value of E_1 of 80 volts. From figure 2 we find that the measured line current I_0 is 0.42 ampere and that the equivalent resistance R_e is 1,090 ohms. The equivalent impedance is

$$Z_e = \frac{E_1}{I_0} = \frac{80}{0.42} = 190.5 \text{ ohms} \tag{1}$$

The phase angle between the line current and reactor voltage therefore is, from figure 14,

$$\cos\theta = \frac{Z_e}{R_e} = \frac{190.5}{1{,}090} = 0.175 \tag{2}$$

Since E_2 is a resistance voltage drop it is in phase with I_0, and the relation between E_1, E_2, and E_0 is, from figure 14,

$$E_1^2 + E_2^2 + 2E_1E_2 \cos\theta = E_0^2 \tag{3}$$

Assume a line voltage of 125 volts. Then

$$(80)^2 + E_2^2 + 28E_2 = (125)^2 \tag{4}$$

from which

$$E_2 = 83 \text{ volts} \tag{5}$$

Then

$$R = \frac{83}{0.42} = 197.5 \text{ ohms} \tag{6}$$

When curves *a*, *b*, and *d* in figure 2 are available, the resultant current curve *c* can be calculated approximately as follows:

The component of current due to the equivalent resistance is

$$I_r = \frac{E_1}{R_e} \tag{7}$$

and

$$I_0 = \sqrt{(I_c - I_x)^2 + (I_r)^2} \tag{8}$$

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Theory and Design of NEMA Resistors for Motor Starting and Speed Control

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Synopsis: The National Electrical Manufacturers' Association classification of resistors for motor starting and speed control specifies the initial current peaks and the duty cycle for which the resistor must be designed. The nature of the load driven by the motor decides the valley current at which succeeding resistor steps must be switched in starting, and consequently the number of steps in the resistor, and points in the controller. The rms current, and the time each step is in the circuit, taken in conjunction with the cyclic current-carrying capacity of individual resistor elements, determines the selection of the elements on the basis of their continuous-current rating. It is usual to assume an rms current of 125 per cent, and switching at equal time intervals. However, in this paper, formulas are developed for more accurately calculating these values, and the formulas and necessary constants are tabulated. Consideration is given to d-c shunt and series-connected motors, and to wound-rotor a-c induction motors. Resistors for regulating duty are also discussed.

THE performance of any motor controller depends primarily upon the accuracy of the resistor design. A knowledge of the fundamental relations involved, and the theoretical basis and practical methods of resistor design are necessary to a clear understanding of motor control.

Resistor design has been simplified, and to some degree standardized, by a standard classification system. Such a system was adopted in 1917 by the Electric Power Club. The club was later merged into the National Electrical Manufacturers' Association, and the classification scheme became known alternatively as the American Engineering Standard or the NEMA standard system.

It was first based upon a four-minute duty cycle, but a few years ago was modified to conform more nearly with actual duty cycles most common in practice. The various classes are indicated by numbers. As originally used there were two figures in the class numbers, the first representing the duty cycle and the second the initial or peak value of the starting current. When the classification was revised, inasmuch as it was necessary to change the meaning of some of the figures, the number of digits was increased to three by the addition of the figure "1" before the two significant figures, to indicate the new classification.

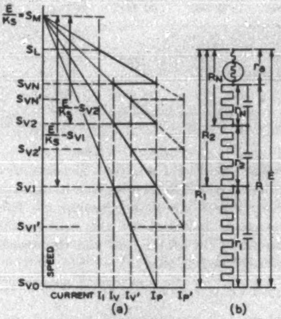

Figure 1

(*a*)—D-c motor speed-current relations with a NEMA starting resistor

(*b*)—Circuit connections

This classification, together with approximate starting torque, is given in table I.

Shunt-Wound D-C Motors—Total Resistance Value

On the first point of the controller, all of the resistance is in the circuit, the current rises toward a peak value determined by Ohm's law. However, as the motor starts, a counter electromotive force is developed in the armature which is proportional to the speed, and the current decreases toward a value which is just sufficient to develop the torque necessary to carry the mechanical load. The applied voltage, E, is at any instant exactly balanced by the sum of the resistance drop, ir, and the counter electromotive force so that, in a shunt-wound motor,

$$E = ir + K_s s \tag{1}$$

in which s is the speed, and the constant K_s may be evaluated by substituting for the other symbols the motor rated voltage, current, and speed, and the armature and lead resistance.

For current equal to zero, the second term vanishes, and the current-speed curves converge to the maximum theoretical speed, E/K_s, for all values of r, as shown in figure 1.

Now let it be assumed that the controller is arranged to cut out each succeeding step of resistance at the instant the current decreases to a value I_v, and that the resistance steps are so proportioned that the current peaks are each equal to I_p, on the successive steps. Then the current-speed curves take the form shown by the heavy lines of figure 1*a*. As the total resistance takes the successive values, $R_1, R_2 \ldots R_n \ldots r_a$, at the successive instants of switching, the currents have become I_v, and the speeds $S_{v1}, S_{v2}, S_{v3}, \ldots$ etc. For any step, n,

$$E/K_s = I_p R_n/K_s + S_{vn} \tag{2}$$

From similar triangles

$$\frac{I_p}{I_v} = \frac{E/K_s - S_{vn}}{E/K_s - S_{v(n+1)}} = \frac{R_n}{R_{(n+1)}} \tag{3}$$

Let

$$X = I_p/I_v \tag{4}$$

Then $R_n = XR_{(n+1)}$, and the total resistance

$$R_1 = E/I_p = XR_2 = X^2R_3$$
$$= \ldots X^{(N-1)} R_N = X^N r_a \tag{5}$$

and

$$R_N = Xr_a \tag{6}$$

Let $r_1, r_2, r_3 \ldots r_N$ be the successive values of the individual resistance sections, numbered from the line terminal, as indicated in figure 1*b*, and let it be assumed that the resistance of armature and leads is expressed as a fraction, Z, of line voltage divided by rated motor current; that is:

$$r_a = ZE/I \tag{7}$$

It is standard practice to assume values for Z except in the case of very large motors, inasmuch as even quite a large percentage error will have little effect except in the current peak on the last point of the controller. For most motors 0.1 is a fair approximation.

Let the ratio of the current peaks to rated current be represented by Y. That is:

$$I_p = YI \tag{8}$$

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The values of Y for the various NEMA classes are indicated in the first column of table I, and are repeated in table II.

Also, let the external resistance, $R_1 - r_a$, be represented by R. Then

$$R = UE/I \tag{9}$$

Here, U, defined by the equation, is, for $Z = 0.1$.

$$U = 1/Y - 0.1 \tag{10}$$

It is the constant which when multiplied by the ratio of voltage to normal current gives the total ohmic value of the resistance for a particular application and class number.

Table I. NEMA Resistor Classification Table

Per Cent Full-Load Current on First Point of Controller	Cycle and Corresponding Class Number: 30 Sec. on Out of 15 Min.	5 Sec. on Out of 80 Sec.	10 Sec. on Out of 80 Sec.	15 Sec. on Out of 90 Sec.	15 Sec. on Out of 60 Sec.	15 Sec. on Out of 45 Sec.	15 Sec. on Out of 30 Sec.	Continuous	Approximate Starting Torque in Per Cent of Full-Load Torque: Series Motors	Shunt Motors	Wound-Rotor Motors Three Phase
25	101	111	131	141	151	161	171	91	8	25	25
*50	102	112	132	142	152	*162	*172	92	30	50	50
70	103	113	133	143	153	163	173	93	50	70	70
100	104	114	134	144	154	164	174	94	100	100	100
150	105	115	135	145	155	165	175	95	170	150	150
200 or more	106	116	136	146	156	166	176	96	250	200	200

* Class 162 and 172 a-c mine-hoist resistors shall be designed for $33^1/_3$ per cent current on first point of controller.

The last step in the resistor is given by

$$\begin{aligned} r_N &= R_N - r_a = R(X-1)Z/U \\ &= R(X-1)/10U = C_r R \end{aligned} \tag{11}$$

C_r is a constant defined by the equation.

The resistance of any other step, n, is

$$r_n = X r_{(n+1)} \tag{12}$$

From equation 5 the number of steps in the resistor should be

$$N = \log\,(10/Y)/\log X \tag{13}$$

Inasmuch as N must be a whole number, this equation is necessarily used to determine X for assumed values of N, and the valley current can only approximate a desired value. *For the current peaks to be equal, and performance to be in accordance with the resistor design, the accelerating relays should be set to operate at*

$$I_v = IY/X \tag{14}$$

Values of C_r, U, and X are given in table II.

ACCELERATING TIME

In order to determine the current-carrying capacities necessary for the successive resistor steps, it is necessary first to consider the variations of the motor speed with time. It is to be noted that the following assumptions are made.

1. Torque of the load is constant and less than the torque developed by the motor when carrying current equal to I_v.
2. The field excitation is constant.
3. The effect of inductance in delaying the rise of current, and hence, upon the peak value, is neglected.

The motor speed in radians per second is $2\pi s/60$, or $0.105s$.

The instantaneous torque is proportional to the instantaneous armature current, which in turn is given in terms of voltage and speed by equation 1. This torque is balanced by the sum of the torque of the motor load and the torque of acceleration, that is

$$K_t(E - K_s s)/r = 0.105J\,ds/dt + L \tag{15}$$

where L is the torque of the load, r is the total circuit resistance at the particular time, K_t is a constant, which may be evaluated when the rate of acceleration ds/dt is zero, and $E - K_s s$ is equal to rI_L, the resistance drop at the current I_L required to carry the load. This gives L/K_t equal to I_L.

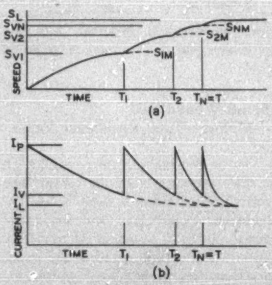

Figure 2

(*a*)—Speed-time curves for motor during acceleration

(*b*)—Current-time curves

Hence equation 15 may be put in the form

$$\frac{ds}{dt} + \frac{K_t K_s s}{0.105Jr} = \frac{K_t}{0.105J}\left(\frac{E}{r} - I_L\right) \tag{16}$$

This is a linear differential equation the solution of which is

$$s = \left(\frac{E}{r} - I_L\right) r/K_s + C\epsilon^{-K_t K_s t/0.105Jr} \tag{17}$$

t being time, ϵ being the base of the Naperian logarithms, and C a constant of integration, which may be evaluated by taking $t = 0$ at $s = S_{v(n-1)}$ for the nth resistor step, when $r = R_n$, so that in

$$s = \left(\frac{E}{R_n} - I_L\right)\left[\frac{R_n}{K_s} + S_{v(n-1)} - \left(\frac{E}{R_n} - I_L\right)\frac{R_n}{K_s}\right]\epsilon^{-Kt/R_n} \tag{18}$$

which K is substituted for the constants in the exponential.

It is evident that $(E - I_L R_n)/K_s = S_{mn}$, the maximum speed attainable on the considered resistor step, so that

$$s = S_{mn} + (S_{v(n-1)} - S_{mn})\epsilon^{-Kt/R_n} \tag{19}$$

Thus, at time equal to infinity the speed curve becomes assymptotic to the ordinate S_{nm}. However, at some intermediate time, T_n, the current becomes I_v, and the accelerating relay causes switching to the next resistor step. This is shown in figure 2*a*.

Substituting into equation 19 the value of s obtained from equation 1 yields

$$i = I_L + (I_p - I_L)\epsilon^{-Kt/R_n} \tag{20}$$

This equation is graphically shown in figure 2*b*.

Let T_n represent the time in seconds on the nth step for the current to die from the peak to the valley value, and T the total starting period. Then from equation 20,

$$T_n=(R_n/K)2.3\log_{10}(I_p-I_L/(I_s-I_L) \quad (21)$$

Hence

$$T_{(n+1)}=T_nR_{(n+1)}/R_n=T_n/X \quad (22)$$

Inasmuch as the total starting time is equal to the sum of the time on all steps, the time on the first step is given by

$$\begin{aligned}T_1&=T/(1+1/X+1/X^2+\ldots+1/X^{N-1})\\&=TX^{N-1}(X-1)/(X^N-1)=C_TT \quad (23)\end{aligned}$$

C_T is a constant defined by the equation. See table II.

Knowing the total starting time, equations 22 and 23 can be used to calculate the accelerating time on each step. *For time-limit acceleration the relays should be set so that the time ratio for the successive steps is equal to* X.

The total period the nth resistor step is in the circuit is $T_1+T_2+\ldots+T_n$. Let this be represented by P_n. Then,

$$\begin{aligned}P_n&=T_1(1+1/X+1/X^2+\ldots+1/X^{(n-1)})\\&=T_1(X^n-1)/(X-1)X^{(n-1)} \quad (24)\end{aligned}$$

The time of duty as given by the classification number does not necessarily give the starting period of the motor. Thus, in a 130 class instead of 10 seconds it may require but 5 seconds to start, with two starts in the 80-second period. However, the most severe condition for a given class is for a single start to occupy the whole of the allowed service time. Therefore, a resistor designed on this basis will be in every case adequate.

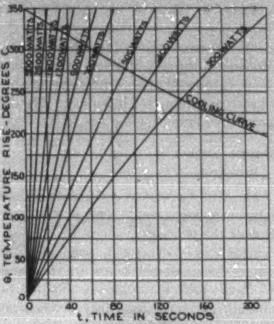

Figure 3a. Time-temperature curves for a starting resistor

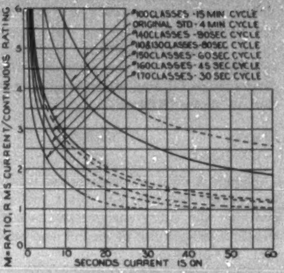

Figure 3b. Cyclic-capacity curves for a starting resistor, showing the ratio, M, of the short-time rms current to the continuous-current rating of the resistor elements, plotted against the time the current is on for the various duty cycles specified by the resistor class numbers

RMS Current

Often the current I_L required by the load during starting and the constant K in equation 20 may not readily be ascertained by the engineer who must lay out the resistor. For a given motor, resistor class, and number of steps, K and I_L are not mutually independent, however, and if I_L is fixed K is determined. The time for the current to decay from peak to valley value is fixed by the class number. Figure 5 shows a family of curves calculated for various assumed values of I_L. It is evident that the condition for highest rms starting current is for I_L to be equal to zero, and that this value does not differ radically from the rms value for most practical cases. On the assumption that I_L is zero, the rms current from equation 20 is, for the nth step,

$$I_{\text{rms}}=I_p\sqrt{R_n(1-\epsilon^{-2KT_n/R_n})/2KT_n} \quad (25)$$

As shown by equation 21, the ratio R_n/T_n is constant for all values of n, so that we may draw the very convenient conclusion that the rms current is the same for all steps. The value of T_n/R_n may be obtained by substituting 0 for I_L in equation 21. It follows that

$$I_{\text{rms}}=C_II \quad (26)$$

where

$$C_I=I_{\text{rms}}/I=Y\sqrt{(1-1/X^2)/4.6\log_{10}X} \quad (27)$$

Values of C_I for the various classes are given in table II.

CYCLIC CAPACITY OF RESISTOR UNITS

In order to apply table II to practical resistor design, it is necessary to know the cyclic capacity of the resistor elements. Such curves can be calculated from the dimensions and the physical and heat dissipation constants of the resistor. They may take the form of families of curves, showing the characteristics of each particular size of resistor element for various duty cycles, or inasmuch as these will be similar, may be

Table II. Constants for Calculation of NEMA Accelerating Resistors for Shunt-Wound D-C Motors and A-C Three-Phase Wound-Rotor Induction Motors, Wye Resistor Connection

First two figures of class number	10	11	13	14	15	16	17
T (seconds)	30	5	10	15	15	15	15

Last Figure of Class Number:		1	2	3	4	5	6
Number of Steps (N)	Y	0.25	0.50	0.70	1.00	1.50	2.00
	$U_{\text{D-C}}$	3.9	1.9	1.33	0.90	0.57	0.40
	$U_{\text{A-C}}$	2.2	1.0	0.77	0.52	0.32	0.23
1	X	40.0	20.0	14.27	10.0	6.67	5.00
	C_r	1.00	1.00	1.00	1.00	1.00	1.00
	C_I	0.092	0.204	0.291	0.464	0.762	1.092
	C_T	1.00	1.00	1.00	1.00	1.00	1.00
2	X	6.325	4.472	3.3780	3.162	2.2582	2.236
	C_r	0.1365	0.1824	0.2085	0.2402	0.2776	0.309
	C_I	0.131	0.282	0.413	0.625	1.002	1.402
	C_T	0.862	0.817	0.792	0.760	0.720	0.687
3	X	3.420	2.714	2.427	2.154	1.882	1.710
	C_r	0.0622	0.0902	0.1072	0.1282	0.1547	0.1775
	C_I	0.1505	0.329	0.477	0.711	1.128	1.564
	C_T	0.723	0.659	0.625	0.592	0.550	0.515
4	X	2.515	2.115	1.944	1.778	1.607	1.495
	C_r	0.0388	0.0587	0.0708	0.0865	0.1065	0.1237
	C_I	0.1685	0.358	0.519	0.770	1.205	1.656
	C_T	0.631	0.555	0.524	0.478	0.447	0.415
5	X	2.091	1.821	1.702	1.585	1.461	1.380
	C_r	0.0280	0.0432	0.0527	0.0650	0.0809	0.0950
	C_I	0.1795	0.381	0.548	0.807	1.254	1.714
	C_T	0.533	0.475	0.445	0.412	0.373	0.350
6	X	1.849	1.648	1.558	1.468	1.373	1.308
	C_r	0.0218	0.0341	0.0419	0.0520	0.0654	0.0770
	C_I	0.1892	0.400	0.568	0.835	1.291	1.756
	C_T	0.472	0.415	0.385	0.352	0.320	0.297
7	X	1.695	1.533	1.462	1.389	1.311	1.259
	C_r	0.01779	0.0281	0.0347	0.0432	0.0546	0.0647
	C_I	0.1962	0.409	0.584	0.857	1.318	1.690
	C_T	0.426	0.368	0.342	0.311	0.286	0.257

shown as one curve for each duty cycle in terms of the ratio M of the rms current to the normal element rating, I_e, plotted against the time P_n the particular step carries current during each cycle of operation, as shown in figure 3*b*.

SAMPLE CALCULATION

Assume a class 135 resistor for a 50-horsepower 230-volt d-c shunt motor, and that the valley current shall be not less than 100 per cent of the full-load current, 190 amperes.

Then $X=I_p/I_v=1.5$ approximately.

Referring to table II, under the 5 class, it is seen that a five-step resistor (six-point controller) is required, the nearest value of X being 1.461. The corresponding value of U is 0.57.

The total resistance is $R=UE/I=$ 0.69 ohm, and r_N is $C_rR=0.0809R=$ 0.0557 ohms. The resistances for the fourth, third, second, and first steps are determined by multiplying successively by X.

The duty cycle for the 130 class is 10 seconds on out of 80 seconds. Hence T is 10 seconds, and T_1 is $C_TT=0.373\times 10=3.73$ seconds. The rms current is $C_II=1.254$ times full load, or 239 amperes. This divided by M, the I_{rms} to element rating ratio (4.17) read from the curve figure 3*b* for the 130 class at 3.73 seconds gives the element rating for the first step. The time on each point of the controller is determined by dividing the time on the previous point by X, and the "on" period is obtained by cumulative addition of those values, as shown in table III.

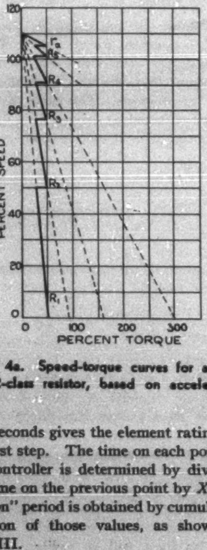

Figure 4a. Speed-torque curves for a five-step 2-class resistor, based on acceleration

From a table of standard resistors (having the characteristics given by the curve) elements are chosen having continuous ratings equal to or greater than those calculated, and a sufficient number of each are connected in series to give the proper resistance for the respective steps.

REGULATING RESISTORS

Many of the NEMA resistor classes are inherently regulating. This is obviously true of the 90 classes, which are for continuous duty. The 150, 160, and 170 classes being designed for $^1/_4$, $^1/_3$, and $^1/_2$ duty cycles are evidently adapted to intermittent regulating duty. When on the first point of the controller they give currents greater than 100 per cent full load, the resistance and capacity for the successive steps may be laid out in accordance with the theory as previously outlined for accelerating resistors. However, with less than full-load current on the first step, they must be designed on a different basis. Consider for example a class 172 resistor having five steps.

In figure 4*a* are shown the speed torque curves laid out on the basis of acceleration with equal 50 per cent current peaks on all steps. It is evident that there can be little practical application for such a resistor, for the motor could operate only without load. However, for hoist duty, a high resistance on the first step is desired while taking the slack out of the hoisting cable. On subsequent steps, the resistor is required to give speed regulation while carrying full-torque load. With the resistor of figure 4*a* the load will not be started until the third point is reached, when 156 per cent torque will be developed, and a maximum speed of 42 per cent will be attained.

In figure 4*b*, is shown the speed-torque relation for a class 172 resistor based on speed regulation. Here approximately 120 per cent torque is available on the second point, giving a slow pickup, and a maximum speed of 20 per cent. The succeeding points give 50, 80, and 93 per cent speeds, respectively. The capacities of the various steps must necessarily be arbitrarily set at values which experience has indicated are adequate for usual conditions of service in regulating duty of this type.

A-C Three-Phase Wound-Rotor Induction Motor

In the case of an a-c wound-rotor three-phase motor, with the resistor wye-connected in the secondary, the speed, torque, and secondary-current relations are similar to those of the d-c shunt-wound motor. If in this case we let E and I represent the normal secondary voltage and current, respectively, the total resistance per phase is

$$R=E(1/\sqrt{3}Y-Z)/I=UE/I \tag{28}$$

Assuming values of Z between 0.05 and 0.1, U takes the values given in table II.

Except for this different value of U, the calculation of the resistor is the same as for the case of the shunt-wound d-c motor.

If the resistor is delta connected the calculated resistance values should be multiplied by three and current values divided by $\sqrt{3}$.

Classes 162 and 172 a-c mine-hoist resistors, are modified by specific provision that the initial peak current shall be $33^1/_3$ per cent of full load. The class 162 for vertical hoists is designed for 340 watts per horsepower, and the class 172 for slope hoists for 510 watts.

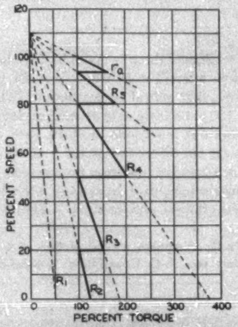

Figure 4b. Speed-torque curves for a five-step 2-class resistor, based on speed regulation

Acceleration With Unequal Current Peaks

If, instead of switching when the valley currents are I_v, the accelerating relays are set to operate at some other value I_v', then on all points after the first, the current peaks will be I_p', as shown in figure 1 From similar triangles,

$$I_p'/I_v'=I_p/I_v=X \tag{29}$$

Let the ratio I_v/I_v' be represented by G. By symmetry, from equation 21, assuming $I_L = 0$ as before, we have,

$$T_1'=T_1(\log I_p/I_v')/\log I_p/I_v=AT_1 \tag{30}$$

where

$$A = \log GX/\log X \qquad (31)$$

Also, for $n>1$,

$$T_n' = T_n(\log I_p'/I_v')/\log I_p/I_v = T_n \qquad (32)$$

Hence, the time on all points of the controller may be calculated as in the previous cases, then the time on the first point modified by multiplying by A, and the period each resistor step is in the circuit obtained by cumulative addition.

On the first point, from equation 25, since from equation 21, $KT_1'/R_1 = \log GX$, the rms current is $C_{I1}'I$, where

$$C_{I1}' = Y\sqrt{(1-1/G^2X^2)/4.6\log_{10}GX} \qquad (33)$$

For the other points, from equation 25, since $T_n = T_n'$, and $I_p' = YI/G$, the rms current is equal to $1/G$ times the rms current for equal current peaks, or C_II/G.

The rms current for the complete period on any step is the weighted average for the time it is in the circuit.

Table III

Resistance Per Step	Accelerating Time	Period on Each Step	$M = I_{rms}/I_e$	I_e = Element Rating
$r_1 = 0.1750X = 0.2560$	$T_1 = 0.373T = 3.73$	$P_1 = T_1 = 3.73$	4.17	57
$r_2 = 0.1196X = 0.1750$	$T_2 = 3.73/X = 2.545$	$P_2 = P_1 + T_2 = 6.275$	3.35	71
$r_3 = 0.0817X = 0.1196$	$T_3 = 2.545/X = 1.735$	$P_3 = P_2 + T_3 = 8.010$	3.01	79
$r_4 = 0.0557X = 0.0817$	$T_4 = 1.735/X = 1.185$	$P_4 = P_3 + T_4 = 9.195$	2.87	83
$r_N = 0.0809R = 0.0557$	$T_5 = 1.185/X = 0.805$	$P_5 = P_4 + T_5 = 10.000$	2.75	87
0.6880	10.000			

Table IV. Constants for Series-Connected D-C Motors

Last Figure of Class Number	Y	U	Constant	Number of Steps 2	3	4	5	6	7
5	1.50	0.57	V	1.125	1.200	1.250	1.286	1.312	1.333
			C_r	0.500	0.333	0.250	0.200	0.167	0.143
			C_I	1.240	1.312	1.350	1.370	1.395	1.400
			C_T	0.500	0.333	0.250	0.200	0.167	3.143
6	2.0	0.40	V	1.500	1.600	1.667	1.718	1.750	1.780
			C_r	0.500	0.3333	0.250	0.200	0.167	0.143
			C_I	1.70	1.77	1.81	1.84	1.86	1.88
			C_T	0.500	0.333	0.250	0.250	0.167	0.143

Series-Wound D-C Motors

For series wound motors, the electromotive-force equation becomes

$$E = ir + K_s si \qquad (34)$$

in which the constant K_s is determined at rated current, I, and rated speed, S. Solving for s at the instant of switching, when $r = R_n$ and $i = I_v$, and substituting this value into the equation with $r = R_{(n+1)}$ and $i = I_p$, we find that all steps have equal resistances,

$$r_n = R_n - R_{(n+1)} = R_1(X-1) = R/N \qquad (35)$$

The desired valley current determines the number of steps, N. This is most conveniently used as a ratio, V, to full-load current.

$$V = I_v/I = Y/[1+1/(N+1)] \qquad (36)$$

In practice it is customary to taper the resistance values of the steps a few per cent, the first being the highest, in order to minimize the effects of inequality between the resistance of the steps and that of the armature plus field and leads. This is particularly necessary with small values of Y and N.

In the series motor, torque (neglecting saturation) is proportional to the square of the current, so that the torque equation becomes $i^2K_t = 0.105Jds/dt + L$, in which $K_t = L/I_L^2$, and the load is assumed constant. The solution is

$$t = 0.105JIEB/K_sL$$

$$B = 1/I_p - 1/i + \frac{1.15}{I_L}\log_{10}\frac{(I_p-I_L)}{(i_p+I_L)}\frac{(i+I_L)}{(i-I_L)} \qquad (37)$$

These being free from terms in r, indicate that the time and the rms currents are equal, respectively, on the several accelerating steps. The period any step is in the circuit is, therefore,

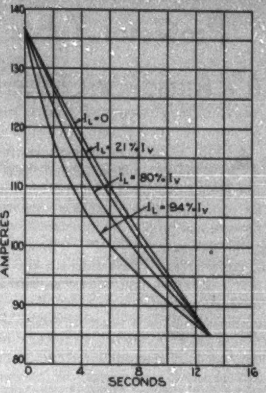

Figure 5. Curves showing the effect upon the current-time curve of varying the load component of the armature current, for fixed values of I_p, I_v, and T_1

$$P_n = nT_n = nT/N = nC_TT \qquad (38)$$

The rms current on each step is (for $I_L < I_v$)

$$I_{rms} = I_L\sqrt{1+(1/I_v - 1/I_p)/B} \qquad (39)$$

Here in the expression for B, $i = I_v$. (I_v must be greater than I_L.) For the 5 and 6 classes I_L is generally equal to the full-load current, I, and the constants for these classes are shown in table IV.

List of Symbols and Equations

Y = fraction of full-load current on first point
E = normal voltage
I = normal current
R = total ohmic value of resistor
X = ratio of peak to valley current
U, C_r, C_I, C_T, M, and V are constants evaluated in tables and curve
T = total time R is in circuit as defined by class number
N = number of resistor steps
$T_1 = P_1$ = time first resistor step is in circuit
P_n = time nth step is in circuit
r_N = resistance of last step
r_n = resistance of nth step
I_e = element rating
$R = UE/I$
$r_N = C_rR$
$r_{(n-1)} = Xr_n$
$T_1 = P_1 = C_TT$
$T_{(n+1)} = T_n/X$
$P_{(n+1)} = P_n + T_{(n+1)}$
$I_{rms} = C_II$
M = constant from figure 3*b* for P_n
$I_e = I_{rms}/M$

Protector Tubes for Power Systems

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AFTER eight years' experience with the design, application, and operation of the protector tube, it is considered an opportune time to review this experience and propose the standardization of this device.

Protector tubes have been applied to practically every type of system, with a total of approximately 50,000 now in operation and for a range in voltage of 13.8 to 230 kv inclusive. In the light of this broad experience it is the purpose of this paper to summarize the present conception of the theory of the operation, the factors that should be considered in the characteristics of the device, the characteristics of the system, and then suggest a standardization of the device to simplify its application. This joint paper was undertaken at the request of the lightning-arrester subcommittee of the AIEE protective-devices committee.

Theory of Tube Operation

Since the protector tubes are to prevent flashover of line insulation the prime requirements are that their discharge voltage be lower than the flashover voltage of the insulation which they are protecting and they must also be capable of interrupting the generated follow current. Operation may be more readily understood by first considering the phenomena involved in current interruption. After the surge currents are discharged to ground, the normal-frequency or generated current may follow and in order to have satisfactory operation, the protector tube must have a rate of insulation recovery after generated-current zero greater than the rate at which the system voltage recovers. This can best be explained by considering the recovery voltage on a simple circuit.

Such a circuit shown in figure 1 possesses the basic elements required to illustrate the transient recovery voltage, following interruption of fault current. This circuit contains lumped constants of L, R, and C in such proportion that it will have a natural frequency of oscillation. When a fault is placed at F and later removed at a normal-current zero, an oscillating voltage will appear across the capacitor, which will be composed of the normal-frequency voltage and a voltage whose frequency corresponds to the circuit's natural frequency. The simplest method of developing the oscillating voltage is to consider the generated voltage and oscillating voltage separately. At the instant the fault is removed, the voltage across the fault must be zero. This condition of zero voltage is satisfied if it is assumed that there is a natural-frequency voltage that appears instantly equal and opposite in magnitude to that of the generated voltage. The sum of these two voltages V_R is therefore the recovery voltage of the circuit. The time to crest of the oscillation is approximately proportional to $\sqrt{LC}$. Therefore if either L or C is increased, the time to the recovery-voltage crest of the natural-frequency oscillation will increase. This conception is important as the L of the source and C of the line vary for different line conditions. Theoretically, the natural-frequency voltage will start equal to the generated voltage at the instant the fault is removed assuming no losses, and oscillate about this voltage as an axis. However, losses produce damping and this natural-frequency component will decay at a rate depending upon the losses in the circuit.

When a protector tube is discharged by lightning and power follow current flows through the tube, it must have the properties of changing from a good conductor to a good insulator for satisfactory operation. In any interrupting device it takes time to establish the insulating characteristics which are commonly called the insulation recovery characteristics of the device. The interruption is therefore a race between the insulation recovery strength of the device and the voltage recovery on the system. Curve A of figure 2 is the recovery voltage for a more complicated system. Instead of having simple oscillations as in figure 1 the recovery voltage is now a more complicated function of time as the result of a succession of traveling waves on the line.

Curves B, C, and D of figure 2 are hypothetical insulation-recovery curves for a protective device. If the protective device has the characteristics shown in curve B and the system recovery characteristics are as shown in curve A, then the interruption will be satisfactory, as the insulation recovery of the device is faster than the voltage recovery on the system. On the other hand, if the protective-device insulation recovery strength is as shown in curve C or curve D, then the system recovery voltage overtakes the insulation recovery strength at second or first crest, respectively, restriking will take place, and if this occurs at more than two consecutive current zeros, the operation is in general not considered satisfactory. It is therefore possible to predetermine the operation of tubes on a system by comparing the insulation recovery strength of the tube and the system recovery voltage.

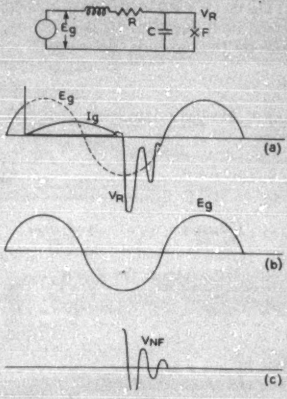

Figure 1. Elements of recovery voltage

E_g —Generated voltage
I_g —Generated current
V_R —Protector-tube voltage
V_{NF}—Natural-frequency voltage

The protector-tube insulation-recovery curves and the voltage-recovery curves will vary for each current value. For a

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given short-circuit current the protector-tube insulation-recovery curves also are lower with increased bore. Thus if the protector tube, when new, starts with a characteristic such as shown in curve *B*, figure 3, as it erodes the bore will increase and the curve will drop until at curve *C* it will fail to give satisfactory interrupting characteristics. It is for this reason that the rate of erosion must be considered in protector-tube design. Likewise, the same tube will have different insulation-recovery curves for different values of current. Curve *A*, figure 4, may be the insulation recovery strength for maximum current, and the insulation recovery strength for minimum current might be of the order of curve *B*, of this figure. Thus the system recovery-voltage curves must be compared with the insulation recovery strengths of the protective device to see that operation will be correct. The following discussion will therefore deal with system characteristics and protector-tube characteristics showing how this has been done to arrive at a standard line of protector tubes.

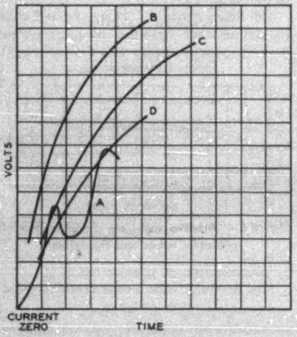

Figure 2. Elements of protector-tube operation

A—Recovery voltage of complex system
B, C, D—Protector-tube insulation recovery curves

System Characteristics

General Consideration

Since protector tubes are connected between line and ground, it is necessary that the system characteristics which affect tube performance be determined as viewed from the tube location. It is therefore obvious that to make a successful application the single, double, and triple line-to-ground follow currents and corresponding recovery voltages must be evaluated.

A number of factors will determine the magnitude of the currents and the associated recovery voltages. For example:

1. Method of grounding the system neutral, that is, solid grounding, resistance or reactance grounding, or any combination of these.
2. Minimum and maximum connected system capacity (variation in operating conditions).
3. Length of connected circuit or lines in miles and relative location of short-circuit-current sources.
4. Line configuration and presence of overhead ground wires.
5. Tower-footing resistance.
6. Protector-tube location.

(*a*) *Determination of the Short-Circuit Current Range*

The three-phase short-circuit current is in general the maximum current obtained, except near the system grounding points where the line-to-ground current may be greater. The single-line-to-ground current should be determined both with and without the effects of tower-footing resistance.

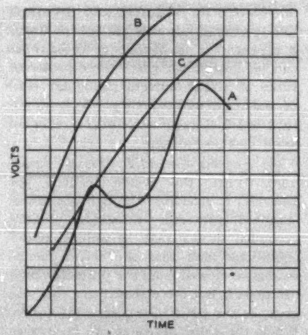

Figure 3. Effect of erosion on protector-tube operation

A—Recovery voltage of complex system
B, C—Protector-tube insulation recovery

The double-line-to-ground currents will usually be between the values obtained for single-line-to-ground and triple-line-to-ground faults. However, there may be exceptions for low ratios of zero to positive-sequence reactance and for high values of circuit resistance. Usually, such deviations are quantitatively unimportant.

Where the line is long or the system is extensive and the short-circuit currents vary widely, the application of tubes can be helped by plotting a current profile of the line (or lines) and zoning the tubes. Figure 5 shows such a profile for an assumed system. It may be noted that the maximum currents are at the generating and system ground points, which is usually the case for a solidly grounded-neutral system. By zoning the tubes it is possible to use tubes having a lower maximum rating in many locations, thereby obtaining tubes capable of operating successfully on lower currents or with higher tower-footing resistances.

When possible, it is desirable to take into account future system growth and its effect upon the currents to which the tube may be subjected.

While it is necessary to determine both the maximum and minimum currents that the tube may be called upon to interrupt, it is equally and perhaps more important to evaluate the currents expected under normal conditions. In doing this it may become quite evident that a particular system condition is imposing altogether too severe restriction on the tubes relative to the probability of that condition being obtained. The system condition which comes under this classification may occur when part of the system is temporarily disconnected.

When determining short-circuit currents it is only necessary to determine the rms symmetrical short-circuit current, as the design of the tube is such as to allow for the maximum asymmetrical current which may normally be expected in a power circuit. Since protector tubes

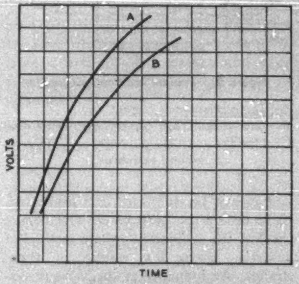

Figure 4. Effect of current on protector-tube operation

A—Tube insulation recovery, maximum current
B—Tube insulation recovery, minimum current

usually interrupt within the first cycle, subtransient values of synchronous-machine reactances should be used in current calculations. For fault locations which are remote electrically from large rotating equipment, generators, or synchronous condensers, it may be necessary to consider the effect of rotating motor loads as both synchronous motors and induction motors will contribute short-circuit currents. In general, this is an important factor only when the tubes are located electrically at some distance from the source of system supply or generation. The method of symmetrical components may be used for the calculation of the short-circuit currents. A-c network calculators or network analyzers may be used to advantage when available. These methods are well known and are fully described in the literature.

(b) System Recovery Voltage Characteristics

It has been shown above that the tube's dielectric-strength recovery must exceed the system voltage recovery in order to prevent a restrike in the tube and possibly failure to interrupt.

It is evident that in general the complete voltage-recovery characteristic is necessary in order to determine tube performance. The effect of system characteristics on the voltage-recovery characteristics has been given a great deal of study,[1-4] particularly by the use of miniature or equivalent systems. By this method, the effect of the various system factors can be simulated and evaluated. Field tests have been made which have checked the equivalent system method of analysis. From the results of these studies, the influence of the various system factors can be evaluated and tube applications made accordingly.

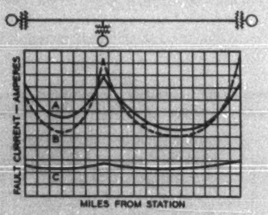

Figure 5. Typical fault-current profile

A—Three-phase fault current
B—Line-ground fault current (zero resistance)
C—Line-ground fault current (100-ohm ground resistance)

(1) *Solidly Grounded Systems.* The solidly grounded system which has all sources of short-circuit current solidly grounded, is a representative type of system found particularly at the higher voltages. The effect of tower-footing resistance is to reduce the current and to reduce the magnitude of recovery voltages across the tube. This resistance reduces the recovery voltage on account of the improvement of the power factor in the circuit which means that at current zero or time of interruption, the instantaneous fundamental-frequency voltage is of lower magnitude than for a pure reactive circuit. However, there is a lower limit to which the current can be reduced and still have successful operation. The limits will be discussed later in this paper.

(2) *Systems Grounded Through Resistance.* For practical purposes the effect of system neutral resistance is similar to the effect of tower-footing resistance discussed above. In cases where both neutral-grounding resistance and tower-footing resistance are encountered, they should be considered as being in series. Thus the same limits apply as pointed out for solidly grounded systems with tower-footing resistance alone. If the sum of those resistances is such that a single protector tube does not have the current range to interrupt both the phase currents not limited by resistance and the single line-to-ground currents limited by resistance, then the use of four protector tubes can be considered where three protector tubes of a high current rating are connected in star and the star point connected to ground through a protector tube of lower current rating.

(3) *Systems Grounded Through Reactance.* When the system neutral (or neutrals) is grounded through reactance, standard protector tubes for solidly grounded service may be applied under conditions such that the zero-sequence reactance viewed from the protector tube locations is not more than three times as great as the positive-sequence reactance. When the ratio of X_0 to X_1 is greater than this special consideration must be given to the protector tube. However, in a large number of cases the protector tube for the next higher voltage rating will be applicable. When the protector tubes are used on a system using a tuned value of reactance or using a ground-fault neutralizer, the line-to-ground currents should not have to be cleared by the tube. Thus protector tubes built for line-to-line voltage can be applied on the basis of the line-to-line fault currents only.

(4) *Isolated-Neutral System.* Protector tubes applied on an isolated-neutral system must be given special consideration, as the single-line-to-ground current (charging current) will usually be below the minimum current rating for the suggested standard tube. In connection with the application of protector tubes to isolated-neutral systems consideration should be given to the four-tube scheme as such a scheme should ease the problem of interrupting charging current.

(5) *Lightning Severity.* Investigations indicate the possibilities of lightning strokes of the order of 200,000 amperes although the probability of getting a stroke in excess of 100,000 amperes is very remote as indicated by actual field data. Furthermore, several tubes may share the lightning discharge current with the result that the duty on each tube is reduced. Operating experience with protector tubes over an eight-year period indicates that the existing designs are adequate for the vast majority of surge currents encountered.

(6) *Relay Settings.* It has been found in service that systems using protector tubes should not have relay settings less than two cycles. Where relays have a shorter operating time, it will be necessary to introduce a delay to prevent unnecessary circuit outages.

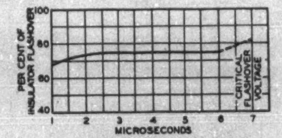

Figure 6. Relative volt-time characteristics of protector tubes and insulators

$1\,^1/_2$x40-microsecond positive wave
115-kv protector tube versus seven standard disk insulators

Protector-Tube Characteristics

(a) Impulse Protection Level

Since the protector tubes are used to prevent the flashover of line insulators, their primary characteristic must be a discharge-voltage value lower than that of parallel insulators which they protect. This requirement naturally serves to limit the length of the protector tube between electrodes. The devices are installed with an air gap in series, provided for the purpose of withstanding the normal impressed voltage to avoid possible corona or leakage currents across the protector tube itself. In general, the spac-

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1. For all numbered references, see list at end of paper.

ing of this air gap is considerably less than the spacing between the protector tube electrodes. Consequently, this series air gap will not add greatly to the over-all discharge voltage value of the applied protector tube. The impulse-discharge voltage level of protector tubes is best shown in the form of a volt-time curve. Such curves have been generalized for protector tubes, a typical curve being shown in figure 6 for the positive $1^{1}/_{2}$x40-microsecond wave. It should be noted that the volt-time characteristic of the protector tube is flatter than that of insulators.

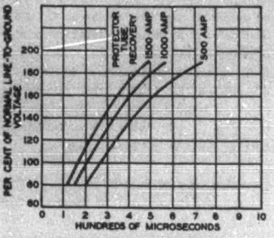

Figure 7. Effect of current on recovery voltage of protector tube

As illustrated in figure 6 it can be shown that where the protector tube gives protection to an insulator on the positive $1^{1}/_{2}$x40-microsecond wave at the critical flashover point, the protector tube will in general also protect for waves rising to flashover in shorter times or on negative polarity. Therefore, a comparison of the margin existing between the protector tube and the insulator based on the $1^{1}/_{2}$x40 positive wave may be taken as a yardstick for determining whether or not a sufficient margin exists between the level of the protector tube and the insulator. To aid in the application of protector tubes, table I has been prepared to show the discharge level of protector tubes. The table includes the minimum tight-string metal-to-metal arcing distance which the tube is expected to protect. These values include a factor of ten per cent between the actual discharge voltage of the protector tube and the impulse flashover voltage corresponding to the dry arcing distance. It is felt that 10 per cent represents the minimum margin which should be used in any case. Sufficient clearance should be provided between the protector tube and the structure to which it is attached to assure that lightning flashover will be confined within the protector tube.

(b) Tube Interrupting Characteristics as Affected by Voltage, Current, Time

It has been shown in previous papers presented to the Institute that for a given design the ability of a protector tube to function properly is a function of voltage and time for a fixed current. Likewise, this characteristic varies with the magnitude of current through the tube. These properties are illustrated in figure 7.

(c) Effect of Bore and Tube Erosion

When the protector tube operates, a part of the fiber wall is volatilized, creating a stream of rapidly moving gases which quench the arc. This volatilization of the fiber results in an increase in the tube-bore diameter. As the bore of the tube increases in diameter, the clearing characteristics of the tube undergo a change, the effect being to increase the required clearing current for any fixed recovery voltage. Figure 8 shows how the minimum clearing current of a particular protector tube increases with bore. It may be noted in this curve that an increase in the bore diameter of approximately 30 per cent doubles the minimum current that can be cleared.

Table I

Circuit Voltage Rating	Tube-Discharge Critical Voltage, Representative Value Dry,* $1^{1}/_{2}$x40 Wave: Positive	Tube-Discharge Critical Voltage, Representative Value Dry,* $1^{1}/_{2}$x40 Wave: Negative	Protector-Tube Impulse Discharge-Voltage Characteristics: Series Gap† (Inches)	Protector-Tube Impulse Discharge-Voltage Characteristics: Minimum Dry Arcing Distance Tube Will Protect (Inches)	Protector-Tube Impulse Discharge-Voltage Characteristics: Number Standard Insulator Disks Tube Will Protect; $5^{3}/_{4}$ by 10-Inch Spacing
13.8	100	110	$^{3}/_{4}$	6	1
23	150	155	$1^{1}/_{2}$	9	2
34.5	200	220	2	$12^{1}/_{2}$	2
46	260	285	$3^{1}/_{4}$	17	3
69	375	420	$5^{1}/_{2}$	28	5
92	475	540	$8^{1}/_{2}$		6
115	575	635	11		7

* Actual values will vary somewhat, depending on the design and mounting of the tube.

† These are recommended minimum series gaps. Under no conditions of operation should the series gap be less than 80 per cent of the recommended minimum.

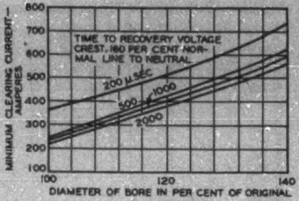

Figure 8. Effect of erosion on minimum current required for interruption

Where protector tubes are installed at every structure along the line, the probability of any protector tube having to operate a large number of times is greatly reduced, as compared to a protector tube applied at a reflection point. Over a five-year period on such a line, out of a total of 810 protector tubes, 228 tubes will operate once, 63 twice, 13 three times, 8 four times, 0 five times.[5] Also, the records of erosion of protector tubes over a six-year period on a particular line[6] indicated that no protector tubes had reached the limit of useful life. An inspection of these results leads immediately to the conclusion that where protector tubes are installed on every structure, the erosion of the bore from the standpoint of operations will not constitute a limiting factor as far as protector-tube life is concerned.

In other types of installation, such as river-crossing towers or terminal structures of lines which are not completely equipped throughout with new protector tubes, the tubes which are installed at these points will be called upon to operate more frequently. In such cases, a record should be kept both of the number of tube operations and the erosion occurring in the protector-tube bore, as it will be possible from these records to determine the time when the maximum permissible erosion of the bore is reached and when therefore the protector tube should be replaced.

(d) Impulse-Discharge-Current Capabilities

In general, protector tubes can withstand lightning-stroke currents of 50,000 to 100,000 amperes on the basis of a current wave that rises to crest in 10 microseconds and decays to half value in 20 microseconds. Experience to date with thousands of protector tubes has shown that the probability of a protector tube being burst from lightning current in excess of its discharge capabilities is small. It therefore appears that the

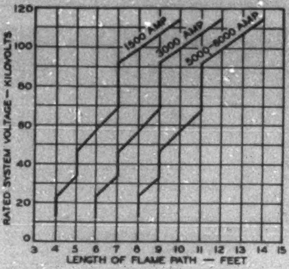

Figure 9. Visible flame path as a function of power, current, and voltage

impulse-discharge capabilities of protector tubes are sufficient to withstand all but the most severe direct strokes.

(e) Effect of Weather

Insufficient time has elapsed since the protector tubes have been in service to form a definite opinion as to the ultimate life expectancy from the standpoint of weathering. The outside finish on tubes has been improved. The experience, with modern finishes, indicates good weathering characteristics. The exact performance will depend on the local atmospheric conditions. Periodic refinishing of protector tubes with a good paint will increase their weather resistance.

(f) Discharge Gases and Recoil Forces

The ionized gases must be properly directed to avoid flashovers due to their conductivity. Figure 9 shows the length of the visible flame path as a function of the power current and voltage. Obviously, the circuit voltage and the protector-tube dimensions should be considered when establishing the safe strike distance from the gas envelope to the object of different potential.

Recoil forces must be considered when designing the mounting hardware. It is of importance that the mounting hardware be designed to facilitate ease of installation. This results in a direct saving to the operator, and broadens the field of application of the device.

Proposed Standard Protector Tubes

In order to facilitate the application of any device, it is desirable to have standards. This very aptly applies to the protector tube because of the large number of variables that enter into its application. It is for this reason that considerable thought has been given to the requirements for such a standardization. Based on the large amount of work done on miniature systems and eight years experience with the operation of protector tubes in the field, requirements for a proposed line of protector tubes are presented in table II.

When establishing the requirements for a standard line of protector tubes, it is necessary to select system conditions having recovery-voltage characteristics of sufficient severity to cover the vast majority of applications and from this determine the tube design. The maximum rating of the protector tube may be determined by either the insulation-recovery characteristics or the bursting strength of the tubes. The minimum current rating is determined by the amount of allowable erosion after operation at maximum current rating.

Since the capacitance of the system (a function of the length of line) and the reactance of the system are the major factors in determining recovery-voltage conditions of the system, it is essential to fix some minimum length of line which establishes the maximum permis-

Table II. Recovery-Voltage Characteristics for Which Proposed Standard Tubes Are Designed

				Minimum Current, Maximum Significant Crest		Maximum Current			
		Tube Current Rating**				First Crest		Maximum Significant Crest	
System Kilovolts	Minimum Miles of Line*†	Minimum	Maximum	Per Cent Voltage	Time (Microseconds)	Per Cent Voltage	Time	Per Cent Voltage	Time
13.8	10	300	1,500	160	230	90	55	165	130
		400	3,000	160	230	90	35	160	120
		600	6,000	160	200	90	25	160	120
23	15	300	1,500	165	400	73	65	165	250
		400	3,000	165	400	86	45	160	190
		600	6,000	165	400	94	30	158	190
34.5	22.5	300	1,500	170	600	65	80	165	390
		400	3,000	170	600	85	60	159	270
		600	6,000	170	600	100	50	156	270
46	30	300	1,500	165	800	70	115	165	530
		400	3,000	165	800	90	80	159	370
		900	6,000	165	750	105	70	153	330
69	45	400	1,500	165	1,100	85	180	165	800
		600	3,000	165	1,100	105	140	158	590
		900	5,000	165	900	115	100	149	490
92	60	400	1,500	160	1,300	88	240	165	1,070
		600	3,000	160	1,300	104	220	157	930
		1,000	5,000	160	1,100	115	180	145	760
115	75	500	1,500	160	1,450	90	300	165	1,350
		600	3,000	160	1,450	98	300	156	1,270
		1,000	5,000	160	1,350	105	300	140	1,100
138	90	500	1,500	160	1,600	90	350	165	1,440
		600	3,000	160	1,600	90	350	155	1,320
		1,000	5,000	160	1,500	90	390	136	1,250

* Miles listed are the minimum circuit miles of overhead line connected or equivalent where the line has a single source of short-circuit current at one end. Cable should be reduced to the equivalent miles of overhead line as indicated in figure 10.

† When two sources of short-circuit current are available, the miles of line in column 2 should be increased by a factor *K*, determined from figure 11. If less miles of line between sources are available than required by the use of figure 11, but more than shown in column 2, then a detailed calculation is necessary for the application.

** Guide for application when minimum current is limited by resistance.

The minimum current rating which is assigned to standard protector tubes is based on the short-circuit current through the tube being limited predominantly by inductive reactance. The protector tubes will actually interrupt currents as low as 85 per cent of their minimum name-plate rating for circuits 69 kv and above or 70 per cent of their minimum current rating for circuits 46 kv and below, where circuit resistance (tube grounding resistance) is included, provided the calculated minimum current neglecting all resistance is equal to or greater than the tube's minimum name-plate rating.

sible severity of recovery voltages. It was recognized in selecting a suggested list of standard ratings that they would not cover all the possible applications. However, it was felt that a balance could be obtained between the circuit requirements and the design limitations of the protector tube which would cover the majority of applications. Weighing all factors, the miles of line listed in table II for each operating voltage were believed best suited. Lines shorter than those listed tend to give rise to more severe recovery-voltage characteristics than are generally encountered. With the system conditions listed in table II the corresponding recovery-voltage characteristics have been obtained from the studies already referred to and requirements for a protector-tube design determined so that the protector tube can operate and still have the dielectric-recovery curve above the system recovery voltage, or in other words, have successful operation of the device. Since rate of rise of recovery voltage immediately following interruption is essentially proportional to current, the significance of the first crests of the voltage-recovery characteristics is not as great for minimum current ratings as it is for maximum current ratings. Therefore, only one point is defined on the system recovery characteristics for protector-tube minimum current ratings while two points are defined for maximum current ratings. Since the majority of protector tubes are applied on solidly grounded-neutral systems, it was felt that this should be the basis for the standard design. Other methods of grounding will be dealt with either by the use of application factors to allow the selection of a standard protector tube for use on the special system or the design of a special protector tube.

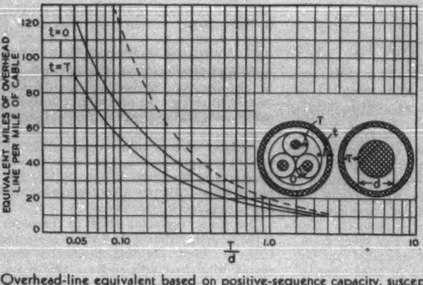

Figure 10. Overhead-line equivalent of cables

Dashed curve—Single conductor

Solid curves—Three conductors

For paper-insulated cable assumed dielectric constant 3.8

For varnished-cambric or rubber insulation multiply by 1.35

Overhead-line equivalent based on positive-sequence capacity, susceptance of 6.0 micromhos per mile

Based on the above general thoughts, the following application guides are suggested for consideration in the standardization of this line of apparatus.

(a) Basis of Design

Protector tubes are designed to withstand one operation for tubes rated at 5,000 and 6,000 amperes, two operations for tubes rated at 3,000 amperes, and three operations for tubes rated at 1,500 amperes, at maximum rated current with fully offset current wave after which the protector tube will be required to clear two operations at minimum rated current and with the associated recovery voltage. The tube performance is based on interruption of the current actually obtained during the one test at maximum rated current in less than $^1/_{60}$ second and the minimum rated current in less than $^1/_{40}$ second.

The maximum symmetrical nameplate rating in table II is based on the maximum crest current which the tube will stand divided by a factor of not less than 2.5 to take into account the effect of asymmetry. The minimum symmetrical rms current rating is based on the minimum crest current divided by 1.41 to convert to rms amperes.

(b) Discussion of Standard Ratings

It is felt that the requirements set forth in table II are suitable for consideration for standard ratings. A review of the actual field experience with a large number of protector tubes indicates that although the tubes are designed for these very severe conditions, actual system conditions are very much less severe. For example, there are not many systems that have as few miles of line as the minimum selected. This means that recovery voltages on actual systems are generally less severe and therefore a greater number of operations or a greater amount of erosion is permissible. Likewise, the requirements are based on a completely offset wave with the low value of damping of the d-c component. The probability of securing the completely offset wave is low and also the damping on the actual system is usually higher than assumed. The rate of erosion is dependent on the magnitude of current. For example, a standard-design tube for a 34.5-kv system might have 25 mils erosion for each operation with maximum current completely offset as used for the design requirements whereas for minimum current it will take several operations before any similar erosion is found. Since tubes are applied for maximum system generation connected, any reduction in this item will reduce the short-circuit current when the tube operates and therefore reduce the rate of erosion.

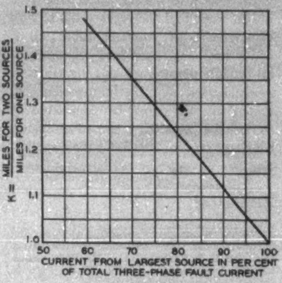

Figure 11. "K" factor for lines with two sources

(c) Standard Tubes Operating at Reduced Voltage

Where the minimum line-to-ground current of the circuit is below the minimum current rating assigned to standard tubes, and the insulation of the circuit is high enough to permit the use of a tube having the next higher voltage rating, this higher-voltage protector tube will interrupt lower than the listed minimum current.

To determine the minimum current which the protector tube will interrupt when used on a circuit below its voltage rating, multiply the normal minimum current rating of the protector tube by the ratio of the lower operating voltage on which the device is to be used to the normal voltage rating of the protector tube. This lower value of current can be considered as being limited by reactance only and applied in the same way standard tubes are applied.

Conclusions

In view of the large amount of analytical work and the eight years of field experience with protector tubes, all of which has been taken into consideration in the preparation of this paper, it is recommended that the information contained in this paper be considered in conjunction with the proposed protector-tube standards as a basis for their application.

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Discussion

M. C. Westrate (The Commonwealth and Southern Corporation, Jackson, Mich.): The paper covers the installation of protector tubes on transmission lines only, and does not mention their use for station application. We believe, however, that there is a definite application for protector tubes on line entrances to substations for protection of air switches and oil circuit breakers in open position. The simplicity with which these tubes can be installed has made it possible for us to install them for less cost than plain rod gaps in some cases. We have carefully engineered all our applications of protector tubes, and the experience has been very satisfactory both on 44-kv isolated-neutral and 22-kv grounded-neutral systems. We are now installing some at stations on a 140-kv grounded neutral system.

On the isolated-neutral system, we have used only the three-tube scheme with tubes of the same rating as for a grounded-neutral system. The problem on isolated neutral systems is usually that single line-to-ground faults do not produce enough current for satisfactory expulsion action of the tubes; however, if the system is not too large, the current may be low enough to interrupt itself without this action.

It was indeed a surprise to learn that protector tubes should be designed for such a limited life as one, two, or even three operations. A further explanation of this would be appreciated since it does not agree with other published information. Even for transmission-line application, where the number of operations would not be as many as for station use, this does not seem to be a reasonable life to use as a "basis of design".

It would also be very desirable to have published data in the form of curves on the volt-time breakdown characteristics of protector tubes. This is covered to a limited extent in figure 6 but is not complete enough to cover application in connection with switch-type insulators or bushings.

Several places in the paper mention is made of the discharge voltage of the tube whereas it is believed that "breakdown voltage" is intended. This is a minor point but may be confusing since in lightning-arrester work, the discharge voltage is usually considered as the *IR* drop after breakdown.

L. W. Gross (American Gas and Electric Service Corporation, New York, N. Y.): Knowing how a device is intended to function and the conditions under which it should be applied in practice are very helpful in studying the performance of the device itself when subject to operating conditions.

On the American Gas and Electric Company's interconnected 132-kv system, we have at the present time over 3,000 protector-tube assemblies installed on each phase conductor at each tower of six separate transmission lines varying in length from some 35 to 67 miles. Before these tubes were installed, staged field tests were made on tube designs to determine their suitability for interrupting power current. The initial tests made in 1932, were carried out on that part of the system where the maximum symmetrical rms short-circuit current was approximately 4,000 amperes. Further staged tests were made two years later on part of the system where the short-circuit current ran up to 10,000 amperes. It is interesting to note that when both of these tests were made, the problem of rate of voltage recovery rise of the system was not even considered. When these tests were made, the tubes were subjected to currents far exceeding the rated current which the design had predicted they were capable of interrupting. The point I wish to make here is that theoretical and design considerations must be co-ordinated with practical experience before the ultimate limits in performance of any protector device are known. From our experience in operating a large number of protector tubes in actual service during the past seven years, we have yet to find any indication that the tube has been subjected to power system current outside of its rating. The fact that our 132-kv system, being interconnected, far exceeds the 90-mile line length, indicated as a minimum by the authors in table II, indicates that we are not concerned with limits of line length in this particular application.

A considerable number of other installations on lower-voltage lines where protection has been undertaken on disconnecting switches, cables, and reactors, have operated satisfactorily from the point of view of tube rupture. On the basis of our experience with the application of protector tubes during the past seven years, we are wondering whether there is any evidence outside of theoretical considerations which indicates that the line length is such an important factor as the authors seem to stress in the paper. The problems which we have encountered in applying tubes may be summarized briefly as follows:

1. Ability of the tube to withstand weathering.

2. External tube flashover under lightning conditions. The record shows a decrease from approximately 11.4 per cent of flashed over tubes of the initial design to 1 to 1½ per cent on the newer designs. This problem seems to be reasonably under control.

3. As pointed out by the authors, the relay time setting must be sufficiently long to permit the tube to function without breaker operation. We have set a limit of about two cycles as a practical lower limit. Where high-speed breakers are installed to minimize conductor burning, line troubles, and for better system stability under fault conditions, the necessity of introducing longer relay times tends to undo the benefits possible with high-speed breaker action if this time delay is too great.

4. The disposal of ionized gases when the tube is operating appears to be a problem which should be given careful consideration particularly on tube application on the lower-voltage circuits where the conductor spacing is low. This consideration should be carefully watched, not only from the point of view of disposal of the gases from the tube vents but also from the viewpoint of possible traveling of the arc at the open gap end of the tube which may develop an arc on insulators or other closely adjacent equipment.

There is at the present time work being done in setting up a standard for protector tubes under the sponsorship of the lightning arrester subcommittee of the AIEE Protective Devices Committee. This work is now in second draft form, and this present paper, as well as the discussions which may be offered here, will be helpful in carrying on this work to a completion.

While it is not clear from the paper whether the authors are proposing that their table II be incorporated as a part of these standards now in progress, it is doubtful whether too much specific limitation should be placed on *tube standards* to the point where development in the device itself may be hampered.

F. E. Andrews and **H. A. Cornelius** (Public Service Company of Northern Illinois, Chicago): Although the probability of having sufficient lightning-stroke current to damage a protector tube is small, as indicated in the paper, such a failure occurred in the past lightning season on the Public Service Company of Northern Illinois 34.5-kv system. This 1,200 to 10,000-ampere tube had been in service just 14 days.

One disturbing point noted in the paper is the recommendation that where protector tubes are used, the high-speed-relay time should be at least two cycles. This is unpleasant news since it means adding external devices to decrease the effectiveness of such relays. If a tube should fail to clear after the relays have been slowed down, the fault might be on the system at least three cycles longer than at present.

Since the experience on which this recommendation is based is not given, an investigation was made of 34.5-kv lines on which protector tubes are installed in the instantaneous zone of the high-speed relays and of the line tripouts which occurred in the past lightning season.

The data are as follows:

Number of relay operations known to be due to tube operation	2
Number of relay operations probably due to tube operation	4
Number of known tube operations unaccompanied by relay operation	6

Included in the "relay operations probably due to tube operation" group are all otherwise unexplained line tripouts, so this figure may be high. Also, there is no practical way to determine the total number of tube operations. Therefore, the number shown as having occurred without causing relay operations is undoubtedly lower than it should be. Although these data are not conclusive, it indicates that using one-cycle relays has caused few unnecessary circuit outages.

The next point to be discussed is that accidental closing of the required external series gap has caused five tube flashovers with line interruptions resulting. The problem of holding the gap within the prescribed limits points to the need for further improvements. Is it practical to include the "series air gap" now used integral with the tube?

In regard to the protection provided, the statement is made that "where the protector tube gives protection to an insulator on a positive 1.5x40-microsecond wave at the critical flashover point, the protector will in general also protect for waves rising to flashover in shorter times or on negative polarity." The basis for including negative-polarity waves, which are about 15 per cent larger in magnitude than positive waves, within the protection provided by the tube, is not clear. The hyperbolic volt-time characteristic of insulation is apparently the basis for the protection provided by the tubes on steeper front waves than 1.5 microseconds.

Since successful operation and tube life vary with the degree of erosion of the tube walls and since the erosion to be expected at these widely separated tube installations would be greater than on lines having tubes on every pole, measurements of the erosion in 80 tubes were made. The data on the service life of these 80 tubes, erosion measured compared to years of service, etc., are given in table I of this discussion. Although the data are not very extensive, they are considered a representative sample for tubes in service for three years or less and indicate that severe tube erosion is rather uncommon.

One of the reasons given for the proposed standardization of protector tubes is that eight years of experience has been obtained with this device. Although in the past two years a much better understanding of protector-tube characteristics and of the effect of system characteristics on tube operation has been obtained, there still remains a question as to whether this device is ready for standardization. The basis for this questioning is the frequency with which troubles are encountered with this device. In support of the preceding statements, the experience with 196 three-phase sets of protector tubes, which have been installed at widely separated locations, in the past six years on the 1,100-mile 34.5-kv system of the Public Service Company of Northern Illinois may be cited.

Table I

Number of Lightning Seasons Tubes in Service		Erosion Measured		
Years in Service	Number of Tubes	1/16 Inch	1/32 Inch	0 Inch
3	30	2		28
2	41	3	7	31
1	9		1	8
	80	5	8	67*

* Twenty-one of these tubes showed arc scars, etc., indicating that operation had occurred. The remaining 46 tubes probably had not operated.

1. Nineteen tubes failed apparently from the generation of excessive internal forces. Eight of these tubes were found destroyed after lightning storms and portions of the tubes were scattered 200 feet away. The other failures are not clear-cut as to cause, but it has been established that the failures were not due to erosion of the tube walls nor to having fault current flow less than the tube rating.

2. Practically all of these 19 tube failures have occurred in the past two years at seven different locations. Sixteen of the tubes have failed at four locations at various times. The number of tubes in service by ratings and the failures to date are as follows:

Tube Current Rating	Number in Service	Failures to Date
200–900	18	0
600–2,500	3	0
900–5,000	156	3
1,200–10,000	66	5
400–4,250	36	6
250–3,500	270	0
400–3,000	27	0
700–3,600	12	3
Unknown	2	2
	588	19

Another point of importance is the probable life to be expected of these proposed new standard tubes. Although the discussion on the basis of the design states that a 6,000-ampere tube is designed to withstand only one operation at maximum rated current when the current wave is completely offset, the discussion on the standard ratings of the tubes implies that more than one operation may be expected under service conditions. Unless the probabilities indicate considerably more than one successful tube operation in service, the new design does not appear practical. It is believed that effort should be made to provide tubes which will operate in service for a reasonable number of times. It is also believed that the tubes should be rated on the basis of expected field performance rather than on the basis of test performance. Such test performance appears widely divergent from typical operating conditions.

There are two points in connection with the proposed standard tube ratings: one has to do with a larger current rating than given in table II and the other with the reasons explaining why the physical length of the tube has been increased but the rating of the tube decreased.

1. At locations where 8,000 to 10,000 amperes short-circuit current may flow, none of the proposed standard tubes could be used, for the maximum current rating given is 6,000 amperes. The service experience at such high fault current locations where 1,200 to 10,000-ampere tubes have been in use for three to six years has been satisfactory. Pole-top switch insulator flashovers have been eliminated where tubes are installed, and there appears to be a general improvement in line performance. Unfortunately, the number of protector tubes in service by years is not available; but the average failure rate is estimated to be less than three per cent per year. In our opinion, the 10,000-ampere tube should be included in the proposed standard ratings.

2. Published data on the new standard protector tubes show them to be longer in the 34.5-kv class than before the change in rating by from one to four inches. This increased length should permit increasing the internal gap and thus the maximum 60-cycle voltage the new tube can clear after an operation, as well as the range of power current that can be interrupted at 60-cycle voltage. Instead, the new standard protector tubes are rated at approximately half the current interrupting capacity previously used. That is, on the old rating a 17-inch tube was rated 1,200 to 10,000 amperes, whereas on the new rating a 23-inch tube is rated 600 to 6,000 amperes. This seems to be a step in the wrong direction from the user's point of view, and the justification for such a marked change in tube rating is not apparent.

Two subjects considered of general interest are (1) some of the difficulties experienced while learning to install the tubes properly and (2) a description of two types of tube failure which have been observed.

1. Improper installation has caused trouble with about 25 tubes. The most common types of difficulty were due (*a*) to mounting the tubes on insulation having lower flashover values than the tubes, which accounts for about half the trouble experienced; (*b*) to flashover resulting from accidentally shortening the external gap; (*c*) to having the discharge gases directed so as to cause flashovers on adjacent equipment; and (*d*) to having insufficient clearance between the tube and the insulator it parallels.

2. The cases of tube failure due to power system current were generally characterized by an irregular parting of the tube fiber just above the metal base fitting which was attached to the mounting bracket. The bore of both the fiber and the base fittings had enlarged considerably. The erosion of the metal in many cases was approximately equal in amount to that of the fiber. One tube which had not failed was found with this same type of erosion, the bore having enlarged from 3/4 inch to 13/16 inch. This was located on a pole adjacent to one of the tubes which had failed and was thus presumably subjected to similar conditions.

On the other hand, in the case of the failure diagnosed as due to lightning stroke current, the tube was not appreciably eroded; and the fiber had parted at the metal base fitting with a relatively clean break.

While much of this discussion may seem to be devoted to tube shortcomings, we wish to point out that in over-all performance our use of tubes has been generally successful; and through their use, a definite reduction in certain types of lightning troubles has been accomplished.

C. Concordia (General Electric Company, Schenectady, N. Y.): From table II and the discussion under it, it might be inferred that protector tubes should be specified for proper operation on circuits having at least the required equivalent line length. However, I do not believe this is the authors' intention, or that such a specification is desirable. Instead, tubes should be specified for successful operation within their current rating and for recovery voltages no more severe than those listed in the table. That is, mere satisfaction of a certain system condition should never logically be taken as the fundamental requirement of any interrupting device; instead, it must be decided whether or not the permissible current ranges or recovery voltages are exceeded, the line length serving as a guide to this decision. Thus, the information given in this table should be of considerable assistance in assuring proper application of tubes.

A. C. Gohlke (Cleveland Electric Illuminating Company, Cleveland, Ohio): This paper gives a clear outline of the many variables to be considered in the application of protector tubes to a given power line and the standardization of protector-tube designs would facilitate the correct tube applications to a great degree.

Our experience with protector tubes of the 34.5-kv class has indicated the following:

1. Tube failures from excessive impulse discharge currents have been most common. Shattered tubes have been found in the 5,000-ampere class as well as the 1,500-ampere class.

2. External flashover of tubes has occurred in a number of instances, indicating high impulse discharge currents or insufficient margin of external insulation over internal-gap breakdown.

3. Several cases of insulation failure of tube walls have occurred showing path of dynamic current from internal electrode through wall of tube to external ground.

4. Tubes have continued to clear successfully after repeated operations at locations of approximate minimum current values.

5. External finishes on tube walls have not been effective for more than three to five years.

R. J. Patterson, R. R. Schump (nonmember), and **E. H. Grosser** (nonmember; all of Commonwealth Edison Company, Chicago, Ill.): It seems to us that, with one or two exceptions, the paper covers the subject very thoroughly. Our only difficulty was in co-ordinating the various factors dealing with protector tubes, and we are of the opinion that a more straightforward presentation would help when and if this information is written into standards.

Enlarging on this matter, it would appear preferable to separate design factors from such application factors as system characteristics, lightning severity, anticipated life, and installation and maintenance methods. Incidentally, we should like to see someone from either of the manufacturing companies present a discussion of this paper giving up-to-date information on electrical connections, various methods of installing protector tubes on lines of various types, relative protection to be expected with different arrangements and numbers of tubes per phase per mile, and methods of mounting the devices on structures. We should also be interested in a discussion of the limitations of these devices in protecting inductive and possibly capacitive apparatus, which was not covered in the paper.

A few specific comments on the paper are given below:

1. In tables I and II, it is not clear that the circuit voltage rating is also the voltage rating of the protector tube. Also, it is difficult to tell whether the columns 2 and 3 values for discharge critical voltage do or do not include the recommended series gap. If they do, then the voltages corresponding to the protected arcing distances shown are more than ten per cent greater than the tube discharge voltage as stated in the second paragraph under "Protector-Tube Characteristics." We wondered if tolerances for erratic discharge voltages were also added to the tube discharge voltages shown. Along this same line, it would be interesting to know whether the series capacitances of the tube and external gap are such as to prevent a cascading type of discharge, in which either the tube or gap operates first. If this did occur, the voltage existing across the combination would probably be limited to the discharge voltage of the tube alone.

2. Under item (*e*) of the section on "Protector-Tube Characteristics" it would be of interest to learn to what extent the impulse and power-frequency characteristics of tube and external gap are affected by rain, humidity, and dirt.

3. Does figure 7 apply to all ratings of protector tubes?

It may be of interest to learn of our experience with the revised installation of protector tubes on a 33-kv wood-pole line. This line, which is about three miles long, has two conductors spaced nine feet on a wood crossarm and the third conductor mounted on the ridge pin, five feet nine inches above the plane of the other two conductors. The first two years of operation, with no protector tubes installed, was unsatisfactory, three cases of lightning trouble occurring in 1936 and eight in 1937. Then protector tubes were installed on all three phases of the line, but spaced about ten spans apart. Seven cases of lightning trouble occurred with this arrangement in 1938. As a result of this operating experience, it was decided to modify the protective installation to provide one protector tube on the top conductor only on every pole throughout the line, except that three gaps were installed on corner poles and such few poles as have flat configuration. Ground resistances were reduced to a value which calculations indicated would prevent flashover voltage between phase wires being attained with severe stroke currents. With this protective scheme in operation during the entire 1939 lightning season (which, however, has not been an unusually severe one), no lightning troubles or outages have occurred on this line to date. Although the minimum and maximum ratings of the tubes were raised for a portion of the line, the improved performance is attributed to the manner of locating the gaps on the poles.

Our comments on the standardization portion of the paper are as follows:

The proposed standard line of tubes as given in table II, taking into consideration the application of a standard tube at reduced voltages, should suitably cover requirements of various systems. The basis of design, namely, one, two, or three operations at maximum rated current with completely offset wave for the 1,500, 3,000, and 5,000 to 6,000-ampere tube, respectively, seems to give little assurance of realizing a fair amount of protection for the investment in tubes and their grounds. The tentative selection of 1,500, 3,000, and 5,000 to 6,000 amperes for maximum rating indicates that some overlapping of currents is permissible for successful operation; otherwise a larger number of tubes should be required to adequately cover line conditions. If this is true then it would seem that a 3,000-ampere tube, for instance, could be expected successfully to withstand more than two discharges at 3,000 amperes with completely offset wave.

It would seem desirable to incorporate in the paper some indication of the number of operations which can be expected from a tube for the most severe, average, and least severe conditions assuming the tube grounding resistance to be zero.

Some mention might also be made of standardization of mountings so that a tube of one manufacturer could be replaced, if desirable, with a tube of another manufacturer without replacing the bracket.

As the tube rating is appreciably affected by the bore diameter, it would be desirable from the user's standpoint to have the nominal diameter stamped on the name plate. This would assist in determining when a tube should be replaced. Also, the construction should be such that the bore can be readily determined by measurement in the field.

A. D. Forbes (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): This paper has been limited by its authors to the field of the type of protector tube which is used for the protection of transmission-line insulation. There is also the type of deion protector usually referred to as a "deion gap" which, to date, has been used principally for the protection of distribution-transformer windings. It may be well to point out the similarities and differences of this type of equipment.

The deion gap operates on the same principle as the protector tube—deionization of the power arc by means of gas generated by decomposition of fiber by the arc.

The deion gap differs from the protector tube in two respects, however. First, the deionizing section of the deion gap consists of a number of parallel slots instead of a cylindrical hole as used in the protector tube. The slotted construction allows the two opposite walls to be close enough together that there is no minimum power current which the deion gap will interrupt—it will clear from zero amperes upward.

Second, series resistors are normally used with deion gaps. The series resistor limits the power follow current regardless of the short-circuit current of the system. There is therefore no maximum current which can be interrupted and the deion gap can be applied universally to any power system.

Data which have been presented by Hodnette,[1] Ludwig,[1] Bellaschi,[2] Sporn,[2] and Gross[3] indicate that the probability of a 50,000-ampere surge each year is 0.23 per cent to 3.7 per cent. More recent experience with deion gaps in transformers on distribution circuits indicates a probability of about 0.9 per cent for 50,000 amperes, and 0.1 per cent for 100,000 amperes. Experience gained indicates that gaps for transformer application which can successfully discharge a surge of 100,000 amperes decreasing to half value in 50 microseconds are satisfactory. We note that the surge-current capacity of the protector tube, as mentioned under "Protector-Tube Characteristics" is 50,000 to 100,000 amperes for a duration of 20 microseconds to half value. For applications where protector tubes are used, there are possibly more parallel paths for lightning to drain off to ground and possibly because of this, the high surge-current capacity of the deion gap, for transformer application, is not required by the protector tube for line application.

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3. LIGHTNING CURRENTS IN 132-KV LINES, Philip Sporn and I. W. Gross. ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), February 1937, page 245.

H. A. Peterson, W. J. Rudge, A. C. Monteith, and L. R. Ludwig: The discussions presented on this paper are very encouraging to the authors as they not only bring out factors that were not included in the paper but also give very valuable operating experience on the device under discussion.

Referring to the discussion by Mr. Westrate, the authors wish to point out that the demonstration tests proposed cover extreme conditions which are very unlikely to exist in the actual application of tubes to power systems. In the first place, in actual operation numerous factors affect the magnitude of the short-circuit current which will be passed through the tubes; that is, the point on the wave at which lightning strikes, the offset factor in the first cycle of current flow, the reactance of the lines, etc., will usually result in a peak current through the tube which is lower than the maximum current called for in the test. The resultant effect is in the direction to give less erosion, or in other words a greater number of operations.

In the second place, it should be recognized that usually it is the minimum current which dictates whether or not the tubes will clear. It is very likely that in actual practice the minimum current of the system will be above the minimum rating of the tube, and the system recovery voltage will generally be less severe. As the result of this influence the tubes are capable of a greater number of operations before the tube bore erodes to the point where the tubes will fail to clear properly. There have been many cases pointed out in the literature and in the discussions presented before the AIEE where tubes have operated numerous times without reaching the end of their useful life. It is our feeling that the proposed test can be used only in a comparative sense. The actual number of operations can be determined only from experience with the tubes in the field.

We are in agreement with the thought expressed that the protector tube can serve as a secondary protection device. It will serve to protect line insulators, disconnecting switches, and the like. However, we wish again to call attention to the fact that we would expect tubes used in this fashion to have a shorter life from the standpoint of erosion than tubes used to protect every structure along the transmission line. This would be particularly true in cases where the protector tube is attached to a station which is fed by a highly insulated line, as practically all strokes in the vicinity of the station will cause operation of the tube, if the oil circuit breaker or pole-top switch were in the open position. Naturally, tubes used for this purpose would have to be replaced more frequently than in cases of normal application. It is entirely possible that additional tubes for, say the first two or three structures out from the station, would divide the number of operations in such a way as to prolong the periods required between tube inspections.

Some of the principles used in this device are incorporated in a tube for the protection of all apparatus in the voltage classes 15 kv and below. There are features included in these tubes, however, that are not incorporated in the tubes discussed in this paper and their standardization should be considered separately. We were particularly interested in the comments on the application to free-neutral systems. It will be of interest to again review this experience in the course of two or three years. It is quite possible that too conservative a point of view is being taken in regard to the application to free-neutral systems.

The volt-time curves Mr. Westrate refers to are available in published literature of both manufacturers. In making use of them, one point should not be overlooked. The laboratories of the manufacturers are able to check each other with a very close degree of accuracy on 1½x40-microsecond critical flashover tests. However, there are discrepancies between laboratories on values obtained for breakdowns at short times in the region of one-half microsecond. Therefore, in making an application, volt-time data for protector tubes obtained in one laboratory should be compared with volt-time data for insulators obtained in the same laboratory.

Mr. Westrate questions our use of the term "discharge voltage", stating that he believes breakdown voltage is intended. We agree that there is need of clarifying both the terms and their definitions. We feel that this can best be done by the AIEE committees who are at present writing standards for the tubes, and we are referring this matter to the proper committees.

Mr. Gross points out that miles of line are not of particular concern to him in the application of tubes, since their minimum miles of line far exceed the 90-mile minimum required in the paper for 138-kv tubes. This is undoubtedly true while the system is interconnected. However, there may be operating conditions in which a portion of the 138-kv system is disconnected and this portion may have less than the 90 miles required in which case the recovery voltage will be more severe with corresponding risk of tube failure. Such conditions probably correspond to emergency conditions and not conditions that will normally exist on the 138-kv system. This is just another way of saying that tubes will be capable of a larger number of operations under usual service conditions as pointed out before.

It is true that in the case of the system cited by Mr. Gross miles of line or the corresponding recovery voltages are not major factors. However, there have been numerous other cases in which the length of line or severity of the recovery voltages have been of major importance in the application of tubes.

In connection with the question of how important length of line is in determining the recovery characteristics, tests have been made not only on the miniature transient analyzers referred to in the paper, but in the field as well. These tests show definitely that the recovery characteristics are affected by miles of line. It should be thoroughly appreciated, however, that the miles of line given in table II are purely to serve as a guide for application and it was not the intent to use miles of line to specify tube characteristics. This also applies to Mr. Concordia's discussion. The tube characteristics should be stated in terms of the recovery voltage characteristic for which they are designed, and it was our intent to do this in specifying these characteristics in the paper.

We agree with the thought suggested by several discussers that the slowing up of high-speed relays introduces another complication, and from this point of view, an unfortunate one. However, a line properly equipped with tubes should normally not require the operation of the relays. This represents a definite improvement in performance both from the standpoint of increased stability and from the standpoint of a decreased amount of conductor burning. In the possible event of a tube failure to operate, or in cases where insulator flashover cannot be controlled by the tube, such as might occur with an accumulation of dirt or the like on the insulators, the two cycles, in general, will not appreciably change the amount of burning nor affect the stability.

The operating experience outlined by Mr. Gohlke is of interest in connection with the performance of the device under discussion. We believe, however, that the tubes in question are of special design to meet special system conditions, and not tubes designed for general application such as are being considered in this paper.

In answer to the discussion by Messrs. Patterson, Schump, and Grosser, it should be pointed out that the factors affecting the different distributions of protector tubes along the line have been analyzed and presented in the literature. We refer to "Protection of Transmission Lines Against Lightning—Theory and Calculations" by L. V. Bewley, *General Electric Review*, June 1937.

With regard to the question of electrical connections and methods of mounting, we believe that both manufacturers' bulletins show typical mounting arrangements and methods of connections and by referring to these publications, this information can be obtained.

With regard to the question of protecting inductive and capacitive apparatus, the question is not clear to us as to just what was intended. However, we believe that our comments on Mr. Westrate's discussion answer this point.

With regard to item 1, the voltage rating contained in table I and table II is the tube circuit-voltage rating. The nominal voltage rating of the tube is the maximum permissible continuous rms voltage across the tube assembly, including the series gap, and is at least the circuit voltage divided by the $\sqrt{3}$. The impulse values given in table I include the listed series gaps appearing also in table I.

With regard to item 2, the extent to which the breakdown characteristics are affected by rain can be determined for standard rates of precipitation, but there appears no practical way of determining the effect of humidity and dirt, as these factors will vary so widely with different localities that it seems impractical to evaluate them.

With regard to item 3, figure 7 of the paper is typical of the characteristics of all protector tubes, although the specific values given apply only to one particular tube, and one particular voltage rating. This curve was included to show how, in general, the recovery characteristics of the tubes would vary as a function of the current passed through the tube.

With regard to the question of standardization of mounting fitting, also of stamping the tube's original diameter on the name plate, we are referring these matters to the proper committees.

Messrs. Andrews and Cornelius bring out a point that is too often overlooked. The length of series gap given in the paper is based on obtaining the performance given in the tabulations. If the insulation of the systems is high then the series gap can be increased minimizing the necessity of careful setting of the series gap. These gentlemen have also given very valuable operating data which will be of interest to the art as these data show that in actual practice more operations can be obtained than outlined for the demonstration test.

We have noted with particular interest the operating experience of tubes reported by Messrs. Andrews and Cornelius. However, we wish to caution against comparing these data with the data presented by Messrs. Gross and Sporn, for it is our understanding that in most of Mr. Cornelius' and Mr. Andrews' cases, tubes have been installed on pole-top switches and isolated structures, whereas the figures given by Messrs. Gross and Sporn are for cases where tubes were installed on every structure along the entire length of line. As pointed out in answering Mr. Westrate, tubes located at terminal structures or guyed structures and located intermittently along the line, will be subjected to a greater number of operations than tubes located on each structure along the line, and for this reason will have a shorter life.

The authors feel that it is very desirable to have a standardization at the present time as this allows starting with a common basis. This will provide a method of making a comparison of the performance of devices for any system. We should not, however, lose sight of the fact that if the basis selected for standardization at the present time is not correct then it certainly can be changed when this fact is established. It should be recognized that the physical length of the protector tube does not determine the rating. Rather, this is dependent on the actual space between internal electrodes, bore, and wall strength. Too much stress should therefore not be placed on the physical dimensions of tubes having different current ratings but the same voltage rating. It should be expected as a result of this standardization that more uniform tube designs will result with a greater reliability of operation.

Modernization of a Transit System—Factors That Determine the Choice of Vehicle

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THE transit industry is facing serious economic problems. Shrinking revenues, due largely to the competition of the private automobile, and increased operating expenses have together so decreased the margin between income and expenses that many systems are now in financial straits. The best solution for these problems, so far advanced, is to modernize the equipment and rehabilitate the service. Some lines have already been modernized; most of the remainder badly need modernization. Present equipment is old and expensive to operate; the service is slow, infrequent, and generally unattractive to the riding public. Through substituting new, fast, comfortable vehicles, run on frequent headways, for the present obsolete equipment and through aggressive management, most transit systems, if not all, may regain a considerable portion of their lost traffic and thus get back to a more satisfactory financial condition.

Most city transportation systems were originally equipped with street cars, because at that time the street car was the only transit vehicle known. At present, in a well-planned modernization program, two additional vehicles, the trackless trolley, or trolley bus, and the gas bus, merit consideration.

Even engineers and operating men disagree as to the relative merits of the three types of vehicles. There are "street-car enthusiasts" and "bus enthusiasts", while still others believe that all three may have a legitimate place in a modern transportation system. In view of the large expenditures involved in even a very modest modernization program, these differences of opinion make appropriate at this time some explanation of the basic factors governing the choice of vehicle, a selection that should be based, as far as possible, on facts rather than on personal preference for a particular type of conveyance.

The purpose of this study is twofold: first to show the application of the engineering method to the problem of selecting the most suitable vehicle with which to modernize an existing transit line; and second, to determine as nearly as possible, for average conditions, the range of traffic volume within which each vehicle is most economical.

Requisites of a Modern Transportation System

A modern transportation system should fulfill three fundamental requirements. These are: first, it should render adequate service; second, the vehicle used should be attractive to its patrons; and third, the over-all cost of operation should be the least practicable in order to make a low rate of fare possible. The three types of vehicles will next be compared with respect to those qualities most essential to a modern transportation system.

First, Adequate Service

The principal factors that determine the adequacy of service, from the patrons viewpoint, are safety, frequency of service, accessibility, and speed. Of these the first is the most important.

Safety. Because the street car follows a pair of rails, in case of icy streets, foggy weather, or other unusual conditions, it may be expected to follow a definite fixed route. Rubber-tired vehicles must be steered, and hence under such unusual conditions are at some disadvantage. However, experience has shown that, given proper operating methods, such as the use of chains, and the sanding of slippery streets, these hazards can be reduced to a minimum.

With street cars, passengers must board or alight from the cars in the middle of the street, but with the rubber-tired vehicles loading takes place at the curb. Curb loading has the advantage that it is less dangerous for the patron because he is not forced to pass through vehicular traffic in order to reach the public conveyance.

Frequency of Service. In the eyes of the public, frequency of service probably has a greater bearing on the quality of a transportation service than any other

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single factor. The modern automobile has taught the public to expect from its transportation system accommodation in a measure approximating in convenience the house-to-house service of the automobile. Unfortunately cost is a limiting factor. The best that can be done is to render as frequent service as possible, with the smallest vehicle practicable to operate within the economic limitations.

Accessibility. Of almost equal importance with frequency of service is accessibility. Formerly the common practice was to space transit lines almost a mile apart. The patron walked, not from choice but of necessity, the half mile to the car line. With the advent of the automobile, an increasing percentage of the population refuses to walk this distance. Experience has shown that where the walking distance is much over a quarter of a mile, or where the service is too infrequent, the public will begin to use other forms of transportation.

This situation may be handled in three ways. One is to space the lines more closely together. This method has the disadvantage that unless the population is very dense, the traffic on all lines will be so light that it is economically impossible to give frequent service on any of them, even with small vehicles. A further disadvantage of this method is that the large number of small vehicles that would be required leads to congestion in the metropolitan areas. However, this may not be as great a limitation as might at first be supposed. The private automobile now on the street carries on the average approximately $1^3/_4$ persons per car. If the use of small vehicles on closely spaced lines would attract back to the public transportation system any considerable number of people that would otherwise use their own cars, the results might well be a decrease in traffic congestion.

The second possibility is to use small vehicles, usually busses, as feeders, transferring the passengers to a few trunk routes, on which the traffic will be heavy enough to warrant frequent service with larger and more economical units. The disadvantage of this plan is that a large proportion of the passengers must transfer.

A third method is to use a terminal system,[1] as that proposed by A. V. Eastman, in which all lines from the outlying districts terminate in two or more terminals located near, but still outside, the metropolitan area. Heavy trunk-line service is provided between these terminals, over several alternate routes so spaced as adequately to cover the metropolitan district.

Speed. Much of the appeal of the private automobile, aside from its convenience, is due to its superior speed. Although speed is important in a transit vehicle, it must not be attained at a sacrifice of safety. Owing to the large number of stops which it must make, no public conveyance in use on city streets will ever be able to equal the speed of the private automobile. A high schedule-speed (by schedule-speed is meant the average speed including all stops, layovers, etc.) is desirable for two reasons; first, it speeds the passenger to his destination, and second, it reduces the cost of giving service. Costs are reduced because both the number of cars required to run a given service and the amount of platform labor are decreased.

The schedule-speed of modern transit vehicles is much superior to that of their predecessors. For example, the Presidents Conference Committee car, usually called the PCC car, is designed to have a schedule-speed, on level track, of 14 miles per hour with eight seven-second stops per mile. The trackless trolley as far as acceleration, etc., are concerned is equally good. The gas bus, because of its limited power supply, and, if it is of the mechanical type, the necessity for shifting gears, is definitely slower than either of the other two; particularly if the number of stops per mile is large. This is one of the factors that makes the gas bus better suited to lines of light traffic, where the number of stops will be low.

The gas bus has a considerable advantage over both the other vehicles, in that, because it does have its own power plant, its route is not restricted to that of an overhead trolley. In times of emergency or even under normal[2] conditions this may be a very valuable asset. The self-contained power plant also makes possible both local and limited or express service on the same street without additional complications or investment. This double service is also made possible with the trackless trolley by use of either an auxiliary turnout in the overhead at the overtaking point, or of double overhead; either equipment requiring some additional investment. With street cars it is not feasible to operate both local and express service on the same street.

SECOND, ATTRACTIVE PROPERTIES OF VEHICLE

The private automobile has made the public more critical than formerly of its transit vehicles. In order to attract patronage the vehicle used should be comfortable to ride in and reasonably quiet in operation, and should be convenient to board and alight from.

Comfort and Quiet. In all of the new vehicles, regardless of type, much more attention is now being given to comfort and quiet. Seats are upholstered, and by an ingenious use of rubber in both running gears and body, the noise has been greatly reduced and the riding comfort improved.

Convenience. Because of curb loading, in convenience of ingress and egress, the advantage is very much in favor of the rubber-tired vehicles. The principal disadvantage of curb loading is that it may necessitate establishment of loading zones at the more important points, especially in business areas. To be effective, these zones must be kept clear of parked automobiles, so that the transit vehicle may draw in to the curb to pick up or discharge passengers.

THIRD, COST

In general, operating costs vary with both the size and kind of vehicle. For a given type, the operating cost per vehicle mile is less for a small conveyance than for a large one, but the cost per passenger mile is just the reverse. For a unit of given size, the gas bus requires the smallest capital investment, but has the highest operating cost (exclusive of interest and depreciation) of the three. Consequently it is best suited to lines of light traffic where the higher operating cost per mile is more than compensated for by the lower fixed charges on the smaller investment required. Hence, at light traffic volumes it yields a larger net return, with better headways and a smaller capital investment than would be possible with either the trackless trolley or the street car. The street car is best suited to very heavy mass transportation, where the traffic is heavy enough to require a large unit and is able to support the charges on the extra investment in track. Hence, with very heavy traffic, the street car, because of its larger size and lower operating cost per passenger mile, yields a greater net return, with entirely acceptable headways, than would be possible with either the trackless trolley or the gas bus, and this in spite of the large investment required for the track. The trackless trolley is intermediate between the other two, both in operating cost and investment.

As in any other business, in order for a transit system to continue as a going concern, its income must exceed, by a reasonable margin, its expenditures. Unfortunately, this has not been the case in the transit industry, as a whole, for many years.

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[1] For all numbered references, see list at end of paper.

Two methods for remedying this deficiency have been tried. The first is to increase the income by increasing the rate of fare; the second is to raise the net income by improving the service so as to attract more patronage, at the same time reducing the cost of operation per passenger.

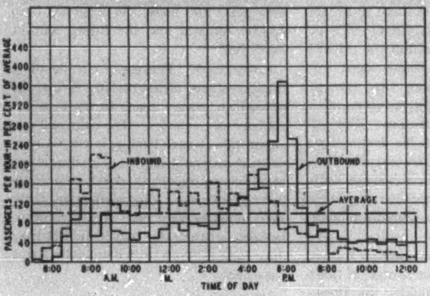


Figure 1. Typical curve of passengers per hour as a function of the time of day

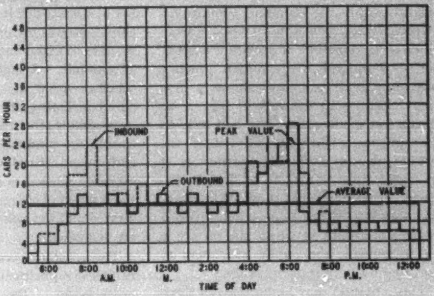


Figure 2. Typical daily car-demand curves

The efforts to increase the income by increasing the rate of fare have not been uniformly successful; in fact, in some cases where fares have been increased the resulting decrease in traffic has very largely offset the effect of the fare increase.

The second method, that of increasing the net return by improving the service, is much to be preferred. The most effective way of improving the service, and at the same time holding down costs, is to modernize the system by replacing old equipment with new vehicles properly chosen to fit the traffic conditions of each individual line.

So far as the author is aware, in all cases where a well-planned modernization program has been put into effect, the results have been better than were anticipated.[3-6] In addition to operating at reduced cost, the new vehicles have won and held increases in traffic that in many instances were truly remarkable.[2,7-10]

Factors That Determine the Choice of Vehicle

The foregoing discussion indicates that many factors influence the choice of vehicle in a given case. These may be classified roughly into two groups; first, those that have to do with the psychological appeal of the vehicle and, second, the economic factors.

The psychological or human elements of the problem are quite difficult or impossible to determine quantitatively and their relative importance in a given case is largely a matter of opinion or personal preference. For this reason no attempt has been made to determine which vehicle will have the greatest popular appeal but rather attention has been confined to the purely economic elements involved. Therefore, in what follows it is assumed that for equal headways the different vehicles are all equally capable of attracting patrons.

ECONOMIC CONSIDERATIONS

In determining the best vehicle with which to modernize a given line, three factors are of primary importance. These are, first, the net return; that is, the excess of income over total operating expenses; second, the possible headways; and third, the investment required.

The first is important because it determines the most economical vehicle to use. The second is important because it fixes the frequency of service and therefore, from the patron's viewpoint, largely the quality of the service rendered. The third is important because it determines the amount of new capital that must be provided.

For each kind and size of vehicle the net return, the headway, and the required investment all vary with the traffic to be handled. In a given case, the vehicle selected will normally be the one that can be operated most economically for the traffic existing. There are, however, exceptions to this rule. Sometimes, particularly with lines of light traffic, the most economical vehicle would require headways too large to give reasonably good service. In that case a smaller vehicle, operated with shorter headways, is desirable even at some sacrifice in the net return. Other exceptions may be found. For instance, the need to limit the number of types and sizes of vehicles on the system may result in choice of a vehicle other than the one that will give the best economy.

The reader has probably surmised that the problem of selecting the proper vehicle involves individual study of each line. This is the case particularly if an accurate forecast of the probable net return with the new equipment is to be obtained. However, as was noted previously, each vehicle has a certain volume range of traffic over which it is most economical. If this is known beforehand, then the problem of selection is greatly simplified.

Basic Data. Before the most economical traffic density range is determined for each type of vehicle the basic data must be established on which the solution of the problem depends.

The following general data were assumed to apply to all vehicles alike (the basis for each assumption is discussed in greater detail below): average schedule speed, 13 miles per hour; interest rate, five per cent; average fare, six cents; and duration of service, 19$^1/_2$ hours per day. Additional data applicable to specific vehicles are given in table I.

These data were derived from various sources. The average schedule speed of 13 miles per hour was estimated. As was mentioned before, the PCC car is designed to have a schedule speed of 14 miles per hour, with eight seven-second stops per mile. The trackless trolley is equally fast and modern busses could probably do as well in any service in which they would likely be used.

This value is probably high for dense traffic but it should approximate average conditions fairly closely. Admittedly, schedule speeds vary on different lines, but in general on a given line the schedule speed will be very nearly the same regardless of vehicle type.

The interest rate is taken at five per cent, as being approximately average. A low rate would be more favorable to those vehicles that require large capital investments.

The average fare of six cents is probably too low for the country as a whole. Its value would not affect the region of best economy in any case, although it would have a very decided effect on the net return. Consequently the comparison, as between vehicles, is the same regardless of the fare. The value of six cents was assumed because it is almost exactly the average fare, including transfers, etc., for the Seattle Municipal Railway System.

An allowance of ten per cent of the vehicles required on the peak was made for spares. This favors the gas busses slightly for, generallyspeaking, the availability of the electric vehicles is better than that of gas units.

The data listed in table I were also drawn from several sources. The values for vehicle-unit costs (column 2 of table I) were taken in part from manufacturers' bid prices, in part from published prices, or were estimated in those cases where information was not available from either. To determine accurate unit-cost data for the gas busses is particularly difficult because of the wide variations in specifications and manufacture for busses nominally of the same passenger capacity.

Depreciation. The life of new street cars is taken as 20 years; that of the trackless trolley as 10 years. These assumptions are close to the usual practice with regard to these vehicles. Except where stated otherwise, the useful life for the 30- and 40-passenger gas busses is taken as $6^2/_3$ years. For the 22-passenger gas bus, because of its generally cheaper construction, a useful life of 3 years is assumed. Practice with regard to depreciating gas busses varies considerably with different operating companies, the useful life assumed generally varying between 5 and 10 years.

As a matter of fact, any of these vehicles can be maintained almost indefinitely. Some street cars, still in operation, are more than 35 years old; similarly, trackless trolleys will last more than 10 years, and gas busses more than $6^2/_3$ years. In fact, the average age of the gas busses now in use in this country is estimated to be over 10 years. But few people nowadays want to keep a street car 35 years. Neither will they want to keep a trackless trolley or a gas bus more than a reasonable length of time. From a practical standpoint, if a vehicle is kept in service too long, operating costs go up, and generally patronage goes down. Modern advertising has taught the public to expect new models in its transit vehicles as well as in its private automobiles.

The assumed life of $6^2/_3$ years is undoubtedly very conservative for busses of good manufacture. Of course, if the depreciation rate were reduced these vehicles would make a somewhat better showing. Similarly, if the depreciation rate for the street cars were increased it would suffer by comparison. The effect of changing the depreciation rate is shown more in detail, for the case of the 22-passenger gas bus, in figures 20 and 21.

Use of a vehicle having a shorter life is not without advantage. For example, suppose that the condition of traffic and track on a given street-car line were such that the line could be modernized at practically equal cost with either new street cars or gas busses. Equal cost means that if new street cars are used they must be kept in service for 20 years in order to recover the investment. If on the other hand, the line be modernized by using, say, 40-passenger gas busses, which are "written off" the books in $6^2/_3$ years, the line could have new equipment of the same or better type, should better-type equipment be then available, every $6^2/_3$ years. Or if at the end of the depreciation period the equipment had some useful life remaining it could be kept in service until completely worn out. Each time new equipment is put into service the result will normally be an increase in traffic, for which the type of vehicle should have credit.

Another disadvantage of a long-term program is that frequently community development makes necessary improvements, such as grade changes, repairing operations, etc., which were not taken into account in the original calculations.

The depreciation rate on the new investment in track, which is assumed also to include any necessary rehabilitation of the overhead, has been taken at four per cent. The assumption is that street-car operation will be continued indefinitely. If street-car operation is to be discontinued at the end of the usual life (20 years) of the new rolling stock, the depreciation rate on the new investment in track will properly be five instead of four per cent. The depreciation rate on the overhead for the trackless trolley has been assumed to be five per cent. The above procedure is more than fair to the street car.

No attempt has been made to depreciate the old investment in track or overhead on the ground that if present street cars are to be replaced, for example by gas busses, all the present investment in track will have to be retired. Salvage value is ignored in all cases. Also no allowance is made for removing the old rails from the street in case rail operation is abandoned. Probably the most satisfactory method of handling this problem is to cover the old rails with asphalt until repaving becomes necessary, at which time they can be removed inexpensively.

Operating Costs. The operating costs per vehicle mile given in table I are estimates. Some of the values check reasonably well with recently published data[11] for similar vehicles. In different

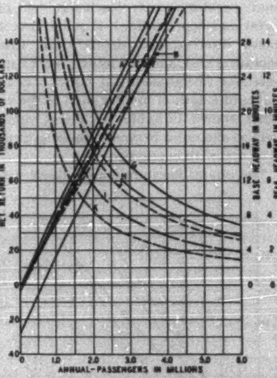


Figure 3. **Net return and headway versus annual passengers, three-mile line, L=0.9, m=2**

A—Net return, PCC car, with no track rehabilitation cost
B—Net return, PCC car, with track at $100,000 per double-track mile
C—Net return, 44-passenger trackless trolley
D—Net return, 30-passenger trackless trolley
E—Net return, 40-passenger gas bus
F—Net return, 22-passenger gas bus
G—Headway, PCC car
H—Headway, 44-passenger trackless trolley
I—Headway, 30-passenger trackless trolley
J—Headway, 40-passenger gas bus
K—Headway, 22-passenger gas bus

parts of the country operating costs vary considerably, owing primarily to differences in wages, and in fuel and power costs. The values quoted are as nearly average as could be determined from the data available. In sections where electric energy is expensive, the operating costs for the electric vehicles will be more than the figures quoted in table I, while cheap gasoline will tend to lower those for the gas busses.

Another cost, difficult to determine in a general study of this kind, is taxes. The difficulty arises from the fact that there are almost as many and varied methods of assessing taxes against transit systems as there are transit systems. In a specific case the taxes will be known and can be taken into account. The fact that taxes are herein neglected will not, in general, greatly alter the region of best economy for the various vehicles, because taxes would affect all to almost the same degree.

Determination of the Most Economical Traffic-Volume Range for Each Vehicle. From the basic data, previously established, values of annual-passengers, annual-vehicle miles, annual-revenue, annual-operating cost (exclusive of interest and depreciation), required investment, interest, and depreciation, total operating cost, and net return, were computed for various assumed values of headway and length of line for each of the vehicles shown in table I. These values were so chosen as to cover the usual range to be met with in practice.

The quantities enumerated above may be calculated individually; or, more conveniently, particularly if only the net return is required, the various relationships can be put into a single equation, thus:

Table I

Type of Vehicle	Unit Cost of Vehicle (Dollars)	Vehicle Seating Capacity	Depreciation (Per Cent)			*Average Operating Cost (Cents Per Mile)
			Vehicles	Track	Overhead	
54-passenger PCC car	15,000	54	5	4	4	23.0
44-passenger trackless trolley	13,000	44	10	None	5	18.5
30-passenger trackless trolley	9,500	30	10	None	5	15.5
40-passenger gas bus	10,000	40	15	None	None	21.0
22-passenger gas bus	2,200	22	$33^1/_3$	None	None	14.25

* Excluding interest and depreciation.

$$R=AF-365k_1lt_0C_a-\frac{k_3PUlC_p}{V}-\frac{k_4lT}{2} \quad (1)$$

or

$$R=\frac{alF}{2}-365k_1lt_0C_a-\frac{k_3PUlC_p}{V}-\frac{k_4lT}{2} \quad (2)$$

or again

$$R=730LFSt_0C_a-365k_1lt_0C_a-\frac{k_3PUlmC_a}{V}-\frac{k_4lT}{2} \quad (3)$$

where

R = net return
P = 1.1 = ratio of total vehicles, including spares to the vehicle required on the peak
F = average fare in dollars = 0.06
t_0 = hours of service per day = 19.5
V = schedule speed = 13 miles per hour
U = unit cost of vehicle in dollars
k_1 = average operating cost per vehicle-mile in dollars, exclusive of interest and depreciation
k_3 = interest rate plus depreciation rate (in decimal) on vehicles
k_4 = interest rate plus depreciation rate (in decimal) on track and overhead (street car) or on overhead (trackless trolley)
h = headway in minutes
C_p = cars per hour during peak period
C_a = average cars per hour = average ordinate of car-demand curve
T = cost of rehabilitating track and overhead, for the street car, or cost of overhead for the trackless trolley, in dollars per double track mile (DTM)
S = seating capacity of vehicle
L = average loading of vehicle, that is, the ratio of the average number of passengers per trip to the vehicle seating capacity, in decimals
l = round-trip length of line in miles
A = total annual passengers
a = total annual passengers per route mile $=2A/l$
m = ratio (in decimal) of the cars per hour on the peak (C_p) to the average, or base, cars per hour (C_a); is also equal to the ratio of the average, or base, headway to the peak headway

The first term, AF, is the annual gross income; the second term, $365k_1lt_0C_a$, is the yearly operating cost exclusive of

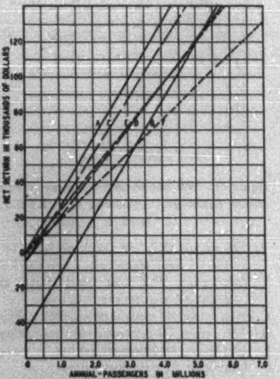


Figure 4. Net return versus annual passengers, five-mile line, L = 0.9, m = 2

Curve designations same as in figure 3

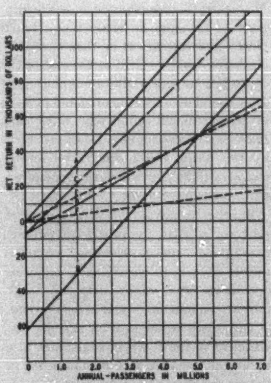


Figure 5. Net return versus annual passengers, seven-mile line, L = 0.9, m = 2

Curve designations same as in figure 3

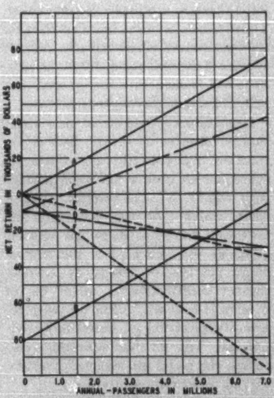


Figure 6. Net return versus annual passengers, nine-mile line, L = 0.9, m = 2

Curve designations same as in figure 3

interest and depreciation; the third term, k_3PUlC_p/V, is the interest and depreciation on the vehicles alone; while the last term $k_4JT/2$, is the interest and depreciation on the new investment in track and overhead, if the street car is under consideration, or the overhead in case of the trackless trolley.

Equation 1 assumes the same schedule every day, including Sundays and holidays. The error introduced only slightly alters the region of best economy for it affects all vehicles to much the same degree. This error could be eliminated by computing separately the annual vehicle-miles for weekdays and for Sundays and holidays, then adding. The additional accuracy obtained would hardly be worth while for the present purpose.

Figure 1 shows typical curves of passengers per hour as a function of the time of day for an existing street-car line. Figure 2 shows the corresponding curves of cars per hour with a 44-passenger vehicle, plotted to the same abscissas. The curves of both figures 1 and 2 were plotted from data taken during a traffic count on this line. For each size of vehicle there are vehicle-demand curves corresponding to those of figure 2.

The connection between the curves of figures 1 and 2, and equations 1, 2, and 3, may be shown as follows: the second term of equation 1 shows that the operating cost is proportional to the quantity t_0C_a, the area under the car-demand curve of figure 2. The daily vehicle-miles, and consequently the operating cost also, are dependent on the length of the line and on the area under the car-demand curve, not upon the shape of this curve, unless of course changing the shape of the curve will alter the average cost of operation per vehicle-mile. The third term of equation 1 contains the term C_p. This is the peak value, averaged over the time of a round trip, of the car-demand curve.

The total number of daily passengers is the area under the passenger-time curve. The total of annual passengers (A) must equal that of passengers per vehicle-mile multiplied by the annual vehicle-miles, or

$$A = \frac{\text{(average vehicle-loading)(seating capacity)}\\ \text{(annual-vehicle-miles)}}{\text{(route-miles of line, one way)}}$$

hence

$$A = \frac{2LS}{l}(365lt_0C_a) = 730LSt_0C_a \qquad (4)$$

Equation 4 shows that for a given vehicle size and loading the annual-passenger total is also proportional to the area under the car-demand curve which is t_0C_a. If the relationship of equation 4 and also that of $C_p = mC_a$ is substituted into equation 1, equation 3 is obtained.

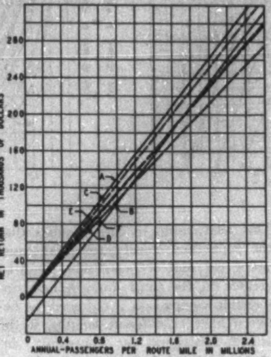


Figure 7. Net return versus passenger density, three-mile line, L = 0.9, m = 2

Curve designations same as in figure 3

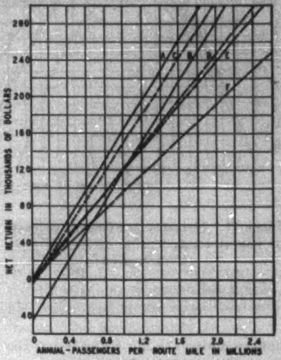


Figure 8. Net return versus passenger density, five-mile line, L = 0.9, m = 2

Curve designations same as in figure 3

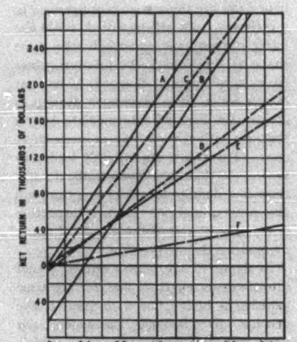


Figure 9. Net return versus passenger density, seven-mile line, L = 0.9, m = 2

Curve designations same as in figure 3

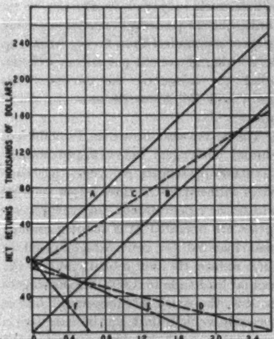


Figure 10. Net return versus passenger density, nine-mile line, L = 0.9, m = 2

Curve designations same as in figure 3

Results. The results of applying the above method of analysis are shown graphically in the following curves.

Figures 3 to 6 inclusive, show the relationship between net return and annual passengers for lines of varying length, assuming a constant average loading of 0.9 and a ratio of average to peak headway of two. In figure 3 are shown, in addition, curves of base and peak headway as a function of annual passengers for the different vehicles. Since the headway, for a given size of vehicle and number of annual passengers is independent of the length of line, headway curves are not shown in figures 4, 5, and 6, for they would be identical with those of figure 3.

Inspection of figures 3 to 6 inclusive reveals a number of interesting facts. For example, the curves show that, if the present track is in such good condition that it will require no more than normal

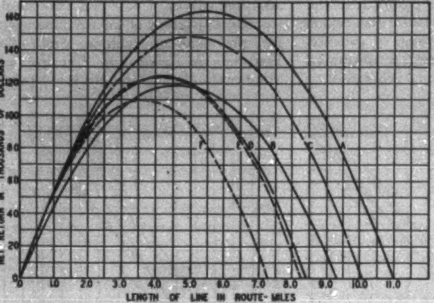


Figure 11. Net return as a function of length of line at constant traffic density; $a = 10^6$, $L = 0.9$, $m = 2$

Curve designations same as in figure 3

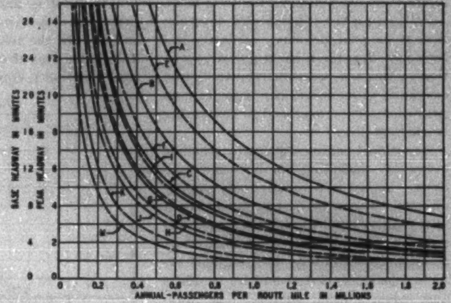


Figure 12. Headway as a function of traffic density

PCC car:
A—three-mile line C—seven-mile line
B—five-mile line D—nine-mile line

44-passenger trackless trolley:
E—three-mile line G—seven-mile line
F—five-mile line H—nine-mile line

22-passenger gas bus:
I—three-mile line K—seven-mile line
J—five-mile line M—nine-mile line

upkeep during the life (20 years) of the new rolling stock, it will be more economical to modernize with new street cars than with any other vehicle. If, on the other hand, the track must be rebuilt at a cost of $65,000[18] per double-track mile, which is about the minimum cost at which track in paved streets can be rebuilt, the 44-passenger trackless trolley is more economical than the street car, up to a value of annual passengers well beyond the range of the curves. This point will be discussed more in detail later.

The curves also show that, for values of annual passengers below approximately 2,000,000 passengers per year, the headways with any of the three larger vehicles will be very poor indeed, unless these vehicles are operated at a smaller average load than 90 per cent, which would in turn reduce the net return. When the annual load falls to 1,000,000 passengers or less, the small (22-passenger) bus is the only vehicle that can be run with a reasonably good headway. This point will also be discussed more completely presently.

Comparison of figure 3 with figure 5 shows that the net return, for a given number of annual passengers, is greater for the three-mile than for the seven-mile line. This result is to be expected, since for the same number of annual passengers, other factors remaining the same, the passengers per vehicle-mile must be larger on the three-mile than on the seven-mile line. But in order to have the same number of annual passengers, the annual-passengers per route-mile must be greater on the short line. This relationship in turn requires that the population density, assuming equal riding per unit of population, must be greater on the short line. A more interesting comparison is obtained, as between lines of different lengths, if the annual-passengers per route-mile, instead of annual passengers, is made the independent variable.

Accordingly in figures 7 to 10 inclusive, are shown curves of net return as a function of annual-passengers per route-mile. The vehicles used, and the other pertinent data, are the same as for figures 3 to 6 inclusive. As will be noted, while the latter curves have essentially the same shape as the former, the relative values have considerably changed. The net return for a given passenger density is greater for lines of intermediate length than for either very long or very short ones. This result immediately leads one to suppose that for a given traffic density there is a definite length of line that will give a maximum net return. Inspection of equation 2 and of the curves of figure 11 confirms this supposition.

In figure 11 are shown curves of net return versus length of line, for the same vehicles as before, for a constant traffic density of 1,000,000 passengers per route-mile per year. For each vehicle there is one definite length of line that will yield the greatest return. This optimum length is greater, the lower the total cost of operation per passenger mile. The curves also show that each vehicle has a maximum length of line beyond which a deficit will appear. This finding agrees with operating experience that long lines are relatively unprofitable unless local traffic is sufficient to keep the passengers per vehicle mile at a profitable level.

In figure 12 are shown curves of headway versus traffic density for 54-, 44-,

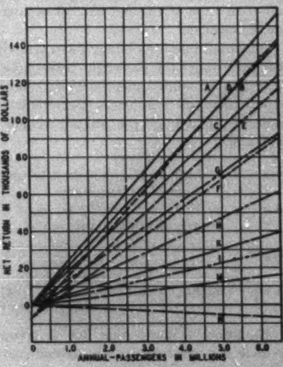


Figure 13. Curves of net return versus annual passengers for different values of the ratio m; seven-mile line, $L = 0.9$

PCC car:
A—$m = 1$ B—$m = 2$ C—$m = 3$

44-passenger trackless trolley:
D—$m = 1$ E—$m = 2$ F—$m = 3$

40-passenger gas bus:
G—$m = 1$ H—$m = 2$ I—$m = 3$

22-passenger gas bus:
K—$m = 1$ M—$m = 2$ N—$m = 3$

and 22-passenger vehicles, for three-, five-, six-, and nine-mile lines. These curves show that for a given number of passengers per route mile and size of vehicle, the longer the line the better the headways will be. This finding agrees with operating experience. As is common knowledge, it is difficult to give close headways on short lines with any but small vehicles, while on long lines it is relatively easy to provide good headways even with large units.

Effect on Net Return of Changes in Ratio of Peak to Average Number of Vehicles per Hour. In figures 3 to 11 inclusive, the ratio (m) of peak to base cars per hour was assumed to be two, which is almost the exact value for the line of figures 1 and 2. The value of m is a measure of the peak service necessary in terms of the average, and therefore of the required investment in vehicles. For this reason the net return will depend, to a considerable degree, upon the magnitude of m. This will be particularly the case with equipments that have a high first cost or a high depreciation rate.

On light lines where, for the sake of maintaining certain minimum standards of service, considerably better headways are usually provided during the off-peak period than the traffic warrants, the value of m may be less than two. On the other hand, on certain routes serving highly industrialized centers the value may be greater than three.

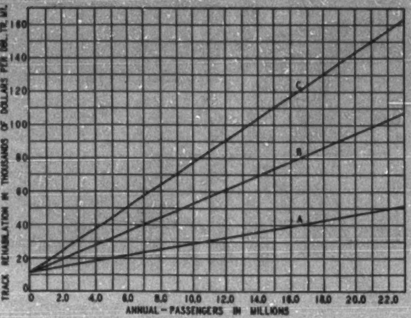


Figure 15. Track rehabilitation cost versus annual passengers for equal net returns, PCC car and 44-passenger trackless trolley; L = 0.9

A—$m = 1$
B—$m = 2$
C—$m = 3$

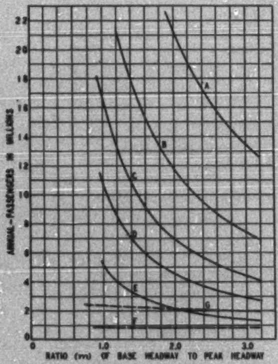


Figure 14. Annual passengers as a function of m for equal net return; L = 0.9

PCC car versus 44-passenger trackless trolley; track per double-track mile at:

A—$100,000 B—$60,000 C—$40,000
D—$30,000 E—$20,000

44-passenger trackless trolley versus 40-passenger gas bus:

F—Gas bus at 21 cents per mile
G—Gas bus at 20 cents per mile

Figure 13 shows the variation in net return with annual passengers for different values of m, so chosen as to cover the range likely to be met with in practice. This figure also shows that the region of best economy for each vehicle also varies with the value of m. As predicted above, the advantage is in favor of the vehicles of low cost and low depreciation rate.

In order to show the region of best economy for the PCC car, the 44-passenger trackless trolley, and the 40-passenger gas bus, the curves of figure 14 were computed. Any individual curve is the boundary line between the regions of maximum economy, under the stated conditions, for the two vehicles to which it refers. To make this point more clear suppose, for example, that a new investment of $40,000 per double-track mile will be needed to rehabilitate the track on a certain existing street-car line that is to be modernized. If the point corresponding to the annual traffic and the value of m for this line is located above and to the right of curve C of figure 14, the street car will give the more economical service; but if the point falls below and to the left of curve C the trackless trolley will give a larger net return. This conclusion does not give the trackless trolley credit for the fact that, because the headways with it will be approximately 22 per cent better than with the street car, it will therefore tend to attract a greater traffic increase.

To illustrate further the use of the curves of figure 14, the line of figure 2 is assumed, for which m is slightly greater than two. This line has an annual traffic of approximately 6,500,000 passengers. Curve C shows that, if a new investment of less than $40,000 per double-track mile is required to rehabilitate the track on this line, the street car will be the more economical; but if the new investment in track must exceed $40,000 per double-track mile, the 44-passenger trackless trolley will give service that is more economical and at the same time more frequent, by approximately 22 per cent.

Figure 15 shows the amount that may be spent in rehabilitating the street-car tracks for equal return with the 44-passenger trackless trolley for different values of m. For example, assume a line that has an annual traffic of 8,000,000 passengers and a value of m equal to three. Curve C of figure 15 shows that if track rehabilitation required an expenditure of less than approximately $64,000 per double-track mile, the street car would be the more economical, but that if the expenditure required exceeded that amount the trackless trolley would be the more economical as well as giving more frequent service.

Importance of Average Loading. In figures 3 to 15 inclusive, the average passengers per vehicle was maintained constant at 0.9 or 90 per cent of the vehicle-seating capacity. Equation 3 shows that a small change in the value of L, the average vehicle loading, will produce a relatively large effect on the net return. This relationship is shown by the curves of figures 16 and 17. The curves show that, for the three-mile line, a 20-per-cent decrease in the value of L (which is another way of saying a 20-per-cent decrease in passengers per vehicle-mile) produces a decrease in the net return that varies from approximately 26 per cent, for the street cars, to about 32 per cent for the 22-passenger bus; while for the seven-mile line the same percentage of decrease in L produces a decrease in the net return that varies between approximately 41 per cent, for the street car, and about 147 per cent for the 22-passenger gas bus. This shows the great importance—one might almost say the

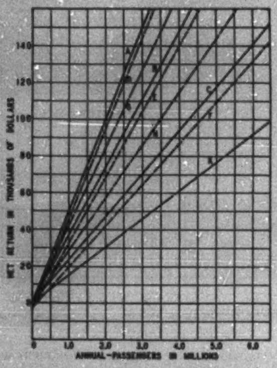


Figure 16. Net return as a function of annual passengers at constant values of vehicle loading, three-mile line, m = 2

PCC car, no track rehabilitation:
A—*L* = 1.0 *B*—*L* = 0.8 *C*—*L* = 0.6
44-passenger trackless trolley:
D—*L* = 1.0 *E*—*L* = 0.8 *F*—*L* = 0.6
22-passenger gas bus:
G—*L* = 1.0 *H*—*L* = 0.8 *K*—*L* = 0.6

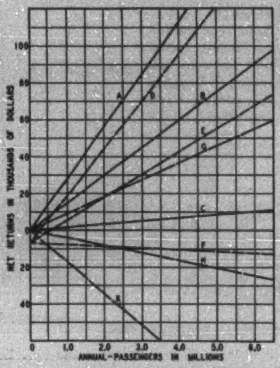


Figure 17. Net return as a function of annual passengers at constant values of vehicle loading, seven-mile line, m = 2

PCC car, no track rehabilitation:
A—*L* = 1.0 *B*—*L* = 0.8 *C*—*L* = 0.6
44-passenger trackless trolley:
D—*L* = 1.0 *E*—*L* = 0.8 *F*—*L* = 0.6
22-passenger gas bus:
G—*L* = 1.0 *H*—*L* = 0.8 *K*—*L* = 0.6

necessity—of keeping the average loading high, particularly on long lines with vehicles that have relatively high operating costs per passenger mile.

This more than proportional change in net return with a given change in loading, is one factor that makes possible, in certain cases—principally with light lines—replacement of a larger vehicle with one of less capacity, thus giving a more attractive headway, and still operating at a profit. During periods of light load, with the same or better headway, the small vehicle will be more nearly loaded, thus raising the value of the average passengers-per-vehicle-mile.

The factors involved can perhaps be most conveniently illustrated by use of an actual example. Figure 18 shows the relationship between passengers-per-hour and the time of day for an existing 6.9-mile street-car line having an annual traffic of approximately 1,745,000 passengers. It is proposed to modernize this line, using the vehicle that will give the best all-around performance. Figure 5 indicates that this line can probably be operated most economically with a 44-passenger trackless trolley, unless the track is in such good condition that an investment of approximately $25,000 per double-track mile will restore it to normal; in this case the PCC car would be the most economical. But reference to the headway curves of figure 3, which also apply to figure 5, indicates that the possible headways, with either the 44-passenger trackless trolley or the PCC car, will not be particularly good, certainly not good enough to attract any considerable amount of additional patronage. This result immediately suggests the desirability of using a smaller vehicle if such a course can be justified economically.

Figure 19 shows vehicle-demand curves for units of the several sizes made up from the traffic curves of figure 18. For the most part the curves of figure 19 provide very liberal schedules. In no case would passengers have to stand during the midday period. This insurance is highly desirable if increased riding is to be encouraged, particularly during the off-peak period. Also the maximum headway was set at 20 minutes, excepting only after 11:00 p.m. After 11 the headway is 30 minutes. The same schedule was assumed for both directions of travel.

Because of the large difference in traffic in the two directions on this line it is difficult to keep the average vehicle loading at a good figure without at the same time forcing some passengers to stand, in the heavy direction of travel, even during the off-peak period. The operating results to be expected for the various conditions and vehicle sizes are set out in table II. Line 1 of table II shows the forecast for the PCC car, assuming an average track rehabilitation cost of $20,000 per double-track mile. If the required expenditure for track rehabilitation is greater than approximately $24,500 per double-track mile, the street car will incur a deficit.

Line 2 of table II shows the projected results for the 44-passenger trackless trolley, with present traffic and a vehicle-demand as shown by curve *C* of figure 19. By decreasing the headways after 7:00 p.m. to values more nearly in line with the traffic after that time, and one or two other minor adjustments in schedule, the $899 deficit may be converted into a net gain of $6,556.

Line 4 shows the results to be expected if the new equipment is able to attract a ten-per-cent increase in traffic. This item shows very strikingly the improved results with even a very moderate gain in patronage. Items 5, 6, and 7 are the same as items 2, 3, and 4, except that they apply to the 40-passenger gas bus. Curve *C* is liberal enough so that a ten-per-cent, and better, increase in traffic can be accommodated without an increase in facilities.

Items 8 and 9 are for the 30-passenger trackless trolley. The first is for present traffic; the second assumes an increase in traffic of 15 per cent (15 per cent is assumed because of the better headways offered by the smaller vehicle). With the 15-per-cent increase in patronage it would be desirable to augment the facilities, as shown by curve *B*. The net return, with the 30-passenger trackless trolley and the 15-per-cent increase in traffic, is $10,502 as compared with $9,571 for the 44-passenger trackless trolley with a 10-per-cent increase in traffic. In addition, the headways with the smaller vehicle are much better and also, owing to fewer stops with it, its schedule speed would be higher.

A 30-passenger gas bus that cost $8,000 per unit and had a useful life of eight years (not 6$^{2}/_{3}$ as assumed for the 40-passenger gas bus in figures 3 to 17 inclusive) would have to have an operating cost of not over 16.87 cents per mile, in order to equal the performance of the 30-passenger trackless trolley. Whether the gas bus could be operated for only 1.37 cents more per mile than the trackless trolley is doubtful. However, the gas bus would have a decided advantage, in that express service could be provided during the peak period without additional complication or expense; something not possible with the trackless trolley.

The small or 22-passenger gas bus, at 14.25 cents per vehicle-mile, shows a deficit on this line, unless the loading is kept

Table II

Item Number	Vehicle	Schedule Curve From Figure 19	Annual Passengers	Headway in Minutes: Peak	Headway in Minutes: Mid-day	Headway in Minutes: Evening Early	Headway in Minutes: Evening Late	Average Loading in Per Cent of Vehicle Seating Capacity	Passengers Per Vechicle Mile	Annual Miles Per Vehicle	Net Return in Dollars	Remarks
1	54-passenger PCC car	*D*	1,745,000	8.5	20	20	30	66.2	5.19	30,455	2,769	Track at $20,000 per double-track mile
2	44-passenger trackless trolley	*C*	1,745,000	6	15	20	30	68.6	4.37	26,297	*899	*Deficit
3	44-passenger trackless trolley	Not shown	1,745,000	6	15	30	30	76.3	4.86	23,643	6,556	
4	44-passenger trackless trolley	*C*	1,919,500	6	15	20	30	75.4	4.81	26,297	9,571	10 per cent traffic increase over (2) above
5	40-passenger gas bus	*C*	1,745,000	6	15	20	30	75.3	4.37	26,297	*2,228	*Deficit
6	40-passenger gas bus	Not shown	1,745,000	6	15	30	30	83.8	4.86	23,642	6,234	
7	40-passenger gas bus	*C*	1,919,500	6	15	20	30	83.0	4.81	26,297	8,242	
8	30-passenger trackless trolley	*A*	1,745,000	5	12	20	30	81.9	3.56	27,305	3,248	
9	30-passenger trackless trolley	*B*	2,006,750	4.6	10	20	30	86.5	3.75	27,701	10,500	15 per cent traffic increase over (2) above
10	30-passenger gas bus	*B*	2,006,750	4.6	10	20	30	86.5	3.75	27,701	4,472	15 per cent traffic increase. 30-passenger gas bus at $8,000 each. Eight-year life. Operating cost at 18 cents per bus mile

high. To accomplish this, passengers must stand in the heavy direction of travel, even during part of the midday period. This is very undesirable if off-peak riding is to be encouraged. The traffic on this line is too large for the most economical operation of the 22-passenger unit.

Undoubtedly the 30-passenger trackless trolley is the proper vehicle to use on this line, unless the cost figures for the 30-passenger gas bus are more favorable than is indicated above.

The above analysis shows very clearly the comparatively large gains in net return due to even small increases in patronage. Therein lies one of the opportunities of modernization. Experience has shown that where service is improved, increased riding results. The best way to improve service, particularly on light lines, is to decrease the headways by substituting smaller, faster, more economical units, operated on closer schedules. In many cases where this has been tried, the results[3] obtained have been remarkable. Lines that were otherwise unprofitable have been converted into money-makers. This is the field for the small bus, and accounts in part for its growing popularity.

The above analysis also shows the importance of adjusting schedules so as to keep the vehicle loading as high as possible, consistent with good service. It shows too what is meant by "operating the smallest vehicle possible within the economic limitations".

Operating Cost. As was mentioned previously, the operating cost, exclusive of interest and depreciation, is the most important of the items of expense in the operation of a transit line. Its importance is indicated by the curves of figures 20 and 21. These show, first of all, that on a short line the operating cost is relatively unimportant. For example, figure 20 shows that for the three-mile line, if the operating cost of the 22-passenger gas bus is reduced from 14.25 to 11 cents per mile, a change of approximately 23 per cent, the corresponding variation in the net return will be slightly less than 13 per cent.

For a long line the conditions are very different. Even small changes in operating cost produce relatively large variations in the net return. This is particularly true of the small units. These statements can be verified by reference to figure 21, which shows that, for a seven-mile line, if the operating cost of the 22-passenger bus varies over the same range of 23 per cent as assumed above, the corresponding change in the net return will be about 72 per cent instead of 13.

Figure 18. Passengers per hour as a function of the time of day

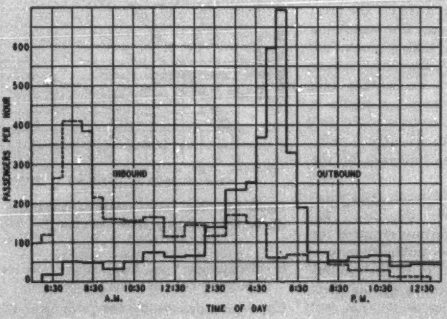
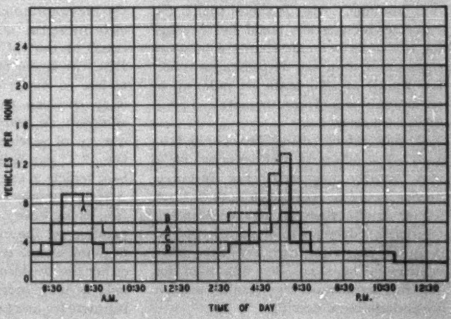


Figure 19. Car-demand curves

A—30-passenger vehicle, present traffic
B—30-passenger vehicle with 15 per cent traffic increase
C—40- or 44-passenger vehicles
D—54-passenger PCC car



In the curves of figures 3 to 10 inclusive,

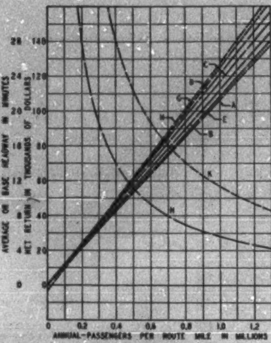


Figure 20. Net return versus passenger density for the small bus; three-mile line, L = 0.9, m = 2

22-passenger gas bus, three-year life; operating cost per mile at:
A—$14\frac{1}{4}$ cents C—12 cents
B—13 cents D—11 cents

22-passenger gas bus, four-year life:
E—Operating cost $14\frac{1}{4}$ cents per mile

44-passenger trackless trolley, ten-year life; operating cost per mile at:
G—$18\frac{1}{2}$ cents H—$17\frac{1}{2}$ cents

K—Headway, 44-passenger trackless trolley
N—Headway, 22-passenger gas bus

the operating cost of the small bus is taken as 14.25 cents per bus mile. This value should be conservative for this unit; in fact some transit companies[13] have operated it for approximately 11.75 cents per mile, exclusive of taxes, interest, and depreciation.

The importance of a low operating cost is strikingly illustrated by the curves of figure 21. This figure shows that an increase in the normal life of this vehicle from three years to four years has about the same effect on the net income as a decrease in the operating cost of one-half cent per mile. Also, it shows that if the 22-passenger gas bus could be operated for ten cents per mile it would be more economical than the 44-passenger trackless trolley at 18.5 cents per mile, up to an annual load of approximately 5,000,000 passengers.

If this vehicle could be operated at such a low figure its usefulness would be very greatly increased. For instance, service would be possible on lines of much lighter traffic than is now economical. Of course, a decrease in operating expense is equally important with any of the other vehicles. The small bus was used for illustration because the results with it are perhaps more striking.

The fact was mentioned previously that to keep equipment in service too long is unprofitable. The small bus is more likely to fall under this rule than any other vehicle. For the conditions shown in figures 20 and 21 the average annual mileage per vehicle amounts to approximately 42,000. On the basis of three years of life, the annual interest and depreciation per unit amounts, at $2,200 each, to $843. If at any time the difference in operating cost per mile between the old and a new bus amounts to as much as two cents per mile, the saving in operating cost will be enough to pay the interest and depreciation on a new vehicle.

A much more important consideration is that patronage falls off if the vehicle is kept in service too long. As was indicated previously, only a very small drop in patronage would be required to reduce the net return by an amount equal to the carrying charges on a new vehicle. Obviously it is unprofitable to keep old equipment in service too long, particularly since new equipment generally brings an increase in riding. With the more expensive equipments obsolescence and decreased riding would make the change desirable long before increased operating costs would have a very great effect.

Capital Costs. In figure 22 is shown for a representative case the amount of new investment required for varying values of traffic density. The curves show that the small bus requires by far the least investment of any of the vehicles considered. For the others the investment required, in vehicles alone, is not greatly different; but when the cost of the overhead for trackless trolley, and of the track and overhead for the street car, are added, the required investment, especially for the street car, may be greatly increased.

Conclusions

The first conclusion to be drawn from the previous discussion is that if the present track has a useful life remaining equal to that of new rolling stock, the PCC car is the most economical vehicle to use in modernizing an existing line; if in addition the volume of traffic is sufficient to warrant a reasonable frequency of service with so large a unit, the PCC car will also probably be the best vehicle to use.

If on the other hand track rehabilitation is necessary, then—the choice depending on the expenditure required for this work and on the traffic density—either the trackless trolley or the gas bus may be more economical as well as offering more frequent service.

Figure 14 shows that, if the track must be completely rebuilt at a cost of approximately $60,000 per double-track mile, the 44-passenger trackless trolley is more economical than the PCC car up to values of annual passengers of approximately 5,500,000, 7,000,000, and 11,000,000, corresponding to values of *m* of four, three, and two respectively.

Five and one-half million passengers per year is fairly heavy traffic. The heaviest line in Seattle has an annual load of approximately 6,500,000 passengers with a value of *m* equal to practically two. (The curves of figures 1 and 2 are for this line.)

In a given case the above conclusions may be modified by a full consideration of the psychological factors involved. The quantitative relations may also be altered if the various costs are appreciably different from the values used above.

The principal advantage of the street car is its ability to handle heavy traffic, with sharp peak loads. In this connection it should be mentioned that at about the time this study was nearing completion one of the vehicle manufacturers announced a new rubber-tired vehicle,[14,15] available either as a trackless trolley or as a Diesel-electric bus, of practically street-car capacity. As no cost data were available on this vehicle it is not included in this report. But if, as would be expected, first cost, operating cost, depreciation, etc., for this unit are in line with corresponding costs for the smaller trackless trolleys now in use, the curve for this vehicle could be expected to occupy a

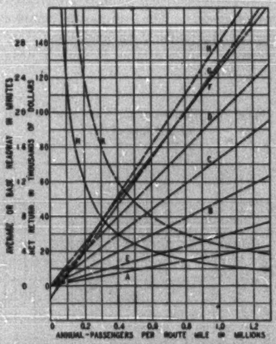


Figure 21. Net return versus passenger density for the small bus; seven-mile line, L = 0.9, m = 2

F—22-passenger gas bus, three year life operating cost per mile at ten cents

Other curve designations same as in figure 20 place, in figures 3 to 13 inclusive, very close to the curve for the PCC car without track-rehabilitation cost. If this should prove to be the case, and if the operating characteristics of the new vehicle are also satisfactory, the street car has a worthy competitor in the heavy-traffic field.

Unless some new development should bring much cheaper track, or a marked drop in interest rates occurs, it is probable that, as present tracks wear out, economic pressure will force the gradual retirement of the rail car from all except the very heaviest surface lines.

The future role of the trackless trolley and of the gas bus is not quite so clear, but for the near future the backbone of the transit system appears to be the trackless trolley, with gas busses on the lighter lines. Figure 14 shows that with an operating cost of 18$^1/_3$ cents per mile for the 44-passenger trackless trolley and of 21 cents per bus mile for the 40-passenger gas bus, the trackless trolley is the more economical for all values of traffic greater than approximately 800,000 passengers per annum. If the operating cost of the gas bus can be reduced to 20 cents per mile the point of equal return is raised to approximately 2,000,000 passengers per year. Evidently the position of the line separating the regions of best economy for these two vehicles is very sensitive to small changes in the relative operating costs. The relative position of these two vehicles could be expected to vary considerably in different sections of the country due to differences in fuel and power costs.

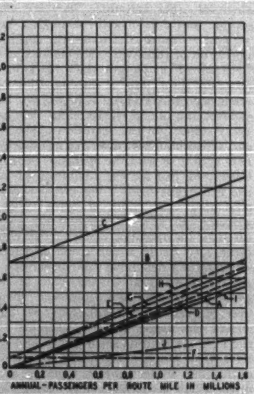


Figure 22. Investment required versus traffic density; seven-mile line, L = 0.9, m = 2

A—PCC car, vehicles only
B—PCC car, track only
C—PCC car, total
D—44-passenger trackless trolley, vehicles only
E—30-passenger trackless trolley, vehicles only
F—Trackless trolleys, overhead only
G—44-passenger trackless trolley, total
H—30-passenger trackless trolley, total
I—40-passenger gas bus
J—22-passenger gas bus

Undoubtedly the future will bring additional improvements in both vehicles that will reduce operating costs. The Diesel-electric is a step in this direction for the self-propelled vehicle. Whether the Diesel-electric, or perhaps the Diesel with hydraulic transmission, will be able to partially or completely supplant the electric vehicles remains to be seen. One influence that will tend to prevent this replacement is the fact that the trend in power rates is steadily downward while that of gasoline and oil prices is up.

The previous discussion also shows the importance of good engineering and good management in a prospective modernization program, not alone in selecting the most suitable vehicle for each line, but also in adjusting schedules and maintaining service so that riding will be encouraged.

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Discussion

G. M. Woods (nonmember; Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Professor Hoard's paper is a valuable document to those engineers who are faced with a decision as to the type of vehicle to use for particular transit conditions. The study of the problem from the patron's standpoint as well as the economic standpoint is particularly desirable. The ability to attract new patronage and, by merit, to retain it must be the primary objective of any transportation system. Frequently economies in operation have been carried too far, with the result that gross revenues have decreased more rapidly than operating expenses. Fortunately, the modern vehicles and equipment available are not only more attractive to the passengers but they also are more economical to operate than the older vehicles.

Care must be exercised in a study of this type to avoid drawing general conclusions from a specific example. All of the conditions surrounding any particular application must be carefully analyzed and the correct basic data determined. The paper points out the material effect on the "regions of best economy" of relatively minor changes in operating cost.

Not only must present conditions be considered but thought must be given to the future also. For instance the author assumes a useful life of 20 years for street cars, 10 years for trolley coaches, 6$^2/_3$ years for the larger motor busses, and 3 years for the 22-passenger busses. The price which must be paid for replacement vehicles at the end of 3, 6, or 10 years is unknown, although it is an important factor in long-term planning. As brought out in the paper the downward trend of power rates is in favor of electrically propelled vehicles.

L. C. Josephs, Jr. (Mack Manufacturing Corporation, Allentown, Pa.): Perhaps the most important factor in the choice of transportation equipment is the matter of merchandising and not engineering. There has perhaps been too much engineering in some places and not enough attention paid to the merchandising of transportation. The engineering side is apt to look on such factors as the cost of operation, without paying enough attention to the rather indefinite question of what makes people ride and how can more of them be made to ride.

In a general way, the engineering side of transportation equipment has to do more with the cost of operation, whereas the merchandising side of transportation has to do more with the question of gross income. Sometimes to get the optimum difference between income on one hand and operating expense on the other hand, it is necessary to do things that may seem wrong from a strictly engineering standpoint. That, however, is just what some of the railroads are doing on their modern streamlined trains, where more attention is being paid to showmanship and less to operating economies. Maybe in this present day it is necessary to use considerable showmanship to get the people to ride on the urban transit systems.

H. E. McWethy (Twin City Rapid Transit Company, Minneapolis, Minn.): In discussing this paper I would first like to commend Professor Hoard on the thoroughness with which he has handled the technical aspects of a complicated problem. It is not easy to condense in so short a paper all of the diversified factors which should be and must be given consideration in the determination of the most economical type of vehicle to serve efficiently and safely the requirements of the riding population of any given community. While it is evident throughout the paper that Professor Hoard has had in mind the application of his general solution to the needs of the city of Seattle, Wash., it is also evident that the principles which he has set forth and the manner of solving the problem has a rather universal application to the street-railway systems of the United States. It is no disparagement to the excellence of this paper to say that many of the principles set forth therein have been used by the transportation industry in one form or another in the solution of similar problems for a considerable number of years. This is the first time, however, that we have had the advantage of having these methods set forth as a coordinated whole amply illustrated by the use of graphs.

I presume that our local engineers in the Twin City area would be happy if in this discussion I could take the graphs and formulas presented by Professor Hoard and apply them to the operations of the local transportation system of Minneapolis and St. Paul and thus determine just what should be done with this system. Obviously in a short discussion such an accomplishment is impossible. However, without attempting to reach definite conclusions, I would like to leave certain thoughts for your consideration which came to me as I went over this paper attempting roughly to evaluate our own performance in the light of the methods of analysis proposed in this paper.

At the outset Professor Hoard's analysis gives little if any consideration to the economy of the continuation of equipment which is at present in use on a given system. He assumes that all existing equipment has reached the point where it is uneconomical to continue its use further and that the choice of the transportation industry lies only with the PCC car, the trolley bus, and the gasoline bus. So far as our own system is concerned, we believe that this conclusion as to existing equipment being obsolete for the purpose of solving our transportation problems is not entirely warranted. We have reason to believe that the newer types of equipment must prove themselves by demonstrated economies over the equipment at present in use. In our case unless the newer types of equipment can demonstrate a much greater attractiveness to the public than the equipment at present in use, thus increasing the riding habit materially, there can be little question, so far as our entire system is concerned, as to the net economy of the continuance of the existing equipment in service. Quite rightly, we believe, Professor Hoard dismisses the question of increased riding which would follow the adoption of the new equipment as controversial and a matter of individual opinion, and he assumes for the purpose of his analysis that no change in the riding habit would follow the adoption of a different type of vehicle.

Having reached this conclusion, the remainder of the problem lies in the decision as to whether or not it is more economical with present patronage to continue in operation existing equipment than to adopt one or more of the three types of equipment which are suggested by Professor Hoard.

To justify the increased fixed charges on new equipment purchased there must be quite a marked differential in performance between the old and the new equipment. This differential may take the form of increased speed, greater comfort and convenience of passengers, or lower maintenance costs. The need for the new type of equipment will be most apparent on those properties where this differential between the old and new equipment is greatest. On properties where the differential is least no justification for a change may exist. We feel that the equipment on our property offers a case in point. Certain it is that good economics would frown on the adoption of stream lines without perceptible increase in the comfort and convenience of passengers, and it seems also fundamentally true that to destroy useful remaining life of well-maintained equipment without securing in the new equipment increased benefits of equal or greater value would be likewise unsound. We feel at the present time that the only case where the adoption of a new vehicle is warranted on this system is where a large section of track and paving on an existing line is worn out and must be replaced. In such a case the adoption of a rubber-tired vehicle may be more economical than the continuance of existing rail service.

When we come to compare operating data experienced on this system with that assumed by Professor Hoard, we find on the whole that a close similarity exists. For instance, in table I of his paper he shows 23 cents per car-mile as the average operating cost (excluding depreciation) for the PCC car. The comparable figure on our system for 1938 was 21.8 cents. For our busses which average 26 passengers each, the annual operating cost, exclusive of depreciation, was 15.78 cents per bus-mile, which may be compared with 21 cents per bus-mile which he has used for a 40-passenger gas bus and 14.25 cents for a 22-passenger gas bus.

The average fare per passenger (including transfer passengers) which Professor Hoard assumed of 6 cents compares with the average railway fare on this system of 5.52 cents. (Our record indicates that the average fare per *revenue passenger only* in Seattle approximates 8.3 cents as compared with 7.75 cents, the average fare on this system per revenue passenger.)

Now just a word as to the transportation property which serves the Twin City area. The population served approximates 800,000. The number of street cars is 715, operated on 459 miles of track. These cars operate approximately 21,860,000 car-miles annually and haul in the neighborhood of 100,000,000 passengers annually. This railway operation is supplemented by the operation of 116 gasoline busses which operate approximately 3,800,000 miles annually and carry approximately 4,500,000 revenue passengers. Of the 715 railway cars, all but 35 are unusually heavy cars weighing 44,000 to 50,000 pounds each. Of these heavy cars 459 are equipped with four 50-horsepower motors, and 221 are equipped with four 40-horsepower motors. The 35 lightweight cars average approximately 29,000 pounds, and are equipped with four 25-horsepower motors. These weights may be compared with approximately 35,000 pounds for the average PCC car. Practically all of the heavy cars are equipped with field shunting devices which, while resulting in higher consumption of electricity, have resulted also in increasing our average speed. The average speed of our railway cars for 1938 amounted to 10.72 miles per hour, which according to the American Transit Association records was, I believe, third highest in the nation.

It is true that the heavy cars in use on this property were designed and built 20 or 30 years ago. They were not, however, designed and built as most of the older cars which have been and many of which still are in use on many of the transportation systems of this nation. They appear to have had points of design which still make them more acceptable to our patrons as comfortable-riding vehicles than most of the older cars of other systems to which we refer. Moreover, they have been maintained considerably above the average of those of other systems. They are clean, strong, and comfortable—somewhat noisy we grant—but on the whole the continued operation of these very cars we believe has been one of the factors which has kept our company on a basis of earnings which has made it possible to keep us paying our bond interest and out of receivership. We have every respect for the operation of the PCC car, and although we have had no experience here with it or with the operation of trolley busses, we can see the advantage of their use in certain fields. We are slow to be convinced that either of these types of vehicles can now be economically substituted for the operation of our present equipment. To do so would result in the destruction of a remaining value in our present equipment which we feel would not be compensated for in the new equipment. Moreover, our experience with our own lightweight cars makes us rather skeptical that the PCC car with its small wheels and relatively light weight can give adequate service during our winter months, bucking snowdrifts which accumulate here during these months and contending with the low temperatures which also contribute to the spinning of wheels and the slowing up of service. We have found our own lightweight cars to be inadequate in snowstorms and low temperatures, and conversely we have found that our heavy cars powered with 40- and 50-horsepower motors do maintain our schedules during winter months even though our wheel wear during these months averages six times as great as it does during the summer months of June, July, and August. Under the circumstances, therefore, it appears to us that the substitution of these new types of rolling stock for our present standard cars must be predicated upon our being relieved of the expense of rebuilding of a substantial section of wornout track and paving in a particular route. Having such a route up for consideration, decision as to whether to substitute trolley busses or gasoline busses must rest upon the principles outlined by Professor Hoard in this paper.

While we may not agree that the time is ripe here for the substitution of some of the newer types of equipment, this very fact can be brought out most effectively by such an analysis as Professor Hoard has presented in this paper.

H. A. Perryman (nonmember; Los Angeles Railway Corporation, Los Angeles, Calif.): This paper has been developed with considerable labor and research and is based necessarily on assumed averages for various operating and traffic factors. As an academic study the paper shows considerable enterprise and has, in my opinion, a decided value in the field of research.

In general, these assumed averages do not apply to this property, with the result that the charts as developed cannot be applied to specific lines operated by the Los Angeles Railway.

The study is based on an average 6-cent fare which is higher than our average fare (4.51 cents) and an average speed of 13 miles per hour which is higher than our average rail speed (10.5). Rates of depreciation and interest are also different.

The study is also based on two assumed averages, which are not often found on our lines, namely *L*, or the average loading of vehicle, and *m*, or the ratio of the cars per hour in the peak to the average base cars. Table I of this discussion is a tabulation showing the values of *L* and *m* on our rail and principal coach lines.

The study is based on interest rates for new money only, and does not make any allowance for interest on tracks already in place and in good operating condition. As a result of this the values of *A* in the charts (PCC cars with no track rehabilitation) are somewhat delusive.

The study is based, of course, purely on engineering and/or accounting data and does not take into consideration the operating policy of the company or the popular requirements of the districts to be served.

In planning the modernization of a line or property, the nonengineering considerations, particularly public sentiment with regard to the choice of street car, trolley bus, or bus, are of primary importance.

George L. Hoard: There are in this country transit systems of almost all degrees of preservation. On some systems the equipment is old and run down, with very little useful life remaining. Contrariwise, on others the standards of maintenance are much above the average. Equipment that nominally belongs to a previous generation has been rebuilt one or more times, so that while its appearance may not compare favorably with the latest in modern vehicles, its operating characteristics and cost of maintenance may be reasonably satisfactory. The discussion by Mr. McWethy indicates very strongly that the property of the Twin City Rapid Transit Company falls in the latter class.

Table I. Los Angeles Railway Corporation—Passengers Carried, Load Factors, and Equipment Requirements

	Miles of Route (One Way)	Passengers Carried in 1938 (Thousands)	Passengers Per Mile of Route (Thousands)	Passengers Carried Daily	Number of Trips Daily	Number of Seats	Value of L	Value of m
Rail lines								
A	9.36	9,786	1,046	23,118	396	48	1.2	1.9
B	10.06	10,594	1,053	29,263	356	48	1.7	1.9
D	3.13	1,998	638	4,905	210	44	0.5	1.0
F	12.92	7,736	599	18,073	312	44	1.3	1.8
H	9.62	9,088	945	24,027	403	48	1.2	1.6
I	1.51	609	403	1,360	202	34	0.2	1.0
J	12.54	15,310	1,221	42,690	470	59	1.5	1.6
K	6.44	1,809	281	4,609	156	44	0.7	1.0
L	11.01	12,132	1,102	27,326	409	48	1.4	2.0
N	4.94	6,106	1,236	18,934	326	48	1.2	1.9
O	9.04	6,896	763	16,270	264	44	1.4	1.5
P	10.06	25,877	2,572	63,447	637	59	1.7	1.7
R	11.14	15,397	1,382	38,559	503	48	1.6	1.4
S	12.42	18,163	1,462	42,567	518	48	1.7	1.6
U	13.02	16,675	1,281	43,286	424	44	2.3	1.5
V	12.05	15,585	1,293	36,821	500	48	1.5	2.4
W	15.70	17,605	1,121	45,081	569	48	1.7	1.8
2	6.81	2,816	414	6,636	232	44	0.7	1.1
3	7.00	12,280	1,754	28,293	498	53	1.1	2.0
5	21.80	16,420	753	39,948	508	52	1.5	1.9
7	9.63	8,765	910	21,622	370	48	1.2	1.9
8	8.15	6,863	842	19,028	366	44	1.2	2.1
9	8.37	6,089	727	14,611	325	48	0.9	2.1
10	9.93	5,917	596	13,934	263	44	1.2	1.4
Coach lines								
Through coach lines								
44 Beverly Blvd.	9.19	4,693	511	13,300	478	40	0.7	2.3
47 East 9th-Whittier	7.55	1,700	225	5,700	284	30	0.7	2.1
49 Figueroa	7.22	1,136	157	3,400	210	30	0.5	1.8
Cross-town lines								
41 Alvarado	3.36	2,752	819	7,300	292	30	0.8	1.6
50 Florence-Soto	13.94	7,517	539	21,600	377	30	1.9	2.0
54 Manchester	11.57	1,125	97	3,600	126	30	1.0	1.0
Intermediate lines								
55 Maywood-Bell	7.55	1,747	231	5,200	223	30	0.8	2.0
64 Highland Park	5.97	1,414	254	4,600	195	30	0.8	1.2

L—Average loading of vehicle, that is, the ratio of the average number of passengers per trip to the vehicle seating capacity, in decimals.

m—Ratio (in decimal) of the cars per hour on the peak to the average or base cars per hour.

This diversity in the condition of present equipment and its associated variables makes it practically impossible to offer a general solution to the question of when will it be more economical to modernize a system or to keep the present equipment in service. Each specific case must of necessity be an individual problem. For this reason, attention is confined in the paper to the case where the present equipment has little useful life remaining.

However, the methods of analysis outlined in the paper are equally applicable where the present equipment may be in good repair. The decision here usually rests on more precarious ground, however, for the reason that if the new equipment is to show a better return than the old it must produce an increase in riding. This increase in riding must be estimated, which is a matter of personal judgment and hence somewhat uncertain at best.

The paper clearly indicates the value of an increase in patronage to a transit system. It also suggests the importance of fast, frequent service with attractive vehicles in encouraging riding. Mr. Josephs, in his discussion, points out the importance of merchandising transportation. Undoubtedly much more attention will be paid to merchandising of transportation in the immediate future. In this respect, the advantages are with modernization. It is much easier to sell transportation with modern streamlined vehicles than with their predecessors. As pointed out in the paper, in many instances where modernization has taken place the results have been so successful that it would have been possible to "write off" considerable in the value of the present equipment and still show a satisfactory financial return.

The figures from the Los Angeles Railway submitted by Mr. Perryman are both interesting and instructive. In general, the value of *m* will depend to a considerable degree upon community characteristics; for example, a city that is highly industrialized would normally have high peak loads. However, the values quoted are, with a few exceptions, about as one would expect; namely, one or slightly over, for the very light lines, around two or better for the lines of intermediate traffic, while on the heavy lines the value of *m* is again lower, approaching an approximate average of 1.7.

The value of *L*, as would be expected, varies over a considerable range being generally low on the light short lines and much higher on the long heavy ones where there would naturally be much local riding.

The figures quoted are particularly interesting in that they illustrate the wide variation in conditions that may obtain on the different routes of a single system, especially if it be a large one. They also indicate the possible danger, as suggested by Mr. Woods, of drawing too general conclusions from specific examples. They further point the necessity for individual study of each line if a complete picture is to be obtained.

Power Supply for Resistance-Welding Machines

Foreword

OWING to the tremendous increase in the use of the resistance-welding process during the past several years, and the magnitude of many of the more recent installations, the problem of power supply for these operations has become one of primary importance to all concerned. Resistance-welding machines present an electrical load of intermittent character, high magnitude, and extremely poor load factor, and because of these poor inherent characteristics, the providing of adequate electric service, both from the standpoint of plant electrical distribution and the power system supply, involves special considerations and methods.

The AIEE subcommittee on power supply for welding operations with members representing manufacturers, users, power companies, and the American Welding Society, has been actively studying the various phases of the problem with the fundamental aim in view of bringing about a closer relationship and better spirit of co-operation between these groups in order that each will better understand the problem from the other's viewpoint.

From the viewpoint of the power companies and users, it has been difficult in the past to obtain adequate and reliable engineering data on proposed welder installations prior to their actual installation and operation and, in addition, some of the machines had exceedingly poor electrical characteristics. From the viewpoint of the manufacturers and users, there have been instances where they have been unable to get proper service connections from the power company for handling welder loads. Such refusals to provide service were, no doubt, justified in some cases but others may have been due simply to a lack of interest in the load by the power company and a concern that the load might be disturbing to other customers. Sometimes the user has not adequately prepared for the welder load by the installation of proper bus and transformer facilities with the result that welds are poor and results inconsistent. It is certain that many of these cases of poor co-operation and unsatisfactory installations have been due to a lack of knowledge of all the pertinent facts.

The subcommittee has been working for some time on a series of three reports covering the various phases of the general problem of electric service for resistance welders; two of these are presented herewith:

I. Guide to good electrical performance of resistance-welding machines

II. Resistance-welder installations

Under preparation is:

III. Factory wiring for large resistance welders

It is the hope of the subcommittee that these reports will assist, at least in some small measure, power engineers, industrial plant engineers, welding machine engineers, and any others actively interested in resistance welding in the planning of layouts for welder installations and also in the purchase of new equipment.

I. Guide to Good Electrical Performance of Resistance-Welding Machines*

Purpose of Report

Resistance welding consists of clamping two or more pieces of metal between electrodes and passing electric current through them for the purpose of joining the pieces by fusion of the metal. The adequacy of supply and proper control of this electric current are determining factors in the production of consistently good welds. Relatively high values of current are required at the weld, and the electrical characteristics of the welding machine determine the amount of current which must be drawn from the electric supply lines when delivering these high values of welding current. An efficient machine of good electrical characteristics and low kilovolt-ampere demand requires a much smaller plant investment for transformers, feeders, etc., than the machine with poor electrical characteristics and resulting high kilovolt-ampere demand, although both may deliver the same welding-current output.

This report shows what is meant by good electrical performance and outlines factors upon which good electrical characteristics are dependent. A better understanding of what constitutes such good characteristics will bring about a better appreciation of them by the users of the equipment. Concurrently, as the art advances and the demand for improved designs increases, the manufacturers will meet these demands with continued improvements in their machines. There are several recent developments that eventually may result in radical reductions in demands of resistance welders and a resultant easing of the power-supply problem, but until such machines are commercially and economically available, a thorough understanding of the electrical characteristics of the present-day conventional machines is necessary if they are to be economically served.

It is intended that the report shall assist the purchaser in the preparation of specifications for the purchase of welding machines of the best electrical design, thereby minimizing the problem of power supply. Among the more common types of machines to which it applies are spot, projection, butt, flash, seam, and portable welders.

The report has been made very general in nature and to all who are closely associated with the electrical industry, it may appear that too many elementary factors are discussed, but to production and industrial engineers who represent perhaps the largest single group interested in welding as a production tool, the report should fill a long-felt need in acquainting them with the electrical problems associated with resistance welding.

Scope of Report

The report covers the following subjects:

1. Electrical characteristics of welding machine:
   - (*a*). Kilovolt-ampere demand
   - (*b*). Kilowatt demand
   - (*c*). Power factor
   - (*d*). Heating
   - (*e*). Welder primary voltage
2. Power supply and its effect upon welder performance
3. Secondary circuit of welding machine
4. Welder transformer
5. Control equipment
6. Name-plate data for welding machine

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* **Paper 40-58.**

Electrical Characteristics of Welding Machines

The electric current required at the weld is supplied by means of a welding transformer which reduces the supply or plant voltage to a relatively low value (usually less than 20 volts). By this method the current from the supply lines is stepped up to the high value required for welding. The magnitude of actual welding current required depends upon many interrelated factors, such as the type of weld, material being welded, the geometry of the pieces, applied pressure, time of current flow, and perhaps others, all of which are beyond the scope of this report.

The current drawn from the supply lines, while also dependent upon these same factors, is in addition greatly affected by the design and physical arrangement of the welding machine itself. The ratio of the welding or secondary current to the supply or primary current is the best relative measure of the desirable electrical characteristics of two machines of the same secondary current output and the same primary voltage rating. The higher this ratio, the more desirable will be that particular machine, other factors being equal.

KILOVOLT-AMPERE DEMAND

The kilovolt-ampere demand is the primary or supply current multiplied by the rated primary voltage and is also approximately equal to the secondary current multiplied by the open-circuit secondary voltage. (The kilovolt-ampere demand as used here in referring to the machine is the instantaneous demand as measured by an oscillograph and should not be confused with the integrated demand of the power meter used for billing purposes.) It bears no relation to the kilovolt-ampere rating of the welding transformer except that it will usually be somewhere between one to about six times as great.

The kilovolt-ampere demand is a direct measure of the desirable electrical characteristics of the machine design—the greater the kilovolt-amperes, the poorer the machine for equal outputs of secondary current.

The kilovolt-amperes taken by a given welder will be a maximum when the electrodes are short-circuited and when the voltage taps are set to give the maximum secondary voltage. When the machine is actually welding, the kilovolt-amperes will always be less than the short-circuit maximum because the impedance of the material being welded causes a reduction in secondary current. If the material being welded is nonmagnetic and of low resistance, such as copper or aluminum, the short-circuit condition is rather closely approximated.

KILOWATT DEMAND

The kilowatt demand of a welder reveals very little regarding the machine characteristics. (The kilowatt demand as used here in referring to the machine is the instantaneous demand as measured by an oscillograph and should not be confused with the integrated demand of the power meter used for billing purposes.) It represents the energy component of the kilovolt-ampere demand.

POWER FACTOR

The power factor is the ratio of the resistance to the impedance of the welding machine and is also equal to the kilowatt demand divided by the kilovolt-ampere demand. In general, the power factor of a welder should be as high as possible commensurate with satisfactory welding performance. Usually the more efficient and better designed machines will have higher power factors accompanying lower machine impedances and lower kilovolt-ampere demands.

However, high power factor in itself is not necessarily a guarantee of an efficient machine. With the welding electrodes short-circuited, the impedance of the machine measured at the primary terminals of the welding transformer should be as low as possible. This impedance is composed of resistance and reactance and can be reduced by reducing either or both of these quantities. If both are reduced in the same proportion, the power factor will remain unchanged. If the reactance is reduced and the resistance is kept the same, the over-all impedance will be reduced and the power factor increased. On the other hand, if the reactance is not changed and the resistance is increased, the power factor will be increased, but also the over-all impedance will be increased and the efficiency reduced which is highly undesirable. It is, therefore, insufficient to state merely that the machine power factor should be high, because the high power factor might be obtained by the latter method mentioned above. High power factor is advantageous only when accompanied by low over-all impedance and obtained by reduced reactance rather than increased resistance.

The approximate power factor of a machine must be known when calculating the voltage regulation of the electrical supply system due to the welder operation. However, an accurate knowledge of the maximum kilovolt-amperes drawn by the welder is of much more importance than an accurate knowledge of the power factor ratio and, in general, results obtained when using estimated power factors are reasonably satisfactory.

Tests of many flash, butt, spot, and projection welders show that the usual range of power factors for the larger machines (50-kilovolt-ampere rating and up) is between 20 and 40 per cent, and that for estimating purposes the use of 30 per cent power factor, coupled with the known value of kilovolt-ampere demand, will prove reasonably accurate. Similarly, 40 per cent power factor can be used for machines of 10- to 50-kva rating, and 60 per cent power factor for machines under 10 kva in size.

HEATING

The heating of the machine is another important consideration due to the fact that the insulation life of the welding transformer is a function of operating temperature. The manufacturer shall provide ample cooling of transformer and secondary loop to prevent overheating at the rated current and duty cycle of the machine. (The duty cycle is the per cent of time that current flows, averaged over a definite period. The length of the period depends upon the class of equipment and is, for example, one minute for electronic control devices.) A duty cycle of 50 per cent has been adopted as standard for rating purposes and the transformer kva rating is an indication of the allowable heating load for which the machine is designed.

WELDER PRIMARY VOLTAGE

If possible, the larger welding machines should be supplied from 440 or higher voltage sources due to the fact that the primary current demand is inversely proportional to the primary voltage, everything else being equal. The use of higher voltages assists in keeping voltage drop or regulation at a minimum.

Power-Supply Requirements and Welder Performance

Allowable voltage drop rather than allowable heating usually becomes the limiting factor in determining supply conductors and transformers because of the extremely low operating duty cycles of most welding operations. The supply to the welders should be such that the maximum voltage fluctuation or dip, measured at the last point on the welder bus or distribution wiring which is common to two or more machines, does not exceed 10 to 15 per cent. (This value of 10 to 15 per cent applies to the welding circuit and is not to be confused with the usual limitation of one to two per cent for lighting circuits affected by the welding load.) It is highly desirable to provide a system which will keep the voltage fluctuation within the ten per cent limit if consistently good welds without an excessive number of rejects are to be expected. In extreme cases the 15 per cent limit might prove satisfactory, and there are conversely some few applications in which the ten per cent limit might be excessive.

The nominal base voltage may vary gradually from time to time throughout the day due to general plant load changes and other factors, and no attempt is made here to specify an allowable range for this gradual shifting of the base voltage. Obviously, if the gradual change is appreciable from, say the day operating shift to the night operating shift, it might become necessary during such periods to change the heat or tap setting on all welding machines to compensate for the changed voltage conditions.

This report does not concern itself with the methods of providing service for welders, except to show the importance of keeping the kilovolt-ampere swings or instantaneous demands at a minimum. A succeeding report entitled "II. Resistance Welder Installations" describes the various commonly used methods of providing service for welders.

Small welder installations are usually served by a power company at either 220 or 440 volts, and the minimum charge for service may be roughly proportional to the magnitude of the maximum kva swing or instantaneous demand and, consequently, it is much to the advantage of the user to purchase a machine with as small a demand as possible.

Welder installations in the large factories, which either have their own power plants or are served by a power company at primary voltages, such as 2,300, 4,800, 6,600, or 22,000 volts, present a similar service problem with the exception that the plant engineer is directly concerned with transformer and distribution bus investment costs needed for the welders.

To show the importance of purchasing a welder of good electrical design, a typical example will be worked out. The welding engineer of a certain plant needs a projection welder with a maximum welding-current output of 100,000 amperes. He gets quotations from different manufacturers on machines incorporating the desired mechanical and control features for handling the particular type of work involved. The final selection eventually is between two competing machines, each having the same mechanical features and same method of control, but with the following differences in electrical rating:

	Machine A	Machine B
Rated kilovolt-amperes of transformer (50 per cent duty cycle)	300	500
Maximum open-circuit secondary voltage—volts	12	20
Maximum secondary current (maximum secondary voltage and minimum throat spacing)—amperes	100,000	100,000
Rated primary welder voltage—volts	440	440

Welder *B* was slightly more expensive than welder *A* but the fact that it had a larger transformer at first glance indicated it to be the best machine. However, a true analysis of the cost of serving the two welders shows that welder *A* is the logical selection. The basic consideration as far as welding ability is concerned, is the secondary current output and the ability of the welder to operate without overheating at a certain prescribed rate of production or duty cycle. Both welders had the same maximum current output and the productive output was guaranteed the same by both manufacturers. The larger transformer simply was needed to supply the excessive losses of machine *B*. The approximate kilovolt-ampere input for machine *A* is:

$$\frac{(100{,}000\times 12)}{1{,}000} = 1{,}200 \text{ kva}$$

and for machine *B* is:

$$\frac{(100{,}000\times 20)}{1{,}000} = 2{,}000 \text{ kva}$$

Assuming that a 1,200-kva step-down power transformer is needed to serve welder *A*, it would be necessary to go to a 2,000-kva transformer for machine *B* for the same voltage regulation. At $3 per kilovolt-ampere, this amounts to $2,400 added investment for machine *B* in transformer capacity alone. Conductor costs for bus and distribution are correspondingly greater for the machine with the higher kilovolt-ampere input. The plant engineer who is responsible for costs of service for the welding operations will certainly select welder *A* as the most economical and desirable machine. The welding engineer who is interested primarily in machine output and production, is equally satisfied as both machines are equal in this respect.

In addition, there is always an upper limit of allowable kilovolt-ampere demand, the exact magnitude depending either upon the factory's location with respect to the power company's substations and power lines or the size of its own generating plant.

In case of power company service, the point of connection to the power company's system determines the allowable size of welder that can be handled. The sudden flow of welder current through the system impedance causes a momentary voltage fluctuation which, if of sufficient magnitude, results in lamp flicker for all lighting customers at that and nearby locations. The farther the point of service is from the main generating plants and larger substations, the greater the system impedance and resultant voltage fluctuation for a given size welder. Voltage fluctuations must be kept within accepted limits for lighting customers. These limits vary, depending upon the frequency of the fluctuation and, in some cases, if the frequency is high, such as with certain types of seam welders, the allowable voltage fluctuation may be as low as one-half volt at the lamp, and for ordinary frequencies experienced with spot, projection, and flash welders may be as high as one and one-half to two volts.

Whenever the kilovolt-ampere demand of the welder is great enough to cause lamp flicker at other nearby customers, there are only two fundamental changes which can be made to eliminate the flicker. The system impedance beyond the point of service common to the lighting and welder loads can be reduced or the sudden flow of current through that impedance can be reduced. There are numerous methods for accomplishing either of these changes, such as reducing the system impedance by increasing substation and line capacity, tapping the welder load direct to the transmission system, separating the lighting and welder loads and serving them from separate substation bus sections, or reducing the amount of current by the use of series capacitors, synchronous condensers, phase balancers, or motor generator sets. Most of these methods involve expense of some considerable magnitude which must be passed on to the user of the welder. Obviously, if the purchaser of the welder can select a machine of more efficient design with a kilovolt-ampere demand within the allowed limit for that particular location, such expensive power-system changes or the addition of corrective equipment would not be needed. It is, consequently, much to the advantage of the prospective purchaser of the welder to obtain in advance of the purchase the allowable maximum kilovolt-ampere demand that the power company can permit at that location and then, if at all possible, select a machine that will produce the desired welding current without exceeding this allowable kilovolt-ampere input.

Welders of large size can usually be served readily in large industrial areas which generally have concentrations of power quite separate from any lighting services. The main business sections of large cities do not usually have distribution facilities suitable for welders and generally the cost of providing service at such locations is prohibitive even for relatively small machines. Suburban areas and small towns are also very likely to be unsatisfactory and expensive locations except for the smaller machines.

Secondary Circuit of Welding Machine

The single, most important factor in keeping the kilovolt-ampere demand low is the design of the secondary circuit of the welding machine regardless of whether it is a spot, seam, or other type of welder.

In order better to understand why this is so, it is only necessary to consider from the fundamental angle what is required to make a given weld. As a simple illustration, suppose a welding machine is required to spot weld two pieces of 16-gauge SAE 1010 cold-rolled steel. From existing engineering records the design engineer would possibly conclude that to obtain the desired quality weld, it would be necessary to limit the time to eight cycles and to pass 12,000 amperes through the parts to be welded.

The next point to be considered is the geometry of the parts to be welded since they, in turn, will dictate the minimum clearance in the secondary circuit of the welding machine which will permit properly placing the parts to be welded between welder electrodes. In this case, perhaps a 12-inch throat depth and 7-inch clearance between horns would readily admit the work to be welded. The next step is to determine the voltage necessary to cause the required 12,000 amperes to flow through this 12-inch by 7-inch secondary circuit with the work in position. This voltage might be found to be four volts which would immediately establish the kilovolt-ampere demand of 48 kva. If, on the other hand, a machine had been selected with a throat depth of 24 inches and a horn clearance of 12 inches because it looked as though it was plenty big enough for the work and would also permit welding a larger job at some future date, a voltage as high as ten volts might have been required to give the 12,000 amperes, thereby imposing a demand of 120 kva on the distribution system and penalizing everyone concerned forever after.

From this it may be concluded that the first rule in obtaining a machine with a minimum demand for a given welding operation is to pick out a machine with a clearance that is just large enough for the work to be done, fully recognizing the fact that if any larger clearance exists, the demand is going to be larger than it need be. There are, of course, cases where the user definitely has need for a larger throat for a certain portion of his work and he must then either put up with a larger than necessary demand on smaller parts or perhaps purchase two machines.

Having determined the throat clearance which is to be furnished in the machine, there are other factors which influence the demand, such as the actual design of the machine having this throat depth. These matters are entirely in the hands of the welding-machine manufacturers and it may be said that they, as a group today, are doing their utmost to reduce the impedance of their welding machine circuits in order to reduce to a minimum possible voltage disturbances in plant-distribution and power-system lines.

The prospective purchaser of welding machines can be assured of the merit of the electrical design of the machine being purchased, by comparing the secondary current outputs and the kilovolt-ampere inputs of the machines being considered. For the same secondary current output the machine having the lowest kilovolt-ampere input will be the lowest impedance machine and, consequently, the most desirable from this angle. The purchaser can also observe the physical geometry of the welding machine parts and select a machine in which the transformer is placed as close to the useful throat of the machine as possible and in which the flexible connections are so arranged as not to introduce excessive reactance.

Portable Welders

In the case of portable welding machines, the same fundamental principles apply. There is here, however, another factor to be considered and that is the type of cable used between the transformer and the gun, assuming, of course, the same principles regarding clearances have been applied to the gun as were mentioned above in regard to the throat of the welding machine. Here it is important that (1) cables no longer than necessary are selected, (2) that in the case of parallel conductors, cables be strapped closely together at all times to reduce the reactance, and (3) that full consideration be given to single, low reactance, interleaved, and concentric designs of cable which are now on the market and which substantially reduce the demand during weld.

Welder Transformer

The transformer incorporated as part of the complete welding machine should be built in accordance with the latest revision of the AIEE Standards No. 39 as soon as this revision can be accomplished. In the meantime the latest Resistance Welder Manufacturers Association Standard, which is a carefully drawn one, can be relied upon. The transformer name-plate rating is based upon a 50 per cent duty cycle and indicates the kilovolt-ampere load it can safely carry at 50 per cent duty cycle. It in no way indicates either useful output of the machine in welding current, or the maximum kilovolt-ampere input to the machine from the supply lines. The equivalent continuous ratings corresponding with loads at various duty cycles are:

70.7 per cent of the load at 50 per cent duty cycle

31.6 per cent of the load at 10 per cent duty cycle

22.4 per cent of the load at 5 per cent duty cycle

Control Equipment

Proper control of a welding machine is essential if consistently good results are to be obtained. Many pages would be required to cover adequately and completely all the various phases of welding-machine control. It is the intention here to simply outline in as concise a manner as possible the fundamental concepts of control requirements, the various types of control equipment, and the field of application for each.

Fundamental Requirements

The energy required to produce a weld is directly proportional to the current squared, the resistance of the weld area between the electrode points, and the time during which the welding current is allowed to flow. To obtain consistent results, each of these three factors must be controlled as carefully as possible.

The welding current is usually regulated by changing the position of a tap switch on the primary side of the welding transformer or by delaying the ignition of electronic interrupters, thereby reducing the effective voltage applied to the transformer primary. In any case the transformer secondary voltage is raised or lowered to bring about the required change in current.

Contact resistance varies inversely with the pressure applied to the work. On manually-operated or motor-driven machines a limit switch can be set to close at a point equal to satisfactory welding pressure. Operation of the limit switch would initiate the flow of current. The pressure switch, actuated by means of the back pressure built up in the operating cylinder, is usually substituted for the limit switch on air-operated machines. However, an adjustable time delay introduced between the operation of the air valve and the application of power to the work, would accomplish the desired result.

Timing of the interval during which current is allowed to flow is a more difficult problem. Many types of equipment are available for this purpose. In general, the complete installation consists of an interrupter for the power line and some means for predetermining the period during which it is closed.

TYPES OF EQUIPMENT

The interrupter may be electronic, motor-driven, air operated, or magnetically operated. To apply recently adopted standard rating curves in the selection of interrupting equipment, it is essential that these factors be known:

1. Transformer maximum primary amperes
2. Required number of welds per unit of time
3. Estimated length of each weld

Control devices are classified as two different types—synchronous and nonsynchronous. The first is generally used where a premium is placed on absolute accuracy. High initial cost and relative complexity discourage universal use.

Nonsynchronous control involves an inherent possible error amounting to at least a plus or minus one-half cycle and possibly more, depending upon the nature of the equipment. In addition, it does not guard against the danger of variable transient current surges introduced by haphazard circuit closing present in nonsynchronous control. These errors become objectionable when applied to short timing periods and when the nature of the work is such that precision control is necessary. The fabrication of extremely small parts and the welding of stainless steel or nonferrous metals are usually within this classification.

A design classification for timers would include mechanical, electromagnetic, electrostatic, or electronic types. Final selection of a specific device should be based on the accuracy required, initial cost, maintenance cost, accessibility, range of adjustment, reliability, and simplicity.

Either the "fixed" or "current responsive" principle of current timing may be used. "Fixed timing" is satisfactory for most applications and results in less complicated equipment. Some few types of work require automatic compensation for variations in work conditions and "current responsive" systems are available for these. The machine supplier or control manufacturer will be acquainted with these requirements.

When air-operated machines are equipped with solenoid-operated valves, a fully automatic timing device performing the following functions can be used:

1. Automatic application of pressure to the work
(Squeeze Period)
2. Initiation of power flow at time interval later
(Weld Period)
3. Termination of power flow at the conclusion of the weld
(Hold Period)
4. Removal of pressure after the weld has cooled
(Off Period)
5. Reapplication of pressure after the work has been removed and new parts placed between electrodes

A low-voltage (110 volts or lower) push-button circuit is recommended to minimize danger of electrical shock.

If the voltage regulation in the welder supply lines is excessive, a separate supply line for the timer control circuit should be installed. A 110-volt lighting supply circuit is usually available for this purpose. The installation of a fusible control circuit disconnect switch separate from the power switch is considered an operational advantage although separate power and control sources are not always required.

Name-Plate Data for Complete Welding Machine

Power and industrial engineers have been handicapped in the past when called upon to plan and provide service for proposed welder installations because of a dearth of reliable and accurate information regarding name-plate and specification data for the proposed machines, particularly information about maximum kilovolt-ampere demand or load. To improve this situation the following name-plate data should appear on the name plate of the complete welding machine:

1. Manufacturer's name and address
2. Manufacturer's type and designation number
3. Rated kilovolt-amperes of transformer (50 per cent duty cycle)
4. Maximum open-circuit secondary voltage
5. Minimum open-circuit secondary voltage
6. Maximum secondary current (with maximum secondary voltage and minimum throat spacing)
7. Rated primary voltage and frequency
8. Manufacturer's serial number

From the above essential name-plate data, the following additional information can be readily calculated:

(*a*). Transformer turn ratio [rated primary voltage (7) divided by maximum open-circuit secondary voltage (4)]

(*b*). Maximum kilovolt-ampere input at rated voltage (neglecting exciting current) [Maximum secondary current (6) multiplied by maximum open-circuit secondary voltage (4)]

(*c*). Maximum primary-current demand (neglecting exciting current) [Maximum secondary current (6) divided by transformer turn ratio (*a*)]

II. Resistance-Welder Installations*

Purpose of Report

Factory wiring and electrical distribution for resistance-welding loads must be designed on an entirely different basis than that for the ordinary power and lighting loads encountered in industrial plants. To provide a sound basis upon which recommended methods of wiring, types of bus construction, and transformer arrangements can be established the AIEE subcommittee on power

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* Paper 40-57.

supply for welding operations has obtained descriptive reports of a number of commercial resistance-welder installations, in plants throughout the country. These reports show the different methods successfully employed in existing installations, and give a clear picture of some of the problems involved. It is hoped that they will serve other users of resistance-welding equipment as a basis upon which new installations can be made.

Design Principles

In reviewing these reports, it should be remembered that the majority of resistance-welding machines have instantaneous kilovolt-ampere demands several times their normal rating, and that in order to prevent excessive voltage drop at this equipment, as well as at any nearby equipment, the supply and distribution facilities must be designed on the basis of the peak kilovolt-ampere demand rather than the normal rating of the machine. From a standpoint of voltage regulation the supply and distribution system must be such as would normally be employed if this peak kilovolt-ampere demand were drawn continuously. However, due to the low operating duty cycle of this load, its heating effect on the system is usually extremely small.

Since the capacity of all power equipment and distribution systems is based on heating or temperature-rise limitations it is seen, therefore, that in order to serve one resistance-welding machine, the normal capacity of the system must be much higher than necessary from a heating standpoint in order to satisfy the conditions of voltage regulation. In fact, in designing a system to supply two or three or even ten welding machines operating on the usual duty cycle encountered, it is safe to neglect the consideration of heating capacity required and design the system to satisfy the consideration of voltage drop. It is only when a considerable number of machines are involved on a single system that the heating becomes a consideration of any importance, or when the duty cycle of a single welder exceeds about 25 per cent, such as with most seam welders.

Summary

In the following representative welder installations, several types of feeder distribution systems are represented. Two of the companies employ a bus system to which the individual welders can be readily and quickly connected or disconnected. Tap points are available at regular intervals and the welding machines can be moved around from season to season with very little expense. The same is true, to a lesser degree, of the wire or cable feeders used by the other companies. This type of feeder does not lend itself quite so readily to frequent changes in plant layout, but is generally cheaper and easier to install.

The bus system employing the concentric arrangement of copper tubes will be found to have the lowest voltage drop, and where this factor is of major consideration this type of feeder is of highest efficiency. In installations where the equivalent thermal loading is high, this type of construction may be unsatisfactory since the heat radiation or conduction from the inside conductor is comparatively low. However, should it become necessary, cooling air can be forced through the inner conductor thus raising its load capacity. The system of interlaced copper bus bars, usually has a high load capacity, but gives a slightly higher voltage drop than the concentric construction.

The system of cable construction is of course the simplest, since the problems of insulation are minimized. In general this type of construction gives a higher voltage drop and lower load capacity than either of the above types. However, if the various conductors are interlaced and spaced very close together the voltage drop can be reduced to a value comparable with the bus-type construction. In so doing the load capacity is reduced due to the reduced radiating surface, and this must be compensated for by a sufficient number of parallel lines to reduce the thermal loading to the safe point.

Table I shows the ratio of total connected welder load to total supply-transformer capacity for the various installations, the ratio of largest single welder to total supply-transformer capacity, and the ratio of resultant thermal load to total connected load. The first ratio in some cases may be determined by the thermal loading of the system and thus be dependent upon the average operating duty cycle of the units comprising the installations. If the average operating duty cycle is high, the thermal loading will be high and this ratio must be low. The ratio of largest single unit to supply-transformer capacity is usually determined by the requirements of over-all voltage drop. If a large part of the permissible voltage drop is consumed in the feeder line, the allowable drop in the supply transformer is small, and this ratio must be small. In most commercial installations it is this consideration of voltage drop that determines the supply-transformer capacity, rather than the consideration of thermal loading.

It will be noted that the ratio of total connected load to total supply-transformer capacity varies from 0.4 to 8.74, but the weighted average is about 2.0. This wide variation may be partly explained by the fact that the method of rating resistance-welding equipment has, in the past, been rather vague, and the values of connected load taken from the name-plate ratings may be

Table I. Summary

Company	Feeder	Phases	Power-Transformer Capacity	Connected Load	Type of Service	Ratio Total Connected Load to Total Supply-Transformer Capacity	Ratio Largest Single Welder to Total Supply-Transformer Capacity	Ratio of Thermal Load to Total Connected Load
A		Three	3,000	4,500	Flash	1.5	0.25	0.118
B	*A*	Single	1,000	2,000	Projection, flash, and seam	2.00	0.50	0.10 for all feeders combined
B	*B*	Single	2,000	1,700	Projection and butt	0.85	0.375	0.10 for all feeders combined
B	Top	Single	2,000	1,960	Flash and projection	0.98	0.3	0.10 for all feeders combined
B	Center	Single	2,000	2,750	Flash and projection	1.375	0.3	0.10 for all feeders combined
B	Bottom	Single	2,000	1,250	Projection and flash	0.625	0.3	0.10 for all feeders combined
C		Single	1,500	13,119	Miscellaneous	8.74	0.3	0.048
D		Three	1,000	1,500	Seam	1.5	1.5	0.67
D		Single	1,260	500	Projection	0.4	0.4	0.44
D		Single	840	750	Spot	0.89	0.89	
E		Single	666	1,565	Spot and seam	2.35	0.60	
F		Three	1,500	2,400	Miscellaneous	1.6	0.267	0.177

quite at variance with the now commonly accepted practice of rating equipment on the basis of a 50 per cent duty cycle. For example under company *C*, section 6, we see that a normal current of 300 amperes applies to four different sizes of equipment, namely 50, 75, 100, and 150 kva.

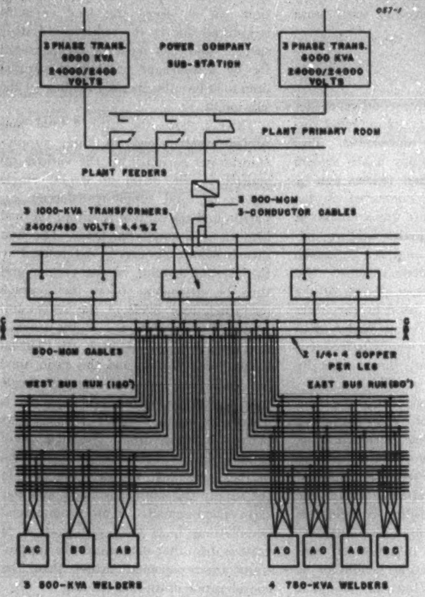
057-1
3 PHASE TRANS. 6000 KVA 24000/2400 VOLTS
POWER COMPANY SUB-STATION
3 PHASE TRANS. 6000 KVA 24000/24000 VOLTS
PLANT PRIMARY ROOM
PLANT FEEDERS
3 500-MCM 3-CONDUCTOR CABLES
3 1000-KVA TRANSFORMERS 2400/480 VOLTS 4.4% Z
500-MCM CABLES
2 1/4×4 COPPER PER LEG
WEST BUS RUN (180')
EAST BUS RUN (80')
AC
BC
AB
AC
AC
AB
BC
3 500-KVA WELDERS
4 750-KVA WELDERS

Figure 1. Schematic diagram of welder supply system—company A

The minimum value of 0.4 applies to a projection welder operating from a special supply transformer, and supplied by means of a concentric-cable feeder. Obviously the major consideration in this case is that of voltage drop.

The maximum value of 8.74 applies to an installation having a relatively large number of units. In such an installation the probability of a large number of units operating simultaneously is rather remote, so that from a standpoint of voltage drop, the required capacity of the supply-transformer bank is determined by the size of the largest single unit. In this instance the ratio of largest single unit to supply-transformer capacity is 0.4.

Another factor which makes for low supply-transformer capacity, in this case, is the use of the low-impedance concentric tubular bus. This type of bus gives a comparatively low voltage drop, so that a higher impedance (lower capacity) supply-transformer bank can be tolerated. The chief reason for the ability of company *C* to serve 13,119 kva of welding load with only 1,500 kva of supply-transformer capacity, is their use of a good concentric bus design with extremely low impedance. This permits serving an exceptionally large factory area by the single-bus system without obtaining excessive voltage drop at the welding equipment. Other types of bus construction and cable assemblies cannot serve as large an area, nor as many units, without excessive voltage drop. The result is that where such construction is used there must be more feeders operating in parallel or a higher supply-transformer capacity.

Where a large number of smaller units are to be served, the thermal loading of the supply transformers and feeder lines may become the determining factor. This determination, however, depends upon a knowledge of the operating duty cycles of the various equipments. In this particular case the average operating duty cycle is comparatively low, the ratio of average kilovolt-ampere load to connected kilovolt-ampere load being 4.8 per cent. The ratio of average kilovolt-ampere load to supply-transformer capacity is approximately 0.4, the extra capacity being necessary to limit the voltage regulation.

The ratio of largest single unit to supply-transformer capacity varies from 0.3 to 1.5, with a weighted average of about 0.5. The maximum value of 1.5 applies to a seam-welder installation in which the thermal loading of the system is the determining factor in selecting supply-transformer capacity.

The ratio of thermal load to total connected welder load for these commercial installations of all types except seam welders is between 0.048 and 0.177, and for seam welders, between 0.44 and 0.67.

It must be remembered that it is impossible to give any definite empirical values for any of these ratios, since the proper values depend upon individual conditions. The values given in table I do, however, give a general idea as to the practice followed in some of the industry's successfully operating installations.

It is interesting to note that in all of these installations overload protection of the feeders and transformers is confined to instantaneous overcurrent devices, designed to protect against short circuits. In one installation this instantaneous tripping is reduced well below the short-circuit current to provide a measure of protection against high overloads on individual welders. The protection thus afforded against individual machine overloads is very small. In so far as we are informed, the only practical overload protection is that afforded in the connections to each individual machine. This may take the form of instantaneous overcurrent trips, overtime trips, or a combination of these two, or some thermal relay or fuse with thermal characteristics similar to the equipment to be protected.

For the protection of supply transformers, the instantaneous overcurrent devices set for short-circuit currents have proved satisfactory. In installations where the thermal loading is high, contact-making thermometers immersed in the cooling oil have been employed to trip the circuit breakers when the oil temperature becomes excessive.

Another point of interest is that the power supply for the resistance-welding loads is usually separated from the general plant power supply. Generally speaking the voltage disturbances produced by the resistance-welding load cannot be tolerated on other plant loads, particularly lighting. For this reason an attempt is made to minimize these disturbances by separating the two loads as much as possible.

Installations—Company A

1. METHOD OF SERVICE BY THE POWER COMPANY

The power company provides service for all plant load, including the resistance welders, through a step-down substation consisting of two 6,000-kva three-phase transformers connected 24,000/-2,400 volts, located on the plant property as near as practicable to the load. Each transformer is radially fed by means of an underground cable from a 24,000-volt substation located about one-quarter mile from the plant. The transformer secondaries are solidly connected to the 2,400-volt bus. The 2,400-volt plant feeders are connected to the bus through oil circuit breakers and double-throw disconnecting switches, as shown in figure 1.

2. COST TO THE POWER COMPANY TO SERVE WELDER LOAD

No special consideration or expense was required by the power company to serve the welder load. The step-down substation was needed to supply the load at the voltage required, and the 24,000-volt supply to the substation was of ample capacity to take the kilovolt-ampere swings of the welder without producing troublesome flickers.

No other customers are fed from this step-down substation and so the power company has not been concerned by the flicker on the 2,400-volt bus. If, in the future, some new load develops in this area that could advantageously be fed from this substation, it might be necessary to set aside one substation transformer for welders and other flicker-producing load and use it also as the "throwover."

3. PLANT DISTRIBUTION FOR WELDERS

The welder power transformers which supply the welder bus are three 1,000-kva single-phase 2,400/480-volt 4.4 per cent impedance, connected delta-delta in a three-phase bank. They are fed from the primary room by three 250-foot lengths of 500,000-circular-mil three-conductor cables, operating in parallel. The transformers are located on the roof near the load center of the welders. The 480-volt transformer bus is made up of two one-quarter- by four-inch copper bars per phase. Solidly connected to the transformer bus are 12 three-phase feeders of three single-conductor 500,000-circular-mil cables distributed over the entire bus. These feeders are brought down from the transformer bus on the roof to the welder room in four groups of three three-phase feeders. Each group is supported in wooden cleats and taped together. In the welder room, the four groups of three feeders divide into two groups of six feeders each. One group extends about 80 feet east and the other about 160 feet west from the transformer bus. The three conductors of each three-phase feeder are taped together and supported from messenger cable strung from the building girders above the line of welders. Each welder is connected to the same phase of each of the six three-phase feeders so as to divide the load about equally among the six feeders. The different welders are connected to the three phases so as to have about an equal load division on the three phases.

4. COST DATA ON THE WELDER FEEDER

Low cost was obtained by the use of the same size copper (500,000 circular mil) throughout.

5. WELDER FEEDER PROTECTION

All connections are solid from the oil circuit breakers in the primary room to the welder. The oil circuit breaker is automatic and set for time-delay trip on overload.

6. TYPE AND RATING OF WELDERS

The welders are all flash welders of which four are rated 750 kva and three are rated 500 kva.

7. VOLTAGE DROP

Voltage-drop data given below are calculated values in per cent based on a single-phase load of 800 kva of welding current at 0.4 power factor, 480 volts.

1. At 24,000-volt bus in power company substation....................0.7
2. At 2,400-volt bus in plant switch house..............................2.3
3. At 480-volt transformer bus........4.7
4. At welder, including 65-foot welder bus...................................5.0

The voltage drop for the above four items is accumulative and represents the total drop to each point. To find the drop in any particular part of the system, for example, the drop from the 480-volt transformer bus (item 3) to the welder over 65 feet of bus (item 4) is 5.0 per cent minus 4.7 per cent equals 0.3 per cent.

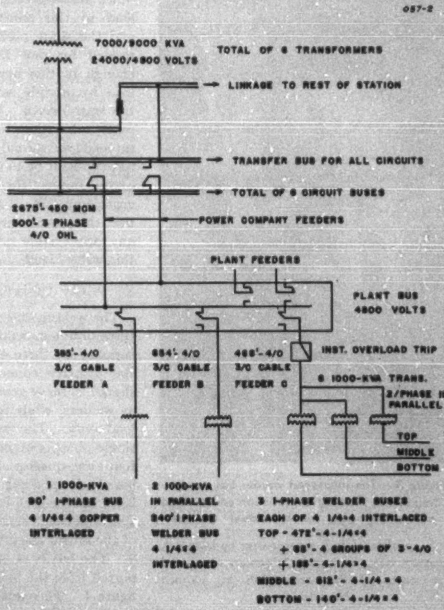
057-2
7000/9000 KVA
24000/4800 VOLTS
TOTAL OF 6 TRANSFORMERS
LINKAGE TO REST OF STATION
TRANSFER BUS FOR ALL CIRCUITS
TOTAL OF 6 CIRCUIT BUSES
2675'- 450 MCM
800'- 3 PHASE
4/0 OHL
POWER COMPANY FEEDERS
PLANT FEEDERS
PLANT BUS
4800 VOLTS
INST. OVERLOAD TRIP
385'- 4/0
3/C CABLE
FEEDER A
654'- 4/0
3/C CABLE
FEEDER B
468'- 4/0
3/C CABLE
FEEDER C
6 1000-KVA TRANS.
2/PHASE IN PARALLEL
TOP
MIDDLE
BOTTOM
1 1000-KVA
90' 1-PHASE BUS
4 1/4×4 COPPER
INTERLACED
2 1000-KVA
IN PARALLEL
240' 1 PHASE
WELDER BUS
4 1/4×4
INTERLACED
3 1-PHASE WELDER BUSES
EACH OF 4 1/4×4 INTERLACED
TOP - 472'- 4-1/4×4
+ 88'- 4 GROUPS OF 3-4/0
+ 188'- 4-1/4×4
MIDDLE - 812'- 4-1/4×4
BOTTOM - 140'- 4-1/4×4

Figure 2. Schematic diagram of welder supply system—company B

8. EQUIVALENT THERMAL LOADING

The heating load on the welder feeder obtained by maximum-demand heater-type ammeters was approximately 128 amperes per phase at 2,400 volts, which was 530 kva. The heating load in per cent of total connected welder kilovolt-amperes is 530/4,500 equals 11.8 per cent. At the time these readings were taken, it was known that only 3,500 kva of welders were actually operating. The heating load in per cent of operating-welder kilovolt-amperes was 530/3,500 equals 15.2 per cent.

Company B Installation

1. METHOD OF SERVICE BY THE POWER COMPANY

This plant is fed by two 4,800-volt power lines out of a 24,000/4,800-volt step-down substation as shown in figure 2. The step-down substation consists of six 7,000/9,000-kva 24,000/4,800-volt three-phase radially fed transformers, serving six 4,800-volt circuit busses. Each transformer is linked to the rest of the substation through reactors and a linkage bus. The circuit busses occupy the lower position of a double bus arrangement with a transfer bus above. The transfer bus is a continuous section energized from the linkage bus.

Figure 3. The interlaced welder bus used by company B, showing construction and method of making taps for individual welders

Bus consists of four one-quarter by four-inch copper bars on one-inch centers, insulated by means of five one-eighth by six-inch Micarta strips

The plant primary room bus is also a double-bus arrangement with a power line feeding each bus. The welder feeders are operated on the "throwover" bus and general plant load on the other bus.

2. COST TO THE POWER COMPANY TO SERVE WELDER LOAD

Originally the power line supplying the welder load was connected to a circuit bus at the substation with only power load. On such a bus the flicker limit is three volts (at lamp voltage) which would permit 1,500-kva load swings. The plant later increased the size and number of welders until load swings exceeded 2,000 kva and caused voltage flickers of four to five volts, and complaints from other customers on this bus.

There were sufficient spare positions at the substation so that it was possible by rearranging and transferring lines to set aside one circuit bus for this welder load. At present this circuit bus carries only the load of this one power line, but the transformer is still connected to the other busses through the linkage. If the load in this substation area increases and the spare positions on this bus are needed for new load, it may be that enough of this new load will be flicker load to provide satisfactory loading for the transformer. This has happened at other substations where a bus has been set aside for special load. Another alternative might be to use the spare positions on this bus as "throwover" lines to other customers. It is not felt in this case that the power company has been put to any appreciable added expense to serve this welder load.

3. PLANT DISTRIBUTION FOR WELDERS

The welding transformers are located at three different parts of the plant and are supplied by three 4,800-volt feeders from the primary room. Feeders *A* and *B* (figure 2) serve general plant load as well as welders, while feeder *C* serves welder load only. The welders are tapped to single-phase 480-volt busses made up of four one-quarter by four-inch copper bars with the going and return bars interlaced on one-inch centers with a Micarta spacer between each bar and on each side of the entire assembly as shown in figure 3. The bus is of open construction supported from the building pillars shown in figure 4. To reduce the chance of short circuits from foreign material falling across the bars, the Micarta spacers extend about one inch above the bars. Data on each of the feeders are given below:

Feeder A

1. 350 feet of three-conductor 4/0 cable from primary house to transformer

2. Supply transformer—one 1,000 kva, single phase, 4,800/480 volt, 4.6 per cent impedance

3. Feeder bus—80 feet four one-quarter by four-inch copper bars interlaced

Feeder B

1. 654 feet of three-conductor 4/0 cable from primary house to transformers

2. Supply transformers—two 1,000 kva, single phase, 4,800/480 volts, 4.6 per cent impedance, two in parallel

3. Feeder bus—240 feet four one-quarter by four-inch copper bars interlaced

Feeder C

1. 468 feet of three-conductor 4/0 cable from primary house to transformers

2. Supply transformers—six 1,000 kva, single phase, 4,800/480 volt, 4.6 per cent impedance (two transformers in parallel on each phase)

3. Feeder bus:

Top bus—472 feet of four one-quarter by four-inch copper bars interlaced plus 85 feet of four interlaced groups of three 4/0 cables, each on about four-inch centers to another bay and there 185 feet of four one-quarter by four-inch copper bars interlaced

Center bus—512 feet of four one-quarter by four-inch copper bars interlaced

Bottom bus—140 feet of four one-quarter by four-inch copper bars interlaced

4. COST DATA ON WELDER FEEDER

The welder bus cost was $10.50 per foot installed.

5. WELDER FEEDER PROTECTION

There is no protection provided on feeders *A* and *B*. The oil circuit breakers in the primary room are nonautomatic. The oil circuit breaker of feeder *C* is automatic and set for instantaneous tripping on overload. The power company's incoming line is also provided with an automatic instantaneous-tripping oil circuit breaker, so it is doubtful if a worthwhile degree of selectivity is obtained between the plant feeder *C* breaker and the power company's breaker.

6. TYPE AND RATING OF WELDERS

The type, number, and rating of the welders on each bus are given below:

Feeder A

Projection, flash, and seam	6
Total connected load (kva)	2,000
Largest single unit (kva)	500

Figure 4. Two separate interlaced welder busses used by company B, showing method of support

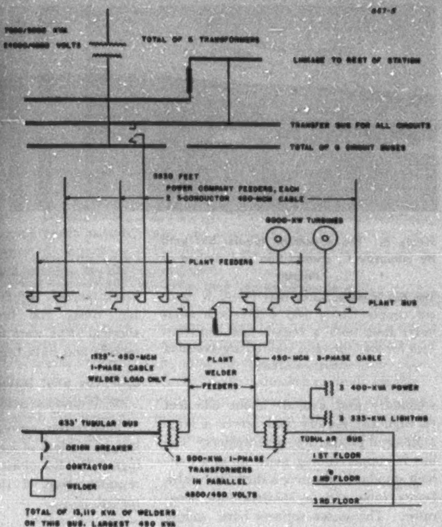
867-5
7000/9000 KVA
24000/4800 VOLTS
TOTAL OF 6 TRANSFORMERS
LINKAGE TO REST OF STATION
TRANSFER BUS FOR ALL CIRCUITS
TOTAL OF 6 CIRCUIT BUSES
5530 FEET
POWER COMPANY FEEDERS, EACH
2 3-CONDUCTOR 450-MCM CABLE
6000-KW TURBINES
PLANT FEEDERS
PLANT BUS
1525'-450-MCM
1-PHASE CABLE
WELDER LOAD ONLY
PLANT
WELDER
FEEDERS
450-MCM 3-PHASE CABLE
3 400-KVA POWER
3 333-KVA LIGHTING
433' TUBULAR BUS
DEION BREAKER
CONTACTOR
WELDER
3 500-KVA 1-PHASE
TRANSFORMERS
IN PARALLEL
4800/480 VOLTS
TUBULAR BUS
1ST FLOOR
2ND FLOOR
3RD FLOOR
TOTAL OF 13,119 KVA OF WELDERS
ON THIS BUS. LARGEST 480 KVA

Figure 5. Schematic diagram of welder supply system—company C

Feeder B

Projection and butt	9
Total connected load (kva)	1,700
Largest single unit (kva)	750

Feeder C

Top Bus	
Projection and flash	11
Total connected load (kva)	1,960
Largest single unit (kva)	600
Center Bus	
Projection and flash	11
Total connected load (kva)	2,750
Largest single unit (kva)	600
Bottom Bus	
Projection and flash	3
Total connected load (kva)	1,250
Largest single unit (kva)	600

Test data have been obtained on two of the largest welders used.

1. 600-kva projection welder; maximum swings, 960 kva, 2,110 amperes, 450 volts at 0.347 power factor at welder (1,000 kva at substation)

2. 750-kva welder; maximum swings, 1,760 kva, 4,300 amperes, 418 volts at 0.299 power factor at welder (2,100 kva at substation)

7. Voltage Drop Caused by Welder Load

The voltage drop in per cent at various points has been calculated and is based on a load of 2,100 kva at 480 volts, 0.4 power factor.

1. At substation, 4,800-volt bus	3.5
2. At plant primary-room 4,800-volt bus	6.4
3. At welding-transformer primary	7.1
4. At welding-transformer secondary (two 1,000 kva in parallel)	11.8
5. At welder over 100 feet of four one-quarter by four-inch bus	13.0

The voltage drop for the above five items is accumulative and represents the total drop to each point. To find the drop in any particular part of the system, for example, the drop from the welder transformer secondary (item 4) to the welder over 100 feet of bus (item 5) is 13.0 per cent minus 11.8 per cent equals 1.2 per cent.

8. Equivalent Thermal Loading

There are no available data on thermal loading on the individual feeders. For the total connected load of 9,660 kva, the average kilovolt-amperes or thermal load is approximately 900 kva, or slightly less than ten per cent.

Company C Installation

1. Power Company Method of Service

The step-down substation from which this plant is fed consists of six radially fed three-phase transformers of 7,000/9,000 kva, 24,000/4,800 volts, feeding six 4,800-volt circuit busses as shown in figure 5. The transformers are linked by reactors on the 4,800-volt side. The circuit busses and transfer bus form a double bus arrangement, with the transfer bus tied to the linkage bus. The short-circuit kilovolt-amperes on each 4,800-volt circuit bus is 147,000 kva.

Each power line to the plant consists of two cables of 450,000 circular mils and supplies a separate bus at the plant primary house. In addition to the power-company supply, there are two 6,000-kw turbines at the plant which operate in parallel with one power-company line.

2. Power Company Cost to Serve Welders

The substation bus supplying this plant can stand 1,500-kva swings without exceeding the flicker limit of three volts on the substation bus. This limit has never been exceeded, and so there has been no additional expense to serve the welder load.

3. The Welder Feeders

There are two welder feeders, but since their construction is nearly identical detailed description will be given for only one of the feeders.

This feeder consists of 1,525 feet of 450,000-circular-mil single-phase cable from the primary house to three 500-kva 4,800/480-volt transformers connected in parallel. The 480-volt welder bus is connected solidly to the transformer

Figure 6. The concentric tubular bus used by company C, construction and method of support

The bus is made up of bare four-inch standard copper tubing over a three-inch extra-heavy tube with a Micarta-tube separator. Taps for the individual welders are provided every six feet

Figure 7. Showing welder bus and controls used by company C

The controls are mounted on racks alongside the bus, and consist of De-ion circuit breaker, welding contactor, and timer for each welder

secondary and extends about 633 feet through the factory and serves a large number of production-type welders. The bus is copper tubing made up of a four-inch standard tube over a three-inch extra heavy tube with a Micarta tube separator. The outer tube is bare, solidly grounded, and supported by the building girders (figure 6). There is a tap provided on the bus every six feet. The De-ion breaker contactor and timer for each welder are located on a rack by the welder bus (figure 7). The lead into each welder is made of special cable with the control wiring laid in the main cable. This cable required no conduit.

4. Welder Bus Cost

The cost of the tubular bus, made up of four-inch standard tubing over three-inch extra-heavy tubing in 12-foot sections with taps every six feet is about $9 per foot. Installation cost is extra and will depend somewhat upon local conditions.

This company uses two-inch standard tubing over one and one-quarter-inch extra-heavy tubing on the branches of the second welder bus, but no figures are available on its cost.

5. Bus or Feeder Protection

Faults on the welder bus or the 4,800-volt supply are cleared by the oil circuit breaker at the primary house. The breaker is tripped after a time delay by an induction-type overcurrent relay and is selective with the main supply breaker from the power company. Each welder tap has a De-ion type air breaker which is a two-pole breaker with the poles in parallel. This gives it a rating of 190 amperes and an interrupting rating of 10,000 amperes. The breaker is set for instantaneous tripping at about eight to ten times the breaker rating. The thermal trips were found to be unsatisfactory and have been disconnected.

6. Type and Rating of Welders Served

This feeder serves a total of 13,119 kva of welders of various types and ratings. The list given in table II is fairly representative of the types and sizes served.

7. Equivalent Thermal Loading

The following readings were obtained during normal production on the feeder which supplies welders only:

Connected kva	13,119
Normal peak kva	860
Average load kva	610
Average load kw	304
Average power factor	304/610 = 49.8 per cent
Average kva/-connected kva	610/13,119 = 4.8 per cent

8. Voltage Drop Caused by Welder Load

Voltage drops in per cent are:

1. At the substation bus, 4,800 volt . . 2.5
2. At the plant primary house, 4,800 volt 3.8
3. At the welder transformer primary, 4,800 volt 4.5
4. At the welder transformer secondary, 480 volt 8.7
5. At the welder over 633 feet of bus . . 11.3

The above figures are calculated for 1,500-kva 0.4-power-factor, 480-volt welder load supplied by one power-company line and do not take into account any assistance obtained from the plant generators.

The voltage drop for the above five items is accumulative and represents the total drop to each point. To find the drop in any particular part of the system, for example, the drop from the welder transformer secondary (item 4) to the welder over 633 feet of bus (item 5) is 11.3 per cent minus 8.7 per cent equals 2.6 per cent.

Company D Installation

There are at present 269 resistance welders installed in this plant, with the following name-plate ratings:

Number of Welders	Rating (Kva)
51	Under 5
70	5 to 25
25	30 to 50
60	60 to 100
26	125 to 175
26	200 to 300
6	350 to 400
1	500
2	600
1	750
1 five head	1,500

Electronic control is used wherever accurate timing and synchronous switching is required. In installations requiring less accurate control, ignitron contactors are now being used in place of the former standard magnetic contactor in order to cut down service costs.

Practically all the welders are at present protected by enclosed fused switches. Where control equipment and switch are located at a distance from the welder, it is customary also to include a line contactor operated from a tumbler switch at the welder. In future, however, enclosed air circuit breakers will be used with all but the smaller units. These breakers will be either manually or electrically operated, depending on their location in respect to the welders, and will have instantaneous overload trips, in some cases with an additional undervoltage trip coil operated by a thermal overload relay incorporated in the welder control.

From the standpoint of power supply, these welders may be divided into three classes, as follows:

Class I

This includes resistance welders of all types, both with and without electronic control, which are installed as separate units and obtain power from the regular 575-volt three-phase 60-cycle shop lines. With the exception of a few spot or projection welders, the name plates of which may be as high as 350 kva, these welders are not rated over 150 kva or, in the case of multiple-head machines, the individual welding heads do not exceed this limit. About 94 per cent of the resistance welders are in this class.

Table II. Type and Rating of Welders—Company C

Type	Kilovolt-Ampere Rating	Normal Current	Maximum Current	Rated Current at Rated Volts
Gun	50	150	550*	114
Gun	75	300	500	170
Hydromatics	100	300	550*	228
Hydromatics	150	300	800*	340
Spot and projection	450	5,000	6,000*	1,000
Spot and projection	50	300	650*	114

* Estimates based on test readings. This company generally figures five to eight times rated kilovolt-amperes for momentary load.

Before connecting a welder to a shop line, consideration is given to the effect of voltage drop, caused by the actual power demand during weld, on other load supplied by that line. Inasmuch as most of the lighting load is on d-c or separate a-c lighting feeders, it is possible to permit fairly large momentary voltage variations on the a-c shop feeders without undesirable results. Figure 8 shows a typical section of the 60-cycle distribution. This distribution system is designed to supply shop load, and neither its cost nor design has been directly affected by welders of this class.

The central substation *A* from which the primary feeders of this system radiate has a capacity of 30,000 kva with a calculated bus short-circuit value of 500,000 kva. Primary power is distributed at 13.8 kv three phase, grounded neutral, over 17 feeders, most of which are rated 4,800 kva and consist of three-conductor 4/0 15-kv cable. Each feeder has a five per cent current-limiting reactor which limits feeder short circuits to approximately 100,000 kva. Relaying of feeder oil circuit breakers is for short-circuit protection only. These feeders serve 24 substations each of which has an emergency tie to at least one other than its normal feeder.

Substations vary from 1,800 to 3,500 kva in transformer capacity, the more recent transformer units being 1,500 kva instead of the 600 kva shown in figure 8. Although the 800-ampere 600-volt manually operated oil circuit breaker has been standard for 575-volt feeder protection, air circuit breakers will be used in any new substation construction. This also applies to shop entrance panels.

Secondary feeders are 575 volt, three phase ungrounded, and consist usually of one three-conductor 500,000-circular-mil cable nominally rated 400 kva. If more capacity is needed a similar cable is added in parallel or a separate feeder is established. Length of feeder between substation and building may be up to 1,000 feet. Inside building feeders usually are of open construction extending the length of the building and may be anywhere from number 0000 to 1,000,000 circular mils in size. It is obvious that the larger welders in this class cannot be fed from the more remote sections of the shop 575-volt lines, and as mentioned above, each case must be considered in the light of the particular line capacity and demand involved.

Class II

This covers group installations of welders where the voltage disturbances set up by the operation of one or more of the welders, if from the shop 575-volt lines, would interfere with the operation of other welders of the same group. In some cases such installations can be supplied from the nearest substation through a special low-impedance line, but in two installations the following methods have been used.

The first group consists of 14 seam welders and one projection welder four of them having two welding transformers each (figure 8). Name-plate rating of the group is 2,524 kva, 550 volts. The 19 electronic controls required, together with their protective equipment, are mounted together on a centrally located overhead platform. Directly below the platform is installed a Pyranol-filled transformer rated 1,000 kva, 13,800/-575 volts. The low-voltage side of this transformer is connected to a short bus directly underneath the platform floor, from which connection is made through

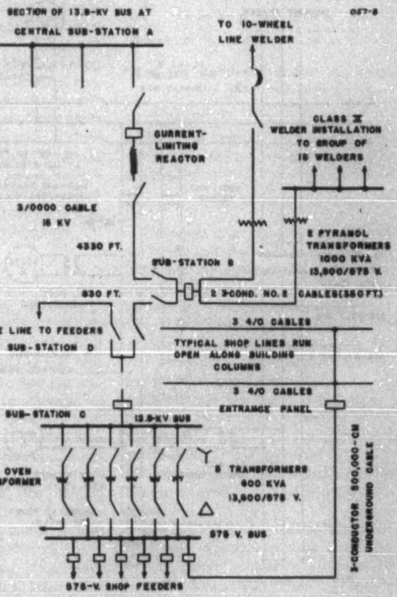
SECTION OF 13.8-KV BUS AT
CENTRAL SUB-STATION A
057-8
TO 10-WHEEL
LINE WELDER
CURRENT-
LIMITING
REACTOR
CLASS II
WELDER INSTALLATION
TO GROUP OF
15 WELDERS
3/0000 CABLE
15 KV
4330 FT.
2 PYRANOL
TRANSFORMERS
1000 KVA
13,800/575 V.
SUB-STATION B
830 FT.
2 3-COND. NO. 2 CABLES(350 FT.)
TIE LINE TO FEEDERS
AT SUB-STATION D
3 4/0 CABLES
TYPICAL SHOP LINES RUN
OPEN ALONG BUILDING
COLUMNS
3 4/0 CABLES
ENTRANCE PANEL
SUB-STATION C
13.8-KV BUS
OVEN
TRANSFORMER
6 TRANSFORMERS
600 KVA
13,800/575 V.
3-CONDUCTOR 500,000-CM
UNDERGROUND CABLE
575 V. BUS
575-V. SHOP FEEDERS

Figure 8. Schematic diagram of typical welder supply system—company D

two-pole fused enclosed switches to each separate welder and control. Each welder control is also equipped with a two-pole contactor, operated from a tumbler switch at the welder, which opens the power circuit to the welder without removing control voltage from its control panel. With all welders operating under normal production, the actual load, as measured with indicating instruments, is 1,116 kva, three phase, with a maximum unbalance between phases of $12^1/_2$ per cent, and an average 0.45 power factor. (Note that these are short-throat welders.) Bus voltage averages 549 volts with a maximum variation plus and minus of $3^1/_2$ per cent. It is probable that the average kva load over a period of several hours is slightly below the measured amount which may explain why there is no trouble with overheating the 1,000-kva power transformer.

A second installation directly adjoining the above consists of one seam welder equipped with five welding heads each with a 300-kva name-plate rating. Control consists of electronic panels mounted on an extension of the above-mentioned platform. Power is obtained from a second 1,000-kva three-phase Pyranol-transformer similar to the one mentioned above and located directly below the platform. The low-voltage side of this transformer is connected through a manually operated disconnecting switch and a 1,200-ampere three-pole inverse-time-limit electrically operated enclosed air circuit breaker directly to the five welding circuits and controls. Excitation for the controls is fed through an enclosed fused switch tapped into the line ahead of this breaker so that the breaker, which is operated from a push-button station at the welder, may be opened without breaking the control excitation. Under normal operation this welder demands approximately 1,500 kva with a 66 per cent duty cycle (two cycles on, one off) and draws, as measured with indicating instruments, 1,006 kva, three phase, with a maximum unbalance between phases of 22 per cent and an average 0.50 power factor. Supply voltage averages 525 volts with the welder in operation, and 550 volts at no load, with a plus and minus variation at either point of two per cent.

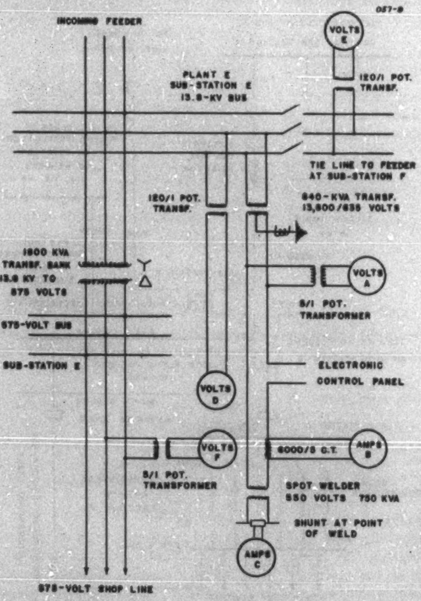

Figure 9. Location of voltage and current readings taken during test of power-supply system with 750-kva spot welder in operation —company D

The primary power supply to these two installations is taken from one of the regular shop 13.8-kv feeders *A* indicated in figure 8.

The control panel for the 13.8-kv oil circuit breaker is located on the welder control platform. Connection to the high side of each transformer is through pothead-type terminals which in turn are protected by an additional steel enclosure.

Class III

This includes large electronically controlled projection or spot welders where, although the equivalent continuous load is not great, the line-current demand at the time of making the weld is so high that it is inadvisable to supply it from the regular power transformers of one of the shop substations. In general, this applies to welders having a demand in excess of 2,000 amperes root mean square, 550 volts, during weld.

There are at present two installations that illustrate this classification. The first is a projection welder rated 550 volts, 500 kva (50 per cent duty cycle). Power for this welder is brought through a two-conductor 750,000-circular-mil concentric cable 500 feet long, from a special transformer. This transformer is rated 1,260 kva, 13,800/287.5-575-1,150 volts, and is designed to supply 15,000 amperes (peak) when connected for 575 volts. Maximum current obtainable at the welder is 2,200 amperes root mean square during weld with about 70 volts drop in the line.

The second welder in this class is a spot welder with name-plate rating of 550 volts, 750 kva, equipped with electronic control arranged for interrupted-operation welding. Power is obtained from a special transformer rated 840 kva, 13,800/635 volts and designed to give an impedance drop of less than 15 per cent when loaded at 6,000 amperes, 0.60 power factor. This transformer is located outside the building about ten feet from the welder and is fed from the 13.8-kv bus of the nearest shop substation through two lines of a three-conductor 15-kv underground cable approximately 210 feet long. This substation is in turn fed by a 13.8-kv feeder 3,800 feet in length, similar to the one shown in figure 8. Protection for both the welder and the power transformer is by an oil circuit breaker located in the shop substation and controlled from a panel adjacent to the welder. Both thermal and instantaneous overload relays are provided. Also the midpoint of the 635-volt winding of the power transformer is grounded through a current transformer which operates an instantaneous relay of low current setting to trip the circuit breaker in the event of a ground in either the welder transformer or control. The low-voltage side of the power transformer is directly connected to the control and welder through a two-pole manually operated disconnecting switch.

Recently tests were made to show the effect of the operation of this welder on various parts of the power system. The welder was operated at maximum capacity with a duty cycle of 4 cycles on and 54 off. All readings were taken simultaneously on an oscillograph with the following results. Figure 9 indicates the points at which these readings were taken.

A. Voltage of 603-volt supply to welder dropped 21.9 per cent (note that drop as read on indicating voltmeter was only 4.5 per cent).

B. Current of 603-volt supply to welder was 4,800 amperes (root mean square during weld).

C. Current in weld was 90,600 amperes (root mean square during weld).

D. Voltage of 13.8-kv bus in substation *E* at point of feed to welder power transformer dropped 9.7 per cent.

E. Voltage of the 13.8-kv tie line to substation *F* dropped 4.4 per cent. This indicates the drop on the central substation bus as this tie line is energized from a 13.8-kv feeder different from the one supplying the welder. Note that the capacity of the supply to the central substation bus was one-half normal at the time these tests were made.

F. Voltage of the 575-volt shop lines in plant *E* supplied through wye-delta-connected transformer bank substation *E* dropped 6.7 per cent.

All readings but the last were definitely taken from the same phase. It should be noted that the general plant voltage drop of 4.4 per cent (in one phase) observed at *E* is reduced one-half when the central substation bus is operating with full-capacity supply, and is probably further reduced by the time it reaches the 575-volt distribution system by the balancing effect of synchronous- and induction-motor shop load.

Company E Installation

In this plant like in many others where acquisition of new equipment and general expansion have been gradual, the majority of the welding equipment is widely scattered all over the plant. Furthermore, because of the nature of the plant's business, drastic changes in the location of a large portion of the welding equipment available are quite frequent. When they occur, certain general shifts in power loads and temporary feeds are sometimes made which result in different power-supply conditions.

For these reasons to present a general picture of the power feed of all welding equipment used would be both difficult and inconclusive. Accordingly this report is limited to a description of power supply of one group of welders which, because of the time and permanency of their installation, can be treated as an integral unit.

General Description of Equipment

The resistance-welding equipment comprising this group consists entirely of spot and seam welders. All but two are of the pedestal stationary type. One is a fully portable welding gun and one a semiportable type with provision for 30-foot travel. All stationary machines are located within 50 feet of the distribution board.

The kilovolt-ampere capacities of the equipment are shown in figure 10. It will be noted that several of the machines have full-load capacities of such magnitude that were they to form part of the normal production load unsatisfactory voltage regulation would result with the power supply available. However, the maximum production requirements of these machines are considerably lower, as shown on the "normal maximum" line, and rather than increase the power-supply installation mechanical provisions are made preventing the setting of these machines at a tap higher than the normal maximum for all work except experimental which is conducted by plant engineers only when this group is inoperative, and with one machine at a time.

Load Conditions

The total maximum load is 1,375 kva. This load, however, corresponds to operation with short-circuited secondaries. The actual working loads of these machines run 5 to 15 per cent less, depending on the machine and the material used. Assuming an average of 10 per cent, the actual maximum load becomes 1,237.5 kva. Application of this load to the 460-volt bus results in a net drop of 13.6 per cent on this bus which would be excessive from the welding point of view. In addition it produces a 5 per cent drop on the 2,300-volt bus which is also considered undesirable.

However, the application of maximum load presupposes two facts: (1) that every machine is operated at its maximum setting; (2) that every machine should fire at the same time. As the machines are operated most of the time at setups averaging 50 per cent capacity, and as the combined duty and welding cycle (time of flow of current to total operating time) is between 5 per cent and 25 per cent, the probable maximum load is considerably smaller.

Prolonged and extensive oscillographic studies of the group failed to reveal load peaks higher than 500 kva with a highest daily average around 250 kva.

No attempt has been made to measure the kilowatt or kilowatt-hour loads as no evidence of heating of transformers, equipment, or feeders has ever been observed.

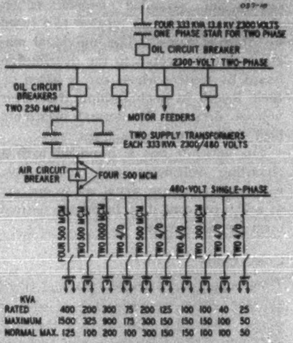
037-10
FOUR 333 KVA 13.8 KV 2300 VOLTS
ONE PHASE STAR FOR TWO PHASE
OIL CIRCUIT BREAKER
2300-VOLT TWO-PHASE
OIL CIRCUIT BREAKERS
TWO 250 MCM
MOTOR FEEDERS
TWO SUPPLY TRANSFORMERS EACH 333 KVA 2300/480 VOLTS
AIR CIRCUIT BREAKER
A
FOUR 500 MCM
480-VOLT SINGLE-PHASE
FOUR 500 MCM
TWO 500 MCM
TWO 1000 MCM
TWO 4/0
TWO 500 MCM
TWO 4/0
TWO 4/0
TWO 300 MCM
TWO 4/0
TWO 4/0
KVA
RATED 400 200 300 75 200 125 100 100 40 25
MAXIMUM 1500 325 900 175 300 150 150 150 100 50
NORMAL MAX. 125 100 200 100 300 150 150 100 100 50

Figure 10. Schematic diagram of welder supply system—company E

Power Supply

The power is supplied by two 333-kva 2,300/460-volt single-phase 3.56 per cent impedance oil-immersed self-cooled transformers connected in parallel. The transformers are installed on an outdoor platform adjacent to the floor housing the welding equipment. The 460-volt bus and distribution are thus not over ten feet from the transformer bank.

The transformer bank is fed from a 2,300-volt bus which, in addition to the welding load, also feeds a miscellaneous motor load averaging about 300 kva, two phase, with a normal maximum 15-minute demand of around 650 kva, two phase.

The 2,300-volt bus is fed by a 930-kva, two-phase transformer bank (belonging to the power company) consisting of four 333-kva 13,800/2,300-volt single-phase oil-immersed self-cooled transformers, star connected.

Distribution System

The 2,300/460-volt transformer bank is fed from the 2,300-volt bus by two 250,000-circular-mil stranded copper cables through an automatic oil circuit breaker equipped with instantaneous overload trip normally set at 500 amperes. The circuit breaker thus, besides providing short-circuit protection, provides against dangerously high overloads which would result in excessive voltage drop.

The secondary side of the 2,300/460-volt bank is connected by four 500,000-circular-mil stranded copper cables through an air circuit breaker to a fused distribution board. The individual machines are connected to the distribution board by feeders whose size is based on their length in such fashion that their voltage drop at the normal maximum load shall not exceed 0.25 per cent.

The 460-volt bus is provided with a recording voltmeter and an audible undervoltage alarm signal set to operate at a $7^1/_2$ per cent drop below the normal open-circuit voltage.

Conclusion

The extensive voltage-regulation studies, extending over a period of several years, appear to indicate that the normal voltage fluctuation on the 460-volt bus is plus or minus three per cent of the average operating voltage. Under exceptional operating conditions a drop of minus four per cent has been observed. At the same time the voltage fluctuation of the 2,300-volt bus during normal operating conditions has been plus or minus two per cent of the average operating voltage.

These voltage conditions appear to be entirely satisfactory both from the plant operating and welding standpoints. For this reason the company has discontinued the use of an automatic instantaneous-operation-type voltage regulator which was originally part of this installation. The use of the voltage regulator, although it proved to be entirely practical, did not appear to be justifiable in this case as it presented certain problems and the further improvement in the voltage regulation it offered did not appear to be essential.

Company F Installation

1. Method of Service by Power Company

The power company supplies all of the load at the plant through two step-down transformer banks each rated 1,500 kva. One bank supplies all the small loads from a 120/208-volt radial system and the second bank supplies all the large loads and welders in the plant at 240 volts. The transformer banks are supplied from a 13,200-volt circuit, which is fed from two large substation busses. The entire load is metered on the primary side of the transformers and the customer owns the transformers.

2. Cost to Power Company to Serve Welder Load

No special equipment or expense was required by the power company to supply the welder loads. Voltage dip was not great enough to cause objectionable light flicker.

3. Plant Distribution for Welders

The transformer bank supplying the welders consists of three 500-kva transformers connected delta-delta of 4.4 per cent reactance and 1.7 per cent resistance. The welders are in two groups at two widely separated locations.

The first group, of which the largest welder is rated 125 kva, is supplied over a three-phase line consisting of three sets of three 800,000-circular-mil copper conductors in parallel. Other welders in the group are taken from a three-phase bus about 100 feet long made up of two one-quarter by four-inch copper conductors per phase, also supplied from the 800,000-circular-mil conductors.

The second group, of which the largest welder is 400 kva, is supplied from about 50 feet of three-phase bus made up of four one-quarter by four-inch copper bars per phase, with a spacing of one-half inch between bars. The center lines of the phases are spaced about six inches apart.

Welder-Feeder Protection

The bus to the second group, which includes the largest welders, is connected directly to the secondaries of the power transformer. The circuit to the other group is supplied through a low-voltage air circuit breaker.

Type and Rating of Welders

The largest welders in both groups are spot welders. The first group includes 21 welders having a combined rating of 1,300 kva, of which the largest is 125 kva. The second group includes 5 welders having a total rating of 1,100 kva of which the largest is 400 kva.

Voltage-Drop Data

The following calculated voltage drops at various points on the distribution system apply to 0.4-power-factor loads, which oscillograph records show to be closely approximated:

	Per Cent	
	Location 1, 800-Kva Load	Location 2, 150-Kva Load
On 13.2-kv line at the plant	1.5	0.3
On 240-volt bus	5.3	1.0
On 240-volt circuit at welder	8.0	2.0

Equivalent Thermal Heating

The timing on all of the spot welders is between 15 and 20 cycles. The time between welds is about three seconds on the larger welder and one second on the smaller group. The equivalent kilovolt-amperes on a heating basis is about 150 kva for the larger group and was about 275 kva for the smaller group.

Upper-Air Weather Soundings by Radio

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Synopsis: A radio system for upper-air weather soundings with small unmanned balloons is described. The balloon instrument comprises elements for measuring pressure, temperature, and humidity, and a miniature radio transmitter; the latter converts the response of the measuring instruments into characteristic signals modulating the emitted radio wave and sends this wave to a ground station where the signals are measured and recorded. Details of the system are given and a discussion is given of the accuracy of the upper-air observations obtained and of the tolerances permissible for component elements of the system.

I. Introduction

A RECENT significant advance in weather forecasting is based on the routine collection of upper-air data to heights of up to 25 kilometers by means of radio soundings. This paper describes a method of radio sounding now in widespread use and presents a discussion of the accuracy of the observations. This method has recently supplanted the use of airplanes carrying calibrated recording instruments for securing upper-air data.

The radio-sonde system employs an instrument carried aloft on a small unmanned balloon, and ground-station receiving and recording equipment. The balloon instrument, called a radio sonde, comprises elements for the measurement of barometric pressure, air temperature, and humidity; a radio transmitter and power supply; and means for impressing on the radio transmitter modulating frequencies which have predetermined relationships to the values of the factors measured. At the ground station, the frequencies of the received signals are measured and recorded automatically, producing a chart of temperature and humidity plotted against pressure.

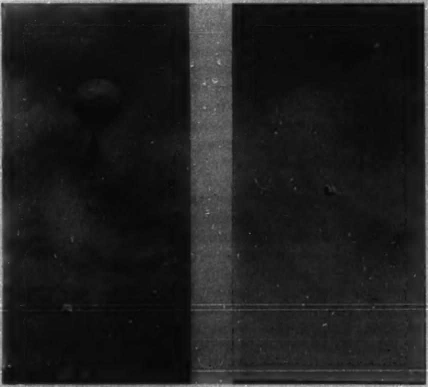

Figure 1. Radio sounding equipment during ascent and descent

Figure 1 (at the left) shows a radio sonde just as the sounding has begun. The balloon is of latex rubber and is inflated with helium or hydrogen gas to give a lift of about one kilogram in excess of the over-all weight of the sounding equipment. An ascensional rate of approximately 200 meters per minute is obtained. The diameter of the balloon is about 5 feet at surface pressure and reaches a value of the order of 15 feet before bursting; the corresponding maximum altitude ranges from 20 to 25 kilometers. A small silk parachute between the balloon and the instrument operates when the balloon bursts, bringing the instrument back to earth at a safe rate of descent. See figure 1 (at the right). The wind may carry the balloon up to 200 miles from the point of release before the equipment returns to the earth; nevertheless, more than a third of the radio sondes released are recovered and are used over again.

The potential value of our method of radio sounding was determined in experimental use by the Weather Bureau, Navy Department, and Coast Guard at 12 stations during 1938–39. The service has since been expanded to include some 30 land and 5 shipboard stations; more than 1,000 radio sondes being used monthly. This paper deals with an improved form of radio sonde using this method which incorporates a new device for measuring relative humidity; the improved instrument has been introduced at nine stations and its use at one more is contemplated in the near future.

A bibliography on radio methods for upper-air soundings was given in an earlier paper.[1] The method herein described was developed under the sponsorship of the Aerological Service of the United States Navy Department with the object of providing a new approach toward meeting the rigid operating requirements faced. In the latter stages of the development, considerable co-operation was had with Julien P. Friez & Sons, Baltimore, Md.

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Harry Diamond, Wilbur S. Hinman, Jr., Francis W. Dunmore, and Evan G. Lapham are all affiliated with the National Bureau of Standards, Washington, D. C.

[1] For all numbered references, see list at end of paper.

The instrument requirements are rather severe. Measurements of pressure, temperature, and humidity are desired to heights of up to 25 kilometers. In such excursions, the range of pressures encountered may be from 1,070 millibars at the surface to 25 millibars at the top, the range of temperatures from +40 to −90 degrees centigrade, and the range of humidities from 0 to 100 per cent relative humidity. The required accuracies of measurement are ±5 millibars throughout the range of pressures, ±0.75 degree centigrade at temperatures above −50 degrees, and ±5 per cent relative humidity at temperatures above −20 degrees.

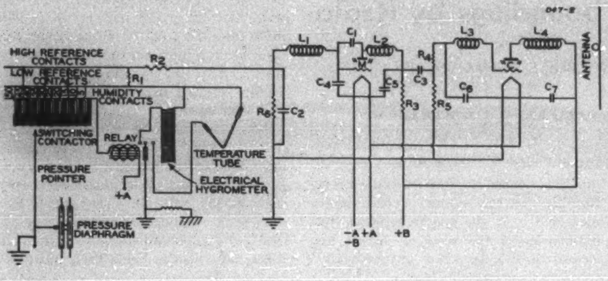

Figure 2. Electrical circuit arrangement of the radio sonde

R_1, R_4—1,000 ohms
R_2—40,000 ohms
R_3—25,000 ohms
R_5—5,000 ohms
R_6—1,000,000 ohms
C_1, C_4, C_7—0.00025 microfarad
C_2—0.07 microfarad
C_3—0.05 microfarad
C_5—0.01 microfarad
C_6—0.0001 microfarad
M—Modulating oscillator
C—Carrier oscillator

Superposed on the instrument requirements are practical requirements which increased materially the difficulty of the problem. Considerations of economy dictate that the unit cost of the radio sonde must not exceed the amount hitherto expended for an airplane sounding ($25 to $30) despite the facts that airplane soundings were generally limited to heights below 6 kilometers and that their regularity of operation was frequently interrupted by adverse weather conditions. The weight of the instrument must be kept sufficiently low (of the order of one kilogram) to allow its being carried to the desired height by a balloon of the dimensions given. However, a rugged construction is essential since the instrument must be capable of shipment by ordinary transportation to considerable distances and of storage for several months under field conditions without upsetting the original calibrations. Ease of handling by field personnel and minimum time requirements for making the sounding and for evaluating the observations are additional conditions.

From these considerations, it will be apparent that the practicability of a radiosonde design depends not only on its potential accuracy of measurement but also upon the ruggedness of the component elements required and upon the possibility of their manufacture by mass-production methods. The equipment to be described complies with the requirements outlined largely because of the special arrangement devised for carrying out the measurements and for translating them into signals which may be evaluated at the ground station.

II. Technical Features of the Radio Sonde

The principles underlying the operation of the radio sonde will be understood from a study of the electrical circuit diagram shown in figure 2. As already stated, the radio sonde consists of a radio transmitter and elements for measuring pressure, temperature, and humidity. A photograph of the radio transmitter is shown at *b* in figure 3, while the pressure-measuring unit is shown at *a*. Figure 4*a* shows the temperature element and figure 4*b* the humidity element (comprising three interconnected units). The temperature and humidity elements are fastened to opposite sides of a mounting plate which fits into a ventilated compartment of the radio-sonde container. As will be shown, these elements constitute electrical resistors which vary respectively in accordance with the temperature and relative humidity of the air to which they are exposed.

1. The Radio-Sonde Transmitter

Referring to figure 2, the transmitter employs a conventional receiving-type vacuum tube having two sets of triode elements in a single glass envelope. One of the triodes is connected in a carrier oscillator circuit, operating at a frequency of 65 megacycles per second and feeding a half-wave doublet transmitting antenna. The second triode is used in an auxiliary oscillatory circuit, oscillating at about one megacycle per second and having in its grid circuit a resistance-capacitance network which operates to interrupt or "block" the oscillator at a rate inversely proportional to the time constant of this network. The capacitance is of fixed value but the resistance varies so that the "blocking" frequency ranges from 10 to 200 cycles per second. This frequency is made to modulate the carrier oscillator, resulting in an emitted carrier wave having a variable audio-frequency modulation.

The one-megacycle oscillations of the modulating oscillator (controlled by the coils L_1 and L_2 and by the capacitors C_1

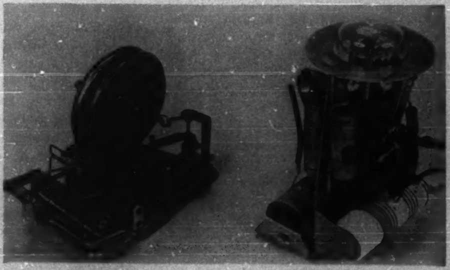
(*a*) (*b*)

Figure 3. The radio-sonde transmitter and pressure-switching unit

and C_4) are used only for triggering purposes. During the oscillating condition, the grid swings positively each half-cycle and a current flows through the resistance-capacitance network formed by the capacitor C_2 in parallel with the series circuit comprising R_2, R_1, and either the temperature or the humidity resistor. (The shunt resistor R_6 sets an upper limit to the value which may be attained by the resistance in parallel with C_2.) The voltage developed across this network is applied as a negative biasing voltage on the grid. The magnitude of this biasing voltage is a function of the charging rate of the capacitor C_2 through the tube grid-filament resistance and of the amplitude of oscillation; it builds up within about 0.5 millisecond to a value sufficient to block the oscillator. During the nonoscillating condition, the capacitor C_2 discharges through the resistance combination until the negative biasing voltage is again sufficiently low to allow the tube to oscillate. This action is repeated cyclically at a rate which depends upon the time-constant of the resistance-capacitance network, which, as is evident, controls only the duration of the nonoscillating period.

In the absence of the modulating oscillator, the carrier oscillator would operate continuously. However, when the modulating oscillator begins to oscillate, its plate begins to draw current and a negative pulse is applied (through the coupling capacitor C_3) across the resistor R_5 in the grid circuit of the carrier oscillator. This voltage is of sufficient magnitude to block the carrier oscillator; the carrier-frequency oscillations do not begin again until the modulating oscillator ceases to draw plate current (because of cessation of its oscillations) so that a positive pulse is applied to R_5. It will be evident from the foregoing that the carrier wave sent out by the radio-sonde transmitter is interrupted for short periods of constant time duration, the rate of occurrence or frequency of these interruptions being controlled by the value of the resistance in the resistance-capacitance network.

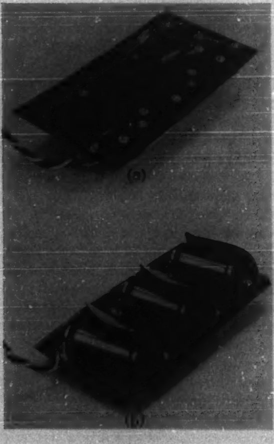

Figure 4. The temperature and humidity measuring elements

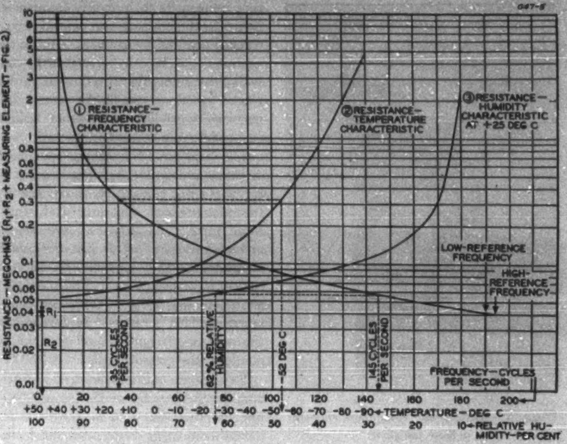

Figure 5. Curves illustrating the principles involved in the measurement of temperature and humidity

2. The Pressure-Switching Unit

This unit performs two functions, one as a measuring instrument of barometric pressure in the specified range, the other as a switch which connects into the resistance-capacitance circuit of the modulating oscillator, the temperature and humidity resistors and two fixed values of resistance known as the low and high reference. (See figures 2 and 3.) The pressure arm carries a tapered contact which bears against the polished face of a switching element at a position controlled by the ambient pressure. As the balloon rises into lower levels of pressure, the diaphragm expands laterally thereby sweeping the contactor over the face of the switching element and making the desired connections.

The switching element consists of 80 metallic strips separated by insulating strips. The conducting strips are arranged in groups of four adjacent intermediate contacts, the adjacent groups being separated by wider index contacts. The intermediate conducting strips are all connected together and to the field coil of a miniature relay which is energized upon completion of a low-voltage circuit by contact of the pressure-arm contactor with any of the intermediate conducting strips. The grounded armature of the relay normally rests against its back contact to which is connected one side of the temperature resistor. When the coil of the relay is energized, its armature bears against a front contact to which is connected one side of the humidity resistor.

By reference to figure 2, it will be evident that the relay operates to connect into the resistance-capacitance network of the modulating oscillator (in series with R_2 and R_1) either the resistance thermometer or the resistance hygrometer, depending upon whether the pressure-arm contactor bears against an insulating strip or an intermediate conducting strip of the switching element. The intermediate conducting strips are more conveniently referred to as humidity contacts since the time constant of the resistance-capacitance network and, hence, the modulating frequency of the radio-sonde transmitter is controlled by the resistance hygrometer when the pressure-arm contactor bears against any one of these contacts.

Figure 6. Radio-sonde component elements in their container

The index conducting strips of the switching element are arranged in two groups; the members of one group being connected electrically to the junction of resistors R_2 and R_1, and the members of the second group to the junction of R_1 with the two measuring elements. Contacts of the first group, comprising contact numbers 15, 30, 45, 60, and 75, are termed the high reference contacts, since only one resistor R_2 remains in circuit when the pressure-arm contactor bears against one of these contacts and, hence, a fixed high reference modulating frequency is obtained. Contacts of the second group, comprising contact numbers 5, 10, 20, 25, 35, 40, 50, 55, 65, 70, and 80, are termed the low reference contacts, R_2 and R_1 being in circuit when the contactor bears against one of these contacts and a somewhat lower fixed reference modulating frequency being then obtained.

3. THE MEASURING SYSTEM

The method employed for measuring and indicating at a remote point the values of the pressure, temperature, and humidity encountered by the radio sonde during the ascent of the balloon is now evident. The radio-sonde transmitter emits a radio wave modulated by audio frequencies which correspond successively to temperature and humidity observations. The temperature observations occur when the pressure-arm contactor passes over an insulating strip while the humidity observations are obtained when the contactor bears against a humidity contact. The changeover from temperature frequencies to humidity frequencies occurs as the pressure-arm contactor just reaches a humidity contact. When the contactor reaches successive fifth contacts, the humidity observation is replaced by one of two fixed reference frequencies depending on whether a high or low reference contact is reached.

The sequence of switching operations serves as indication, by actual count, of the particular contact being reached by the pressure-arm contactor and, thus, from prior calibration, of the actual ambient pressure. This count is much facilitated by the occurrence of the high reference frequency every 15th contact and of the low reference frequency at intervening fifth contacts. In this usage, the reference frequencies serve as index points on the pressure scale.

To evaluate the temperature and humidity observations at the ground station, it is necessary to measure the received modulating frequencies and to interpret the measurements in terms of the resistance-frequency characteristic of the radio-sonde transmitter and the resistance-temperature and resistance-humidity characteristic of the temperature and humidity elements in the radio sonde. Fortunately, for this method of measurement, it is readily possible to control the manufacture of the three elements involved (the transmitter, the temperature tube, and the electrical hygrometer) so as to attain substantially uniform characteristics. Thus, the necessity for individual calibration of the three component elements is obviated and standard charts and scales may be used in evaluating the observations at the receiving station.

The principles involved in the measurement of temperature and humidity are illustrated in figure 5. Here curve 1 is the resistance-frequency characteristic of the modulating oscillator, curve 2 is the resistance-temperature characteristic of the temperature tube, and curve 3 is the resistance-humidity characteristic of the electrical hygrometer. In later sections of this paper, it will be shown that the measurements are actually made on the basis of relative frequencies rather than on an absolute frequency basis as might be inferred from the foregoing explanation. In this way, rather large tolerances may be allowed in the manufacture of the radio-sonde transmitter without affecting the accuracy of the measurements.

4. THE TEMPERATURE ELEMENT

This device, shown in figure 4, utilizes the effect of temperature on the conduction of electricity through an electrolyte. Its construction is quite simple, as will appear from the following.

The glass capillary tube has an over-all length of 5 centimeters, a bore diameter of 0.875 millimeter, and a wall thickness of 0.4 millimeter. Capillary lengths of the required bore are purchased to a tolerance of ten per cent and, by volumetric measurements, their lengths are adjusted so as to produce an electrical resistance of 14,000 ohms (plus or minus five per cent) when filled with a standard electrolyte at +30 degrees centigrade. The capillary tubes are then bent into V form and are furnished with two glass bulbs fused to the ends. The wells provide for low-resistance contact between the electrolyte and the copper terminals of the tube, which extend through organic seals. One of the terminals consists of a very fine copper tube through which the capillary tube is filled in a vacuum chamber; the end of this copper tube is soldered to complete sealing of the device.

Figure 7. Ground-station receiving and recording equipment

The standard electrolyte used consists of a solution of 24 per cent (by volume) concentrated hydrochloric acid, 76 per cent of ethyl alcohol, and 2.7 grams of cuprous chloride for each 100 cubic centimeters of the resultant mixture. The alcohol lowers the freezing point of the electrolyte to below −100 degrees centigrade, while raising the resistivity to the desired value. The cuprous chloride operates in combination with the copper terminals to eliminate polarizing action in the tube when passing direct current.[2]

The resistance-temperature characteristic of the tube is shown by curve 2 in figure 5. Provided that excessive direct current is not passed through the tube, its calibration remains constant within 0.5 per cent for a period of a year or more.

Advantages of the device for radio-sonde application include: uniformity of calibration for all tubes filled with the standard solution, large area of exposure to the ambient temperature, rapidity of response to temperature changes, and thermal isolation from other parts of the radio sonde with consequent reduction of errors in measurement due to heat conduction to a negligible order.

5. THE ELECTRICAL HYGROMETER

This device,[3] shown in figure 4, utilizes the effect of moisture on the conductivity of a hygroscopic salt. Three units are required to give the desired variation of resistance with relative humidity. The units are mounted on the opposite side of the mounting plate from the temperature tube and rain shields are provided to prevent direct contact with rain. Each unit consists of a dual winding of number 38 American wire gauge bare palladium wire (25 turns each with a pitch of 8 double turns per centimeter) wound on a coil form one centimeter in diameter. The coil form consists of an aluminum tube having a wall thickness of 0.25 millimeter and covered with an insulating coating (0.25 millimeter thick) which is highly resistant to water. After winding, the three units are each coated with different predetermined solutions of lithium chloride in polyvinyl acetate.

The surface-leakage resistance between the two interspaced windings of each unit varies with relative humidity according to predetermined characteristics. The most sensitive coating (3 per cent lithium chloride) covers the humidity range from 10 to 35 per cent; the next sensitive coating (2 per cent lithium chloride) covers the humidity range from 25 to 50 per cent; while the least sensitive coating (1 per cent lithium chloride) covers the humidity range from 40 to 100 per cent. By connecting the three units in parallel (with suitable resistors in series with the 2 per cent and 3 per cent units), the resistance-humidity characteristic shown by curve 3 of figure 5 is obtained.

This characteristic holds only at room temperature, the range of variation of resistance being modified as the temperature decreases. This results in a progressively narrower range of humidity indication at the lower temperatures; the range at −60 degrees centigrade is from 100 to about 35 per cent relative humidity.

The electrical hygrometer has important advantages over other forms of hygrometers, particularly for radio-sonde application. In an earlier form of the radio sonde described in this paper, a variable resistor controlled by the more conventional hair hygrometer is used for measuring relative humidity. While useful observations are obtained, there are two limitations to this type. The first is that the hair hygrometer does not function efficiently at temperatures below about zero degrees centigrade; the second, that even at higher temperatures it does not respond sufficiently to the sharp changes in humidity encountered by a balloon ascending at the lowest practical rate (175 meters per minute). The electrical hygrometer overcomes both of these defects. It is characterized by a very rapid response to humidity variations at temperatures down to at least −60 degrees centigrade. Its rate of response is sufficient to permit any practical balloon ascension rate, as has been determined in actual tests at ascension rates of up to 400 meters per minute.

6. THE COMPLETE RADIO SONDE

A photograph of the complete radio-sonde assembly is shown in figure 6. A light cardboard case, covered with a metallic foil and divided into two compartments, is employed. The front compartment houses the radio transmitter *a*, the plug-in battery power supply *b*, the pressure-switching unit *c*, and the miniature relay *d*. The rear compartment supports the radiation shield *e*, for protecting the temperature and humidity elements from heating by solar radiation. The shield consists of two thin concentric aluminum tubes, the inner one of which supports the mounting plate on which the temperature and humidity elements are fastened. The shield normally extends about two centimeters above its compartment and the top edge of the mounting plate is normally flush with the top edge of the shield. The temperature and humidity elements are ventilated by the flow of air through this shield produced by the upward movement of the balloon.

The complete instrument weighs slightly less than one kilogram and its present unit price in quantities of several thousand is $27. An interesting feature of the instrument is that, with the radiation shield assembly slid down into its cardboard container, top and bottom flaps (tied back during the ascent) may be used for closing off this container. The instrument is then in convenient form for shipping or for return through the mail upon its recovery. Complete mailing instructions and the reward offered for its return are printed on the foil cover.

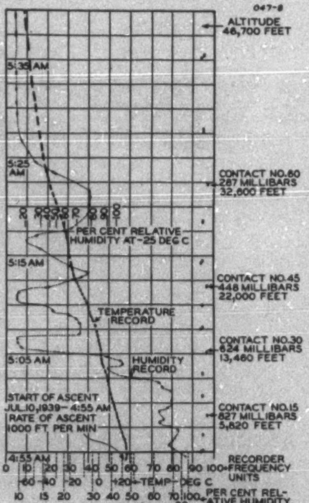

Figure 8. Record of a typical sounding

III. Ground-Station Equipment

Typical ground-station receiving and recording equipment are shown in figure 7. The receiving set *a* feeds an electronic frequency meter *b* which measures the frequency of the modulating signal from the balloon and converts it into a direct current proportional to the frequency. This is indicated by a microammeter on the electronic frequency meter and is simultaneously recorded by a recording microammeter *c*.

An important operating feature of the electronic frequency meter is that the current is directly proportional to the frequency within very narrow limits. Moreover, a control in the output circuit allows adjustment of the current corresponding to any frequency within the range by up to ±15 per cent without upsetting the linear relationship between the current and frequency throughout the range. The use made of this property is an essential feature in the use of the radio-sonde system described, as will appear later.

IV. Record of a Typical Sounding

A record of a typical sounding, redrawn for reproduction, is shown in figure 8. The start of the record, corresponding to

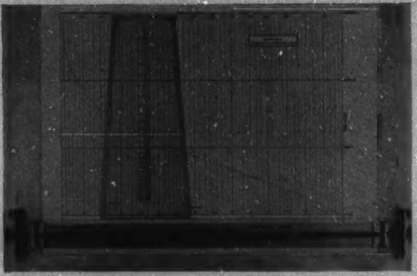

Figure 9. Illustrating the method of correlating the standard radio-sonde resistance-frequency characteristic with the standard resistance-temperature characteristic of the temperature tube

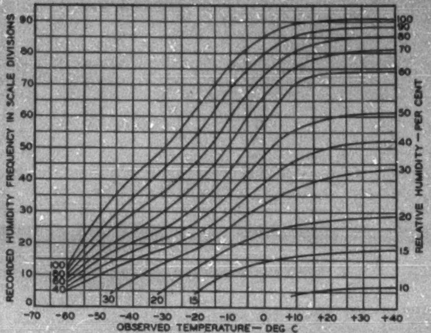

Figure 10. Charts for evaluating the relative humidity from the observed recorder frequency and the temperature at which the observation is made

the release of the balloon, is at the bottom of the chart. The recorder chart has 100 equally spaced divisions corresponding to a full-scale range of 200 cycles per second; however, it is more convenient for station personnel to work in terms of the recorder divisions which may be considered as arbitrary frequency units. For an approximate interpretation of the record, a scale of temperatures (for an average temperature bulb) and scales of humidities (at two ambient temperatures) are marked on the chart.

The traces at the extreme right of the chart represent the high reference frequencies while the traces just to the left of these represent the low reference frequencies. The physical positions of the pressure-arm contactor as these marks were being traced by the recorder may be visualized by referring to the contact numbers shown at the lower edges of the high-reference traces. (See also figures 2 and 3). The altitude of the balloon corresponding to each high-reference trace is marked on the chart as of likely interest to the reader. Altitude is determined from the air density computed on the basis of the pressure, temperature, and humidity observations.

The temperature and humidity traces are seen to form broken lines which plot, respectively, the variation of these two meteorological factors as a function of the balloon altitude. The temperature traces are readily distinguishable from the humidity traces by the nature of their variation. Each interruption of the temperature plot designates that the pressure-arm contactor has just reached a humidity or reference contact; each interruption of the humidity plot indicates that the contactor has just left a humidity contact. Light dotted lines have been added between adjacent humidity traces to emphasize the sharp variations obtained.

1. Evaluation of Pressure, Temperature, and Humidity

In determining the exact pressure level at which a given temperature or humidity trace obtains, the aerologist refers to the calibration chart for the particular pressure unit used. In this chart, heavy horizontal lines represent the reference contacts on the switching element and lighter horizontal lines represent the humidity contacts. The line thickness and spacing between lines are proportioned to correspond respectively to the widths of the conducting and insulating strips comprising the switching element. The ordinate scale thus represents contact numbers; the scale of abscissas represents barometric pressure in millibars. The calibrating points are generally taken just as the pressure-arm contactor reaches the reference contacts and a smooth line is drawn through them.

A special chart and glass scale are used for evaluating the temperature observations corresponding to particular recorder traces. The curve on the chart is the standard resistance-frequency characteristic of the radio-sonde transmitter plotted on semilogarithmic paper. The ordinate (logarithmic) scale represents resistance added in series with R_2 and R_1 in figure 2; that is, the resistance of the temperature tube. The scale of abscissas is in arbitrary frequency units corresponding to divisions on the recorder chart.

Since the temperature tube follows a fixed law of variation of resistance with temperature, the curve on the chart may also be considered to represent a plot of temperature versus generated audio frequency for a standard temperature tube and transmitter. However, in order to reduce the requirements as to the accuracy of the bore in the manufacture of the temperature tubes, the resistances of different tubes at a given temperature may differ appreciably even though they have the same temperature coefficient of resistance. This, in effect, requires that the temperature ordinate scale be permitted to slide up or down along the resistance scale. The required condition is met by mounting the chart on a drawing board and providing a glass plate bearing a vertical index line graduated in degrees centigrade which moves up or down on the chart. See figure 9.

The chart and scale together form a plot of temperature versus frequency, with means for sliding the temperature scale of ordinates to fit individual tubes. The lower edge of the glass scale rests against a movable horizontal crossbar (fastened to the drawing board) which facilitates making the adjustment.

Just prior to the release of a radio sonde, the operator measures the true temperature at the ground surface and the corresponding frequency observation from the radio sonde. These data are used in setting up the glass-plate temperature scale on the standard resistance-frequency characteristic. The operator moves the glass plate horizontally on the frequency chart so that the vertical index line coincides with the frequency corresponding to the observed recorder trace and then slides the crossbar on the drawing board upward or downward until the observed surface temperature intersects the resistance-frequency characteristic.

The chart used for evaluating the humidity observations is shown in figure 10. The ordinate scale is in arbitrary frequency units corresponding to divisions on the recorder chart. The scale of abscissas represents the temperature at which the humidity measurements are made. The graphs represent curves of equal relative humidity. Knowing the frequency of the observed humidity trace and the corresponding observed ambient temperature, the relative humidity is determined by interpolation on the chart. Reasonable control of the manufacturing processes makes it possible to obtain electrical hygrometers which conform very closely to the standard characteristics of figure 10, within plus or minus three per cent relative humidity.

V. Accuracy of the Observations

1. Analytical Considerations

It will be evident that the accuracy of the pressure observations of sounding will depend practically entirely on the accuracy of repetition of performance of the pressure diaphragm and pressure-arm linkage. Repeated calibrations of individual units show agreement of the calibration points within two millibars. A control for moving the switching element laterally with respect to the pressure-arm contactor provides for setting the unit to read correctly at the surface pressure prior to an ascent, thereby correcting for zero shift and at the same time applying a surface correction for temperature effect at ground-level pressure. The over-all temperature effect in service use is within three millibars.

The accuracy of the temperature and humidity observations depends on a number of factors which at first hand would appear to require considerable control of the component elements employed in the radio sonde and at the ground station in order to secure the required accuracy. However, simple expedients operate to reduce the tolerances to practicable values.

One expedient is to make the measurements in terms of relative rather than absolute frequency. For this purpose, the low reference frequency, having a nominal value of 190 cycles per second (95 recorder divisions), is used as a control point. From the discussion in connection with figure 2, it is apparent that the measurement of audio frequency is made merely as a convenient way of measuring the ratio of the resistance of the temperature or humidity elements to the resistance of the low reference resistor. The parameters of the vacuum tube and of the modulating oscillator circuit and the variation of these parameters even for the wide variations of plate and filament voltages and ambient temperature encountered during a sounding have but little effect upon the ratio of any two frequencies corresponding to two values of the resistance network. Similarly variations in C_2 have negligible effect upon such ratios. It is obvious then, that if the value of (R_2+R_1) is maintained within ±0.5 per cent under operating conditions and the current control in the output of the electronic frequency meter (in the ground-station equipment) is adjusted so that the recorder reads 95 divisions whenever the low reference signal is received, the observed frequencies corresponding to particular resistance values of the temperature- and humidity-measuring elements must have predetermined relationships to the low reference frequency. This is equivalent to saying that all radio-sonde transmitters may be made to have practically identical resistance-frequency characteristics whether in the laboratory or during an ascent merely by close control of (R_2+R_1). (The resistor R_4 being much higher than the other resistors, needs be controlled only within plus or minus five per cent.)

Thus, if the absolute reference frequency of a particular transmitter happens to be considerably different from the nominal value, the operator merely adjusts the frequency-meter control to make it read the standard nominal value. The same is true if the reference frequency drifts during a sounding. The small step variations in several of the low-reference traces on the record of figure 8 show where such adjustments were made.

Such small departures from the average characteristic as are found to occur in individual transmitters (after the reference-frequency adjustment is made) invariably take the form of a practically parallel shift of the curve upward or downward. The method of setting up the temperature scale on the basis of the surface-temperature data (described in a previous section) takes care of these departures in so far as the temperature measurements are concerned. A little consideration will show that, with this method, not only is the actual resistance of the temperature tube at the surface temperature taken into account, but a correction is automatically applied for the parallel departure of the actual resistance-frequency characteristic from the standard characteristic. While this correction does not apply to the humidity measurements, the requirements of accuracy of the humidity measurements do not demand such refinements.

Let us now consider what requirements must be met by the electronic frequency meter and the recorder in the ground-station equipment. The essential requirement is that the output current of the electronic frequency as indicated on the recorder trace shall be substantially linearly proportional to the received signal frequency and shall repeat this relationship within ±0.3 division under all conditions of operation. This condition must be fulfilled regardless of the actual value of the reference frequency and represents a maximum error in temperature measurement of 0.5 degree centigrade at zero degrees centigrade. Small deviations from the required linear relationship may be taken care of by applying a suitable correction factor to the standard resistance-frequency characteristic used with a given ground-station setup or, even if neglected, is largely corrected for (in temperature measurements) by the process of setting up the temperature scale just described in connection with the surface-temperature check.

On the basis of the foregoing discussion, it is apparent that temperature measurements to an accuracy of ±0.75 degree centigrade can be made with the radio-sonde system without placing undue manufacturing and calibration restrictions on either the radio sonde or the ground-station equipment.

2. Experimental Results

The curves of figure 11 provide experimental corroboration of the foregoing conclusion.[4] Curve 2 summarizes the results of 25 nearly simultaneous radio and airplane soundings. Some 100 such simultaneous ascents were made with practically the same order of agreement. The abscissas represent deviations be-

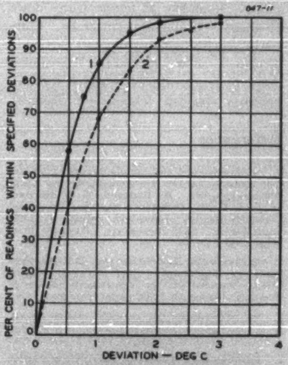

Figure 11. Summary of comparative data obtained: (1) with simultaneous radio-sonde and balloon meteorological soundings; (2) with simultaneous radio-sonde and airplane soundings

tween the two types of observations while the ordinates represent the percentages of the total observations falling within the specified deviations. In considering the results, it should be noted that the deviations include the effect of errors in the airplane instrument as well as in the radio sondes. Also, since the pressure measurements within the two types of instruments were required in computing altitude in order that the temperature observations might be compared at equivalent altitudes, the deviations include the effect of possible errors in the observations of pressure.

Curve 1 summarizes similar results for 20 soundings in which the balloon carried a recording meteorograph in addition to the radio sonde; the data from this recording instrument were obtained only after recovery. The better agreement shown by curve 1 is attributed to the fact that both instruments were tied together whereas, in the case of curve 2, the balloon was often carried away by winds up to 50 miles from the airplane at the upper levels.

In curve 1 temperature comparisons down to about −50 degrees centigrade are averaged, while in curve 2 the comparisons include data down to −30 degrees centigrade.

Similar data for the accuracy of measurement with the electrical hygrometer cannot be presented because of the lack of a standard for comparison. As nearly as can be determined on the basis of checks against a psychrometer in a controlled chamber, the electrical hygrometers agree within about three per cent relative humidity at temperatures down to about −15 degrees centigrade. Comparisons with hair hygrometers carried in airplanes show reasonable agreement down to temperatures at which the hair becomes sluggish (about −5 degrees).

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Lightning Protection of Wood-Pole Lines

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Synopsis: The reliability of wood-pole transmission lines during the lightning season has long been a problem with operating companies. To meet the ever increasing service demands approximately 60 miles of line were reconstructed using the inherent insulation of a wood-pole line with lightning protector tubes on one of three conductors. Two years' experience with this type of construction has shown better than a 90 per cent reduction in lightning outages.

A REVIEW of the past nine years' performance on the 26-kv wood-pole lines of the Public Service Electric and Gas Company indicated that approximately 79 per cent of all interruptions were caused by lightning which represented about 38 lightning tripouts per 100 miles of line per year. This compared to a rate of 3.1 for 132-kv steel-tower lines.

After unsuccessful attempts to improve the lightning performance of wood-pole lines by the use of ground wires or wood insulation, lightning protector tubes were installed on a single-circuit 26-kv line. This line is part of a three-phase system with the neutral grounded through a 75-ohm resistor which limits the single-phase-to-ground fault currents to somewhere between 200 to 500 amperes. Since the three-phase short-circuit currents are as high as 10,000 amperes it was impossible to meet the range of currents between single-phase and three-phase faults with a single tube and a four-tube scheme would be necessary.[1] In order to reduce the cost, tubes were selected having a 60-cycle current rating of 200–1,000 amperes.

In the absence of any records at the time it was thought that the number of phase-to-phase operations would be very small. Experience showed that many of the lightning strokes involved more than one phase, resulting in the failure of a number of tubes due to the high phase-to-phase short-circuit currents. Although the number of outages was not decreased with this scheme there was very little damage to any of the transmission-line equipment and the time of outage was decreased materially.

This three-phase installation was tried for two seasons with no reduction in the number of lightning outages. In 1937 the line was rebuilt to make one phase a shielding conductor by locating it so that the remaining phases would be shielded from direct strokes. Lightning currents were drained from the shielding conductor by means of lightning protector tubes. The remaining phases were isolated from the shielding conductor and tube ground circuit by insulation sufficient to prevent phase-to-phase faults. This allowed the use of a single tube designed to withstand only phase-to-ground currents which were much less than phase-to-phase currents.

The performance of this line during the 1937 lightning season indicated that the method of protection was satisfactory but that the tubes used (15,000 to 30,000-ampere impulse current rating) would not withstand the lightning currents encountered. This conclusion was based upon the large number of tubes that failed on single-phase line outages with no phase-to-phase currents involved.

Table I. Wood-Pole Lightning-Protection Performance

	1938	1939
Miles of line (circuit)	18.9	59.4
Number of tubes installed	341	830
Tripouts	1	0
Intermediate poles flashed	0	0
Shielded phases flashed	0	0
Tube insulator flashed	1	0
Tubes destroyed	1	0
Tubes operated but failed to protect	1	0
Tubes operated correctly	103	108
Total number of tubes operated	105	108

In 1938 new tubes of a greater lightning-current rating (100,000 amperes) were installed. In addition, two more lines were rebuilt to the same type of construction, making a total of 18.9 cir-

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[1] For all numbered references, see list at end of paper.

Table III. Comparison of the Lightning Performance of Protected and Unprotected 26-Kv Wood-Pole Lines in the Same Lightning Area

	1938		1939	
	Unprotected	Protected	Unprotected	Protected
Miles of line (circuit)	48.7	18.9	53.3	59.4
Lightning tripouts	13	1	13	0
Minutes interruption	19	1	266	0
Tripouts per 100 miles	26.7	5.3	24.4	0
Minutes interruption per 100 miles	39	5.3	500	0
Reduction in outages with lightning-protection construction (per cent)		80.3		100
Reduction in minutes interruption with lightning-protection construction (per cent)		86.4		100

cuit miles. In 1939 an additional 40.5 circuit miles of line were rebuilt.

Performance

The performance of these lines during the 1938 and 1939 lightning seasons is given in tables I and II.

Table I shows the operating record, by years, of the lines rebuilt for lightning protection. The absence of intermediate-pole and shielded-phase flashovers indicates that the method of application is very satisfactory; also the very high per cent correct tube operation shows that the current rating of the tubes as now used is apparently satisfactory.

Table II is a comparison of the lightning performance of these lines before and after lightning protection was installed.

In order to eliminate the effect of variation in lightning severity in different territories and years, table III was made up to show the comparison between standard and protected wood-pole lines. By putting the lines on a common basis the operating records for the years 1938 and 1939 are compared for unprotected and protected lines in the same territory. The comparison shows a reduction of about 80 per cent for 1938 and 100 per cent for 1939 in the number of lightning outages and time of interruption.

The construction used followed the original design[2] which showed an estimated reduction in lightning outages of approximately 75 per cent. Although two years' experience indicates a greater reduction in outages it is expected that a variation in intensity and number of storms will result in less improvement than the 100 per cent obtained this year.

Table II. Comparison of Transmission-Line Performance Before and After Installation of Lightning Protection

	Before	After	
	1930–37	1938	1939
Circuit miles	63.3	18.9	59.4
Total thunderstorm line days*	3,180	150	215
Total lightning outages	204	1	0
Tripouts per 100 miles per yr	36.6	5.3	0
Tripouts per storm line day	0.64	0.0067	0
Tripouts per storm line day per 100 miles	1.02	0.035	0
Reduction in outages per 100 miles per yr by protection (per cent)		85.5	100
Reduction in outages per storm line day by protection (per cent)		98.9	100
Reduction in outages per storm line day per 100 miles by protection (per cent)		96.6	100

* An index of storm exposure which is the sum of the number of thunderstorm days as determined for each line individually.

Design

The design of lightning protection of low-voltage transmission lines incorporating high insulation strength and a shielding phase conductor with a lightning protector tube was presented in an earlier paper.[3] In brief, this system of protection is accomplished as follows:

1. One phase of each circuit is made a shielding conductor by locating it so that the remaining phases will be shielded from direct strokes.

2. Lightning currents are drained from the shielding conductor by means of a lightning protector tube.

3. The remaining phases are isolated from the shielding conductor and tube ground circuit by insulation sufficient to prevent phase-to-phase faults. This allows the use on each structure of a single tube designed to withstand only phase-to-ground currents which, in general, are much less than phase-to-phase currents.

Lightning Protector Tubes

One of the advantages of this type of protection co-ordinated with the neutral-grounding impedance is that the variation in operating conditions and future system growth and its effect upon the currents to which the protector tube may be subjected may be neglected. This is not the case when protector tubes are installed on all three phases. The lines upon which these tubes were installed are a three-phase system nominally rated 26.4 kv with the neutral grounded through a 75-ohm resistor. In one network there are two neutrals operating. In many cases if the protector tubes were to be applied to all three phases it would have been necessary to use four protector tubes where three protector tubes of a high current rating are connected in star and the star point connected to ground through a protector tube of lower current rating.[1]

In addition, where the line is long or the system is extensive, the short-circuit currents of the lines may vary widely and zoning the tubes would have been necessary had they been installed on all three phases.

Since the single-phase-to-ground fault current is limited to a value of somewhere between 200 and 500 amperes, a tube was selected having a 60-cycle current rating of 200 to 1,000 amperes and an impulse discharge current rating in the order of 100,000 amperes. These tubes were used throughout the 26-kv system for all single-phase lightning protection. In order to have the tube function properly with the system under consideration the minimum internal tube gap permitted by the manufacturer was $6^1/_4$ inches.

In order further to decrease the cost of protection a simplified tube was used. It was felt that the life of protector tubes is dependent upon weathering of the fiber and erosion of the bore during operation and that expensive hardware was not warranted. For this reason a tube was designed using standard galvanized pipe fittings for hardware and a nail electrode. Five years' experience with tubes of this type has not shown any reason for a change to more expensive hardware.

Impulse tests using standard ($1^1/_2$x40-microsecond) positive and negative waves were made on each pole top, with and without the tube electrode in place, in order to obtain volt-time breakdown curves of the pole-top assembly and tube as installed in the field. It was found that with 45-kv pin-type insulators without wood in series there was not sufficient margin between tube breakdown and insulator flashover for all points on the curves and that in some cases the curves actually crossed. This was verified by observation when the insulator flashed over without tube breakdown. Since it was necessary in rebuilding some of the lines to increase the height of the poles with a steel extension bracket, it was not practical to insulate the tube mounting and ground from the insulator pin. It was found that a shield ring installed on the tube would lower the impulse breakdown of the tube, so that within practical limits, all points on the curves were at least ten per cent below the minimum breakdown values of the shielding phase. In practice shield rings were used on all tubes where the mounting was such that there was less than six inches of wood in series with the insulator and a grounded connection.

Figures 1 and 2 are typical volt-time breakdown curves of the single- and double-circuit pole tops respectively. These are composite curves made from several sets of test data and show the margin of protection between the tube and the shielding-phase breakdowns.

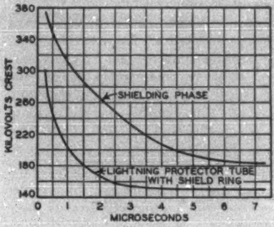


Figure 1 (left). Volt-time breakdown curves, single-circuit protected pole, $1^1/_2$x40-microsecond positive wave

Figure 2 (right). Volt-time breakdown curves, double-circuit protected pole, $1^1/_2$x40-microsecond positive wave

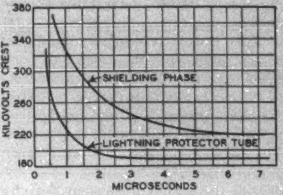


Figure 3. Single-circuit pole top, protected pole

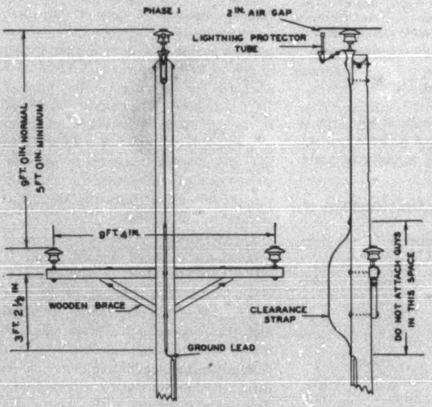
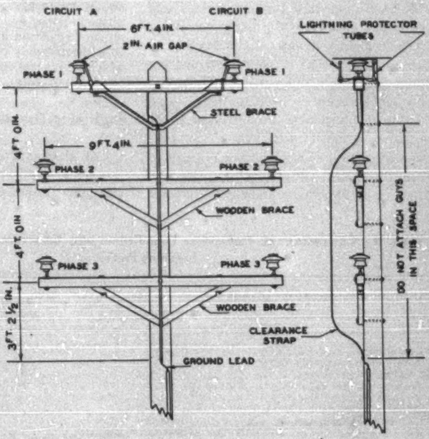

PHASE 1
2 IN. AIR GAP
LIGHTNING PROTECTOR TUBE
9 FT. 0 IN. NORMAL
5 FT. 0 IN. MINIMUM
9 FT. 4 IN.
3 FT. 2½ IN.
WOODEN BRACE
GROUND LEAD
CLEARANCE STRAP
DO NOT ATTACH GUYS IN THIS SPACE

Figure 4. Double-circuit pole top, protected pole


CIRCUIT A
CIRCUIT B
6 FT. 4 IN.
2 IN. AIR GAP
PHASE 1
PHASE 1
STEEL BRACE
4 FT. 0 IN.
9 FT. 4 IN.
PHASE 2
PHASE 2
WOODEN BRACE
4 FT. 0 IN.
PHASE 3
PHASE 3
WOODEN BRACE
3 FT. 2½ IN.
GROUND LEAD
LIGHTNING PROTECTOR TUBES
CLEARANCE STRAP
DO NOT ATTACH GUYS IN THIS SPACE

Construction

An attempt was made to keep as much similarity as possible between protected and unprotected poles from both construction and appearance standpoints. It was also thought desirable to have a single-circuit pole that could be expanded to a double-circuit pole with only minor construction changes. In order to obtain the most economical construction the insulation between the shielding or struck conductor and its ground lead was coordinated with each of the shielded conductors, and to ground, for the various pole-top assemblies.

In order to conserve space in this paper photographs of the actual construction are not included. It was felt that drawings giving dimensions and other information would be far more valuable.

Figure 3 shows the assembly of a single-circuit protected pole top. It will be noted that the tube ground was brought down the pole by means of a clearance strap in order to increase the insulation between the tube ground and the unprotected phase wires which in turn increased the permissible distance between protected poles.

The unprotected pole is similar to figure 3 except that the tube, mounting bracket, and ground lead were eliminated and all grounds such as guys were attached at least two feet below the bottom crossarm.

The arrangement of the double-circuit protected pole top is shown in figure 4. Again the unprotected pole is similar except that the tubes, mounting brackets, and ground lead were eliminated with the highest guy connection two feet below the bottom arm. In addition a wooden crossarm brace was substituted for the steel brace shown on the top arm.

Location of Protector Tubes

The duty of a lightning protector tube is, in the event of short-time overvoltages, to prevent flashover between phases and flashover to ground, other than through

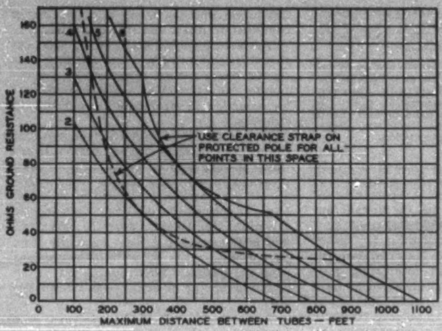


Figure 5. Curves showing required location of lightning protector tubes, single-circuit pole line

Numbers on curves are minimum distance in feet of guy below bottom arm on unprotected pole

the tube. It must accomplish this protection on the structure upon which it is installed, called the "protected pole," and on adjacent structures called "unprotected poles."

This protection is dependent upon the insulation of the various structure assemblies, the resistance of the tube grounds, and the distance between the tube installations. These conditions were taken into account, both for the protected and unprotected poles, in determining the required location of tubes as given on the following curves.

Figure 5 is for a single-circuit line and shows the allowable distance between protected poles as dictated by the ground resistance of the tube grounds and the location of guy attachments. Since guy locations are determined by local field conditions a family of curves is included to provide for the pole in the section, between protected poles, having the least insulation to ground. As mentioned previously, the clearance strap is installed on the protected pole in order to increase the insulation between the tube ground and the protected phase wires. This strap is not always necessary as indicated by the dotted line on figure 5. For example no clearance strap is needed if the ground resistance is below 24 ohms or if there is an unprotected pole in the section which requires a guy as close as two feet below the bottom arm.

Similarly figure 6 gives data for a double-circuit line based upon the use of a clearance strap on all protected poles. Because of the protected-pole limitation there is no advantage in attaching guys on the unprotected poles more than two feet below the bottom arm.

Two years' operating experience with this type of lightning protection has confirmed the conclusions given in the paper presenting the original design.[2] At the present time it is planned to rebuild an additional 90 circuit miles in 1940 to be in operation for the coming lightning season so that further experience will be obtained.

References

1. Protector Tubes for Power System, H. A. Peterson, W. J. Rudge, A. C. Monteith, and L. R. Ludwig. AIEE Transactions, volume 59, 1940 (May section) pages 282-92.

2. Lightning Protection for Transmission Lines, A. W. Gothberg and A. S. Brookes. Electrical Engineering (AIEE Transactions), volume 56, January 1937, pages 13-16.

Discussion

E. Piepho (The Detroit Edison Company, Detroit, Mich.): The authors have presented a good case for lightning protection on wood-pole transmission lines using protector tubes on a high-shielding phase wire. However, reduction of lightning tripouts alone will not justify the expenditure for this type of protection in all cases. Other factors such as maintenance expense, the number of actual service interruptions occasioned by lightning tripouts as compared with tripouts due to other causes, duration of service interruption, etc., must be fairly evaluated. In cases where the line serves as a tie in the transmission network, its opening will not necessarily result in a service interruption and is, therefore, of small consequence if it can be immediately reclosed. Generally speaking, lightning damage does not prevent reenergizing the line within a minute or less, so that even when service interruption occurs it is of short duration. Interruptions due to wind, trees, equipment failure, or public interference, while not very frequent, are much more likely to hold the line out for a considerable time.

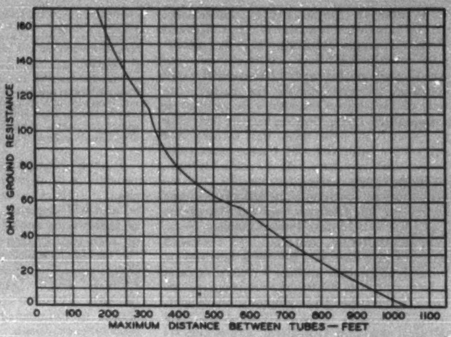


Figure 6. Curves showing required location of lightning protector tubes, double-circuit pole line

On The Detroit Edison Company 24-kv system the five-year operating record for 1,000 miles of overhead lines shows an average of 28.5 lightning tripouts per 100 miles per year. Of this number, 10.5 lightning tripouts per 100 miles per year caused service interruptions of five minutes or less, and only one caused a service interruption exceeding five-minutes' duration. This latter figure compares with 1.4 service interruptions exceeding five minutes due to other causes previously mentioned. It is doubtful whether tube-arrester protection can be justified for the entire system to eliminate so few service interruptions due to lightning, although in the case of a few lines the application of protector tubes appears to be a good investment. Three such lines were equipped with protector tubes in 1939, the selection having been based on line maintenance cost due to lightning, and the importance of the service supplied by the line.

In addition to this protector-tube application, tube arresters were installed on 24-kv pole-top switches in 1938. One-hundred-forty switches were selected for this protection, the selection having been determined by the past operating record of these switches with respect to lightning damage. During the 1939 storm season this protection functioned at an average rate of 1.3 times per switch protected. Only one switch suffered damage due to failure of the tube arresters. This was caused by failure of the protector tube to clear follow current due to excessive tube erosion, which may have been caused by multiple or continuing lightning strokes.

These tubes were inspected last autumn and several removed because of excessive erosion. It appears that isolated installations of protector tubes such as these result in more rapid erosion than has been reported for installations of tubes distributed along a line.

Two protector tubes were found with their bores partly plugged by mud and foreign material such as used by wasps. It is possible that such obstructions may account for the ruptured tubes reported by some users.

H. P. Sleeper (Public Service Electric and Gas Company, Newark, N. J.): Because of the economics involved, open-wire wood-pole transmission-line construction has constituted in the past, and probably will continue to constitute in the future, the bulk of high-power transmission systems in this country. For that reason the lightning protection of such lines has formed the subject of innumerable papers presented before this Institute. It is probably safe to predict that means for combatting this problem will continue to accumulate in the records of this Institute and the files of the United States Letters Patent Office.

It is, therefore, refreshing to review a paper which does not invent a new device but which merely applies an old one in a novel manner to secure improved operation. Such is the subject of the paper on "Lightning Protection of Wood-Pole Lines" by Messrs. Sels and Gothberg, and if the results quoted are any indication of the expected performance, it would seem that the operating results should be meritorious and the economics very favorable.

A study was made by this discusser in connection with the transmission system described by the authors to determine whether the scheme described in this paper, or a Petersen coil, should be applied to improve operation. A study of faults was made from oscillographic data obtained over a considerable period of years. The type of transmission construction involved consisted of a triangular arrangement of phases with two phase wires on top and a 36-inch spacing.

Obviously this construction favored the occurrence of short circuits due to lightning. The cable portion of the complete transmission system constituted 30 per cent of the total mileage. Results over a five-year survey showed that 80 per cent of all faults occurred on the open wire and bus sections of the system, and 20 per cent on the cable section. Furthermore, only 21 of the 80 per cent open-wire and bus faults started to ground. As a matter of interest, 16 of the 20 per cent cable faults started to ground. Hence it was apparent that the 21 per cent of all faults was all that could be eliminated by any scheme of ground-fault extinction. Hence if 90 per cent correct operation were granted to either Petersen-coil operation or expulsion-tube operation, it would still leave 80 per cent plus faults to be relayed out. On this basis the installation of a Petersen coil was not considered economically justifiable. On a program of gradually rebuilding the transmission lines, however, the preferred construction as shown in the authors' paper was practically as cheap as any other and the full effect of the expulsion-tube operation could be obtained at once on the rebuilt lines. On that basis the protective-tube scheme was approved.

This scheme of open-wire transmission construction has distinct advantages:

1. All single-phase-to-ground faults should be extinguished in not more than one cycle of arcing time. In this feature it should compare directly with the operation of a Petersen coil.

2. It should not affect in any way the operation of existing protective relays on the lines. If relays are in use which will respond to short-circuit impulses of one cycle or less, as is the case on the system in question, it will be necessary to delay the tripping of the breaker for that length of time to avoid incorrect operation during the time of tube operation. A scheme for doing this was described by me in the past and is published. (See page 292, ELECTRICAL ENGINEERING, 1938.) The same is true of a Petersen-coil installation.

3. With a high-impedance system neutral in use, as the 75 ohms in the neutral of the system described by the authors, the duty on the tubes does not increase with changing generating capacity in service due to time of day or changing load growth with the years. To a certain degree only, this is also true of a Petersen-coil installation.

Probably the greatest contribution to the continuity of service to the customers of electric utilities today and in the near future is, and will continue to be, the installation of fault-extinguishing devices of some kind on high-voltage transmission lines. Today the two types which have been compared in these comments present interesting possibilities, and existing systems, and particularly systems constructed hereafter, should be capable of capitalizing on their proved capabilities. If a choice must be made as to which system to employ, a study of each individual case seems to be the only answer. If existing line construction is considered satisfactory, or if major reconstruction is not to be made, the Petersen coil will, in this commentator's opinion, prove to be the cheaper to install and maintain. However, it is this writer's opinion that serious consideration should be given to the installation of both types of protection on a given system for the following reasons:

1. The duty on the expulsion tubes would be reduced and longer life should result.

2. Smaller tubes could be used on high capacity systems because of the reduced recovery voltages.

3. Ground faults on busses would be extinguished, which is not accomplished by tubes. This should make the installation of bus differential relay protection unnecessary, except on the very most important busses, since almost all bus faults originate to ground. The economics of this possibility may be paramount.

L. E. Merrow (Rockland Light and Power Company, Nyack, N. Y.): The Rockland Light and Power Company, after considering protection to about 90 circuit miles of 33-kv delta transmission lines of both wood-pole and steel construction, installed a Petersen coil last spring. The territory covered by these lines is north of Public Service Electric and Gas Company, which is above the New Jersey-New York border and has about the same amount of lightning.

Since the coil was installed over 60 per cent of the line disturbances were caused by lightning and 52 per cent of these were extinguished by the action of the coil. The remainder of the disturbances causing trip-outs were either phase-to-phase or two-phase-to-ground. Since some of the wood-pole lines have flat construction, many of the phase-to-phase short circuits occurred on these lines. It is possible, however, that if these particular lines had been protected by rebuilding each pole as suggested in Mr. Sels' paper, the operation of the coil might have been more satisfactory.

F. Von Voigtlander (The Commonwealth and Southern Corporation, Jackson, Mich.): The authors, following the earlier paper on this subject by A. W. Gothberg and A. S. Brookes, present interesting and valuable data on the development of the idea of converting one conductor of a line into a combination phase and shielding wire.

The writer would like to ask to what extent this combined phase and shielding conductor has provided direct-stroke protection for the line such as might be evidenced by reduction in structural damage due to lightning.

The double-circuit construction shown in Figure 4 appears to employ quite conventional spacing and is similar to the design proposed in the 1937 paper. The single-circuit construction as shown in figure 3, however, places the shielding conductor at a considerable height above the other two and in this respect departs markedly from the conventional as well as from the previous proposed design. What factors led to the adoption of the present single-circuit design?

Whenever the use of the conductor as a combination shield wire is proposed, the treatment of the transpositions must be given consideration if the possibility of two-phase-to-ground faults is to be avoided. Various schemes have been proposed to meet this problem. What features have the authors incorporated in their design to take account of the presence of transpositions in the protected line?

E. J. Allen (General Electric Company, Pittsfield, Mass.): The paper by Messrs. Sels and Gothberg is a valuable addition to the field experience with protector tubes, particularly since it describes a substantial application of protector tubes on the top phase or phases only.

This scheme of protection is fundamentally the same as that for the overhead ground wire, since under impulse conditions the top protected phase acts as an overhead ground wire by discharging through protector tubes to ground. Hence, the same fundamental requirements apply as to adequate shielding to unprotected conductors, low ground resistance, and sufficient clearance at mid-span, which govern overhead-ground-wire practice.

Since other operating companies may wish to consider lightning protection of lines by protector tubes on top conductor only, the curves figure 1 of this discussion have been prepared to show the allowable stroke current (two microseconds to crest) through different ground resistances at the protected structure with various impulse flashover values from ground wire to shielded phase conductors. This is based upon the flashover formula[1] described in the appendix of this discussion. A coupling factor of 0.22 was used which appears to be reasonable for the cases presented by the authors, and the relatively negligible effect of normal-frequency voltage was neglected.

Table I was based on 250-foot separation of protector-tube installations, corresponding to tubes on the top phase only at every structure if the span length is 250 feet; or a tube at every other structure if 125-foot spans are used. With the 250-foot separation of protector tubes and two-microsecond lightning-current front, reflections from adjacent protected structures will reduce the voltage and if flashover to shielded phase occurs, it will occur at approximately two microseconds for a stroke between protected structures and at

2 to 3.5 microseconds for stroke to protected structure.

Figure 1 enables analysis under two conditions:

(*a*). Stroke between protected structures

(*b*). Stroke to protected structure

The curves, figure 1, show the advantage of wood crossarm braces and the clearance straps on lower phase (or phases) of protected structures to increase the impulse insulation as described in the paper. For example, for stroke to the protected structure (solid curves) with currents not over 30,000 amperes, an increase in the insulation flashover of shielded conductors from 200 to 400 kv permits ground resistances of 25 ohms instead of 10 ohms. If the flashover level is but 200 kv and ground resistance 25 ohms, the protection would be had for lightning currents up to only 15,000 amperes.

Where the advantages of protector tubes having high discharge current capabilities of 100,000 amperes are to be fully realized, both the effect of high insulation of shielded conductors and low grounding resistance must be utilized if protector tubes are applied to top phase or phases only. For example, for strokes to protected structure of 100,000 amperes, the ground resistance required to prevent flashover of 400 kv lower-phase insulation is 5.5 ohms.

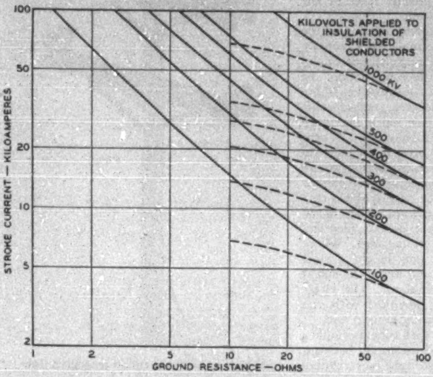


Figure 1. Effect of stroke current upon potential applied to insulation of shielded conductors for various ground resistances of protected structure

Two-microsecond front, protected structures 250 feet apart

Solid curves—Stroke to protected structure

Dashed curves—Stroke between protected structures

The dotted curves, figure 1, show the similar relationships when a stroke occurs between protected structures. As the authors point out, where intervening unprotected structures do exist, use should be made of available wood insulation between line insulators and ground, eliminating any grounded objects at the top of the structure, as for example, guy wires or down leads to ground. For example, a structure of the type shown in figure 3 of the paper not having a protector tube or a down lead to ground would have a higher flashover voltage *V*(*t*) owing to the longer flashover path from top phase to shielded phases. This voltage (dotted curves) should be used in figure 1 when considering the stroke as occurring between protected structures. Where there are no intervening unprotected structures, as for example, a protector tube at each structure, the impulse voltage to cause mid-span flashover corresponds to the air strike distance between conductors at mid-span.

In the example previously considered, where the shielded conductors of a protected structure had a line-to-ground flashover of 400 kv and a ground resistance of 25 ohms, an intervening unprotected structure without down leads may have a line-to-line flashover of 500 kv or more. If the flashover level is 500 kv, the dotted curve shows protection would be afforded the shielded conductors up to 28,500 amperes for 25-ohm ground resistance as compared to 30,000 amperes permissible stroke current to protected structure. By increasing the insulation level between protected structures, this has the advantage of making the line capable of withstanding stroke currents of the same order of magnitude at mid-span as at the protected structure.

The maximum stroke currents which can be permitted at and between protected structures, obtained from figure 1, can be referred to a curve showing frequency of occurrence of various lightning current magnitudes in service.[2] From this the percentage of strokes which the line can handle without flashover can be obtained.

In the two cases investigated, strokes between and at protected structures were considered separately and methods given for estimating them. Actually, however, the stroke may hit the line at any point. Since the nature of the problem hardly justifies a separate investigation for each point on the line where the lightning may strike, it can be assumed, on the average, any stroke within a quarter of the distance of separation between protected structures will be equivalent to a stroke at the structure with the solid curves figure 1 applying, while strokes within similar distance from a mid-point between protected structures are evaluated by the dotted curves, figure 1.

Where protector tubes are applied on all three phases, the protection afforded is independent of ground resistance, the latter values are important only in so far as they affect the single line-to-ground power follow current during tube discharge, and the selection of the minimum-current interrupting rating of the protector tube.

Table I of the paper shows in 1938, 105 operations of the 341 installed tubes were recorded, or 30.8 per cent. The tube operations were 5.7 per mile per year. In 1939, 108 operations of 830 tubes installed were recorded, or 13 per cent, corresponding to only 1.8 operations per mile per year. The total thunderstorm line days, however, had increased to 215 (1939) as compared with 150 thunderstorm line days during the preceding year. The variability between the number of operations of protective devices and the number of thunderstorm days is not unusual. It may be partially accounted for, however, if the storms experienced in 1939 were localized over a small zone of the 59.4 circuit miles of line where protector tubes were installed.

APPENDIX

The flashover formula derived by Bewley is:

$$V=(1-C_n)aR'I+e$$

in which

V = voltage across the insulation

C_n = coupling factor (depends upon the number, configuration, and spacing of the conductors, and is modified by corona)

a = crest factor (depends upon the length of spans or the distance between protector tubes, structure footing resistance, lightning wave front, and conductor surge impedance)

R' = effective resistance (depends upon the grounding resistance, and the surge impedance of the lightning stroke and of the stricken conductors)

I = lightning current which the stroke could deliver to a zero-resistance ground (which is equal to twice the traveling-wave current in the stroke)

e = effective instantaneous normal-frequency voltage (depends upon the normal-frequency polarities and the conductors involved)

REFERENCES

1. PROTECTION OF TRANSMISSION LINES AGAINST LIGHTNING: THEORY AND CALCULATIONS, L. V. Bewley. *G. E. Review*, April 1937, pages 180–8.

2. LIGHTNING INVESTIGATION ON TRANSMISSION LINES—VI, W. W. Lewis and C. M. Foust. ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), January 1937, page 101, figure 1.

H. N. Ekvall (Philadelphia Electric Company, Philadelphia, Pa.): The paper by Messrs. Sels and Gothberg describes a very interesting application of protector tubes to 26-kv wood-pole lines and indicates that it has effected a reduction in line outages of 80 per cent and better. It should be noted, however, that in applying this scheme the insulation strength of the wood-pole lines was materially increased by changing to high ridge pin and twin vertical wire configurations, replacing metal with wood braces and relocating guys.

Experience on approximately 600 miles of 13- and 33-kv wood-pole lines of the Philadelphia Electric Company during the past five years indicates that such increases in the wood insulation strength alone (without application of protector tubes) can effect large reductions in trouble. For example, on 13-kv lines, changeover from flat-top to ridge-pin configuration, relocation of guys, and use of wood braces to take better advantage of the wood-pole insulation have been largely responsible for an 89 per cent reduction in line interruptions per 100 circuit miles during the period 1935 to date. Similarly good performance has been obtained on the 33-kv lines.

The principle of protection employed by the Philadelphia Electric Company is to minimize the formation of power arcs following lightning flashover by increasing the length of wood insulation between phases and between phase and ground up to certain minimum values. This principle of protection has proved effective as indicated above. Power arcs are the chief source of trouble, while lightning itself seldom causes more than slight splintering on the pole or crossarm.

Based on the Philadelphia Electric Company operating experience, it is estimated that the protected 26-kv lines referred to in this paper would have shown a reduction in outages of the order of 80 per cent due to their increased insulation alone, with protector tubes and ground wires omitted. It appears, therefore, that much of the benefit to line performance probably comes from the increased insulation and that a small improvement may come by addition of the protector tubes.

The authors indicate that it is planned to rebuild an additional 90 miles of line in 1940 with increased insulation and protector tubes. In order further to verify the benefits of wood insulation referred to above, it is suggested that on some of the rebuilt lines the ground wires and protector tubes be omitted. This will permit a comparison of the performance of high insulation with and without protector tubes.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Messrs. Sels and Gothberg are to be congratulated on at least two counts. Two years ago one of the authors in collaboration with Mr. Brookes published a paper in which the use of one phase wire equipped with tubes was proposed as a ground wire for the circuit. This in itself was a departure from usual practice, and the authors are to be complimented for their initiative in risking the innovation. But further than this, they made calculations two years ago and predicted a reduction of 75 per cent in lightning outages. This was a bold statement at the time. That it was carefully considered and based on sound logic is indicated by the fact that their predictions have more than been fulfilled. We note, however, that now they are more conservative and are careful to state that they do not expect the 100 per cent record to continue. It would add to the completeness of the paper and thus to its value, if the data and premises on which the curves of figures 5 and 6 are based were reproduced also.

The first paragraph mentions about ten times as many line tripouts on the wood 26-kv as on the steel 132-kv circuits. The ground-wire protection on the 132 kv, no doubt, makes much difference. The paper illustrates that a high degree of immunity to lightning outage can be achieved either by proper ground-wire or tube application.

This paper and the one of two years ago together constitute a good example of the solution of a lightning problem by applied engineering.

G. E. Dean (Public Service Electric and Gas Company, Newark, N. J.): In discussion of the paper by Messrs. Sels and Gothberg, I wish to present some of the aspects of field application.

The 26-kv wood-pole transmission of the Public Service Electric and Gas Company contains many miles of single-circuit single-crossarm flat-configuration construction. In order to adapt the new configuration where one phase wire is elevated, considerable rearrangement of the pole top is necessary in addition to the relocation of guys. Where sufficient pole height is present the pole-top pin is used. With this construction, where there is no wood insulation between the insulator pin and the tube base, a shield ring is used to lower the breakdown voltage of the tube. The intermediate pole is identical but is without tube or ground wire. Where there is not sufficient pole height a steel pole extension is used and again the shield ring for the tube is required. The intermediate pole is again identical with the omission of the tube and the ground lead.

The shield ring used with tube assemblies where there is no wood insulation between the insulator pin and the tube base reduces the breakdown voltage of the $6^{1}/_{4}$-inch internal gap by redistributing the electrostatic field around the tube. The ring design is simple and rugged, consisting of three legs to support the ring; these legs in turn being welded to a large washer base. The washer of the shield ring fits over the pipe nipple at the base of the tube so that the ring assembly is slipped over the tube base and the tube installed in the normal manner.

Existing double-circuit poles are of either the two-crossarm triangular configuration or the three-crossarm vertical configuration.

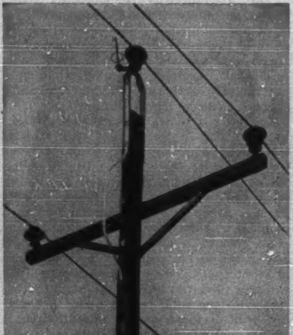


Figure 2. Single-circuit protected pole

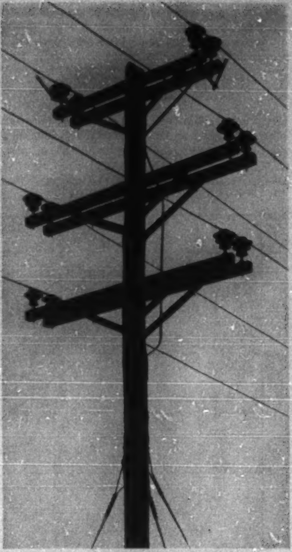


Figure 3. Double-circuit protected pole

Since the new construction is of the three-crossarm type considerable rearrangement may be required. Side guys may be attached to the end of the top crossarm when tubes are used or, in all other cases, must be well below the bottom crossarm. Either the tube or the intermediate pole may of course be double armed for strength requirements without affecting the protection. During the work of the last few years, poles of insufficient height have been replaced with a higher pole but design and test work has been completed on a joint which will permit a wood-pole section supporting one crossarm to be spliced on top of an existing pole. Much of this work will be done during the 1940 program.

Where a tube is installed on a heavy angle pole using suspension type insulators it is necessary to increase the number of insulators on the lower phase wires and attach the guys either to the top of the pole at the tube base or well below the bottom phase. Where no tubes are installed on the corner pole the number of insulators on all phases is increased and the guys must be insulated.

The simplified tube mentioned in the paper consists of the horn fiber tube, itself consisting of concentric tubes of suitable outside diameter to accommodate one-inch pipe threads and the inside, or bore diameter, slightly over one-quarter inch as determined by the 60-cycle phase-to-ground current. The internal gap length is established by choosing a nail of the proper length to give $6^{1}/_{4}$ inches between the end of the nail and the washer at the grounded end of the tube. This internal gap length is of course determined by the flashover characteristics of the insulator to be protected and the recovery-voltage characteristics of the system on which the tube is used. The base of the tube, which is grounded, consists of a straight threaded pipe nipple which is inserted in a hole punched in the mounting strap. A lock washer and nut or nut and lock nut on the nipple below the strap completes the assembly.

It is not expected that this simplified tube will have a life, when exposed to the weather, equal to that of the more elaborate designs. However, the development of the expulsion gap is far from finished and designs have changed so materially that it is not felt necessary to adopt a design of long expected life. The installation of the tubes is made in such a manner that warping of the tube cannot close the external gap and result in the burning of the tube by power current and the tripout of the circuit. It is expected that these tubes will be repainted each three years during the regularly scheduled pole-top inspection.

H. K. Sels and A. W. Gothberg: The authors value the discussions offered and will attempt to close the paper as briefly as possible.

Edward Beck suggests that the data and premises on which the curves of figures 5 and 6 are based should be reproduced also. To include this information would be merely a repetition of a previous paper, reference 2 of the present paper. There have been no changes in the values used as can be borne out by figure 8 in the original paper which corresponds to figures 5 and 6 of this paper. Additional curves were added to figures 5 and 6 to provide for various locations of guy attachments on the poles and also to take advantage of the additional insulation provided by the clearance strap.

The economics of lightning protection is questioned by E. Piepho. It is not suggested that all electric operating companies should rebuild all of their lines to provide this type of protection, but it is felt that if lightning is any problem at all, new lines when constructed can be built to accommodate this protection and the tubes added at any time.

Interruptions of any kind are a hard thing to evaluate. Since transmission is one cog in the electric-utility field there are many associated benefits with this type of protection, such as a reduction in the duty of terminal equipment by lessening the number of tripouts, reducing the number of dangerous overvoltages in stations and adjacent cable sections, improved customer service and its effect on system planning. In considering the necessity of any type of lightning protection the isokeraunic level must be taken into account. There is no doubt that there are some sections of this country where an expenditure for any lightning protection could not be justified. The method of protection proposed is, in the authors' opinion, the most economical to date for the benefits derived and the justification for its economic application depends upon many considerations of the operating company.

E. J. Allen presented a curve, figure 1, showing the effect of stroke current upon the potential applied to insulation of shielded conductors for various ground resistances at the protected structure, with a given distance of 250 feet between protected structures. It will be found by referring to the paper presenting the original design (reference 2) and the results given in figures 5 and 6 of this paper, that they agree very favorably. We wish to thank Mr. Bewley for the time involved to show this agreement and it was upon his original methods that some of our analysis was based and led to the prediction of a 75 per cent reduction in lightning outages.

F. Von Voigtlander has asked to what extent this combined phase and shielding conductor has provided direct-stroke protection for the line as evidenced by reduction in structural damage due to lightning. Two years' experience, with a close examination of all pole tops, has shown no structural damage, indicating 100 per cent shielding effect. As evidence that direct strokes were encountered there were many cases of consecutive tube operations along the line. Anywhere from 5 to 11 tubes have operated in a row. In one particular case in 1938 a tube rated at 100,000 amperes surge current blew up without a line tripout, indicating power current did not cause the damage. In this case all tubes operated in a section of the line for approximately 3,500 feet. Mr. Von Voigtlander also points out that the present single-circuit pole top differs from the one proposed in the 1937 paper. The only difference shown is an additional four inches vertical separation of the shielding conductor above the other two conductors for "minimum" spacing. This change is brought about by a change in pole-top bracket, otherwise the dimensions are the same. The nine feet "normal" distance is used when a single-circuit pole may be converted into a double-circuit pole and the crossarm then becomes the lower crossarm of a double-circuit pole top by the addition of two crossarms and the elimination of the ridge pin. Reference has been made to the question of transpositions but nothing has been worked out to take them into account. Fortunately, low-voltage pole lines are usually short, as is the case of the lines involved in this instance, and no transpositions exist in the lines with this protection.

L. E. Merrow's discussion of his experience with a Petersen coil indicates the necessity of providing a drain point to ground for lightning currents when a coil is used, otherwise many phase-to-phase tripouts will result. This was pointed out in a discussion by one of the authors in AIEE TRANSACTIONS, volume 57, 1938 (December section) page 672.

H. N. Ekvall states that on 13-kv lines of the Philadelphia Electric Company the use of wood insulation has been largely responsible for an 89 per cent reduction in line interruptions and that similarly good performance has been obtained on the 33-kv lines. He suggests that we rebuild some of the lines and omit the protector tubes to show that the increased wood insulation will give an 80 per cent reduction in outages. There is no doubt that the rebuilding of any line by providing more clearances and newer equipment will eliminate many tripouts of all causes. We have tried the use of wood insulation only, exactly as Mr. Ekvall has suggested without the 80 per cent reduction in line outages and found by experience that a good number of the strokes were causing wood splintering. Since this was a source of later trouble wood protector blocks were used in order to direct the wood splintering to a piece of equipment that could be easily replaced. It was then found that these wood blocks would blow up and result in a power arc.

If induced strokes only are to be considered we would agree with Mr. Ekvall but since direct strokes have to be accounted for and putting performance on a basis of outages per 100 miles per year rather than per cent improvement, which depends upon what you start with, we estimate 18 tripouts or cases of damage per 100 miles per year with wood insulation only as compared to 3 tripouts per 100 miles per year with tubes. Generally speaking, we think Mr. Ekvall is getting his protection in the deionizing action of the wood as indicated by the splintering so that the economics of his applying tubes depends upon the damage he is experiencing.

In connection with Mr. Sleeper's recommendations that serious considerations should be given to the installation of a Petersen coil and tubes on the same system, it seems to the authors that the tubes on a Petersen-coil system are entirely unnecessary with protected-phase-wire construction. As pointed out previously, arcing gaps should be provided between the top phase wire and ground to drain the lightning currents from the line. The attention of the manufacturers is called to the necessity for such a device.

We wish to thank Messrs. Dean and Sleeper for their contributory discussions and all of our associates for their share in making this development a success.

The Characteristics and Power Requirements of Spinning Frames—II

E. A. UNTERSEE
NONMEMBER AIEE

THIS PAPER is a continuation of the investigation on the characteristics and power requirements of spinning frames presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3–5, 1939, and published in this volume of AIEE TRANSACTIONS, pages 1–4 (January section).

The original investigation involved a 348-spindle Whitin frame with $1^1/_2$-, $1^3/_4$-, 2-, and $2^1/_4$-inch diameter rings operating at various speeds, and while the analysis indicated very definite characteristics and power requirements, the tests were not sufficiently extensive to conclude that the power formula developed would hold true for larger ring sizes and different-size yarns.

Further tests were therefore conducted on a 216-spindle Whitin frame, with $2^1/_4$-, $2^1/_2$-, $2^3/_4$-, and 3-inch rings spinning 10*s* yarn, and it is the analysis of these tests and their association with the previous tests that is now presented.

In comparing these recent tests with the original tests, it is immediately evident that the $2^1/_4$-inch ring is common to both and that they do not indicate the same power requirement. It is also evident that these two tests were made with different size yarns, and therefore, the formula submitted for the power requirements in the first paper cannot apply, inasmuch as it takes no account of yarn size.

The effect of yarn size on power requirements, other conditions being the same as indicated in figure 1, is definitely a factor to be considered in establishing a formula.

In figure 2, the relationship between yarn size and yarn weight is shown, and since the weight is so small and decreases so gradually in sizes smaller than 20*s*, it is doubtful whether it will affect the power appreciably in this range. In sizes larger than 20*s*, however, the weight increases rather rapidly, and should, therefore, be taken into consideration, but probably 4*s* yarn is about the largest generally made on spinning frames.

In figure 1, which covers the $1^1/_2$-, $1^3/_4$-, 2-, and $2^1/_4$-inch rings on the 348-spindle frame, and the $2^1/_4$-, $2^1/_2$-, $2^3/_4$-, and 3-inch rings on the 216-spindle frame, the power curves have all been reduced to a 216-spindle basis for the purpose of comparison, and in this comparison we have the following data to analyze for the effect of yarn size on power:

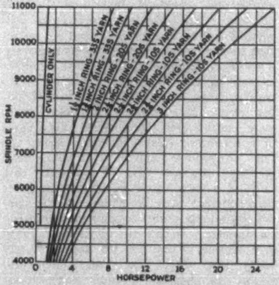


Figure 1. Power requirements of a 216-spindle spinning frame with different diameter rings and various yarn sizes, plotted from formula derived from test data

Ring Diameter (Inches)	Yarn
$1^1/_2$	33*s*
$1^3/_4$	33*s*
2	20*s*
$2^1/_4$	20*s*
$2^1/_4$	10*s*
$2^1/_2$	10*s*
$2^3/_4$	10*s*
3	10*s*

If we analyze each one of the curves separately, the power seems to vary as $(\text{rpm}/1{,}000)^{2.04}$, rpm being the spindle speed.

The spacing of these curves must depend on the ring diameter and the size of yarn, and if we consider frames on which the same size of yarn is spun and run at a fixed speed, it shows the power varies in accordance with 1.42 power of the ring diameter.

The other variable which enters the picture, namely yarn size, is evident in the power curves for the $2^1/_4$-inch rings with 10*s* and 20*s* yarn, and here the power appears to vary in accordance with

$$\frac{1}{\sqrt[4.75]{\text{yarn size}}}$$

from which we may develop the formula for power:

Horsepower per spindle =

$$\frac{0.0003055}{\sqrt[4.75]{S}} \times R^{1.42} \times \left(\frac{\text{rpm}}{1{,}000}\right)^{2.04}$$

where

S = yarn size
R = ring diameter in inches
rpm = spindle speed

Table II has been calculated using this formula. Ring sizes and yarn num-

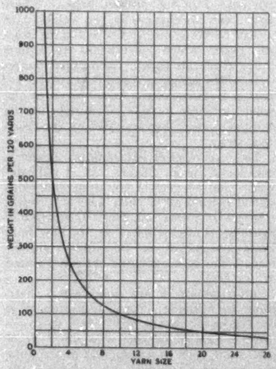


Figure 2. Relation between yarn size and weight

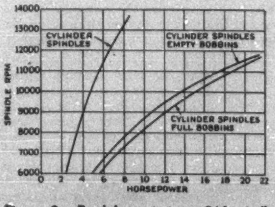


Figure 3. Breakdown test on 216-spindle frame, $2^1/_4$-inch-diameter ring

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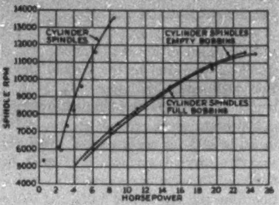

Figure 4. Breakdown test on 216-spindle frame, $2^1/_2$-inch-diameter ring

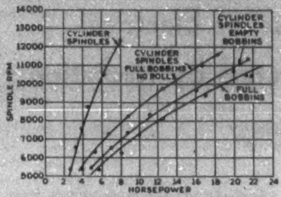

Figure 5. Breakdown test on 216-spindle frame, $2^3/_4$-inch-diameter ring

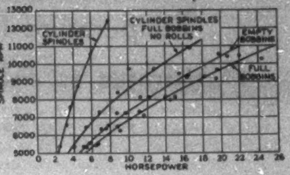

Figure 6. Breakdown test on 216-spindle frame, 3-inch-diameter ring

bers spun in cotton mills vary so widely that no attempt has been made here to cover all conditions, but the figures given will serve as a basis for the more common combinations.

Power readings were taken while spinning yarn at the empty-bobbin position, and again at the full-bobbin position for each diameter of ring, over a range of spindle speeds from approximately 6,000 rpm to 11,500 rpm, the upper range being considerably beyond the point of practical spinning, but as in the previous tests, this was necessary to establish the characteristics of the frame.

Figures 3 and 4 show the power requirements when spinning with $2^1/_4$- and $2^1/_2$-inch-diameter rings, with medium-large spindles.

The power curve for the cylinder and bare spindles is also plotted.

Figures 5 and 6 show the power requirements when spinning with the $2^3/_4$- and 3-inch-diameter rings with the large spindles. It is interesting to note on the breakdown tests on these larger ring diameters that the power required to spin yarn at the empty-bobbin position was considerably more than the power required to drive the spindles with full bobbins, and without the front rolls running. This increase in power is accounted for in the windage and friction of the balloon of the yarn and the traveler.

From the results of both investigations it shows that the cotton-spinning frame

Table I. Spinning-Frame Tests—Frame Data

Type and model—*F*2, long draft
Number of spindles—216
Type spindle—large medium 2*K*, large 4*K*
Whorl diameter—$1^1/_8$ inches
Gauge spindle—$4^1/_2$ inches, four boss rolls
Width tape—$^5/_8$ inch
Traverse—8 inches

Cylinder diameter—10 inches
Cylinder bearings—all ball bearing six-section cylinder
Ring diameter—$2^1/_4$, $2^1/_2$, $2^3/_4$, and 3 inches
Type of ring—number 2 flange
Traveler weight and number—number 10 square point, Bowen
Number yarn spun—10*s*
Yarn spun—warp-filling wind
Ply roving—two ply—1.0 hank. Draft 20. Twist per inch 16.10
Type builder—four-point filling
Thread board—traversing

Diameter front roll—1 inch
Diameter middle roll—$^7/_8$ inch
Diameter back roll—$^7/_8$ inch
Tape idler pulleys—regular; weight used, $2^1/_2$ pounds
Separators—solid aluminum
Weight on front rolls—$4^1/_2$ pounds

Gearing:
- Cylinder—26 teeth
- Jack—112 teeth
- Twist—88 teeth
- Front roll—100 teeth
- Lay—53 teeth
- Crown—140 teeth

Table II. Spindles Per Horsepower for Different Size Yarns at Various Speeds

Ring Diameter (Inches)	Spindle RPM	Yarn Size 6	8	10	12	14	16	18	20	30
$1^1/_4$	12,000							27.4	28.0	28.0
	11,000							32.9	33.6	33.6
	10,000							39.7	40.6	40.6
	9,000							49.5	50.7	50.7
	8,000							63.0	64.5	64.5
	7,000							82.7	84.5	84.5
$1^1/_2$	12,000				19.4	20.0	20.6	21.1	21.6	
	11,000				23.35	24.1	24.8	25.4	26.0	
	10,000				28.15	29.1	29.9	30.7	31.6	
	9,000				35.3	36.5	37.4	38.4	39.2	
	8,000				44.7	46.2	47.5	48.7	49.8	
	7,000				58.5	60.5	62.2	63.7	65.2	
$1^3/_4$	12,000			15.70	16.30	16.85	17.3	17.72	18.15	
	11,000			18.90	19.60	20.3	20.8	21.35	21.9	
	10,000			22.9	23.80	24.6	25.2	25.9	26.5	
	9,000			27.2	28.25	29.2	30.0	30.8	31.5	
	8,000			35.2	37.60	38.9	40.0	41.0	42.0	
	7,000			47.3	49.10	50.7	52.1	53.5	54.7	
2	12,000		11.44	12.4	12.87	13.3	13.68	14.0	14.35	
	11,000		13.78	14.9	15.5	16.0	16.42	16.85	17.27	
	10,000		17.20	18.0	18.7	19.35	19.9	20.4	20.9	
	9,000		21.46	22.5	23.3	24.1	24.8	25.4	26.05	
	8,000		27.20	28.5	29.6	30.6	31.4	32.2	33.0	
	7,000		35.7	37.3	38.8	40.0	41.2	42.25	43.25	
$2^1/_4$	11,000	11.35	12.05	12.63	13.1	13.55	13.93	14.3	14.62	
	10,000	13.70	14.58	15.25	15.85	16.4	16.85	17.25	17.7	
	9,000	17.10	18.20	19.1	19.8	20.5	21.05	21.6	22.1	
	8,000	21.70	23.10	24.2	25.1	26.0	26.7	27.4	28.0	
	7,000	28.40	30.20	31.7	32.9	34.0	35.0	35.8	36.7	
	6,000	38.90	41.40	43.3	45.0	46.5	47.8	49.0	50.2	
	5,000	56.4	60.0	62.8	65.2	67.5	69.4	71.0	72.7	

Ring Diameter (Inches)	Spindle RPM	Yarn Size 2	4	6	8	10	12	14	16
$2^1/_2$	10,000		10.81	11.78	12.53	13.13	13.64	14.10	14.50
	9,000		13.50	14.75	15.65	16.40	17.04	17.60	18.05
	8,000		17.10	18.70	19.82	20 80	21.75	22.30	22.9
	7,000		22.40	24.4	26.0	27.20	28.25	29.20	30.00
	6,000		30.80	33.50	35.70	37.40	38.80	40.10	41.20
	5,000		44.50	48.6	51.60	54.2	56.20	58.00	59.5
$2^3/_4$	10,000	8.17	9.45	10.3	10.95	11.47	11.9	12.30	
	9,000	10.17	11.76	12.82	13.65	14.3	14.85	15.34	
	8,000	12.92	14.95	16.28	17.3	18.16	18.8	19.42	
	7,000	16.95	19.6	21.3	22.7	23.8	24.7	25.50	
	6,000	24.1	27.9	30.4	32.3	33.9	35.1	36.25	
	5,000	33.7	38.9	42.3	45.1	47.2	49.0	50.60	
3	10,000	7.24	8.36	9.1	9.68	10.15	10.54		
	9,000	9.01	10.4	11.35	12.08	12.65	13.14		
	8,000	11.45	13.25	14.43	15.35	16.1	16.7		
	7,000	15.05	17.4	18.9	20.15	21.1	21.9		
	6,000	20.6	23.8	25.9	27.6	28.9	30.0		
	5,000	29.9	34.5	37.6	40.0	41.9	43.4		

Temperature Rise of Electrical Apparatus as Affected by Radiation

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ASSOCIATE AIEE

Synopsis: Radiation is generally of importance only in apparatus cooled by natural convection and radiation. If such apparatus with a smooth exterior surface is mounted in the sun and shielded from the wind, an additional rise as high as 20 degrees centigrade may occur. White or gray nonmetallic paints may decrease solar effects. Fins and corrugations increase the ratio of convected to radiated heat and thereby decrease radiant effects.

IN electrical engineering, radiation is relatively unimportant in the normal application of most apparatus, but in occasional applications it should be considered.

The subject of radiation has received considerable attention from heating and ventilating engineers.[1,2] It is always considered in computing the amount of refrigeration required for air conditioning. In this case, large areas are exposed to sunlight and small temperature differences are important. In the dissipation of heat from the human body, radiation is normally more important than convection and differences of a few degrees in wall temperature are important.[3]

In electrical engineering, motors, transformers, and similar apparatus operate at higher temperatures, so that a difference of a few degrees is not as important as in air conditioning. The effect of radiation on the temperature rise depends on the enveloping area of a device. In most devices the cooling obtained from a smooth surface is inadequate, so that finned or corrugated surfaces, forced air circulation, or water cooling are provided. Under these conditions the normal losses of the device per unit of enveloping area are large, so that the radiant effect of hot walls or the sun is small as compared to the normal losses. For such cases radiation will have a negligible effect on the temperature rise.

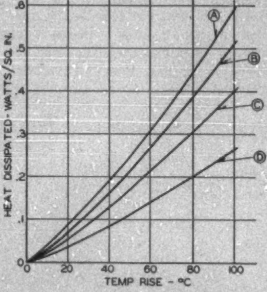

Figure 1. Heat dissipation–temperature rise curves

A—Radiation from black surface to black surrounding surfaces at 25 degrees centigrade

B—Natural convection from a large (two feet or over) surface facing upward

C—Natural convection from a large vertical surface

D—Natural convection from a large surface facing downward

However, some types of apparatus must depend for cooling on a relatively smooth external surface, and may be mounted in locations exposed to radiation from the sun or hot walls. It is the purpose of this paper to point out the relative magnitudes of these factors and to give curves which will be helpful in estimating the temperature rise under unusual circumstances where radiation is important.

Radiation of Heat

The basic law of heat radiation is that any object is radiating heat proportional to the fourth power of the absolute temperature, but all surrounding objects are likewise radiating. If the surfaces are partially reflecting, multiple reflections lead to complicated calculations.

In the case of a sphere or plane surface small as compared to the enclosing space, the heat loss from the small surface becomes independent of the emissivity of the enclosing surface because of multiple reflections within the enclosing

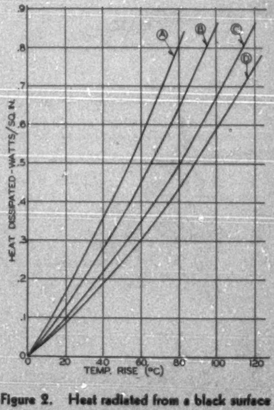

Figure 2. Heat radiated from a black surface

A—100-degrees-centigrade surrounding surface temperature

B—70-degrees-centigrade surrounding surface temperature

C—40-degrees-centigrade surrounding surface temperature

D—25-degrees-centigrade surrounding surface temperature

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The author takes this opportunity to acknowledge the help of G. W. Hewitt, who has conducted some of the tests reported, and contributed many valuable suggestions in the preparation of the manuscript.

[1] For all numbered references, see list at end of paper.

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has a very definite speed-power characteristic which is not only affected by the number of spindles, but also by the ring diameter and the size of yarn spun. When these conditions are known, it is possible to predict, within reasonably close limits, the power required to drive a spinning frame.

There is a possibility that these machines, like most others, may vary slightly in power requirements even when so-called duplicates are involved. These variations should not affect the horsepower rating of the motors selected to drive the frames, and these data should be of considerable help to the machine manufacturer and the textile manufacturer in selecting the proper-size motor for applying to spinning frames within the scope of these investigations.

space. The heat loss from the small surface is that given by the familiar Stefan-Boltzman law.[15,4]

$$W=36.9e_1\left[\left(\frac{\theta_1}{1,000}\right)^4-\left(\frac{\theta_2}{1,000}\right)^4\right]$$

W^* = energy radiated, watts per square inch
θ_1 = temperature of radiating surface (degrees Kelvin)
θ_2 = temperature of the enclosing surface (degrees Kelvin)
e_1 = emissivity of the small surface

The emissivity of any surface is equal to its absorption coefficient. Also, $1-e=$ reflectivity.

If the radiating object becomes of appreciable size as compared to the enclosing surface, the emissivity of the enclosing surface becomes of importance since, in a reflecting enclosure, radiation from the sphere will be reflected back onto the emitting surface. A. D. Moore[4] has evaluated the heat loss for several typical cases of this type and also the increase in radiation due to grooves or corrugations in reflecting surfaces. Another complication is that in general the emissivity of a surface varies with the wave length of the radiation. For example, white paint reflects a large percentage of solar radiation but is a good absorber for low-temperature radiation. In the case of irregular partially reflecting surfaces, exact calculations are usually too complicated for engineering purposes.[5] Fortunately, most electrical apparatus is painted with a nonmetallic paint which has an emissivity of the order of 0.9 for long wave lengths. The effective surface for radiation is then approximately the same as the enveloping surface. Radiation is in general not of importance in apparatus which is cooled by fans or is water cooled. In dealing with apparatus cooled by natural convection and radiation which must be located adjacent to hot surfaces, it usually is sufficient to compare the relative magnitudes of radiation and convection on the test floor with those calculated for service conditions and from these estimate the temperature under the service conditions. Natural convection varies widely, depending on the shape of the surface and surrounding objects. However, curves are available which give the convection under average conditions, and for many engineering purposes these values will prove sufficiently accurate. Figure 1 compares radiation for 25-degree enclosing walls and convection for large plane surfaces as given by McAdam's equations.[6] Figure 2 gives radiation for several other enclosing surface temperatures. The curves are useful in approximating the temperature rise under conditions near hot walls.

Solar Radiation

The radiant energy received from the sun has been the subject of many careful measurements.[8,9,10] It fluctuates about seven per cent due to variation in the distance of the sun and sun spots. The radiant energy from the sun as it would be measured by an observer at the earth's mean solar distance but outside our atmosphere, is called the "solar constant."

The mean of 692 determinations of the solar constant in the period from 1902 to 1912 gave a value of 1.94 gram-calories per (minute × centimeter square) or 0.87 watt per square inch.[11]

The intensity of radiation as measured at the earth's surface varies over a wide range, depending on the altitude, humidity, and cloudiness. The curves of figure 3 are plotted from values given in the Smithsonian Physical Tables[11] and give the intensity as a function of wave length for various altitudes and angles of the sun from the zenith. These curves apply to conditions of low humidity and no volcanic dust, so that they give practically maximum values of direct solar radiation.

A large portion of the reduction in solar energy in passing through the atmosphere is due to scattering. Much of this scattered energy reaches the earth as "sky light." This has not been studied as extensively as the direct solar radiation. In general, the scattered radiation or sky light is increased as the direct solar radiation is decreased by a longer path through the atmosphere or by increased haziness. Abbot[12] gives a value of sky light on Flint Island (sea level) of 32 per cent of the direct radiation and for Mount Wilson 7 per cent of the direct radiation.

Taking 0.6 as a typical value of direct radiation at sea level, 32 per cent of 0.6 gives 0.19 watt per square inch as sky light. For Mount Wilson 7 per cent of 0.74 gives 0.05 watt per square inch as sky light. The total radiation received by a horizontal surface with the sun at zenith would then be 0.6+0.19=0.79 for Flint Island and 0.74+0.05=0.79 for Mount Wilson. These meager data would seem to indicate that under favorable conditions a surface at sea level may receive nearly as much total energy as at high altitude, and that this value may be as high as 0.8 watt per square inch. Houghten, Blackshaw, Pugh, and McDermott[1] give values of solar radiation

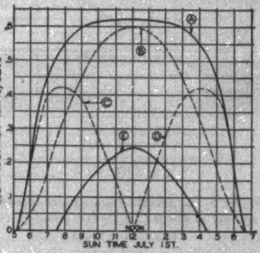
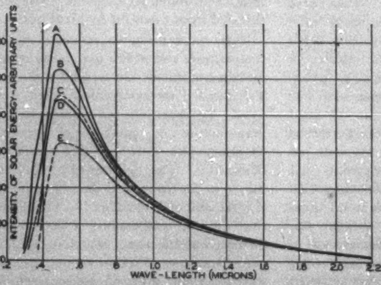


Figure 4. Solar radiation July 1 at Pittsburgh, Pa.

A—Surface normal to sun
B—Horizontal surface
C—East wall
D—West wall
E—South wall



A—Outside the earth's atmosphere
B—Mount Wilson—5,680-foot elevation, zero degree angle of sun from zenith
C—Mount Wilson—5,680-foot elevation, 60-degree angle of sun from zenith
D—Washington, D. C., 112-foot elevation, zero degree angle of sun from zenith
E—Washington, D. C., 112-foot elevation, 60-degree angle of sun from zenith

Figure 3. Intensity—wave length curves for direct solar radiation

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* Watts, inches, and degrees centigrade are generally used in this paper because they are believed to be the most convenient units for the electrical engineer. To convert watts per square inch to Btu per (hour × square feet) multiply by 493; to convert to gram calories per (second × square centimeters) multiply by 0.037.

as measured at the United States Weather Bureau station in Pittsburgh, Pa. Figure 4 gives their values. Their maximum value of radiation is 0.63 watt per square inch. From the data available, the writer considers 0.75 watt per square inch as the typical maximum value of solar radiation (that is, 20 per cent greater than that given by figure 4).

Effect of Solar Radiation on the Temperature of Apparatus

As has been mentioned, most devices having forced air circulation or water cooling, will have a high loss per unit of external projected area exposed to the sun. The heat received from the sun is, therefore, small as compared to the normal losses and has a negligible effect on the temperature rise. Such cases will not be considered further.

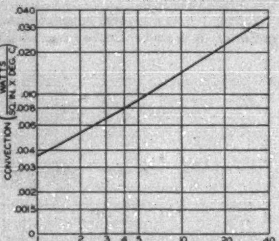


Figure 5. Convection from six-inch-diameter cylinder as affected by air flow perpendicular to axis of cylinder

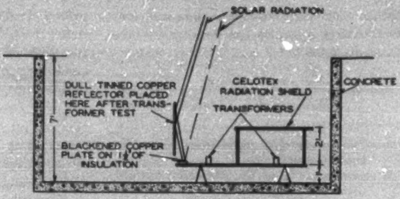
The heat received by apparatus exposed to the sun can be estimated from data on solar radiation. If all parts of the device can be assumed to be at the same temperature, and if the watts lost and temperature rise under test conditions are known, the watts dissipated per degree rise can be calculated. The additional rise due to radiation from the sun can be estimated.

However, under service conditions, several factors tend to reduce the temperature below that which would be obtained in this way.

1. Large apparatus cooled by natural convection and radiation approaches its final temperature slowly so that the solar radiation will be decreasing before the apparatus reaches its maximum temperature.

2. Most locations exposed to the sun will likewise be exposed to wind which will give a greater loss per degree by convection than that obtained in the test floor.

3. The sky is very seldom cloudless, so that the maximum value of solar radiation is seldom obtained continuously.



SOLAR RADIATION
DULL TINNED COPPER REFLECTOR PLACED HERE AFTER TRANSFORMER TEST
CELOTEX RADIATION SHIELD
CONCRETE
TRANSFORMERS
BLACKENED COPPER PLATE ON 1½" OF INSULATION
7'
2'
1'

Figure 6. Diagram of apparatus for measuring temperature rise due to solar radiation

Apparatus exposed to the sun receives direct and scattered radiation and radiates to the sky. To be strictly accurate in estimating the radiation from apparatus out of doors, the effective temperature of the sky should be used. However, calculations indicate that for apparatus with a surface rise of 50 degrees centigrade or less, such refinements are not justified by the accuracy of our knowledge of scattered radiation and effective temperature of the sky. The term "sky light" has been applied to the net radiant energy received by a black surface exposed to the sky, taking as zero the condition when the black surface is covered by a radiation shield at ambient temperature. Sky light is, therefore, the difference between the scattered solar radiation falling on the measuring surface and the additional heat radiated from this surface due to the fact that it is exposed to the sky rather than to a surface at ambient temperature. Therefore, in this paper the additional temperature rise of apparatus due to solar radiation will be calculated on the assumption that the apparatus receives both direct solar radiation and sky light, and that the apparatus dissipates its heat as though it were radiating to surfaces at ambient temperature.

Figure 5 is a curve of convection as affected by wind velocity. This curve was plotted from a dimensionless curve given by McAdams.[7] From this curve it is evident that a wind velocity of one mile per hour is sufficient to give a noticeable increase as compared to natural convection in still air. A wind velocity of ten miles per hour gives a convection coefficient of 0.014 watt per (square inch × centigrade degrees). At 40-degrees centigrade rise this gives 0.56 watt per square inch, which is 4.3 times the loss by natural convection. Appendix I considers a simplified case of apparatus exposed to sun and wind simultaneously, showing that a wind velocity of four miles per hour is sufficient to compensate for the heat received from the noonday sun.

Another method of reducing the temperature rise due to solar radiation is to use a paint which reflects much of the short-wave-length radiation, but acts as a good radiator for long wave lengths. White paint may absorb only 25 per cent of solar radiation but 95 per cent of long wave lengths. There would be few locations where a clean white surface could be maintained, but a gray paint showing an appreciable gain might be practical. Metallic pigments as in aluminum paint give a high reflectivity for all wave lengths. This reduces the solar radiation absorbed, but also reduces the dissipation of heat by radiation so that the device will usually operate at a higher temperature than if painted black. Montsinger[13] has discussed this question, and has given several typical curves showing the temperature rise of transformers under service conditions. The wind velocity was not reported, so that his values cannot be used to predict the temperature which would occur under other wind conditions.

Test Results

Under many conditions there will be sufficient wind so that the increased convection will compensate for the heat received from the sun. Occasionally there are periods of calm, or apparatus may be mounted in locations where it is shielded from the wind by buildings.

Tests were made to demonstrate the temperature rise which may occur under certain conditions due to solar radiation. Two small (100-watt) transformers were used. To determine any differences in manufacture or position of inserted thermocouples, tests were first run in the laboratory. The temperature rise obtained in the first test is plotted in curves *A*1, *B*1, and *AB*2 of figure 7. This shows that the iron temperature of both transformers was the same, but that the copper of transformer *A* ran about 2 degrees warmer than that of transformer *B*. To be certain that both were loaded equally, the primaries were operated in parallel and the secondaries in series supplying a common lamp load.

To obtain results not influenced by clouds, the field tests were made in Colorado at 38 degrees latitude and at an elevation of 4,200 feet. The first test was made with the transformers supported about one foot above the ground and in a location shielded from the wind by a small building. Transformer *B* was exposed to the sun, while transformer *A* was shaded by a piece of Celotex four feet by four feet supported about two feet above the transformer. The arrangement was similar to that shown in figure 6, except that the test was made on level ground instead of in a pit. The results of this test are shown as curves *A*3 and *B*3 of figure 7. While an attempt had been made to locate the test where it would be shielded from the wind, this was not entirely successful. Especially toward the end of the test, gusts of wind would occasionally give anemometer readings of 75 feet per minute near the transformers. It is believed that the drop in temperature near the end of the test was caused by these gusts of wind. Under these conditions the copper of transformer *B* which was exposed to the sun operated at 17 degrees centigrade greater rise than that of transformer *A* which was shaded, although in the laboratory the copper of transformer *A* had been 2 degrees warmer. The difference due to solar radiation was, then, 19 degrees centigrade.

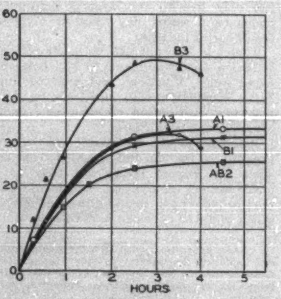


Figure 7. Time–temperature rise curves of small transformers

*A*1—Copper temperature of transformer *A* as tested in laboratory

*B*1—Copper temperature of transformer *B* as tested in laboratory

*AB*2—Iron temperature of transformers *A* and *B* as tested in laboratory

*B*3—Copper temperature of transformer *B* exposed to the sun

*A*3—Copper temperature of transformer *A* shaded

To eliminate the effect of the wind, another test was made in a pit 7 feet deep by 15 feet wide with concrete walls. This test arrangement is shown in figure 6. This time transformer *A* was exposed to the sun and transformer *B* was shaded. As would be expected in a location so well shielded from the wind, the ambient temperature rose considerably. For this reason actual temperatures are plotted rather than temperature rise in figure 8. In this test at the end of $2^1/_2$ hours the copper of transformer *A* was 22 degrees warmer than that of transformer *B*. Correcting for the 2-degree difference in the laboratory test, the rise due to radiation would be 20 degrees centigrade. The effect of solar radiation on iron temperatures was to give an additional rise of $20^1/_2$ degrees centigrade. An approximate calculation of temperature for this case is given in the appendix.

To illustrate one unusual condition, a piece of blackened sheet copper was mounted on $1^1/_2$ inches of thermal insulation (wood-pulp packing material). In this way the loss from the back of the sheet metal was small and the area for dissipating heat by convection was the same as the area exposed to radiation. With an ambient of 40 degrees centigrade the sheet metal attained a temperature of 90 degrees centigrade. To illustrate the effect of possible reflections, a piece of dull tinned copper was set up as a reflector as shown in figure 6. This increased the temperature of the blackened metal to 109 degrees centigrade. Or course, many other special tests could be made showing still higher local temperatures. Apparatus under a glass cover gets very hot because most glasses transmit solar radiation efficiently, but are opaque to long wave lengths, so that apparatus under glass receives solar radiation but cannot be cooled by radiation through the glass. Almost everyone is familiar with the use of a lens or parabolic mirror to produce fire, which is an extreme example of high temperature resulting from solar radiation.

Summary

In apparatus with a smooth exterior surface and without fans or liquid cooling, radiation to surrounding objects is usually more important than convection. In this case, the surface temperature of surrounding objects may be more important than the air temperature.

Solar radiation on clear days may be as high as 0.8 watt per square inch of surface normal to the sun.

The effect of solar radiation on the temperature rise of apparatus with forced air circulation or water cooling is generally negligible.

If apparatus cooled only by natural convection and radiation from a smooth surface is exposed to the sun but shielded from the wind, additional temperature rise may be of the order of 20 degrees centigrade. However, a wind velocity of a few miles per hour can compensate for the solar radiation. The effect of solar radiation can be decreased by white or gray paint.

High local temperatures can result from unusual conditions, a common example being an object with a glass cover. An extreme example is the high temperature at the focus of a parabolic mirror or lens.

Appendix

Approximate calculations are given for two smooth-exterior, self-cooled devices showing the additional temperature rise resulting from exposure to the sun in still air, and the effect of a slight wind on the temperature rise.

A. Calculations for 100-Watt Transformers Used in Tests

Under laboratory conditions the transformer case operated at an average temperature rise of 21 degrees centigrade. The total surface area was 81 square inches. Using the curves of figure 1 in approximating the heat dissipated from the case gives, in watts per square inch,

Convection loss	0.055
Radiation ($e=0.95$), $0.092\times0.95=$	0.087
Loss by radiation and convection	0.142

$0.142\times81=11.5$ watts total heat dissipated, which compares with measured losses of 12 watts

When operating in the sun, the total solar radiation from the curves of figure 4 give 13.8 watts at 9 a.m. This value is obtained by multiplying the projected horizontal area by the value per square inch for a horizontal surface from curve *B* of figure 4; the east area by the value given by curve *C*, etc. At 10 a.m., the radiation was 14.2 watts; at 11:00, 13.3 watts, and at noon, 11.6 watts. Averaging these values gives 13.3 watts as the average heat from the sun, which would be received by a perfect black body. The value of emissivity was assumed to be 0.95. The curves of figure 4 were obtained from the weather-bureau station in Pittsburgh, Pa. From the data available, the values for Colorado are probably 20 per cent higher.

Correcting the above average in this way gives

$13.3\times0.95\times1.2=15$ watts

as the estimated average solar radiation.

Adding this to the 12 watts electrical losses gives 27 watts, or 0.33 watt per square inch. By trial and error, using figures 1 and 2, we find that 40-degrees centigrade rise gives, in watts per square inch,

Radiation (40-degree surroundings)
$0.23 \times 0.95 = 0.205$
Convection loss 0.126

Total 0.331

This gives an additional surface rise of $40 - 21 = 19$ degrees centigrade. The drop from copper and iron to the surface is not changed, so that all temperature rises should be 19 degrees higher than in the shade, which compares with $20^1/_2$ degrees centigrade in the test values given by figures 7 and 8.

B. Effect of Wind

To illustrate the effect of the wind, assume that some device (for example, a transformer) in a cylindrical case six inches diameter by ten inches high is mounted on a pole. Assume the case is smooth and at a uniform temperature. Calculating the solar heat input by considering as before the horizontal, east, south, and west projected areas and the corresponding values of radiation, gives an average heat input from 9 a.m. to 12 noon of 39.5 watts or

$\frac{39.5}{244} = 0.162$ watt per square inch of total surface

Assuming that the normal surface rise is 30 degrees centigrade above an ambient of 25 degrees centigrade gives a heat dissipation of

Convection loss 0.085 watt per sq. in.
Radiation $0.135 \times 0.95 =$
0.128 watt per sq. in.

0.213 watt per sq. in.

0.213×244 square inches = 52 watts normal dissipation
$0.213 + 0.162$ sun effect = 0.375 watt per square inch

to be dissipated when exposed to the sun. By trial and error from figure 1

47 degrees centigrade rise gives
Convection loss 0.155 watt per sq. in.
Radiation 0.23×0.95
0.22

0.375 watt per sq. in.

We would then expect this device to operate at 17 degrees centigrade additional rise if exposed to the sun with no wind.

A wind of four miles per hour gives from figure 5 a heat dissipation of 0.008 watt per (square inch × centigrade degrees). At 30-degrees centigrade rise, this gives

0.24 watt per square inch convection loss
$0.135 \times 0.95 = 0.13$ watt per square inch radiation
$0.24 + 0.13 = 0.37$ total

In other words, a wind of four miles per hour would increase the convection by an amount equal to the heat received as solar radiation, so that the temperature rise in the sun would be the same as on the test floor.

A wind of ten miles per hour would give a convection coefficient of 0.014 watt per (square inch × centigrade degrees). At 21 degrees centigrade rise this gives

Convection loss $(0.014 \times 21) =$
0.294 watt per square inch
Radiation $0.095 \times 0.95 =$
0.09 watt per square inch

0.384 watt per square inch

Therefore, a wind of ten miles per hour would dissipate the heat at 21 degrees centigrade rise, or 9 degrees centigrade below normal.

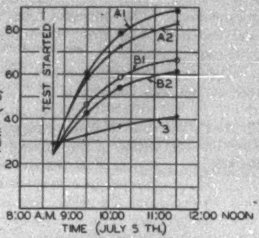


Figure 8. Time–temperature curves of transformers shielded from the wind

*A*1—Copper temperature of transformer *A* (exposed to sun)

*B*1—Copper temperature of transformer *B* (shaded)

*A*2—Iron temperature of transformer *A*

*B*2—Iron temperature of transformer *B*

3—Ambient air temperature

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Discussion

M. F. Beavers (General Electric Company, Pittsfield, Mass.): This paper presents some very interesting test data on the effect of solar radiation and wind on the thermal characteristics of small transformers compared to the characteristics under laboratory conditions.

Mr. Penney concludes among other things in his summary that "If apparatus cooled only by natural convection and radiation from a smooth surface is exposed to sun but shielded from the wind, additional temperature rise may be of the order of 20 degrees centigrade. . . ."

This certainly should be a warning to avoid installing small transformers with smooth surfaces in a location shielded from the wind but exposed to the sun for the major part of the day. It is probable that a small percentage of installations fall into this class.

On the other hand, it should not be alarming to those who have made tests or obtained reports to the effect that the temperature rise over ambient of an ordinary oil-immersed pole-type distribution transformer, under actual service conditions, will run only five to ten degrees centigrade higher on a sunny day than on a cloudy day or during the night.

Therefore I would like to recall to your attention some statements in Mr. Penney's paper which, I believe, will tend to show better correlation with previous tests on pole-type distribution transformers under actual service conditions.

Referring to the additional rise due to exposure to the sun but shielded from the wind, Mr. Penney points out that ". . . under service conditions, several factors tend to reduce the temperature below that which would be obtained in this way.

> "1. Large apparatus cooled by natural convection and radiation approaches its final temperature slowly so that the solar radiation will be decreasing before the apparatus reaches its maximum temperature.
>
> "2. Most locations exposed to the sun will likewise be exposed to wind which will give a greater loss per degree by convection than that obtained in the test floor.
>
> "3. The sky is very seldom cloudless, so that the maximum value of solar radiation is seldom obtained continuously."

Item number 13 in Mr. Penney's bibliography refers to an article by Montsinger and Wetherill entitled "Effect of Color of Tank on the Temperature of Self-Cooled Transformers Under Service Conditions" in which the results of various tests on distribution transformers located outdoors under normal weather conditions are given.

Table I

Color of Tank	Oil Temperature Rise—Deg C (1) Min.	(1) Avg.*	(1) Max.	(2) Min.	(2) Avg.*	(2) Max.	(3) Min.	(3) Avg.*	(3) Max.
Black	4	6.9	11.5	8	10.8	15	20	24	29.5
Medium gray	4	7.16	10	10	12	16.5	20	23.4	29
Light gray	1.5	3.67	7.5	7	10.55	14	18	22.8	27

(1). Transformer without excitation or load (idle).
(2). Transformer with normal excitation only (no load).
(3). Transformer with normal excitation and normal load.
* Average temperature rise over a 24-hour period.

Figure 1 of the Montsinger-Wetherill paper shows a chart of oil temperature and ambient air temperature for a period of two days for four 5-kva transformers under excitation only (that is, no load). Two of the transformers were in black-painted tanks, one was white, and one was aluminum painted (tests were made in Pittsfield, Mass.).

In order to show the effect of the sun on the transformers in the black-painted tanks I analyzed the oil temperature rise over ambient air for the two-day period from August 12 to 14, 1909. The average minimum oil rise of 7 degrees centigrade occurred in the neighborhood of 4 a.m. to 8 a.m. and the maximum oil rise of 14 degrees centigrade occurred at 6 p.m. The additional rise due to the sun, then, is $14-7=7$ degrees centigrade.

With reference to the temperature of the transformers in the white and aluminum-painted tanks Montsinger states ". . . that the transformer with the black tank averages one to two degrees centigrade hotter than the one with the white tank. . . . The temperature of the aluminum-painted tank was as a rule between those of the black and white tanks."

Tests made on several 3-kva lighting transformers in August and September 1922, by Messrs. Moore and Moulton of the San Joaquin Light and Power Corporation are reported in this reference and are summarized in table I of this discussion.

An analysis of the data from which this summary was taken indicates that the maximum oil rise occurs in the vicinity of 6 to 8 p.m. and the minimum oil rise occurs during the early part of the morning with very little increase in rise before 10 a.m.

The additional temperature, then, due to the sun, is the difference between the minimum and maximum oil rises observed.

For the Moore-Moulton tests the maximum difference is 9.5 degrees centigrade, on the transformer in the black tank under normal load and excitation.

In regard to the benefits of the gray paint Moore and Moulton concluded that "We have reached the conclusion, after a very careful study of the results herein recorded, that any additional expenditure to procure a gray case instead of black would not be justified."

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): I wish to discuss the curves given in figures 1 and 2 of Mr. Penney's paper. In my work I have used curves or equations quite similar to those used by the author. For example: I have used 36.8 for the constant in the radiation formula, and 0.95 for the emissivity factor e_1 for all surfaces painted with nonmetallic or pigment paints.

The equation of curve *C* in figure 1 for loss of heat by convection from a large vertical surface appears to be, $W_c=1.265\times 10^{-3}\ \theta^{1.25}$, where W_c=watts per square inch and θ=temperature rise in degrees centigrade. I have used the same form of equation but with a constant of 1.4×10^{-3} which gives approximately ten per cent more loss by convection than curve *C* gives for the same temperature rise.

The curves in figure 1 show that the percentage of losses from a large vertical surface are approximately 60 per cent by radiation and 40 per cent by convection at 25 degrees centigrade. I have found approximately 55 per cent and 45 per cent respectively, the difference being due to the difference in the constants used in the convection formula. This ratio changes with room temperature, since loss of heat by radiation increases with increasing room temperatures, whereas loss by convection is practically unaffected by changes in room temperature. An easy rule to remember is that loss by radiation increases from 1.0 to 1.2 per cent for each degree increase in the room temperature. This is shown by the curves in figure 2. For example: for 60 degrees rise the ratio of the losses in a 100-degree room and a 25-degree room is (0.57/0.30):1.9. That is, the loss is 90 per cent more in a 100-degree room than in a 25-degree room. 90 per cent divided by 75 degrees gives 1.2 per cent increase per degree. For 80 degrees rise the difference between the 40 and 100-degree curves is one per cent per degree. The average value is approximately 1.1 per cent.

For limited temperature rises—up to about 75 degrees—it is possible to approximate closely loss of heat by radiation at various room temperatures by a simple logarithmic equation, as is shown on page 272 of the book on "Transformer Engineering". The equation is:

$$W_r=1.84\times10^{-3}\theta^{1.10}E\left(1+\frac{1.1\theta_a}{100}\right)$$

where

W_r = watts per square inch
θ = temperature rise degrees centigrade
E = emissivity factor
θ_a = room temperature degrees centigrade

Referring again to loss of heat by convection, it should be kept in mind that the constants mentioned before apply only to large vertical surfaces of approximately two feet height and more. For short heights, the constant increases. Some investigators state that the loss varies inversely as the height raised to the one-fourth power. For example: if the loss is unity for a 24-inch height, for a 2.4-inch height the loss would be $(24/2.4)^{1/4}=1.78$ or 78 per cent more watts per square inch for the same temperature rise. I would like to ask Mr. Penney if he took the height of his 100-watt transformer into consideration when evaluating the ratio of losses by radiation and convection. If not, I am inclined to believe that the effect of the short vertical surface of the 100-watt transformer on the loss by convection was much more than the effect of the low wind velocity.

Referring to my paper—reference 13—as Mr. Penney surmises no record was made of the wind velocity when the field tests were made. The conditions under which the several tests were made correspond to those under which transformers operate in service. As all the tests indicated that the effect of the color of the tank on the relative temperature rise was quite small, two degrees centigrade at the most, it is felt that the conclusions drawn were sound, namely, that the question of the kind of paint used on distribution and power transformers exposed to the sun's rays, is one of appearance and durability only.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Penney's paper gives some very important practical as well as theoretical information on the effects of radiated heat on apparatus. In addition to apparatus intended for outdoor installation, apparatus designed for indoor use is frequently installed outdoors in enclosures of some kind or other. From Mr. Penney's paper it would seem that if these enclosures are ventilated, the sun radiation will have little influence. On the other hand, if the enclosures are not ventilated and if the surface of the enclosure is much larger than the surface of the apparatus enclosed, conditions might be different from those of the transformer discussed in the paper. It would be interesting if Mr. Penney could give in his closing discussion some data regarding installations of this kind.

R. E. Hellmund and F. R. Benedict (nonmember; both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Penney's paper discusses two factors, namely, sun radiation and wind, as affecting temperature conditions of electrical apparatus installed outdoors. It may therefore be appropriate to give some data which have recently become available on outdoor temperatures. In the paper entitled "Temperature Survey of the United States," by J. J. Smith and H. W. Tenney, which was presented at the 1939 AIEE summer convention, some valuable information was given on ambient outdoor temperatures in the United States. It is now generally conceded that the life of insulation is a function of both temperature and time during which certain high temperatures prevail. It is also well known that temperature variations below a certain safe temperature have little influence upon the deterioration of insulation. In view of this, the electrical engineer is interested principally in ambient-temperature conditions in excess of certain values and the time during which they prevail. The information of this nature given by Smith and Tenney covered seven selected typical stations for the period 1934 to 1938 inclusive, and included curves for the average number of hours per year and the average number of hours per day versus the average number of days per year during which the ambient temperature exceeded 90 degrees Fahrenheit. The paper also gave information for three selected stations for the period 1934 to 1939 inclusive, using 105 degrees Fahrenheit as a basis. These data were prepared from records of the United States Weather Bureau.

It is naturally desirable to have similar data from a larger number of stations, and such data have been made available in tables published in *Heating and Ventilating*, March 1939, under the subject "Number of Hours Per Year of High Dry-Bulb Temperatures." These tables give an analysis of 41 well selected and distributed stations. Three tables are shown, as follows:

Table 1. The maximum number of hours in a year that dry-bulb temperature exceeds 85, 90, 95, 100, 105, 110, and 115 degrees Fahrenheit. Maximum in any year from 1932 to 1937 inclusive.

Table 2. The average number of hours per year the dry-bulb temperature exceeds 85, 90, 95, 100, 105, 110, and 115 degrees Fahrenheit. Average for the years 1932 to 1937 inclusive.

Table 3. The minimum number of hours in a year that dry-bulb temperatures exceed 85, 90, 95, 100, 105, 110, and 115 degrees Fahrenheit for the years 1932 to 1937 inclusive.

From these data the number of hours the ambient temperature exceeds 30, 35, 40, and 45 degrees centigrade can be obtained (85F = 29.4C; 95F = 35C; 105F = 40.5C; 115F = 46.1C).

For our problem the data in table 2 are most pertinent because they cover averages for a period of six years. In a study of the deterioration of insulation we are interested not so much in an individual or maximum-temperature year, but in what happens during the entire life of the equipment. A 6-year period gives a pretty good idea of this, as any variations that may exist between a 6- and a 10- or 20-year period are negligible for the problem at hand.

Referring to table 2, we find that the average number of hours per year during which the temperature exceeds 35 and 45 degrees centigrade for the station having the maximum average number of hours above the given temperature is as follows:

Deg C	Hours	Station
30	1,084	Dallas, Tex.
35	292	Topeka, Kans.
40	37	Topeka, Kans.
45	0	

Expressed in percentage of total hours per year (8,760 hours), this leads to table *A* in figure 1 of this discussion.

As will be seen, the maximum hours for a given temperature do not all occur at the same station and thus no individual piece of apparatus is exposed to conditions quite as severe as those indicated in the table. It is therefore quite safe to state that, for our problem, the figures in table *A* represent the "maximum climatic ambient temperatures" in the United States. It may well be contended that the use of the maximum ambient conditions met by all electrical apparatus will lead to poor economy in the application of the large majority of apparatus. Reference to table 2, *Heating and Ventilating*, March 1939 indicates that the stations which are of high average are localized in Texas, Oklahoma, Kansas, Arkansas, and Louisiana. If stations in these areas are eliminated, the station with the maximum average number of hours at the given temperatures will then be as follows:

Deg C	Hours	Station
30	892	Kansas City, Mo.
35	248	Kansas City, Mo.
40	32	Kansas City, Mo.
45	0	

In considering the elimination of some stations or areas, the number of motors and horsepower installed in the various states in which the high-average stations were located, was taken into account. The paper by Smith and Tenney gives sufficient data to show that the elimination of Texas, Oklahoma, Kansas, Arkansas, and Louisiana involves only 3.05 per cent of the total number of motors and 3.9 per cent of the total horsepower installed (probably less than 5 per cent of all electrical apparatus). On the basis of an 8,760-hour year, the predominant climatic ambients for the areas in which over 95 per cent of all electrical apparatus is installed would then be as shown in table *B*, figure 1.

Stated in another way, it would be expected that the ambient temperature would be above 30 degrees centigrade ten per cent of the time or less; above 35 degrees centigrade three per cent of the time or less; above 40 degrees centigrade 0.5 per cent of the time or less; and above 45 degrees centigrade such a small part of the time that for all practical purposes it can be considered as zero.

Figure 1

Circles indicate weather - bureau stations

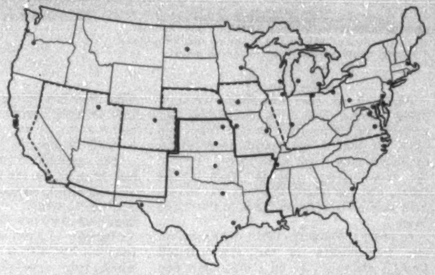

Table *A*. Climatic Ambient for the Country on the Basis of Maximum-Hour Station

Temperature Below (Deg C)	Per Cent of Time
30	88
35	97
40	99.5
45	100

Table *B*. Climatic Ambient for the Country After Eliminating Stations Below the Solid Line

Temperature Below (Deg C)	Per Cent of Time
30	90
35	97
40	99.5
45	100

Table *C*. Climatic Ambient for the Country After Eliminating Stations Below the Dotted Line

Temperature Below (Deg C)	Per Cent of Time
30	95
35	99
40	99.5
45	100

It may be of interest to consider only the essential industrial areas of the country. This can be done by excluding North Carolina, Tennessee, and the states south thereof, the southwest corner of Illinois, the states of Iowa and Nebraska and those south thereof, the states Arizona, Nevada, Utah, and New Mexico, and the southeast corner of California. We then find conditions as shown in table *C*, figure 1, for the areas included.

As a matter of course, the values in these tables apply also for any well-ventilated indoor areas where the indoor temperature is about the same as the outdoor. In indoor areas not so well ventilated and in which the electrical and other heat-generating apparatus is small compared with the space available, conditions should be even more favorable. On the other hand, in poorly ventilated indoor areas having a relatively large amount of heat-generating electrical apparatus and other heat-producing devices such as turbines, steam pipes, etc., conditions may be less favorable. Unfortunately, very few reliable data are available for such cases. There may also be indoor installations, especially in factory buildings with glass roofs, where the heat from sun radiation may raise the indoor temperature above the outdoor. Since these last two conditions may apply to an appreciable percentage of electrical apparatus, the compilation of data for installations of this nature seems desirable. However, the ambient temperature conditions given in this discussion, together with the information in Mr. Penney's paper and the Smith and Tenney paper should be of value in the application of electrical ap-

Phase Occurrence of Arc-Backs in High-Current Mercury-Arc Rectifiers

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THE phenomenon of arc-back in mercury-arc rectifiers is the major factor in determining present-day designs. To keep the frequency of occurrence of arc-back sufficiently low, the anodes are set at a sufficient distance from the cathode, shields, baffles, and grids are interposed, and cooling water temperatures are kept in a rather narrow range. These practically successful means for overcoming arc-backs were developed largely empirically as the cause and nature of arc-back is still little understood.

In the normal operation of rectifiers, a transient small inverse current flows to each anode just after its voltage polarity reverses at the end of each normal current-conducting period. This inverse current is a maximum just after transition from the normal conducting to normal insulating state of the anode, and decreases rapidly to a small fraction of its initial value in less than ten electrical degrees, under usual operating conditions. The inverse current is believed to consist of positive ions reaching the anode from the rapidly disappearing residual ionization left before the anode in the preceding conduction period. Most investigators express a belief in a strong causal relationship between this inverse current and the arc-back frequency. The fact that the means mentioned in the preceding paragraph for reducing arc-back frequency also reduce greatly the inverse current seems strongly to support this view. Prince and Vogdes,[1] however, state that "if there is any connection between arc-back and the inverse current, it is probably rather remote."

Little is known as to where in the alternating cycle arc-back occurs. If the

Table I. Arc-Back Data for Rectifiers Tested

Rectifier Tested	Load Amperes	Load Volts	Point on Inverse Cycle at Which Arc-Back Occurred*
Six-phase ignitron	1,250	300	T
	1,250	300	T
	1,250	300	T
	1,250	300	T
	1,250	300	T
	1,250	300	T
	1,875	300	T
	1,875	300	T
	1,875	300	T+26 deg
	1,875	300	T
Six-phase multianode rectifier	2,400	600	T
	3,750	600	T
	3,750	600	T+37 deg
Six-phase ignition	1,250	600	T+170 deg and T+36 deg
	1,250	600	T+68 deg
	1,250	600	T+47 deg
	1,250	600	T+26.6 deg
	1,250	600	T+20 deg
Three-phase ignitron	400	600	T+13 deg
Six-phase ignitron	3,000	300	T+20 deg
	2,500	300	T+27 deg
	2,500	300	T
	2,500	300	T+16 deg
	2,500	300	T+6.5 deg
	2,500	300	T
Six-phase ignitron	1,000	600	T
	1,000	600	T+30 deg
Six-phase ignitron	2,500	600	T
	2,500	600	T
	2,500	600	T
	1,700	600	T
	1,700	600	T+22 deg
Six-phase ignitron	2,500	600	T
	2,500	600	T
	3,750	600	T
	2,500	600	T+23 deg
	2,500	600	T
	2,500	600	T
	6,000	600	T+53 deg
	6,000	600	T+79 deg
	2,500	600	T
	3,750	600	T+80 deg
	3,750	600	T
	7,500	600	T+93.5 deg
	7,500	600	T
	7,500	600	T
	5,000	600	T+128 deg
	5,000	600	T
	7,500	600	T
	7,500	600	T
	5,500	600	T+36 deg
	6,000	600	T
	2,500	600	T
	2,500	600	T
Single-phase full-wave ignitron	400	600	T
	400	600	T+85 deg
	400	600	T
	300	600	T
	600	600	T+76 deg
	....	600	T
	200	600	T+90 deg
	600	600	T+38.5 deg
	600	600	T
	400	600	T
	....	600	T
	800	600	T+106 deg
	800	600	T+56 deg
	800	600	T+82 deg
	200	600	T+36 deg
	200	600	T+23 deg
	200	600	T+77 deg
	800	600	T+93 deg
	800	600	T+54 deg
	800	600	T+127 deg

*T = Transition.

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The authors wish to thank Doctor Joseph Slepian, who proposed collecting and publishing the data of this paper, and who is responsible for some of the suggestions in interpretation of the results.

1. For all numbered references, see list at end of paper.

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paratus, particularly transformers, the greater number of which are outdoors or in well-ventilated areas.

G. W. Penney: Natural convection depends on low-velocity air movements and therefore varies with the shape and size of the surface, type of ventilation in the room, and similar factors which are not normally closely controlled, so that rather large variations are to be expected between results obtained under various conditions. W. H. McAdams in his book on "Heat Transmission"[5] has compiled a good summary of available data. The curves of figure 1 are believed to represent average conditions for large objects.

As Mr. Montsinger mentions, natural convection for small objects is frequently assumed to vary inversely as the one-fourth power of the height, but this simplification usually requires some adjustment of constants. McAdams (page 244) gives a curve showing the variation of convection with height of cylinder which would give convection as varying inversely with a fractional power of the height up to 10 inches and then gradually changing to a constant value for heights over 18 inches. In reply to Mr. Montsinger's question as to the value used for the 100-watt transformer mentioned in the paper, if we use the formula for convection as a function of height as given by McAdams on page 245, equation 32, a value of 12 per cent greater than that given by figure 1 would be obtained. However, the transformer was tested sitting on a wooden platform as shown in figure 6. This platform somewhat obstructed the natural convection so that it was believed that the curves of figure 1 probably came very close to the actual condition.

Heat dissipation due to wind velocity depends on the size of the object and is also commonly approximated as varying inversely as the one-fourth power of the size of the object cooled so that the relative effect of the wind tends to be independent of the size of the object cooled.

Mr. Hellmund asked as to the effect of mounting apparatus in small outdoor enclosures, these enclosures being exposed to the sun. If the enclosure is not ventilated and if its temperature could be assumed to be uniform, the approximate methods suggested in the paper could be used to estimate the effect of the sun. Such a calculation would give an idea of the order of magnitude of the sun effect. However, the temperature throughout the case will not be uniform, and the sources of heat will be irregularly located so that tests should be made if exact figures are needed. A number of tests under typical operating conditions would be desirable.

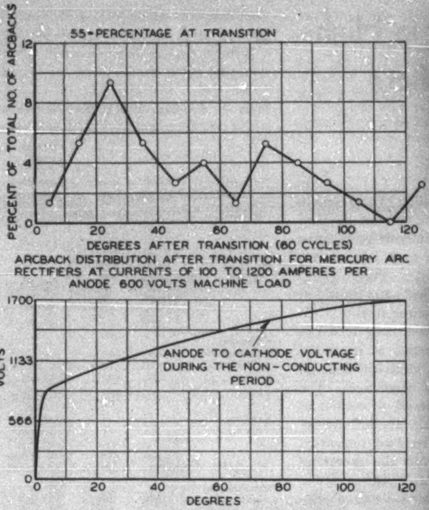

Figure 1 (above). Arc-back distribution and inverse voltage of a mercury-arc rectifier

Figure 2a. Oscillogram of an arc-back which occurred at transition

Figure 2b. Oscillogram of an arc-back which occurred 86 degrees after transition

inverse current is the controlling cause, then all arc-backs should occur immediately after transition, since under normal operating conditions for low-voltage rectifiers, there is considerable inverse current only just after transition. Actually the belief is quite widespread that arc-backs occur almost entirely at transition. Prince and Vogdes,[2] however, give a contrary viewpoint, and describe experiments with a tube in which quite definitely the arc-backs did not take place at transition. However, the tube used in their experiments was very special, so that it might still be believed that in practical low-voltage rectifiers, arc-backs occur only at transition.

It is difficult to determine the time of occurrence of arc-backs by usual oscillographic means with rectifiers operating under normal conditions, because of their completely random and infrequent occurrences. However, the development of the "memnoscope"[3] has solved these difficulties so that there has now accumulated a considerable number of oscillographic records which permit a direct determination of the time of occurrence of arc-backs in practically operating rectifiers. These records indicate that arc-backs occur frequently throughout the whole inverse period.

Table II. Per Cent of Arc-Backs at Transition for Each Rectifier Unit

Rectifier Tested	Number of Arc-Backs Observed	Per Cent at Transition
Six-phase ignitron, 300 volts	10	90
Six-phase multianode tank rectifier, 600 volts	3	67
Six-phase ignitron, 600 volts	5	20
Three-phase ignitron, 600 volts	1	0
Six-phase ignitron, 300 volts	6	33
Six-phase ignitron, 600 volts	2	50
Six-phase ignitron, 600 volts	5	80
Six-phase ignitron, 600 volts	22	66
Single-phase full-wave ignitron, 600 volts	20	35

Test Conditions and Procedure

All the oscillograms were taken on the test floor or in the laboratory, on commercial 300- and 600-volt rectifiers feeding a d-c motor. Currents ranged from 100 to 1,250 amperes per anode with zero ignition angle.

Anode to cathode voltages of all the anodes of a rectifier under observation were brought to memnoscopes whose outputs were passed through the elements of a magnetic oscillograph with continuously rotating cylindrical film. On occurrence of an arc-back, a reverse-current relay opened the oscillograph shutter, and a photographic record was thus obtained of the anode to cathode voltages preceding, during and after the arc-back. From these oscillographs the time of occurrence of the arc-back could be determined.

Results

Seventy-four oscillograms were obtained with arc-back occurrence times, as shown in table I. Two typical oscillograms are shown in figures 2*a* and 2*b*.

Forty arc-backs or 54 per cent occurred at transition. The remaining 34 or 46 per cent were distributed through the inverse period.

The curve of figure 1 shows the distribution of these 34 arc-backs through the inverse period. Only one was observed at more than 130 degrees after transition. It occurred at 170 degrees and another anode arced back a few degrees later.

By direct experiment with a deck of cards it was found that the curve of figure 1 is not inconsistent with the hypothesis that the probability of arc-back is the same in each of the 10-degree intervals making up the 130 degrees. That is, 34 points placed at random in the 13 10-degree intervals gave distributions with as great a saw-tooth irregularity as shown in figure 1.

Discussion

The fact that only one arc-back was observed at more than 130 degrees later than transition may have been not because the later ones have a lower probability of occurrence, but rather to the method used for tripping the oscillograph shutter. On the six-phase rectifiers the oscillograph shutter was tripped by the reverse-current breaker, hence no record of arc-backs which clear without fault, if there are such arc-backs, was obtained.

There are not a sufficient number of arc-backs to obtain the true form of the distribution curve. About all that can be said definitely from the results obtained is that not all arc-backs have a probability of occurrence which is a strongly varying function of the instantaneous positive ion current density. The most that can be said in this direction would be that there are several types of arc-backs; and that one type, which constituted 54 per cent of the total, is occasioned by the momentary positive-ion current.

On the other hand, other possibilities will fit the above results. For example, we may postulate that arc-back causes occur at random throughout the cycle, but with effects lasting a cycle or more. In this case all causes which occur during the conducting period will produce their effect at transition and their number will depend on ratio of conducting to nonconducting period in the rectifier. If there is an equal probability for the whole inverse period, then for the double three-phase rectifier connection we should obtain 230°/360° = 63.8 per cent of the arc-backs after transition and 36.2 per cent at transition. However, as can be seen from table II for the nine rectifiers tested, the fraction for each rectifier occurring at transition is very variable, which makes the possibility just discussed rather unlikely.

Bibliography

1. Mercury-Arc Rectifiers and Circuits (a book), D. C. Prince and F. B. Vogdes. McGraw-Hill, New York, 1927, page 69.

2. Reference 1, page 71.

3. A Memory Attachment for Oscilloscopes, W. E. Pakala. AIEE Transactions, volume 57, 1938, pages 682–4.

Station-Type Lightning-Arrester Performance Characteristics

AIEE LIGHTNING-ARRESTER SUBCOMMITTEE

THIS PAPER presents the results of additional work by the AIEE lightning arrester subcommittee and contains station-type-arrester performance values which may be expected of present-day commercial station-type arresters rated from 3 to 245 kv. The characteristics of line-type arresters were presented by the subcommittee in the May 1937 and November 1938 issues of Electrical Engineering.

Table I includes station-arrester impulse gap breakdown values obtained from tests using the rate of voltage rise specified in the AIEE Standards for lightning arresters No. 28 or ASA Standards *C-62*; that is 100 kv per microsecond per 11.5 kv of arrester rating. To enable a more complete analysis of arrester performance, a complete curve (figure 1) of arrester gap breakdown voltage is included as a supplement to the tabulated values. This curve is plotted in per cent of the arrester's maximum permissible line-to-ground voltage rating, using the crest value of the voltage rating as the 100 per cent of reference point. The actual breakdown voltage for any arrester at any time *T* can be obtained by multiplying the crest value of the arrester's maximum permissible line to ground voltage rating by the percentage value indicated for any time *T* on the curve.

The 60-cycle spark potential of the arresters referred to in this report will not be less than 150 per cent of the arrester's maximum permissible line-to-ground voltage rating.

The table of arrester-performance values also includes the *IR* discharge voltage of the arrester valve element for currents of 1,500, 3,000, 5,000, 10,000, and 20,000 amperes, all of which are based on a current wave having a time to crest value of 10 microseconds and a time to 50 per cent of the crest value on the tail of the wave of 20 microseconds.

The lightning-arrester manufacturers have furnished the data from which the average characteristics of available station arresters are plotted and tabu-

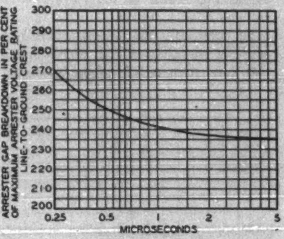

Figure 1. Impulse gap breakdown, tolerance ±20 per cent

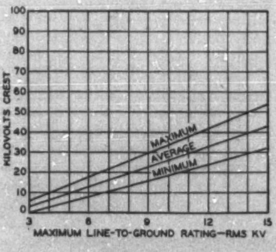

Figure 2. Impulse gap breakdown based on AIEE wave

Paper 40-62, prepared by the lightning-arrester subcommittee of the AIEE committee on protective devices, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, January 22–26, 1940. Manuscript submitted June 14, 1939; made available for preprinting December 28, 1939; released for final publication February 23, 1940.

Personnel of AIEE lightning-arrester subcommittee: L. R. Ludwig, chairman; H. W. Collins, H. A. Dambly, J. M. Flanigen, I. W. Gross, Herman Halperin, J. C. McFarlin, J. R. North, H. L. Rorden, W. J. Rudge, A. H. Schirmer, H. K. Sels, L. G. Smith, H. R. Stewart, and E. R. Whitehead.

Table I. Industry Station-Type Arrester Characteristics

All *IR* Voltages on 10–20-Microsecond Current Wave; Kilovolts Crest

Kv*	Gap Breakdown on AIEE Rate			IR at 1,500 Amp.			IR at 3,000 Amp.			IR at 5,000 Amp.			IR at 10,000 Amp.			IR at 20,000 Amp.		
	Avg.	Min.	Max.	Avg.	Min.	Max.	Avg.	Min.	Max.	Avg.	Min.	Max.	Avg.	Min.	Max.	Avg.	Min.	Max.
3	13	10	16	9	8	10	10	9	11	10	9	11	11	9	13	12	10	14
6	23	18	28	18	15	21	19	16	22	20	17	23	22	19	25	24	20	28
9	35	28	42	27	23	31	29	25	33	30	26	34	33	28	38	35	30	40
12	43	34	52	36	31	41	38	32	44	40	34	46	44	37	51	47	40	54
15	53	42	64	45	38	52	47	40	54	50	43	57	54	46	62	59	50	68
20	72	58	86	60	51	69	64	54	74	67	57	77	72	61	83	78	66	90
25	89	71	107	74	63	85	79	67	91	83	71	95	90	77	103	100	85	115
30	106	85	127	89	76	102	95	81	109	100	85	115	108	92	124	118	100	136
37	131	105	157	110	94	126	117	100	134	124	105	143	132	112	152	145	123	167
40	136	109	163	119	101	137	127	108	146	134	114	154	144	122	166	153	130	176
50	178	143	213	149	127	171	158	134	182	167	142	192	179	152	206	191	163	219
60	214	171	257	179	152	206	191	162	220	200	170	230	217	185	249	234	199	269
73	261	209	313	217	185	249	232	197	267	245	208	282	262	223	301	283	241	325
97	345	276	414	289	246	332	308	262	354	323	275	371	340	297	401	377	321	433
121	430	344	516	360	306	414	384	326	442	403	343	463	438	372	504	470	410	530
145	515	412	618	431	366	496	461	392	530	487	414	560	523	445	601	564	480	648
169	602	482	722	502	427	577	538	457	619	566	481	651	610	519	701	658	559	757
195	691	553	829	578	491	665	617	519	715	647	550	744	698	593	803	755	642	868
242	860	688	1,032	720	612	828	768	653	883	806	685	927	872	741	1,003	940	799	1,081

* Maximum permissible line-to-ground voltage rating.

lated. These values hold for new arresters and do not necessarily hold for the older types of arresters.

Tolerances are given to include the expected variation from the average values referred to above. The tolerance from the average value of gap breakdown is ±20 per cent and the tolerance from the average value of the *IR* discharge voltages is ±15 per cent.

By comparing the complete volt-time curve of the arrester with the complete volt-time characteristic of the insulation to be protected, a reasonable evaluation can be made of the margin of protection obtained.

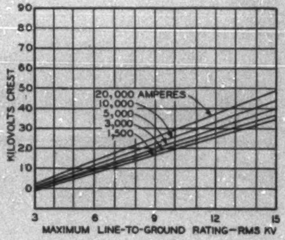

Figure 4. IR discharge voltage, 10–20-microsecond wave; tolerance ±15 per cent

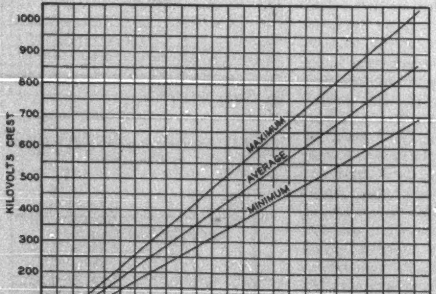

Figure 3. Impulse gap breakdown based on AIEE wave

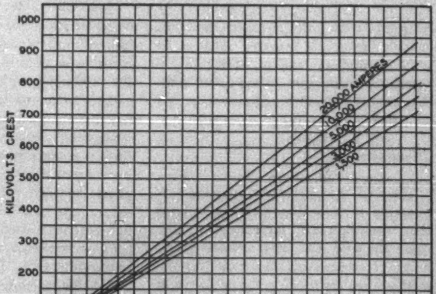

Figure 5. IR discharge voltage, 10–20-microsecond wave; tolerance ±15 per cent

The Measurement of Spot-Welding Current

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CURRENT is an essential factor in the complete specification of spot-welding conditions. Therefore, the need for the measurement of spot-welding currents arises, if optimum conditions determined in experimental laboratories are to be duplicated in production shops. Laboratory methods must be accurate and supplemented by facilities for ready calibration into secondary amperes. Shop methods must be convenient, reasonably accurate, and free from the possibility of accidental error. Shops find current measurement of great value in resetting welding machines for conditions which have previously been found satisfactory.

The problem is complicated by several factors, chief of which is the duration of current flow, which is commonly of the order of one-tenth second, but may be more or less. Another factor is the variable transient current produced by nonsynchronous closure of the welding circuit. A third factor is the wave shape of the welding current. Methods which depend upon the peak value of the current, or the rate of change of the current as it passes through its zero value, will be satisfactory for sinusoidal current waves. However, such methods will be of no value with wave shapes such as are produced by phase control of the firing of mercury-vapor tubes, or the wave shapes of stored-energy discharges.

The present paper includes brief descriptions of a variety of methods of current measurement, together with our evaluation of the methods, based upon experience in using them in spot-welding research programs, and upon the work of Mr. Muller in the preparation of his master's thesis. More recently a number of tests involving simultaneous determination by different methods have been made for the comparisons presented in this paper. The necessary calibrations to convert readings to secondary amperes were also performed. The magnetizing currents for the welding machine were determined through the range of the comparison tests to establish corrections for primary current readings.

Methods of Current Measurement

Figure 1 diagrammatically illustrates various methods of measuring spot-welding current. The solid lines connecting the instruments to the welding circuit indicate the connection which we prefer to use. The dotted lines indicate alternate methods of connection. We shall discuss these methods individually in the following order:

1. Electromagnetic oscillograph with secondary shunt
2. Electromagnetic oscillograph with primary shunt
3. Current-crest meter
4. Cathode-ray oscillograph with primary shunt
5. Pointer-stop ammeter with primary clamp-on current transformer
6. Vacuum-tube voltmeter with secondary current transformer
7. Neon-tube meter with secondary toroid

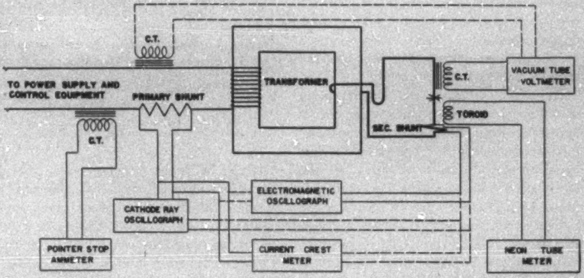

Figure 1. Various methods of measuring spot-welding currents

Electromagnetic Oscillograph—Secondary Shunt

The shunt used with this method is a hollow cylinder of manganin, built into one of the water-cooled electrode holders. The first shunt built for this purpose was silver soldered into the copper electrode holder but this method was found to have softened the copper to an objectionable degree, so that later a double male-threaded construction was adopted. Potential connections to the shunt are of the knife-edge type usually employed for the precise determination of low resistances in the Kelvin double bridge Figure 2 shows a photograph of the arrangement of connections and location of the secondary shunt in the electrode holder. The resistivity of the manganin was determined with the material in the form of a hollow cylinder, about six inches long, in a Kelvin double bridge. The cylinder had an external diameter of one and one-quarter inches and an internal diameter of one-half inch. Calculation shows that the skin effect of this bar is negligible. A twisted pair of conductors carry the potential connections away from the shunt in a direction perpendicular to the electrodes and parallel to the plane of the secondary loop. Care must be taken to avoid the introduction of vertical loops in the circuit, by properly backtracking and arranging the leads at the point where the conductors leave the shunt. The effectiveness of these precautions to eliminate induced voltages from the potential circuit should be checked experimentally. This may be accomplished by taking an oscillogram during the passage of welding current, with the oscillograph connected to the potential circuit which is short-circuited and insulated from the shunt.

The simplicity, directness, and precision of the calibration involved in the use of a secondary shunt make this method a

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Wendell F. Hess is associate professor in metallurgical engineering and head of the welding laboratory at Rensselaer Polytechnic Institute, Troy, N. Y.; Robert A. Wyant is Engineering Foundation research fellow and Albert Muller is International Nickel Company research fellow, department of metallurgical engineering, Rensselaer Polytechnic Institute.

Figure 2. Arrangement of connections and location of secondary shunt in electrode holder

standard of comparison for other methods. If current were the only measurement desired, a simpler method than the use of the secondary shunt could be used. Our greatest use of the method has been in the determination of resistance and energy values in spot-welding circuits. This involves simultaneous determination of voltage drops in the parts of the circuit being investigated, and current measurement with the shunt. An example of this type of measurement is shown in the oscillograms of figure 3. The fact that these voltages and current are in phase indicates that inductive pickup has been eliminated, and that the quantities measured are purely resistive in character.

Electromagnetic Oscillograph—Primary Shunt

This method is simpler than the one previously discussed. A shunt for use in the primary circuit is a standard piece of apparatus, which is therefore easy to purchase. It may even be constructed without much difficulty and is simple to calibrate and install. The use of a primary shunt does not involve as much precaution as does the secondary shunt, to avoid the pickup of induced voltages.

In common with all methods involving measurement of primary current, it is necessary to make measurements of transformer ratio for all winding arrangements which are used. The transformer ratios are obtained by dividing the primary voltage by open-circuit secondary voltages. It is also necessary to correct the total primary current for its magnetizing component before calculating the secondary current. It is common practice to measure the magnetizing current under open-circuit conditions, and in the case of a welding machine, to subtract this from the total primary current arithmetically rather than vectorially. This procedure is not much in error since the power factor of the welding machine is usually so low that the magnetizing and load components of the primary current are nearly in phase. In many cases the magnetizing currents will be so small that they can be neglected, but this should never be done unless tests have shown this to be true. If transformers are overexcited or improperly designed, the magnetizing currents may be large.

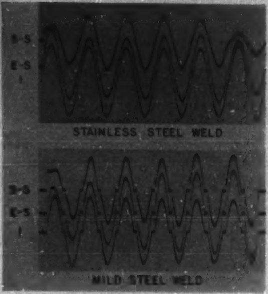

Figure 3. Oscillograms of voltage drops in secondary of spot-welding circuit

S-S—Sheet-to-sheet voltage drop
E-S—Electrode-to-sheet voltage drop
I—Voltage drop across shunt

Current-Crest Meter

This method of measurement uses an electromagnetic-oscillograph element to measure the drop in potential across a shunt in the welding circuit, usually on the primary side of the welding transformer. The oscillograph element deflects a light beam upon a translucent scale, usually graduated in millimeter divisions. No attempt is made to sweep the light beam across the screen and introduce a time axis. The reading of the meter consists in observing the amplitude of the deflection of the spot of light during the passage of the welding current. This reads the peak value of the current. If the wave form of the latter is sinusoidal, the effective value of the current may be calculated by well-known relations. With a little practice an observer may easily read these deflections to a half millimeter, and thus determine the welding current with an error of not over two per cent. These readings are most easily taken when synchronous closure of the circuit avoids the introduction of transients. If transients do occur, their magnitude may be observed. Readings may be taken by this method even if the time of current flow is only one or two cycles. The pointer has such low inertia that there is no problem with rapid response such as provides difficulty with an ordinary ammeter or even with the pointer-stop ammeter.

The writers are not acquainted with a meter which is actually manufactured and sold for this purpose. One of the elements of an ordinary portable, two-element oscillograph has been used by us for this type of measurement. Such a meter would appear to be a very useful laboratory instrument.

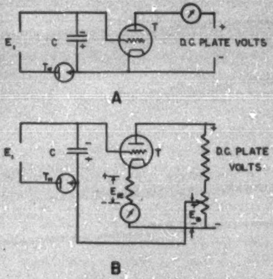

Figure 4. Fundamental vacuum-tube voltmeter circuit

Cathode-Ray Oscillograph—Primary Shunt

A limited amount of experience indicates that a cathode-ray oscillograph may be satisfactorily used in the same way as the current-crest meter above described. The calibration of this meter will not remain permanent so that a means for adjusting the amplifier gain to bring the meter into calibration must be available. A simple circuit, using a voltmeter, variable alternating voltage, and voltage divider, may be set up permanently to accomplish the above calibration easily and as frequently as desired.

A cathode-ray oscillograph is frequently available in a welding laboratory for checking wave forms, transient conditions, and phase-control settings, so that the possibility of using this instrument for current measurement may prove desirable.

A word of caution regarding the use of the cathode-ray oscillograph for current measurement is necessary because of the fact that these instruments are frequently affected by the magnetic field of the welding currents. This difficulty may usually be overcome by placing the instrument at a reasonable distance from the welding machine and by turning the instrument in such a direction as to minimize disturbance. The instrument must be calibrated in the exact position used for current measurement.

POINTER-STOP AMMETER

This instrument is commercially available together with calibrated leads and a clamp-on current transformer for use on the primary circuit. The ammeter consists of an ordinary instrument to which is added an adjustable stop which may be moved up behind the pointer. In measuring the short-duration spot-welding current, the pointer is gradually moved up the scale by adjusting the stop until the pointer just leaves the stop when a weld is made. Thus the current does not have to move the pointer all the way up the scale, but only a sufficient distance to indicate that the current is very slightly greater than the value set by the stop. To determine a value of current, it is necessary to make several welds in order to approach the correct setting of the stop for that value of welding current.

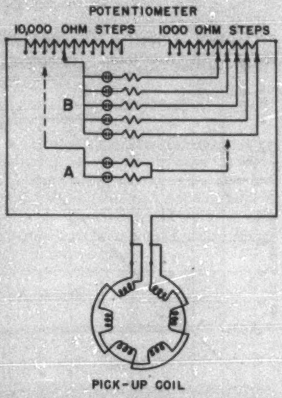

Figure 5. Fundamental neon-tube meter circuit

This instrument possesses a great advantage in simplicity of reading and ease of attachment to the circuit. It is very suitable for setting a welding machine to a particular value of current. It is necessary only to set the pointer, by means of the adjustable stop, to the value desired and then to adjust the current control until the making of a weld causes the pointer to slightly leave the stop. A further advantage of this method is that it may be used to determine the effective value of phase-controlled currents, which of course are not sinusoidal.

Among the disadvantages of this type of meter are the errors resulting with nonsynchronous circuit closure due to the transients produced, and the optimistic readings resulting from the double-frequency deflecting torque. The deflecting torque periodically varies above and below the control torque. When the pointer is moved up to the value of current which should be indicated, the tendency of the control torque to reduce the deflection below the average value is prevented by the pointer stop. Under these conditions the pulses of the deflecting torque above the average value tend to cause the pointer to jump away from the stop. A false indication of a higher value of current is thus given. In consequence of this we have commonly found a pointer-stop ammeter to consistently read five to eight per cent high. Less significant disadvantages consist of those common to all methods of measuring primary current, and which have already been discussed under the section describing the use of the electromagnetic oscillograph with a primary shunt. We have found also that the pointer-stop ammeter reading falls off when the time of current flow is three cycles or less. The amount of this falling off is not serious for a welding time of three cycles and amounts to only four per cent in two cycles.

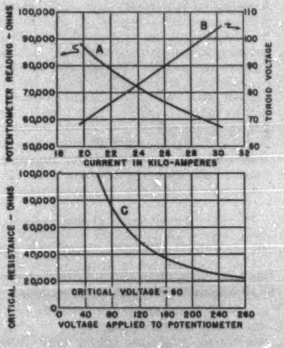

Figure 6. Characteristics of neon-tube meter circuit

A—Meter calibration
B—Toroid characteristic
C—Potentiometer characteristic

VACUUM-TUBE VOLTMETER

Our experience with this type of instrument for the measurement of current has been made possible by the generosity of two men[1,2] who have been responsible for original development work. Each has kindly loaned us his instrument for study. Figure 4 shows the fundamental circuits in which the vacuum-tube voltmeter is used to measure the welding current in terms of the drop in potential across a shunt connected in series with the secondary circuit of a current transformer. The current transformer which we have used is designed for use in the secondary circuit by slipping the transformer over the electrode holder. The basic principle involved in this method is illustrated in figure 4*A*. The voltage drop, E_1, across the shunt on the current transformer charges the capacitor *C* through a rectifying tube T_R. This causes the grid of the measuring tube to become more negative, thus reducing the plate current. The capacitor is expected to retain its charge and thus maintain the reduced plate current for a sufficient time to permit reading. Figure 4*B* illustrates a second stage in the development of the circuit of this meter. Two desirable features of adjustability are provided in this circuit. The sliding contact with the indicated voltage drop, E_B, permits adjustment of the plate current to the de-

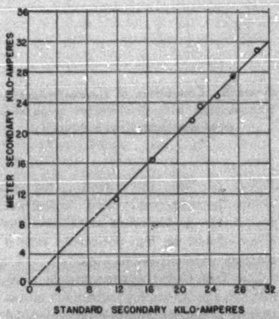

Figure 7. Comparison of primary-shunt measurements with simultaneous secondary-shunt measurements as a standard using an electromagnetic oscillograph in both cases

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[1] For all numbered references, see list at end of paper.

sired top-scale value, before each reading. The resistance associated with the voltage drop E_B in the cathode lead permits adjustment of the over-all sensitivity of the circuit.

One of the principal advantages of this instrument is that the readings are definite and may be obtained with one weld. Also, the secondary current may be measured directly. With circuit refinement to delay the charging of the capacitor the meter may be used to measure welding currents with large transients existing during the first few cycles. Such transients are caused by nonsynchronous closure of the welding circuit, and render inaccurate the measurement of current by almost all methods except the oscillographic.

The difficulties with the vacuum-tube voltmeter are associated with the current transformer and with the fact that it is a peak-reading instrument. The design of an iron-core current transformer to handle such heavy currents involves some sacrifice in portability. The introduction of so much iron into the secondary current loop affects somewhat the output of the welding machine. In our case with a short-throat machine, the output is reduced about three per cent when the current transformer is placed in position. This effect should be less noticeable with a machine of larger throat area.

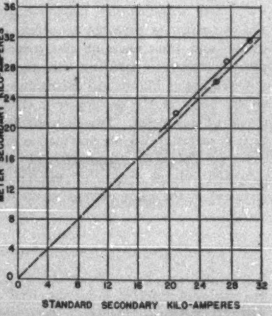

Figure 8. Comparison of current-crest-meter measurements with simultaneous secondary-shunt measurements as a standard

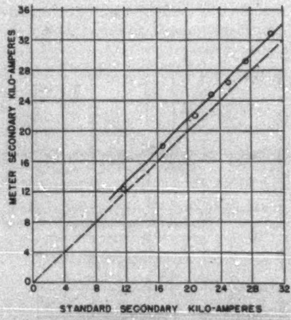

Figure 9. Comparison of pointer-stop-ammeter measurements with simultaneous secondary-shunt measurements as a standard

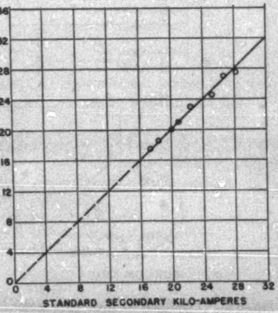

Figure 10. Comparison of vacuum-tube voltmeter measurements with simultaneous secondary-shunt measurements as a standard

The fact that the vacuum-tube voltmeter is a peak-reading instrument means that it is not suitable for nonsinusoidal currents such as are caused by the phase control of mercury-vapor tubes. A suggested method of dealing with these currents is impractical to use since it involves the use of full heat, which probably cannot be absorbed by the work.

NEON-TUBE METER

A meter that employs neon-glow lamps as voltage indicators in conjunction with a potentiometer and pickup coil was also investigated. The circuit of this meter is shown in figure 5. An air-core pick-up coil of the toroidal form is used in the secondary circuit of the welding transformer. The voltage induced in this coil is applied across a potentiometer. The original and basic voltage indicating circuit[8] employs two neon-glow lamps connected in parallel across the same points on the potentiometer as shown at figure *5A*. Lamps having nearly the same critical voltages are selected for this purpose. As welds are made, the potentiometer connections are progressively changed until a point is reached at which just one of the lamps glows. The magnitude of the secondary current is then obtained directly by reference to the meter calibration.

In the present investigation it was desired to determine the approximate potentiometer setting more quickly and with fewer welds. Therefore a bank of five matched neon-glow lamps is employed as shown at figure *5B*. By means of switches the entire range of the potentiometer can be explored in four steps. At each step the number of lamps that glow when a weld is made is observed and the approximate setting is that above which all lamps glow. Frequently, however, only one or two welds are necessary to obtain an approximate setting. A high resistance is connected in series with each lamp to avoid the shunting effect of those lamps that pass current. After the approximate setting is determined in this way the final meter reading is determined by means of the basic two-lamp circuit which has already been discussed.

A typical calibration of this meter is shown in figure *6A*. This calibration was obtained by using the electromagnetic oscillograph and secondary shunt as the standard means of current measurement. The voltage induced in the toroid is a direct measure of the secondary current, if it is sinusoidal. A voltage-current characteristic of the toroid used in this investigation is shown in figure *6B*. In order to control the voltage applied to the potentiometer, provision is made for connecting the pickup coils in series for measuring low currents and in parallel for high currents. This is very important because, as shown in figure *6C*, the change in critical resistance for a given change in applied voltage becomes much greater as the applied voltage approaches the critical voltage of the lamps.

This type of meter is probably most useful in its basic form. Its primary advantage is its simplicity and low cost. When used simply as an indicator it does not necessarily have to be calibrated. For example, after the optimum welding conditions have been established for a given job, the reading of the meter can be recorded and then used in checking when the job is set up again at a later date. In our experience this meter was only used in measuring synchronously-timed currents. Its use with nonsynchronous timing should not be attempted since the maximum rate of change of welding current, which the meter actually measures, is only simply related to effective value for sinusoidal currents. Calibration and use of the meter requires that the critical voltage of the lamps remain constant with time, a relationship about which more information is needed. In general, experience thus far indicates that further development of this type of meter might prove to be worth while.

Comparison

Figures 7, 8, 9, and 10 are plotted for comparison of results by different methods. These are plotted using simultaneous secondary shunt measurements with an electromagnetic oscillograph, as a standard of comparison. The dotted curves would represent exact agreement. The solid lines represent the instrument readings. The primary shunt and vacuum-tube voltmeters give excellent agreement. The current-crest meter seems to have an optimistic tendency although the readings are within the limits of experimental error to be expected with this method—that is, about five per cent. The decidedly optimistic tendency of the pointer-stop ammeter is evident in figure 9.

It is hoped that this experience may prove helpful to those who have the problem of measuring such currents, and that it may be an encouragement to others to continue the development of these and possibly better methods of spot-welding-current measurement.

References

1. G. G. Somerville, General Electric Company, Pittsfield, Mass.

2. L. H. Frost, Thomson-Gibb Electric Welding Company, Detroit, Mich.

3. A NEW GENERAL-PURPOSE METER FOR RESISTANCE WELDING, C. Stansbury. The American Welding Society *Journal*, September 1935, pages 82–5.

Discussion

C. Stansbury (Cutler-Hammer, Inc., Milwaukee, Wis.): For several years we have been using the neon-tube type of meter. In our design the two neon tubes, calibrated rheostat, and switch for selecting the range are mounted directly on one side of the case in which the air-core toroid is built. Recent models are arranged so that the toroid can be opened up by means of a hinged construction for convenience in placing it around the welding electrodes or primary conductors. Also in recent models only one neon tube is employed. It has been found that these small neon tubes have a slightly different starting voltage for the two halves of the a-c wave and about the same result in bracketing the reading can be obtained with one tube as with two, with less chance for confusion.

With regard to using this meter on nonsynchronous circuits, although no great precision is obtainable, results sufficiently accurate for most practical purposes can be obtained by making a number of welds and using the highest setting which causes a glow on all of the welds. Welds on which the circuit happens to be closed so as to cause minimum transients correspond to the lowest setting of the instrument for which a glow is obtained. We use the meter in either the primary or secondary circuit as most convenient.

As we are not manufacturing this meter commercially, we have not been able to follow up certain things about which we would like to have more information as a matter of general interest.

Some time ago I had occasion to use the meter in the secondary circuit of a spot welder in which the primary current was controlled by ignitron tubes. These ignitron tubes were not synchronously controlled, but were controlled by a relay timer which caused random completion of the circuit. Under these conditions, the meter could not be used at all in the secondary circuit since the tubes glowed at all positions of the dial. When the meter was connected in the primary circuit, it worked satisfactorily. In using the meter in the primary circuit we ordinarily use a flexible, heavy connector and make several passes through the toroid.

Although I did not have an opportunity to determine the reason for this with an electromagnetic oscillograph as I wished, I did try to do so with a cathode-ray oscillograph. This instrument was so seriously affected by the magnetic field around the welder that I could not get any satisfactory results. As the authors state, the cathode-ray oscillograph is very unsatisfactory around a resistance welder for this reason.

A point which I would like to see brought out clearly, either in an appendix to this paper or elsewhere, is the difference in characteristics between an air-core toroid operating with negligible back ampere-turns and the conventional iron-core current transformer.

G. F. Gardner (nonmember; General Electric Company, Schenectady, N. Y.): By drawing attention to the necessity for accurate current measurements in welding, the authors have performed a great service to all who are in any way interested in spot welding. The measurements have evidently been carried out with great care and each method has been given the consideration which it deserves.

In connection with the electromagnetic oscillograph-secondary shunt method, the authors do not state the highest values of current which have been measured in their tests, but it may be of interest to note that measurements have been made on welding machines using similar shunts at currents up to 80,000 secondary amperes.

The electromagnetic oscillograph-primary shunt method is also of definite value. It would seem, however, that if it were to be generally applied, the use of a current transformer might be somewhat preferable to the shunt from the standpoint of ease of application and safety to the operator. Care should, of course, be taken to select a transformer whose limitations are not exceeded.

Current-Crest Meter. In considering this and the methods which follow, attention should probably be directed to the quantity which it is desired to measure—either the root-mean-square or the peak value of the current. Granted that it is possible to measure some transients and take account of their influence using this type of instrument, it nevertheless must be borne in mind that its indications are as dependent as any other of the crest measuring instruments upon the peak value of the wave. It was noted with considerable interest that the authors mentioned that such an instrument would be a useful laboratory tool. Experimental samples of such devices have been constructed and it is hoped that one will appear on the market shortly.

Cathode-Ray Oscillograph—Primary Shunt. The word of caution regarding the use of this instrument is certainly not out of place. Some of our experience indicates that a reasonable distance for an unshielded instrument may be as much as 40 feet from the welding machine.

Pointer-Stop Ammeter. It was gratifying to note the authors' experiences with this relatively simple type of instrument since at this stage of the welding measurements art, it appears that the choice is often between a simple means of current measurement or none at all. As regard the "optimistic" indications, this is not an essential feature of the pointer-stop ammeter and instruments can be so designed that they show but very little, if any, increase in reading due to the conditions mentioned by the authors.

The closing paragraph of the authors' work is especially significant and, in line with this, it may be of interest to mention further work which has been done along this line. A device has been made available known as the weld recorder which will give an accurate indication and record of the ampere-squared-seconds existing for a given weld. If the ampere-squared-seconds are not within preset limits, such as plus or minus five per cent, a bell rings and the machine is automatically locked out. The record will show whether the I^2t is above or below the limits. Nearly 100 of these have been made and have been found to have been very useful in establishing levels for resistance-welding measurements and, although their use has been restricted to comparative work at the present time, there is no reason why they could not be applied where definite values of ampere-squared-seconds are required. Space does not permit a full description of the instrument characteristics here, but it is hoped that it will be possible to publish an article containing this at an early date.

A. J. Corson and **E. W. Clark** (nonmember; both of General Electric Company, Lynn, Mass.): This paper is a very thorough and balanced evaluation of several existing methods of measuring spot-welding currents. It is hoped that it will stimulate interest, not only in the improvement of these methods, but in the application of them as well. The following discussion is based on a somewhat similar series of experiments, during which a comparison of the secondary shunt and oscillograph was made with the pointer-stop ammeter and the vacuum-tube voltmeter.

Figure 7 shows excellent agreement between the primary- and secondary-shunt readings. The primary-shunt readings were presumably multiplied by the welding-transformer ratio as determined by the voltage method. This indicates an absence of errors due to such causes as saturation of the welding transformer, and varying current distribution in the secondary shunt, for the particular conditions of this test. It is doubtful if these conditions would apply to all welding transformers and all secondary loops. For these reasons, we prefer the use of the current method in determining this transformer ratio using a specially designed current transformer in the welder-transformer secondary since it more nearly simulates operating conditions and also avoids the necessity of measuring very low alternating voltages. Also in the interest of minimizing current distribution errors in the secondary shunt, this device was reduced to a minimum axial length 0.080 inch, as compared to the six-inch dimension described in the paper.

The response characteristic of the pointer-stop ammeter is, as indicated by the authors, somewhat dependent on the duration of the impulse. It would be of interest to know, therefore, what duration was used in determining the curve given in figure 9, also what interval of travel was allowed for the pointer oscillation. It would appear that this characteristic, while definite for an instrument of given design constants, is not universal, and by modification of these constants, improvement could be obtained in accuracy. In the meantime the application of a simple correction curve will result in a highly satisfactory degree of accuracy for all ordinary work.

W. F. Hess, R. A. Wyant, and A. Muller: In answer to the implied question in the second paragraph of Mr. Gardner's discussion, the maximum secondary root-mean-square amperes which we have had occasion to measure with the oscillograph and secondary shunt is 40,000 amperes.

Mr. Gardner makes a good point in his third paragraph with regard to the safety of the operator when using a shunt in the primary circuit. Care should be taken to mount the shunt so that there will be no occasion to come in contact with it while the primary circuit is energized, and to bring out insulated potential connections. The substitution of a current transformer for the primary shunt in oscillographic work is open to some objections. The wave forms may be distorted by transient currents produced in the secondary of the current transformer. These may be reduced by careful adjustment of the phase angle of this circuit to coincide with the angle of firing of the main power tubes. This adjustment would be an objectionable added complication. The calibrations involved are more complicated. The selection of a shunt to be used with the current transformer and oscillograph must be carefully made to secure good sensitivity and at the same time to avoid overloading the current transformer. Great care must be taken to insure continuity of the secondary circuit of the current transformer to avoid the hazardous voltages which are produced by an open-circuited current transformer. The more precise current transformers are usually not of the clamp-on type. It is therefore almost as difficult to attach them to the welding circuit as to install a shunt. Phase-angle errors are objectionable when simultaneous measurements of voltage drops and current are desired.

Our experience coincides with that of Mr. Stansbury in that the two electrodes of a single neon tube are as effective as two separate tubes in determining the final setting of the voltage divider which measures the toroid voltage.

In response to the suggestion in the last paragraph of Mr. Stansbury's discussion we offer the following. When a toroidal coil is placed around a conductor carrying an alternating current, a portion of the magnetic field set up by the current will link with the coil and induce a voltage proportional at any instant to the rate of change of the current. The peak voltage which we measure is therefore a measure of the maximum rate of change of current. Only for sinusoidal currents is the effective or heating value proportional to the maximum rate of change of the instantaneous value. Hence only such currents will be correctly measured by the peak value of the voltage developed in a toroidal coil.

A precaution that must be exercised in the use of a toroid is that the voltage must be measured without drawing appreciable current from the winding, since any current taken will tend to distort the flux set up by the current being measured, displacing it from within the toroid and causing the toroid voltage to drop.

In the case of the iron-core current transformer, a current must be permitted to flow in the secondary winding which balances at every instant the magnetic effect of the primary current. If such a secondary current does not flow, the flux density in the core will tend to rise and the secondary current will no longer be proportional to the primary current. Also the primary winding and iron core will act as a choke coil or reactance in the power line, and will absorb large energy losses from the line which it is incapable of dissipating. On the other hand if the resistance of the secondary circuit is kept sufficiently low, so that the current-transformer core does not saturate, then the current in the secondary will be a direct measure of the primary current even with considerable departure from sinusoidal wave shape.

In reply to the discussion of Messrs. Corson and Clark, the transformer ratios used in calculating secondary current from primary readings were, as stated in the body of our paper, determined by voltage readings. It is true that for the comparison tests presented in the paper the magnetizing currents were so low as to be negligible, indicating the absence of any approach to saturation of the welding transformer. Before using any method involving primary-current readings we have recommended the measurement of magnetizing currents for all settings of the welding transformer. If the magnetizing currents are found to be appreciable, as they may be on the higher taps of the transformer, the primary readings should be corrected for magnetizing current, before applying the transformer ratio to calculate the secondary current.

If the transformer core is approaching saturation, as evidenced by relatively high magnetizing current, there will be an error in determining the transformer ratio by voltage readings, due to primary leakage impedance. The voltage drop due to this impedance should be subtracted from the applied primary voltage to obtain the primary voltage induced by the mutual flux, which latter should be divided by the secondary open-circuit voltage to obtain the true transformer ratio. Owing to the difficulty of measuring the separate primary leakage impedance, another approach to the determination of transformer ratio is preferable. For those welding-machine connections which give rise to appreciable magnetizing current at rated voltage, the primary and secondary voltage readings should be taken with the primary voltage at such a reduced value as will avoid an excessive magnetizing current. The turn ratio may then be calculated from these readings without appreciable error.

The use of primary and secondary current transformers to determine the transformer ratio involves the use of special costly equipment not readily available, and would appear to involve at least as much chance for error in the two transformers and accompanying meters as should be expected with the voltage method. We have found no difficulty in measuring the low alternating voltages, with a good laboratory instrument. The axial length of the shunt used for secondary current measurement was approximately three-quarters inch between potentiel connections. The total length of manganin in the secondary circuit was about one inch. The six-inch dimension mentioned in our paper was the length of the manganin cylinder used in the calibration measurement to determine resistivity.

The duration of current flow for the curve given in figure 9 was six cycles. It had been determined that no definite reduction in reading occurred until impulses of less than four cycles duration were used. The interval of travel allowed for the pointer oscillation was approximately $^1/_{100}$ inch, which was about the minimum definitely perceptible.

Vector-Response Indicator

B. D. LOUGHLIN
ENROLLED STUDENT AIEE

THE performance characteristics of a communication circuit are often conveniently described in the form of a response curve which indicates the steady-state transmission of the circuit at frequencies within the range of interest. These curves can be obtained by making a sufficient number of individual measurements of transmission at various frequencies within the band of interest and plotting the results; however, considerable time may be saved by the use of a device which indicates directly the desired curve. Many such response-curve tracers are found in radio laboratories, with the curve of transmission or amplification as a function of frequency being obtained as a trace on a cathode-ray tube. However, strictly speaking, the transmission of a circuit, being the ratio of output voltage to input voltage of the same frequency, is a vector quantity having an angle as well as a magnitude—the angle being the phase angle between the output voltage and the input voltage. The response-curve tracers now in general use indicate only the magnitude of the amplification as a function of frequency. The vector-response indicator described in this paper is a unique apparatus that indicates directly both the magnitude and phase angle of the transmission as a function of frequency. This vector response of the circuit is indicated by a special type of pattern on the screen of a simple cathode-ray tube.

Figure 1. Comparison between elliptical-trace and reference-point methods of phase-angle measurement

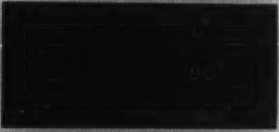

Figure 2. Reference-point method used for a direct-reading high-frequency phase-meter

Some of the new communication systems recently developed have brought about an appreciation of the importance of phase characteristics. For accurate reproduction of a complex voltage wave the circuit must have, over the range of frequencies present in the wave, the properties that the magnitude of the transmission is constant and the phase angle is a linear function of frequency with the intercept at zero or $n\pi$. Except with circuits of long time delay, such as long telephone lines, little or no thought need be given to the phase characteristic requirement in sound circuits, since the ear is not sensitive to moderate phase displacements. However, in television and facsimile systems the complex wave shape must be preserved for acceptable reproduction, and this means that the desirable characteristics must be preserved in both magnitude and phase.

Phase-angle measurements above audio frequencies can be made from the elliptical trace produced on a cathode-ray tube screen when the two sine-wave voltages to be compared are impressed on the horizontal and vertical deflecting plates respectively. The phase angle can be calculated from measurements of the geometry of such a trace. If the phase angle were to vary periodically, as might be caused by a periodic variation of frequency, a series of elliptical traces would be produced, but this trace on the cathode-ray-tube screen would not indicate in a direct manner the variation of phase angle as a function of frequency.

For the vector-response indicator, it was necessary to develop a new method for measuring phase angles, which would give a more direct indication of the angle. This new method differs from the elliptical trace method in that one of the two voltages to be compared is applied to the vertical deflecting plates, the other voltage being used to give a reference pulse which is applied to the control grid of the cathode-ray tube. The reference pulse is obtained from some particular point in the cycle, such as the positive peak; this reference pulse is applied to the grid with such polarity that it causes the beam to become brighter. With a linear saw-tooth sweep applied to the horizontal deflecting plates, the sine-wave trace of the voltage on the vertical plates is seen, together with a bright spot at the instant that the other voltage reaches its positive peak (or other reference point). Thus, the location of the spot along the trace is a direct indication of the phase angle between the two voltages. Figure 1 shows a comparison between the ordinary elliptical trace and the reference-point methods for several phase angles.

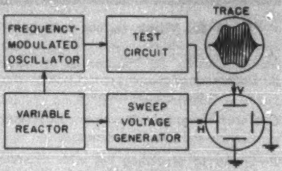

Figure 3. Simplified block diagram of a response-curve indicator

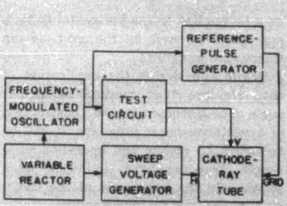

Figure 4. Simplified block diagram of a vector-response indicator

If no sweep voltage is applied to the horizontal deflecting plates, the trace will be merely a vertical line with a bright spot on it; the location of the bright spot indicates the phase angle between the voltages. An appropriate scale might be placed alongside this line and the phase angle read off directly. This is one way of using the reference point method to make a direct-reading high-frequency phase meter. However, to make the exact location of the spot along the line more evident, the pulse voltage

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Figure 5. Vector-response patterns

A—Simple resonant circuit

B—Double-tuned coupling circuit with greater than critical coupling

C—Three-element circuit with one inductance and two capacitances

D—Similar to preceding but with different ratio of capacitances

may be applied to the horizontal deflecting plates as well as the grid, giving traces such as figure 2.

Before considering the application of the reference-point method of phase-angle measurements to the construction of a vector-response indicator, it will be well first to review the operation of a simple response-curve indicator that shows only the magnitude of the transmission and not the phase angle.

If a constant sine-wave voltage is applied to the input of a circuit under test, the amplitude of the output voltage, with respect to the input voltage, indicates the transmission of the circuit at that frequency. If further, the frequency of the input voltage is slowly varied, but its amplitude always kept constant, then the variation of amplitude of the output voltage indicates, in some manner, the variation of transmission of the circuit with frequency (that is, the response curve). By varying the frequency periodically, and applying the output voltage to the vertical deflecting plates of a cathode-ray tube, a vertical trace, whose length varies periodically, will be produced. Then by applying a sweep voltage of the same period as the frequency variation to the horizontal deflecting plates, the variation of the length of the trace will be visible. The result will be a trace, symmetrical about a horizontal axis, whose envelope indicates the response curve of the circuit over the range of applied frequencies. Figure 3 shows a simplified block diagram for such a response-curve indicator.

The simple response indicator just described can be easily made into a vector-response indicator by a circuit addition. By adding a circuit which derives a pulse at some reference point in the cycle of the voltage applied to the input of the circuit, and then applying this pulse to the grid of the cathode-ray tube, an indication of the phase angle is obtained (figures 4 and 5). The trace produced now has a bright line appearing within the envelope of the trace. At any abscissa, the relation between the ordinate of this bright line and the ordinate of the envelope indicates the phase angle of the circuit at the frequency corresponding to the particular abscissa. If the reference pulse is obtained at the positive peak of the input cycle, then when the bright line coincides with the top envelope, the output voltage is in phase with the input. When it coincides with the lower envelope, they are 180 degrees out of phase. It may be desirable at times to have a 90-degree leading phase angle indicated by coincidence with one envelope and a 90-degree lag by coincidence with the other envelope. This can be accomplished by producing a 90-degree phase shift between the voltage applied to the test circuit and that applied to the reference pulse generator.

Figure 6. Vector-response patterns for a band-pass amplifier with a double-tuned input circuit and a single-tuned output

A—Proper adjustment of all circuits

B—Similar to *A* with single-tuned circuit tuned above mid-band frequency

C—Insufficient loading in double-tuned circuit

D—Similar to *C* with single-tuned circuit tuned below mid-band frequency

E—Less than critical coupling in double-tuned circuit

F—Similar to *E* with single-tuned circuit tuned above mid-band frequency

A simplified block diagram of the vector-response indicator is shown in figure 4. The variable reactor can be a motor-driven variable capacitor, or a reactance tube, that is, a vacuum-tube circuit arranged to appear like a reactance. The sweep oscillator is synchronized with the variable reactor so that they have the same period. A simple reference-pulse generator might be a vacuum-tube biased beyond cutoff to such an extent that plate current flows only at the positive peak of the applied alternating voltage.

Some illustrations of traces obtained on a vector-response indicator using simple circuits are shown in figure 5. It will be noted that in figure 5*C* there are two zero power factor frequencies, while in figure 5*D* the circuit has capacitive reactance at all frequencies. Figure 6 shows results using a band-pass amplifier with one double-tuned coupling circuit and one single-tuned circuit operating at 175 kc.

All the oscillograms were photographed directly from a small cathode-ray tube (type 902) having a screen of two inches diameter. The apparatus constructed for these tests differed from the simplified block diagram of figure 4 by the use of a beat-frequency signal generator instead of a simple oscillator to generate the desired signals. Properly designed

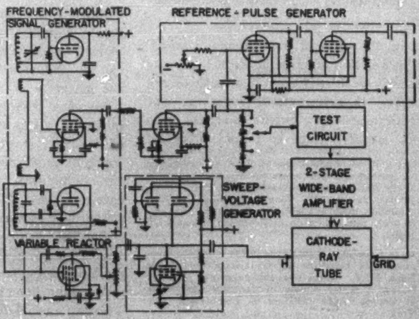
FREQUENCY-MODULATED SIGNAL GENERATOR
REFERENCE-PULSE GENERATOR
TEST CIRCUIT
2-STAGE WIDE-BAND AMPLIFIER
SWEEP-VOLTAGE GENERATOR
V
CATHODE-RAY TUBE
H
GRID
VARIABLE REACTOR

Figure 7. Circuit diagram of vector-response indicator used in tests

amplifiers were added where necessary. Figure 7 shows the circuit diagram of the apparatus.

Conclusion

An apparatus has been described that is believed to be new and useful. From the ideas presented there can be developed various phase measuring equipment, such as direct-reading high-frequency phase-meters, phase-characteristic indicators, phase-curve tracers, and time-lag-curve tracers, as well as the vector response indicator described. The increasing use of television and frequency modulation systems should make phase-measuring apparatus such as these of increasing importance in circuit investigation and testing.

Causes of Corrosion of Fine Copper Wires Carrying a Potential

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COPPER CORROSION is a serious problem in electrical equipment which uses very fine wires carrying a potential, the outstanding examples being various types of coils used in radios and ignition systems. The usual type of corrosion involves the formation of green material on the surface of the wire, which is obviously due to chemical reaction of the copper with materials in contact with it. As this chemical reaction progresses at a given point in the wire, the cross section of unchanged copper gradually diminishes until finally a break occurs, opening the circuit and rendering the coil useless.

Previous Views of Copper Corrosion

In the past, the question of the actual cause or causes of copper corrosion has often been approached by means of inference rather than experiment, and it is, therefore, not surprising that widely varying views have been held by different people. Probably one of the most popular beliefs is that free acids in materials contacting the wires are responsible. This belief has been the basis of specifications of "acid-free" papers and tapes to be used in the construction of coils. In some cases, specifications are given which require that a distilled water extract prepared by some standardized technique must fall within certain limits of pH. More recently it has been recognized that acids are not the only substances which might cause this effect. Assuming that electrolysis is the fundamental cause of corrosion, it would follow that not only acids but also bases and salts would produce the same effect.

Another similar view is based on the idea that the rate of corrosion of copper wires in contact with a given material is proportional to the conductance, or inversely proportional to the insulation resistance of the material in question.

Again, it has been a common observation that copper corrosion is most serious in high humidities, and this fact has given rise to the belief in some quarters that moisture absorption is the primary factor in corrosion.

Corrosion by Adhesive Tapes

The present study was primarily concerned with the causes of corrosion by adhesive tapes used in the fabrication of coils, and although the results have implications of broader significance, we shall first deal briefly with the specific problem which prompted this work.

Adhesive tapes may be considered as a combination of two functional parts, the adhesive and the backing. We may conveniently consider these in order.

At the present time, electrical-tape adhesives are mainly of the rubber-resin type, although in the past, glue has been used. The use of the latter may be disposed of rather summarily, as it is now known that glue hydrolyzes rather rapidly in high humidities at elevated temperatures, with the production of organic acids of sufficient activity to be a major cause of corrosion. The ill-advised attempts to correct the difficulty by using dialyzed glues, which could be made substantially electrolyte-free, resulted in only a transitory improvement; because in use, the glue itself was a source of electrolytes.

Experimental Study of Corrosion

In view of the fact that corrosion by the more satisfactory electrical tapes developed so slowly under any practical operating conditions, it was necessary to develop an accelerated corrosion test. We therefore canvassed the radio industry, and finally standardized on a test similar to the most severe ones in use. The arrangement consists essentially of two parallel bare copper wires at a potential difference of 90 (later increased to 250) volts direct current held under tension against the surface to be tested, under conditions of 90 per cent relative humidity and 120 degrees Fahrenheit. The specimen was placed on the bowed surface of a moulded block with two binding posts at each end and was held in position by two parallel bare copper wires, spaced $^3/_{16}$ inch apart and each held under slight tension by a pair of binding posts. Originally, number 40 wires were used, but later it was found more satisfactory to use number 32 wires and estimate the amount of corrosion microscopically. At 90 volts, the usual duration of the test was four days.

Glues, however highly purified, were found in this test to be highly corrosive. Purification delayed slightly the beginning of corrosion, but did not result in an important improvement.

The rubber-resin type of adhesive, on the other hand, is not a cause of corrosion, provided that reasonable care and common sense is used in its formulation. The usual adhesive made from rubber, rosin, and zinc oxide also yields, during artificial aging which would correspond to at least five years of natural aging in the absence of light, products which contribute nothing toward corrosion. Thus, in spite of the fact that the very term, "acid", to the layman, is the symbol of death and destruction, we find that an adhesive which contains a large percentage of abietic acid is noncorrosive. Abietic acid and similar resin acids are so weak and so difficultly soluble in water that their acid properties may be neglected.

Corrosion by Cellulosic Backings

The elimination of the adhesive as a factor in copper corrosion by pressure-

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Table I. Insulation Resistance at 120 Degrees and 90 Per Cent Relative Humidity

	Megohms	
	Unextracted	Extracted
Rayon (viscose)	3	112
Linen	4	
Silk	20	4,000
Wool	1,700	1,400
Cellulose acetate	5,000	10,000

sensitive tapes leaves the backing as the remaining possibility. At first it was assumed that impurities in the paper or cloth were responsible, and we therefore tested a wide variety of papers ranging from cheap wrapping papers through purified papers for electrical uses, and finally, the most highly purified alpha-cellulose obtainable, in the form of ash-free filter paper. The extracts of these various papers, prepared according to ASTM standard *D*-202-34*T*, section 45, showed large differences in both pH and electrolytic conductivity, but although the low-grade papers caused very bad corrosion, we were surprised to find that there was no significant difference in this respect between the most highly purified alpha-cellulose and any good saturating paper. The inference was plain that the cellulose itself (in the presence of water) was causing corrosion, but we were at first so reluctant to accept this conclusion that we attempted to purify the cellulose further by exhaustive extraction, in a syphon extractor, with highly purified water. An additional extraction with highly purified ether was performed in some cases. However, all such purified samples behaved similarly, and there was no noticeable improvement over the unextracted samples. A similar study of the effect of purification on cotton fabrics yielded exactly parallel results.

Now, if cellulose itself, in the presence of water is a source of copper corrosion, cellulosic materials other than paper or cotton ought to show the same behavior. We therefore studied samples of linens and viscose rayons, all free from loading or sizing materials, and we found that they caused corrosion at essentially the same rate as cotton or paper. Regenerated cellulose films such as are used as the base for Cellophane behaved in the same manner. Furthermore, we found that exhaustive extraction of any of these materials as in the case of paper, produced no perceptible improvement. Other fabrics made from natural fibers, such as silk and various kinds of wool, were also found to cause corrosion, but wool was very much superior to all other natural fibers in this respect. Again, it was obvious that impurities were not responsible because exhaustive extractions failed to produce any improvement. It is rather surprising that silk is not much better than cellulose in the matter of corrosion whereas wools, which are also protein fibers, are very much better than either silk or cellulose.

Noncorrosive Materials

The elimination of paper and all fabrics made from natural fibers then led us to search for a synthetic material which would be free from corrosive nature. Briefly, it has been found that there are a number of materials which are satisfactory from a chemical viewpoint; for example, cellulose esters and ethers, rubber and some of its derivatives, and a number of synthetic resins from the polymerization of styrene, vinyl derivatives, or methacrylates. Of the above materials, cellulose acetate is the most economical to use and the one with the best mechanical properties; consequently it is the only one which has been exhaustively tested over long periods of time. It has been tested both in the form of transparent sheets and in the form of cloth woven from the fibers, and apparently is completely free from tendency toward copper corrosion. There is every indication that a fine copper wire in contact with cellulose acetate will last as long as one suspended freely in air.

Possible Causes of Corrosion

After having been compelled to recognize the fact that cellulose itself was a source of corrosion, the next step was to inquire into the reason and several possibilities were investigated. One obvious possibility, to which reference has already been made, is that the corrosive nature of a given substance is a function of its absorptive (or adsorptive) capacity for water. Only a cursory examination of the facts, however, is sufficient to show that this is not so. Wool shows a much higher moisture absorption than cotton, yet is far superior with regard to freedom from corrosive nature. Also, cellulose acetate shows a moisture absorption which is reported to be over half as great as for cellulose itself, yet it is completely free from corrosive nature.

If corrosion is fundamentally due to electrolysis, a flow of current would necessarily be involved. Conductivity (or its reciprocal, insulation resistance), ought therefore to parallel the rate of corrosion by various test samples. Accordingly, a series of measurements was made on fabrics of approximately 0.006 inch thickness as nearly alike as possible in density of weave, and all free from loading and sizing materials. The specimens were mounted on binding posts $1^1/_2$ inches center to center, and were conditioned for 48 hours at 120 degrees Fahrenheit and 90 per cent relative humidity before measuring resistance under those conditions. A parallel series of measurements was then made on the same fabrics after they had been exhaustively extracted with highly purified water. We believe that the results are sufficiently striking to merit including them (table I).

A glance over the trend of the insulation-resistance values for the unextracted samples might seem to indicate that the real explanation of corrosion was to be found here. However, when we pass on to the values for the extracted samples, we find that the enormous increases in resistance for rayon and silk completely refute the idea that corrosion parallels insulation resistance. For example, a 20-fold, or more, increase in insulation resistance by extraction results in no appreciable decrease in corrosive nature. There still remained the possibility that insulation resistance might decrease rapidly during the period of the corrosion test, but this was shown not to be so. It seemed impossible that the corrosion process could be independent of any flow of current, but our results indicated clearly that flow of current in itself was not the controlling factor.

Electrochemical Oxidation as a Cause of Corrosion

As a last resort, we attempted to test the possibility that electrochemical oxidation of cellulose, in the presence of water, might take place at the anode, and we were finally rewarded with the correct explanation of the observed copper corrosion. If the cellulose were oxidized at the anode, water-soluble organic acids would be formed and ought to be capable of detection. For test purposes, it was necessary to replace the copper wires by some metal which would not react with such acids. We therefore used platinum wires in contact with rayon and carried out the test in other respects exactly as the standard corrosion test described above. At the end of six days, the specimen of rayon was examined for chemical changes in the cellulose. By means of Harrison's reagent[1] it was found that the cellulose had acquired reducing properties throughout the whole area between the wires, indicating either hydration or oxidation. By means of indicators, it was shown that free acid was present only along a thin line which had been in contact with the positive wire. It is a commonplace fact that green copper corrosion always appears on the positive wire. Hence, the production of water-soluble acids at the anode offers an explanation of this corrosion which appears to be inescapable. Incidentally, it can readily be demonstrated on a large scale that cellulose is electrochemically oxidized to a complex mixture of water-soluble organic acids. By grinding up cellulose fibers and suspending them in water in an oxidation cell, large quantities of these acids may be obtained.

The above explanation of corrosion also offers an obvious explanation of the failure of exhaustive extraction to reduce appreciably the corrosive nature of paper or cellulose fabrics. If appreciable amounts of water-soluble acids are formed by the oxidation of cellulose, the presence or absence of traces of electrolytes in the original paper or cloth is quite unimportant. Therefore, as far as corrosion problems are concerned, extremely rigid specifications on paper or cloth probably do not accomplish anything useful. This does not imply, of course, that any low-grade material is just as good as a highly purified one, but merely that there is a definite limit to the usefulness of purification. The initial presence of large amounts of electrolytes is, of course, bound to have an effect.

Only in the case of cellulose has it been possible, by means of sensitive tests, to demonstrate under conditions to which radio coils are normally exposed, what actually takes place during the development of corrosion. The development of corrosive properties in protein materials may be largely or even altogether due to hydrolysis instead of oxidation. One might, therefore, tentatively specify as a noncorrosive material one which is nonelectrolytic itself and which is (*a*) resistant to oxidation and hydrolysis or (*b*) yields nonelectrolytic products.

Rubber is an example of the latter type, whereas the cellulose ethers and esters are quite resistant to oxidation. Apparently all that is necessary to increase enormously the resistance of the cellulose molecule to oxidation, is to replace the hydroxyl groups. It appears to be quite immaterial whether they are replaced by the acetate, nitrate, or any one of various other groups; the result is always a great increase in resistance to oxidation. Thus we find that any one of these cellulose derivatives is apparently satisfactory as far as corrosion is concerned, although cellulose acetate is the only practical possibility at the present time.

Mechanism of Copper Corrosion

During the corrosion of copper wires under a potential, the fundamental change that takes place is the oxidation of metallic copper to cupric ion ($Cu - 2e = Cu^{++}$). If we designate the negative ion which appears in combination with Cu^{++} as A^-, the compound may be written as CuA_2. The latter accumulates as a green cupric compound on the positive wire. The two prerequisites for the appearance of green corrosion are, therefore, oxidizing conditions at the copper wire and availability of a negative ion. It is obvious that the deficiency of electrons at the positive wire provides the oxidizing conditions, and if an electrolyte is present in the path between positive and negative wires, all that remains is to provide mobility for its ions. At high humidities, water which is absorbed, or adsorbed, by cellulose provides the medium in which the ions may migrate. In low humidities, that is, in the virtual absence of free water, corrosion is, in general, exceedingly slow.

It is obvious that the presence of any electrolyte which will yield A^-, will contribute toward the formation of the green product of corrosion, CuA_2. It is immaterial whether the electrolyte is an acid, HA, or a salt such as NaA. Thus, pH measurements are without significance in predicting or explaining corrosion, as a neutral salt may be at least as harmful as an equivalent amount of acid. In fact, if we are considering a very weak acid such as abietic acid and its salt, the latter will be much more harmful. The acid itself may be so weak or so insoluble, or both, that it will be practically un-ionized, whereas the salt will be highly ionized. It is a familiar fact that rosin itself has no corrosive action, whereas sodium abietate (rosin size) is highly corrosive.

Now to return to the specific case of cellulose, electrochemical oxidation will take place at the wire at which oxidizing conditions exist; namely, the positive wire. The organic acids formed by oxidation will thus make immediately available their negative ions, and conditions for formation of a salt, CuA_2, are complete.

If, however, we examine this mechanism of corrosion critically, we find that in order for corrosion to proceed by deposition of negative ions of organic acids on the positive wire, a corresponding flow of current would necessarily take place. This would require that corrosion be a function of conductivity and would appear to be in direct conflict with our finding that samples of cellulosic fabric, extracted and unextracted, respectively, show no correlation between rate of corrosion and conductivity. This conflict, however, may be apparent rather than real, for the reason which follows.

Suppose we examine two samples of cellulose fabric, one contaminated with a trace of active electrolyte and one exhaustively purified. The initial insulation resistance of the former will be greatly decreased, particularly in high humidities, by the presence of electrolyte; but one might expect that with continued application of potential, the electrolyte would be used up and the insulation resistance would rise to approximately that of the previously purified sample. This is actually what we observe experimentally. In addition, the disappearance of the contaminating electrolyte would leave the cellulose itself as the only remaining source of corrosive material, and one would expect that the continuing rate of corrosion would approximate that of the previously purified sample. Thus we see that the amounts of corrosion caused, respectively, by purified cellulose with a high insulation resistance and by material contaminated by electrolyte, will differ only by the corrosion caused by the contaminant. This might well be so small as to be unimportant.

Impregnation as a Protection for Corrosive Materials

In view of the high cost of cellulose-acetate films and cloth, it would be highly desirable to use paper or cotton as a base material for tape backing and impregnate or coat with some noncorrosive material. A number of attempts have been made to develop a practical construction of this type, but the result has been consistent failure. It must be borne in mind that a satisfactory material for the purpose must be sufficiently tough so that contacting wires will never penetrate the film of impregnating or coating material and come in contact with the cellulose. Such noncorrosive materials as paraffin, shellac, and other highly thermoplastic or excessively brittle materials are obviously inapplicable. Cellulose acetate itself is satisfactory, but as a matter of experience, such a heavy coat is required to give complete protection of the surface, that no economic advantage could be gained. In addition, when such material was slit, cut, or torn, cellulose fibers were exposed, thereby generating a region at which corrosion could take place.

To sum up, we feel that the most satisfactory and practical construction for a corrosion-free electrical tape is cellulose acetate coated with a good quality rubber-resin adhesive.

Reference

1. Society of Dyers and Colorists *Journal*, volume 28, 1912, page 359.

Discussion

B. W. Erikson (General Electric Company, Schenectady, N. Y.): H. N. Stephens and G. B. Gehrenbeck are to be congratulated on their masterly and timely solution of a very difficult problem. It is well-known to anyone who for years has been connected with the manufacturing problems incident to fine-wire coils that so-called corrosion failures have constituted the majority of all field failures.

The successful use in recent months of so-called noncorrosive material has plainly indicated that the writers have correctly analyzed the causes for fine-wire corrosion failures. Coils operated on an accelerated test at 100 per cent humidity in 40 degrees centigrade ambient over periods of months have conclusively proved that if the ordinary cellulose materials such as tapes and papers are excluded from the coil structure and acetate, mica, or asphaltic materials substituted the coils will last practically indefinitely. As a comparison, coils made with the best so-called acid-free cellulose materials on these accelerated tests seldom last beyond ten days, whereas the so-called corrosion-free coil construction shows a life expectancy of at least four months under these very severe tests. Our tests agree with the authors' that moisture is an essential factor in corrosion failures.

In relation to the paragraph on impregnation as a protection of corrosive materials it has been quite definitely shown that even three or four coats of a good baking varnish is not enough to protect against fine-wire corrosion. For this reason we agree with the authors that the only really satisfactory solution is the strict adherence to corrosion-free materials or in other words the exclusive use of cellulose acetate, mica, and asphaltic materials.

G. B. Gehrenbeck: Mr. Erikson lists three materials which he considers as corrosion free; namely, cellulose acetate, mica, and asphaltic materials. When tested across a flat unbroken surface, mica is indeed noncorrosive. However, when the test is made between two edges or along one edge, we have found rather low resistivity and some evidence of corrosion. Apparently, some electrolyte is present between the laminae of the mica, which under high humidity may carry an appreciable current. In a coil, there is always the possibility that the fine wires will come in contact with the edge as well as the face of the insulation. On this basis, mica appears to be less desirable for the insulation of fine wire coils than does cellulose acetate.

Contact Phenomena in Telephone Switching Circuits

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Synopsis: The phenomena occurring at the closing and opening of contacts carrying weak currents have been investigated by means which include a study of the high-frequency transient voltages and currents. These influence the erosion in a complex manner which varies with contact materials, surface conditions, and surrounding atmosphere. Three principal classes of effect have been distinguished. These are: (1) Disruptive spark-overs initiating a series of metallic arcs lasting less than a microsecond each. (2) A nitrogen-gas glow discharge at about 300 volts, preceded by a brief group of disruptive spark-overs. (3) High field breakdowns due to cold point discharges which cause transient metallic closures of approaching contacts and similar transient reclosures of separating contacts.

THE operation of a telephone system depends on the proper performance of many millions of electrical contacts, a large proportion of which are in relays. The relays must be designed for a life during which they operate from as few as 5,000 to as many as 400,000,000 times. Although the nominal currents and voltages carried by the contacts are rather low, the large number of operations may cause erosion which in a very small percentage of cases leads to failures to close or open the circuit. The difficulties caused by even very rare failures make the control of contact erosion a problem of major importance for the telephone companies.

Research and development work on contacts has of course been carried on continuously since very early in the development of the telephone system. The aim is to design contacts to have a life at least equal to that of the apparatus of which they form a part and to require a minimum of maintenance. Although this aim has in general been successfully met there have been some cases in which the contacts have worn out too rapidly.

Although it has long been realized that contact operation necessarily involved the generation of high-frequency transients, there was at first no apparatus available which would permit these transients to be studied. The Dufour oscillograph was for a long time the only instrument which covered the range of frequencies involved. It was employed as early as 1926 in studies of contact sparking but it was very cumbersome in use, often introduced artificial conditions into the circuit of the contacts, and progress with its use was necessarily very slow. During the past few years rapid advances have been made in the development of glass-envelope cathode-ray oscillograph tubes. By employing the latest types of tubes, and combining them when necessary with wide-band high-frequency amplifiers and with circuits which permit synchronization of the tube sweep circuit with the contact operation, it has been possible to make thousands of observations in the time originally taken by a single oscillogram, and to cover the entire range of currents, voltages, and frequencies involved. We now have available means which will permit the visual observation of transient voltages at frequencies as high as 400 megacycles per second, and transient currents with components reaching 20 megacycles per second. Single pulses lasting a small fraction of a microsecond, and complex transients containing components as high as 5 megacycles can be clearly resolved and photographed while the envelopes of still higher frequencies can be recorded.

In order to study the transients at contacts operating at 50 volts and steady currents under one ampere, in common types of telephone circuits, voltages as high as 2,000 and currents reaching 20 amperes must be within the range of the apparatus. A detailed description of the apparatus will not be attempted in this article, but the results of observations made with it and photographs of the more significant transient components will be presented.

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The author wishes to acknowledge the collaboration of E. T. Burton in the observation and explanation of the phenomena and the assistance of I. E. Cole in the development of the testing apparatus; of Mr. Glass, who developed the cathode-ray tubes; and that of many engineers and physicists in our organization, in particular Messrs. Mathes, Hogg, Goucher, and Pearson, in the formulation of some of the hypotheses expressed.

Study of the currents requires an amplifier as an impedance matching device and some circuit conditions make a shielded input transformer necessary. An input impedance of from 0.5 to 2 ohms, a voltage gain of about 75 times, and a substantially flat characteristic of output versus input from 20 kilocycles to 20 megacycles are usually employed. Lower frequencies may be observed with other amplifiers and the range from zero to 10,000 cycles per second is studied by means of the "rapid record" oscillograph.

With earlier cathode-ray tubes, beam currents of 40 microamperes at 5,000 volts were employed. The latest tubes give a beam current of about one milliampere at this voltage. A Leica camera with an $f/1.5$ Xenon lens and ultraspeed panchromatic film has been used in most of the photographic work. The photography is complicated by the presence in a single transient photograph of some components in which the beam speed may be a thousand times as fast as it is in others. However, beam speeds in excess of 200 kilometers a second are photographed, and a continuous sine wave of 5 megacycles frequency may be clearly resolved on a single transit. Sweep speeds which permit resolution of much higher frequencies are employed for visual observation, where the transient component being studied can be found by frequent repetition of the contact operation. As the occurrence of a particular component varies in time of its position in the entire transient, very high sweep speeds are impractical for photography as a prohibitively large proportion of exposures would be blanks. A sweep speed of about 15 kilometers per second is about as high as is useful except in some special cases.

We may commence the discussion by setting up what appears to be a very simple circuit (see figure 1), a pair of contacts, one of which is connected by a length of wire to a relay winding, which is in turn connected to one pole of a 50-volt battery. The mate contact and the other pole of the battery are grounded by very short wires. The oscillograph is arranged so that the voltage between the contacts and the current through them can be observed, great care being taken to insure that the added apparatus does not appreciably change the circuit characteristics even at very high frequencies. A low-power microscope may be set up to observe the operating area of the contacts.

When the contacts close, the first thing that happens is the discharge of the relay structure (a capacity) and of the wire through the contacts. The wire may be thought of as a radio antenna, more or less open-circuited at the load relay-winding terminal, and either grounded or opened at the contacts. The wire forms an oscillatory circuit of moderately heavy damping with a surge impedance of about 100 ohms. As it is charged to 50 volts, when the contacts come together an oscillation having a peak current of 0.5 ampere occurs, and is over before the steady current through the relay winding has more than started to build up. The frequency of the line oscillation depends on the length and other characteristics of the wire, but it is (in the telephone plant) rarely lower than 500,000 cycles, and on short leads it may be many megacycles. Fortunately most contacts are not much affected by closing a half ampere. Erosion and build-up will occur, but at rather slow rates, and they are usually completely obscured by effects due to the contact opening. Of course, if the contacts bounce,* the effect will be more complex, but we are assuming for the moment that they do not bounce. The structure of the relay itself, including the pair of springs separated at its base by an insulating sheet, is also an oscillating circuit. We have not been able to get inside of this circuit and measure the current surge but its oscillation frequency seems to be about 250 megacycles for certain telephone relays.

Now suppose that the simple circuit of our closing contacts is complicated by an additional wire connected to the contact-spring terminal. This is also charged to 50 volts before the contacts close, and being a second circuit of a hundred ohms surge impedance in parallel with the original wire, the current peak discharged through the contacts will now be about one ampere. But now the contacts are likely to act differently.

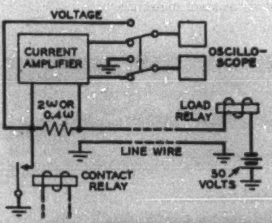

Figure 1 (left). Typical relay and contact circuit

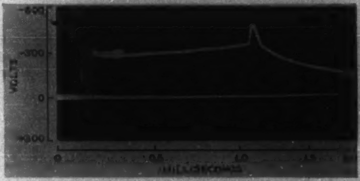

Figure 2 (right). "A" transient starting with metallic arc (voltage)

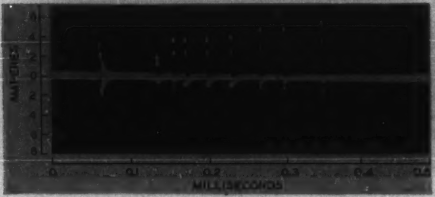

Figure 3. Entire "B" transient (voltage)

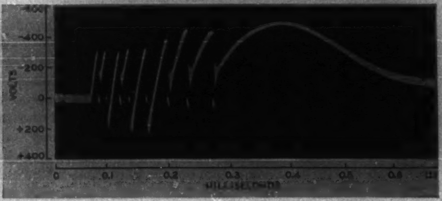

Figure 4 (below). Entire "B" transient (current)

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* Bounce, as distinguished from chatter, reopens the contacts after several thousandths of a second.

About a microsecond after the current reaches its peak, but before the charge in the wires has been completely dissipated, the circuit is interrupted and the discharge stops. A spark, which is visible in the microscope, suggests that the current-carrying areas have been exploded and blown apart. A few microseconds later they again close and the rest of the energy is discharged, but some of the contact metal must have been destroyed.

If several "idle" wires are attached to the contact, the current surge, and the number and duration of the contact reopenings increase, but not usually in direct proportion to the number of wires. If the idle wires are attached to the load relay-winding terminal instead of to the contact, the current is smaller, as the length of single wire from relay winding to contact is effectively in series with them.

In the telephone relay circuits which we are considering, the steady-state current plays little part during contact closing if the contact-carrying relay is properly adjusted, as the contacts come to rest while the current is still held at a small fraction of its final value by the inductance of the load relay winding.

Under some conditions which are more likely to occur in telegraph than in telephone circuits the contact closure phenomena are somewhat different than those described above. Assume, for example, that the potential between the open contacts may be adjusted in a range between 30 and 250 volts while the final direct current is limited by circuit resistance to less than 0.5 ampere. At the low voltage, observations of the current and voltage transients indicate that the closing contacts merely discharge the line. As the voltage is raised so that the current surge peak is in the range between 0.5 ampere and 1 ampere the reopenings, due presumably to overheating of the contacting areas by the discharge, are observed. These become more frequent as the voltage and current increase, and a new type of current surge begins to appear. This is placed on the time axis ahead of the point at which the initial closures have been occurring (usually five to ten microseconds earlier) and consists of one or more irregularly spaced heavily damped pulses of current lasting only a small fraction of a microsecond and evidently discharging only a minute amount of the energy stored in the system. They occur perhaps once in a hundred closures at 30 volts, nearly every closure at 100 volts, and several for every closure at 250 volts. It is believed that the transients observed indicate the formation of minute metallic bridges[1] between the approaching contacts due to a softening of the metal by a cold point discharge and its deformation by the static field, and that once formed they are exploded by the discharge of current from the relay structure and adjacent wiring. The high fields necessary for phenomena of this type are of course due to the minute distances as the contacts approach final closure. A good deal of the erosion on telegraph-relay contacts operating on capacitative loads or shunted by resistance-capacity "spark-killer" circuits is probably due to these "preclosures," but they are not thought to be of much importance at the lower battery voltages of the telephone plant.

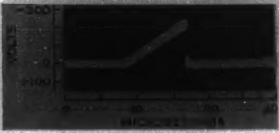

Figure 5. Initial opening and reclosure—"B" transient (voltage)

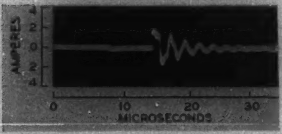

Figure 6. Initial opening and reclosure—"B" transient (current)

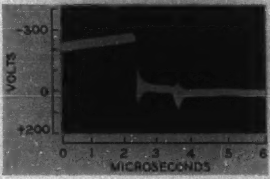

Figure 7. Initial opening and reclosure—"B" transient (voltage) rapid sweep

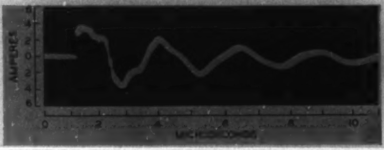

Figure 8. Initial opening and reclosure—"B" transient (current) rapid sweep

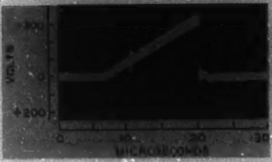

Figure 9. Evidence of point discharge (voltage)

Having now described the phenomena as contacts close in a doubtless oversimplified manner, we may consider that they have been closed for a long time, the direct current and the magnetic field of the load relay are established, and the contacts are to be separated. The action now becomes really complicated and much of it is as yet only surmised. Several different things may happen, and these are influenced by humidity, dirt, surface films, absorbed gases, and many other factors, including the speed of contact separation, the roughness of the surfaces, and the presence or absence of a wiping motion as well as the physical properties of the contact materials.

If the steady current exceeds certain well-known values ranging between 0.4 ampere and 1 ampere, characteristic of the contact materials, a metallic arc is formed as the contacts separate.[2] This is maintained at an initial potential of about 15 volts, and increases to a final value usually below 30 volts. The arc may last several milliseconds, but when it breaks it is followed by a complex transient lasting possibly another millisecond. These transients may be of two general types to be described later. This case is not of much importance in the telephone plant as the steady current is ordinarily kept below the value at which prolonged arcing occurs.

Metallic arcs lasting several ten-thousandths of a second, and also followed by complex transients, may occur in breaking steady currents considerably less than those ordinarily believed to cause arcing. The effect of these transient

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[1] For all numbered references, see list at end of paper.

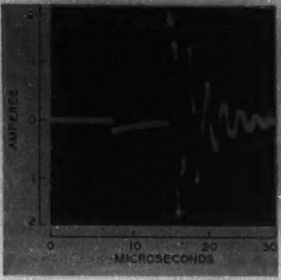

Figure 10. Evidence of point discharge (current)

arcs on contact life has not been studied separately, but they can hardly fail to increase the erosion. Their effect is unavoidably included in the studies of contact life in the higher range of d-c values. Figure 2 shows the voltage between a pair of opening silver contacts in which the steady current (0.25 ampere) is strong enough so that a brief metallic arc (indicated by the upward deflection of the trace to a new horizontal position) precedes the final transient.

If the steady-state current is low enough so that neither prolonged nor brief metallic arcs are formed at the initial contact separation, one of two general types of complex transients occur, or both types may be mixed. These have been designated the *A* and *B* types. The *B* type transient seems to be the more normal and it is difficult, probably impossible, to set circuit and contact conditions which will never give a *B* transient. It is identified by a bright spark between the contacts, showing in a spectroscope bright lines of the vaporized metal, and consists of a series of disruptive spark-overs at gradually increasing voltages. Each spark-over is individually very complicated. The appearance of the contacts during the *A* type transient is radically different from that during the *B* transient. There will be a minute bright spark, surrounded by a violet cloud which spreads out from the immediate contact area over the negative contact, and sometimes travels as far as a sixteenth of an inch from the working area.

As a result of thousands of observations of the transient currents and voltages, and many experiments, and discussions with several physicists and engineers with whom the writer is associated, a plausible explanation of the phenomena has been arrived at and will be given as at least a working hypothesis.

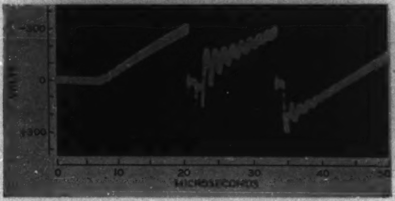

Figure 11. Early part of "B" transient (voltage)

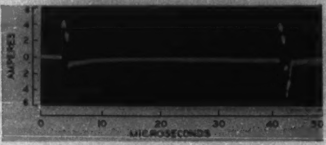

Figure 12 (right). Early part of "B" transient (current)

The voltage wave form of an entire *B* transient, covering the time from the initial separation of the contacts to the final subsidence of the voltage charging the line wire is shown in figure 3, and the a-c components of the current in the range from 20 kilocycles to 20 megacycles are shown in figure 4. The low-frequency components of the current are comparatively weak. A line and load relay were chosen to give a relatively simple transient with the important components at frequencies which could be photographed. A 500-ohm Western Electric U-type relay and a line of 300 feet of number 22 switchboard pair were used. The mate wire of the pair was grounded at both ends. The currents and voltages were not photographed simultaneously but the types of the transients were correlated by repeated observations. A current picture will not exactly correspond to a voltage picture, as the transients produced by successive operations of a contact are never identical.

The *B* transient may be explained as follows, using as a basis the simple circuit of figure 1. The steady current is established and the contacts start to separate, moving apart at a speed, which is at first surprisingly slow (about an inch a second). The contacts have been deformed by the pressure between them, and as this is relaxed, the current density and the temperature at the contacting areas rapidly increase until at some light pressure the area becomes so small that the current explodes it. There may be some necking out of the softened contacts before this and under some conditions there are indications of a metallic arc lasting a fraction of a microsecond, but at any rate, an initial rupture occurs between hot and soft metal areas.

The wire has been at ground potential, but the battery plus the collapsing magnetic field of the load relay commence to charge it at a rate depending on the line and relay-winding capacity and the relay inductance and losses. In 10 or 20 microseconds, it has reached at the contacts a potential of from 50 to 200 volts. This is below the voltage at which spark-over due to ionization of the air can occur, but something usually happens which recloses the circuit. This is believed to be caused in somewhat the same manner as the "preclosures" mentioned earlier. It is probable that a cold point discharge reheats the contacts. This is followed by a collapse of the voltage to about 15 volts above zero in the direction of the previous voltage, indicating the formation of a metallic arc. This lasts a fraction of a microsecond and the voltage then drops to nearly zero, suggesting that the contact areas heated by the field current and the arc have been drawn together in solid metallic contact. The line is discharged with an oscillation of comparatively low damping (which is characteristic of the line wire) reaching a current peak usually ranging from 0.5 to 2 amperes. The first cycle of the oscillation is distorted by the higher resistance of the path to ground caused by the arcing stage in the reclosure. After a few microseconds the

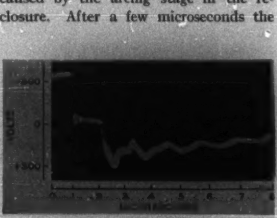

Figure 13. Single spark-over of "B" transient with single arc (voltage)

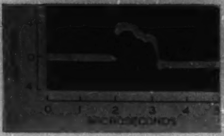

Figure 14. Single spark-over of "B" transient, with single arc (current)

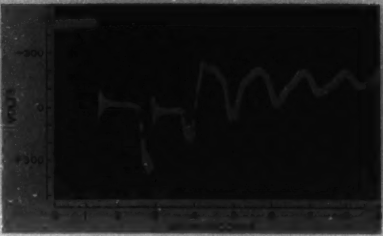

Figure 15. Single spark-over of "B" transient, with double arc (voltage)

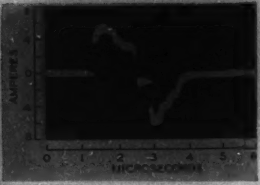

Figure 16. Single spark-over of "B" transient, with double arc (current)

contacts are opened a second time by the continued motion. Occasionally they reclose a second time but they usually stay open until the voltage has built up by the continued discharge of the load relay inductance to a value between 300 and 350 volts. Then a spark occurs at what is usually considered the minimum sparking potential between contacts in air.

Figures 5 and 6 show the voltage and current of the initial opening and reclosure of the contacts at the start of a *B* transient. The brief arc at initial opening is barely detectable in figure 5. Figures 7 and 8 show similar voltages and currents at an increased sweep speed. In figure 7 the metallic arc established during the reclosure is plainly evidenced by the collapse of the voltage to about 15 volts and its maintenance at this value for about a microsecond before it drops to zero. The effect of the arc in distorting the oscillating discharge of the current from the line wire is evident in figure 8. The current oscillation of figure 8 may be duplicated merely by charging the line wire to a suitable voltage through a high resistance and closing the contacts, the far end of the line being grounded through the load relay and a large capacitor which replaces the usual battery.

It is likely that the point discharge precedes the arc on reclosure by such a short time that it cannot ordinarily be resolved. Nevertheless disturbances of the voltage and current are occasionally found which seem to indicate that a discharge path formed and was checked (possibly by melting off the point) without establishing an arc or metallic bridge. Such a disturbance of the rising voltage is indicated in figure 9 by a high-frequency oscillation about five microseconds after the first rise of the voltage trace. Figure 10, which shows the current of the second of two initial reclosures, indicates a similar phenomenon. Five microseconds after the rupture of the circuit, shown by the downward deflection of the zero line, a dim line upward records a current surge lasting a fraction of a microsecond and reaching about three-fourths ampere. This surge however did not result in the immediate formation of an arc which was established about five microseconds later.

The initial separation of the contacts does not always result in a metallic reclosure. Figure 11 shows the voltage of the early part of the *B* transient. Here the first collapse of the voltage is a spark-over from about −300 volts which establishes an arc at about −15 volts. This arc is broken and as the line is not completely discharged, the voltage between the contacts rises to about +140 volts; a second arc is established at +15 volts and broken in its turn. Possibly because of the continually increasing distance, the arc is not re-established, and the voltage builds up with oscillations of a frequency characteristic of the line wire insulated at both ends until it reaches −300 volts a second time and another spark passes. This time only one arc is formed, and the recovery of the voltage starts from the positive side of the zero axis. The current surges corresponding to the voltage collapses of figure 11 are shown in figure 12. Here the first pulse represents a spark-over which formed only one arc. As the current from the line reached about four amperes it was checked and the conducting arc was broken (possibly by being extended laterally into the region of cooler metal). The second pulse shows the current of a spark-over which formed two arcing periods.

These phenomena are shown in more detail in figures 13 and 14 which show the voltage and current of a spark-over forming only one arc, and 15 and 16 which show the wave forms when two arcs are formed. Note that the frequency of the current oscillation is that of the line grounded at one end only (the impedance of the load relay being high at this frequency) and is about half that of the voltage oscillation which is that of the line open at both ends. Oscillations of both frequencies may be found in the line at a distance from either end.

Corresponding observations may be made of the occurrence of 3, 4, and 5 arcing periods, the pattern followed being about the same. The higher the voltage at spark-over the more arcing periods;

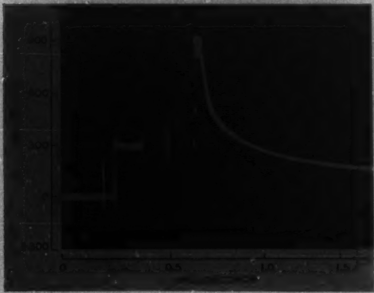

Figure 17 (left). Typical "mixed A and B" transient (voltage)

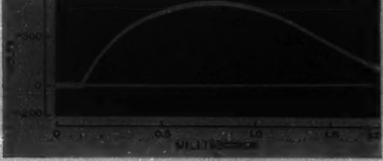

Figure 18. Effect on voltage transient of changing wire-line length—1,100 feet

an odd number of arcing periods is followed by a recovery of the voltage from the opposite side of the zero axis from that of the voltage before spark-over, an even number by recovery from the same side of the zero axis. The arcing periods are individually complex, having superposed on them oscillations believed to be due to the relay structure and the leads to the oscillograph which are too fast to be resolved photographically by the means available. These oscillations may be observed visually by using higher sweep speeds and reach frequencies of 250 megacycles.

While the arcs ordinarily do not exceed a microsecond in duration, they are probably an important factor in determining contact erosion, as several hundred may occur at each contact opening.

As may be seen from figure 3, the spark-overs continue to occur, the successive voltage breakdowns corresponding to the normal sparking potential as the contact separation increases with time (with some irregularities due to residual ionization in the gap) until the separation is finally so large that the energy remaining in the load relay cannot charge the line to the breakdown voltage. At this stage the line discharges slowly back through the load relay to the battery voltage. The peak voltage reached may be as high as 2,000 volts.

The principal characteristics of the *B* discharge can be produced by a simple experiment which does not use a load relay. The transient is not dependent on a load inductance, but only on a source of voltage which will charge a wire at a sufficiently rapid (but not too rapid) rate while a pair of contacts, which initially ground the wire, are separating. If a wire about 100 feet long is connected to a source of somewhat more than 350 volts through a resistance of from 5,000 to 20,000 ohms, and is also grounded by a contact at one end, a transient is produced when the contacts open which shows the characteristics of a *B* type transient except the final dying away of the voltage to 50 volts.

It must not be understood that every spark transient is purely of either the *A* or the *B* type. It is very common for the *A* type transient to break down into the *B* type and less often the *B* transient establishes the gas glow discharge for a brief period in the middle of the spark-overs.

A "mixed" transient is shown in its entire duration in figure 17. Here, after a group of spark-overs, a period in which the voltage is maintained steadily at about 300 volts for about 0.0002 second intervenes, and is followed by more spark-overs from considerably higher voltages. In order to produce this transient the length of the line wire was reduced to ten feet, at which length and with a 1,000-ohm load relay the tendency is to produce intermittent groups of *A* or *B* transients, interspersed with the mixed type shown, when the relay is operated frequently.

The number of spark-overs in each *B* transient varies with the circuit conditions. As many as a thousand may be found with a load consisting of a number of relays in parallel on a wire of moderate length and as few as one in the limiting case.

While the occurrence of the *B* transient is favored by long line wires and high-impedance relay loads, beyond a certain length which with telephone relays and wiring is from 300 to 2,000 feet (the longer lengths being associated with the lower impedance relays) no spark-overs at all occur. The voltage build-up is so slow that the spark-over potential is not reached at any time during contact opening and the contacts may be said to be protected by the line wire. The series of voltage oscillograms, figures 18 to 24 inclusive, shows the change from a smooth transient with no spark-overs through the *B* type with an increasing number of spark-overs to the final *A* type. The *A* type transient of figure 22, which has superposed on the 300-volt gas glow discharge stage a relaxation type of oscillation, the *B* transient of figure 23 and the simple *A* transient of figure 24, were all produced under identical conditions in quick succession. The change in characteristics from figure 18 to figure 24 was produced merely by a

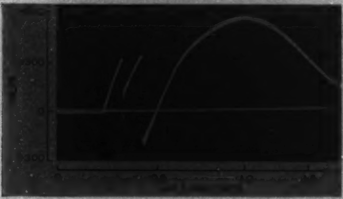

Figure 19 (left). Effect on voltage transient of changing wire - line length—600 feet

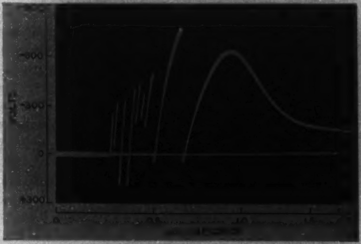

Figure 20 (right). Effect on voltage transient of changing wire-line length—150 feet

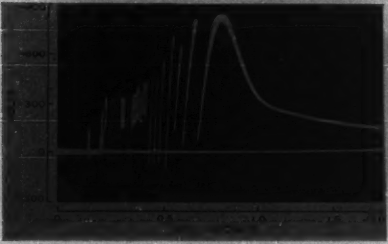

Figure 21 (left). Effect on voltage transient of changing wire-line length—50 feet

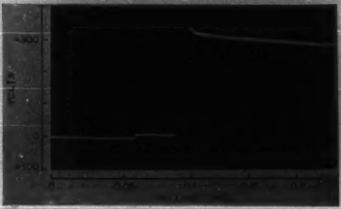

Figure 22 (right). Effect on voltage transient of changing wire-line length—10 feet reduction in the length of the connecting wire from 1,100 feet through three intermediate stages to 10 feet, a 1,000-ohm load relay being used. This explains a puzzling effect noted with many contact materials. With a supposedly identical circuit, the erosion will be small with very short wires, increase rapidly as the wiring length increases, and then decrease again becoming very small with very long wires.

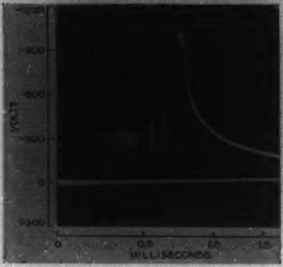

Figure 23. Effect on voltage transient of changing wire-line length—10 feet

The *B* transient is more frequently observed with freshly filed contacts, at high humidities, and with a rolling or wiping motion of the contacts in opening. It is always found if the contacts are of oxidized metal or operate in an oxygen atmosphere. In fact, there seem to be good reasons for believing that its production is bound up with the presence of oxygen on or in the surface of the active contact metal.

It may be seen from the last series of oscillograms that if the circuit and the conditions of the contact surfaces are just right, the *B* transient is replaced by a much simpler and less stable type, the *A* transient. It will occur usually when the wiring is short or the load relay is of low impedance, with contacts which have been operated until the original surface has been burned off and have not stood idle more than a few minutes. It starts much as does the *B* type, but after a dozen or a hundred spark-overs from about 350 volts, which come much closer together in time than those of the *B* transient, the voltage becomes steady at about 300 volts. This condition lasts for perhaps 0.6 millisecond, then the voltage rises to about 400 or 450 volts and gradually reduces, reaching the battery voltage after several milliseconds. A typical *A*-type transient is shown in figure 24. It is suggested as a hypothesis that during the spark-over stage, the oxygen is being exhausted from the surfaces of the current-carrying areas of the contacts by burning the metal and that when this has been completed, a nitrogen-gas glow discharge is formed and maintained during the rest of the contact opening, if the supply of energy from the load inductance through the line is rapid enough to prevent the voltage from dropping below about 280 volts.

The glow-discharge phase of the *A* transient is unstable. If transient voltages induced by the operation of relays in other circuits reach the contact gap during the time that a sustained glow discharge is attempting to form, its formation is interfered with and a mixed or *B*-type transient results. Occasionally as illustrated in figure 22 the glow discharge of the *A* transient has superposed on it a saw-toothed oscillation of from 10 to 50 volts peak-to-peak. Part of an *A* type transient showing this peculiarity is illustrated by the oscillogram of figure 25. This appears to be a relaxation oscillation such as is commonly produced in ionized-gas tubes, riding on the normal 300-volt axis of the gas glow discharge. The conditions which lead to the occurrence of this oscillation at atmospheric pressure have not been identified, but it is found to be quite stable in some cases where contacts have been sealed in a mixture of air and gases at about half atmospheric pressure.

A typical *A* transient is shown in detail in figures 26, 27, and 28. The circuit consisted of a 250-ohm relay connected to the contacts by ten feet of wire, the battery being 50 volts as usual. Figure 26 shows the voltage of the early part of the transient during which rapid spark-overs are interspersed with two brief periods during which a gas glow discharge was established but not maintained. Figure 27 shows the final spark-over before the establishment of the glow discharge at about 300 volts. A group of the current pulses corresponding to the initial part of the spark-over stage is shown in figure 28. These are complicated by the line oscillations (which should be of about 30 megacycles frequency) and appear to last less than 0.1 microsecond.

It may be seen that the individual spark-overs at the start of the *A* transient are somewhat different in form from those of the *B* transient. The voltage reaches 320 volts in a microsecond or so, and in some cases collapses to zero or beyond immediately. There are sometimes indications of arcing periods lasting much less than 0.1 microsecond and the voltage recovers with oscillations of the line wire but the duration of the phenomenon is too brief for very accurate analysis. But in many cases, the voltage having reached its peak, drops to an intermediate value of 280 volts and recovers to 320 volts before it collapses. This is probably due to the temporary formation of the nitrogen glow discharge, which is finally established and maintained during the remainder of the contact opening when for some reason the spark-over does not occur. In cases where the contacts are on the verge of producing a *B* transient the voltage may rise to 500 volts and then collapse to the 300 volts of the gas glow discharge.

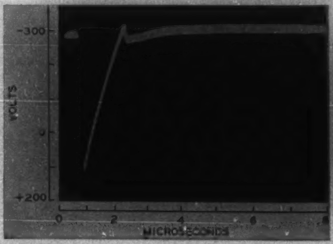

Figure 24. Effect on voltage transient of changing wire-line length—10 feet

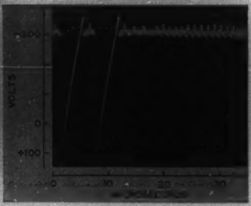

Figure 25. Oscillation on glow discharge of "A" transient (voltage)

It is very interesting to set up a circuit which will cause the *A* transient to predominate, and start operating freshly filed contacts several times a second observing the transient voltage at contact opening on the oscilloscope. The first transient will always be of the *B* type. Usually the first few dozen will also. However, after a while one of the transients will show a flat top at about 300 volts for a very brief period and this tendency increases until finally a complete *A* transient occurs. After this, the *A* transients become more and more com-

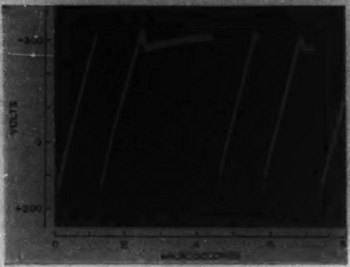

Figure 26 (left). Start of "A" transient (voltage)

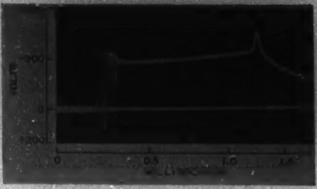

Figure 27 (right). Start of stable glow discharge of "A" transient (voltage)

mon until finally the *B* transients occur perhaps once in a hundred openings. If then a gentle stream of oxygen is blown on the contacts, only *B* transients will occur until a few seconds after it has been turned off. Blowing the breath on the contacts has a similar but less definite effect, while a stream of dry compressed air has no effect.

If on the contrary the circuit conditions are selected so that *B* transients predominate, a stream of nitrogen will induce *A* transients. That is, *A* transients are not found in oxygen and *B* transients are rare in nitrogen.

If instead of operating the contacts several times a second they are operated at longer intervals, the tendency to produce the *A* transient is reduced. When contacts are operated in air a certain interval between operations can be found which causes all transients to be of the *B* type. This probably depends on humidity and also on circuit conditions and contact material. In one experiment, a wait of 45 seconds between operations gave all *B* transients with silver contacts, while a wait of five minutes was required with palladium contacts. This is possibly due to a different rate of film formation.

Life tests on palladium contacts show much lower erosion with *A* transients than with *B* transients. The effect of the two types of transient in terminating the life of silver contacts is not markedly different. The contours of the eroded surfaces exhibit a wide variety, and it is not easy to correlate the transient type with its effect. It is evident, however, that areas of the contacts which have never been in the d-c path may be severely eroded.

When we consider that the *B* transients produce oscillations in the line wires reaching several hundred volts and often 15 amperes, it is not to be wondered at that clicks will be produced in circuits in the immediate neighborhood of unprotected relay contacts. The *A* transients produce much weaker currents than the *B* transients and many contacts on successive operations will produce *A*, *B*, or mixed types. This explains the common observation that relay clicks vary over a wide range of amplitudes. The arrangement of telephone circuits in which the cabled wiring always contains a large number of grounded conductors, and is often enclosed in a lead shield, prevents any appreciable free radiation of the spark transient oscillations.

With the foregoing information available the contact erosion process at opening contacts appears briefly to be as follows. At very minute separations high field strengths exist even for moderate voltages. The resulting cold point discharge is often followed by a metallic arc which softens a tiny point on the contact which is pulled out and fused into metallic contact under the action of the high fields. After rupture by increasing separation or increasing current density, the process may repeat or, as is more likely, the separation is too great for another metallic bridge to form. The high field discharge then sets the stage for the next type of conduction or breakdown. This may be either a series of spark-overs interspersed with metallic arcs of extremely short duration or a gas glow discharge, initially intermittent and then more or less stable. Factors predisposing toward one or the other type of discharge are known thus far only in a most general fashion and much remains to be done before the relation between contact erosion and the transient currents and voltages can be predicted accurately. There is ample evidence that molten metal may be expelled from the immediate contact area at high velocity and may be deposited at distances of at least 0.1 inch. It also appears that both the ionized nitrogen cloud of the *A* transient and the disruptive sparks of the *B* transient may corrode the contacts and their supports at locations and distances which never enter directly into the rupture of the current path.

We have seen that the line wire contributes to the current surges through contacts due to its properties as an oscillatory circuit, charged repeatedly by the energy stored in the magnetic field of the relay. The surges and the resultant erosion may be reduced in several ways. If a radio-frequency choke coil is connected between the contact and the line wire, the discharges of the latter are much reduced, and the *A* type gas glow transient favored. A group of many current surges of 15 amperes peak may in most cases be reduced to one or two of 0.15 ampere or less, and a radical reduction in erosion secured. Unfortunately choke coils are expensive and inconvenient. The usual line wire may be terminated in approximately its surge impedance by shunting both ends to ground with a resistance of about 100 ohms in series with a capacitor of the order of 0.01 microfarad. This heavily damps the line oscillations and greatly reduces the number and severity of the current surges. It is also expensive. Instead of the copper line wire, a material such as iron or Permalloy-plated copper having a high surge impedance and large high-frequency a-c losses may be used. This seems more practical, but brings up new problems in design, handling, and soldering.

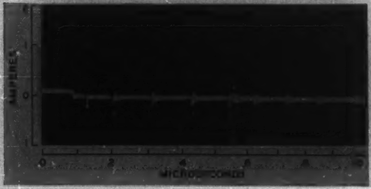

Figure 28. Start of "A" transient (current)

The most effective means of reducing erosion is of course the well-known "spark-killer" (consisting of a capacitor and resistance in series, shunted across the contact or load), which can be designed to hold the voltage below the spark-over point at least until the contacts have separated a safe distance.

When the conventional spark-killer is used it is generally assumed that what sparking then occurs is due to the discharge of the capacitor when the contacts close, provided that the spark-killer prevents the voltage at contact opening from reaching 350 volts. Unfortunately the "reclosure" effect described earlier appears unless the initial rise of voltage as the contacts separate is held down to a value considerably below the sparking potential by a suitable choice of the resistance in series with the spark-killer capacitor. If the rate of increase of the initial voltage in relation to the speed of separation of the contacts exceeds a figure which seems to depend on the contact material and the condition of its surfaces, the high-field point discharge comes into play and causes the separating contacts to reclose metallically while they are still at a minute separation and moving apart very slowly. In "reclosing" the line wire and capacitor are discharged, the current explodes the minute metallic bridge, producing a visible spark, and the circuit is thus reopened. This may occur a dozen times in some cases before the contacts finally stay separated. The higher the voltage which the spark-killer permits the more likely are the reclosures to take place, and the larger the number of reclosures at each contact opening. However, reclosures are usually not very common in cases where the voltage of the wave front is held below 50 volts. In the majority of cases in the telephone plant it is possible to do this without incurring much of a penalty due to erosion of the contacts on closing by the discharge of the spark-killer capacitor.

This discussion is not more than sufficient to serve as an introduction to the problems of contact sparking as revealed by the improved observing technique used in this study. Only the simplest cases have been considered, and the telephone plant is far from being simple. Many relays have multiple windings or metal sleeves, and multiple connections to the contacts are very common. As these complications considerably modify the contact spark wave form and erosion, each contact with associated circuits presents its own problem. The solution of these problems involves the careful study of circuit characteristics of a type which are ordinarily left to the radio engineer, as well as of the mechanical, chemical, and metallurgical properties of the contact materials.

References

1. The Formation of Metallic Bridges Between Separated Contacts, G. L. Pearson. *Physical Review*, volume 56, September 1, 1939, pages 471–4.

2. Minimal Arcing Current of Contacts, H. E. Ives. Franklin Institute *Journal*, October 1924.

Discussion

E. E. George (Phoenix Engineering Corporation, New York, N. Y.): The paper under discussion contains considerable material of a fundamental nature. This subject apparently has not been discussed previously before the AIEE. The paper answers many questions but raises many more and it is to be hoped that further work on the same subject will be published in the future.

Various relay engineers working with high-speed relays or with long cable circuits for pilot-wire protection or remote control, have run into numerous difficulties resulting from transient phenomena. It would be interesting to know whether the author has found any effect on relay operation other than contact deterioration. It seems that sensitive high-speed telegraphic relays should be affected by the transient currents observed by the author. In power-relay practice, a neon lamp with a high series resistance is frequently shunted across open relay contacts to act as a circuit monitoring device. It would be interesting to know the effect of this lamp on contact operation. It would also be interesting to know whether there is a critical maximum battery supply voltage or length of circuit beyond which trouble is likely to be experienced on pilot-wire circuits.

This paper may explain some of the difficulties frequently observed in trying to use very small d-c supervisory currents on pilot-wire circuits—currents under ten milliamperes.

The author makes reference to so-called "spark-killer circuits" consisting of a capacitor and series resistance, and it would be helpful to give some formula or basis for selecting the proper values of capacity and resistance for such circuits. In the past, it has been the practice of most engineers to consider contact operation perfect if there were no visible sparking. The author has contributed much to a better understanding of contact operation and his paper might well be required reading for all relay engineers.

A. M. Curtis: The principal effect of contact sparking transients aside from erosion is in causing contact sticking and consequent failure of the relay to open its circuit. It is possible to conceive of circuit conditions where the cross fire from a lead through which the steady current and transients of a telephone-type relay flows might cause brief operations of a very sensitive relay connected to another lead in the same cable. The principal difficulty from the contact sparking transients would be expected in cases where the induced currents reached the sensitive relay through a rectifier and particularly if the latter should be of a type where the very strong but very brief induced currents might be stored and discharged more slowly. It is hard to make a mechanical relay operate on a pulse only a thousandth of a second long.

A neon lamp shunted across relay contacts gives considerable protection, particularly if the series resistance is not too high. However, this is not as good as a proper spark capacitor as the delay in breakdown of the gas lamp may be 50 to 100 microseconds. During this period the voltage between contacts usually rises high enough to permit several spark-overs.

There is a complicated relation between contact material and battery voltage which has an important influence on the performance of contacts, especially those which work into capacity loads or are protected by capacity spark-killers. Generally it pays to keep the voltage low. Many contact materials which work well between 25 and 50 volts will be unsatisfactory at 150 volts.

I do not know of any general formula which can be used to design spark-killers. They fall into two general classes and some simple guiding principles can be stated.

If the steady current through the contact is above the arcing current of the material it is desirable to keep the voltage at the first opening of the contacts below the arcing voltage. This indicates about as large a capacitor as can be afforded and a resistance at least as low as $R=10/I$. But this leads to a situation where the discharge of the capacitor through the closing contacts may do more damage than the arc which is prevented and a compromise must be made by increasing the resistance to a value where the transient arc is no more troublesome than the capacitor discharge. This, of course, depends on the battery voltage and is another reason for keeping it low.

If the steady current is below the arcing value for the contact materials, the resistance for the best performance should be high enough to prevent the capacitor discharge currents through the closing contacts exceeding about one-third ampere, and low enough to limit the voltage at first instant of contact opening to about 50 volts. That is, it should be higher than $R=3E$ and lower than $R=50/I$. The first condition guards against explosive reopening of the contacts at closure; the second avoids reclosures on opening (always assuming a sufficiently large capacitor). But at the higher battery voltages and currents it is not possible to meet both conditions and in these cases a compromise must be again made. The capacitor should be large enough to keep the surge voltage from reaching 300 volts until after the contacts have separated about 0.002 inch. With many relays this takes about a quarter of a millisecond after the contacts have first started to separate. The capacity required varies with the relay. With a high-resistance relay 0.1 microfarad may be enough. One microfarad is enough for the general run of telephone relays but some heavy-duty electromagnets may require several microfarads.

The Application of Traction Motors

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Synopsis: The application of traction motors is based fundamentally on speed-time curve calculations. This method has been used for many years and is familiar to electrical engineers engaged primarily in transportation activities. Many other engineers infrequently encounter problems of traction-motor application but are not conversant with the effect which variations in service conditions produce on vehicle performance. This paper points out certain recent changes in transportation requirements tending toward greater standardization of equipment, and summarizes the effects of varying operating conditions. The broad basis of economical application is indicated.

THE application of all types of traction motors is based fundamentally on the speed-time curve. Speed-time curve calculations may be unnecessary for many specific applications, but a knowledge of the principles is essential to correct application of motors. The methods of calculation are covered fully in textbooks and for many years have been included in college courses in railway engineering. The purpose of this paper is to illustrate the application of the principles and show the effect on traction-motor performance of varying the more important determining factors.

In the past few years the business of electric transportation has undergone important changes. These have produced changes in apparatus requiring a different technique in application. Until the year 1928 surface cars, rapid-transit cars, and light traction locomotives were propelled by axle-mounted motors driving through single-reduction gears. At that time the lines of traction motors ranged from 25 to 250 horsepower, and the work of application consisted largely of the motor and gear ratio selection to meet the particular service requirements. For this it was usually necessary to calculate the service performance of the motor, chosen on the basis of the engineer's judgment. Motors were self-ventilated and while attention had to be paid to commutating requirements under the most severe conditions of operation and to short-time overloads, the ability to operate within the motor's continuous rating generally insured satisfactory all-around performance. The steady decrease in the cost of energy and increase in car operators' wages had reduced energy consumption to a secondary position instead of one of prime importance which it had occupied 25 years before.

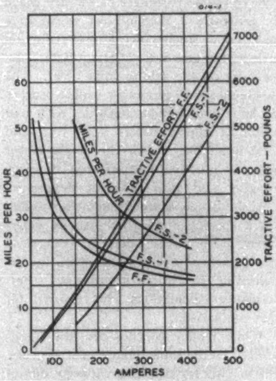

Figure 1. Trolley-coach motor A

Gear ratio, 10.61:1; wheel diameter, 40 inches; volts, 550

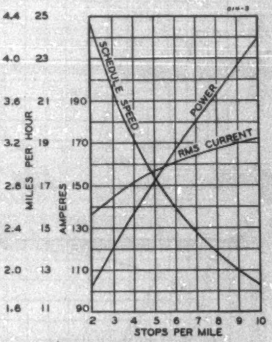

Figure 3. Trolley-coach operating characteristic

Motor *A*, F.S.-1 field strength, normal acceleration and braking

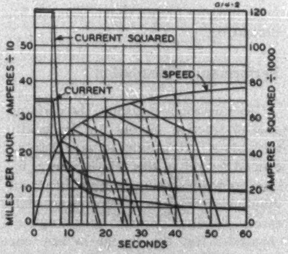

Figure 2. Trolley-coach performance curves

Motor *A*, F.S.-1 field strength, normal acceleration and braking

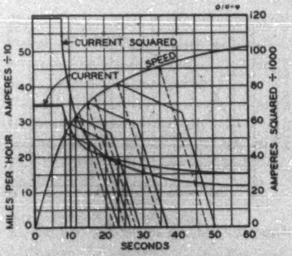

Figure 4. Trolley-coach performance curves

Motor *A*, F.S.-2 field strength, normal acceleration and braking

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Street Cars

Today street-car and trolley-coach motors are of the light-weight high-speed highly ventilated type. Short-time overloads and accelerating currents must be given more detailed examination. Also standardization of vehicles and equipments has progressed. Practically all city street cars purchased are of the Presidents' Conference Committee or similar type utilizing the same electrical equipment. This was the outgrowth of several years' study. All cars are of nearly the same weight and use the same gear ratio and wheel diameter. A study of operating conditions throughout the country was made and the electrical equipment was then designed to meet the maximum safe speed, balancing speed, and acceleration and electric braking rates specified by the committee and to have adequate capacity for the most severe service likely to be encountered. With such standardization, applications to specific cities obviously involve little additional work.

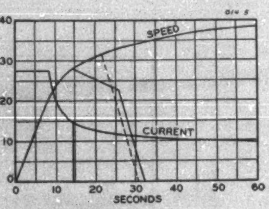


Figure 5. Reduced acceleration rate

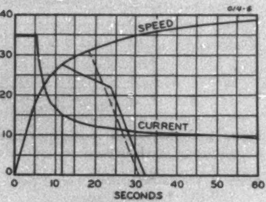


Figure 6. Reduced braking rate

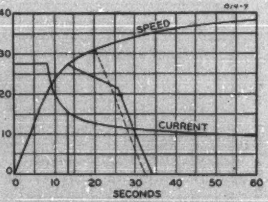


Figure 7. Reduced acceleration and braking rates

Figures 5–7. Trolley-coach performance curves

Motor *A*, F.S.-1 field strength

Trolley Coaches

Trolley coaches came into prominence in 1928. Two sizes are in common use: 30- to 33-passenger and 38- to 44-passenger capacity. The 30-passenger coach is propelled by one 65-horsepower series motor and the 40-passenger coach by two such motors, one 125-horsepower series motor, or one 140-horsepower compound motor. The rear axle of the 30-passenger coach generally has worm drive with 10.25:1 gear reduction. The rear axle of the 40-passenger two-motor coach is generally a double-bowl worm-drive axle or a single-bowl double-reduction axle with a gear ratio of about 9.25:1, depending on the axle manufacturer. The single-motor 40-passenger trolley coaches all have double-reduction axles with gear ratios of approximately 9.25, 10.5, and 11.5 available. Also, the single motor may be used with or without dynamic braking and with different degrees of field shunting. The equipment best suited for a specific application can be determined by economic analysis, comparing the total fixed charges and operating expenses of the various alternatives. Performance calculations are required for this comparison.

While trolley coaches are not standardized to the same degree as street cars, the weights for the same capacity do not differ widely. Hence, when a motor is designed for trolley-coach service, definite effort is made to cover the probable field of application and generally only one capacity of motor is available. Evidently much progress toward standardization has been made.

Performance Calculation

Generalizations regarding the performance of traction motors are frequently misleading. It is safe to say that increased weight of the vehicle always increases energy consumption and increased duration of stop always decreases schedule speed, all other factors remaining unchanged. But the change of any one factor may automatically change some other factor. For instance, if the weight is changed, with a given set of conditions, either the speed margin or the schedule speed must be changed. Both cannot remain the same. Also it is incorrect to assume that changes of the character discussed will always result in the same percentage change, because while the same trend exists the amount of the change is dependent on the characteristics of the particular motor and other conditions.

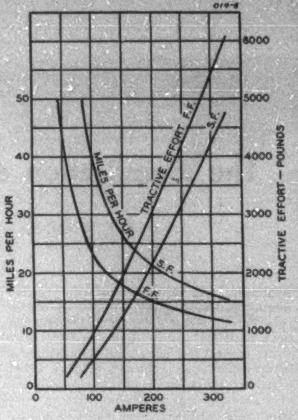


Figure 8. Trolley-coach motor B

Gear ratio, 10.61:1; wheel diameter, 40 inches; volts, 550

Because the trolley coach retains some latitude in the selection of electrical equipment and gear ratio, it is selected to illustrate the method of application. The following basic conditions apply.

The coach weight with average passenger load is 23,000 pounds. The motor has a one-hour rating of 125 horsepower. The gear ratio is 10.61:1 (as extensively used). The effective rolling diameter of the tires is 40 inches. The average voltage is 550, which is typical. Train resistance increases as the speed of the vehicle increases but it varies so widely

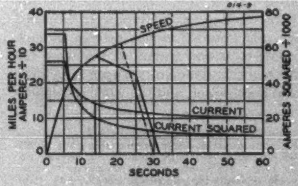


Figure 9. Trolley-coach performance curves

Motor *B*; average voltage, 550; gear ratio, 10.61:1; normal acceleration and braking

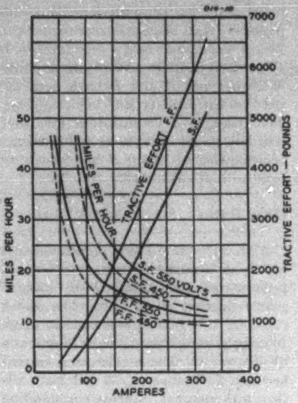


Figure 10. Trolley-coach motor B

Gear ratio, 11.42:1; wheel diameter, 40 inches; volts, 550

with type and condition of pavement that a constant value of 40 pounds per ton is used during motoring. This is amply high for the speeds normally reached. During coasting, the vehicle supplies the no-load losses of the axle and motor. The coasting resistance is thereby increased and a constant value of 47 pounds per ton is used. An average accelerating and braking rate of 3.5 miles per hour per second is used and is as high as can be employed consistently. The minimum time in which the equipment can travel a certain distance under these conditions is increased by a "speed margin" of five per cent (to allow for unavoidable interferences) and schedule speed, motor heating (root-mean-square current) and energy consumption are calculated on that basis.

Figure 1 shows the performance curve of motor *A* on full field and with two percentages of field shunting. The relatively small amount of shunting is designated F.S.-1 and the large amount F.S.-2. By using the motor characteristic, figure 1, and the accepted methods of calculation, the speed-time, current-time, and current squared-time curves are derived and plotted. Starting tractive effort is 4,490 pounds. Accelerating current is 346 amperes. The full-field motor curve is reached at 17.2 miles per hour and the S.F.-1 curve at 18.4 miles per hour. Five different runs between 0.076 mile and 0.369 mile are shown in figure 2.

The one-sixth-mile run may be taken as typical of average trolley-coach service. The required distance without coast is obtained in 29.1 seconds. Adding five per cent speed margin for the typical run with coast gives a running time of 30.56 seconds. The average duration of stop is taken as 7 seconds. Therefore, the total time for this run is 37.56 seconds and the schedule speed is 16.0 miles per hour. The heating current for the total time is 161 amperes which is within the capacity of the motor. The energy consumption is found to be 3.15 kilowatt-hours per coach mile at 550 volts.

The calculated coach performance for the range of runs indicated is shown in figure 3, plotted with stops per mile as abscissas. The decrease in schedule speed and the increases in energy consumption and heating current as the frequency of stops increases are evident. For example, an increase in stops per mile from six to ten decreases schedule speed 23 per cent, increases energy consumption 33 per cent, and heating current 7 per cent.

Field Shunting

The characteristic curve of motor *A*, figure 1, shows by curves F.S.-2 the performance of the motor with fields shunted much more than for F.S.-1. Equipments embodying this characteristic are operating in two cities where they have proved to be very popular with both operators and the public. Their principal advantage lies in maintaining a high rate of acceleration up to high coach speeds and thus enabling the trolley coach to hold a position of superiority where traffic moves unusually rapidly. On the other hand, equipments having the characteristics shown by F.S.-1 are in service in many cities and their performance on the street is entirely successful.

Figure 4 shows the calculated performance with F.S.-2. The full-field accelerating current is the same as for figure 2 but the shunted-field curve is reached at a speed of 25.6 miles per hour instead of 18.4 miles per hour as with F.S.-1. A speed of 30 miles per hour is reached in 10.2 seconds instead of 15.77 seconds. The resulting performance compares with that for F.S.-1 as follows:

Stops Per Mile		Schedule Speed (Miles Per Hour)	Energy Consumption (Kilowatt-Hours Per Coach Mile)	RMS Amperes
2	F.S.-1	24.6	1.84	136
	F.S.-2	28.8	2.21	175
6	F.S.-1	16.0	3.15	161
	F.S.-2	16.7	3.47	186
10	F.S.-1	12.3	4.19	172
	F.S.-2	12.5	4.25	181

Attention is called to the fact that the increased speed of the motor affects schedule speed, energy consumption, and root-mean-square amperes most in the infrequent-stop service and least in the frequent-stop service. The reason for this is the greater amount of operation on the shunted field curve as length of run increases.

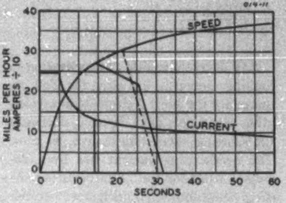


Figure 11. Average voltage, 550; normal acceleration and braking

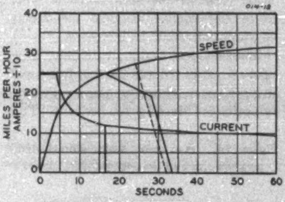


Figure 12. Average voltage, 450; normal acceleration and braking

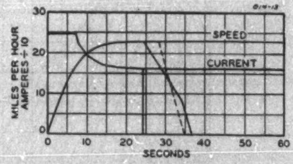


Figure 13. Average voltage, 550; normal acceleration and braking; up five per cent grade

Figures 11–13. Trolley-coach performance curves

Motor *B*; gear ratio, 11.42:1

Acceleration and Braking

Changes in rates of acceleration and braking affect performance. Taking exactly the same conditions as for figure 2, except rate of acceleration and braking, the performance on a one-sixth-mile run has been calculated for the following combinations:

	Rate of Acceleration (Miles Per Hour Per Second)	Rate of Braking (Miles Per Hour Per Second)
Figure 5	2.5	3.5
Figure 6	3.5	2.5
Figure 7	2.5	2.5

Any decrease in rate of acceleration or braking obviously increases the time required for the run. A decrease in accelerating rate alone, as in figure 5, decreases the schedule speed almost five per cent. The accelerating current has been decreased from 346 to 274 amperes but the energy consumption is unchanged because the duration of constant current has been increased. If, instead of maintaining the same speed margin, an attempt were made to make the same schedule speed as in the original example the energy consumption would be increased.

A decrease in braking rate only, as in figure 6, again reduces the schedule speed five per cent. However, the energy consumption is reduced seven per cent. This results from retaining the economically high rate of acceleration and the covering of a greater portion of the distance during coasting and braking when power is not applied to the motors.

When both accelerating and braking rates are reduced as in figure 7, the schedule speed is reduced almost nine per cent and energy consumption is reduced only five per cent.

Slow-Speed Motors

The foregoing comparisons have been with one specific motor. The effect of using a motor with different characteristics, motor *B* of figure 8, will be considered now. This motor has a much slower full-field speed curve than motor *A* and a steep-shunted field curve. In the balancing-speed range the speeds are the same. The effect of the slower full-field curve is to increase the tractive effort per ampere. As a result the starting current is only 260 amperes, or 25 per cent less than the 346 amperes starting current of motor *A*. Figure 9 shows the curves for a one-sixth-mile run.

The schedule speed is 15.64 miles per hour; only 2.3 per cent less than with motor *A*. But heating current is reduced 20 per cent and energy consumption is 17 per cent less. In several cities, motors of both types *A* and *B* have been applied on the same type of vehicle. They have been able to maintain the same schedule speeds and the saving in energy consumption of motor *B* has been very close to that calculated.

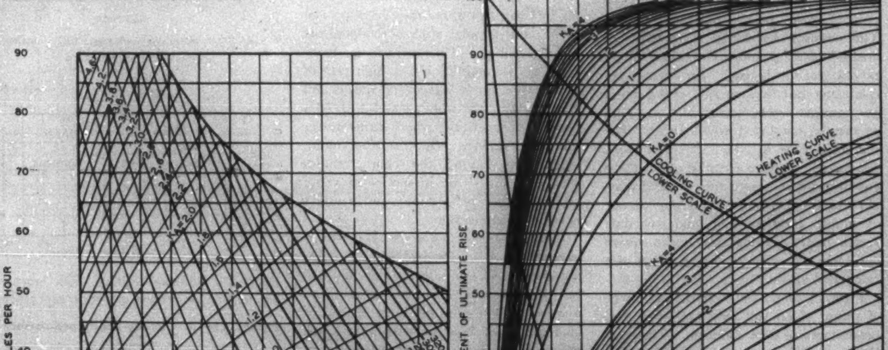
Gear Ratio

With less severe speed requirements it is possible to secure even more economical performance with motor *B* by using a gear reduction of 11.42:1. The motor curve for this gearing is shown by figure 10 and the curves for a one-sixth-mile run by figure 11. The accelerating current becomes 247 amperes, 29 per cent less than with motor *A*. Energy consumption is 20 per cent less but schedule speed is reduced only 3 per cent.

Voltage

Figure 12 shows the effect if the average voltage at the motor is reduced from

Figure 14. A-c series motor ultimate-temperature-rise and time-temperature curves



550 to 450 volts, all other conditions being the same as in figure 11. The schedule speed is reduced almost 5 per cent and energy consumption at the coach is reduced 15 per cent. However, low voltage is usually the result of drop in the distribution system, hence it is necessary to compute the energy delivered by the substation to obtain a true comparison. At 600 volts the values become 2.75 and 2.86 kilowatt-hours per coach mile for the conditions of figures 11 and 12 respectively. The reduction in energy at the car due to slower schedule is more than offset by the energy lost in distribution. Also in any particular service a definite schedule speed must be maintained. Low voltage reduces the amount of coasting and increases the energy consumption at the substation even more than calculated above on the basis of a fixed speed margin.

Grades

Few cities are entirely level. Obviously grades increase running time and energy consumption. When the profile of a line is rolling with its two ends at approximately the same elevation, it is customary to calculate performance on the basis of an equivalent grade resistance. The percentage of equivalent grade is calculated by adding all the rises in feet in a round trip, multiplying by 100, and dividing by twice the round-trip distance in feet.

Where there is a long grade in one direction and a material difference in elevation of the ends of the line it is necessary to calculate both an up-grade run and a down-grade run. Where there are short down grades at various points in a long continuous up grade it is customary to calculate the average percentage grade by adding the rises in feet, subtracting half the sum of the drops, multiplying by 100, and dividing by the length in feet. This formula, as well as that for equivalent grade, is based on the assumption that half of the potential energy in the vehicle at the top of any rise is recovered in useful work in descending the grade and that half is dissipated in the brakes.

On the usual hilly route the profile must be analyzed and divided into various equivalent-grade, up-grade, and down-grade sections. Separate calculations of performance are made for each section and the results combined on the basis of the lengths of the sections. In steam-railroad electrification and rapid-transit work, where the stopping points are definitely known continuous speed-time curves are frequently calculated and the effect of each condition of grade, curve, and speed restriction is taken into account. This process is rather laborious and is used only when the service cannot be represented properly by typical runs.

Figure 13 shows the performance of the same equipment as figure 11 under the same conditions except on a five per cent up grade and with accelerating current held at the same value as on the level. The accelerating rate, therefore, is reduced by the grade resistance of 100 pounds per ton to 2.5 miles per hour per second. Schedule speed is 12 per cent less but energy consumption is increased 75 per cent from 2.52 to 4.40 kilowatt-hours per coach mile.

Rapid Transit

In general, rapid-transit motor application follows the practice used for surface vehicles. In rapid transit, high-speed spring-supported motors with separate gear units have not yet been accepted generally, largely because rapid-transit equipment has always been applied on a very conservative basis. The reasons for this conservatism are (*a*) the seriousness of any failure which may cause a road delay in subways or on elevated structures, (*b*) the large number of cars usually involved in any extension of service, and (*c*) the requirement that new equipment must operate in train with existing equipment. However, in spite of these conditions there is a definite trend toward the use of lighter-weight higher-speed equipment.

Locomotives

The application of motors on locomotives differs from the application on cars or trolley coaches in some respects. The latter vehicles are seldom, if ever, subjected to runs of long duration with the motor tractive effort close to the slipping point of the wheels. Locomotives frequently start and pull trains for long distances under such conditions. Therefore, the locomotive motor must be able to commutate successfully the current required with good, sanded rail. Effort is made to apply electric-locomotive equipment so that the motors in the specified service will not be damaged by any conditions within the limits of adhesion between wheel and rail.

Heavy traction locomotives may run without stops for distances of 50 miles or more over a varied profile. The application of a-c series motors, used principally on such locomotives on electrified railroads, presents problems different from those encountered on d-c motors. The factors which produce heating of the motors are different. The heating of a d-c motor depends almost entirely on the current and ventilation. In addition, the heating on an a-c motor is greatly affected by speed; that is, the current rating increases with reduction in speed. As a result, motor heating depends on horsepower output rather than on current. Consequently, some method other than the calculation of root-mean-square current must be used to determine the heating of a-c series motors.

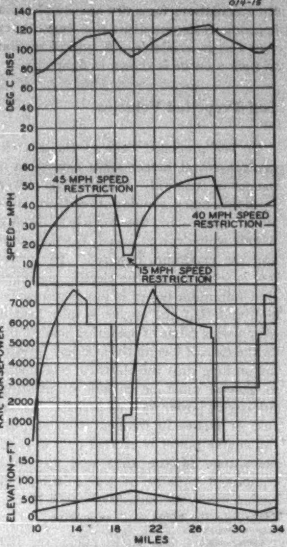


Figure 15. A-c locomotive. Profile, horsepower, speed, and temperature curves

A series of heating and cooling curves for the a-c motor over its entire operating range is determined by test. For each section of the profile the tractive effort and speed are calculated and, from the curves at the left of figure 14, the corresponding values of K_A and ultimate temperature rise are read. The number of minutes for the section is calculated from speed and distance. For this number of minutes the per cent of ultimate rise for the value of K_A is read from the curves at the right of figure 14. This percentage converted into degrees gives the temperature rise in the section. The cooling curve shows the per cent of initial temperature at the end of various durations of no-load operation. When the calculations for a run are completed a continuous temperature-rise curve over that run is available. A typical section of such a curve is shown in figure 15.

Self-Propelled Vehicles

On self-propelled vehicles the electric generator and motor serve simply as a means of smoothly transmitting power from the engine, running at relatively constant speed, to the vehicle's driving wheels which run at speeds between zero and the maximum required. The problem is so to design the electric machines that minimum power is lost in transmission and that the engine is fully loaded over a wide range of operating conditions. In the case of locomotives or cars operating on long runs the electrical equipment in general must be capable of utilizing full engine power for long periods. In the case of busses in frequent stop service, full engine power is exerted only a small percentage of the time and electrical equipment capable of transmitting full engine power continuously would be uneconomically large and expensive.

Gear ratio has little effect on the schedule speed of a self-propelled bus with electric transmission in city service because the input to the electrical equipment is fixed. Therefore, with a specific engine output the tractive effort at any given vehicle speed within the normal operating range is the same regardless of gear ratio, except for the difference in efficiency resulting from different values of current and voltage. The greater the gear reduction with a specific equipment, the greater is the tractive effort at starting, but the lower is the vehicle speed at which the engine is not fully loaded by the generator and "runs against" the governor. Greater gear reductions result in lower current demand and hence lower temperature rise of electrical equipment.

Summary

The basic methods of calculating the service performance of traction motors have proved to be correct throughout years of application. In some installations, at first glance, results in service seem to contradict theoretical calculations. Careful analysis of such cases discloses that the service conditions or methods of operation differ from the basis of calculations. It has been seen that the calculations themselves are simple. To determine the correct basis requires experience and judgment.

To secure maximum economy of operation, the total operating costs must be studied for each application. Higher schedule speeds tend to reduce expense of vehicle operators but increase power costs. For each particular service it must be determined whether increased schedule speed will actually reduce the number of vehicles in service or merely increase wasted time at the ends of the route. On the other hand, it must be borne in mind that high schedule speeds have merchandising value and usually attract additional patronage.

The greatest power economy in city service is obtained by providing motors with the slowest practicable full-field speed and with a shunted-field speed which can meet the requirements of schedules with a speed margin of some five to ten per cent. The highest accelerating and braking rates practicable should be maintained. Vehicles and equipment of the lightest weight consistent with reliable operation and low maintenance should be employed.

In the future a greater degree of standardization may be expected. The continued use of electrical apparatus in transportation is dependent on obtaining the maximum economy both in first cost and operating cost. According to most recent data 8,000,000,000 kilowatt-hours are consumed annually by the electric railways. All electrical engineers should be interested in retaining and increasing this load for the electrical industry.

A New Postgraduate Course in Industry in High-Frequency Engineering

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THOSE FIELDS of engineering which utilize high-frequency electric currents and electronic devices have long ago graduated from the trial and error stage. The empirical experimenter who is ingenious and observant will always be useful, especially in some new and fruitful phase of the art not yet well understood. However, reliance has for many years been placed upon the ability of the engineer to design analytically upon a sound basis in the underlying theory and analytical tools are recognized as valuable aids and guides in laboratory work.

The newer fields of application in radio have made the engineer's need for a firm foundation in theory constantly more essential. Thus, the complication in both number and operation of the components of a television system is such that the ability to predesign results in untold savings. The problems of short waves have made clear to the engineer that many concepts in electricity he had regarded as basic are narrow in scope, limited to comparatively low frequencies, and inadequate if not altogether erroneous when applied to ultrahigh frequencies.

A major recent development in radio is a system combining frequency modulation with limiters. This outstanding improvement was due to a man who possessed intuition, vision, and courage. He is also a fine analyst in the broad sense of understanding exceptionally clearly what goes on in radio circuits. Success would not have been attained without the intuitive invention but it is doubtful if intuition alone could have solved the problem if the inventor had not had a very clear idea of the theory involved. Of course, after the whole problem had been solved technically, great vision and courage and determination were necessary to bring it to its present state of acceptance.

Even those workers who possess the highly inventive type of mind and are able to find new ideas with only a hazy notion of the theory behind their experiments can have their programs radically accelerated by the assistance of fellow workers who can correlate experiment with sound theory.

To push forward the understanding of high-frequency theory, and provide a solid foundation for good design and

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development requires that the engineer possess: (1) the analytical tools; (2) the ability to apply these tools to the solution of practical engineering problems. The latter characteristic includes more than simply the ability to substitute into a formula; it involves also the ability to extract out of a practical situation the problems whose solutions have bearing on the ultimate answers, the judgment to introduce the usually necessary approximations, and finally the important ability to express the results in an easily understandable and useful form.

Need for Course in Industry

Men rarely come to industry direct from college with the amount of training in the difficult radio field that is required for the possession of both of the foregoing characteristics. This is not stated in the nature of a criticism of college curricula. At least as far as undergraduate work is concerned, it is undoubtedly best to give basic and general training to engineers, leaving the study of more specialized branches of engineering to postgraduate work as is being done in many colleges.

Among the subjects of special interest to high-frequency workers but for which there is little time in an ordinary four-year course (and in many five-year courses), is the study of Maxwell's field equations. Unless time is devoted to a study of this theory in postgraduate work, even a phenomenon so fundamental to radio engineering as the radiation of energy by electromagnetic waves is seldom understood. Displacement current may be somewhat familiar to the student but the consequences of the concept are usually not appreciated until problems concerning it are met and solved. Without a feeling for retardation in electrical phenomena one is usually limited to concepts of inductance and capacitance which are valid only at low frequency. Even aside from retardation effects, it is necessary that the high-frequency engineer possess a broader notion of resistance, inductance, and capacity in which current distribution and proximity effects play a part, so that these circuit concepts are not thought of as necessarily independent of frequency. The ideas under discussion here are not new, but there is a need for more young men who thoroughly understand them.

Because it is necessary to teach circuits in a way that will be useful to students who may ultimately enter either the power or communication fields, circuit equations and concepts are most often introduced to the student as one branch of science he must master, and field theory (if treated at all) as another. When the frequency of the circuit is increased to the point that radiation of energy must be considered, the student without additional training is apt to find his circuit concepts inadequate in explaining the phenomenon. For ordinary circuits, he thinks of Kirchoff's and Ohm's laws; for radiation problems there has been little opportunity to equip him with more than a formula or two whose derivation he may not have seen, still less understood.

Transmission-line theory is usually presented entirely from the "voltage along the line" and "current along the line" standpoint. The notion of the transmission of electrical energy by means of propagating fields is so often passed over, or only mentioned as "an interesting way of looking at transmission," that when confronted with the problem of analyzing the transmission of power down the inside of a single hollow conducting tube "wave guide", the student has no basis for attack. Even if time has been found to treat the field concept in college and the graduate does possess a certain amount of appreciation for it, to set up the electromagnetic field equations and solve them in practical cases requires ordinarily more mathematical ability than the student has had a chance to acquire.

In acquainting the student with the operation of electron tubes, the "machinery" of the electronics or radio engineer, the available college time must be limited to the simpler problems. Thus this theory is usually taught in such a way that when higher frequencies are used and problems arise because of the presence of previously negligible transit-time effects, the student must discard what he thought were the basic concepts underlying amplifier or oscillator action and learn a set of more fundamental ones. For example, he can no longer assume that the number of electrons arriving per second at the anode of a diode is exactly equal at every instant to the number leaving the cathode; nor must he take as fundamental that a negatively biased electrode which collects no electrons does by that token not require any driving power.

Some students with more years of college study or an unusual selection of courses obtain a thorough background in the fundamentals of electricity and magnetism and the mathematical tools necessary to apply the theory. To these cases the examples cited are not applicable. However, it is again rare indeed that these men will have had the exposure to the practical type of problem in which the problems themselves are often difficult to isolate and the theory can be used only after a number of engineering approximations have been made.

In general, it may be said that there are three kinds of ability required in engineering work. Analytical ability, though essential, is not the only one demanded by industry. The highly inventive type of mind that contributes valuable ideas, often without apparent need for theoretical background, is one that is unfortunately too rare. The third type of ability, to follow directions and copy designs, also has its place. It is upon the first two kinds of ability, however, that we must depend for technical advancement.

Now, the ingenious inventor is difficult to recruit, for the colleges (especially in undergraduate years) have little time to detect this ability. The analytical man on the other hand is selected by his college record and examinations and trained with success. Since postgraduate study is essential to supply men for the more difficult phases of high frequency and electronics, and since the combination of analytical and experimental ability is so desirable in this field, it has seemed logical to equip men with analytical tools while actually engaged in practical work in industry. This work allows the man with inventive ability to strengthen it while engaged in experiment or design; against it as a background, the newly learned theory may become at once an active tool.

As John Dewey, the famous philosopher and educator has said:*

> "But there is all the difference in the world whether the acquisition of information is treated as an end in itself, or is made an integral portion of the training of thought. The assumption that information which has been accumulated apart from use in the recognition and solution of a problem may later on be freely employed at will by thought is quite false. The skill at the ready command of intelligence is the skill acquired with the aid of intelligence; the only information which, otherwise than by accident, can be put to logical use is that acquired in the course of thinking. Because their knowledge has been achieved in connection with the needs of specific situations, men of little book-learning are often able to put to effective use every ounce of knowledge they possess; while men of vast erudition are often swamped by the mere bulk of their learning, because memory, rather than thinking, has been operative in obtaining it."

This theory of education agrees with the experience of the General Electric Company.

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* From his book "How We Think," pages 52 and 53.

Postcollege Training at the General Electric Company

The General Electric Company has had experience in fitting men with the essential qualifications implied above for work in electrical, mechanical, and thermal engineering. The three-year postgraduate course, known as the Advanced Course in Engineering,† has been in operation for about 16 years during which time it has graduated several hundred engineers. In view of the successful operation of the courses in other branches of engineering a new section of the advanced course has been organized to cover electronics and high frequency engineering. Before describing the new course it will be well to summarize briefly the aims and operation of the advanced course in general.

When the advanced course was instituted, the company realized that its achievement thus far had rested in a large measure upon the sound technical foundation laid by the pioneers and that future progress would depend on filling their places with high-caliber men who had the ability to think along fundamental lines, apply their knowledge to the development of improved apparatus, and deal generally with the advanced theoretical aspects of engineering work. It was recognized that such work is essentially mathematical in character and that in order to deal effectively with such problems one must be able to use mathematics as a tool in the application of fundamental laws of physics to the problems of engineering and must have a real appreciation of its significance as an aid in straight thinking.

Engineering graduates from American colleges, however, usually did not have sufficient training along the necessary lines. Most of them had no mathematical training beyond the calculus, and those who had studied differential equations had not learned how to make use of them in the analysis of physical problems. There were coming to the company relatively few engineers who were thoroughly grounded in methods of analysis. The courses in many colleges had become too practical; men were being taught routine rule-of-thumb methods of design, and there was insufficient emphasis on thinking problems through by the use of fundamental principles.

The experience of the founders of the course, R. E. Doherty and A. R. Stevenson, Jr., and of those with whom they were associated indicated very definitely that in order to be effective the course should have two major objects. These objects were to train men:

1. To apply the fundamental principles of engineering to the solution of their problems.
2. To present the results of their work clearly and concisely, whether in written form or orally, so that others might easily understand and use these results.

In the 16 years since its founding these objectives of the course have been adhered to.

The first year, or *A* class, is common to all sections of the course and is made up of 30 to 40 men of the testing department who have been with the company only a short time. Entrance is competitive and selection is made from among the applicants on the basis of an examination, school records, and personal interview. Personality and ability to co-operate with others are carefully considered, as well as technical ability.

Those selected remain in the testing department and obtain the same experience there as those not taking the course. In addition, they spend a half day a week in class on company time.

The work during the first year consists of a study of the application of the fundamentals of engineering to the solution of problems involving mechanics, thermodynamics, heat transfer, electricity, and magnetism. Throughout the three years of the course, as far as possible the problems selected are ones which have actually arisen in the engineering departments of the company. In many cases unsolved problems have been assigned, and the classes have thus been given the chance to be of assistance in useful engineering work. These problems differ from most textbook problems in that the students are expected to select from a group of facts those which are necessary for the solution. Another important aspect is that usually it is necessary for the students to make a number of simplifying assumptions in order to bring the problem within the range of practical mathematics. About 15 to 20 hours a week are spent at home by the students in solving and writing up their problems.

Lecture Material in the High-Frequency Section

A few of the 12 or 15 men selected each year from the *A* class for the upper classes are given the final two years of that part of the course dealing with electronics and high frequency. They leave the testing department and are taken directly on the payroll of the advanced course. While on this payroll the men continue to spend at least a half day a week in class on company time. The class work during the second and third year is illustrated by the accompanying chart giving briefly the work of the first year, and in more detail, the lectures of the second and third years.

This chart is intended to show how the mathematical, physical, and design material is developed simultaneously so that uppermost in the student's mind is the regard for the analytical processes as useful tools when properly applied in the solution of engineering problems. The subjects given in the table are intended to be merely illustrative and will change from year to year, although the fundamental nature of the lecture material is preserved. The material is adjusted to fit the needs of the company, the problems which arise, and the previous background of the students such as the *A* class. The technical lectures are given by a great many men from many different departments. In this way, the subjects are presented from many different points of view and there is little danger of the course becoming "inbred".

Problem Assignments in High-Frequency Section

The greatest part of the value of the course is obtained by the student in solving the weekly problems. Therefore, their variety and difficulty are a measure of the engineering ability that is both required and developed. For the purposes of discussion the types of problems assigned in the final two years of the course may be divided into several classes, although, with the exception of the first group discussed below, the problems will usually possess characteristics of two or more of the classes to be described.

The first class of problem is the academic type—given certain facts, to deduce certain relations, or prove other facts. This type of problem is one the student is used to solving from his college career. He finds the problem definite and knows that the answer can be found. Problems of this sort are assigned only occasionally as an exercise when a difficult and new concept is introduced; less than ten per cent of the assigned problems fall into this class.

A favorite type of problem, because of its frequent occurrence in practice and because it leads the student to fundamental thinking, is one that may be described as follows: An engineering design is to be attempted along conventional lines which are outlined to the student;

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† "An Advanced Course in Engineering", A. R. Stevenson, Jr., and Alan Howard, ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), March 1935.

Problems Encountered in Design	Theoretical Bases	Mathematical Tools
First year common to all sections		
	Mechanics	Differential equations
		Determinants
		Simultaneous linear equations
		Superposition
	Electricity and magnetism	Vector analysis
	Introduction to field theory	
	Elasticity	
	Waves and resonance	Fourier analysis
	Thermodynamics	Dimensional analysis
	Fluid flow	Functions of a complex variable
	Heat flow	
	Radiation	Operational methods
		Fourier integral
		Numerical integration
Second and third years in high-frequency section		
	Vibrating systems	Hamilton's principle and principle of least action
	Approximate methods for calculating resonant frequencies	LaGrange's equations
		Normal co-ordinates
	Hydrodynamics of sound	Review of vector analysis
	Equations of sound waves	Velocity potentials
	Reflection, absorption, attenuation, interference of waves	Solutions of wave equation Generalized curvilinear co-ordinates
	Radiation from various sound sources	Bessel functions
		Spherical harmonics
Horns Loud speakers	Physiological and psychological considerations in sound	
Microphones Sound measurement	Generalization of experimental laws of electricity and magnetism	
	Displacement current	
	Maxwell's equations	Scalar electric and vector magnetic potentials
	Static fields	Solutions of Laplace's and Poisson's equations
Electrostatic shields		Three-dimension flux plotting
	Varying fields in free space	
	Poynting's vector	Retarded potentials
Antennas	Electromagnetic radiation	Hertzian potential
	Electric waves in free space	
	Electric waves in ionized space	
	Electric waves over imperfect-conducting earth	
	Propagation characteristics of long and short waves	
	High-frequency resistance, inductance, and capacitance	
	Waves in and near conductors, guided waves	Solutions of "heat" or skin-effect equation
High-frequency shields	Maxwell's moving images	
Electrical measurements	Introduction to modern physics (radiation, spectra, thermionic emission, photoelectric effect, etc.)	
	Motion of electrons in electromagnetic fields	
Vacuum tubes	Space-charge theory, static and transit-time effects	
	Kinetic theory	
Gas-filled tubes	Conduction through gases	
Rectifiers	Noises in tubes	
	Coupled-circuit theory	
Amplifiers	Reactance theorems	
Feed-back circuits	Circuit theorems	
Oscillators	Four-terminal network theory	
	Introduction to synthesis methods	
	Conventional transmission line theory	
	Characteristics of amplitude, frequency, and phase-modulated waves	Series expansions with Bessel and other functions
Modulators and demodulators	Nonlinear circuits	
Radio transmitters	Distortion in tubes	
Radio receivers		
Applied optics	Geometrical optics	
	Physical optics	
Television devices	Electron optics	

however, because of certain unusual requirements it is feared that the usual type of design may not be satisfactory. For example, a connecting link is to be designed to operate between the antenna of a transmitter and the transmission line feeding it, and the student is acquainted with the usual type of design. In this case, however, the band width of frequencies to be passed is many times that which has ever been attempted with the common design. Should the design be altered or is it nevertheless satisfactory? Can an improved design more applicable to the wide band width be suggested? To deal with a problem of this type the student must ask such questions as: What constitutes a satisfactory connecting link? How does band width enter to determine the requirements of each part of the whole system and their matching? It would have been much easier for the student, had the problem been broken up into the finding of a frequency characteristic for each part of the system—a series of ordinary circuit or transmission-line analysis problems. Then, the student could have been given a series of rules determined by experienced designers by which he could ascertain whether the frequency characteristics indicate satisfactory or unsatisfactory design. But it is in the study of the bases for these rules that the student derives the training that will enable him to make original analyses in the future.

Many valuable problems come from the analyses of new inventions. Often an idea is suggested by analogy with a device in another branch of science or by certain physical considerations, and an analysis is required to determine whether the new idea is a workable one. In this case the student does not know beforehand whether the problem will require a long analytical study with many approximations, or whether the simple application of such tools as the conservation of energy and momentum or dimensional analysis may yield the necessary conclusion. One thing he is often certain of, however, is that the problem has not been solved before.

> **"When pupils get the notion that any field of study has been definitely surveyed, that knowledge about it is exhaustive and final, they may continue docile pupils, but they cease to be students."***

The largest group of problems are those in which an effect or characteristic is known to be present and the problem is that of obtaining quantitative data. These differ from the usual college problem in that in the first place many of them have never been solved before. The student does not have the assurance that by proper application of the material heard in the lecture the week before he is certain to obtain the result. The problem may not be exactly soluble. Almost always a number of approximations requiring a series of preliminary estimates have to be made. The student must decide whether to use a simple technique with additional computations intended to better the accuracy, or to approximate the problem by an exactly soluble mathematical counterpart for which a precise and elegant solution may be made.

Rotating Assignments

As was pointed out in the introduction, it is essential that the engineer (particularly in the high-frequency field) possess a balance in analytical and experimental ability, for one augments and enhances the other. To give this balance, during the second and third years of the course, the students are placed on assignment in various engineering departments of the company. Here they do regular engineering work varying from practice in standard design and manufacturing to research in the newest fields of radio. A good insight into the workings of a department is obtained during the assignment which may last for three or four months. (This period allows time for from six to eight assignments before completion of the course.)

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* From "How We Think" by John Dewey, pages 197 and 198.

Some of the assignments that the men in the high frequency section may have are as follows:

1. Design of radio and television transmitters and receivers.
2. Design of vacuum tubes.
3. Advanced development problems in radio and television.
4. Design and development of industrial control devices.
5. Design and development of gas-discharge devices of the power type.
6. Mathematical studies in the consulting departments.
7. Research in gas discharges, high-vacuum electronics, dielectrics, vibrations.
8. Research in electrical measurements.
9. District-office engineering, to obtain the customer's viewpoint.
10. Manufacture of radio receivers and vacuum tubes. A knowledge of factory processes and a real appreciation of the factors affecting cost are of prime interest to the radio engineer.

In these assignments, the men have proved so useful to departments in solving difficult problems and contributing new ideas, that the departments are glad to pay the men's salaries and often there are requests for students to fill assignments that must wait because of the limited number of students available.

Conclusions

Under this program, by the end of three years the men have received a thorough training in the fundamentals underlying electronics and high-frequency engineering. They have to a large degree learned how to use the analytical tools needed to apply the theory. It is comparatively easy for the graduate, when he is assigned to one particular phase of radio or television to go on by himself to master the special theory that applies. He is well acquainted with the problems and equipped to pursue their solution. Moreover he has obtained practical experience doing actual engineering work of varied types in regular departments alongside experienced men.

Discussion

H. W. Bibber (Ohio State University, Columbus): Stevenson and Ramo's paper will stimulate the thought of any teacher of electrical engineering who reads it with the problems of education in our field in the foreground of his mind.

To begin with, how is the student a few years out of college going to have the basic concepts in electricity which we gave him, widened in scope and extended to cover the ultrahigh frequencies which have now come on the scene? Here is a chance for the AIEE to help, by the publication in ELECTRICAL ENGINEERING of educational articles specially written for this purpose.

Next, the authors statements as to what the industrial practice of high-frequency engineering requires is very reassuring to teachers who have been advising students who wished to enter this field, to take graduate work with particular emphasis on mathematics and advanced physics. Only a small proportion of students interested in this field can hope to secure employment with a company that has a training course like the one described in this paper.

The quotation from John Dewey serves to remind college teachers of the necessity of continuing some organized laboratory work at the graduate level, in order that use may be made of principles set forth in lectures or in books, "in the recognition and solution of a problem". It is fortunate that high-frequency phenomena may be studied with laboratory equipment that is not beyond the means of the larger colleges to buy or build from parts. The same cannot be said of the study of power systems or large machinery. Postgraduate study while employed in industry has so many advantages that it is only fair to mention one which college postgraduate instruction possesses. That is the easy availability of laboratory facilities to students to settle immediately minor points that may arise in the solution of a major problem. If a student runs into a question of whether an assumption or simplification is justified during an evening's work on a long problem, he may try it out the next morning with a minimum of red tape in securing permission.

It is true that colleges tend to organize and co-ordinate their instruction with assigned problems in courses in order to give a student in a few months a knowledge of principles which it took earlier pioneers in the field many years to learn from haphazard experience. But they do recognize that at an advanced level it is essential for a student to work on problems of an original character, whether he has already mastered the theory necessary to their solution or not. The master's thesis and the doctor of philosophy dissertation are the academic answer to this necessity. A fair share of a graduate student's time is usually devoted to such work, and the award of an advanced degree is usually based on satisfactory demonstration of ability to think independently.

In speaking of ingenuity, the authors state that the colleges (especially in the undergraduate years) *have little time* to detect this ability, and in many other places throughout the paper there is either direct reference to the lack of time and crowding of the curriculum or inferences to the same effect. To speak of the area I know best, I must agree that the curriculum in the great state universities needs lengthening in time with only minor additions to content. If the pressure on our students could be relieved in part they might be encouraged to show inventiveness in their regular course work, or outside of it. They would have time to make use of shop privileges for constructing devices, privileges that I believe are widely offered but little used.

The extension of the curriculum to five years in the state universities should be just a reduction of the credit units per semester, and not a preliminary year (or two years!) of arts work before coming to engineering.

The authors properly place much stress on analysis as one of the desirable qualities to be sought for and developed in students. The counterpart of this quality they mention as "inventiveness," without stressing an educational point which I believe is important precisely in connection with high-frequency work. It is suggested by the item in their list of "theoretical bases" of course content entitled "Introduction to synthesis methods", which is probably intended to apply to synthesis of networks. Most teachers would agree that synthesis should have a far larger place in the scheme of engineering education than it does. It is certainly the fundamental psychological process involved in designing new equipment. Some training in it seems to come as a reflex from training in analysis, and in the machinery field it is difficult to carry out. Thus it would be impracticable to have each student make and put together the laminations, windings, shaft, and frame of a particular motor that he had designed.

But in the high-frequency field, laboratory work intended to develop synthesis is far more practicable. Electron tubes can be built by students themselves within a wide range of dimensions, and circuits built up from a stock of parts which are luckily not so expensive as to prevent a college laboratory from having enough on hand to supply its graduate students. It seems to me that it is in high-frequency work in colleges that synthesis as an intellectual process in electrical engineering may be taught to the extent that it can be taught at all.

A perusal of this paper serves to emphasize the enviable situation of students who secure employment with a big company that can afford to operate a training course —and has the vision to see its value to its future operations—such as that described. For those recent graduates employed in metropolitan districts, the evening courses at the graduate level recently started by some colleges, such as those in the New York City region, may serve as a partial substitute.

Ernst Weber (Polytechnic Institute of Brooklyn, Brooklyn, N. Y.): As the authors point out properly, the average four-year college program in electrical engineering cannot find time for "fundamental" courses, that is, courses which develop the prerequisite mathematical and physical concepts in a rigorous manner and show their applica-

Table I

Mathematical Tools	Basic Theory	Applications
Vector analysis (*M*)	Maxwell's field theory (*M*)	Power transmission and distribution
Functions of a complex variable (*M*)	Operational circuit analysis (*M*)	Advanced electrical machinery
Advanced differential equations (*D*)	Fundamentals of mechanics (*D*)	Network analysis
Higher mathematical analysis I	Fundamentals of radiation	Network synthesis
Matrix and tensor analysis	Fundamentals of electronics	Electronic tubes and circuits
	Statistical mechanics (*D*)	Higher mathematical analysis II
	Quantum mechanics	

tion in practical problems. One might perhaps refer to the usual undergraduate program as a first "sightseeing tour" to be followed in the postgraduate programs by real "exploration". In this sense, the outline offered by the authors is an excellent contribution from the point of view of industrial training courses and comes very close to our own graduate program except that we spend more time on the individual phases and use considerably more rigor in keeping with formal academic training. Thus, our program for the advanced degrees comprises essentially the individual courses listed in table I of this discussion and grouped in a manner similar to the authors' listing of topics, though not necessarily implying a definite sequence. The courses required of all students for the master's degree are marked (*M*), and those required additionally of all students for the doctor's degree are marked (*D*). For specialization in the field of power or communication engineering a sufficient choice of selective courses is available, including courses in other fields not listed in table I.

The similarity between the fundamental first year of the General Electric course and our required courses is striking, as is also the parallelism of the two years in the high-frequency section of General Electric and our own elective list for students specializing in communication engineering. This need not surprise one in view of the fact that most of our evening graduate students come from the research and development laboratories of the industrial and manufacturing companies of the metropolitan area of New York and, therefore, represent just as selected a group of interested and progressive young men as are admitted to the General Electric training-course program. In fact, our students usually bring with them a rich store of individual experience which makes teaching an enjoyable and gratifying activity. Again, as the authors point out, the success of such a fundamental program depends primarily upon the spirit in which it is carried on and that in turn has very much to do with the teachers of the courses. The great advantage of an industrial training program is the availability of nationally known experts in the particular subjects of study, but there is no reason why the same should not be true for the advanced courses in a well-organized graduate school. More and more emphasis is now given to the appointment of experts to teaching positions in the graduate divisions and to encouragement of industrial contact of teachers by means of conferences, of summer work, and of subsidized research. This is particularly true for our graduate division to which we also were able to attract eminent part time faculty members from the metropolitan industrial concerns. Thus, the old criticism that school training is dry and impractical can no longer be maintained with respect to a graduate program of the kind indicated above.

E. A. Guillemin (Massachusetts Institute of Technology, Cambridge, Mass.): The basic ideas set forth in the paper by Messrs. Stevenson and Ramo with regard to postgraduate work at the General Electric Company are unquestionably sound. They apply equally well, however, to the undergraduate curriculum where in fact it is essential to instill in the students' minds ideas which, even though they are elementary, are entirely consistent with the fundamental principles of electromagnetic theory to be developed later. It was this thought which motivated the rather comprehensive revision of the basic undergraduate curriculum for the electrical-engineering department of the Massachusetts Institute of Technology begun some nine years ago. The undergraduate work cannot be as comprehensive as the usual undergraduate plus graduate study, of course, but there is no reason why, by proper selection of material and form of presentation, the undergraduate work cannot provide a background which is thoroughly sound with regard to fundamental principles so that these need only to be amplified, and not discarded for lack of soundness, when the student proceeds to build upon them in his graduate study, whether this is done in college or carried on in connection with his practical work in industry. The very fact that the industry finds it necessary to give a graduate course of this nature indicates that the need for doing something of this sort in the undergraduate curricula (and perhaps in some graduate curricula) is not generally recognized.

As a specific example let us consider a semiqualitative discussion of the relation between field theory and circuit theory. One naturally cannot expect a sophomore to fully master the mathematical mechanism necessary for a complete discussion of this rather complex problem. On the other hand, it is entirely possible and appropriate to forewarn the student that the lumped circuit parameters (*R*, *L*, and *C*) as calculated by the ordinary engineering methods are merely approximations which are adequate for the lower-frequency ranges. Qualitative reasons for this can readily be given; it is even possible to derive rough criteria which show when and why the simple methods may be expected to fail. Even if this first presentation is not properly digested by the student, he has at least acquired an open mind on the question and need not suffer disillusionment later on in finding things to be only half true which he had always been led to understand were the whole and unshakable truth.

It is another principle in our scheme of undergraduate instruction that when a conception is sufficiently fundamental, though admittedly difficult to assimilate, a certain percentage can eventually be made to "stick" if brought to the student's attention in small doses at sufficiently frequent intervals. In our opinion it is definitely unsound to maintain that certain so-called "advanced" concepts are inappropriate for undergraduate instruction. A course of action based upon such a premise makes the shock fatal when the time comes to tell the truth.

Here at MIT, after a certain background for the relation between electromagnetic field theory and circuit theory has been laid in the sophomore work, the student is conditioned to a certain degree to receive a more substantial dose in a subject dealing with transmission line theory which is given to the seniors in the communications option. This subject is introduced by an elementary discussion of electromagnetic wave propagation—plane waves in free space, conductors, or semiconductors, surface waves and the accompanying phenomenon of skin effect. The theory of the radiating dipole and its application to antenna problems, which forms the concluding material in this subject, follows also quite logically from the same introductory discussion.

It is fully realized that only a few of the abler students get more than a moderate grasp of the more advanced portions of such a discussion. We have no illusions on this score. But we do feel that attempts of this nature are worth the time and effort. For if some of the men do continue in this field in their graduate work it will then be a third exposure for them and they should be in a position to accomplish something worthwhile, whereas a serious treatment of fundamental electromagnetic theory will still be just a play of symbols to them if it is their first exposure.

There is, of course, a certain amount of good judgment in postponing the presentation of material of a more special nature until the graduate year because of the fact that the average graduate class is a more select group of students from the standpoint both of ability and serious interest. However, it is also possible to inject into the undergraduate teaching procedure the necessary flexibility to make available to the student a variety of opportunities to suit the individual or group interests and abilities. The procedure of sectionalizing according to mental speed, the individual encouragement given the more able students, and the operation of the honors group system are methods which have proved useful toward making possible the differentiation in subject matter and in teaching procedure necessary for effectively carrying out a more ambitious program.

The comprehensive written and oral examinations given the honors group men in place of the ordinary term examinations provide original problems of a comprehensive nature (contributed by practicing engineers) requiring for their solution the initiative and perspective which Messrs. Stevenson and Ramo point out as being essential to the proper training of student engineers.

Finally I would like to state the opinion that graduate courses in the industry excepting those peculiar to a particular industry are fine for those students who cannot afford such study in college, but since this type of work is a side line for the industry it should follow that the college, whose primary responsibility is teaching, can do a better job of it. On the other hand it is clear that efforts of this sort on the part of the industry are bound to have beneficial effects, not only within their own group but also in academic circles because such activity cannot but stimulate thoughts along these lines by men having teaching responsibilities and thus lead them to make more constructive appraisals of their own efforts.

C. Francis Harding (Purdue University, Lafayette, Ind.): Although possibly directed more effectively to the selection of personnel for such an advanced course than to the content thereof, I would like to ask what credit and advancement are available in such a program for those who have secured their master of science or doctor of philosophy degrees in the graduate schools of universities and colleges? Although it has been stated that such courses in industry are not in competition with similar graduate courses in the universities, yet unless advanced students entering industry can capitalize upon such university graduate training the competition does really exist and the time is actually lengthened during which the new employee must remain in training before he is able to carry responsibility.

The five-year courses which are being considered at various universities and the relation of their resultant product to the regular four-year curriculum, plus a year's graduate course in industry, have been presented. The necessity for more time for assimilation of the curriculum and for thesis or other original work on the part of the undergraduate student has been emphasized. It may be of interest to note that Purdue University has just established two new five-year curricula in engineering to meet such needs. Curriculum *B* is identical in content, and in the bachelor of science degree granted, with the present curriculum *A* except that it is spread out over five years. Curriculum *C* contains approximately 24 credit hours of nontechnical subjects with the specification that there shall be included therein a minimum of three extra hours of economics, six hours of history and/or government, and six hours each of advanced mathematics and physics. The remainder of the extra credit hours are expected to include more of the humanities, English literature, and social sciences. The same bachelor of science degree is granted with certificated recognition of the extra work which has been accomplished. The minors of a master of science degree may also be satisfied in such a curriculum if the subjects are selected with proper care and approval.

These curricula should not be considered as replacing, in any sense, the present standard four-year engineering curricula. They are made available as substitutes for those interested in devoting more time to the securing of the bachelor of science degree or for those who wish to secure a broader cultural background for such a degree. They are not expected to decrease the interest in, nor the election of, the requirements for the more rigorous curricula leading to the master of science degree.

R. W. Sorensen (California Institute of Technology, Pasadena): If I were to apply the common critical methods of my occupation to this paper, I would begin by changing the title to the extent of eliminating the word "new", thus having the title read: "A Postgraduate Course in Industry in High-Frequency Engineering". I do this, because I am not acquainted with any other postgraduate course in high-frequency engineering and I would like to give full credit to the authors for their original, efficient, and complete way of laying the proper groundwork or foundation for a particular engineering activity incident to the work being done by the company by which the authors are employed. When many of us were young men just out of college, our training courses with industry were quite different. We took our place in the shop with the workmen and organized our own theoretical classes or seminars, if any.

As a college teacher, I am more than gratified to note the continued introduction of special advanced courses by industry to meet the needs which arise, particularly as these courses are of a type that not only supplements the work done by technical men in college, but continues to develop in these men, in parallel with their supplementary practical work, an interest and a facility in the theoretical and analytical aspects of the work they are doing.

The course under discussion is such that it could well be a regular graduate course given in an engineering college, and therein lies one danger which should be avoided. That is, I think colleges should not give courses of the type described, not because of lack of quality or trend of such courses, but rather because such courses are too specialized for a general course and hence are too specialized for college courses.

In fact, one of the authors of this paper and builders of the course described in the paper is a young man recently out of college who did not have such a course as a part of his college curriculum. Nevertheless, he has been able to plan and teach the course here outlined. He could do this, because he obtained a doctorate degree for the completion of a graduate course in electrical engineering, a considerable part of which comprised advanced courses in physics and mathematics, taught by the ablest of teachers and research men specializing in these subjects, rather than by engineers somewhat versed in mathematics and modern physics. Even as an undergraduate, the college course which the author had, provided some tools for doing work of the type described in this paper. As the use of one tool was mastered by Doctor Ramo other subjects listed as tools in this paper were added, very much in the order named, as his work continued throughout the graduate course.

In fact, about the only contribution which I see can be made to the paper by my discussion is to show that the course described is sound, because it provides the men taking the course with technical ability which will enable them to make analyses which keep pace with the developments that take place in this field in laboratory and shop. Also, it provides for a continuation of theoretical and analytical study by men who have been properly prepared for such work and thus further knits together our whole program of education in college and industry through a co-operative rather than a competitive program.

As long as colleges confine graduate work to equipping men with analytical tools rather than endeavoring to do things which naturally pertain to industry, in the way of developing designers, etc., and do not make graduate courses merely continuations of undergraduate work—just so long will our whole educational program be an efficient, co-operative one between the colleges on one side and industries on the other.

A. R. Stevenson, Jr., and Simon Ramo: It is reassuring to the authors to find their colleagues in the colleges, who are specialists in the field of engineering education, in general agreement as to the advisability of training along broad, fundamental lines in preparation for engineering work in high frequency and electronics.

The question has been raised as to what credit and advancement are available to students with advanced degrees in a program such as we have described. To answer this, it should first be pointed out that there is no "standardization" in the placing and developing of men in the General Electric Company. This applies also to the placing and training of men in our advanced course. Each man is considered as an individual case. We have had some doctors of philosophy whose ability and training were sufficiently removed from that which we try to develop in the course that they were not able to complete the work of the first year. Others have been given only one or two years of the three-year course and some have been given the job of supervising a year of the course which they did not take.

We like to have men with advanced training in our classes both as students and lecturers. We depend upon these men who come to us with new tools learned under skilled professors to add to our course ideas of which we might not otherwise be aware.

It has been stated in discussion that co-operation between industry and the colleges makes for the most efficient educational program. From our standpoint, this statement has already been borne out well by the discussions presented by the teaching profession; many ideas were put forth which will help us to improve our educational system and make the best use of the collegiate training of the student engineer.

Condensation of Mercury in Mercury-Arc Tubes

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Synopsis: It is found experimentally that the efficiency of condensation of mercury vapor on cold metal surfaces is quite low. The condensing efficiency of steel surfaces is increased several fold by positive-ion bombardment of the surface. Nickel surfaces condense mercury vapor several times as efficiently as steel. The operation of a high-voltage ignitron rectifier was greatly improved by positive-ion bombardment of the condensing surfaces.

IN mercury-arc power converting tubes, there is a continuous evolution of vapor from the mercury-pool cathode. This vapor is recondensed upon condensing surfaces which are kept cool by water, in larger tubes, and forced or natural circulation of air, in smaller tubes. The vapor density existing in a tube will then depend on the dynamic balance between the evolution at the cathode, and the removal of vapor by condensation at the condensing surfaces. It is well known that the successful operation of mercury-arc tubes is strongly dependent upon the vapor density being kept low, at least in the neighborhood of the anodes. If the temperature of the condensing surfaces is allowed to exceed 60 degrees centigrade, operation usually becomes unsatisfactory due to excessive frequency of arc back.

In estimating the vapor density which might exist in mercury-arc tubes, the authors, and probably also mercury-arc tube engineers generally, had not been seriously concerned about a possible low efficiency for condensation on the condensing surfaces. Elementary mechanical considerations lead them to believe that heavy mercury atoms, striking a surface made up of atoms not heavier than mercury atoms, should lose all their kinetic energy with no recoil, and come to rest on the surface. Thus, all mercury atoms reaching the surface should be condensed, with no direct reflection, and mercury atoms should leave the surface again only as re-evaporation. Because of the low temperature of the condensing surfaces, this re-evaporation is generally negligible.

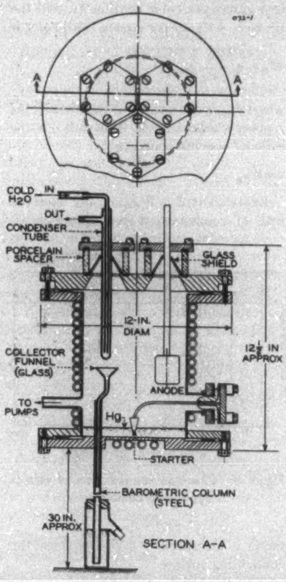

Figure 1. Mercury-condensation chamber

In estimating vapor densities, therefore, the authors never took into account a possible counterflow of molecules from the condensing surfaces, and it seemed that condensing surfaces generally used were sufficient to ensure a low vapor density at the anodes of tubes in operation.

It was a considerable shock, therefore, when Doctor R. C. Mason[1] called their attention to the fact that only for extremely clean condensing surfaces is the direct reflection of mercury atoms negligible, and that slight contamination, quite unavoidable in practical tubes, would reduce the condensing efficiency to very low values. This phenomenon has been known a long time to physicists,[2] but its significance seems not to have been appreciated by engineers.

It seemed desirable then to make a direct study of the efficiency of condensation of mercury on various surfaces under conditions similar to those existing in practical operating mercury-arc steel tanks. Also, it was desired to check the idea that continuous positive-ion bombardment might keep condensing surfaces sufficiently free from contamination that their condensing efficiency might be kept high.

Apparatus

The apparatus in figure 1 was built around an experimental ignitron tank as a foundation. The coils which are soldered to the sides and bottom of the tank were connected so as to form a closed circulating system with a water pump, electric water heater, and a thermostat. Thus, the temperature of the mercury in the bottom of the tank was known to the accuracy of the operation of the thermostat, which was plus or minus one-half degree centigrade. The special top plate, with provisions for three small electrodes was made for this experiment. The three electrodes were insulated from the rest of the apparatus by the porcelain spacers; the bolts which held them in place were insulated from the electrodes by Micarta bushings. Two of the electrodes were of the shape and size of the one shown on the left, while the third was an "anode," as shown on the right. Glass funnels under each of the two similar electrodes conducted the mercury which condensed on to and fell from the condensing tubes to the two barometric columns. Mercury so collected was drained from the barometric columns and weighed at suitable intervals. Readings taken in this manner had no effect on the conditions within the chamber. The third electrode, as shown, was used as an anode when it was desired to run an arc in the chamber, simulating conditions in an ignitron. Tap water was used to cool the condensing tubes. Seasonal fluctuation caused the temperature of the tap water to vary between 10 and 25 degrees centigrade. The vapor pressure of mercury in equilibrium with condensed mercury in this temperature range is low in comparison with that at 60 degrees centigrade, as the pressure doubles for each 10-degree rise in temperature in this range.

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1. For all numbered references, see list at end of paper.

Calculation of Efficiency From Observed Condensation

Besides directly investigating the relative yields of condensed mercury from various surfaces under different conditions, it is desirable to make an estimate of the efficiency, or condensation, coefficient. This coefficient may be defined as the number of molecules condensing divided by the number of molecules striking the surface. If the area under consideration is a condensing surface of extremely poor efficiency, so that there is no appreciable flow, a formula from simple kinetic theory may be used to compute the number of molecules which strike the surface per second. At the other extreme, a surface of 100 per cent condensing efficiency appears to the condensing vapor as a hole into a completely evacuated chamber. In this case, the vapor at the condensing surface is in a state of flow, and its density and temperature are different from that of the vapor at rest far from the surface. We may calculate what this flow will be by using the mathematical theory of the mechanics of continuous fluids, although we must recognize that at the condensing surface this theory fails to be applicable, since the vapor there, with its non-Maxwellian distribution of molecular velocities, cannot be said to have a scalar pressure and temperature as contemplated in fluid mechanical theory. However, this theory should be applicable up to within a few molecular mean free paths from the surface. We may then attempt to modify the simple kinetic-theory formula, by taking into account the flow and the altered density and temperature predicted by the continuous-fluid mechanical theory.

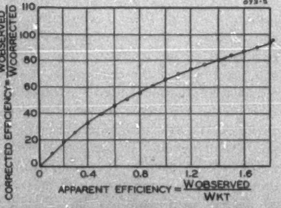

Figure 2. Calculated efficiency as function of observed yield

Kinetic theory gives v the number of molecules crossing from one side to the other side of a unit surface in unit time in a gas in equilibrium as

$$v = {}^1/_4 N\bar{c}$$

where N is the number of molecules per unit volume, and $\bar{c}$ is the mean velocity of thermal motion. The weight in grams of these molecules is

$$W_{KT} = p\sqrt{\frac{M}{2\pi RT}} = 43.7\times 10^{-6} p\sqrt{\frac{M}{T}} \qquad (1)$$

where p is the pressure, dynes per square centimeter, M the molecular weight, T the absolute temperature in degrees Kelvin, R the gas constant in ergs per degree, and the units of area and time are the square centimeter and second.

From the mechanics of continuous fluids the mass of a perfect gas flowing per second across an area of one square centimeter at a point where the density is ρ grams per cubic centimeter, and the pressure p dynes per square centimeter is

$$W_H = \sqrt{\frac{2\gamma}{\gamma-1} p_0\rho_0 \{x^{2} - x^{\gamma+1}\}} \qquad (2)$$

where p_0, ρ_0 are pressure and density at a point where the fluid is at rest, γ is the ratio of specific heats and

$$x = \rho/\rho_0$$

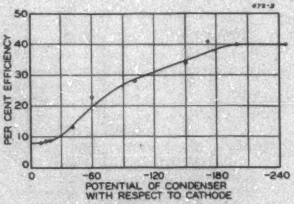

Figure 3. Effect on condensation of voltage applied to condenser

Experimentally, W_{observed}, the observed rate of condensation per square centimeter is measured. Obviously, near the condensing surface

$$W_{\text{obs}} = V\rho = Vx\rho_0 \qquad (3)$$

where V = mean or hydrodynamical velocity of motion of the gas toward the condensing surface. Assume that the molecules of the gas have a Maxwellian distribution of velocities about a set of axes moving toward the surface with the velocity V, although as mentioned before, this cannot be true within a mean free path of the condensing surface. From equations 2 and 3, we calculate V and x. We then return to (1) and modify it to obtain an expression for the number of molecules striking the surface in terms of the pressure and temperature of the vapor in regions unaffected by the condensation, and where it is at rest, taking into account the effect of the hydromechanical motion corresponding to the observed rate of condensation. Equation 1 is derived by integrating

$$\rho\int_0^\infty uf(u)du$$

where $f(u)$ is the Maxwellian distribution function for the velocities in a direction perpendicular to the area. If the body of the gas or vapor is approaching the surface with a velocity V, the distribution function will be $f(u-V)$. As we are interested in knowing how these conditions deviate from those in our first equation, we let

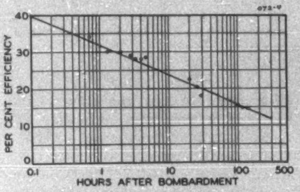

Figure 4. Decrease of condensing efficiency with time after bombardment

$$R = \frac{\int_0^\infty uf(u-V)du}{\int_0^\infty uf(u)du}$$

$$= e^{-\left(\frac{V}{\alpha}\right)^2} + \sqrt{\pi}\frac{V}{\alpha}\left[1 + P\left(\frac{V}{\alpha}\right)\right]$$

where $P(y)$ is the probability integral

$$P(y) = \frac{2}{\sqrt{\pi}}\int_0^y e^{-y^2}dy$$

and α is the most probable molecular velocity. Thus, equation 1 should be multiplied by R to allow for the motion of the vapor toward the condensing surface.

As the gas moves toward the condensing surface, it undergoes an adiabatic expansion. The changes in p and T from the equation of state of a perfect gas are given by

$$x = \frac{\rho}{\rho_0} = \left(\frac{p}{p_0}\right)^{\frac{1}{\gamma}} = \left(\frac{T}{T_0}\right)^{\frac{1}{\gamma-1}}$$

where γ is the ratio of the specific heats which for mercury vapor is 1.66. Hence, equation 1 will need to be modified to allow for this changed density and temperature by multiplying by $x^{\frac{\gamma+1}{2}}$. The value of α in the correcting factor R, is also a function of x.

Combining all of these correcting factors, we obtain for calculated mass of molecules striking a centimeter square of area of condenser per second,

$$W_{\text{corrected}} = W_{kT}x\frac{\gamma+1}{2}R$$

$$= A(M, p_0, T_0, \rho_0, \alpha_0)F(W_{\text{obs}}) \qquad (4)$$

where A is a function of the conditions existing in the vapor at parts unaffected by the presence of the condensing surface.

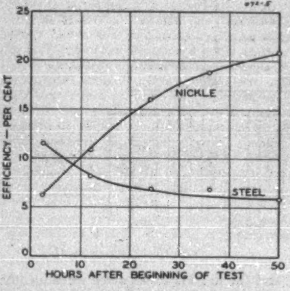

Figure 5. Efficiencies of condensers during a test

The corrected efficiency $W_{\text{observed}} \div W_{\text{corrected}}$, can then be calculated from the apparent efficiency, $W_{\text{observed}}/W_{kT}$. A curve of corrected efficiency is plotted against apparent efficiency in figure 2. The data in this paper are expressed in terms of the corrected efficiencies as taken from figure 2.

Experimental Results

With the temperature of the condensed mercury 60 degrees, and that of the condensing surfaces 10 to 25 degrees centigrade, the efficiency of condensation of mercury vapor on freshly sand-blasted steel electrodes is found to vary between 1.5 per cent and 8 per cent. This variation is attributed to different surface conditions.

Surfaces which have been roughened with coarse alundum cloth and washed in alcohol or benzene give results similar to sand-blasted surfaces, as do freshly machined (rough lathe turned) steel surfaces. We found no treatment of a steel surface, prior to its insertion into the vacuum chamber, which would cause its efficiency of condensation to be higher than that of a sand-blasted surface.

To simulate conditions existing in an ignitron, the third electrode (on the right in figure 1) was used as an anode, and an average current of 15 to 50 amperes was passed through the tube when it was being used as a half-wave rectifier feeding a resistance load from a 220-volt a-c power source. It was found that the yield obtained from the condensing tubes during alternate one-hour periods when a 15-ampere arc was run in the chamber was 20 per cent higher than that observed during the interspersed one-hour periods when no arc was run. This was taken as an indication that the presence of the arc raised the pressure of the mercury vapor in the tube by 20 per cent.

Effect of Negative Voltage Applied to Condensing Surface

The application of a negative potential, with respect to the cathode, to the condensing tube is believed to cause it to be surrounded by a positive-ion sheath during the conducting half cycle. The thickness of the sheath, according to Langmuir's theory of currents to a probe in a plasma, is small in comparison to the diameter of the condensing tube. Positively charged mercury ions then will fall through the sheath and bombard the condensing surface with an energy which is proportional to the potential difference between the arc plasma and the condensing tube. The positive-ion current collected by the condensing tube does not vary as the negative potential of the tube is increased, which is as should be expected if the thickness of the positive-ion sheath remains small, since this current is determined by the random positive-ion current density in the plasma, and all of the positive ions which come to the sheath are drawn to the condensing tube.

The bombardment of the condensing surface by positive ions has a remarkable effect on its efficiency of condensation. The manner in which the efficiency increased as the negative potential was increased is shown in figure 3. These data have been corrected for the increase in mercury-vapor pressure caused by running the arc, as explained earlier. The observed increase in efficiency cannot be explained by the simple collection of positively charged molecules, since the current drawn by the condensing tube and the amount of condensation are of such magnitude that there are of the order of 1,000 mercury atoms collected for each positive charge. Hence, we must conclude that the positive-ion bombardment so alters the condensing surface as to increase its efficiency for the condensation of neutral molecules. The efficiency of the condensing surface as a function of time after the arc is turned off is shown in figure 4. The rate at which the efficiency decreases, as well as the value it reaches in a given time, is a function of the length of the preceding bombarding period; if the bombardment has been for a few hours instead of several days, as it was before the data of figure 4 were taken, the efficiency returns to its initial value of less than ten per cent in less than a day.

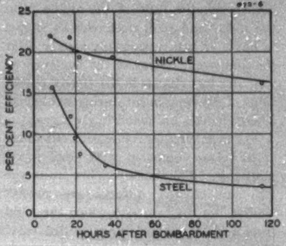

Figure 6. Decay in efficiency after stopping positive-ion bombardment

Condensation on Nickel

The condensation of mercury on a nickel surface was also investigated. One of the steel electrodes shown in figure 1 was replaced by a similar one made of nickel, and in this manner the behavior of the two surfaces was compared under identical conditions. The interesting difference in the rate at which mercury condenses on the two surfaces is shown in figure 5. In this instance, the steel electrode had been bombarded by positive ions before the nickel electrode was placed in the chamber. No arc was run after the insertion of the nickel electrode. The yield from the steel electrode declined as in figure 4, but the yield from the freshly sand-blasted nickel electrode increased during the same interval of time.

When the nickel and steel electrodes are bombarded by positive ions, the maximum yield obtainable from each is similar. After the arc was turned off, the yields from the two electrodes decreased as shown in figure 6.

When two freshly sand-blasted electrodes, one steel and one nickel, were placed in the evacuated chamber and a temperature difference between the mercury pool and the electrodes maintained, the interesting curves of figure 7*a* were obtained. The absolute yields show large fluctuations during the month period, but the ratio of the yields from the nickel and steel electrodes as shown in figure 7*b* was remarkably steady after the first week. Some of the fluctuations were due to a change in the steady conditions of the experiment during weekends. These changes are noted in the figure.

The much greater condensing efficiency of nickel as compared with steel is remarkable. The initial yields from the two sand-blasted surfaces are always similar, but the rate with which the yield from a nickel surface improves did vary. Thus, as shown in figure 5, the improvement was more rapid than in figure 7*a*. Apparently the rate of improvement depends on the time between sand blasting and the setting up of a temperature difference for condensation.

Effect of Electric Excitation of Condensing Surfaces in Operating Rectifiers

Some preliminary tests were made to determine whether improving the condensing efficiency by positive-ion bombardment of condensing surfaces would give any improvement in performance of operating rectifiers. For these tests we used a pair of ignitrons as a full-wave rectifier, feeding into a resistance load at 8,000 volts direct current. During the greater part of the test, the load current was maintained at 25 amperes, and the condensing surfaces were energized on alternate one- or two-day periods, unenergized on the intervening periods. The number and distribution of observed arc backs are shown in figure 8*a*. The arc backs appear to occur at random during both the energized and unenergized periods. The average number of arc backs per hour was 0.862 and 0.08, a reduction of more than ten by the application of potential to the condensing surfaces.

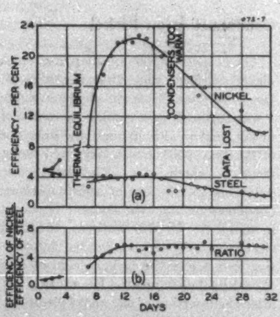

Figure 7. Comparison of nickel and steel condensers over 30 days

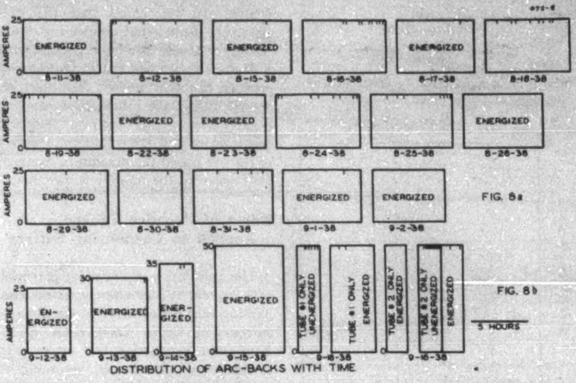

Figure 8. Plot of arc-backs observed with two ignitrons feeding an 8,000-volt 25- to 50-ampere resistance load

Arc-backs indicated in figure 8*a* are for either tube, those in figure 8*b* are for the individual tubes

The first part of the test was concluded by the failure of the mechanical vacuum pump. After the ignitrons had again been treated out, a short test was made at higher currents, to 50 amperes. The data of this test are shown in figure 8*b*. An entire day without a failure was obtained while the condensing surfaces were energized on September 15. The next day the surfaces of one tube at a time were de-energized, and the data for the two tubes are shown separately. Both tubes operated satisfactorily for a while, but the one whose condensing surface was de-energized soon began to fail. Tube 2, when de-energized, ultimately became so bad that it would not carry the load for as long as one minute. When the surface was energized, it failed only twice in the following two hours. This complete failure to carry the greater load of 50 amperes when the surfaces were not energized, and the immediate ability to operate satisfactorily when the surfaces were energized were verified by data similar to that in figure 8*b* several times.

However, when the tubes were being operated at 3,000 volts direct current, and the arc backs occurred infrequently, no definite improvement was obtained by the application of potential to the condensing surfaces. Similar tests on larger ignitrons at 600 volts also failed to show conclusively that arc backs were reduced in number by improved condensation.

Summary and Conclusion

1. Very low condensing efficiencies for mercury usually less than ten per cent were found for water-cooled steel surfaces under conditions simulating those obtaining in practical steel-tank rectifiers.

2. Positive-ion bombardment of condensing surfaces caused a great increase in their condensing efficiency.

3. Nickel surfaces usually showed a condensing efficiency several times as large as for steel surfaces.

4. Positive-ion bombardment of the condensing surfaces of an 8,000-volt d-c 25-ampere single-phase ignitron rectifier greatly improved its operating performance. Similar treatment of a 3,000-volt 200-ampere single-phase ignitron, however, gave no improvement in performance.

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Electric Power for Airplanes

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MEMBER AIEE

Synopsis: Electric-power loads are increasing rapidly on airplanes and auxiliary-engine-driven generators offer dependable power sources independent of main engines. Minimum weight for the required capacity is essential in the design of generators, motors, and control. The a-c 110-volt 400-cycle three-phase system has been developed for large airplanes with heavy motor loads. Alternators rating 12.5 kva at 75 per cent power factor and two-pole motors running at 22,500 rpm full-load speed are available. Control apparatus builds up the alternator, establishes accurate voltage regulation, and provides direct current for excitation and battery charging. The 24-volt system is applied on airplanes using d-c power which have relatively light motor loads. Generators rate 5 kw and are designed for speeds ranging from 3,200 to 6,000 rpm to conform to prime-mover requirements. Motors have been built which run at 7,500 rpm. The control effectively regulates the voltage, handles battery charging, and provides for starting the power plant.

Featherweight Electrical Apparatus

An engineer intently watches the speeding motor shaft—so fast that it is just a blur to the eye. The light of the stroboscope is turned on it and the shaft is seen stationary. The surface of the small 22,500-rpm rotor of the six-horsepower motor travels the distance from New York to Chicago in less than four hours. This is one of the impressive electrical developments for airplanes wherein speed goes up and weight comes down. There is an insistent demand to save "pounds" in aircraft electrical equipment and thus increase "pay-load" capacity. Featherweight generators, motors, and control are now available for such applications.

Airplane Electric-Power Problems

The growth of electric-power loads on airplanes coincides with increases in size, speed, and flying range. Aircraft developments have been extensive during the past few years, resulting in substantial boosts in the uses of electrical energy. Small 6- or 12-volt d-c automotive-type generators, either main-engine driven or located in the air stream, have long served as power sources. These low-voltage systems are inadequate for modern airplanes because of low weight efficiency. It is necessary to apply large generators operating at relatively high voltage to assure economic installations. The result has been the development of the auxiliary-engine-driven power plant, trial of the five-kw main-engine-driven generators, and investigation of steam, hydraulic, and exhaust gas turbines as prime movers.

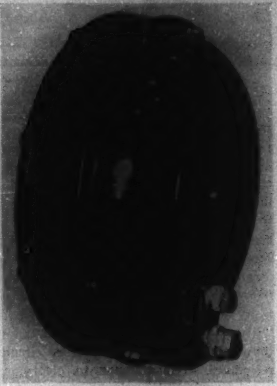

Figure 1. Aircraft alternator

This is a 400-cycle three-phase machine and is rated 12.5 kw, 120 volts at 75 per cent power factor. It weighs close to 100 pounds

The auxiliary-engine-driven generator offers a satisfactory means of power supply, continuously available under all operating conditions, independent of the main engine. Units are designed to secure minimum weight for a specific flight time. This requires consideration of the weight of the fuel, oil, engine, generator, and control as well as the efficiency of the plant. Low fuel and oil consumption, which means high engine efficiency, are pertinent in equalizing the weight of fuel and oil with that of the engine. The generator requires the same consideration. A balance must be reached for generator active parts in comparison with fuel and oil weight for a given flight period.

Motors are being extensively used to perform various functions on airplanes. Light weight is essential and in this case, efficiency, the duty cycle, and ventilation affect design. Here again, efficiency has a bearing on the power used and can be evaluated in terms of fuel and oil weight. However, the duty cycle is a factor since high efficiency becomes progressively more important in terms of fuel and oil weight for momentary, intermittent, and continuous operation. Motor weight will go up with decreased ventilation which makes it desirable to limit the use of explosion-proof and enclosed types.

110-Volt A-C System

The 110-volt 400-cycle three-phase a-c system is applicable to large aircraft. Alternators have been built and tested which are rated 12.5 kva at 120 volts and 75 per cent power factor, and weigh practically 100 pounds. The machines operate at 3,430 rpm, a speed which is fixed by the prime mover. Rotors have 14 poles and are mounted on engine-shaft extensions to eliminate bearings. The same shaft extension carries a propeller-type fan which provides additional flywheel effect, cools the engine, and ventilates the alternator. Stators are attached directly to engines.

Alternator efficiency is 92 per cent, a figure selected after an analysis of the weight of the complete power plant plus fuel and oil. Above 92 per cent efficiency,

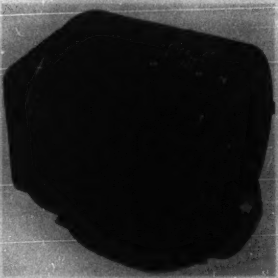

Figure 2. A 400-cycle three-phase a-c aircraft motor

This machine is rated six horsepower at 110 volts and weighs slightly over 15 pounds. When operating at full load the speed is 22,500 rpm

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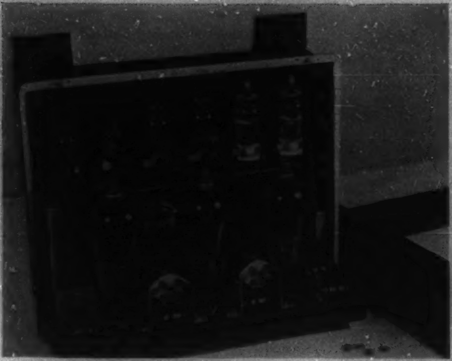

Figure 3. Aircraft electronic voltage regulator used with the 110-volt 400-cycle three-phase a-c system

This device regulates the voltage of each of the three phases and accurately maintains the bus potential

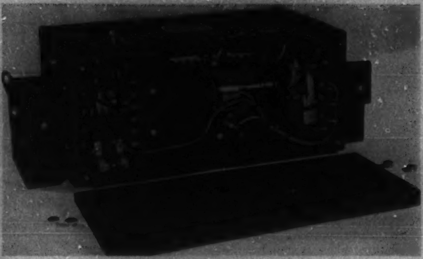

Figure 4. Aircraft battery charger for the 110-volt 400-cycle three-phase a-c system

This dry plate type rectifier furnishes either 14 or 28 volts direct current for battery charging and the operation of d-c auxiliaries

Figure 5. The aircraft control station contains all the necessary instruments and switches for the operation of two 110-volt 400-cycle three-phase a-c power plants

the increase in alternator active parts balances the fuel and oil weight reduction for a 40-hour flight. Overload capacity is determined by sudden load changes, such as may occur with the starting of large induction motors. This is particularly true if the lights and continuous-running motors are operating and momentary starting loads are applied. This condition may reduce voltage too much and consequently alternators are designed for high current overload capacities at low power factors.

Lightweight high-speed induction motors are well suited for 400-cycle three-phase operation. A wide speed range is obtainable by varying the number of poles and machines have been designed to meet many different requirements. Some applications demand high torques for momentary or intermittent use, while others need high efficiency for continuous operation. Two-pole machines have a synchronous speed of 24,000 rpm and normally operate at 22,500 rpm when fully loaded. The larger sizes develop double full-load torque with 3 to $3^1/_2$ times full-load current.

The control regulates the a-c power supply, furnishes 14 or 28 volts direct current for battery charging and d-c auxiliaries, establishes satisfactory parallel operation, and practically eliminates radio or audio interference. The electronic voltage regulator, which has been developed, is faster in response than the mechanical type. Simpler and more positive antihunting methods are obtainable for less weight. It is both a regulator and exciter in one device since d-c field power is provided by rectification from the a-c bus. The regulating tubes are in duplicate to assure reliability. An intermittent-rated Rectox and transformer cause the alternator to build up from its residual magnetism. This function is under the control of a small "Silverstat" regulator which corrects for temperature variations in the circuit resistance. The transfer to electronic regulation is automatic by means of a small thermal timing relay. This method of initial build-up is necessary since sufficient time must elapse for the electronic regulator tubes to reach operating temperature before field current is obtained by rectification.

The engine driving an alternator is started and automatically idled until it reaches operating temperature after which it comes up to normal speed. The alternator switch is then manually placed in the "build-up" position and the voltage rises to normal value under self-excitation and magnetic voltage-regulator control. After some 45 seconds, during which the filaments of the electronic regulator have reached normal operating temperature, the alternator control switch automatically trips to the "run" position. The machine is then under electronic regulation and may be connected to the main bus by closing a circuit breaker. When a second unit is started and is under electronic regulation it may be connected to the bus by closing the circuit breaker. If a reversal of power occurs manual-reset wattmeter contacts function to disconnect the alternator from the bus.

The battery-charging unit consists of a dry-plate rectifier fed from a three-phase transformer through a tap-changing device under control of a mechanical voltage regulator. The rectified voltage is maintained practically constant regardless of load and ambient temperature changes. A central control station contains the alternator circuit breakers, the manually operated build-up switch, and necessary metering equipment.

The 110-volt 400-cycle three-phase system is suitable for lightweight radio equipment and is practical for three-phase induction motors. The use of three wires in conduits, together with the

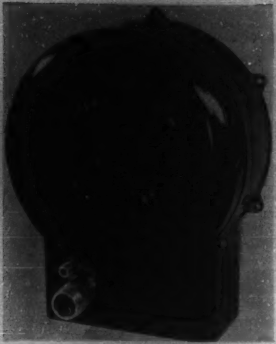

Figure 6. This 4,000-rpm aircraft generator is rated 5 kw, 28.5 volts, 175 amperes. It has a five-minute rating of 7.5 kw. The total weight including the starting switch is just over 50 pounds

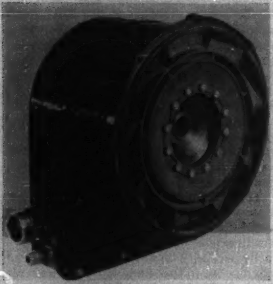

Figure 7. This aircraft generator is in the featherweight class and weighs slightly over 40 pounds including the starting switch. It is rated 5 kw, 28.5 volts, 175 amperes, and will carry 7.5 kw for five minutes. It runs at 6,000 rpm

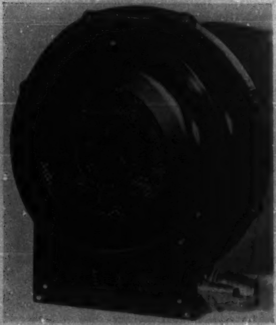

Figure 8 (left). This aircraft generator runs at 3,200 rpm and weighs just under 70 pounds including the starting switch. It is rated 5 kw, 28.5 volts, 175 amperes, and will carry $6^3/_4$ kw for 15 minutes

proper balance of single-phase loads between the three phases, avoids the impedance drops which occur with single phase. Motors will start and produce rated outputs without the use of centrifugal switches, external capacitors, and other control devices. Thus, the system is well adapted for application on large airplanes which make extensive use of electric power and have heavy motor loads.

24-Volt D-C System

The 24-volt system is becoming a standard on airplanes using d-c auxiliary power. Generators have been built which are rated five kw, 28.5 volts, 175 amperes and will carry short-time overloads of 150 per cent normal capacity. Operating speeds are 3,200, 3,430, 4,000, and 6,000 rpm and are determined by the available prime movers. In the case of the 6,000-rpm machine the engine runs at 3,000 rpm. Machines are designed with magnesium-alloy supporting brackets and housings to assure light weight. Fans are magnesium, aluminum, or a heavier metal depending on the added flywheel effect needed for the engine when the armature weight is insufficient. The effectiveness of this weight varies directly as the square of the radius and the fan is the largest-radius rotating member. Special iron alloys are used for the magnetic circuit. Insulation is composed entirely of glass, mica, and asbestos. Generator armatures and fans are mounted on shaft extensions and stators are bolted directly to engines.

It appears that motor speeds will remain at or below 10,000 rpm. The inherent limitations of armature construction and the high starting torques required tend to limit maximum speeds. Motors which have been built run at 7,500 rpm as compared with the much higher speeds practicable with a-c induction types. The materials used and the insulation are essentially the same as for generators.

The control regulates the d-c power supply and furnishes charging current for a small battery. It automatically disconnects the battery when the power plant is shut down and functions with little radio or audio interference. The voltage regulator is a small lightweight device utilizing a modification of the industrial Silverstat. Contact maintenance is practically eliminated by distributing the regulating duty over 15 to 20 contacts, each operating safely below the "sparking voltage". Rocking-type bearings, spring restrained, minimize friction and wear.

Change in regulated voltage during the warm-up period has been reduced by using low-temperature-coefficient wire in the regulator coil. Correction for ambient-temperature variation is provided by bimetal compensation of the calibrating-spring tension. The ratio of battery capacity to that of the generator is small, which makes it necessary that the reverse-current device drop out at a small current

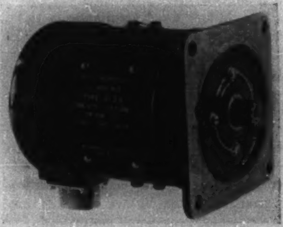

Figure 9. This aircraft motor develops five-eighths horsepower at 25 volts and 7,500 rpm. It weighs close to seven pounds complete

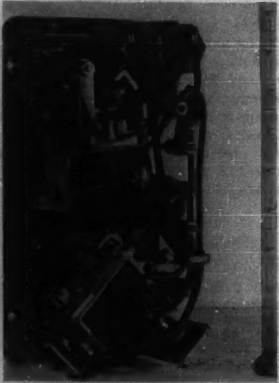

Figure 10(right). This aircraft voltage regulator and reverse-current switch is applicable to the 12-volt d-c system

It weighs under four pounds and maintains accurately regulated bus voltage for 15-volt main-engine-driven aircraft generators

Figure 11. This aircraft voltage regulator and reverse-current relay handles the 24-volt d-c system

It maintains 28.5 volts at the bus. The complete weight including cover is less than nine pounds

value. The relay closes when generator voltage is one volt above battery voltage and opens on less than ten amperes reverse current. It operates the generator contactor which has silver contacts of adequate capacity to handle 175 amperes rated current. By using the generator as a motor to start the engine there is no need for a separate starting motor and its coupling. This combination of functions reduces equipment and cable weights since a separate heavy starting-motor circuit is unnecessary.

The 24-volt d-c system appears to be best for standardizing on one potential for a number of different sized airplanes. It is possible to handle readily battery charging without upsetting present standards. Two 12-volt batteries are applicable, thus facilitating changing from 12 to 24 volts by having 12 volts available during the changeover period. The 24-volt system can be employed advantageously on the lighter types of airplanes which have small motor loads.

New A-C and D-C Apparatus Available

The past 18 months has been a period of intense development of auxiliary engines, generators, motors, and control for airplanes. The two systems now available—110-volt 400-cycle three-phase a-c and 24-volt d-c—meet present requirements and provide for future increases in the use of electric power. The former is well suited for large airplanes with predominating motor loads and the latter is an applicable standard for medium-sized craft with light motor loads.

Discussion

V. H. Grant (United States Navy Department, Washington, D. C.): Mr. Clardy has presented some interesting points on a-c power for aircraft. The Navy, having at least as extensive and complex aircraft electrical systems as exist today, has been undertaking a comprehensive research program on electric power for airplanes. Our results to date do not check entirely with Mr. Clardy's analysis, and a brief summary of our points of difference may be of interest.

First, auxiliary engines for driving generators are extremely expensive, as compared to main-engine drive, in reduced airplane performance and lower useful or pay load. An analysis which we made recently indicates that a 50,000-pound plane suffers less loss in performance by diverting 50 horsepower for auxiliaries from the main engines than in carrying additional auxiliary engines and the extra fuel and oil which they require.

Second, a twin-engine airplane with two auxiliary engines is actually a four-engine plane, insofar as maintenance and overhaul are concerned. The small engines are almost as complex to overhaul as are main engines.

Third, valuable space in the plane must be appropriated, and made soundproof and fireproof, for auxiliary engines.

Fourth, auxiliary engines present an unnecessary additional target, in the case of military planes.

True, main engines are "cleaned up" by the use of auxiliary engines, and a constant drive speed is obtained, permitting the use of a-c generators but these advantages are gained at a considerable penalty. And, whether we want alternating current after we have provided for its generation is questionable, at least for our peculiar requirements. Some of the arguments on this matter may be summarized as follows:

1. Practically all our motors are of the momentary-duty type, requiring high starting torque; they are designed on the basis of torque rather than heating. A d-c series motor is ideal for this service.

2. D-c generators use simple regulators and may be paralleled easily.

3. Battery stand-by may be used for vital services and for peak loads.

4. Much of our load, if alternating current, would be operated from a single-phase source. Load balancing during emergency operation would probably keep the poor flight engineer too busy to do anything else.

5. Voltage diversity would be simplified, but, to date, radio equipment, the only load requiring voltage change, cannot be operated in parallel with fluctuating power loads, owing to the light weight master oscillator circuits being very critical of supply voltage.

6. High-speed motors, although very light in weight, require gear reducers of considerable weight to provide usable speeds. Starting these motors with their low torque, at low temperatures with the gear box grease congealed, may be a problem. Also, the matter of bearing design and lubrication is by no means solved.

7. Conductor weights are smaller with direct current due to fewer wires, and to the absence of the power-factor problem; the power factor of an induction motor when starting is notoriously low.

Mr. Clardy has mentioned high efficiency of equipment. Our studies, so far, indicate that generators, conductors, motors, etc., should have just about as low efficiencies as possible without burning up. The reason is, of course, the weight saving possible by "forcing" the equipment far beyond its maximum efficiency. Weight, in general, appears to be more important in determining airplane performance than power input.

It is the intent of my remarks to promote further discussion and thought on this matter, which is by no means settled. The Navy will benefit as much as anybody from the final solution to the aircraft auxiliary-power problem, and will appreciate any other remarks.

W. J. Clardy: Aircraft auxiliary-engine-driven power plants appear essential for ground or sea-level service in order to avoid main-engine operation for furnishing auxiliary power. Increase in the use of such units will, naturally, lead to improvements to give better weight efficiency. The accessibility, dependability, and small size of the auxiliary engine makes it relatively simple to inspect and maintain. Growth in the use of electric power on airplanes has stepped-up generator sizes. Gear boxes and drive shafts are needed for main-engine drives and the mechanical problems become acute in the case of large units. A main-engine-driven generator, drive shaft, and gear box should be just as good a target as an auxiliary-engine-driven generator—perhaps better since fire may be concentrated on main engines.

The 110-volt 400-cycle three-phase a-c system has been developed to meet the demand for more power—10, 12.5, and 20 kva in single power plants. Alternators are easily paralleled by merely bringing a second machine up to speed and closing the circuit breaker to connect it to the line. Batteries and battery chargers are used with the a-c system but it seems desirable to keep such heavy power sources to a minimum in the interest of weight economy. The proper balancing of single-phase loads can be worked out in advance of any emergency. It is entirely practicable to build light-weight radio equipment for the 110-volt 400-cycle three-phase system and voltage regulation is extremely accurate.

Light-weight high-speed induction motors are well adapted for 400-cycle three-phase operation. High starting torques are obtainable and alternators are designed for high current overloads at lowered power factors to avoid impairment of voltage regulation. Two-pole induction motors run at 24,000 rpm synchronous speed. Lower speeds are secured by increasing the number of poles. In a weight analysis it is necessary to include gearing required for a motor but even on this basis high speed is advantageous. This seems to be borne out by the universal trend to higher speeds. A lubrication system has been evolved for 24,000-rpm motors which appears to be satisfactory under all service temperature conditions.

An airplane with an extensive motor load requires high voltage to avoid exces-

Some Impulse-Voltage Breakdown Tests on Oil-Treated Paper-Insulated Cables

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Synopsis: Data are presented on impulse-voltage breakdown values obtained on oil-treated paper-insulated cables over the insulation thickness range from 0.187 inch to 0.600 inch. The average stress at breakdown approximates 1,600–1,700 volts per mil. High-density papers show some superiority over ordinary-density papers. Cathode-ray oscillograms of the testing wave ($1^1/_2$x40 impulse wave—positive polarity) before and at breakdown are included for a typical test.

INFORMATION on the impulse strength of high-voltage apparatus has accumulated steadily in the last 20 years due particularly to advances in technique of generation and measurement of high-voltage surges. Line insulators, bushings, transformers, and other apparatus have been tested. Cables have received attention during the last decade of this period, and the literature is beginning to record this information.[1,2]

This paper presents data on the impulse breakdown voltage of oil-treated paper-insulated cables over the range of insulation thickness from 0.187 inch (0.472 centimeter) to 0.600 inch (1.520 centimeters). The impulse breakdown voltage at the latter thickness was of the order of 1,000,000 volts. Cable breakdown tests are necessarily destructive of the sample and involve many samples and a costly preparation procedure. Data thus accumulate more slowly than in cases involving air flashover. These data have been accumulated over the period 1931 to 1938. The testing technique has differed somewhat during this period; early tests employed a 1x10 wave (that is, one microsecond to crest voltage, ten microseconds to decrease to one-half crest voltage); later tests employed a $1^1/_2$x40 wave. In all cases a positive impulse was applied to the cable conductor, the lead sheath being connected to ground.

The tests on the thinner (0.187 inch) insulation were made on samples having ten feet of lead. The ends were built up with a varnish-cloth reinforcement over which was placed a spun-copper stress-distributing bell. This latter was connected to the sheath by a closely wrapped spiral copper braid on the lower slope of the varnished-cloth reinforcement. The whole cable was then immersed in an oil-filled tank. All other tests were made on 16-foot (lead length) samples using standard porcelain oil-filled terminals of usual design. All cable failures secured were in the leaded portion of the cable.

Impulse Generator

The impulse generator used for these tests has a 2,000-kv open-circuit rating and is of the parallel-charging, series-discharging type. The d-c charging voltage is 200 kv for each of the ten parallel capacitor banks. The generator has a series capacitance of 0.0067 microfarad and a circuit inductance of 75 microhenries.

The circuit arrangement used is shown in figure 1. The wave-front time is determined by the resistance-capacitance combination R_1C_2 and the wave tail by the resistance R_2 and the combined capacitances C_1 and C_2. The determination of these values is done experimentally and without delay on the oscillograph electric-transient analyzer.[3,5] All oscillograph records were taken with the type *HC*-15[4,5] hot-cathode high-vacuum high-voltage cathode-ray oscillograph.

Table I. Specimen Sphere Gap–Cathode-Ray Oscillograph Calibration Check

No.	Surge-Generator Excitation, Volts	Sphere Gap (100 Cm) Setting (Cm)	Sphere Gap Voltage (Kv)	Sphere Gap Corrected Kv	Cathode-Ray Oscillograph Millimeters	Cathode-Ray Oscillograph Volts	Cathode-Ray Oscillograph Ratio	Cathode-Ray Oscillograph Kv	Deviation (Per Cent)
10	40	3.59	100	105	22.2	913	113	103	−1.3
11	40	3.59	100	105	22.2	913	113	103	−1.3
12	40	3.59	100	105	15.5	637	162	103	−1.6
13	40	3.59	100	105	15.5	637	162	103	−1.6
14	78	7.62	200	210	31.0	1,274	162	207	−1.5
15	78	7.62	200	210	30.8	1,266	162	205	−2.1
16	78	7.62	200	210	21.8	897	228	204	−2.8
17	78	7.62	200	210	22.1	909	228	207	−1.5
18	119	12.53	300	315	34.5	1,418	228	323	+2.4
19	119	12.53	300	315	34.2	1,406	228	320	+1.6
20	119	12.53	300	315	24.5	1,007	328	330	+4.6
21	119	12.53	300	315	23.2	995	328	326	+3.3

Pressure, 77.37 centimeters mercury; temperature, 14 degrees centigrade; relative air density, 1.056; gap correction, 1.052.

Demonstration of Measurement Accuracy

Standard procedure used in these tests for calibration of the surge generator is given in the recently revised AIEE Standard No. 4, "Measurement of Test Voltage in Dielectric Tests". This calibration recommends an intercomparison between sphere gap and cathode-ray oscillograph for demonstration of measurement accuracy. These intercomparisons were made at frequent times during this cable-testing program. Record of one such comparison is reproduced in table I. The deviation between sphere gap and oscillograph

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C. M. Foust and J. A. Scott are in the general engineering laboratory, General Electric Company, Schenectady, N. Y.

Acknowledgment is made for the many details of testing, preparation of cable samples, and for the specialized impulse-testing technique to J. B. Feiter, C. Zubal, and N. Rohats.

1. For all numbered references, see list at end of paper.

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sive weight in the distribution lines. This seems to eliminate the 24-volt d-c system when a large number of motors is used. At 110 volts, the 400-cycle three-phase a-c system offers some attractive weight-economy possibilities because of high-speed induction motors. In considering auxiliary-power-plant efficiency on airplanes it is obvious that "weight efficiency" is the determining factor. In the analysis of an auxiliary power system there is no question about its importance. "Weight efficiency" requires consideration of the fuel, oil, engines, generators, control, distribution system, motors, lights, radio, and any other loads. The efficiency of a specific part of the system must be selected to attain the lowest total weight.

Table II.

Specimen Breakdown Test on High-Voltage Cable

Thickness 315 Mils, 1$^1/_2$x40 Wave

		Cathode-Ray Oscillograph				
		Deflection				
Number	Surge Generator (Volts)	Mm	Volts	Divider Ratio	Kv	Time to Breakdown (μ Sec)
1	56.0	10.4	427	773	329	
2	60.5	11.4	468	773	362	
3	64.5	12.3	506	773	391	
4	68.0	13.0	537	773	415	
5	72.0	14.0	576	773	445	
6	75.0	14.7	605	773	467	
7	78.0	15.5	635	773	490	2.6

Time-Scale Calibration

Number	Frequency (Cycles Per Sec)	Time Scale (μ Sec)
8	500,000	5.9 average

Deflection Calibration

		Deflection	
Number	Applied Voltage	Mm	Volts Per Mm
9	250	6.3	39.6
	500	12.0	42.5
	750	18.3	41.1
			41.1 average

Table III. Impulse Breakdown Values for 33 Cables Having Oil-Treated Paper Insulation Ranging From 0.187 Inch to 0.600 Inch Thick, Tested at 25 to 30 Degrees Centigrade

Cable Number	Conductor Size (Stranded)	Thickness of Insulation (Inches)	Paper Density	Oil Viscosity at 100 Deg F (Saybolt Sec)	Impulse Breakdown (Kv Crest)‡
1	00 Brown and Sharpe*	0.187	0.8	2,500	350
2					350
3					350
4					350
5					350
6					375
7					400
8	2,100,000 circular mils**	0.315	1.0–1.1	100	610
9					512
10		0.315	0.8	100	448
11					453
12					461
13		0.315	40 per cent 1.0–1.1, 60 per cent 0.8†	100	500
14					490
15		0.315	Medium density	100	555
16					550
17					559
18		0.500	1.0–1.1	100	730
19					765
20		0.500	0.8	100	630
21					648
22		0.500	40 per cent 1.0–1.1, 60 per cent 0.8†	100	902
23					928
24					940
25	500,000 circular mils§	0.600	40 per cent medium, 60 per cent 0.8†	100	1,067
26					1,104
27					1,115
28					856
29					916
30					836
31		0.600	Medium density	100	896
32					940
33					1,014

* Stranded; 0.420 inch outside diameter. Solid-type cable.
** Hollow core; 1.890 inch outside diameter. Oil-filled cable.
§ Hollow core; 1.03 inch outside diameter. Oil-filled cable.
† Graded insulation with higher-density paper next to the conductor.
‡ Wave shape of first seven tests was 1x10, of all others 1$^1/_2$x40 as defined by (microseconds to crest)–(microseconds to half value).

is below five per cent for this record and for all comparisons made during these tests, satisfactory accuracy for such measurements.

Testing Procedure and Calibrations

The routine followed in applying the impulses was as follows. The generator having been adjusted to give the desired

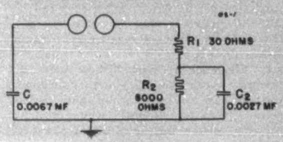

Figure 1. Impulse-generator circuit schematic diagram

Constants shown are for the cables of group C of figure 3

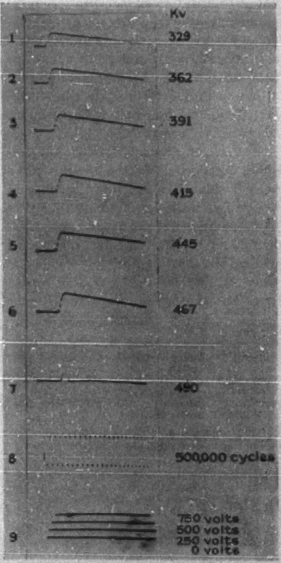

Figure 2. Specimen oscillograph record of impulse-breakdown test on cable sample with calibrating records

1–6—Successively applied impulse-voltage waves of increasing crest value
7—Impulse wave producing cable failure
8—Oscillator wave for time-axis calibration
9—Direct-voltage records for deflection calibration

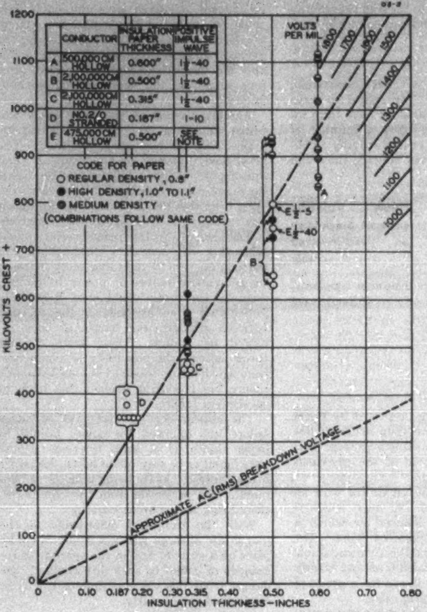

Figure 3. Summary of impulse tests on cable insulation

wave shape and its excitation calibrated in terms of crest kilovolts, an impulse was applied to the cable of a magnitude well below (approximately 50 per cent) expected breakdown. Successive single waves were then applied, each exceeding the previous wave by an approximately constant increment until a cable-insulation breakdown was obtained. Failure was indicated in the tests made in 1931 by the action of a surge-crest ammeter[6] inserted in the ground lead from the cable sheath; in all tests made since that time a cathode-ray oscillograph[4] was employed using a resistance voltage divider, and an oscillogram was obtained of each wave up to and including the one causing failure. The magnitude of the crest voltage was obtained from the cathode-ray oscillograph deflection and voltage-divider ratio as indicated in table I. Practically all failures occurred on or near the crest of the wave.

Specimen Record

A specimen oscillograph record is reproduced in figure 2 wherein are recorded the six full ($1^1/_2$x40) waves at increasing voltage amplitudes followed by the wave at 490 kilovolts crest which caused cable failure. The eighth record is that of a 500,000-cycle timing wave and the ninth a deflection calibration. The data pertaining to all these are given in table II. The recorded waves were analyzed to be 1.55 microseconds to crest and 39.3 microseconds to half value on the wave tail.

Results

Figure 3 shows the results obtained in these tests. Plotted here is kilovolts crest against insulation thickness. Table III records the numerical values. No attempt has been made to draw a curve through the points. In fact, the dispersion is such that a straight line fits the data as well as does a curve. A polar-co-ordinate system has been superimposed from which the average stresses (volts per mil) may be read. An average figure of 1,600 volts per mil may be carried in mind as representative.

It is not attempted to take account of the stress variation between conductor and sheath, although conductors from number 2 Brown and Sharpe to 2,100,000-circular-mil hollow with outside diameters of 0.420 inch and 1.890 inch respectively were used. The tests included several grades or densities of papers, and in general the denser papers gave higher breakdown values. In the presence of the natural variation or dispersion of the results, including the fact that many samples included graded insulations, an accurate statement cannot be made comparing the different densities. We may perhaps infer that the dense papers yield results 10–15 per cent better than the lower-density papers.

The results reported in this paper are in excellent agreement with those reported by Held and Leichsenring,[2] who tested over nearly the same range of insulation thickness.

All the tests here reported were made with positive polarity and with waves of 1 to $1^1/_2$ microseconds wave front. However, as far as analysis of breakdown values reported herein are concerned, the wave-shape change is not important. The wave fronts were practically the same, and failures occurred most frequently on or near the wave crest. A 1x10 wave was used on the earlier tests as a compromise between the 1x5 and $1^1/_2$x40 waves then most common. Later, as the 1x5 wave diminished in use, all tests were made with the $1^1/_2$x40 wave. These waves are those in most common use in this country for impulse testing.

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Discussion

Herman Halperin: See discussion, page 399.

P. L. Bellaschi and W. L. Teague: See discussion, page 400.

L. G. Smith: See discussion, page 401.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): Several years ago tests were made in the high-voltage engineering laboratory in Pittsfield to determine the volt-time characteristic of a cable. Although in general high-voltage cables probably are not subjected to steep-fronted lightning waves, it might be of interest to publish the result.

The cable sections tested were prepared similarly to the samples described in the paper with the exception that the leaded portion was only ten inches long as compared to ten feet in the paper. The total length of a sample was 4½ feet. The whole section was immersed in oil. All failures occurred in the leaded section.

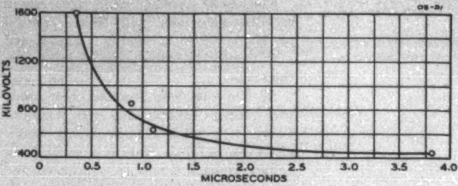

Figure 1. Volt-time curve of breakdown on lead-sheathed cable

Negative polarity, 1.5x40 wave

The cable had a conductor of 500,000 circular mils, 0.281-inch paper insulation, oil treated, and a total outside diameter of 1.6 inches. Tests were made with negative polarity.

For determining the minimum breakdown strength, tests were started at a voltage of approximately 60 per cent of the expected failure voltage. Voltage was raised in about 25-kv steps until failure occurred at 440 kv. Two impulses were applied at each step.

The breakdown voltage thus obtained is probably somewhat too high for design purposes, because it does not take into account the effect of successive strokes on the breakdown. In transformer insulation the damaging corona level is reduced to 80 per cent of the single-shot corona level, when 15 or more impulses are applied. In a cable with its more uniform field and uniform insulation, the multishot breakdown probably is not quite as much lower as in transformer insulation but nevertheless their effect must be taken into account to arrive at the proper safety factors.

The other three sections were tested by applying various amounts of overvoltage to the cable and recording the resulting failure by means of a cathode-ray oscillograph. For the purposes of the investigation described, the few breakdown points obtained showed results which gave a sufficiently consistent curve as shown in figure 1 of this discussion. However, a greater number of points and different thicknesses of insulation would have to be investigated for design data.

Of interest is a comparison of the overvoltage characteristics of various electrode shapes in comparison with the cable. This is shown on figure 2 of this discussion for a 20-inch rod gap, a 10-centimeter sphere gap at 20 centimeters spacing and the cable.

It is interesting to note that the cable in spite of the relatively uniform field has a much steeper characteristic than the sphere and even approaches the characteristic spark-over curve of the 20-inch gap at 0.5 microsecond. The reason for this behavior may be imperfections in the lead sheath producing point effects and corona on the positive sheath.

R. W. Atkinson (General Cable Corporation, Perth Amboy, N. J.): In regard to surge strength of paper in cable we have been for some time depending upon a number of tests made in 1931. It is gratifying to observe the close agreement between these and the more complete series reported by Foust and Scott. Besides our data in direct line with Scott's we are reporting here some tests tending to answer some of the questions which have been asked.

Our tests were made on cables with an insulation thickness of 190 mils, which is identical with the thickness on which a number of tests were made by Scott. The average value of 12 tests directly comparable with his was 348 kv, which is almost exactly the same as the value shown for a group of his tests on corresponding cable.

The tests included in the above group were all made with surges of positive polarity. A few tests were with surges of negative polarity. These required slightly higher voltage to produce breakdown than required for breakdown of similar cable tested with positive polarity.

Half of the samples were made with paper of high density and half of lower density. The average strength of those with high-density paper was 18 per cent greater than for those of low density. It is noted that this is somewhat greater than found by Scott but reasonably comparable with his figures.

Two longer lengths were subjected to 83 and 115 repeated surges respectively. In the first surges, the applied surge voltage was about 60 per cent of the expected surge breakdown. Ten shots were applied after which the applied voltage was increased by about 6 per cent of the expected breakdown. A third set of ten shots at 6 per cent higher voltage was then applied, after which successive sets of ten shots each were applied with voltage increments of 3 per cent until failure occurred.

While the voltage of the ten successive shots at one setting was substantially the same, this repetition would not be exact. Thus the fact of failure at other than the first shot at one voltage may be due either to the cumulative effect of successive shots at substantially the final breakdown value or may be due to a slightly greater voltage at the surge producing failure than on the preceding surges at the same nominal voltage.

On one of the lengths, 35 of the shots were at a value within 10 per cent of the average surge breakdown of similar cable and the breakdown occurred after the cable had stood two surges at the breakdown setting. On the other, 33 of the shots were above the average surge breakdown of similar cable and the surge breakdown occurred after four surges at the same setting without failure.

Another portion of the same cable was tested similarly at 60 cycles but without the application of the surge voltage. The breakdown voltage on the "surged" and "unsurged" samples fell within the same range and thus there was no evidence from these tests that the 100 surges had produced any reduction in strength of the insulation.

The tests of this last named series appear reassuring with respect to the possibility of partial puncture by lightning being followed by subsequent failure at power frequency. Actually the result could have been fully anticipated from analysis of the relation between strength and length of time required for failure. These tests do not throw light on the possibility of a puncture from lightning not followed at once by power failure, but producing damage that leads to subsequent failure.

The appearance on dissection of surge failure not followed by power failure in paper cables, is of some interest. These were found to be tiny radial holes, the paper being "burred," as by being punctured by a minute fast projectile from the conductor, there being no apparent burning.

With the increasing knowledge of the magnitude of surge voltages which can take place on a system, and of the amount of resistance of cables to such disturbances, the danger from this cause of failure has been diminished very greatly.

L. I. Komives (nonmember; The Detroit Edison Company, Detroit, Mich.): First of all the authors of this paper are to be congratulated for the valuable data which they collected in a field largely unexplored.

1. I am rather puzzled with the results presented in this paper because the voltage

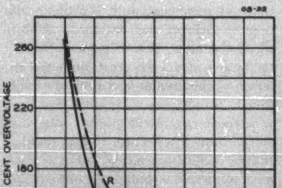

Figure 2. Negative-voltage characteristics

C—Cable, 0.281-inch oil-treated paper (440 kv critical)

R—Rod gap (410 kv critical)

S—Sphere gap (450 kv critical)

100 per cent equals critical breakdown voltage

gradient seems to have had no effect whatsoever on the impulse strength of the cables tested by the authors. In our rather incomplete experiments we found that the voltage gradient has a very strong influence on the impulse strength.

2. We have also found that uniformity of the taping has a very decided influence on the impulse strength. No mention whatsoever was made in the paper in this respect.

3. I feel that the results shown on small-size copper conductors (samples 1 to 7—table III) are due to a highly viscous oil used in these cables in contrast with the much lower viscosity oils of the rest of the samples. I would like to know whether the authors concur with this explanation.

4. We feel that it is perhaps of more importance to cable users to know just what the impulse strength of a piece of cable is when tested with the maximum potential available. Although the general opinion is that potentials lower than those causing breakdown of the insulation have practically no effect on the insulation because of the short time involved, we have reasons to believe that every application weakens the insulation and therefore, testing cables with consecutively high potentials does not indicate the true impulse strength of the insulation.

5. I would like to know what the "medium density" paper mentioned in table III of the paper is.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): The following questions occurred to me in reading the paper:

1. Would any materially different values of volts per mil have been obtained if actual rather than nominal insulation thicknesses had been used?

2. Were any of the cables shielded with copper shielding tapes? Would the use of such tapes, by holding the paper tapes firmly together, increase the impulse strength?

3. With unexplained variation of impulse strength from 1,300 to 1,900 volts per mil on one wall thickness, is it safe to base insulation walls on impulse strengths higher than 1,300 volts per mil?

4. Figure 1 shows a resistor R_1. Is there any reason for preferring this to a reactor?

C. M. Foust and **J. A. Scott:** In J. H. Hagenguth's discussion he has added three front-of-wave short-time breakdown values and a minimum breakdown value on 0.281-inch paper to our test data. The minimum breakdown value is a good check on our values for this thickness, being almost exactly on our average curve. The three short-time values permit an approximation of the volt-time curve from actual test data on cable samples down to 0.4 microsecond. The comparison of rod gap, cable, and sphere gap volt-time curves wherein the cable curve conforms more nearly to the 20-inch rod-gap curve than to the spheres suggests a long breakdown path, possibly in many parts parallel to and between paper layers.

The authors appreciate the discussion contributed by Messrs. Bellaschi and Teague, pertaining to volt-time breakdown relations for short time periods. Other than that the point of breakdown on the wave shape was recorded by cathode-ray oscillograph, no effort was made in our work to gather data on volt-time relations. As pointed out in the paper, all failures occurred at the crest or a short time after, indicating very little increase in voltage down to 1.5 microseconds for wave-crest failures. This, the authors presume, is the basis for the statement by Messrs. Bellaschi and Teague that the volt-time characteristic is essentially constant down to one or two microseconds. However, Mr. Hagenguth's data which was taken on the wave front indicates a 20-per cent increase in voltage at 1.5 microseconds. This is likely due to the fact of the wave-front failures being higher than wave-crest values at the same time point.

In commenting on L. G. Smith's discussion, he is correct in presuming that the values are for new cable. We agree that similar data on cable having been in service for some time are desirable. It seems logical to expect that there should be no change in impulse strength with age for oil-filled cable, while solid-type cable might show a decrease due to void formation. The point of variation of breakdown strength with conductor size has been twice referred to in the discussion with the inference that our tests showed no such variation. We did not wish to convey this impression. To determine such a relation accurately would require a large number of tests with different sizes of conductors and a range of insulation thicknesses for each. Such a test program on high-voltage cable samples is quite formidable. We wish to say that because of the dispersion of our test points analysis of the data did not provide us with a sufficiently definite relation between conductor size and breakdown to warrant comment.

Mr. Atkinson has referred to a considerable group of data taken by his company. He had very kindly made these results available to us early in our investigation. To show the good agreement between these results as well as those reported by Held and Leichsenring (reference 2 of the paper), figure 3 of this discussion consolidates these together with some other data identified in the figure. It is here seen that all data from some six sources are in excellent agreement. An excellent idea of the spread of the values is given. This spread is very reasonable for such breakdown values.

Mr. Atkinson also refers to the lack of damage done by impulses of value somewhat below the breakdown value. This freedom from so-called "impulse fatigue" is verified in our own experience. For example, in our tests on the samples having 0.187-inch paper, two additional samples were subjected, one to 100 impulses at 325 kv, the other to 500 impulses at 300 kv; following this single impulses at 25-kv increments were applied. Both cables failed at 375 kv which compares with the highest results obtained on samples 1 to 7 reported in the paper. However, an open mind should be kept on this subject, as indeed on all aspects of this relatively young subject, for we have seen unpublished data from abroad where on relatively thin samples several thousand successive shots did produce a decrease in the breakdown value.

In reply to Mr. Komives in the order of his questions:

1. We would agree that voltage gradient is probably the controlling factor in the breakdown

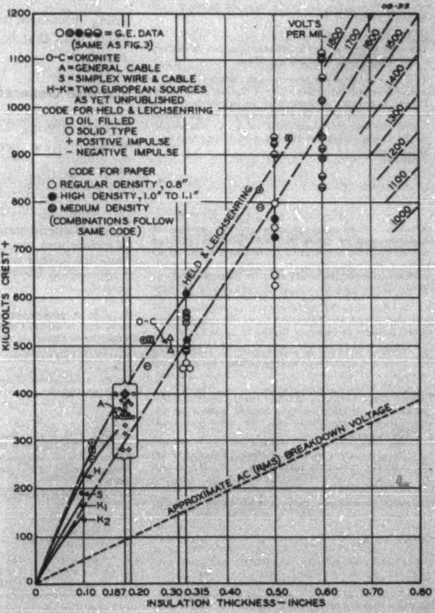

Figure 3. Summary of impulse tests on cable insulation

Impulse Strength of Cable Insulation

E. W. DAVIS
FELLOW AIEE

W. N. EDDY
FELLOW AIEE

INSULATED CABLES often are exposed to lightning and often have been damaged by lightning, especially rubber- or cambric-insulated cables of medium or lower voltage. It is also true that some service trouble has been improperly attributed to lightning. Knowledge of the impulse strength of cable insulation is necessary to analyze such cases correctly and to minimize lightning trouble by proper cable design. In spite of the practical need for such information there seems to be a conspicuous lack of it in the literature.

From time to time for several years we have been making impulse tests on cable insulation, either as we needed the data or as we got the opportunity. As a result we have accumulated a general assortment of data without conducting a complete or continuous investigation. We have found these data worth while and hope eventually to make the data complete enough to merit a more detailed analysis than seems justifiable at present. In the meantime we are reporting the results to date with the hope that the ensuing discussion will uncover results of other tests that have been made but not reported.

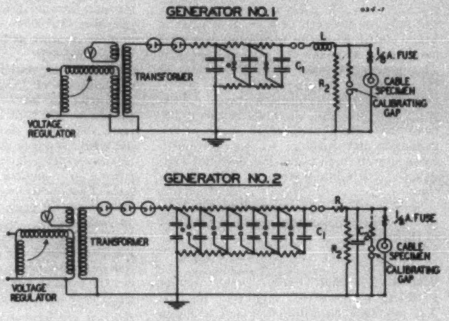

Figure 1. Connections

Impulse Generators

Each testing impulse consists of a nonoscillatory increase and decrease of voltage at a definite rate. Two different impulses have been used for these tests, a 1x10 wave and a 1.5x40 wave. The first figure in each designation is the time in microseconds from the beginning of the impulse to the crest of the impulse. The second figure is the time in microseconds from the beginning to the point on the impulse tail at which the voltage has fallen to one-half the crest or maximum value.

The impulses are generated by discharging a capacitance through a resistance. The resulting voltage drop across the resistance is the impulse. The shape of the impulse is controlled by the values of the constants in the discharge circuit; the impulse front by inductance in series or capacitance in parallel with the resistance and the impulse tail by the resistance itself. The assembly necessary to produce these impulses constitutes the impulse generator.

Figure 1 shows the connections of the two generators that were used for the tests. In both, the necessary high discharge voltage is attained by the familiar Marx circuit which has been fully described in the literature. The differences between the two generators are evident from the connections in figure 1 and the ratings and circuit constants in table I.

Generator 1 was made several years ago, using available glass plates for the main capacitance. The constants in the discharge circuit were adjusted for a 1x10-microsecond wave because this was the intermediate of the three waves then in common use for testing lightning arresters.

Generator 2 was built more recently, in order to enlarge the range of sample length and size that could be tested. The discharge circuit was adjusted to a 1.5x40-microsecond wave because this

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E. W. Davis is chief electrical engineer and W. N. Eddy is experimental engineer, Simplex Wire and Cable Company, Cambridge, Mass.

The data in the paper represent the efforts of several people at irregular intervals over the last four years. In particular the authors wish to acknowledge the valuable advice of C. M. Foust in building generator 2 and the conscientious work of S. N. Alexander, W. D. Fenn, and Oscar Hess in calculating and testing.

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strength. It is, however, only one of a number of variables and we chose the more practical minded way of plotting breakdown against insulation thickness. Conductor sizes are given so that approximate maximum gradients may be calculated if desired.

2. We felt that the uniformity of taping was excellent and found no indication from the examination of the failures to indicate taping to be at fault.

3. We see nothing in our results to lead us to attribute different performance to the two different viscosities of oil used. As a first approximation we would assume them to be identical. Samples 1 to 7 of table III have thinner insulation than the remainder and consequently benefit somewhat by any curvature that may exist in the breakdown voltage-thickness curve.

4. The alternative method of testing referred to by Mr. Komives is highly interesting. We proceeded the way we did because we were on untried ground and we preferred to feel our way along as it were. Our results we feel are more indicative of the lowest breakdowns that might be obtained and consequently more useful in estimating safely sustained voltage values. However, the procedure suggested has its merits and will, we hope, be used. We have already referred to "impulse fatigue" in connection with Mr. Atkinson's discussion.

5. The medium-density paper was a special paper having a density of 0.85 or lower.

Mr. Halperin emphasizes nicely a number of points and gives some valuable suggestions for further work.

Answering Mr. Del Mar in the order of his questions:

1. The difference between actual insulation thickness and nominal thickness was small compared with the variation in breakdown-voltage results.

2. No shielding tapes were used. Since all tests were made on single-conductor cables, which were newly manufactured and whose sheaths had not been expanded by heating and cooling cycles, our results should be the same as if shielding tapes were used.

3. Mr. Del Mar's third question covers a lot of ground. To answer it briefly our consolidated data between 0.315-inch thickness and 0.600-inch thickness regardless of the particular construction of the cables (paper, etc.), indicates the probability that five per cent of such a group of cables would fail at 1,300 volts per mil or less. (Average breakdown, 26 tests = 1,615 volts per mil; $\sigma = 191$ volts per mil or 11.8 per cent of average.) Such variation is typical of breakdown data.

4. Other circuits, of course, than that shown in figure 1 may be used. We preferred a resistor, R_1, to a reactor because of the lesser liability to oscillations which frequently occur when lumped reactors and capacitors are used.

shape had become generally accepted as the standard test wave. The ballast capacitance in parallel with the sample has two functions; to help control the wave shape and to decrease the effect of changing sample capacitance on the wave shape.

Our principal purpose in making these tests was to compare different sizes and types of cable on an impulse reasonably representative of lightning, without attempting to get co-ordination with other impulse data in the literature. Therefore, we have been more interested in keeping our wave shape constant or fixed than in making sure it was exactly 1x10 or 1.5x40. For this reason we have not yet considered it necessary to acquire a cathode-ray oscillograph and our wave shapes therefore have not been confirmed by oscillograph. The test results in table II indicate that the value of our results is not seriously reduced by this omission.

Each test is made by increasing the charging voltage in seven-kv steps usually beginning at 50 per cent of the estimated breakdown voltage. Each voltage is held constant while two successive impulses are applied to the sample by lowering a third sphere into the first-stage gap. Failure of the sample is indicated by explosion of the one-eighth-ampere fuse wire in series with the sample. The test voltage is indicated by a voltmeter coil in the charging transformer calibrated in terms of a sphere gap in parallel with the test sample. Both ends of the sample are connected together. Various arrangements of the sample were tried without finding any effect on the impulse strength.

Table I. Ratings of Impulse Generators

	Generator 1	Generator 2
Maximum discharge voltage at terminals of C_1 (kv)	300	600
Maximum voltage on specimen (kv)	260	550
Main capacitance C_1		
(μf per 50 kv)	0.04	0.5
(μf at full voltage)	0.0067	0.042
Ballast capacitance C_2 (μf)	None	0.0048
Discharge resistance R_1 (ohms)	None	115
R_2 (ohms)	2,300	1,100
Calculated wave shape	1x10	1.5x40

Table II. Effect of Wave Shape on Impulse Strength (Volts Per Mil)

	Generator 1—1x10 Wave			Generator 2—1.5x40 Wave		
	Average	Maximum	Minimum	Average	Maximum	Minimum
Cambric	1,050	1,120	1,000	1,090	1,150	1,050
Rubber	1,080	1,300	900	1,070	1,240	915

Table III. Effect of Impulse Polarity on Impulse Strength of Rubber-Insulated Wire

Wire Size	Insulation Wall	Impulse Polarity	Impulse Strength in Volts Per Mil			Ratio, Positive/Negative (Average)
			Average	Maximum	Minimum	
No. 6 solid	$^{6}/_{64}$-inch	Positive	1,033	1,150	900	0.84
		Negative	1,225	1,300	1,200	
No. 6 solid	$^{10}/_{64}$-inch	Positive	1,400	1,550	1,250	1.09
		Negative	1,290	1,450	1,150	
No. 6 solid	$^{14}/_{64}$-inch	Positive	928	1,050	780	1.02
		Negative	905	1,000	850	

Figure 2 shows the general arrangement of generator 2. At the left is the portable control table connected to the generator through two multiconductor portable cables. The voltage regulator controls the input to the transformer in the corner which through the tube rectifiers charges the capacitors on the wooden rack. The stage resistors consist of water in the garden hose visible along the upper edge of the rack. The third or trigger sphere is visible just above the first stage gap. A sample of plain rubber wire is shown ready for test in water. The series fuse wire is on the framework above the water drum. The motor on the floor is circulating a sodium carbonate solution through the vertical hose for R_1 and R_2. The capacitor bank at the right is the ballast capacitance or C_2.

Effect of Wave Shape on Impulse Strength

These tests were made on five-foot adjacent samples taken from two lengths of single-conductor cable, number 6 solid, $^{6}/_{64}$-inch wall of insulation, lead sheath, one length insulated with rubber and one with varnished cambric. In table II are the results in volts per mil wall, the average, maximum, and minimum of five tests. Samples from both lengths were tested with each of two different impulse wave shapes, 1x10 and 1.5x40.

The results show no definite difference in strength between the two wave shapes in spite of the considerable difference in shape. One wave is 50 per cent slower than the other on the front and four times longer than the other on the tail. This lack of a real difference in strength between the two waves is good evidence that a reasonable deviation in our wave shape from the calculated should have no appreciable influence on the test results, and therefore that the lack of an oscillograph should not seriously limit the value of the test results.

This lack of difference also indicates that any conclusion based on either shape should hold for the other shape and for a fairly large range of impulses occurring in actual service.

Effect of Impulse Polarity on Impulse Strength

Table III shows the effect of impulse polarity on the impulse strength of three

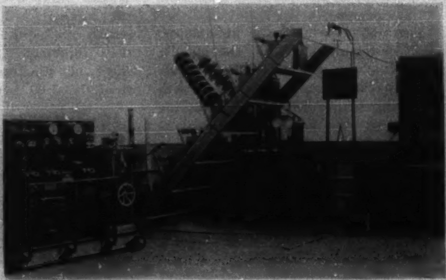

Figure 2. Generator 2

Table IV. Effect of Electrode Surface on Impulse Strength (Volts Per Mil)

	Average	Maximum	Minimum
Lead sheath	1,400	1,490	1,300
Rubber surface in 0.1 per cent salt water	1,390	1,440	1,300
Lead foil wrapped around rubber insulation	1,410	1,480	1,340

different sizes of rubber-insulated wire. The tests were made on five-foot samples with generator 1 and the 1x10 wave.

On the average no real difference in strength is indicated between the two polarities. Unless specified otherwise all other tests reported in this paper were made with the negative polarity.

Impulse Strength of Rubber-Insulated Wire—Effect of Electrode Surface

Table IV gives the results of tests made on five-foot samples of number 6 solid, $^6/_{64}$-inch wall of rubber, using three different kinds of outer electrode surface. All tests were made on generator 2 with the 1.5x40 wave. The results are in volts per mil wall, average, maximum, and minimum of at least five tests.

These results show that any one of these types of outer electrode can be used indiscriminately. In most cases the salt water is most convenient for testing rubber-insulated wire.

Impulse Strength of Rubber-Insulated Wire—Effect of Specimen Length

All samples used for these tests were taken from the same length of wire, single conductor number 6 solid, $^6/_{64}$-inch wall of 60 per cent high-voltage rubber compound, tape, and lead sheath. Generator 2 was used with the 1.5x40 wave. Table V gives the test results in volts per mil of wall.

The results in table V indicate no effect of sample length on the impulse strength of the wire, while the 60-cycle strength of the same wire decreases with increasing sample length at about the usual rate. We have seen this unexpected tendency of the impulse strength in other of our test results but plan to investigate it further before accepting it as conclusive. There has been no tendency for the faults in the longer samples to be located near the sample ends. Different arrangements of the longer samples have been tried without finding any consistent effect. Without a cathode-ray oscillograph we have been unable to determine the effect of the sample capacitance on the wave shape, but in view of the results in table II we doubt that this effect is sufficient to affect the impulse strength.

Table V. Impulse Strength of Rubber-Insulated Wire—Effect of Specimen Length

Specimen		Impulse Strength (Volts Per Mil)			60-Cycle Strength (Volts Per Mil)*			Ratio, Impulse to 60 Cycle (Average)
Length (Feet)	Capacitance (μf)	Average	Maximum	Minimum	Average	Maximum	Minimum	
1	0.00006	1,050	1,230	900	580	600	510	1.8
2	0.00012	1,120	1,260	960	550	590	500	2.0
5	0.0003	1,117	1,240	920	496	550	420	2.2
10	0.0006	1,231	1,380	1,020	470	520	430	2.6
20	0.0012	1,130	1,340	920	440	490	400	2.7
40	0.0024	1,128	1,330	925	415	510	380	2.7

* All 60-cycle strengths given in this or other tables are effective and not peak.

The ratios of impulse to 60-cycle strength in table V are seen to be considerably lower than those in table VII because the rather high 60-cycle strength of the table V wire is not accompanied by abnormally high impulse strength. That is, the impulse strength does not seem to have any fixed relation with the 60-cycle strength. On five-foot samples the ratio may vary from two to four.

Impulse Strength of Rubber-Insulated Wire—Effect of Insulation Thickness

These tests were made on five-foot samples of rubber-insulated wire in salt water, using generator 1 with the 1x10 wave. Five samples were tested at each of seven different wall thicknesses between $^5/_{64}$ and $^{22}/_{64}$ inches, on a number 6 conductor or thereabouts. All insulating walls were 60 per cent rubber but of slightly different types. For this reason the actual results gave a rather irregular plot versus wall thickness. The results given in table VI represent a smooth curve averaging the actual results. Therefore, although the reported results are subject to some variation, they represent the trend or general effect of thickness on the impulse strength of 60 per cent rubber insulation on a number 6 conductor.

Table VI. Effect of Wall Thickness on Impulse Strength of Rubber-Insulated Wire

Insulation Wall in 64ths Inch	Impulse Strength in Volts Per Mil
5	1,400
10	1,150
15	970
20	800

In table VI these average results of unit impulse strength decrease with increasing wall thickness. The same tendency has been shown by the a-c strength of many insulations.

Impulse Strength of Rubber-Insulated Wire—Effect of Compound Type

It is well known that there are many different types of rubber cable insulation and that they may differ considerably in their properties. Each type is designed and used for a particular kind of service. The results of table VII are given to show the effect of the rubber compound type on the impulse strength and the ratio between that strength and the 60-cycle dielectric strength.

All samples were five-foot active, number 6 or number 8 with $^5/_{64}$-inch or $^6/_{64}$-inch wall, tested in salt water on generator 2 with the 1.5x40 wave. The results are in volts per mil wall.

The results in table VII indicate at least general correlation between the impulse and 60-cycle strengths. Because of variables in each type of compound that are outside of the present discussion, the impulse and 60-cycle strengths of each type are subject to some variation from the values given. But the results in table VII serve to give the general magnitude and to indicate that the impulse strength of rubber compounds may be as high as 1,600 volts per mil and as low as 700 volts per mil for these insulation walls. In general the impulse strength is seen to be about three times the 60-cycle strength.

Impulse Strength of Rubber-Insulated Tree Wire

These tests were made on generator 2 with the 1.5x40 wave, using five-foot samples of number 6 solid, $^6/_{64}$-inch wall of rubber, braid, with and without fiber tape under the braid, dry and after soaking in water for 20 hours. The dry tests were made with lead foil wrapped over the wire and those after soaking were made in water. Each result given in table VIII is the average of at least five tests on adjacent samples. The 60-cycle tests were made by increasing the voltage three kv per 15 seconds.

The impulse strength of this type of wire is particularly important because the wire is directly exposed to lightning and the insulation wall is seldom more than $^{5}/_{64}$ inch. In table VIII the impulse strength is seen to be generally typical for that rubber wall. The differences between the two types of wire are not enough to indicate that the fiber tape is of any real value as insulation, especially when the wire is wet.

Table IX. Impulse Strength of Cable—Rubber Versus Cambric Versus Paper

Insulation	Impulse Strength (Volts Per Mil)			60-Cycle Strength (Volts Per Mil)			Ratio, Impulse to 60 Cycle (Average)
	Average	Maximum	Minimum	Average	Maximum	Minimum	
Anhydrex rubber	1,143	1,300	900	350	400	310	3.3
Cambric	1,040	1,100	1,000	300	360	250	3.5
Paper	1,920	2,000	1,770	540	570	520	3.6

Table X. Impulse Strength of Sheets—Rubber Versus Cambric Versus Paper

Insulation	Impulse Strength (Volts Per Mil)			60-Cycle Strength (Volts Per Mil)			Ratio, Impulse to 60 Cycle (Average)
	Average	Maximum	Minimum	Average	Maximum	Minimum	
Paper—two layers	3,890	4,400	3,550	1,600*	1,900	1,480	2.4
Cambric—two layers	3,630	4,000	3,200	1,400*	1,590	1,220	2.6
Rubber—one layer	1,993	2,500	1,200	750*	960	600	2.7

* One layer.

Relative Impulse Strength of Rubber, Cambric, and Paper-Insulated Cable

All tests were made on the same size cable, number 6, $^{6}/_{64}$-inch wall insulation, lead sheath. The rubber was a typical 60 per cent, the black varnished cambric was typical of the best available four years ago, and the paper was kraft of about 300 seconds Gurley air resistance saturated with a typical oil used for the solid type of paper cable. Generator 1 was used with the 1x10 wave. The results in volts per mil wall are given in table IX.

Table VII. Impulse Strength of Different Rubber Compounds

Compound	Impulse Strength (Volts Per Mil)			60-Cycle Strength (Volts Per Mil)			Ratio, Impulse to 60 Cycle (Average)
	Average	Maximum	Minimum	Average	Maximum	Minimum	
Code	880	920	840	315	320	310	2.8
AO	870	990	690	255	260	250	3.4
Ozone resistant	1,090	1,290	920	359	400	290	3.0
Performite	740	830	700	275	280	270	2.7
AA-35	840	970	700	347	360	320	2.5
60 per cent S. C.	1,137	1,500	710	375	450	300	3.0
60 per cent H. V.	1,600	1,665	1,535	470	510	430	3.4
Anhydrex	1,340	1,460	1,360	338	365	320	4.0

Table VIII. Impulse Strength of Rubber-Insulated Tree Wire

	Average Impulse Strength				Average Wet 60-Cycle Strength		Wet Ratio, Impulse to 60 Cycle
	Dry		Wet				
	Kv	Volts Per Mil	Kv	Volts Per Mil	Kv	Volts Per Mil	
Fiber tape	107	1,370	89	1,140	24.4	313	3.6
No fiber tape	96	1,230	86	1,100	22.3	286	3.8

The impulse strength of the paper is nearly double that of the rubber and cambric but it is interesting to note that here again as in table VII there is good correlation between impulse and 60-cycle strength. The insulations of high 60-cycle strength show correspondingly high impulse strength.

Although the impulse strength of the rubber in table IX is 40 per cent less than that of the paper, one of the rubber compounds in table VII is only 17 per cent less than the paper in table IX. Since the 60-cycle strength of the best rubber compound in table VII does not represent the highest obtainable with rubber, this suggests that it might be possible to make rubber compounds that are equal to paper in impulse strength. Because of the compounding variables the dielectric strength of rubber is subject to control over a much wider range than that of either cambric or paper.

Impulse Strength of Rubber, Cambric, and Paper Sheets

All tests were made between two-inch-diameter brass electrodes with rounded edges, using generator 1 with the 1x10 wave. The paper was kraft of about 2,000 seconds Gurley air resistance, in layers of 0.005 inch, saturated in paper-cable oil. The varnished cambric was in 0.012-inch layers of the same type as that in table IX. The rubber was 60 per cent in 0.050-inch layers. The test results in volts per mil are given in table X.

As would be expected these results are seen to be considerably higher than those on the insulations in cable form. The strength of the sheet rubber is some 50 per cent higher than that of the thinner rubber cables tested. Similar to the cable insulation the paper sheet strength is about double that of the rubber sheet, but unlike the cable insulation the sheet cambric strength is nearly as high as the paper sheet. On sheets the ratio of the impulse strength to the 60-cycle strength is seen to be about 2.6.

Properties of Impulse Faults in Rubber and Cambric Insulation

The above results indicate that in most cases the impulse strength of rubber is in general as high as that of cambric. This does not agree with our general experience that rubber is somewhat more subject to lightning damage in service than is cambric. For this reason we made a few comparisons of the proper-

Table XI. Properties of Impulse Faults in Rubber and Cambric Insulation

	Rubber Sample	Cambric Sample
Sample 1		
15 kv d-c for 15 min.	OK	OK
165-kv impulse	Failure	Failure
3 kv a-c for $1^{1}/_{2}$ hours	OK	OK
D-c dielectric strength* (kv)	6.2	10.1
A-c dielectric strength (kv)	3.8	5.2
Sample 2		
165-kv impulse	Failure	Failure
A-c dielectric strength (kv)	4.3	5.8
Sample 3		
A-c dielectric strength (kv)	45 (480 volts per mil)	31 (290 volts per mil)

* One-fifth-ampere fuse.

Table XII. Low-Energy Impulse Faults in Rubber

Sample 1

Impulse failure at 70 kv. No flash but glass broken
No location with magnifying glass
OK on 1,000-volt Megger
Fault picked up at 4 kv d-c by 0.6-milliampere kenotron
Fault less than 0.5 millimeter in diameter

Sample 2

Impulse failure at 60 kv
Fault located to within two inches by aid of flash
No location with magnifying glass
Fault picked up at 3.4 kv d-c by 0.6-milliampere kenotron
Fault less than 0.1 millimeter in diameter

ties of these faults in the two types of insulation.

The tests reported in table XI were made on two-foot samples of number 6 solid, $^{6}/_{64}$-inch wall of insulation lead sheath. The impulse tests were made on generator 1 with the 1x10 wave.

As shown in table XI both number 1 samples withstood 15 kv direct current for 15 minutes without failure but failed when subjected to a 165-kv impulse. After this both samples withstood 3 kv alternating current for 1.5 hours without failure, when they were removed from test. Since this voltage is more than double the operating voltage of each cable it shows that both faults would have remained in service for a considerable period after the impulse failure. Finally each fault was broken down with direct current. Although this test was made with a one-fifth-ampere fuse in series with the sample each fault was burned out to some extent. In spite of this burning each fault showed an a-c strength more than twice the operating voltage. It is interesting to note that the cambric fault gave considerably higher dielectric strength on both the d-c and a-c breakdown tests. That is, both faults would have withstood the operating voltage for some time after the impulse failure, possibly indefinitely, but the cambric fault was less likely to cause a service failure than the rubber fault. The tests on the number 2 samples confirm this comparison.

This indicates that an impulse fault in cambric is less likely to cause a service failure than one in rubber but that in either insulation the impulse failure may not cause a service failure for days, months, or even years after the impulse failure. In confirmation of this we have found lightning faults in rubber-insulated cable that was taken from service after a perfect operating record. The presence of water undoubtedly would increase the probability of an impulse fault failing in service; that is, the cable may fail during the first rainstorm after the lightning, or it may never fail.

Incidentally, these tests bring out one advantage of high-voltage d-c testing. It is a most effective means of developing any lightning faults that may be in the cable.

Table XII gives additional data to indicate the small size of a low-energy impulse fault. Each sample was one foot of number 1 4,$^{3}/_{64}$-inch wall of rubber, tested under water in a glass jar, using generator 1 and the 1x10 wave.

The results show that the size of such a fault may be extremely small, too small to be visible on the outside surface of the rubber. Even after being burned out by the kenotron the faults were very difficult to locate visually.

Conclusions

Within the limits of the tests made, the following conclusions appear justifiable:

1. The impuse strength of cable insulation is not critically affected by the shape or polarity of the impulse, the type of outer electrode, or the length of the specimen.

2. In walls of $^{6}/_{64}$ inch or thereabouts the impulse strength of rubber-insulated cable may be from 700 to 1,600 volts per mil depending on the type of rubber compound. The strength decreases with increasing wall.

3. The impulse strengths of varnished-cambric and oil-paper insulated cables are about 1,000 and 2,000 volts per mil respectively.

4. The impulse strength of cambric and oil paper insulated cable is between three and four times the 60-cycle strength but that of rubber-insulated cable may be anything from two to four times the 60-cycle strength.

5. In sheets between two-inch disks the impulse strength of rubber is some 50 per cent higher than that of rubber cable insulation of the same wall. Similar to cable insulation the sheet paper is about double the sheet rubber but unlike cable insulation the sheet cambric is nearly as high as the paper.

6. The fiber tape and braid in rubber-insulated tree wire do not add appreciably to the dielectric strength impulse or 60 cycles of the wire, particularly when wet.

7. An impulse fault in cambric insulation has higher a-c strength than one in rubber insulation but the a-c strength of either fault may be sufficient to withstand the operating voltage for long periods of time.

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Discussion

R. B. McKinley (nonmember; General Electric Company, Schenectady, N. Y.): We have also made some impulse tests on rubber insulation. Most of these were on 30 per cent and ozone-resisting compounds.

Our test results were in agreement with those presented by Messrs. Davis and Eddy in the fact that the impulse strength in volts per mil decrease rather rapidly with increasing wall thickness. Some of the answer for this might be due to the mechanics of manufacture in that it is more difficult to hold the heavy insulation walls perfectly centered during the vulcanization period, although this was checked carefully and the fact that there might be greater difference in the degree of "cure" throughout the insulation wall. That the degree of vulcanization does have some effect is indicated by a few tests we have made.

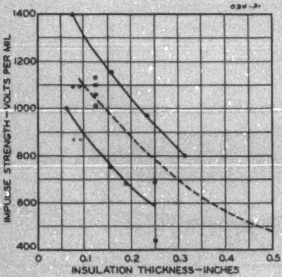


Figure 1

Our values, together with some of those reported in the Davis and Eddy paper, have been plotted, and I believe the chart is worthy of a little study, as it might offer an additional reason why general experience indicates that rubber is more subject to lightning damage than varnished cambric. Furthermore, it might also indicate the advisability of further study before adopting rubber insulation for voltages in the range from 22 to 35 kv in places exposed to severe surges.

The upper curve of figure 1 of this discussion was drawn from values given in table VI of the Davis and Eddy paper. The lower solid curve shows our results obtained on 30 per cent insulation. It is interesting to note how closely the two curves parallel each other throughout the range of thicknesses tested. The points marked by crosses were taken from table VII for 30 per cent class *AO* rubber and indicate how closely the tests agree.

The points indicated by circles or squares are those for the ozone-resisting or oil-base compounds, including insulations of various manufacturers. I have drawn the dotted curve in parallel to the other two curves and extrapolated to one-half-inch insulation thickness merely to investigate what the possibilities might be at the higher voltages. The true curve for ozone-resisting insulations might of course be much different from the dotted curves shown, although the small amount of information that we do have does indicate that these insulations also have lowered impulse strength with increased thickness.

I have calculated the breakdown voltages for 26-kv cable with varnished cambric, paper, and oil-base insulations. The thicknesses of insulation for 26-kv grounded neutral for the three insulations would be 0.422 inch, 0.297 inch, and 0.469 inch, respectively. Using 1,040 volts per mil for the varnished-cambric insulation in accordance with the Davis and Eddy results, 1,600 volts per mil in accordance with the Foust and Scott paper, and 500 volts for the oil-base insulation as indicated by the extrapolated curve, the following values would be obtained:

Varnished cambric	439 kv
Paper	475 kv
Ozone-resisting	235 kv

I do not want to leave the impression that I am saying rubber insulation will not operate at 26 kv. There are cases in service to disprove that, but I do believe the results given in this paper show us something that bears looking into further before we extend the use of high-voltage rubber too rapidly.

C. M. Foust (General Electric Company, Schenectady, N. Y.): The work reported by Messrs. Davis and Eddy is interesting and valuable. On several occasions I have conducted similar tests on impulse breakdown strength of rubber insulation and have reached conclusions in agreement with the authors on most points. My general level of voltages and gradients for various wall thicknesses agrees with the figures given in the several tables of the paper and breakdown gradients decreased with increasing thicknesses as shown in table VI.

I am puzzled by the results shown in table V on the effect of specimen length. In view of the variation in breakdown between samples at any one specimen length, it seems reasonable that the impulse strength for the longer specimens should decrease as the 60-cycle values do. It is a little disconcerting to have the impulse 60-cycle breakdown ratio vary with specimen length. I am wondering if in some of the tests described an appreciable portion of the output voltage of the generator might not have been across the ground-electrode water resistance, particularly for the longer test pieces of high capacitance. At a breakdown voltage of 90 kv reached uniformly in one microsecond across a capacitance of 0.0024 microfarad, the current through the water would be about 200 amperes. At anything less than, say, ten ohms, this would be inconsequential. However, at higher resistances an appreciable resistance voltage would be obtained, resulting in reduced voltage across the insulation particularly for breakdown values occurring before crest.

Our tests using the cathode-ray oscillograph with the viewing and recording each applied wave showed breakdown to be occurring before or on the wave crest. In many cases final breakdown indicated by the chopping of the wave, occurred substantially below the previously applied crest-voltage level and on the rising front. When these actual chopped wave values are taken a breakdown-voltage value some ten per cent below the full-wave value is obtained.

In my test only one voltage wave was applied at each level while the authors applied two. Indications of occasional damage without breakdown suggest that some five applications at each level would provide a more representative breakdown value.

The authors have described tests wherein successive applications of direct, impulse, and direct voltage were applied with the conclusions derived therefrom that a cable having an impulse breakdown may remain in service for some time. However, in service the normal excitation would usually be on at the time of impulse failure and immediate follow-up of short-circuit current would certainly result in many cases.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): Information on the surge breakdown strength of insulated power cables has been rather meager, and so the data on a variety of insulations from Davis and Eddy and on impregnated-paper insulation from Foust and Scott are quite welcome. In general, data on surge strengths of various insulations have been appearing in relatively good quantity in recent years in this country and abroad, particularly at the Paris meetings of the International High-Tension Conference.

Troubles in cables due to lightning have been pretty much confined to the lower-voltage cables connected to overhead systems. Dielectric strength to withstand surges, either due to switching or due to lightning, may, however, be the limiting factor in determining minimum safe insulation thicknesses for the higher types of insulation in extra-high-voltage cable. Surge strength to withstand switching surges was a factor, for example, when in Chicago we adopted, in 1935, oil-filled insulation of 315 mils thickness for the 100,000-kva 66-kv lines, whereas with the solid-type 66-kv cable with about 688 mils of insulation the surge strength was greatly in excess of requirements.

The industry would, I believe, be interested in data obtained by others similar to the data on a number of variables obtained by Davis and Eddy, since such data are comparatively rare.

The rest of this discussion applies to impregnated-paper insulation for particularly power cables.

The average surge strength of 1,600 volts per mil found by Foust and Scott agrees reasonably well with the results of Held and Leichsenring[1] who reported 1,770 to 2,400 volts per mil average surge breakdown stress and with the average value of 1,920 volts per mil given in the paper by Davis and Eddy presented at this convention.

Some discrepancies between the results of various investigators probably are due to the effects of different variables which have not been well established as yet. It appears, for example, that higher breakdown values are obtained with small laboratory samples than with complete cables. It is gratifying, therefore, that the tests reported in the Foust-Scott paper were all made on complete cables. In a 1933 AIEE paper, Mr. Scott reported the results of surge tests on 15 mils of impregnated-paper insulation between flat electrodes. The surge strength for these samples was about 3,400 volts per mil. J. Borel[2] reported last year results of surge tests on small laboratory samples with concentric electrodes and insulation thicknesses of 40 to 160 mils. He obtained surge breakdowns at a maximum stress of about 2,900 volts per mil and an average stress of about 2,500 volts per mil. Also the results of Davis and Eddy confirm the conclusion that much higher surge strength values are obtained for small laboratory samples than for complete cables. They report for the former an average impulse strength of 3,890 volts per mil as compared with 1,920 volts per mil for the complete cables.

The relative surge strength of various types of cable still seems somewhat indefinite, even after considering the two papers. In a paper by Buss and Vogel,[3] published in 1935, the following tentative values were given: solid-type cable, 2,540 volts per mil; oil-filled cable, 2,000 to 2,540 volts per mil; pressure cable, 2,540 to 3,000 volts per mil. While these results indicate higher surge strength of pressure cable than for solid-type or oil-filled cables, Mr. Scott stated in his 1933 AIEE paper that "at power frequencies and under long time application of voltage there is a considerable increase in endurance strength of oil-treated insulation at pressures of several atmospheres. Under impulse stresses, however, practically no benefit is derived from increasing pressure. The effect of pressure on dielectric strength is greater the longer the time of voltage application, being negligible for the impulse voltage tests of only a few microseconds duration." On the other hand, Borel found an increase in surge strength with increasing pressure.

Other questions of interest are the effect of thoroughness of impregnation and of aging on the surge strength of cables, the cumulative effect, if any, of repeated surges, the surge strength of three-conductor belted cables as compared with single-conductor cables, and the shape of the time-lag curve. It is realized that tests of this type are cumbersome, and progress in answering these questions will be of necessity slow.

It is noted that all tests were made by Foust and Scott with positive polarity and most tests by Davis and Eddy were with negative polarity. Held and Leichsenring found no effect of the polarity on oil-filled cables but for solid-type cables obtained 5 to 13 per cent higher surge strength for positive waves than for negative waves. The higher lightning voltages are usually negative.

REFERENCES

1. SURGE STRENGTH OF HIGH-TENSION CABLE, paper number 207, at 1939 Paris meeting of International High-Tension Conference.

2. CONTRIBUTION TO THE STUDY OF ELECTRIC CABLE, paper number 202, presented at the 1939 Paris meeting.

3. SURGE TESTS ON HIGH-VOLTAGE CABLES, *V.D.E. Fachberichte*, 1935, pages 61–3.

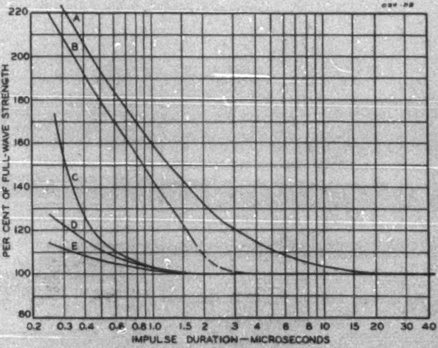
P. L. Bellaschi and **W. L. Teague** (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): These papers by Foust and Scott and Davis and Eddy present interesting data on the impulse and 60-cycle strength of insulating materials particularly those used in cables. In testing similar materials and parts and in our experience with the various factors affecting insulation breakdown, we have obtained results which are, on the whole and in principle, in accordance with the findings so ably reported in the papers by Messrs. Davis and Eddy and by Messrs. Foust and Scott. It would lead the discussion too far afield with perhaps no particular profit to attempt a minute comparison of the various findings.

There is a pertinent point which has a practical and a fundamental bearing on the insulation problem. Both papers indicate a volt-time characteristic for the type of insulation tested which is essentially constant down to one or two microseconds. These are in effect the shortest times to which the investigations were carried. The curves of figure 2 of this discussion enlarge on this fundamental question. These curves present the volt-time characteristics of liquid and solid insulation, and of apparatus insulation from the long times down to the very short impulses of 0.25-microsecond duration.

Curve *A* is the characteristic for transformer oil between a disk and a plate; curve *D* is for oil impregnated pressboard for the same electrode arrangement.[1] Curve *B* is the volt-time characteristic of major transformer insulation as applied to high-voltage design.[2] Curve *E* is for turn-to-turn oil-impregnated paper insulation. Curves *D* and *E* are also typical for other insulation, as varnished cloth, etc. While we have no data at this moment on the impulse characteristic for rubber at the very short times, there are no good reasons to suspect that it has a greater upturn at the very short times than in these last two curves.

The degree of the upturn of the volt-time characteristic depends on a number of factors, as the relative amount of liquid and solid insulation which make up the insulation body or structure, the treatment applied to the insulation, the electrode arrangement and insulation of the electrode proper, etc.

Figure 2. Volt-time characteristics of insulation



A—Transformer oil, one-fourth inch between disk and plate electrodes (reference 1)
B—Transformer major insulation (reference 2)
C—Oil-filled cables (reference 3)
D—Oil-impregnated pressboard. Average for $^1/_{16}$- and $^1/_8$-inch thickness between disk and plate electrodes (reference 1)
E—Oil-impregnated turn-to-turn insulation. Average values for 0.03-, 0.06-, and 0.12-inch thick paper

Curves *A*, *D*, and *E* apply for repeated applications, which consist usually from 15 to 30 impulses being applied before the point of breakdown is reached. Curve *B* is an endurance curve for hundreds of applications.

Curve *C* (from the test data on cables by Held and Leichsenring) was established from single applications. On the flat part, repeated applications gave voltage values not more than five to ten per cent lower. It is possible that the upturn at the very short times would be lower on repeated applications and that the curve would come nearer to *D*. There are, however, other factors which may account for the substantial upturn of curve *C*, as the presence of oil in relatively good amount in an oil cable. The results of Held and Leichsenring at the longer times apparently differ in one respect from those of Messrs. Foust and Scott, in that the former found the breakdown gradient at the conductor to be a constant.

The additional data on the dielectric strength of cable insulating materials, presented in these two papers, should make it possible to better tie in the relative strength and characteristics of various apparatus, and to this additional extent, give further scope and strength to the general problem of insulation co-ordination.

References

1. Dielectric Strength of Transformer Insulation, P. L. Bellaschi and W. L. Teague. Electrical Engineering (AIEE Transactions), volume 56, page 164.

2. Vacuum Boost Insulation Strength of Transformers, P. L. Bellaschi and F. J. Vogel. *Electric World*, January 1939.

3. The Behavior of High-Tension Cable Installations Under the Effect of Voltage Impulses, Ch. Held and H. W. Leichsenring. Paper number 207, C.I.G.R.E., Paris, June–July 1939.

L. G. Smith (Consolidated Gas Electric Light and Power Company of Baltimore, Baltimore, Md.): The industry is indebted to the authors for the data contained in the papers by Foust and Scott and Davis and Eddy. We have impulse flashover strength of line and station insulators and have available impulse levels of transformers and other apparatus. These data for cable augment the information necessary to design the system insulation levels and lightning protection installations. The impulse breakdown values reported presumably are for new cable. While information for new insulation is a necessary step, the user is also interested in similar data for older cable, after voids have formed, the oil deteriorated, or some "tracking" has occurred in paper cable; the rubber has aged in rubber-insulated cable; or the insulation has deteriorated in varnished-cambric cable. Lightning protection must be designed to protect the cable throughout its useful life. It is realized that obtaining the data presented in the papers involved considerable time and expense; and that the testing necessary to establish impulse strength of insulation as it ages represents even greater costs. However, the suggestion is made at this time as a guide to further research.

In interpreting the data on paper cable, the conclusion is reached that insulation thickness and conductor size has slight effect upon the impulse strength. On the other hand, the insulation thickness appears to have a material effect upon the impulse breakdown of rubber cable. It would be of value to determine the effect of conductor size on both rubber and varnished-cambric insulated cable and the effect of insulation thickness on varnished cambric cable.

It is interesting to note that the type of rubber compound that supposedly possesses the best aging characteristics developed the lowest impulse breakdown. The statement made in the Davis and Eddy paper that an impulse breakdown of rubber or varnished-cambric insulated cable may not result in a service failure is questioned. The 60-cycle tests were made subsequent to impulse breakdown. If normal rated 60-cycle potential had been connected to the cable at the time of the impulse test, it is believed that power follow current would have reduced the fault resistance to the point where normal voltage could not have been withstood.

L. F. Hickernell (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): This paper is a valuable contribution to the knowledge of the electrical characteristics of insulated power cables in that it presents, for the first time before the AIEE, impulse-strength data on varnished-cambric and rubber cable insulation. Previous and accompanying papers have been confined to similar data on impregnated-paper insulation. ("Traveling-Wave Voltages in Cables" by H. G. Brinton, F. H. Buller, and W. J. Rudge, AIEE Transactions, March 1933, page 121, figure 7, and "Some Impulse-Voltage Breakdown Tests on Oil-Treated Paper-Insulated Cables" by C. M. Foust and J. A. Scott, AIEE Transactions, volume 59, 1940, pages 389–91.)

Impulse Generators

Referring to "Impulse Generators", it is stated the *impulse* voltage was applied in seven-kv steps. Referring to "Impulse Strength of Rubber-Insulated Tree Wire", it is stated that the *60-cycle* voltage was applied at the rate of three-kv per 15 seconds. In determining values of ultimate breakdown strength, the rate of rise of the applied voltage is important. Generally speaking, the faster the rate, the higher the value obtained. Accordingly, the following questions arise:

1. Were all 60-cycle breakdown tests conducted with the same rate of rise of applied voltage?
2. If so, what was the rate? (That is, does the three-kv per 15 seconds, stated under "tree wire" apply to *all* 60-cycle tests?)

If the rate of rise were three-kv per 15 seconds, it should be noted that the values in the paper cannot directly be compared with data obtained from factory test reports which generally are governed by ASTM designation *D*470-37*T*. Section 13 of this standard specifies a rate of rise of three-kv per second.

Effect of Electrode Surface on Impulse Strength

Table IV shows the results obtained with the following means of providing the grounded electrode:

1. Lead sheath
2. Immersion in salt water
3. Lead foil wrapped over insulation

It is concluded that the method used has no effect on the results. Inasmuch as (1) is unaffected by the time of immersion whereas (2) and (3) are, it would seem that the time of immersion prior to test was relatively short and should be so stated. In any event, the limit of the time of immersion for which this conclusion holds should be stated.

Effect of Insulation Thickness on Impulse Strength

Table VI indicates a decrease in unit impulse strength with increasing wall thickness which is the basis for the statement, "The same tendency has been shown by the a-c strength of many insulations." While it is generally true that the unit *60-cycle* strength decreases with increasing thickness, most investigators have found that the unit *impulse* strength of insulations in general is practically proportional to their thickness; or as popularly stated, "an inch is an inch" insofar as impulse strength is concerned. This has been demonstrated by many investigators for porcelain insulators and to a more limited extent for impregnated-paper insulation (see references in first paragraph). Therefore, it would seem that if the data in table VI are truly representative after confirmation, the conclusion should be confined to *rubber* insulation.

Effect of Compound Type on Impulse Strength

In table VII, the following average 60-cycle strengths are shown for three types of rubber insulation:

Rubber Compound	60-Cycle Strength (Average) Volts Per Mil
Code	315
ASTM type *AO*	255
Performite	275

While no claim has been made generally that type *AO* or Performite are high-voltage insulations, it is rather surprising to note that the authors obtain lower dielectric strength with their type *AO* (30 per cent) and Performite (35 per cent) than they do on their Code (20 per cent) compounds. It

Table I

Table Number	Insulation	Conductor (American Wire Gauge)	Insulation (64ths)	Outer Electrode	Impulse (Average Volts Per Mill)
VII	Anhydrex	6 or 8	5 or 6	(Salt water)	1,340
IX	Anhydrex	6	5	Lead	1,143
				Ratio =	1.17

is not believed that this is truly representative of the relative rating of these types of insulations in the industry in general.

RELATIVE IMPULSE STRENGTH OF RUBBER, CAMBRIC, AND PAPER-INSULATED CABLE

Rubber. The text states these tests were conducted on a "typical 60 per cent rubber," table IX indicating this to be "Anhydrex" rubber. It would be interesting to know the reason for the lower impulse strength of Anhydrex rubber shown in table IX as compared with table VII, as shown in table I of this discussion.

Paper. It would be of interest to know the thickness of the paper tapes as well as the Gurley air resistance. It may be pointed out that 300 seconds air represents a *low* density paper as judged by modern standards of paper-cable manufacture. This paper is quite different from that used for the sheet tests reported under "Impulse Strength of Rubber, Cambric, and Paper Sheets" (five mils, 2,000 seconds Gurley).

The term "typical oil" is rather indefinite in view of the present use of naphthenic and paraffinic-base oils from different fields, with or without rosin, by the several manufacturers.

Control of Rubber. The authors suggest the possibility of making rubber insulation equal to paper in impulse strength because the "compounding variables" in rubber are subject to a wider range of control than in paper or cambric. In this endeavor, the well wishes of the entire industry will be extended to them. However, it would seem only fair to point out that both theory and present experience would indicate this to be a most difficult if not impossible assignment. In general, dielectric strength seems to follow the same laws as mechanical strength; namely, a laminated structure is stronger than a solid structure. Rubber is a more or less solid material; paper and cambric are laminated structures.

While it is true that there are more "compounding variables" in rubber than paper and cambric and hence more factors to juggle, there are also more chances for things to go wrong and the manufacture is on the whole less subject to control. A recipe for a rubber insulation will contain from five to ten ingredients compared with two for paper or cambric. The advantage of this flexibility is offset by the difficulty of controlling the quality and dispersion of so many ingredients (gum rubber itself is practically impossible to control). In addition to formulation, a rubber compound must be mixed, rested, warmed, tubed or stripped, and vulcanized. While these operations are subject to some control, the degree does not approach that capable with paper insulation. A paper cable can be closely governed by actual measurement of its electrical characteristics during its processing. Accordingly, it is doubtful if a commercial length of rubber cable can be made foot by foot of as high and uniform quality as paper cable simply because the manufacturing processes are inherently not subject to as fine a degree of control.

PROPERTIES OF IMPULSE FAULTS IN RUBBER AND CAMBRIC INSULATION

The conclusion that the 60-cycle strength of either rubber or cambric is sufficiently high after an impulse fault to prevent a *service* failure for some time is not substantiated by the test data in table XI. In actual service, a lightning fault is generally followed by a 60-cycle power arc which burns the insulation and greatly lowers the a-c strength. This service condition is not simulated in a shot from an impulse generator with no power follow-up.

E. W. Davis and W. N. Eddy: Mr. McKinley's calculated comparison of the impulse strength of 26-kv walls of varnished cambric, paper, and rubber leaves the rubber a poor third with only one-half the impulse strength of paper or cambric. We cannot agree that this comparison is justifiable, for three reasons:

1. His determination of the ozone-resistant rubber strength by extrapolation to 0.469-inch wall is too speculative with the available data limited to six points between 0.075- and 0.125-inch wall.

2. His assumption that the cambric strength at 0.422-inch wall is as high as that at 0.094-inch wall does not appear justified in the complete absence of data. Cambric may not be any more similar to paper in impulse strength than in other electrical properties.

3. For a 26-kv cable we would use a 60 per cent high-voltage rubber (with proper ozone protection) which in table VII has an impulse strength 50 per cent higher than that of the ozone-resistant rubber.

Both Mr. Foust and Mr. Hickernell question the significance of the data in table XI because the impulse faults were not produced with the 60-cycle and impulse voltages superposed as they probably would be in the field. We do not believe that this limitation is serious. Considerable data from laboratory tests on cable taken from service, from field tests on installed cable, and from customers' records indicate that impulse faults may be formed in the presence of continuously applied 60-cycle voltage and the cable still remain in service for an indefinite period after the lightning damage. A typical field experience is that the breaker opens during the storm but is immediately reclosed with no further trouble. Sometimes there is not even this indication of lightning trouble. Hours or even days after the storm the cable fails. Subsequent tests show that in addition to the developed fault causing the failure the cable often contains other faults having properties very similar to the laboratory-made impulse faults described in tables XI and XII.

Mr. Foust is puzzled by the constant impulse strength shown in table V with increasing sample length. We share his point of view. In fact we investigated the matter in more detail than is indicated in the paper and intend to continue this investigation at the first opportunity. In connection with his discussion of the voltage drop across the water the following testing details lead us to doubt that an appreciable portion of the impulse is taken by the outer cable electrode.

All water was salt water. Tests were made in water on samples from one length at one, two, five, and ten feet from another length at one, two, five, and ten feet and from another length at two and five feet. Tests were made on leaded samples (not in water) from another length at 10, 20, and 40 feet. None of these 70 tests show any appreciable effect of the length. They are all well represented by the values in table V.

With reference to Mr. Hickernell's questions, in the order given;

1. All 60-cycle breakdown tests were made with the same rate of voltage increase, three kv per 15 seconds. Our experience indicates this rate to be the most practicable in general for such purposes. We have been using it more than 12 years.

2. In connection with table IV we do not understand his statement that method 3 (lead foil wrapped over insulation) is affected by the time of immersion. Only method 2 (immersion in salt water) is affected by the time of immersion. In general our tests indicate that there is no appreciable influence of immersion time in salt water on dielectric strength for several months. Since none of the samples reported in the paper were immersed in the salt water more than one hour, we believe there is no influence of immersion time.

3. We agree that any conclusion from table VI should be limited to rubber insulation. In fact we feel that the wording of the paper makes this quite obvious.

4. With reference to table VII he questions the relative 60-cycle strengths of Code, *AO*, and Performite compounds (given as 315, 255, and 275 volts per mil respectively). Our tests indicate that these differences in dielectric strength are too small to be of any significance. These particular values were obtained on the particular wire used for the impulse tests. Hundreds of other tests made by us on these three compounds indicate no significant difference in 60-cycle strength among the three of them.

5. He questions the slight difference in the impulse strengths of Anhydrex shown in tables VII and IX. We have a good many different Anhydrex compounds. As it happens, that in table IX showing a strength of 1,143 volts per mil is one of the earlier compounds while that in table VII (1,340 volts per mil) is a more recent compound. As stated in the paper, all the tests were not made at the same time.

7. The tapes in the paper cable were three-fourths inch wide by 0.007 inch thick. The oil was paraffin-base, without rosin.

8. Without attempting a detailed discussion of the control of rubber we may say that we are unable to share Mr. Hickernell's pessimism in this respect, because we have too much evidence that the physical and electrical properties of rubber compounds can be kept under close control.

Co-ordination of Power and Communication Circuits for Low-Frequency Induction

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WHERE power and communication facilities are in proximity, electromagnetic induction from the power system may cause disturbances in the communication system. The avoidance or minimizing of such disturbances, with due regard to the service and other needs of both systems, is a problem of co-ordination, which is conveniently divided into two parts, one dealing with low-frequency inductive co-ordination and the other with noise-frequency co-ordination.

The present paper undertakes a general examination of the problem of low-frequency inductive co-ordination in the light of developments during the past decade. The situation as it existed at the beginning of the decade is to be found well set forth in a paper presented in 1931 at the AIEE winter convention by R. N. Conwell and H. S. Warren. The present paper, like its predecessor, derives from the work of the Joint Subcommittee on Development and Research[*] of the Edison Electric Institute and the Bell System. It is largely concerned with induction from currents due to power system ground faults and the transients which accompany such faults. It gives relatively little attention to continuous low-frequency effects since, up to the present at least, such effects have not been a primary concern in the low-frequency co-ordination of commercial power circuits and Bell System communication circuits.

A further object of the paper is to outline the various factors that require consideration in practical situations and to discuss their significance under present-day conditions. To provide necessary background for this, recapitulations of fundamentals are included at appropriate points. Detailed discussions necessarily omitted from the paper itself are to be found in the papers listed in the bibliography, many of which, particularly the Conwell-Warren paper,[1] contain further references.

A situation which calls for measures of inductive co-ordination ordinarily involves a parallelism or quasi parallelism of sections of power and communication lines. The possibility of disturbance by electromagnetic induction from the one system to the other depends upon certain characteristics of the power system (called "influence factors") and also upon such characteristics of the communication system as affect its liability to disturbance ("susceptiveness factors"). Of equal significance are the "coupling factors", which determine the electromagnetic coupling between the two systems. Figure 1 is a diagrammatic representation of the main factors of these three groups in a low-frequency induction situation. Some of these items can obviously be evaluated by calculation or by relatively simple tests, while others do not lend themselves to determination by such methods.

In a low-frequency induction problem, where, as in figure 1, power-system ground-faults are involved, it is customary, first, to calculate the maximum "open-circuit" longitudinal induced voltage. To do this, values are required for the residual power current (that is, in general, ground-return current) to produce this maximum voltage and for the corresponding coupling coefficient. The "open-circuit" voltage thus calculated is next reduced to what may be called the "shielded" maximum longitudinal induced voltage. In this reduction, allowance is made for shielding effects exerted by normally grounded paralleling conductors (ground wires, cable sheaths, etc.) and, in the case of cables, by core conductors. In addition, allowance must be made for additional shielding resulting from longitudinal currents set up in communication conductors normally free from connections to ground, but which become grounded, through protector operation, as a result of the induction. This latter allowance is not ordinarily a simple matter.

It is also usually necessary to know approximately how frequently voltages within specified ranges of magnitude will be induced, and how long they will last when they occur, since the duration of the disturbance affects the energy transferred to the communication system.

Further, although the longitudinal voltages are determining with respect to some kinds of interference, voltages to ground are important with respect to other types. Hence a certain amount of estimating of the distribution of the induced longitudinal voltages with respect to ground is generally desirable.

The final step is, of course, the estimation of the adverse effects of these voltages upon the operation and maintenance of the communication circuits, and the determination where necessary of measures to be taken in either or both systems to minimize the effects.

Valuable information regarding the fundamental factors just discussed has been secured in an analysis[2] of automatic oscillograph observations from 29 actual exposures, which also indicated sources of uncertainty in making advance appraisals of low-frequency induction situations. Comparison of maximum observed voltages with maximum estimated values showed deviations up to about 75 per cent, in both directions. Contributing to these discrepancies were shielding from telephone-line conductors grounded by protector operation, erroneous assumptions of fault resistances, failure of faults to occur at or near locations to produce the maximum voltage, and wave-shape distortion of induced voltage. Duration of induced voltage, distance between protector locations, and number of wires on the telephone line were important in cases of protector grounding. Distribution of induced longitudinal voltage with respect to ground was clearly a matter of importance, although not covered by the available data.

The several factors of influence, coupling, and susceptiveness will now be discussed in order.

Power Systems—Influence Factors

RESIDUAL CURRENT MAGNITUDES—CALCULATION AND LIMITATION

The magnitude of fault current and its distribution among the various branches of a network may be calculated for any

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Paper 40-49, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 22-26, 1940. Manuscript submitted November 8, 1939; made available for preprinting December 27, 1939; released for final publication February 21, 1940.

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[*] Referred to hereinafter as the "Joint D. & R. Subcommittee."

1. For all numbered references, see list at end of paper.

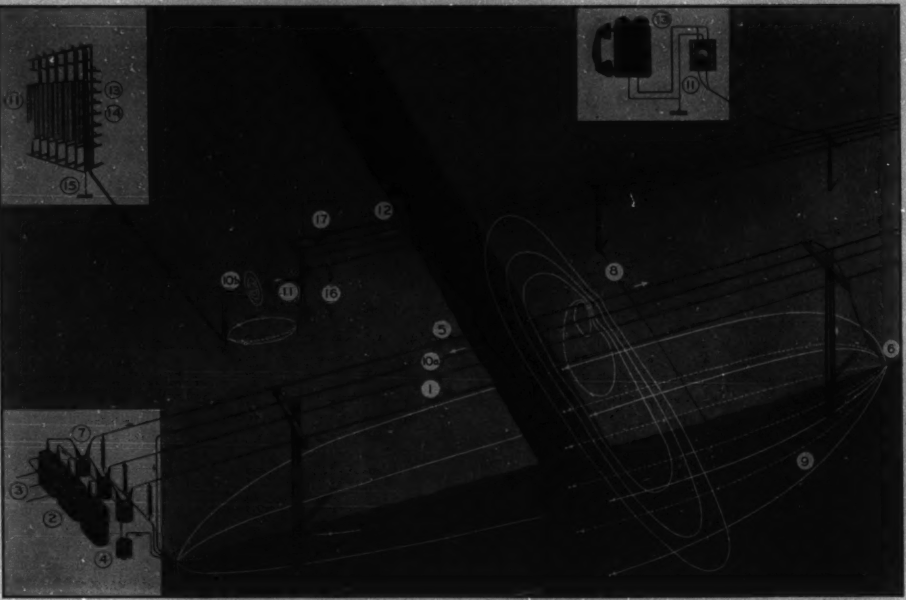


Figure 1. Factors affecting the severity of an inductive exposure

Influence Factors—Affecting magnitude and duration of power-system residual fault current:

1. Line voltage
2. Transformer impedance
3. Impedance of supply to transformers
4. Method of neutral grounding and grounding impedance if used
5. Power-line impedance
6. Fault location and fault resistance
7. Circuit breakers and tripping equipment

Factors (not indicated in drawing) affecting frequency of occurrence in faults include:

Lightning, wind, and sleet storms
Construction practices
Foreign objects in line

Coupling and Shielding Factors—Affecting induced voltage in communication system for given residual current in power system:

8. Separation between lines
9. Earth resistivity (also affects influence)

10a, 10b. Shielding from power-line ground wire and telephone-cable sheath when present

Susceptiveness Factors—Affecting communication system disturbances under given induced-voltage conditions:

11. Protectors—breakdown of blocks and rating of fuses
12. Size, length, kind, and number of wires on line (Some wires may become shielding conductors due to protector-block operation)
13. Low-frequency characteristics of equipment
14. Impedance to ground of terminal equipment
15. Resistance of ground connection
16. Voltage from conductors to ground
17. Voltage between wires (arising from differences of voltage to ground)

(Not indicated)—Location of exposure with respect to nearest protector blocks

assumed fault location and operating setup.[3] For a grounded-neutral system the fault location is usually assumed at the end of the exposure remote from the neutral ground. When line-to-ground fault current may be fed into the exposure from two directions, consideration should be given to the sequence of breaker operation in calculating the conditions for maximum induced voltage. Where ungrounded circuits are involved, it is the usual practice to assume a fault on one phase at one end of an exposure and a fault on another phase at the other end. This condition gives the maximum possible ground current through the exposure and hence the maximum induced voltage. As a rule, this current is larger than the current through the same exposure under the assumption of direct-grounded power neutral.

The impedances of power lines and cables are important elements in determining the magnitudes of residual currents under fault conditions. The most important gaps of ten years ago in data regarding these quantities have been filled in a report of the Joint D. & R. Subcommittee.[4] Impedances in power-circuit neutral-ground connections of course affect ground-fault current magnitudes. Such impedance may be inherent in the grounding equipment or may take the form of an external resistor or reactor, or a Petersen coil (ground-fault neutralizer). Considerations arising from power system design and operation affect the magnitude of neutral impedance which may be used.

In calculating ground-fault currents for use in estimates of induced voltages, significant errors may be caused by failing to take account of fault resistance. This, and other fault characteristics of interest, have been studied by the Joint D. & R. Subcommittee with the co-operation of a number of power companies.[5,6] Data were collected by means of automatic oscillographs from 11 operating power systems, having a total of about 2,800 circuit miles of transmission line at voltages ranging from

26 to 220 kv. The periods of observation were from $1\frac{1}{4}$ to $5\frac{1}{2}$ years. For one-line-to-ground faults, the most frequently occurring apparent fault-resistance, irrespective of fault location, was found to be about 20 ohms (5 ohms for faults at stations), with no great difference as between lines supported on steel structures and on wood structures. However, the studies did not include any lines designed for maximum utilization of the impulse strength of wood.

Since the installation in 1931 of two Petersen coils on a 140-kv system,[7] about 30 other Petersen coils have been installed in this country. With a given arrangement of coils, the residual current in a given exposure, and consequently the induced voltage, is practically independent of fault location. With a directly-grounded or an isolated power system, the induced voltage depends upon the relative locations of fault and exposure, and may vary over a wide percentage range. In grounded-neutral systems, residual currents large enough to make limitation desirable for inductive co-ordination purposes usually occur only where the neutral is grounded either directly or through a low impedance, and large reductions are obtainable through the employment of a Petersen coil.

Line outages preventable by Petersen coils are mainly those due to lightning flashovers. To clear faults not extinguished by the coil, recourse is usually had to short-circuiting the coil, either directly or through an impedance, and relaying out the faulty section of line. Unless a fairly high impedance is used, the residual current would usually be quite large under this condition. But the proportion of all faults that can be cleared without by-passing the coil (reported as from 70 to 90 per cent in various cases[8]) is sufficient to be of real value, even when no impedance is used in series with the by-pass breaker.

From the standpoint of inductive co-ordination, other types of neutral impedance, such as resistors, can be used for current limitation, and their status does not appear to have changed in the past ten years or so. Fault-current limitation has certain advantages from the power operating standpoint, for example, in limiting voltage dips, possible loss of load, and shock to system during faults,[7] and for improving transient stability.[9] Against these advantages, certain disadvantages have to be weighed, if the neutral impedance is to be high enough to have a large effect in most low-frequency induction cases. These questions center mainly upon dynamic overvoltage and the closely related matter of system insulation, and upon relaying.[10]

Dynamic overvoltages vary over a power system because of the flow of residual current through line impedances. In figure 2 a few results are shown from a recording oscillograph study of dynamic overvoltages on overhead systems under operating conditions.[11,12] The percentage of observed cases in which the voltage exceeded twice normal was about 64 for the isolated system, about 5 for the system grounded through Petersen coils, and about 34 for the resistance-grounded system (75 ohms). These records from operating systems were in general accord with the results of staged tests.[13] No evidence was found of the building-up of high sound-phase voltages, such as have been inferred to be possible from theoretical studies of the "arcing-ground" phenomenon. The view has recently been expressed by other investigators that such a voltage build-up is not to be expected if the arc path is between fixed contacts in open air, as in the case of an arc to ground on an overhead line, but that it may occur if the arc occurs in a confined space[14] or between breaker contacts whose separation is increasing.[14,15]

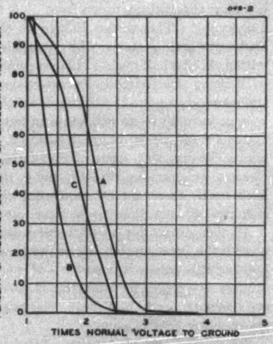


Figure 2. Dynamic overvoltages—magnetic-oscillograph observations on operating systems

A—800-mile 140-kv isolated system
B—274-mile 140-kv Petersen-coil system
C—158-mile 26-kv 75-ohm resistance-grounded system

Questions concerning system insulation[16,17,18] may arise when it is proposed to apply a relatively high neutral impedance to a system previously operated with neutral grounded directly. The nature of these questions can be indicated briefly by recalling that a lightning arrester must operate at an impulse voltage amply below the insulation level of the protected apparatus and still not operate or pass current on dynamic voltages likely to be impressed upon it by power sources in the system. While there are many factors to be considered in suiting lightning-arrester protection levels to the insulation levels of equipment, a considerable improvement during the past decade in lightning arresters, together with more detailed knowledge of their characteristics, has somewhat simplified this matter.[19]

Relaying questions involved in neutral impedance applications to systems previously operated with direct ground connections[10] include the possibility of changes in ground relays or the adoption of ground relaying if not previously used, additional potential transformers (when ground relaying is not already in use), possible changes in bushing current transformers, and in some cases, a change to a different relaying system in some portions of a network (to maintain proper selectivity). Ground relaying frequently presents difficulties for systems grounded at more than one point through high impedance.

Frequency of Occurrence and Duration of Ground Faults

The frequency with which induced voltages of various magnitudes may occur in an exposed communication system depends, first, upon the frequency of occurrence of ground faults in the power system and, second, upon their distribution with respect to the locations of exposure and power sources. In most cases, appraisals of these factors are largely matters of judgment. Valuable guidance is usually obtainable from operating experience on the power system or similar systems in the same locality, and from consideration of the character of line construction, connected line mileage, and number and location of neutral grounds. Duration of ground faults can usually be approximated well enough from relay and circuit breaker characteristics.

A simple illustration of the bearing of the relative locations of exposure and power source upon the frequency of occurrence of induced voltage is given in figure 3. In diagram (*A*) the exposure is at an unfavorable location—practically every ground fault would result in residual current through the exposure. On the other hand, an exposure located as in diagram (*B*) would be appreciably affected only by the faults between stations *B* and *C*. In diagram (*A*), it would be advantageous, if practicable, to transfer the neutral ground to station (*B*). Moreover, this would not increase the number of cases of induced voltage in the exposure of diagram *B*, but would probably increase the voltages themselves.

In studies already referred to,[5,6] a large amount of data was collected regarding the characteristics of ground faults. As to frequency of occurrence there was considerable variation among systems (from 5 to 60 faults per 100 line-miles per year, averaged over the periods of observation), and also from year to year on a single system. Lightning was assigned as the known or probable cause of from 50 to over 90 per cent of the faults, depending on the system. On the average, some 10 per cent of the faults were cleared in about 10 cycles, and about 10 per cent required two seconds or more for clearance. The percentage of faults involving each of two phase wires and ground, but at different points, was small—from about one-half per cent to about six per cent in three systems for which data were available. This point is of interest in the use of neutral impedance for inductive co-ordination, since, if an exposure lies between the two fault points on different phase wires, a high induced voltage may result, the neutral impedance being ineffective for limiting ground current in this situation.

In the past ten years, the importance of direct strokes in the problem of protecting transmission lines from lightning has become fully recognized. This has led to a series of investigations which have greatly increased our knowledge of lightning,[20,21,22] and to the development of several methods of preventing interruptions to service from lightning. Measures for preventing ground faults or power follow-up after spark-over, or for limiting the duration of dynamic current to ground faults are directly helpful in low-frequency inductive co-ordination. In the remainder of this section brief comments are made on the developments that seem most significant from this point of view.

The use of ground wires on steel-tower lines results in a substantial reduction in ground faults from lightning.[23] The combined use of counterpoises and ground wires has given further improvement where tower-footing resistance or earth resistivity is high.[23,24] Where the counterpoise is continuous, it, as well as the ground wires, makes a further contribution by reducing coupling coefficients.

Possible improvements in line operation through the use of the insulating properties of wood,[25,26] while perhaps somewhat less attractive than they appeared some ten years ago, have also resulted in reduction of frequency of occurrence of faults. Quite recently renewed interest in the subject has led to a comprehensive experimental study of fundamental factors.[27]

The protector tube (expulsion protective gap), for controlling lightning flashovers on power lines, first used commercially in 1931–32, is finding increasing application, and has been used on lines covering the voltage range from 13.8 to 138 kv.[28,29] This device interrupts the flashover and restores the insulation within about a half cycle. It has no effect in limiting short-circuit current magnitude, and thus none in holding down the voltage induced in an exposed communication circuit. But its great rapidity of action limits the energy transferred. Because it operates directly upon the fault itself, there are no sequential circuit-breaker operations to be considered.

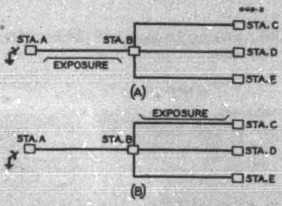


Figure 3. Relative locations of exposure and grounded sources of power—illustrative diagrams

Another device for arc-extinction purposes is the Petersen coil. From the viewpoint of this paper, it is perhaps of more interest as a means for limitation of residual current, and has been discussed above under that heading.

Relay and circuit-breaker developments have been toward substantially higher speeds of operation, both for new installations and in the modernization of existing installations. These developments have made available breakers with an operating time of about 0.13 second, and quite recently still higher speed (about 0.085 second). These figures do not include relaying time. The exceptional speed of about 0.05 second over-all has been reached in the breakers for the Boulder Dam lines.[30,31] Like "lightning-proof" line construction and the use of expulsion gaps, high-speed breakers have, of course, no effect upon short-circuit current magnitudes. But they have the advantage of not being limited to the clearance of lightning flashovers. As used in combination with high-speed reclosing immediately following high-speed opening of circuit breakers, there will probably be some cases where fault recurrence after reclosure will diminish the inductive co-ordination benefit resulting from the high-speed opening.

From the low-frequency induction standpoint, the most notable improvements in relaying have been increases in speed of response, the further development and use of pilot-channel methods,[32,33,34] and particularly the combination of the two to replace earlier methods of selection, with much greater rapidity of relay action. In some recent applications, the time to energize the breaker trip circuit has been reduced to about one cycle (60 cycles per second) by using high-speed relays in combination with pilot channels,[35] with practically simultaneous relay operation at the two ends of the line section.

These developments in the power art, tending to reduce both the frequency of occurrence and the duration of ground faults, are all favorable to low-frequency inductive co-ordination. From a practical standpoint, the resulting degree of benefit depends upon the extent of use of these developments, and upon which are more widely used, since they are not all equally favorable. These matters, of course, are not static and the actual extent of the resulting reaction upon low-frequency inductive co-ordination is not easy to specify in quantitative terms.

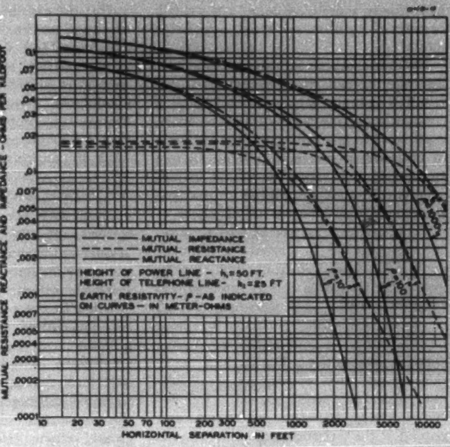
Coupling and Shielding

COUPLING

The residual power current, when multiplied by the coefficient of coupling between the power and telephone circuits, gives the open-circuit longitudinal induced voltage in the telephone circuit. In determining this coefficient of coupling, or mutual impedance, the telephone and power circuits are treated as ground-return circuits, regardless of normal conditions of operation as metallic circuits. Coupling coefficients can be determined as precisely as required. Methods of calculation have been considerably advanced, and in preliminary estimates, calculated values are generally used. As a rule, however, expenditures for co-ordination measures should only be made on the basis of test data or experience.

In earlier work, the calculation of coupling coefficients was based on the "equivalent ground plane" theory. This theory was first superseded by one in which account is taken of the finite conductivity of the earth, assumed to be homogeneous. Another assumption, retained from the earlier theory, is that the primary or "disturbing" circuit is straight and of infinite length. Experimental work interpreted by this theory showed that the earth is frequently far from homogeneous and that its resistivity varies widely from place to place, from 5 to over 10,000 meter-ohms in extreme cases, with corresponding variations in coupling coefficients between circuits. This variation is much smaller at close than at wide separations. For example, at a separation of 50 feet, the 60-cycle mutual impedances between parallel circuits are about 0.36 and 0.63 ohm per mile for resistivities of 10 and 1,000 meter-ohms, respectively. At 5,000 feet separation for the same resistivities the figures are about 0.0035 and about 0.1. These relationships are shown in more detail in figure 4, which is taken from a comprehensive report[36] on coupling between ground-return circuits.

Figure 4. Variation of mutual impedance with separation



This theory has since been expanded to allow certain departures from homogeneous earth structure,[37,38,39] including a horizontally stratified earth of two layers with different resistivities. In many situations calculated values based on this assumption have agreed well with experimental results.

While the assumption of an infinitely long, straight primary circuit is satisfactory in most practical cases, there are situations in which its application is accompanied by uncertainty. Examples are where the grounding points of the power and telephone lines are in proximity at one or both ends of the exposure, and where breaks in direction occur in one or both of the circuits. Investigation of these "end-effect" phenomena have led to the development of formulas[38,39,40] in which no restrictions are placed upon the lengths of the circuits or the routes they follow, nor upon the relative locations of their end (grounding) points.

In application to practical cases, these "finite-line" formulas require a great deal more labor than the infinite-line theory. Theoretical and experimental study of the need of better precision than the latter affords has not been completed, but work done to date indicates that results based on this simpler theory need scrutiny, particularly if the earth resistivity is high, in cases of proximity of grounding points, and of breaks of direction in either circuit, toward or away from the other, and not compensated by breaks at other points in the opposite direction. If reasonably accurate knowledge of earth resistivity is available, the effect of proximity of grounding points can be approximated by an easily calculated correction.

In "probe-wire" tests for determining earth resistivity, a number of secondaries (probe wires), parallel to a primary line and at various separations, are used. If, for some of the wires, the separations are too large compared to the length of the primary, errors in the earth resistivity, as inferred from the infinite-line formula, can be avoided by applying relatively simple corrections based upon the finite-line theory.

Earth resistivity depends on the earth structure at the location in question. A compilation of data has been made of earth resistivities in various parts of the country, including correlation of geological formation with resistivity,[41] which will serve as a general guide in preliminary estimates.

SHIELDING

In this discussion, the word "shielding" is used in a restricted sense to mean a reduction in an induced voltage, this reduction being caused by the action of currents brought into existence by the same primary source that causes the induced voltage. The conductors in which these currents exist are called shielding conductors or shields. They may be associated with the primary system (for example, power line ground wires) or with the secondary system, (for example, telephone cable sheaths) or they may be especially placed purely for shielding purposes. Grounded metallic structures such as water or gas pipes also frequently provide shielding. The effect of extensive networks of such pipes, as in cities, is essentially the same as a lowering of the earth resistivity, and is accounted for directly in the coupling coefficients rather than as shielding.

Results of experimental and theoretical work done on shielding by the Joint D. & R. Subcommittee up to about five years ago have been published in an engineering report.[42] Here methods and data are given for calculating shielding from conductors on open-wire telephone or power lines, from the sheaths of aerial or underground power cables, and from the sheaths of aerial or underground telephone cables with or without tape armor. An approximate method for estimating shielding from grounded-core conductors in telephone cables is included.

The information in this report shows, for example, that the shielding from a three-eighth-inch steel ground wire on a 60-cycle transmission line is 10 per cent or less; two high-conductivity aluminum-conductor steel-reinforced ground wires afford a shielding of about 55 per cent. At 60 cycles, shielding by the lead sheath of a full-size telephone cable is of the order of 50 per cent if the sheath is in good contact with the earth. When the sheath is wrapped with tape armor (two 40-mil steel tapes on the larger-size cables), this figure is increased to 80 or 90 per cent. Shielding due to power cables depends upon the construction and may vary anywhere from 40 to 70 per cent. The effects of buried pipes are usually difficult to estimate unless the pipes are welded, because of the unknown resistance of pipe joints.

Existing methods for calculating shielding take account where necessary of distributed admittance between shielding conductors and ground. More recently, consideration has been given to the effects of currents in the disturbed conductors themselves. In long telephone cables, such currents are established in part because of the distributed admittances of the core conductors, and in part they result from the use, in normal operation, of equipment connected from conductors to ground at terminals and at intermediate repeater points. The shielding effects* of these currents, particularly those through terminal equipment, are of substantial importance. For example, considering a 50-mile repeater section of full-size cable inductively exposed, the 60-cycle voltage between conductors and ground at the terminals, where the sheath is grounded, may be only some 5 to 15 per cent of the open-circuit longitudinal voltage, that is, the induced voltage with the effects of all shielding currents, both in the sheath and in the core, eliminated. At points away from terminals, considerably larger voltages may exist between conductors and sheath and more particularly between conductors and ground, when as a result of two-directional power current feeds, shielding effects may be partially cancelled. In construction and maintenance work, a part or all of the core conductors may be open, eliminating or reducing shielding currents in them. In this case, voltages across breaks in conductors are important, as well as voltages to sheath or ground.

The usual methods for calculating reductions of longitudinal induced voltage by shielding assume in effect that in any measurements made, for example to verify the calculations, the potential of a distant ground is taken as reference. However, in cases of very close association between shielding and shielded conductors, of which cables are the best example, the voltage between these conductors (core and sheath, in a cable) is frequently of as much interest as the voltage between some point on the core and a distant point.

Questions of the character of those just discussed are on the current program of the Joint D. & R. Subcommittee. Another matter which is receiving attention is shielding by communication conductors grounded through the operation of protectors. This involves rather difficult calculations, with results liable to considerable error.

TESTING METHODS

Even with the advances already described in the theory of coupling between ground-return circuits, tests are generally necessary for important situations. Considerable attention has therefore been devoted to the possibilities of simplified test procedures. When lines are available for test, coupling can usually be determined most satisfactorily by a direct measurement of induction at the fundamental frequency, or by measuring the induction into short temporary ground-return circuits (probe wires). For situations in which such measurements cannot easily be made, development work on a d-c method of determining earth resistivity, using simple, readily portable apparatus has had promising preliminary results. It is expected that a report covering this and other methods will be available before long.

The development of vacuum-tube voltmeters of high sensitivity has been of value in connection with coupling measurements where one circuit is energized with a small current and the voltage induced in paralleling exploring wires is to be measured.

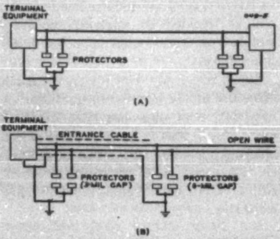


Figure 5. Diagrams to illustrate application of telephone open-space protectors

Susceptiveness

As pointed out in the Conwell-Warren paper,[1] the effects of low-frequency induction upon a communication circuit depend upon the magnitude and duration of the induction. They include service interruption due to protector operation, damage to plant, the possibility of acoustic or electric[43,44] shock, false signaling, and telegraph-signal distortion. Some of these effects, such as interference with signaling, may occur at induced voltages below the protector breakdown value. However, the more serious effects from low-frequency induction are usually associated with induced voltages sufficient to operate protectors.

An interruption to service results from permanent grounding of protectors, which occurs if the discharge current through the protector is not promptly extinguished, or if this current is very high. Protectors are employed as required for the protection of persons, equipment, or property, as at central offices, stations, etc. A simple diagram to illustrate the application is given in figure 5*A*. Where the circuit is in open wire and entrance cable is involved, an additional set of protectors is placed at the junction of the open wire and the entrance cable, as shown in figure 5*B*, to protect the cable. The protectors designed for location near terminal equipment usually consist of carbon blocks with a separation of three mils between blocks. Protectors used at the end of the cable are similar carbon blocks with about six-mil spacing. Use is also made occasionally for special purposes of a similar protector with ten-mil spacing.

The behavior of a protector of this type, particularly where the spacing is as close as three mils, depends upon a number of factors. Purely for illustrative purposes, figure 6 has been prepared from laboratory test data taken at 60 cycles. It shows the breakdown voltage distribution for a sample run of new three-mil blocks under certain specific conditions as to duration of test voltage and impedance of the test circuit. These factors, and also the number of operations to which the blocks have previously been subjected, affect the breakdown voltage characteristic as well as the ability of the protector blocks to withstand discharges without becoming short-circuited.

When induced voltages sufficiently high to break down the protectors are impressed upon the gaps on the two sides of the line to ground, there is a certain amount of dissymmetry in the resulting arc voltages across these two gaps. This sets up current through the receiver which may cause acoustic shock to the person using the receiver.

Voltages which might affect telephone linemen in the course of their work are voltages from wire to nearby grounded objects, and voltages between one wire subject to induction and another wire grounded at nearby or distant points.

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* From a different point of view, these effects are merely the voltage drop in the disturbed conductors, resulting, of course, from the currents set up in them by the disturbing field. This voltage drop is a self-impedance drop and the shielding is "self-shielding". When, as in the earlier discussion, the current producing the shielding is in a conductor (for example, the cable sheath) which is not itself one of the disturbed conductors, the shielding is also a voltage drop, but it is due to the *mutual* impedance between the shielding conductor and the disturbed conductors.

The reactions upon the subject of this paper of progress in the electrical communication art in the past decade are

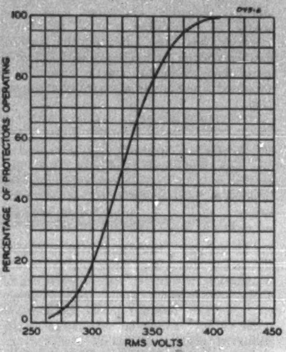

Figure 6. Breakdown characteristics of telephone open-space protectors—laboratory tests at 60 cycles

less direct than those of advances in the power industry, reviewed above. On the other hand, in the development of specific remedial devices for low-frequency induction situations, much has been done with regard to measures for use in communication systems. The effects of trends in the communication art and developments in communication system remedial measures will be discussed in succession.

New Telephone Carrier Systems

The principal developments in wire telephone and telegraph transmission during the past few years have been toward increased use of carrier circuits.

With the availability of a system which permits the use of wider frequency bands and correspondingly more carrier channels on open-wire lines,[45] some open-wire routes can be economically developed for larger numbers of circuits instead of being replaced by cable.

In existing toll cables, the application of carrier transmission makes increases possible in the number of channels per sheath,[46] which is bringing about a tendency toward smaller physical dimensions for new toll cables. This in turn means that the shielding against low-frequency longitudinal induction would tend to be diminished unless counteracted by deliberate increases in the sheath dimensions. Shielding effects exerted on each other by cables in close proximity, which depend essentially on the total conductance of all the sheaths, would be reduced by a reduction in either the size or the number of cables.

While these developments in carrier telephony, of which the coaxial cable[47] is the most recent, present new features, principally in connection with shielding, they do not change the nature of the technical problem in an essential way. An increase in the number of communication channels per physical pair of wires, and in the number of channels along a given route, of course emphasizes the desirability of protecting the system from disturbance. However, low-frequency induction, in this respect, does not stand on a different footing from other possible sources of disturbance, aside from limitations which may be encountered in the application of specific remedial measures, such as relay protectors, to the telephone line.

Signaling

Increasing use is being made of recently developed d-c signaling systems, composited or simplexed in a manner similar to superposed telegraph, for through supervision and dialing on both local and toll trunks. These systems are provided with terminal equipment for neutralizing longitudinally induced voltage or ground potential, the functioning of which requires that it be connected to a conductor exposed to the voltage to be neutralized. For this purpose, one wire of the pair, or in the case of a phantom group, one wire of the group, is used. This normally leaves one wire for each trunk for dialing or supervision. While this arrangement reduces the susceptiveness of the system, there have been some cases in which the neutralization was apparently insufficient to take care of low-frequency induction under normal operating power-circuit conditions. This matter is under investigation.

Induction from power-system voltages under normal operating conditions sufficient to disturb grounded party-line ringers has also been experienced in some cases of exposure to rural power circuits consisting of a single phase-wire and multigrounded neutral. A ringing system has been developed, and is in some use, employing cold-cathode gas-discharge tubes to keep the circuit free of ringing grounds except when the tubes are operated, as by ringing voltage. In its present form, however, this arrangement would frequently be ineffective against induction from rural-line exposures, of the type just mentioned, since the tube operating voltages must be held within magnitudes adapted to satisfactory operation of ringing systems.

Considerable work has recently been done and is continuing on the general question of the susceptiveness of telephone equipment, particularly equipment for signaling and supervisory purposes, to low-frequency induction.

Telephone Protectors

Development work is in progress in connection with the protectors used at stations and at junctions of open-wire and cable, particularly study of the possibilities of material having a nonlinear resistance-voltage characteristic. While some of these materials appear to be valuable for special purposes, the studies have not thus far resulted in any device that seems likely to replace the present protector for general application. As has been noted in a recent paper,[48] a 3,000-volt carbon electrode gap has been developed for use as a specific measure in cases of joint use at the higher voltages.

Relay Protectors

Relay protectors installed at terminals or at the junctions of open-wire lines and cables greatly reduce the probability of permanent protector grounding, and also minimize acoustic disturbance in the communication circuits. In addition, relay protectors installed along the line minimize the possibility of electric shock to linemen by reducing potential to ground throughout the entire line. The operation of relay protectors of course results in an interruption to the communication circuits for the duration of the disturbance.

The multigrounding type of relay protector operates so as to ground all wires of a telephone line at the point where the apparatus is installed when the voltage to ground at that point rises sufficiently to operate any one of the open-space protectors associated with the installation. The discharge of current through any one or more of the open-space protectors operates a master relay, which operates the relays associated with each protector. These individual relays each short-circuit the associated protector gap and prevent permanent grounding. The improvements over the earlier equipment[49] have been (*a*) substitution of an a-c master relay, in series with a saturating transformer, for the earlier combination of rectifier and d-c relay; (*b*) speeding up of operation, which is now completed in about 0.025 second; (*c*) increase in current-carrying capacity of the short-circuiting relay contacts; (*d*) increase in minimum release current (master relay); (*e*) increase in capacity of master relay to absorb heat without damage. For relatively rare situations, in which the master relay may be held up by normal induction after the power-line fault causing its operation has cleared, means may be provided for opening all the short-circuiting relay contacts after a suitable interval, these contacts reclosing quickly if the fault condition should prove not to have cleared.

In addition to this "multigrounding" type of relay protector, a "unit type", applicable to a single pair of wires only, has been developed. It is used principally at points where protectors of the ordinary type would be subject to frequent grounding and where, because the location is remote, maintenance would be delayed or expensive.

Neutralizing Transformers

Neutralizing transformers installed along a communication line provide counter electromotive forces against induced longitudinal voltages, and permit the line to operate normally through inductive disturbances. A type of transformer for neutralization on voice-frequency toll circuits has been developed and has proved satisfactory in a service trial, where 12 were used in series in two inductive exposures totaling about 50 miles in length. Each transformer is wound with 24 secondary wires. The primary, consisting of two telephone wires, is grounded at each end of the exposure and between each two consecutive transformers. This trial installation proved satisfactory both in connection with neutralizing induced voltages and in meeting the circuit transmission requirements under normal operation. Additional installations have not been made as other means have offered more economical over-all solutions of specific cases. Further development work would be required to produce a transformer adequate for circuits over which carrier frequencies are transmitted.

A unit-type neutralizing transformer, to take care of a single circuit, has also been developed. This device consists of a single primary winding with two secondary wires. Where more than a single circuit is involved, a number of units can be used with the primaries in parallel. An important application of this device is in connection with maintaining uninterrupted communication service to power stations or substations, at times of power disturbances which cause large rises in the potentials of station grounds with respect to distant points.[50] Two sizes are in use, one for 4,000 and one for 2,000 volts (60 cycles). Frequently use can be made of a cable sheath for the primary or part of it. These transformers are now in operation in a number of places.

Acoustic Disturbances

The copper-oxide rectifying element[51] has superseded the earlier combination of step-up transformer and protector gap as a means of reducing acoustic disturbances to telephone operators. Tests and operating experience have shown that the copper-oxide element is about as effective as a reducer, and has no net effective transmission loss. It is smaller and lighter than the earlier type and the indications are that it will have a satisfactory service life in use at central offices.

In circuits with telephone repeaters, the nonlinear characteristics of the vacuum tubes cause the tubes to function as devices for limiting acoustic disturbances. A study of the quantitative relationships has been made, including determination of the factors to be taken into account in order to realize as fully as practicable the power-limiting possibilities.[52]

Conclusion

The foregoing review will have shown, it is believed, that the net result of development of the past ten years has been favorable to low-frequency inductive co-ordination. Over-all improvement has in fact occurred, and has accompanied a growth in the power and telephone industries which, though less than expected ten years ago, has not been inconsiderable. It is estimated that in the decade increases in the neighborhood of 30 per cent have taken place in power generating capacity and in mileage of transmission circuit in the range from 22 to 66 kv. In the Bell System, telephone toll circuit mileage has increased some 22 per cent and telephone stations about 7 per cent.

In an important degree, the contribution of the Joint D. & R. Subcommittee to this favorable result has been due to the assistance it has received from operating power and telephone companies. This helpful relationship will, it is hoped, continue in the working out of the problems that remain and the new ones to be expected from future growth and development.

Summary

We recapitulate here principal developments of the past decade in the problem of low-frequency induction, with remarks on the resulting present status and future plans for study of different phases of the problem.

General

1. Studies of effects in actual low-frequency inductive exposures have been useful in developing general procedural methods, but have indicated the need of further work on certain items, with reference particularly to facilitating the appraisal of proposed situations in advance of construction.

Influence

2. In the calculation of residual currents, the more important gaps have been filled by the experimental determinations of the impedance of ground-faults on operating systems, and of the impedances and admittances of lines (especially cables).

3. In residual-current limitation, probably the most interesting development is the installation, within the last three years, of about 30 Petersen coils. The Petersen coil generally limits residual currents to small values. Little or no change in the status of other methods of fault-current limitation has taken place. As heretofore, the application of fault-current limitation is primarily a matter of its reaction on power-system operation, although developments have somewhat simplified this question.

4. Frequency of occurrence of ground faults has been reduced by improvements in the insulation of power lines, including improved ground-wire application and features of structural design. In the reduction of duration of ground faults, the protector tube is probably the most important development from the standpoint of low-frequency induction. Others are increased speed of circuit-breaker operation, including high-speed relaying and closely simultaneous clearances at both ends of the faulted line section, and (except for permanent faults) the Petersen coil.

These developments have had a definitely favorable reaction on low-frequency inductive co-ordination, although it is impracticable to ascertain the extent of this, taking the country as a whole.

Coupling and Shielding

5. The theory of the mutual impedances of ground-return circuits has been extended to cover circuits of arbitrary lengths and contours, and now includes also certain types of heterogeneous earth structure. Some further work is desirable in simplifying the engineering application of the theory to types of problems which may be encountered.

6. Coupling tests should be made before final decision in important cases. Development work has cheapened and simplified field testing methods and apparatus, and is continuing.

7. Methods and data for calculating reductions in longitudinally induced voltages resulting from shielding currents in usual types of shielding structures have been published. In the case of telephone cables, the voltage between conductors and sheath

(as contrasted with longitudinally induced voltage) is of interest, and in long cables, shielding by currents in the conductors as well as by those in the sheath is important. These matters are currently receiving attention.

8. Shielding by currents in conductors which become grounded through protector operation is difficult to estimate, although it has frequently been found to be of considerable importance. Additional investigations of this factor are to be carried out.

Susceptiveness

9. Possibilities of using toll cables of relatively small physical dimensions, and of new structural designs, presented by developments in the application of carrier to cables, require consideration of changes in the inherent shielding afforded by the cable structure. These and allied questions may continue to require study for some time.

10. The use of composited d-c toll-line dialing and through-supervision systems is increasing. Inclusion in these systems of arrangements for neutralizing induced voltages reduces their susceptiveness to low-frequency induction, although further study of inductive co-ordination with these and other signaling systems is needed.

11. Development work on the relay protector has been substantially completed. This device has had a good record in use as a specific measure of co-ordination.

12. Two types of neutralizing transformers have been developed. One, for neutralizing ground potentials on communication lines serving power stations, is being successfully used in practice. The other, designed for toll-line use, proved satisfactory in field trial, but the probable field of use is small.

13. A copper-oxide device for reducing acoustic disturbances has been developed for use at operators' positions. Experience indicates that it is generally satisfactory.

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Discussion

L. F. Roehmann (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): The authors gave an excellent review of the progress in the art of low-frequency inductive co-ordination which has been realized in this country during the past decade. It might be appropriate to add that during that interval substantial work has been done on the other side of the Atlantic also.

In 1926 and 1930, the CCIF issued "Guiding Principles Concerning the Means for Protecting Telephone Lines Against the Adverse Effects of High-Current or High-Voltage Lines." Last year, an entirely new edition has been published ("Guiding Principles Concerning the Protection of Telecommunication Lines Against the Adverse Effects of Industrial Power Lines"). This publication, containing 150-odd pages, is an up-to-date textbook on inductive coordination rather than just a set of recommendations. It climaxes the co-operative efforts of the International Mixed Commission for the Study of Inductive Interference, the CCIF, and a number of other international groups and organizations. Power and communication engineers of France, England, Germany, Switzerland, Sweden, and many other countries contributed to its setting-up.

The publication is written in French but an English translation has been prepared. While many of the minor details are different in the two hemispheres, the basic conceptions are of course the same.

J. W. Milnor (The Western Union Telegraph Company, New York, N. Y.): This paper discloses a very healthy condition in the development of low-frequency inductive co-ordination. A comparison with that presented in 1931 by Messrs. Conwell and Warren makes it evident that the past ten years have produced few actual changes in the basic method of attack; instead there has been a continuous advance in the knowledge of the problem, of the mathematics which represents the variable conditions, and of the apparatus and equipment necessary to provide the desired safety and stability of service.

The scope of the discussion is so broad that within the necessary space limitations the authors have been able to describe only the most prominent advances. Fortunately there is included a bibliography which in itself is an index of the extent of the progress, and which should be of much value to the reader who desires more intimate knowledge of any phase of the problem.

The authors have dealt chiefly with the transient low-frequency interference which occurs particularly at times of fault conditions in the power system. They have perhaps underemphasized the continuous interference which may occur during normal power operation, and which often has serious effects especially to high-speed telegraph circuits, although it is rarely damaging to the telephone.

J. O'R. Coleman: Mr. Trueblood and I greatly appreciate L. J. Roehmann's remarks regarding the report of the Comite Consultatif International Telephonique. The English edition of the report of the 11th Plenary Meeting held in Copenhagen, was published in 1938 by the International Standard Electric Corporation, and contains considerable interesting information. Due to the many different conditions existing in European practice, technical, economic, and social, we do not feel that in general the work of the CCIF can be directly applied to American practice, although many of the technical findings are useful.

A Precision Rotating Standard for the Measurement of Kilowatt-Hours

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THE accuracy with which electrical energy is metered is dependent upon the apparent measured value of the kilowatt-hour used. Quite independent of the electrical operating characteristics of the watt-hour meters used or the accuracy limits within which they are adjusted, the average accuracy of a group of watt-hour meters is directly affected by the difference between the value of the kilowatt-hour used and the true value.

The apparatus used to derive the unit while satisfactory for reference does not lend itself to practical use in the checking and calibrating of watt-hour meters in commercial installations. Specially designed and compensated induction-type watt-hour meters, which are calibrated in the laboratory approximately as outlined in table I, have long been used as commercial working standards. These watt-hour meters are known as portable standard watt-hour meters and have been developed to a high degree. In fact, so well do they perform that an improvement in the method of standardizing them is justified.

It is the purpose of this paper to describe an improved system for maintaining the value of the kilowatt-hour to be used primarily for the calibrating and checking of portable standard watt-hour meters.

Analysis of Errors in Method of Checking Rotating Standards

Table I shows the limits of probable error present in the various steps of a particular system. It is evident that all of the values given in table I could add up to be (+) or could add up to be (−) if a sufficiently large number of checks were taken. The probability of either of these two conditions occurring is much less than is the probability of some steps compensating others due to differences in the sign of the errors. Should all errors add (+) the result would be a +0.245 per cent variation, and if all (−), a −0.245 per cent error. It is important, therefore, to obtain some idea of the most likely way the error of the various steps will combine to determine what over-all accuracy is to be expected.

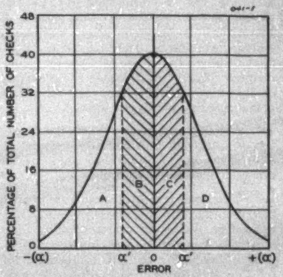


Figure 1. Absolute error in per cent

Paper 40-41, recommended by the AIEE committee on instruments and measurements, and presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940. Manuscript submitted November 13, 1939; made available for preprinting January 3, 1940; released for final publication March 5, 1940.

J. H. Goss is engineer in charge, works laboratory, and A. Hansen, Jr., is engineer, General Electric Company, Lynn, Mass.

Consider a theoretical distribution curve, figure 1, and for the purpose of illustration let the abscissa represent a system of errors similar to table I. To avoid confusion the error limits have been designated $\pm\alpha$. If a large number of checks are made, figure 1 will represent a plot of the results. The tests showing zero error will be the most frequent, but the extreme errors will occur in a small percentage of the readings when the component errors have the same sign.

The technician must realize that with a small number of readings the average value may not be a fair one. If it should occur that in five readings a high (+) error is obtained with four smaller (+) errors, the average would be (+) and would depart appreciably from the absolute value. It is important that a sufficiently large number of readings be taken to show up the individual readings with large variations from the mean.

As the total area under the curve, figure 1, represents all of the readings, half of the area will represent half the total number of readings. The areas A, B, C, and D have been divided to make each equal to one-fourth the total area $(B+C=A+D)$.

The abscissa of B is $-\alpha'$ and of C is $+\alpha'$. The combined areas $B+C$ represent half the total number of readings, and it is, therefore, reasonable to state that one-half of the readings taken are probably within the area $B+C$ and are therefore, within $\pm\alpha'$. This value of the abscissa $(\pm\alpha')$ will be referred to as the probable error.

Precision Rotating Standards Method

The schematic diagram shown in figure 2 gives the circuit and essential elements of a system of three precision rotating standards. This system is capable of maintaining a high degree of accuracy. It will first be described in its component parts and then its method of operation, together with actual performance data, will be given.

A. ROTATING STANDARDS

The rotating standards shown in figure 3 are used as the measuring elements. These were made by taking a commercial portable standard watt-hour meter, removing the register, and rewinding the current coils for a five-ampere current rating only.

The standards were removed from their cases and mounted as shown in figure 4 in a walnut case. Incandescent-lamp housings and lens systems were mounted above the meters and photoelectric cells in suitable housings were mounted below the case. Glass windows were provided in the top and bottom of the case to allow the light from the light sources to be focused through a small hole in the meter disk onto the photoelectric cell below. The case was completely sealed against dust and mounted on a vibration-free concrete wall in a (22 degrees centigrade) controlled-temperature room. Each rotating standard was carefully compensated for both unity and 0.5 power-factor lagging temperature errors (class I and class II errors).[2]

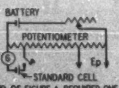
Each rotating standard, together with its light source and photoelectric cell, is considered as a unit having no functional relation with the other two. For the purpose of identification these will be called precision rotating standards numbers 1, 2, and 3.

B. TRANSFORMERS

The standards were designed for 120 volts and five amperes, but a high-

Table I

Step	Fundamental Measuring Circuit and Type of Measurement	Source of Error	Limit of Error	
A—Potentiometric method of maintaining voltage and current from a standard cell using a voltage multiplier and shunt	Figure *A*—Potentiometric method of maintaining a voltage Figure *B*—Voltage multiplier method of maintaining a standardized test voltage Figure *C*—Shunt method of maintaining a standardized test current	1—Error in the standard cell	±0.01	
		2—Error in balancing cell	±0.01	
		3—Error in resistance arms	±0.01	
		4—Error in multiplier	±0.01	
		Total maximum error for sources 1, 2, 3, 4		±0.04
		5—Error in shunt	±0.01	
		Total maximum error for sources 1, 2, 3, 5		±0.04
		Total maximum error		±0.80
B—Calibration of dynamometer wattmeter on direct current	Figure *D*—Potentiometric method of calibrating a dynamometer wattmeter on direct current	1—Holding deflection of wattmeter at scale point	±0.02	
		2—Holding voltage	±0.01	
		3—Error in voltage value step *A*	±0.04	
		4—Error in current value step *A*	±0.04	
		Total maximum error		±0.11
C—Calibration of induction-type watt-hour meter	Figure *E*—D-c to a-c dynamometer wattmeter transfer	1—Holding wattmeter setting (load varying)	±0.04	
		2—Error in checking wattmeter step *B*	±0.11	
		3—Change in wattmeter calibration from check value	±0.03	
	Figure *F*—Time element of power equation $W = \frac{Kh \times N \times 3,600}{t}$ W = meter watts Kh = meter disk constant N = revolutions = time (seconds)	4—Punching key (human error)	±0.05	
		5—Error in measuring chronograph sheet	±0.01	
		6—Error in timing mechanism	±0.005	
		Total maximum error		±0.245



TWO OF FIGURE A REQUIRED, ONE FOR VOLTAGE MULTIPLIER SOURCE (Ep_1) AND ONE FOR CURRENT SHUNT BALANCING VOLTAGE (Ep_2)

FIGURE A

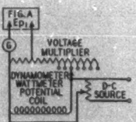


FIGURE B

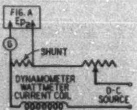


FIGURE C

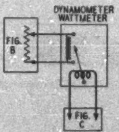


FIGURE D

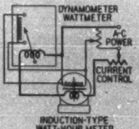


FIGURE E

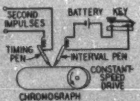
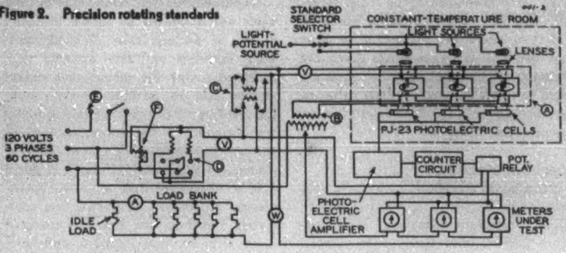


FIGURE F

Figure 2. Precision rotating standards



A—Three special *IB* standards mounted in dust-tight case, special 0.060-inch hole in disks for light shutter

B—Current transformers, ratios 1:0.2, 0.25, 1, 2, 2.5, 10; tap chosen by jumper

C—Potential transformer, 2:1, connected by jumpers

D—Potential transformer, 1:2, connected by 100-ampere switch (for low contact resistance)

E—100-ampere switch for phase selection

F—Voltage regulator

accuracy current transformer having a Mumetal core is used to allow the standards to operate at five amperes under all test conditions. The ratios available are shown under *B* in figure 2 and allow for all points required for the commercial checking of portable rotating standards. The current transformer has a ratio error within 0.04 per cent on all coils and a phase angle error within one minute on all coils.

A potential transformer is provided for 240-volt operation.

Not only are the transformers designed for low inherent errors, but they are considered as an integral part of the precision rotating standard circuits and all calibrations made in accordance with table I are made on the combination. This method of calibration was employed using instruments for all current ratings to check the meter and transformer combination. Although the ratio and phase-angle errors were determined for the transformers, they were not used in arriving at the calibration of the standards.

C. Photoelectric-Cell Testing Relay Circuit and Auxiliary Switching

A photoelectric-cell amplifier, figure 2, is provided which operates a counter circuit which can be preset to energize through the potential relay the potential circuit of one or more portable standard watt-hour meters and to keep the potential circuit or circuits energized for the preset number of revolutions of the precision rotating standard.

By means of a suitable switch, precision rotating standard number 1, number 2, or number 3 may be used to control the portable standard watt-hour meters under test. The repeatability of the relays and photoelectric circuit is in the order of ±0.01 per cent for ten revolutions. The precision rotating standard being used runs at the selected load continuously and only the standard under test is started and stopped.

Method of Use and Results

Each of the three precision rotating standards is operated continuously when not being used for test work at two per cent of rated current and with rated potential applied. This eliminates any error due to potential-coil heating.

The error in the calibration of each of the precision rotating standards is determined by repeated checks in accordance with the steps shown in table I. At no time since the beginning has any change been made in the adjustments of any of the three standards. It is then desirable to cross-check the precision rotating standards in terms of one another using a portable standard watt-hour meter as the transfer medium. If the standard used for the transfer has a known history of calibration, it is valuable as a reference to show up any major changes in absolute values of any of the precision rotating standards.

It would be desirable not to introduce the fourth standard for the purpose of intercomparison. It is possible to use one of the three precision rotating standards for this purpose, and this will eventually be done. The results of using a transfer have been quite satisfactory to date.

Figure 5 shows the results of such an intercomparison over a period of five months for the five-ampere 120-volt load point operating at unity power factor. Precision rotating standard number 1 is assumed to be correct and number 2 and number 3 are plotted in terms of their deviation from number 1.

The maximum variation in number 2 from the original reading is +0.06 per cent and of number 3, −0.06 per cent over the five months' period. Note that at the end of the period number 2 is +0.03 per cent above the original reading and number 3 is −0.01 per cent below its original reading.

Figure 6 shows the same intercomparison as figure 5 except that the load point used was 120 volts, five amperes, and power factor 0.5 lagging. It is to be expected that a greater variation will be obtained using this load due to other variables operating. For example, variation in phase angle, wave form, frequency, and temperature are more effective in

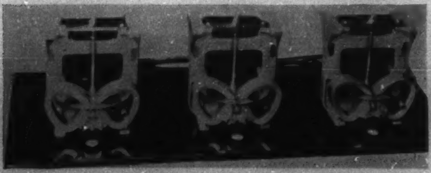


Figure 3. Precision rotating standards without case

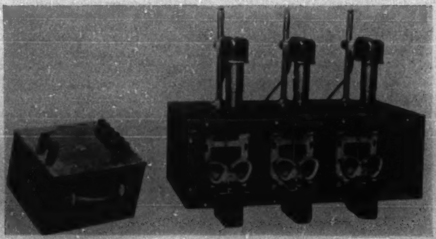
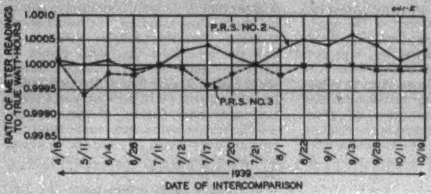


Figure 4. Precision rotating standards complete with light sources, photoelectric tubes, and precision current transformer adjacent



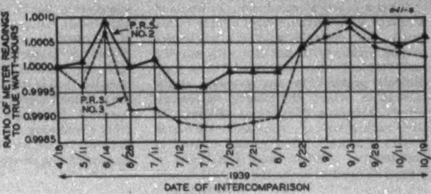


Figure 5. Intercomparison data on precision rotating standards corrected in terms of standard number 1

Five amperes, 120 volts, 60 cycles, unity power factor

producing calibration change than at unity power factor. The maximum variation of precision rotating standard number 2 is +0.09 per cent and of precision rotating standard number 3, −0.12 per cent. At the end of the period, number 2 is +0.06 per cent above the original reading and number 3 is +0.02 per cent above the original reading. No change in the adjustments were made at any time after the initial readings were taken.

Figure 7 shows the results of 36 separate checks made over the five months' period in accordance with table I. The most probable error in absolute calibration is ±0.04 per cent from the mean. The initial instrument check was assumed to be correct for the purpose of plotting the data in figure 7. It is evident that the initial point was low by −0.06 per cent and a better value of the absolute calibration is the peak ordinate or +0.06 per cent above the initial reading. The distribution curve was drawn by first determining the mean and standard deviation for the results of the 36 checks and calculating the ideal curve to fit the data.

Certainly, these standards are running together within far closer limits than any single instrument check can be relied upon to reveal. This is, however, the ideal condition for obtaining the true mean value in accordance with table I. If the precision rotating standards remain constant and the interchecks reveal that they are, then their calibration will approach the value of the maximum ordinate in figure 7. As all these standards were intercompared, figure 7 represents the instrument check on each of the standards.

Should some factor influence all of the standards to drift at exactly the same rate, the intercheck would not reveal the drift. As continual periodic instrument checks are obtained, curves similar to figure 7 can be plotted and compared one with another to show up this drift.

The actual use of this method for checking portable standard watt-hour meters is evident. Each precision rotating standard has a definitely established calibration so that one revolution of the disk under a given load corresponds to a definite kilowatt-hour value. By placing the portable standard watt-hour meters to be checked as shown in figure 2, any one of the three precision rotating standards may be selected to deliver a given number of kilowatt-hours to them. The ratio of the reading of the individual portable standard to the known value of the precision rotating standard is a measure of its accuracy.

The average of the calibration obtained using each of the three precision standards in turn to control the portable standard watt-hour meter or meters under test will give the highest precision. In practice, however, the additional accuracy is not justified because of the additional time required.

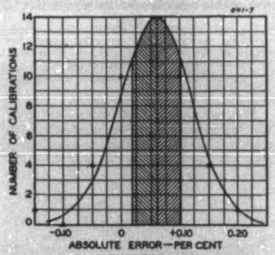


Figure 7. Curve of distribution of absolute error for repeated checks of precision rotating standards

Five amperes, 120 volts, 60 cycles, unity power factor

The high accuracy of the photoelectric testing system makes it necessary to take runs of no longer than 60 seconds for any given point. This greatly speeds up the calibration check of the portable standard watt-hour meters.

Figure 6. Intercomparison data on precision rotating standards corrected in terms of standard number 1

Five amperes, 120 volts, 60 cycles, 0.5 power factor lagging

Conclusions

1. It has been demonstrated that for the highest accuracy of calibration of portable standard watt-hour meters using instruments, a number of readings must be taken and properly weighted.

2. A new system of precision rotating standards has been described that has high inherent accuracy together with fast operation.

3. With care it is entirely practical by the system described to insure the absolute accuracy of the kilowatt-hour used in checking standards to within 0.06 per cent at unity power factor loads and 0.12 per cent at 0.5-power-factor lagging loads.

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3. DEVELOPMENT OF A MODERN WATT-HOUR METER, I. F. Kinnard and H. E. Trekell. ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), January 1937.

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Discussion

George B. Schleicher (Philadelphia Electric Company, Philadelphia, Pa.): The authors are to be congratulated upon their contribution to the important field of maintaining the accuracy of laboratory-standard watt-hour meters. The elimination of "start-and-stop" errors in the so-called master standard, by causing it to rotate continuously during the calibration, serves also to eliminate such errors from the working standards when their corrections are determined by tests *over the same intervals that are used in service.*

From a practical standpoint the limits of accuracy as covered in table I appear rather small. For example, holding a wattmeter setting within 4/100 per cent with varying load (table I, step *C*-1) would be a very difficult problem in the average utility laboratory where the source of energy supply is from a distribution system. In the calibration of standard watt-hour meters with wattmeters, manual control to maintain a constant load introduces a human element of error, and there is a need for an *automatic* means of obtaining *constant wattage* independent of voltage variation. We have succeeded in accomplishing this, at least experimentally, by electronic means.

In the standard described by the authors, bearing friction is reduced by operating the standard only at full-load speeds, but possibly further improvements can be made in this direction. While the magnetic suspension is not new, modern magnetic materials, as for example Alnico, have made possible the design and construction of practical meter bearings of this type. This construction may prove highly desirable for service meters. Figure 1 of this discussion shows such a bearing developed by Walter C. Wagner of the Philadelphia Electric Company, and constructed of magnet materials contributed at our request by the General Electric Company. In the figure the air gap indicated by the arrow shows a greatly enlarged view of the bearing. Meter-performance tests over extended periods show consistent results superior to those of pivot and jewel bearing construction particularly at light load, thus permitting accurate performance to be maintained in standards and meters at lower operating speeds than with the conventional types of bearings. This type of suspension would undoubtedly further improve the uniformity of performance of the precision standards described by the authors.

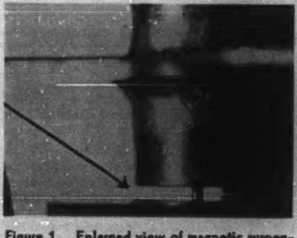

Figure 1. Enlarged view of magnetic suspension on watt-hour meter

The arrow points to the air gap which represents the bearing surfaces

W. J. Seeley (Duke University, Durham, N. C.): I should like to ask a question which is old but the philosophy of which is quite intriguing. It may have been answered to the satisfaction of manufacturers long ago, but not enough information has been given in print to put at rest a question which comes up with fair regularity. It is conceivable that by proper construction and adjustment the greater part of the weight of the rotor on the lower jewel may be removed by magnetic attraction, thus considerably reducing the friction loss. The question then is this: In an effort to reduce the rotation losses of your precision rotating standard, had you considered the possibility of reducing the weight of the rotor on the jewel by the use of a permanent magnet suspended just above the upper end of the shaft?

Wm. M. Young (Ohio University, Athens): Of particular interest in this paper is table I giving assigned values to the various sources of error encountered in standardizing and calibrating rotating standard watt-hour meters. It is assumed that all measurements were made using sinusoidal waves of current and voltage and that the authors were not concerned with irregular wave forms.

The question naturally arises as to whether or not the errors in the instrument check are inherent and whether they cannot be minimized by any standardizing laboratory if the equipment is properly aged and maintained at a constant temperature and if repeated trials are made and the results plotted on a probability curve. Naturally any laboratory can, in this way, reduce the 0.11 per cent accumulated error in the wattmeter's calibration by using proper care.

Might I ask the authors about the 0.03 per cent error assigned in *C*-3 "change in wattmeter from check value". Are there any data available on the accuracy of the determination of this error? Naturally it can be partially reduced if the time interval between checking the meter and using the meter for a watt-hour meter calibration is reduced to a minimum.

It would seem that the greatest contribution of this paper lies in the substitution of

1. A machine-switching machine for a hand-switching machine. This naturally affects the magnitude of errors *C*-4, *C*-5, and *C*-6. They total, as indicated by the table, 0.065 per cent.
2. Multiple standards cross-checked against each other in place of human observation for *C*-1.

In figure 5 might I ask about the drift of the readings in rotating standards with time? It would seem that an aging effect might be here showing up and that the meters would eventually tend to stabilize at some constant error.

In figure 7 might I ask why the point at 0.05 per cent error was not used in the determination of the probability curve? It would seem that more data should be taken before a good probability curve can be drawn and that results should not rely on merely 36 readings.

A. E. Vivell (Princeton University, Princeton, N. J.): It is gratifying to see that the actual title of this paper describes the meter as a "precision" meter and not as a "primary" standard as announced in the advance notices. It would be unfortunate to handicap such an excellent and interesting development with a misnomer.

The calibration procedure described is most thorough and provides a very comforting check on the constancy of these meters. However, the figures in table I seem to show a probable error of about 0.08 per cent in the watt-hour used in checking the rotating standard. It would seem, therefore, that the probable variation of 0.04 per cent from the mean shown in figure 7 is somewhat low. An exceedingly slight change in only four of the calibration points (at 0 and +0.10 per cent) could make the curve much flatter. If the point at +0.05 per cent were taken into account the probable error would be increased to about 0.08 per cent and the theory of probability restored to favor.

This paper again brings home the fact that the measurement of a watt-hour is handicapped by the lack of a simple scheme of measurement. When the meter error becomes less than the error of the standard it is time to work for better standards. It would be interesting to know if the authors have considered trying to improve their watt-hour by means of some other scheme of calibration; for example, a calorimeter or some other energy device.

A. Hansen, Jr.: An analysis of the discussions of our paper reveals four general points of comment: first, the limits of errors shown in table I; second, the manner in which our data were handled and the derivation of the probability curve shown in figure 7; third, bearing considerations; and fourth, the apparent better accuracy of the watt-hour meter than the calibrating standards.

The values shown in table I represent the results obtainable with extremely careful control of each step starting with the standard voltaic cell. Special sources of power of as constant regulation as possible are used without the use of electronic regulators. The authors feel that the values shown are representative for the method, and are the result of a number of years of experience.

An automatic device for holding load might be applied to some advantage in the system described. Up to the present time, however, the authors have had no experience with such a device.

The authors recognize the fact that insufficient data were obtained to fix positively error magnitudes. It was the purpose of the paper to describe a method and a system of arriving at a precise value of the watt-hour.

The probability curve in figure 7 was plotted considering all calibration values and is not sketched in from the grouped data plotted but was calculated from the actual data.

Inasmuch as the precision rotating standards rotate continuously at full-load speed for all check points, the variations due to bearing friction are practically negligible. A magnetic-suspension type bearing is an interesting device in view of the new magnetic materials available.

At the present time no practical way of obtaining the value of the watt-hour is known to the authors that is basically different from the method outlined in table I. Energy-equivalent methods have been experimented with such as the calorimeter. The difficulty in using the calorimeter is not with measuring the heat values, but in determining the correction for heat loss.

In conclusion, the authors wish to express their appreciation for the very excellent criticisms they have received and hope that all questions have been satisfactorily answered.

Lightning Currents in Arresters at Stations

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A CO-OPERATIVE field investigation of the lightning currents discharged by arresters at stations was undertaken in 1936, by the engineering departments of the American Gas and Electric Service Corporation and the General Electric Company. Data have previously been published on the currents discharged by distribution lightning arresters[1,2,3] but little information is available on the duty required of arresters located in stations, where considerable influence on incoming lightning waves may be produced by a large number of circuits connected to one bus, by the capacitance to grounded steel work in the station, by the more or less sheltered location of some stations, and by types of construction commonly used for transmission lines.

The data so far obtained show that there are large variations in the number of arrester discharges from season to season, and that due to local conditions the duty on arresters is far more severe in some locations than others, making it difficult to arrive at a representative average. Hence the investigation is to be continued in order that the effects of these factors may be better determined. Some redistribution of field installations may be made to provide more information on points where the data have been meager.

Scope and Conduct of Investigation

In this investigation the field work was carried on for four years (1936–1939) on four American Gas and Electric Company properties; the Ohio Power Company in Ohio, the Appalachian Electric Power Company in Virginia, the Atlantic City Electric Company in New Jersey, and the Indiana and Michigan Electric Company in Indiana. This diversified territory provided an opportunity for studying lightning conditions as affecting arresters, on properties employing different practices, and with isokeraunic levels varying from 27 to 45 thunderstorm days per year.

Figure 1. Wooden bracket supporting two surge-crest-ammeter links on lead to lightning arrester

Measurements of arrester discharge currents were made with surge-crest-ammeter links[4] mounted on each leg of each three-pole arrester, in a straight part of the conductor between the arrester and the line. Typical field installations are shown in figures 1, 2, and 3. Links were serviced approximately every two weeks during the lightning season for four years, and any which indicated that current had passed through the arrester were removed for calibration and replaced with demagnetized links. The magnetized links were checked in the field in some cases, but received a final calibration at the Pittsfield works of the General Electric Company.

The current measuring installations are listed in table I, where they are grouped

Figure 2. Surge-crest-ammeter link installations on all three poles of 33-kv oxide-film lightning arrester

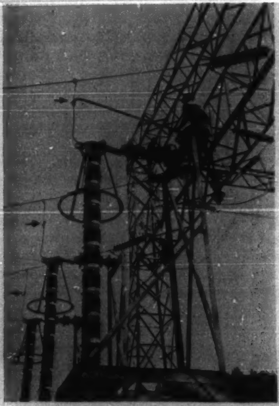
Figure 3. Field servicing of surge-crest-ammeter links on Thyrite lightning arrester on 132-kv energized circuit

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Paper 40-59, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940. Manuscript submitted November 24, 1939; made available for preprinting January 3, 1940; released for final publication February 28, 1940.

I. W. Gross is research engineer, American Gas and Electric Service Corporation, New York, N. Y.; W. A. McMorris is in the lightning-arrester engineering department of the General Electric Company, Pittsfield, Mass.

A large number of people in several organizations have contributed to make an investigation of this type possible and to correlate the data in suitable form for presentation. The authors wish to express their appreciation to the Ohio Power Company, the Appalachian Electric Power Company, the Atlantic City Electric Company, and the Indiana and Michigan Electric Company, where the field work was done; and to those in the Engineering Departments of the General Electric Company and the American Gas and Electric Service Corporation who supervised the work and analyzed the records. Acknowledgment is made to the helpful assistance of George D. Lippert of the American Gas and Electric Service Corporation for correlating the records on which the paper is based, and to Doctor K. B. McEachron and D. D. MacCarthy of the General Electric Company for their assistance in organizing and directing the progress of the investigation.

1. For all numbered references, see list at end of paper.

according to several different classifications. The installations were confined to arresters in stations only, and included arresters connected to lines where they entered the stations, arresters on or immediately adjacent to power transformers, and arresters connected to station busses. The arresters were of several different designs. Approximately 75 per cent were designated by the manufacturer as "station type" and 25 per cent as "line type". All circuits involved operated with grounded neutrals, at line-to-line voltage ratings of 11 to 132 kv.

One hundred three-pole arresters were equipped in 1936 for the measurement of lightning discharge currents. This number was increased each year until 1939, when 166 were under investigation. The total three-pole arrester-years for the four-year period was 580*, making an average of 145 under study each year. On the basis of single-pole arresters, the study included 1,734 arrester-years, or an average of 433.5 each year.

Number of Records Obtained

A total of 459 records of lightning currents discharged by single-pole arresters were obtained during the four-year period, or 0.79 records per three-pole arrester per year. The maximum number of single-pole records obtained during the four-year period on any one three-pole arrester was 29, and three other arresters yielded 24 records each.

Table I. Number of Three-Pole Lightning Arresters Equipped With Surge-Crest-Ammeter Links, 1936 to 1939 Inclusive

Type of Arresters or Connected Transmission Lines	Number of Three-Pole Arresters Equipped With Surge-Crest-Ammeter Links 1936	1937	1938	1939	Total
Arrester *A*	5	0	0	0	5
Arrester *B*	2	5	5	5	17
Arrester *C*	62	105	105	105	377
Arrester *D*	26	30	29	27	112
Arrester *E*	5	12	23*	29**	69**
Total	100	152	162*	166**	580**
Station-type arresters	74	122	122	122	440
Line-type arresters	26	30	40*	44**	140**
Total	100	152	162*	166**	580**
Arresters connected to:					
11-kv lines	0	2	2	2	6
22-kv lines	17	30	33	32	112
27-kv lines	0	23	25*	31**	79**
33-kv lines	46	46	48	47	187
44-kv lines	1	1	1	1	4
66-kv lines	1	11	13	13	38
88-kv lines	1	1	1	1	4
132-kv lines	34	38	39	39	150
Total	100	152	162*	166**	580**
Arresters connected to:					
Wood-pole lines					
No ground wire	14	30	32	31	107
One ground wire	30	46	53*	58**	187**
Steel-tower lines					
No ground wire	5	9	9	9	32
One ground wire	38	43	44	44	169
Two ground wires	2	7	7	7	23
Mixed circuits	11	17	17	17	62
Total	100	152	162*	166**	580**

* Includes one two-pole arrester installation. ** Includes five two-pole arrester installations.

Table II summarizes the number of records per arrester for the 85 three-pole arresters that remained in service during the entire four-year period. These were all on Ohio Power Company and Appalachian Electric Power Company circuits, where the severity was above the average for the four operating companies.

Table II. Number of Records Per Arrester During Four-Year Period, 1936–39

Based on 344 Records From 85 Three-Pole Arresters Which Remained in Service During the Entire Four-Year Period, all on Ohio Power Company and Appalachian Electric Power Company Circuits

Number of Single-Pole Records Per Arrester	Number of Arresters	Per Cent of Total Arresters
0	27	31.7
1	17	20.0
2	9	10.6
3	1	1.2
4	7	8.2
5	7	8.2
7	3	3.5
8	2	2.4
9	1	1.2
10	1	1.2
11	1	1.2
12	2	2.4
14	1	1.2
17	1	1.2
20	1	1.2
24	3	3.5
29	1	1.2
Total	85	100

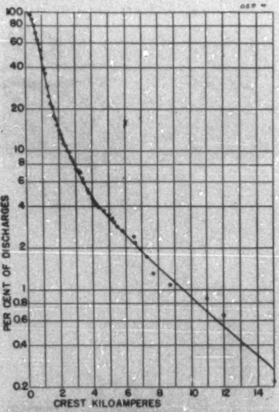

Figure 4. Current magnitude of recorded arrester discharges

Curve shows per cent of discharges with current magnitude at least as great as indicated

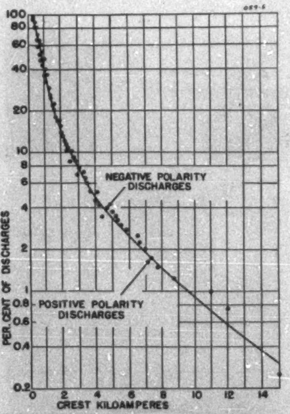

Figure 5. Comparison between magnitudes of positive and negative polarity discharges

Curve shows per cent of discharges with current magnitude at least as great as indicated

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* Including six installation-years on two-pole arresters.

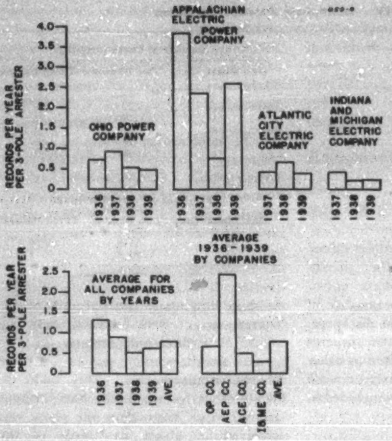

Figure 6 (left). Variation in number of records obtained from year to year, and between different localities

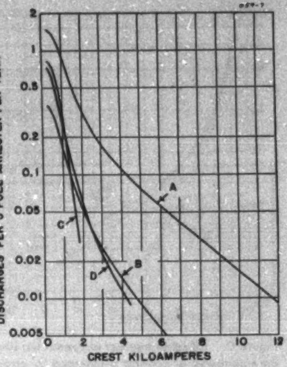

Figure 7 (right). Effect of line construction on number and magnitude of arrester discharge currents

Curves show number of discharges per three-pole arrester per year which have current magnitudes at least as great as indicated

A—Wood-pole lines without ground wires

B—Wood-pole lines with ground wires

C—Steel-tower lines without ground wires

D—Steel-tower lines with ground wires

Magnitude of Currents

The highest individual arrester discharge current recorded was 15,000 amperes, and 16 measurements or 3.5 per cent of the total were 5,000 amperes or more. Arresters at certain locations were subjected repeatedly to high discharge currents.

In many cases there were several arresters in one station connected so that they could operate in parallel. The current which any one might have been called upon to discharge, in the absence of the others, may be estimated by adding together the currents recorded simultaneously in all of the arresters. The highest value obtained in this way for any one phase is 19,000 amperes.

The current measurements obtained are summarized in table III, where the maximum, minimum, and the median current (the current exceeded in 50 per cent of the cases) are listed for several operating conditions, together with the number of current measurements and arrester installation-years upon which the values are based. Figure 4 shows how the 459 current records were distributed with regard to magnitude.

Table III. Summary of Recorded Arrester Discharge Currents, 1936 to 1939 Inclusive

Type of Arresters or Connected Circuits	Number of Three-Pole Arrester-Years	Number of Single-Pole Discharges Recorded	Values of Discharge Currents in Amperes		
			Max.	Min.	Median
Arresters connected to:					
11-kv lines	6	2	500	350	425
22-kv lines	112	97	12,000	350	1,200
27-kv lines	79	29	3,800	Trace	950
33-kv lines	187	169	15,000	Trace	850
44-kv lines	4	4	8,700	1,400	4,200
66-kv lines	38	11	7,700	1,000	1,300
88-kv lines	4	11	1,100	Trace	550
132-kv lines	150	136	4,100	Trace	700
Total	580	459	15,000	Trace	950
Arresters at stations with only one arrester and					
1 line	28	34	8,700	Trace	900
2 lines	111	158	15,000	Trace	1,050
3 lines	29	59	5,800	Trace	1,100
4 lines	20	7	2,200	200	450
5 lines	3	0			
6 lines	4	2	800	600	700
Arresters connected to:					
Wood-pole lines					
No ground wires	107	166	12,000	Trace	1,100
With ground wires	187	72	5,200	Trace	900
Steel-tower lines					
No ground wires	32	26	1,800	Trace	850
With ground wires	192	151	11,000	Trace	650
Mixed circuits	62	144	15,000	250	1,000
Total	580	459	15,000	Trace	950

Polarity of Currents

The polarity of 401 records, or 87.6 per cent of the total, was negative, and of 58, or 12.4 per cent, was positive. This proportion is intermediate between the previously reported values of 63 per cent negative for distribution arrester discharge currents,[1] and 95 per cent negative for lightning currents measured in steel tower legs of transmission lines.[5] In this investigation, the data do not show any recognizable effect of line construction, locality, or circuit voltage rating on the proportional numbers of negative and positive polarity records. Figure 5 shows that the distribution of the negative-polarity records with regard to current magnitude was substantially the same as for positive-polarity records.

Variability Between Localities and Seasons

The data shown in figure 6 indicate that the number of arrester discharges varies considerably from season to season, and is consistently greater in some localities than in others. Seasons which are severe in some localities are comparatively mild in others. The greatest number of discharges per arrester per year was 3.84 for the Appalachian Electric Power Company in 1936, and the smallest number was 0.22 for the Indiana and Michigan Electric Company in 1938. The ratio between these two extremes is 17.5 to 1. During the entire period of the investigation, the average number for the former company was over 8 times that for the latter, although the average isoke-

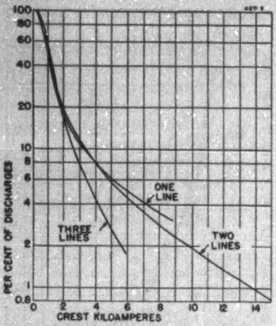

Figure 8. Effect of number of connected lines on magnitude of currents discharged by arresters

Curves show per cent of discharges with current magnitude at least as great as indicated, and include data only from stations with one arrester each

raunic level for the former location was less than twice that for the latter. Among the several factors which may have contributed to this difference are the flashover values of the lines involved, height of conductors above ground, presence of ground wires, and the frequency with which installations were inspected for magnetized links. However, when all known variables are taken into account, it is evident that the number and magnitude of discharge currents is strongly influenced by local conditions which can be evaluated only by experience over a period of several years. Therefore, it must be recognized that conditions may be encountered in service which are quite different from those reported here.

Effect of Circuit Voltage

The data were examined to determine the effect of variation in the rated operating voltage on the severity of arrester discharge currents, but any consistent trend which may exist was obscured by other influences such as the effects of line construction and local conditions. It may be possible to evaluate this effect when more data are available.

Effect of Line Construction

Figure 7 and table III show that arresters connected to wood-pole lines without ground wires are subjected to considerably more severe duty than are arresters on other types of circuits, particularly with regard to the number of high currents which they must discharge. They must pass currents of 5,000 amperes or more about ten times as often as other arresters. Less difference exists between the other types of construction included in figure 7.

Table IV. Multiphase Arrester Operations

Type of Line Construction	Number of Cases Involving: One Phase Only	Two Phases	Three Phases
Steel towers, with ground wire	55	21	18
Steel towers, no ground wire	13	2	3
Wood poles, with ground wire	38	11	4
Wood poles, no ground wire	30	26	28
Mixed line construction	11	6	7
Total	147	66	60
	Per Cent of Cases Involving: One Phase Only	**Two Phases**	**Three Phases**
Wood poles, no ground wires	35.7	31.0	33.3
Other three types of construction	64.2	20.6	15.2

Effect of Multiple Circuits

In figure 8 are shown the results obtained at stations operating with only one lightning arrester each. The curves, shown for cases where one, two, or three transmission lines enter the station, indicate that there is some reduction in the magnitude of currents which the arrester must discharge, as the number of lines is increased. Five per cent of the recorded arrester currents exceeded 6,000 amperes where there was one line, 5,600 amperes where there were two lines, and 3,700 amperes where there were three lines.

It is significant that of the 85 three-pole arresters included in table II, including many at stations with several arresters, over 50 per cent were at stations with four or more incoming lines; yet all of the 11 arresters which produced ten or more records each were at stations with three lines or less.

Multiphase Operations

Data from all arresters involved in the investigation are summarized in table IV to show how many times records of discharge current were found on only one phase of a three-pole arrester, on two phases simultaneously, and on all three phases simultaneously. This table indicates that for wood-pole lines without ground wires, more than one phase was involved in about two-thirds of the cases, and for other types of construction, more than one phase was involved in only about one-third of the cases. For each type of construction the number of two-phase operations was approximately equal to the number of three-phase operations.

Discharge of Arresters Connected in Parallel

In some of the stations there was an arrester connected to each line where it entered the station, and additional arresters connected to the station bus or power transformers or both. The total number of three-pole arresters in any one station, so connected that they could operate directly in parallel, varied from one to ten. Data are presented in table V showing the number of times records of discharge currents were found simultaneously on several such arresters in one station, indicating that they probably discharged in parallel.

The maximum number of arresters apparently discharging in parallel, was five, in a case where there were but five arresters in the station. Three arresters

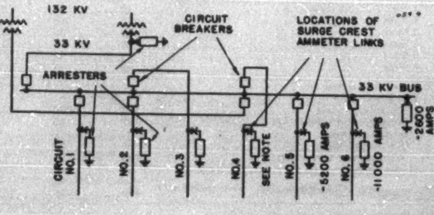

Figure 9. One-line diagram of 33-kv connections at Newcomerstown substation, Ohio Power Company

Current of −450 amperes recorded on phase 2 of this arrester. All other records on phase 1 discharged in parallel on three different occasions, with 4, 8, and 10 arresters respectively in the station, so connected that parallel operation was possible. Two arresters discharged in parallel on 24 different occasions, involving the same two arresters in 10 cases.

A case at a 33-kv substation is of particular interest. The circuit arrangement was as shown in figure 9. Circuits number 2 and number 5 in this sketch were on wood poles and the remaining circuits were on steel towers. All had overhead ground wires. A current of 11,000 amperes was discharged by one pole of the arrester on circuit number 6. Simultaneously a discharge of 5,200 amperes was recorded through the same phase of the arrester on circuit number 5, and one of 2,600 amperes was recorded through the same phase of the bus arrester. No discharges were recorded on the other phases of these arresters, and the only record obtained on the arresters on the other lines which were two, three, four, and five bays distant from the arrester which carried the heaviest current, was 450 amperes on another phase of the arrester connected to circuit number 4.

The curves of figure 10 show that the individual arrester currents discharged when two or more arresters discharge in parallel are in general higher than when only one of the parallel arresters discharges.

Equipment Failures

The only failure of lightning arresters included in the four-year investigation, involved one old-type arrester operating under compromise conditions on a 132-kv circuit, with the possibility of voltage being applied in excess of the manufacturer's rating. This application was made in an attempt to protect power transformers which were of an old design, with an impulse strength considerably below present-day standards. It therefore appears that the arresters involved in the investigation have a discharge capacity adequate for discharges of the magnitude and duration which they experienced.

Five cases of bushing failure occurred. The bushings concerned were of an old type, some ten or more years old, and due to the fact that all cases were internal bushing failures, it is believed they are chargeable to deterioration rather than lack of adequate protection. In one case the current through the arrester was 12,000 amperes; in another case, 2,400 amperes; and in three cases, no currents were indicated. It is interesting to note that in all five cases, the bushings which failed were of the order of 30 to 70 circuit feet from the nearest lightning arrester. Four of the bushings were rated and operating at 33 kv and one at 22 kv.

One bus-support flashover occurred on a 33-kv bus. This was of an old design, the electrical characteristics of which are unknown.

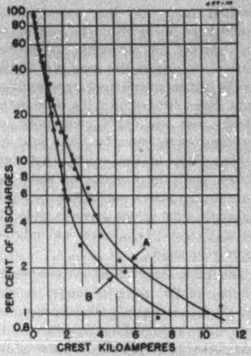

Figure 10. Comparison of (A) discharge currents in cases where two or more arresters discharged in parallel, with (B) cases where only one of several parallel arresters discharged

Curves show per cent of discharges with current magnitude at least as great as indicated

Table V. Operation of Arresters Connected in Parallel

Number of Three-Pole Arresters in Station	Number of Three-Pole Arrester-Years	Number of Cases in Four Years When Records of Discharge Were Found Simultaneously on: One Arrester	Two Arresters	Three Arresters	Four Arresters	Five Arresters	Over Five Arresters
1	195	142					
2	105	27	13				
3	7	3	0	0			
4	67	7	5	1	0		
5	62	11	2	0	0	1	
6	30*	4	2	0	0	0	0
7	9	0	0	0	0	0	0
8	60	9	1	1	0	0	0
9	0	0	0	0	0	0	0
10	30	6	1	1	0	0	0
Totals	565*	209	24	3	0	1	0

* Includes five two-pole arresters.

Table VI. Protection Given by Arresters

Assuming Two Circuits Connected to Bus, With Traveling Wave Entering on One

Circuit Voltage Rating, Kv	Three-Pole Arrester-Years	Maximum Safe Impulse, Kv	Corresponding Discharge Current, Amperes	Number of Discharges Recorded: Total	Number of Discharges Recorded: Unsafe Without Arrester	Number of Single-Pole Discharges Per Three-Pole Arrester Per Year: Total	Number of Single-Pole Discharges Per Three-Pole Arrester Per Year: Unsafe Without Arrester
11	6	104	296	2	2	0.33	0.33
22	112	144	340	97	97	0.87	0.87
27–33	266	180	368	198	176	0.74	0.66
44	4	232	460	4	4	1.00	1.00
66	38	324	596	11	11	0.29	0.29
88	4	412	720	11	4	2.75	1.00
132	150	600	1,000	136	47	0.91	0.31
11–33	384			297	275	0.77	0.72
44–88	46			26	19	0.57	0.41
132	150			136	47	0.91	0.31
All	580			459	341	0.79	0.59

Protection Given by Arresters

The amount of benefit obtained from the arresters becomes apparent when calculations are made to show how many of the recorded disturbances would have resulted in voltages considered unsafe in the absence of arresters. In table VI, the values assumed for maximum safe impulse voltage are 80 per cent of the proposed AIEE and ASA standard chopped-wave impulse test voltages for new transformers. The corresponding arrester discharge current is calculated on a traveling wave basis, assuming one incoming and one outgoing transmission line, with an arrester at the station. All discharges recorded on lines of the rating in question, with currents above the calculated value, are then considered to have resulted from traveling waves which would have produced unsafe voltages in the absence of arresters. A mathematical analysis on this basis is not valid for the higher-current discharges resulting from direct strokes near the stations, in which case successive reflections of traveling waves must be taken into account. Such waves would, however, almost surely be unsafe, and since the current magnitudes would likely be much higher than the values shown in table VI, the mathematical analysis would indicate them to be unsafe.

The table shows that a considerable number of the recorded surges in the 132-kv class, and nearly all of the recorded surges in lower-voltage classes would have been unsafe in the absence of arresters. The number of unsafe waves per three-pole arrester per year decreases progressively as system voltage is increased, ranging from 0.72 for 11–33 kv to 0.31 for 132 kv.

Note that the calculated values in table VI are based on the assumption that a traveling wave entered the station on one conductor only, and that there was a second three-phase line connected to the station bus to provide a path for an outgoing traveling wave. These assumptions were made to simplify the calculations, and appear to represent a fair approximation of the average conditions. For 7.4 per cent of the recorded discharges, dead-end circuits were involved, which would increase the severity as compared to that for the assumed conditions. For 75 per cent of the discharges, lightning currents entered the station over two or more conductors, which would also increase the severity. For 40 per cent of the discharges, there were more than two lines connected to the bus, which would reduce the severity.

Conclusions

1. The data obtained indicate that the number and magnitude of discharge currents through arresters in stations is strongly influenced by local conditions and seasonal variations, and representative values can be determined only by experience over a period of several years. Therefore, conditions may be encountered in service which are quite different from those reported here.

2. A total of 459 single-pole arrester discharge currents were recorded in a four-year investigation, on an average number of 145 three-pole arresters located in stations. This is a rate of 0.79 single-pole discharges per three-pole arrester per year, which may be compared to the previously published rate of 0.51 discharges per single-pole arrester per year on rural distribution circuits, which have exposure somewhat comparable to transmission systems.

3. Additional discharges below the limit of current sensitivity of the recording apparatus probably occurred. Also in case of two or more discharges through any single-pole arrester between successive inspections, only one current record would be obtained; hence discharges undoubtedly occurred without being recorded for this reason.

4. One three-pole arrester yielded a total of 29 single-pole records during the four-year period, and three others yielded 24 each.

5. The highest discharge current recorded through one pole of an arrester was 15,000 amperes. There is no reason for believing that this is the highest discharge current that can occur, and it is expected that as more data are accumulated, substantially higher currents will be recorded.

6. Arresters at certain locations were subjected repeatedly to high discharge currents.

7. Approximately 3.5 per cent of the arrester discharge currents were of 5,000 amperes or more.

8. The polarity of 87.6 per cent of the records was negative, and of 12.4 per cent was positive. The proportion of positive currents was lower than previously obtained for distribution arrester discharge currents and higher than for lightning currents in steel tower legs of transmission lines.

9. The difference in number of records obtained in different localities during the four-year period was as great as eight to one. An attempt to evaluate the factors that might have contributed to this difference, such as the number of thunderstorm days per year, types of line construction, line operating voltage, and the interval between inspections to locate magnetized links, did not account for this great a difference in severities. It is evident that the severity is influenced by the severity of the storms, including seasonal variations, and other local factors that can be determined only by experience, and cannot be accurately predicted on the basis of data obtained in other localities.

10. Any consistent trend which may have existed in the severity of discharge currents, as influenced by the circuit voltage rating, was obscured by the effects of other variables. It may be possible to evaluate this effect when more data are available.

11. Arresters connected to wood-pole lines without ground wires were subjected to more high-current discharges than other arresters. Discharges of 5,000 amperes or more occurred about ten times as often as with other types of line construction.

12. For arresters connected to wood-pole lines without ground wires, more than one pole of the arrester was involved in about two-thirds of the cases when discharges were recorded. For arresters connected to other types of lines, more than one pole of the arrester was involved in only about one-third of the cases.

13. The data indicate that increasing the number of lines connected to a station bus decreases the magnitude of the current which any one lightning arrester in the station is required to discharge.

14. In stations where there were several arresters so connected that they could discharge directly in parallel, the records obtained indicate that five arresters discharged in parallel on one occasion, three arresters in parallel on three different occasions, and two arresters in parallel on 24 different occasions. The individual arrester discharge currents were in general higher when two or more arresters discharged in parallel, than when only one of two or more arresters in a station discharged.

15. The data indicate that when a lightning current enters a station on a line equipped with a lightning arrester at the station entrance, the lightning voltages reaching other parts of the station are not usually sufficient to cause the operation of other arresters.

16. Damage to equipment at stations during this four-year investigation was of minor importance. The only lightning arrester which failed was of an old type, applied under compromise conditions. The five cases of bushing failure involved 22- or 33-kv bushings of obsolete design, the insulation of which appeared to have been in doubtful condition.

17. Calculation of the traveling-wave voltages associated with the recorded arrester discharge currents shows that for 11- to 33-kv lines, about 0.72 records per three-pole arrester per year, or 93 per cent of those recorded, represented lightning voltages that would have been dangerous to station equipment in the absence of arresters. Corresponding values for 44- to 88-kv lines are that 0.41 records per three-pole arrester per year, or 73 per cent of those recorded; and for 132-kv lines that 0.31 records per three-pole arrester per year, or 35 per cent of those recorded, would have been dangerous to station equipment in the absence of arresters. This clearly illustrates the relative number of lightning surges of dangerous magnitude which reach a station not supplied with lightning-arrester protection.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Electric Couplings

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Synopsis: Electric couplings have recently been applied on a large scale in geared Diesel ship drives to couple the engines to the gear. They serve a threefold purpose by acting as a flexible coupling to keep the torsional vibration of the engine out of the gears, as a disconnecting clutch to enable any engine to be cut out of service, and as an aid in maneuvering.

A typical large coupling built for marine service is described, and the general principles and characteristics of this type of device are given. Results of tests taken to confirm the calculated characteristics are included. The operation of the coupling as an aid in maneuvering is described. A summary of applications to date is given with a discussion of possible applications other than ship drive.

ELECTRIC COUPLINGS are devices for transmitting torque by means of electromagnetic forces in which there is no mechanical contact between the driving and driven members. Thus, they differ from electromagnetic clutches which are only friction clutches in which an electromagnet is used to apply the pressure between the friction surfaces. Electric couplings are not new in principle, but their application on a large scale has come only in the last few years. It is the purpose of this paper to review the principles of operation of electric couplings, describe their major application to geared Diesel-engine ship propulsion, and discuss the engineering problems associated with this application.

Probably the idea of an electric coupling is nearly as old as the idea of an a-c generator. There are a number of early United States patents from 1891 to 1906 describing at least the elements of the various types of electric couplings. In 1934 and 1935, the authors made a study of the various types of couplings with the hope of applying them to marine drives. Since 1935, A.S.E.A. in Sweden have equipped a number of ships with electric couplings.[1,2] A 3,000-horsepower coupling was supplied by the Westinghouse Electric and Manufacturing Company to the United States Navy and was quite thoroughly tested. At about the same time, two tankers were equipped with 750-horsepower couplings made by the Elliott Company. Within the past year, the United States Maritime Commission has purchased 116,000 shaft horsepower in electric couplings in 48 units to equip 20 vessels. The extent of electric coupling applications for marine service in this country is given by table I.

Principles of Operation

The electric slip coupling is the type used on all of the large marine applications, and the discussion of the numerous other types will be reserved till the end of the paper. The slip type has poles excited by direct current on one rotating member, and an armature winding, usually of the squirrel-cage type, on the other rotating member. If it is necessary to vary the speed, the winding can be insulated and brought out to slip rings, so that an external resistance can be connected in the circuit to vary the slip.[2] Any relative motion or slip between the two members will induce currents in the armature winding which react with the flux to produce a torque tending to hold the two members close together in speed. This is similar in action to the usual induction motor, except that the rotating field is produced by a direct current in a rotating member instead of by polyphase currents in a stator.

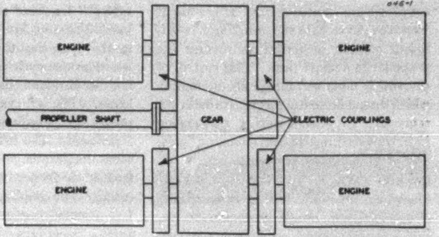

Figure 1. Arrangement of four-engine geared ship-propulsion drive for United States Maritime Commission C-3 cargo vessels, rated 8,500 shaft horsepower

Application to Marine Drives

The principal application of the electric coupling is in geared Diesel-engine ship-propulsion drives as listed in table I. Other applications are described at the end of the paper. The arrangement of a typical marine application is shown in the sketch in figure 1. This is the arrangement being used on the four Maritime Commission *C*-3 cargo vessels listed as number 3 in table I. The four engines drive the single screw through four electric couplings and a two-pinion gear. The manner of mounting the couplings on the engine can be seen in figure 2, and the two members of the coupling are shown in figures 3 and 4.

The coupling performs four functions in the drive, as follows:

1. It acts as a torsionally flexible member between the engine and the gear, eliminating all shocks and limiting the transmission of torsional vibrations produced by the engine to a negligible amount.

2. Since there is no mechanical connection between the two members, the coupling permits a small amount of misalignment. This facilitates the erection of the propulsion plant and increases the reliability in service.

3. It acts as a quick-disconnecting clutch which is not subject to wear, and the torque of which is independent of speed. Any engine can be disconnected in less than a second by interrupting the field current of its coupling. This permits repairs to be made without stopping the ship, and some of the engines can be shut down at low speeds to give greater economy. The greater reliability of multiple-engine drives can be realized only if a disconnecting clutch is available to cut out a faulty engine, enabling the ship to proceed on the good engines. The use of the coupling as a clutch aids in maneuvering as described below.

4. The coupling limits the maximum torque to a safe value. The steady-state pull-out torque is usually about 150 per cent and the transient maximum torque on suddenly applied loads is about 200 per cent. If trouble develops on one engine, such as a seized piston, the electric coupling will limit the torque transmitted to this engine to twice the normal value, and will permit it to stop without injuring the gear and with minimum damage to the engine.

The ease with which electric couplings can be connected and disconnected en-

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1. For all numbered references, see list at end of paper.

ables them to be used in maneuvering the ship in a manner which is impossible with other forms of couplings. When a large amount of maneuvering is to be done at low speed, as in going through a close channel or approaching or leaving the dock, the ship can be maneuvered entirely by the use of the couplings. This is done by running half of the engines ahead and the other half astern. The propeller can be connected to either group of engines by energizing the corresponding couplings, and the ship closely controlled by one coupling control lever. This system saves on starting air for the engines, and approaches the Diesel-electric drive in convenience.

The couplings can also be used to reverse the propeller in reversals from full ahead to full astern, as in a "crash stop". In such a reversal, the coupling excitation is removed and the engines reversed. After they are running on fuel at reduced speed in the astern direction, the coupling fields are energized, and the coupling torque reverses the propeller. Figure 5 shows a test slip torque curve on a coupling and a typical propeller torque curve during reversal,[3] assuming that the engines have been brought to 60 per cent speed in the astern direction before the couplings are excited again. It is evident from the curve that the coupling has sufficient margin in torque to reverse the propeller in a short time. This system of reversal is most advantageous on engines which do not have full torque available for reversal. By disconnecting the engine from the inertia of the gear, coupling fields, and propeller, they can be reversed very quickly, even with rather low torque available, because the inertia to be accelerated is low. With the engine on fuel, full torque becomes available to reverse the heavy inertias of the system and overcome the propeller torque. If the engine has sufficient reversing torque, the maneuvering can be done entirely with the engine, with the coupling acting like a solid coupling.

Characteristics

Losses. There are only three losses in electric couplings—windage, slip or armature I^2R loss, and excitation or field I^2R loss. The core loss is usually negligible, as the operating frequency is ordinarily less than one cycle per second. The core loss is included in the measured slip losses. The slip varies with speed and rating, but is ordinarily between one and 2 per cent. The field loss has about the same magnitude, so the efficiency varies from 95 to 98 per cent depending on the rating. The coupling illustrated in figures 1 to 4 tested just under 1.1 per cent slip and 97.75 per cent efficiency.

Figure 2. Electric coupling mounted on 2,225-horsepower, 240-rpm Diesel engine on shop tests

This is the engine and coupling for the drive in figure 1

Torques. The torques required are determined by the operating conditions for the particular drive. The Maritime Commission engineers realized the full possibilities of using the electric coupling for maneuvering. They specified a minimum torque of 75 per cent for any slip up to 140 per cent to give the coupling some margin in torque over that required by the propeller during reversal. They also specified 150 per cent pull-out, or maximum torque as being sufficiently high to cover all conditions of operation. To obtain high torques at high slips, and at the same time maintain low slip at normal load and minimum weight and space, is a real design problem. Earlier couplings employed single-deck squirrel cages, although some of them used deep bars. The torques of these couplings at high slips were far below the values necessary to reverse the propeller at full ship speed. All couplings being built to the Maritime Commission specifications employ some

Table I. List of Marine-Type Electric Couplings

Completed or Under Construction in United States of America, as of November 15, 1939

Number	Type of Vessel	Number of Ships	Ship Builder	Engines Per Ship	Engine Manufacturer	Engine Horsepower	Rating, RPM	Coupling Manufacturer	Gear Manufacturer
1**				1		3,000	400	W	
2	Tanker	2	C, M	2	F-M	750	400	E	F-B
3	†	1		1	Atlas	600	300	D	Falk
4*	C-3 cargo	4	Sun	4	Busch	2,225	240	W	Falk
5*	C-3 cargo and passenger	4	Sun	2	Sun	4,375	180	W	W
6*	C-1 cargo	5	ST	2	HOR	2,140	240	W	W
7*	C-1 cargo	5	W.P.&S.	2	Busch	2,085	233	E	F-B
8*	C-1 cargo	2	Penna	2	Nord.	2,105	220	W	W

* United States Maritime Commission vessels.

** Experimental unit for United States Navy. Not installed in a ship.

† The "Bear", Antarctic exploration ship, recently repowered for Byrd expedition.

Key to abbreviations:

C—Collingwood Ship Yards, Ltd.
M—Marine Industries, Ltd.
Sun—Sun Shipbuilding and Dry Dock Company
ST—Seattle Tacoma Shipbuilding Company
W.P.&S.—Western Pipe and Steel Company
Penna.—Pennsylvania Ship Yards
Atlas—Atlas Imperial Diesel Engine Company
Busch—Busch-Sulzer Bros. Diesel Engine Company
HOR—Hoover-Owens-Rentschler Division of General Machinery Corporation
Nord.—Nordberg Manufacturing Company
F-M—Fairbanks-Morse Company
W—Westinghouse Electric and Manufacturing Company
E—The Elliott Company
D—Dynamatic Corporation
F-B—Farrell-Birmingham Company
Falk—The Falk Corporation

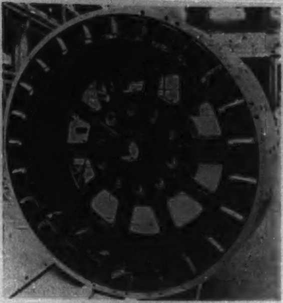

Figure 3. Field member of coupling shown in figure 2

form of double-deck squirrel-cage armature winding to increase the resistance at high slips.

Temperatures. All recent couplings have had guaranteed temperature rises of 70 degrees centigrade by resistance, above an ambient of 50 degrees centigrade. These are standard for class *B* insulation on electrical installations in ship engine rooms. This rise applies only to the field, as in the squirrel-cage type of armature there is no insulation, and the temperature is limited solely by mechanical considerations. It is customary to require that the coupling meet the guaranteed temperature rise when transmitting full torque down to 70 per cent of normal speed. This speed was chosen as it is the speed obtained with full torque on half of the engines, and was considered the most severe operating condition likely to be encountered. Theoretically, it would be possible to operate with full torque at half speed when running on one engine of a four-engine drive, but it was not considered that this would be an operating condition. With class *B* asbestos and mica insulation, the machine is practically incombustible and offers much less fire hazard than any device using oil.

Torsional Characteristics. In the cases studied so far by the authors, the electrical coupling has effectively limited the transmission of torsional vibration from the engine to the gears to negligible values. It has also had negligible effect on the critical speeds of the engine. In information previously published about electric slip couplings,[2] it has been said that the coupling does not transmit the torque pulsations originating in the engine. Strictly speaking, this statement is not exactly true, because as shown in appendix *A*, the electric coupling under the influence of pulsating torques acts as a weak elastic member, or a torsional spring of low spring constant. In appendix *B*, there is a further discussion of the effects of this spring constant, giving the reasons why it has negligible effect on the natural frequencies of the engine and at the same time effectively limits the vibrations transmitted from the engine.

Structural Features

The coupling illustrated in figures 2, 3, and 4 is typical of large marine couplings made in this country, especially those for Maritime Commission vessels. The two members are overhung on the gear and engine shafts. Most of the couplings have the inner member mounted on the engine shaft, but the coupling will transmit torque in either direction and the field can be put on the engine. The arrangement to be used depends largely on the amount of inertia desired by the engine manufacturer for satisfactory operation of the engine and the most advantageous placing of the critical speeds.

The field has the poles bolted to the inside of the rim of a fabricated-steel spider. The spider has a flange for bolting to the pinion shaft. The field is put on the outer member because such an arrangement gives more room for the winding, better ventilation, and more accessibility for replacing coils. The coils are made of edgewound copper strap with class *B* insulation of the same type almost universally used in large a-c motors and generators, representing the most rugged construction available. Repairs can be made quite easily by unbolting the pole, sliding it out axially, and installing a spare field coil. The collector rings are bronze and the collector assembly is split for ease of removal. The field is ventilated by air which is taken in through holes in the spider plate, passes axially along the windings, and is discharged at the open end.

The inner member consists of a fabricated spider which has a flange for bolting to the engine crankshaft flange. The core is made of one piece circular punchings shrunk on the rim of the spider. The winding is a double-deck squirrel cage with separate short-circuiting end rings for each layer. The outer layer of high-resistance bars is generously proportioned to absorb the heat produced during reversal of the propeller, since at high slips most of the loss is in the outer layer.

The coupling can be removed or assembled without disturbing the crankshaft or pinion shaft. Provision has been made to move the two members together axially a sufficient amount so that they will clear the spigot fits. In this position, they are centered together by a centering fit and can be bolted together. The entire unit can then be lifted out between the two shafts.

A screen guard is usually placed around the coupling for the protection of the engine room personnel. The brush holder parts are mounted on the gear case. The couplings take their excitation from the ship auxiliary power supply. The control consists of a selector switch operated by the coupling control lever at the engine control station, ammeters to indicate the field current, circuit breakers in each coupling circuit for short-circuit protection, and suitable magnetically operated contactors. Discharge resistors are provided and also a resistor to reduce the coupling field for long operating periods at low speed and low torque. The necessary interlocks are provided to prevent energizing a coupling when the corresponding engine is shut down.

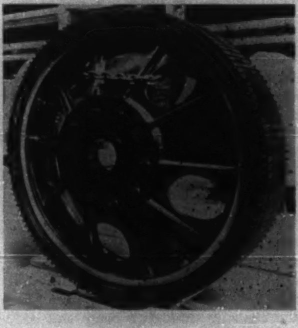

Figure 4. Armature or squirrel-cage member of coupling shown in figure 2

The size and weight of a coupling of given rating increase quite rapidly with the air gap. The minimum gap which can be used is determined by the possible maximum unbalanced magnetic pull. The unbalanced magnetic pull acts in a radial direction and is proportional to the displacement for magnitudes of displacement encountered in normal operation. It acts to increase the displacement, so is equivalent to a negative restoring force. The magnitude of the force depends on the initial displacement which is a function of the bearing clearance, the accuracy of the original alignment, and the possible distortion or misalignment of the ship structure. With conventional ship structure, the misalignment due to the distortion of the ship is very small, as a large deflection of the center of the ship with respect to the ends is produced by a small shear deformation. The air gap used represents a compromise between size and weight on the one hand and magnetic pull on the other. A gap of one-fourth inch appears to be a good compromise for large couplings. Since the magnetic pull may be increased by large bearing clearances, it is desirable to keep these clearances to a minimum consistent with good practice. It should be noted that the magnetic pull in an axial direction is very small.

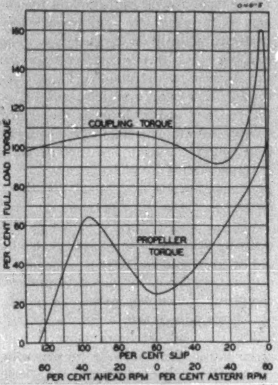

Figure 5. Test slip-torque curve of coupling shown in figures 1 to 4

A typical propeller-torque curve during reversal from full speed is shown assuming that the engine has been brought to 60 per cent speed astern before the couplings are excited

Tests

Since the coupling described above was the first coupling to be delivered to the Maritime Commission, it was given rather extensive tests. The slip-torque curve was measured by locking one member of the coupling through a beam and scale and driving the other member at various speeds with a large d-c motor. The torque measured on the scale was checked by readings of the input to the driving motor. The results are shown in figure 5. The windage was measured by driving the coupling at various speeds and measuring the input. From the curve of friction and windage against speed, the windage could be separated out by the assumption that the friction loss varied directly with the speed while the windage varied as the cube of the speed. The ability of the squirrel cage to undergo a large number of maneuvering operations at reduced speed was demonstrated by locking the inner member, bringing the field member to 70 per cent speed with the excitation off, and then stopping it by applying the excitation. This was repeated 22 times in 9 minutes and 12 seconds with no injurious heating of the bars. Of course, there was no propeller torque to overcome, but the addition of the driving motor gave an inertia comparable to the final installation, and it was felt that the test demonstrated that the coupling had considerable margin above the requirements of actual service.

Further tests were taken on the coupling at the engine-builder's plant. The coupling was mounted on the engine as shown in figure 2, coupling it to a water brake. All of the regular tests on the engine were taken by loading it on the brake through the coupling. This also provided an opportunity for a full-load temperature test on the coupling, demonstrating that it met its guarantees with a good margin. The test setup included a flywheel on the brake side which was roughly equivalent to the mass of the gears and propeller in the final installation. The maneuvering of the ship was simulated, although it was not possible to include propeller torque, and the coupling was able to reverse the mass of its outer member and the flywheel from full speed ahead to full speed astern in from six to eight seconds. The tests indicated that some time in reversal of the propeller would be saved by reversing the engine independently with the coupling disengaged and then using the coupling to stop and reverse the propeller. Several torsiograph records were made to determine the amount of vibration transmitted through the coupling. Typical examples are shown in figure 6. The record marked *A* was taken on the engine crankshaft flange. The vibrations present are the seventh order, or the main firing frequency. They are speed variations rather than shaft vibrations. The record marked *B* was taken on the brake side of the electric coupling. None of the seventh order impulses can be seen on this record. The long swings are due to the natural period of the torsiograph. The high-frequency variations seem to have a frequency of 12 per revolution, and are probably excited by the brake. The amplification on the two records is about the same, record *B* being slightly higher than *A* as shown by the calibration for one degree indicated on the record. The revolution marked off in each record is the same revolution of the engine.

Other Types of Electric Couplings

There are as many possible types of electric couplings as there are types of motors and generators. The more practical types will be discussed briefly.

The slip coupling with squirrel-cage winding has been described in detail. For the purpose of obtaining variable speed, it is possible to insulate the armature winding and bring the leads out through slip rings exactly as in a wound-rotor induction motor. This type will be referred to as a wound-rotor coupling. The problem of controlling the speed through the external resistance is exactly the same as for the wound-rotor motor, except that it is possible to get fine speed adjustments between resistance points by controlling the field current. The efficiency comes down nearly proportional to the slip. This type may find application to Diesel-engine drives where it is desired to run below the lowest operating speed of the engine.

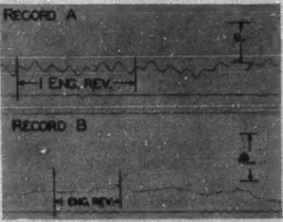

Figure 6. Typical torsiograph records, taken on shop test with engine driving water brake through electric coupling

Record *A* taken on engine crankshaft flange
Record *B* taken on output side of electric coupling

A variable-speed coupling for certain types of load may also be obtained by designing a squirrel-cage slip coupling for high slip and controlling the torque by control of the excitation. This is analogous to controlling the voltage on a squirrel-cage motor. There are two difficulties in this: The high slip necessary for any appreciable range of slip by field control results in lower efficiency at full speed as well as at reduced speed. The other difficulty is the dissipation of the heat from the armature member, since all the slip losses go into this member.

Instead of a conventional squirrel-cage winding, it is obviously possible to use only a solid steel rotor on the armature, making use of the eddy currents induced in its surface. This type gives less pull-out torque and higher slip for the same air gap surface and weight. However, for variable speed by field control, it has the advantage that it can be arranged more readily to dissipate the high slip losses, especially in smaller sizes.

The inductor-alternator principle may be used to make an inductor-type electric coupling. The field is wound peripherally on one member, dividing the air gap into two sections. The flux is unidirectional in each section and is caused to alternate in the armature winding by the relative motion of inductor teeth on the other member. For a given air gap and pull-out torque, more air-gap surface is required in the inductor-type machine since the useful alternating-flux density is limited by saturation to about half of the value in a salient-pole type, due to the superposition of d-c and a-c components of flux. However, where the air gap can be kept very small, this type may result in somewhat cheaper construction for small couplings.

A synchronous coupling can be built by using salient poles and d-c excitation on both members. For most applications this type is less desirable than the slip coupling for several reasons. The increased excitation requirements partially offset the elimination of slip losses, and slip rings are required on both members. The synchronous "spring" torque tends to be higher than in the slip coupling, and if this type of coupling should pull out of step with fields excited, high values of slip-frequency alternating torque would be developed which might excite the torsional natural frequency of the connected mechanical system. Also the presence of field windings on both members makes it difficult to obtain high average torques at high slip.

Other Applications

The application of the slip type of coupling to geared Diesel ship-propulsion drives has been discussed in considerable detail, because that is the largest application of couplings made so far. However, it is interesting to note some of the other possible uses for couplings. This same type of coupling can be applied to any Diesel or gas engine where it is desired to limit the engine torque impulses transmitted to the load. It may also be used for the purpose of limiting the maximum torque and avoiding stalling of the engine. In such applications, the pull-out torque of the coupling must be made less than the stalling torque of the engine. It can be used as a disconnecting clutch to shift gears on an engine which is too large for a friction clutch.

The variable-speed coupling of either the wound-rotor or high-slip squirrel-cage type may be used with a synchronous motor or a constant-speed induction motor to obtain smooth speed or torque control. By use of field control, the speed can be controlled in finer steps than is practicable with a resistance in the secondary of a wound-rotor induction motor. As previously stated, the reduced speed is always accompanied by a reduction in efficiency, but this may not be too serious a handicap for a limited range in speed, for short-time operation at low speed, or for fan or pump drives where the power varies as the cube of the speed.

For a limited range of speed, or for a fan type of load, it is possible in the smaller ratings to obtain speed control entirely with field control of the coupling, using a slip type of coupling with high-resistance squirrel cage, or a solid steel armature.

Conclusions

The electric coupling has filled a definite need in the application of higher-speed Diesel engines to geared ship propulsion. The engineering problems encountered in this application have been satisfactorily solved, as proved by the installations which are now in successful service. As time goes on, a larger proportion of Diesel ship drives are being geared, and the percentage of these geared drives on which electric couplings are applied is increasing rapidly.

Now that the electric coupling has been developed and applied on a commercial scale, it is quite sure to find other applications where its characteristics can be used to advantage.

Appendix A

Electrical Torques Produced by Pulsating Motion in the Electric Slip Coupling

The analogy to the induction motor makes it apparent that the effect of the average torque is to produce a slip between the two members. The effect of any superimposed alternating motion between the two members is to produce both in-phase and quadrature components of alternating torque.

The component of torque in phase with the alternating component of the relative velocity is a damping torque, since it represents energy supplied by the alternating motion. The component of the alternating torque in phase with the angular motion (but opposite in direction) is a transient synchronizing torque, and is equivalent to a weak mechanical spring as far as alternating components of torque are concerned.

The mathematical analysis is based on the following assumptions:

1. That the field resistance is negligible in comparison with the field reactance at the frequencies to be considered.

2. That the squirrel-cage winding can be replaced by an equivalent two-phase winding. This has been proved by applications to induction motors to be quite accurate.

3. That the inductance (L) of these equivalent armature circuits is independent of the relative position with respect to the field. This, in general, is only approximately true, but the effects of variation in inductance will be analyzed further from a physical point of view.

4. The analysis will be carried through for a relative angular motion represented by $\theta=(\omega_s t+\theta_\Delta \sin \omega_\Delta t)$, where θ is the relative angular position of phase a with respect to the field, ω_s is the average angular velocity, θ_Δ is the amplitude of the alternating angular motion, and ω_Δ is 2π times the forced frequency of oscillation. All angles are measured in electrical radians. The actual alternating motion may not be sinusoidal, but it can be resolved into a series of sinusoidal components which can be treated in the same manner.

5. That the amplitude of the angular oscillation θ_Δ, is small, so that $\cos(\theta_\Delta \sin \omega_\Delta t)$ can be taken as unity and $\sin(\theta_\Delta \sin \omega_\Delta t)$ can be taken as $\theta_\Delta \sin \omega_\Delta t$. This is a good approximation up to about 15 electrical degrees. This maximum angle corresponds to about 50 per cent torque transmitted, which is higher than is likely to be encountered. On this basis:

$$\cos\theta=\cos\omega_s t+\frac{\theta_\Delta}{2}\cos(\omega_\Delta+\omega_s)t-\frac{\theta_\Delta}{2}\cos(\omega_\Delta-\omega_s)t$$

$$\sin\theta=\sin\omega_s t+\frac{\theta_\Delta}{2}\sin(\omega_\Delta+\omega_s)t+\frac{\theta_\Delta}{2}\sin(\omega_\Delta-\omega_s)t$$

The equation for voltage and current will be expressed first in volts, amperes, and ohms, and then the torque equation will be expressed in per unit notation in order to give both forms.

$$e_a=-p\left(\frac{M}{L_f}\psi_f\right)\cos\theta=\left(\frac{M}{L_f}\psi_f\right)\left[\omega_s\sin\omega_s t+\frac{\theta_\Delta}{2}(\omega_\Delta+\omega_s)\sin(\omega_\Delta+\omega_s)t-\frac{\theta_\Delta}{2}(\omega_\Delta-\omega_s)\times\sin(\omega_\Delta-\omega_s)t\right]$$

$$e_b=-p\left(\frac{M}{L_f}\psi_f\right)\sin\theta=-\left(\frac{M}{L_f}\psi_f\right)\times\left[\omega_s\cos\omega_s t+\frac{\theta_\Delta}{2}(\omega_\Delta+\omega_s)\cos(\omega_\Delta+\omega_s)t+\frac{\theta_\Delta}{2}(\omega_\Delta-\omega_s)\cos(\omega_\Delta-\omega_s)t\right]$$

The currents can be calculated by using the proper impedance for each frequency. The subscripts s, $\Delta+$, and $\Delta-$ will be used to indicate the impedances determined at frequencies $\frac{\omega_s}{2\pi}$, $\frac{\omega_\Delta+\omega_s}{2\pi}$, and $\frac{\omega_\Delta-\omega_s}{2\pi}$ respectively.

$$i_a=\left(\frac{M}{L_f}\psi_f\right)\left[\frac{\omega_s}{(Z_s)^2}(\gamma_s\sin\omega_s t-X_s\cos\omega_s t)+\frac{\theta_\Delta}{2}\frac{(\omega_\Delta+\omega_s)}{(Z_{\Delta+})^2}\left\{(\gamma_{\Delta+})\sin(\omega_\Delta+\omega_s)t-(X_{\Delta+})\cos(\omega_\Delta+\omega_s)t\right\}-\frac{\theta_\Delta}{2}\frac{(\omega_\Delta-\omega_s)}{(Z_{\Delta-})^2}\times\left\{(\gamma_{\Delta-})\sin(\omega_\Delta-\omega_s)t-(X_{\Delta-})\cos(\omega_\Delta-\omega_s)t\right\}\right]$$

$$i_b = -\left(\frac{M}{L_f}\psi_f\right)\Bigg[\frac{\omega_s}{(Z_s)^2}(\gamma_s \cos \omega_s t +$$

$$X_s \sin \omega_s t) + \frac{\theta_\Delta}{2}\frac{(\omega_\Delta+\omega_s)}{(Z_{\Delta+})^2}\{(\gamma_{\Delta+}) \cos (\omega_\Delta +$$

$$\omega_s)t + (X_{\Delta+}) \sin (\omega_\Delta+\omega_s)t + \frac{\theta_\Delta}{2}\frac{(\omega_\Delta-\omega_s)}{(Z_{\Delta-})^2} \times$$

$$\{(\gamma_{\Delta-}) \cos(\omega_\Delta-\omega_s)t + (X_{\Delta-}) \sin (\omega_\Delta-\omega_s)t\}\Bigg]$$

In per-unit notation, the torque can be determined by the equation developed by R. H. Park[4] and others.

$$T = (i_a\psi_b - i_b\psi_a) \qquad (1)$$

The per-unit currents can be obtained by dividing the actual amperes by rated amperes (I_0). The per-unit flux linkage, ψ_a and ψ_b can be obtained by dividing actual interlinkage by ψ_0 which is the flux linkage corresponding to the resistance drop at full load.

The net flux linkage of each armature circuit can be written:

$$\psi_a = \frac{1}{\psi_0}\left(\frac{M}{L_f}\psi_f \cos \theta + i_a L\right)$$

$$\psi_b = \frac{1}{\psi_0}\left(\frac{M}{L_f}\psi_f \sin \theta + i_b L\right)$$

Then

$$T = \left(\frac{M\psi_f}{L_f\psi_0}\right)(i_a \cos \theta - i_b \sin \theta) \qquad (2)$$

Expanding this equation and neglecting all θ_Δ^2 terms because they are small for θ_Δ less than 15 degrees as assumed, and keeping impedances in ohms:

$$T = \left(\frac{M\psi_f}{L_f\psi_0}\right)^2 \frac{\psi_0}{I_0}\Bigg[\left\{\frac{\omega_s\gamma_s}{Z_s^2}\right\} + \left\{\frac{(\omega_\Delta+\omega_s)\gamma_{\Delta+}}{(Z_{\Delta+})^2} +\right.$$

$$\left.\frac{(\omega_\Delta-\omega_s)\gamma_{\Delta-}}{(Z_{\Delta-})^2}\right\}\frac{\theta_\Delta}{2} \cos \omega_\Delta t + \left\{\frac{(\omega_\Delta+\omega_s)X_{\Delta+}}{(Z_{\Delta+})^2} +\right.$$

$$\left.\frac{(\omega_\Delta-\omega_s)X_{\Delta-}}{(Z_{\Delta-})^2} - \frac{2\omega_s X_s}{(Z_s)^2}\right\}\frac{\theta_\Delta}{2} \sin \omega_\Delta t \Bigg\} \Bigg] \qquad (3)$$

Using per-unit impedances, and using the symbols s and Δ to represent the per-unit slip frequency and forced frequency respectively, the torque may be written in per-unit notation:

$$T = (E_d')^2\left[\frac{\frac{\gamma_s}{s}}{\left(\frac{\gamma_s}{s}\right)^2 + (X_s)^2}\right] + (E_d')^2 \times$$

$$\left[\frac{\frac{\gamma_{\Delta+}}{\Delta+s}}{\left(\frac{\gamma_{\Delta+}}{\Delta+s}\right)^2 + (X_{\Delta+})^2} + \frac{\frac{\gamma_{\Delta-}}{\Delta-s}}{\left(\frac{\gamma_{\Delta-}}{\Delta-s}\right)^2 + (X_{\Delta-})^2}\right] \times$$

$$\frac{\theta_\Delta}{2} \cos \omega_\Delta t + (E_d')^2\left[\frac{X_{\Delta+}}{\left(\frac{\gamma_{\Delta+}}{\Delta+s}\right)^2 + (X_{\Delta+})^2} +\right.$$

$$\left.\frac{X_{\Delta-}}{\left(\frac{\gamma_{\Delta-}}{\Delta-s}\right)^2 + (X_{\Delta-})^2} - \frac{2X_s}{\left(\frac{\gamma_s}{s}\right)^2 + (X_s)_2}\right]\frac{\theta_\Delta}{2} \sin \omega_\Delta t$$

$$(4)$$

In this equation, E_d' is the per-unit voltage proportional to $\frac{M\psi_f}{L_f}$, the flux which would link the armature if the circuit were opened and constant field flux, ψ_f maintained. In dealing with transient phenomena, this voltage would be called the voltage back of transient reactance.

From these torque equations, it is apparent that there is a steady torque dependent only on the average slip, s, and two alternating components, the $\sin \omega_\Delta t$ and $\cos \omega_\Delta t$ terms. The amplitudes of these alternating components are a function of the two frequencies $\Delta+s$ and $\Delta-s$. The sine term is in phase with, but opposed to the displacement and is, therefore, analogous to a weak mechanical spring connecting the two members. The magnitude of this spring constant is simply:

$$\frac{P(E_d')^2}{4}\left[\frac{X_{\Delta+}}{\left(\frac{\gamma_{\Delta+}}{\Delta+s}\right)^2 + (X_{\Delta+})^2} + \frac{X_{\Delta-}}{\left(\frac{\gamma_{\Delta-}}{\Delta-s}\right)^2 + (X_{\Delta-})^2} - \frac{2X_s}{\left(\frac{\gamma_s}{s}\right)^2 + X_s^2}\right]T_0$$

Where P is the number of poles in the coupling and T_0 is rated torque. If T_0 is in pound-feet, the constant is in pound-feet per radian.

The cosine term is a torque in phase with the alternating component of the velocity, and is analogous to mechanical velocity damping. The damping coefficient is:

$$\frac{P(E_d')^2}{4\omega_\Delta}\left[\frac{\left(\frac{\gamma_{\Delta+}}{\Delta+s}\right)}{\left(\frac{\gamma_{\Delta+}}{\Delta+s}\right)^2 + (X_{\Delta+})^2} + \frac{\left(\frac{\gamma_{\Delta-}}{\Delta-s}\right)}{\left(\frac{\gamma_{\Delta-}}{\Delta-s}\right)^2 + (X_{\Delta-})^2}\right]T_0$$

If T_0 is in pound-feet, the coefficient is in pound-feet per radian per second.

From the torque equation (2), it is seen that the pulsating components result from the action of the quadrature axis components of current ($i_a \sin \theta - i_b \cos \theta$). In order to simplify the derivation, the direct and quadrature axis reactances were assumed equal, but if they differ appreciably, the quadrature axis value should be used. If there are damping circuits on the field poles other than the field winding, they must be considered as affecting the reactance at the high frequencies.

Appendix B

Influence of the Electric Coupling on the Torsional Vibrations of the Propelling Drive

An important function of the electric coupling is to limit the transmission of torsional vibration from the engine to the gear. In the information published about electric couplings, it has often been stated that the electric coupling transmits only torques proportional to the slip, and therefore from the standpoint of torsional vibration separates the two systems on each side of it completely. As demonstrated in appendix *A*, this is not strictly true as the coupling acts as an elastic member having a spring constant which is a function of the frequency of the applied torque. It is zero at zero frequency and approaches a maximum value as the frequency increases. When a pulsating torque is applied to the coupling, a certain amount of energy is dissipated in the coupling. This energy acts as damping. For low frequencies up to one cycle per second, it is constant and equal to the torque normally produced in the coupling at normal slip, and then decreases rapidly as the frequency increases, being approximately inversely proportional to the frequency.

The electric coupling being located near the node of the vibration in the crankshaft, the damping in the coupling has very little influence on the amplitude of vibration in the engine. The damping in the coupling will be very effective in limiting the amplitude of low-frequency vibrations originating in the propeller if the nodes are such that large relative motions are produced in the coupling.

On high-speed Diesel engines, the low spring constant of the electric coupling has practically no influence on the natural period of torsional vibrations of the engine proper. Due to the small mass of the inner part of the electric coupling mounted on a very rigid crankshaft, this natural period is very high and only the critical speeds of high orders are in the operating range of the engine. Only an inappreciable part of these torque harmonics is transmitted through the coupling to the gears.

The effect of the spring constant of the electric coupling on the complete propelling drive is to produce a natural period of vibration of low frequency which might be in the range of the frequency of the torque impulses produced by the propeller blades cutting the wake of the ship. Usually, due to the large damping produced in the propeller, the vibrations thus produced are not dangerous unless they occur at the normal operating speed of the propeller. However, a thorough analysis of the complete propelling drive is advisable, making allowance for the spring constant of the electric coupling, to avoid any possibility of trouble.

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Discussion

Discussions will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Enclosed Spark Gaps

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Synopsis: Ionizing radiation has been discovered to be present in spark gaps of the type commonly used in commercial lightning arresters for the last 15 years. This radiation is emitted from discharges near the insulator-electrode contacts, when impulse voltage is applied to the gap. Ionization is produced by the absorption of this radiation in the spark gap. This effectively reduces the time lag of breakdown. The presence of this radiation explains why higher impulse breakdown voltages are obtained with a sphere gap in the dark than with a lightning-arrester spark gap with porcelain spacer. The radiation effect observed here is similar to that investigated by Wynn-Williams in the three-point gap. This paper tells of experiments performed to determine the reason for the low impulse ratios observed with rutile-spacer spark gaps. The results of this study explain the reason for the low impulse ratio of the "quench gap" and the "ionization gap" utilized in lightning arresters. This new information aids in understanding why low impulse ratios are obtained in high-voltage lightning-arrester spark gaps.

THIS PAPER describes experimental work which revealed a hitherto unrecognized, important factor responsible for the low impulse breakdown voltage found for short enclosed spark gaps such as are commonly used in lightning arresters. To obtain a low impulse ratio it has long been known that some form of ionizing radiation must be present.[1-4] In lightning-arrester spark gaps this radiation is emitted from subsidiary discharges discovered to exist between the electrodes and the insulating spacer, accompanying the dielectric displacement current. The intensity of this radiation may be expected to be proportional to the dielectric displacement current so that spark gaps with spacers of higher-dielectric-constant material, such as rutile,* give lower impulse ratios. This paper describes experiments made to study the interesting breakdown results obtained with rutile-spacer gaps. These tests led to the view that in lightning-arrester spark gaps the spark lag is reduced by radiation emitted from the region of the contact junctions between the insulator and metal electrode. Practical application of this work has been made in the design of lightning-arrester spark gaps by utilizing common insulating materials with intensified radiation means.

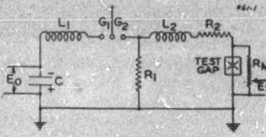


Figure 1. Impulse test circuit

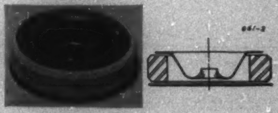
C—One-fourth-microfarad capacitor
E_0—60,000 volts direct current
L_1—140 microhenries
L_2—75 microhenries
R_1—400 ohms
R_2—25 ohms
R_M—Resistance potentiometer
G_1, G_2—Synchronizing gaps
E_1—Voltage for oscillograph plates

Electrical Tests

The test circuit of figure 1 is commonly used for impulse tests on spark gaps. A capacitor bank, C, is charged to 60 kv, just below the breakdown potential of the double gap, G_1, G_2. The breakdown of G_1, G_2, is synchronized with the measuring circuits by means of a timed impulse applied to the middle electrode. The rate of increase of voltage over the test gap is determined by the circuit constants C, L_1, and R_1. After the breakdown L_2 and R_2 assist in regulating the magnitude and wave shape of the surge current. Breakdown potentials are reduced by a tapped noninductive resistor, R_m, for voltage measurements by a cathode-ray oscillograph. All impulse breakdowns were made on a front-of-wave-test in which the surge increased at the average rate of 50 kv per microsecond. This rate of rise is recommended by AIEE Standards No. 28 for arresters of six-kilovolt and under rating. Sixty-cycle breakdown tests were taken with a testing transformer having a calibrated third winding and an induction regulator in the low-voltage winding. In approaching the breakdown voltage the rate of increase of 60-cycle voltages controlled by the regulator is reduced so that if the breakdown lag is long, the time of voltage application is also comparatively long. Experiments have shown a small dependence of the 60-cycle breakdown upon radiation, particularly with polished electrode gaps. However, for the purpose of this paper comparisons are based on the measured 60-cycle breakdown values rather than the true d-c breakdown which is more difficult to obtain.

The procedure in testing a gap was to record eight impulse-breakdown tests at ten-second intervals immediately followed by eight 60-cycle breakdown measurements taken consecutively. The average impulse-breakdown value divided by the average 60-cycle crest breakdown determines the impulse ratio. The impulse ratio is used to compare gaps because of its lesser dependence on small differences in gap spacing. A rough indication of the accuracy on repetition of a gap is obtained by expressing in per cent the lowest and highest readings in the series of eight breakdowns as a fraction of the average value. The column in table I under the heading maximum and minimum consistency compares the various gaps in this way.

A typical enclosed spark gap, figure 2, has a disk and a shaped brass electrode separated by an insulating-ring spacer which completely surrounds the sparking regions. The breakdown characteristics of this gap with the porcelain spacer are listed in table I, line A. When rutile ceramic was substituted for porcelain as the spacer material, the breakdown values measured are given in table I, line B. The main value of the rutile spacer previously anticipated was an improvement in voltage distribution over individual gaps in a multigap arrester. The lower im-



(A) (B)

Figure 2. Typical spark gap with porcelain spacer and metal electrodes

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The author is indebted to Doctor Joseph Slepian for many suggestions and guidance of this work. Messrs. Strausser, A. Keto, and R. P. Shimp aided in the early experimental work. Doctor M. E. Bell helped in the preparation of rutile-ceramic spacers. In particular, M. J. Kofoid contributed valuable assistance in the experimental tests and in the preparation of this paper. The co-operation of A. M. Opsahl, F. B. Johnson, and W. G. Roman, of lightning-arrester engineering group, was very helpful and is appreciated.

* One common mineral form of titanium dioxide.

[1] For all numbered references, see list at end of paper.

Table I. Average Breakdown Characteristics of Enclosed Spark Gaps With Disk and Shaped Electrodes of Figure 2

Line	Spacer	Total Number of Tests	Average Impulse Breakdown	60-Cycle Average Breakdown	Impulse Ratio	Per Cent Consistency: Impulse Max.	Impulse Min.	60 Cycle Max.	60 Cycle Min.
A	Plain porcelain	120	13.0	7.5	1.75	114	81	103	95
B	Rutile ceramic	152	9.2	8.2	1.12	102.5	97.5	101.5	97
C	Leaded porcelain	128	9.5	7.7	1.23	108	93	102	95
D	1.65 × 10^{-6} g radium, plain porcelain	8	8.85	7.35	1.20	105	95	100	98

pulse breakdown obtained with rutile-spacer gaps was so surprising that an investigation was made of this phenomenon.

Rutile

Rutile is a common mineral and is one of the three crystalline forms of titanium dioxide. Powdered rutile can be molded and fired with or without the addition of bonding materials such as china clay. The important difference between rutile ceramic and porcelain is the high dielectric constant of the rutile ceramic. A dielectric constant of 80 to 100 has been measured for rutile ceramics used as spacer materials. Rutile retains its high dielectric constant at frequencies of 10^6 cycles per second.

Electrical discharges appeared at he spacer-electrode contacts during the breakdown tests. This contact voltage is illustrated in figure 3 where under similar conditions higher gradients exist over the air space when a rutile spacer is used. When highly conducting metal films were placed over the rutile spacer-electrode contact areas no change in impulse ratio resulted. Metal films on porcelain spacer contacts lowered the impulse ratio materially as indicated in line *C* of table I.

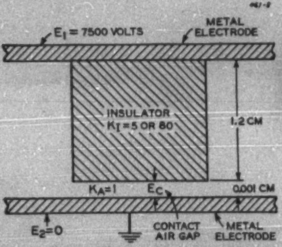


Figure 3. High gradients on assumed-contact air gap with porcelain and rutile spacers

Insulator	K_I(SIC)	E_c(Volts)	Gradient(Kv/Cm)
Porcelain	5	31	31
Rutile	80	470	470

The observed phenomenon with the rutile spacer gaps is similar to the effect of radiations from radium[5,6,7] and ultraviolet radiation[5,8] on the breakdown of spark gaps. Tests were made to see if the rutile contained enough radioactive materials to cause this improvement in impulse breakdown. The discharge rate of an electroscope was measured in room air, in close proximity to a rutile ceramic spacer, and with 1.65×10^{-6} gram of radium on a brass disk two centimeters from the collecting plate. The corresponding discharge rates were:

1. Air — 0.025 centimeter per minute
2. Rutile — 0.038 centimeter per minute
3. 1.65×10^{-6} grams radium — 150 centimeters per minute

A plain porcelain-spacer gap was tested with the radium-treated disk used in the electroscope tests and found to have a low impulse ratio of 1.2, table I, line *D*. It was concluded that the radioactive impurities in the rutile do not exist in sufficient quantities to cause the observed phenomena.

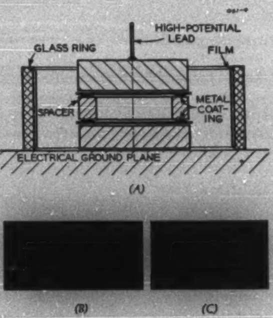
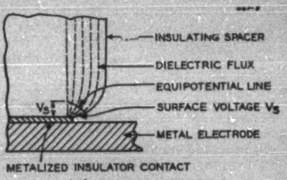


Figure 4. Dark-room tests

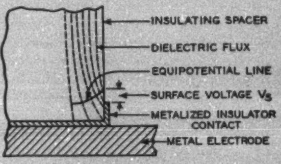
The possibility of the insulating surface emitting a radiation when electrically stressed was suggested and tests were made to see if such radiations were present. A rutile ceramic spacer with sprayed lead contacts was dipped into hot liquid ceresin wax, then cooled so that the entire surface of the spacer was coated with wax. The lead contacts on the rutile spacer were then scraped to remove the wax enough to make good contacts with the metal electrodes. In this scraping it is probable that some of the high-dielectric-constant spacer was exposed in the contact region. A low impulse ratio was obtained with the spacer insulating surfaces covered with ceresin wax. It, therefore, seems improbable that radiation from a stressed insulating surface causes the observed effect.

Barrier Tests

An attempt was made to interpose a barrier between the spacer-electrode junction and the central sparking region, by placing a loose fish-paper cylinder within the spark gap. At first low impulse voltages were obtained, but after changing the paper cylinder to a telescope construction, which was really light-tight, a high impulse ratio was obtained. When a $^1/_{64}$-inch-diameter hole was drilled through the otherwise opaque barrier, the impulse ratio was lowered.



(A) RECESSED CONTACT



(B) OVERLAPPED CONTACT

Figure 5. Potential over insulator surface near metalized contacts

Additional tests were made with picein wax covering the rutile spacer-electrode contact junctions on the inside of the spacer. When both of the electrode-insulator contact regions were covered with wax, high impulse ratios resulted. With only one contact region waxed, low impulse ratios were obtained. A contact layer of sprayed metal was brought up the inside of the rutile spacer and the contact regions waxed but with a small ring of exposed lead extending beyond the wax. Tests showed low impulse ratios when the lead film was exposed.

Dark-Room Tests

A bare X-ray film was located around a rutile spacer with sprayed lead contacts and two disk electrodes as shown in figure 4*A*. No blackening appeared when this gap was energized at 7,500 volts, 60-cycle frequency, for several hours. One hundred short-time chopped surge waves of ten kilovolts crest gave no trace of radiation on the film. When the gap of figure 4*A* was excited to 8,000 volts crest at 540 kilocycles for one hour the X-ray film obtained is reproduced in figure 4*B*. The whole film was diffusely blackened but two intensely blackened lines appeared as a result of radiation from the contacts. A picture of the contact discharges was made with Zeiss camera (approximately *f*5) with a two-minute exposure in a dark room using the fastest available commercial film. The contact discharges were greatly amplified by increasing the voltage to just under the insulator flashover value. The picture in figure 4*C* shows some indication of the localized nature of the contact discharges. One flashover took place during the exposure and is visible in the picture. The two bands of light in the picture are the result of several points of light from contact discharges that wandered around irregularly on the periphery of the rutile spacer at the lead-sprayed film outside edges.

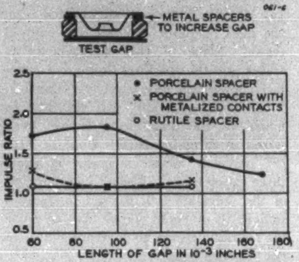


Figure 6. Variation in impulse ratio obtained by increasing gap distance while maintaining constant dielectric thickness

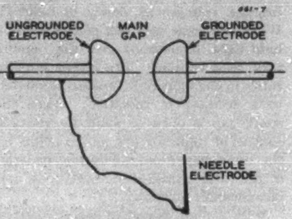


Figure 7. Three-point spark gap

Radiation From Contact Discharges

It is now obvious that the lower impulse-breakdown values obtained by the use of rutile spacers are due to a radiation in the gap coming from localized electric discharges at or near the inside contact junctions of the insulator and electrode. The radiation is present in abundant quantities to produce the effect through a $^1/_{64}$-inch-diameter hole. The discharges are attributed to stress concentrations and are present with highly conducting metal contact films. Two ways of producing stress concentrations with typical spacer shapes are shown in figure 5. It is not necessary to postulate imperfections in the surface of the dielectric to explain high air gradients. A metal contact film on the rutile spacer reduced the visible radiation intensity so that the contact discharges could not be seen in a dark room with careful observation under normal gap stress. Under this reduced visible intensity the impulse ratio was equally low. In the case of porcelain spacers the lead contact films definitely lowered the impulse ratio. This is attributed to the more favorable location of the radiation source on the porcelain.

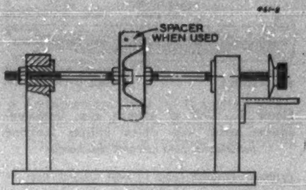


Figure 8. Externally supported spark gaps

That porcelain spacers may be made to radiate with greater intensity so as to lower still further the impulse ratio was proved by testing the porcelain gap at increased gap spacings which effectively increased the working stress of the porcelain. In the curves of figure 6 are plotted the impulse ratio as a function of the gap spacing where the gap spacing was increased by the addition of metal-ring spacers on top of the various insulating spacers. At about three times the normal gradient the plain porcelain spacer gap has an impulse ratio of 1.2.

The Three-Point Spark Gap

The impulse breakdown potential of a spark gap can be lowered by the three-point effect first explained by C. E. Wynn-Williams[9] in 1926. A typical three-point gap is reproduced in figure 7. When the sharp point of the needle was placed within six centimeters of the ungrounded electrode and electrically connected to it so that it was "visible" to the gap, a lower surge breakdown voltage was obtained. All barriers when placed in front of the point neutralized this effect except a specially prepared thin celluloid film. The effect was obtained in a dark room where no discharge could be seen to pass

Table II. Tests With Electrodes Supported Externally—Figure 8

Spacer	Electrodes	Gap (Mm)	Surge Breakdown (Kv)	60-Cycle Breakdown (Kv)	Impulse Ratio	Average Surge Gradient (Kv/Cm)	Average 60-Cycle Gradient (Kv/Cm)
None—open to room	Brass disk and shaped	1.65	14.5	8.84	1.64	88	53.5
None—in light-tight box		1.65	17.6	9.42	1.87	107	57.0
Plain porcelain		1.65	13.2	7.68	1.72	80	46.5
Leaded-contact porcelain		1.90	10.8	7.93	1.36	57	41.7
Leaded-contact rutile		2.11	9.08	8.48	1.07	43	40.2
None—open to room	3.8-cm brass* polished spheres	1.65	14.5	9.64	1.50	88	58.3
None—in light-tight box		1.65	24.4	9.29	2.63	148	56.3

* One hour between polishing and testing.

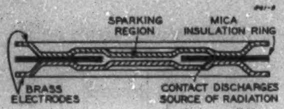


Figure 9. Unit of quench gap, lightning-arrester spark gap which utilizes ionizing radiations from contact discharges

Average impulse ratio of unit quench gap = 1.05

from the needle point to an electrode. His conclusions were that:

"1. Before the three-point phenomenon can be obtained some form of discharge, silent or spark, must be 'visible' from the main gap and within a certain distance of it.

"2. The phenomenon is obtained in the absence of:

"(a). Disturbance of the electric field near the main gap and

"(b). The passage of ions from the pilot discharge into the main gap.

"3. A radiation is emitted by the pilot discharge which can ionize the gas in the main gap.

"4. When this radiation is prevented from reaching the main gap the usual three-point effect is not obtained.

"5. The three-point effect is still produced when the electrodes are so shielded as to prevent any photoelectric effects...

"6. The observed properties of this radiation seem to establish the limits of its wave length as being approximately 13 to 1,000 angstroms, and they coincide to a large extent with the corresponding properties of *entladungstrahlen*."

As these conclusions so thoroughly explain the rutile-spacer spark-gap tests, it seems safe to conclude the same phenomena must be present in both cases.

Recent Work

H. Rather[10] has measured the absorption coefficient of similar radiation from a spark discharge. He believes the gas-ionizing radiation to be between the limits of 800 to 1,000 angstroms. By means of a Wilson cloud-chamber apparatus he has measured and extrapolated the absorption coefficient of the effective radiation from sparks to be $\mu=2$ for air, $\mu=0.8$ for hydrogen, and $\mu=5$ for oxygen, at 760-millimeter pressure.

Mounted-Electrode Tests

To observe the performance of a gap without short insulating spacers the disk and shaped electrodes were mounted on an adjustable micrometer spark gap as shown in figure 8. The electrode sparking surfaces were first polished, cleaned, and then roughened with fine alundum paper. The roughening process was repeated before each set of tests. The gap spacing was set equal to that of the porcelain gap of figure 2, and tested with the electrodes open to room air. The impulse ratio measured was 1.64 with 60-cycle and impulse voltages as shown in table II. When the same gap was placed inside a light-tight maplewood box the impulse ratio rose to 1.87. The higher impulse ratios in the light-tight box are attributed to a decrease of radiation. When the spark gap is open to room air, possible sources of radiation are corona points on the surge generator, radioactive material in the room, atmospheric radiations, and incandescent filaments.

With a plain porcelain spacer inserted between the electrodes of figure 8, the impulse ratio measured was 1.72. As the porcelain spacer makes the gap relatively light-tight some radiation must be present normally in the plain porcelain gap. The lower breakdown gradients recorded in table II for this test are added evidence of radiation. When a sprayed-lead porcelain spacer was tested in the same gap structure the impulse ratio was 1.36. This low value is attributed to a more favorable location of the radiation present in the porcelain-spacer gap. A leaded rutile spacer in the same gap structure gave an impulse ratio of 1.07. The breakdown gradients are low, indicating a greatly increased intensity of radiation.

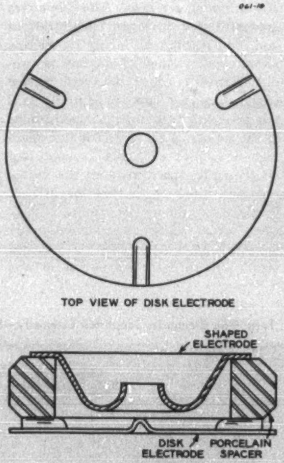


Figure 10. "Ionization" gap

Average impulse ratio of single ionization gap = 1.2

Two 3.8-centimeter spherical electrodes were mounted in the micrometer support and tested open to the room and in the dark box. The results listed in table II show a similar increase in impulse ratio for spherical electrodes in the light-tight box.

Applications

A single unit of the "quench gap" construction[11] is shown in figure 9. In this gap effective radiations are emitted from the mica insulator and electrode-contact region. Some of this radiation penetrates into the sparking region and effectively lowers the time lag of breakdown.

In the "ionization" gap sketched in figure 10 the disk electrode has been raised in three locations. Contact with the porcelain spacer is made on these three raised lands. The air in the region of the porcelain-disk contact is stressed sufficiently on rapid voltage rises to cause the sparking region to be irradiated as in the "quench gap."

Conclusions

The impulse ratio of spark gaps with spacings of between one and two millimeters depends upon the radiation present in the gap. An effective radiation is present in the usual porcelain-enclosed gap used in lightning arresters. This radiation is emitted from electrical discharges at the insulator-electrode junctions. A high-dielectric-constant spacer gives low impulse ratios, probably because the intensity of the effective radiation is proportional to the displacement current in the insulator. The wave length of the most effective part of a similar radiation was determined by Wynn-Williams in a study of the three-point spark gap as lying between 13 and 1,000 angstroms. Experiments in this region of the soft X-ray spectrum are difficult due to rapid air absorption.

The discovery of this radiation in porcelain-enclosed spark gaps explains why porcelain space gaps have lower impulse ratios than plain spark gaps of the same spacing in the dark. The results of this study fully explain the reasons for the low impulse ratios obtained in the "quench gap" and the "ionizing gap" which are both utilized in commercial lightning arresters.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Water Solution in High-Voltage Dielectric Liquids

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IT HAS long been recognized by the practical engineer responsible for the proper maintenance of high-voltage oil-filled electrical equipment that water constitutes one of the chief and in some instances the only serious threat to successful service. However, consideration of the dangers presented by water during the operation of high-voltage machines must not be limited to a study of the relation between the dielectric strength and the water content of the insulating liquid used. For successful operation, the cellulosic insulation must be maintained in a dry condition. Water in such dielectrics leads ultimately to insulation failure. One of the first lines of defense against this disastrous result is the adequate drying of the solid dielectric in the factory manufacture of the equipment. Just as important, however, is the clear recognition that cellulosic insulation may be brought to a hazardous condition in service by water absorption from the surrounding atmosphere through the insulating liquid. The sorption of moisture by cellulosic insulation in this manner is of importance to all types of liquid-treated electrical apparatus but is of especial importance to liquid-cooled transformers in which the circulation of the liquid, carefully factored into the design for cooling purposes, so effectively presents a continuous supply of dissolved moisture to the hygroscopic insulation of the windings.

The dielectric-strength test of insulating liquids has long been accepted as an adequate protection against dielectric deterioration produced by water, but such a test as used in practice gives no adequate protection against the insidious penetration of water and its sorption by the cellulosic insulation. The practical dielectric strength represents the summation of many deteriorating effects, including the effect of fibers, dust, and other suspended or dissolved materials. The effect of these materials becomes greatly pronounced in the presence of water[1–4] and their prevalence in normal commercial practice has necessitated the establishment of dielectric-strength standards of such value that dissolved moisture may unsuspectedly be present in an amount which, because of its continuous transfer to the liquid-immersed cellulose insulation, may be sufficiently large seriously to affect the insulating properties of the latter and to present a dangerous condition for commercial use.

A Laboratory Test for Quantitatively Determining the Amount of Dissolved Water in Insulating Liquids

A number of test methods have been suggested for quantitatively determining the amount of water dissolved in mineral oil and other organic liquids.[5] The method adopted in this study has been a modification of the procedure suggested by Smith and Bryant,[6] the water content being expressed in parts per million by weight.

Dry nitrogen gas is bubbled through 500 to 1,000 grams of the carefully weighed insulating liquid at 130 degrees centigrade for a period of three hours. From the insulating liquid the nitrogen gas is led through two liquid-air traps where the entrained moisture is removed. The frozen condensate in the traps is dissolved in 25 cubic centimeters of dry acetone and slowly added to a cold acetyl chloride-pyridine mixture. In the preparation of this mixture, 10 cubic centimeters of acetyl chloride dissolved in dry benzene (118 grams per liter of solution) are contained in a dry 250-cubic centimeter glass-stoppered flask and cooled in an ice bath. Two cubic centimeters of dry pyridine are added dropwise to the acetyl chloride solution with continual shaking. A white precipitate of acetyl chloride-pyridine complex is formed.

After the slow addition of the acetone condensate solution to the ice-cold acetyl chloride-pyridine complex has been completed, the mixture is removed from the ice bath and allowed to stand at room temperature in diffused light (not in direct sunlight) for at least 15 minutes, but not more than 30 minutes, after which 20 cubic centimeters of dry ethyl alcohol are slowly added with continuous shaking. After a further standing at room temperature for five minutes, the solution is ready for titration with 0.5 *N* sodium hydroxide solution using phenolphthalein indicator.

A blank run is made simultaneously with the sample, using completely duplicate conditions except that no frozen distillate is added. This allows correction to be made for errors due to moisture inadvertently present.

In the actual application of water-testing procedures for insulating liquids, mineral oil is one of the more important insulating liquids tested. As is well

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1. For all numbered references, see list at end of paper.

known, this material will be found in commercial apparatus in all stages of oxidation. This oxidation results in the formation of volatile acidic materials. Such products entrapped in the refrigerated tubes of the water-testing apparatus, will in themselves react with acetyl chloride or the sodium hydroxide used for the titration and produce erroneous results. For mineral transformer oils in normally good condition, the correction factor is negligible but for oils in the higher stages of oxidation a correction in the water content value must be applied. This correction corresponds to 7.3 parts per million of water content for each 1.0 milligram of potassium hydroxide per gram of oil used in the acidity titration.

The Solubility of Water in Dielectric Liquids

The amount of water dissolved by a liquid is a function of the chemical nature of the liquid itself and the conditions to which it is exposed. In the case of mineral insulating oil, the degree of refining and the previous oxidation to which it has been subjected are factors of real importance. Figure 1 illustrates the effect of refining treatment on the solubility of water in mineral transformer oils of low cold test.

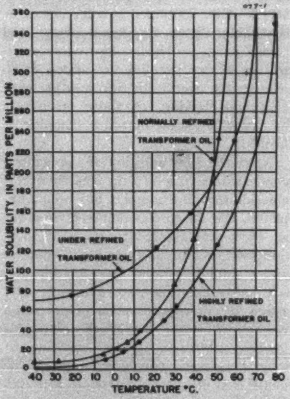


Figure 1. The solubility of water in mineral transformer oil and the effect of the refining treatment

In the use of mineral oils, oxidation effects must not be ignored when water-solubility problems are considered. Oxidation promotes the emulsification of water and leads to increased water solubility in the oil. Unless otherwise stated, only new, unoxidized mineral oils are used throughout this paper.

Synthetic, noninflammable, dielectric transformer liquids in commercial use are the chlorinated derivatives of the benzenoid hydrocarbons. These compounds are characterized by a high resistance to oxidation and are, therefore, not susceptible to changes in water solubility during use. This fact is demonstrated in the data of figure 2 which describe the solubility of water in new and used Pyranol.

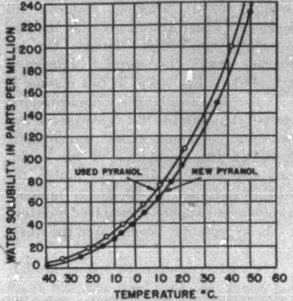


Figure 2. The solubility of water in transformer Pyranol

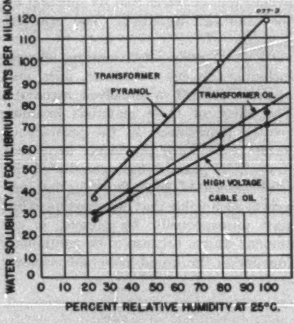


Figure 3. The maximum amount of water absorbed by transformer and cable insulating liquids exposed to humid air

The Hygroscopicity of Dielectric Liquids

The data presented in figures 1 and 2 are of interest and importance in indicating the limiting values of solubility beyond which water cannot accumulate in the liquid without visible cloud formation. Such values, however, give no indication of the sorptive ability of the liquid when exposed to less than water saturated atmospheres as in normal handling and use.

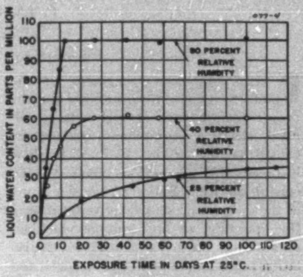


Figure 4. The absorption of water from humid air by transformer Pyranol

Figure 3 illustrates the water solubility in transformer liquids at equilibrium with air, the water content of which is expressed in the commonly used terms of relative humidity. Figure 3 also compares the water sorptive characteristics of a typical, American, Pirelli-type, high-voltage mineral cable oil. The comparative Saybolt Universal viscosity values at 37.8 degrees centigrade for the three liquids are:

Transformer Pyranol	55 seconds
Transformer mineral oil	58 seconds
Pirelli cable oil	100 seconds

The water equilibrium set up by an insulating liquid and the air with which it may be in contact is of importance in many commercial uses. In the handling of insulating liquids, however, it is also important to know the rate at which water is taken up by liquid. This is an extremely difficult practical problem since the speed with which a liquid approaches its water equilibrium condition is dependent on a large number of empirical factors, prominent among which is the intimacy of contact between the liquid and the humid atmosphere from which the water is taken. Large surface exposure produced by liquid circulation or other means accelerates water absorption. Figures 4 and 5 illustrate the rate of water absorption for mineral transformer oil and for transformer Pyranol exposed to air of different humidities. In this work the liquids were exposed in a quiescent state under the following conditions:

Volume of liquid, 7,500 cubic centimeters
Surface area exposed, 375 square centimeters
Liquid depth, 20 centimeters
Temperature of exposure, 25 degrees centigrade

The rate at which water is taken up by mineral oil is but little affected by those physical and chemical changes accompanying increased oil viscosity. This is illustrated in the data of figure 6 which compare three mineral oils of the Gulf Coast crude type, with viscosities which range from 58 seconds to 195 seconds Saybolt Universal at 37.8 degrees centigrade.

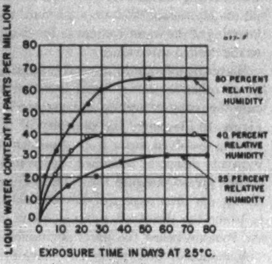


Figure 5. The absorption of water from humid air by mineral transformer oil

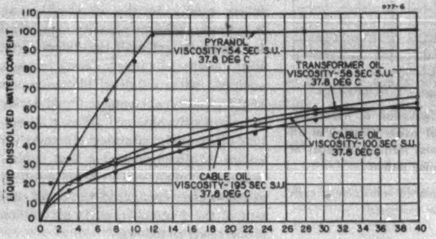


Figure 6. Comparing the water absorption by insulating liquids of different viscosity when exposed to humid air at 25 degrees centigrade

The Transmission of Water Through the Insulating Liquid and Its Sorption by the Cellulosic Insulation Immersed Therein

Cellulose has been described as a very hygroscopic material whose ability to absorb water is comparable to even the best of chemical desiccants.[7] It is, therefore, not unexpected to find that dry cellulose immersed in wet oil, reduces the water content of the latter. The rate at which the water is transferred from wet oil to the dry cellulose depends on factors similar to those which apply in the transfer of water from humid air to dry oil. Chief among these is the intimacy of contact between the cellulose fiber and the oil. Factors causing oil circulation, thus bringing a continuous supply of wet oil into contact with the paper insulation of a typical transformer-coil winding, will accelerate the moisture removal from the oil. Figure 7 illustrates the water transfer for one specific setup in which the dried cellulosic insulation was immersed in wet transformer liquid maintained without circulation at 25 degrees centigrade.

The data of figure 7 were obtained by humidifying the transformer liquid at 25 degrees centigrade. Kraft insulating paper (0.001 inch) was dried at 110 degrees centigrade under a pressure of 0.3 millimeter of mercury for 24 hours. The dry paper weighed 40 grams and was in the form of a cylindrical tube. The vacuum-dried paper, approximately 15 square feet in area, was immersed in 1 1/2 quarts of the wet liquid contained in a two-quart sealed, glass container which was allowed to stand without agitation. Samples of the liquid were withdrawn at frequent intervals for measurement of the dissolved water content.

Figure 7 is of value in indicating the rapidity with which dry cellulosic insulation will absorb moisture from the wet insulating liquid of a transformer. In obtaining the data of figure 7, it must be emphasized that there was a limited supply of water available that held by the insulating liquid. As the water was removed from the latter, its water content dropped until after about four days for the mineral transformer oil and 16 days for the transformer Pyranol an equilibrium was established between the water-in-liquid and water-in-cellulose condition. This equilibrium was substantially independent of the nature of the insulating liquid.

Figure 8 illustrates the water transfer occurring when wet insulating paper is immersed in dry transformer liquids. These data were obtained during a test run in which 40 grams of paper, prepared by exposure for 48 hours to air at 70 per cent relative humidity, 32 degrees centigrade, were immersed in the shape of a cylindrical tube in 1 1/2 quarts of dry insulating liquid, maintained throughout the test in a sealed container at 25 degrees centigrade. It is obvious from the data of figures 7 and 8 that the direction of water transfer, whether from liquid to cellulose or cellulose to liquid, is dependent on the relative moisture content of the materials involved.

In a sealed container, once the equilibrium condition is reached between the water content of the cellulose and that of the insulating liquid, it cannot be considered in an easily changed condition. This is illustrated in the data of figure 9. Here the 25-degrees-centigrade data of figure 7 for dry paper in wet mineral transformer oil and that of figure 8 for

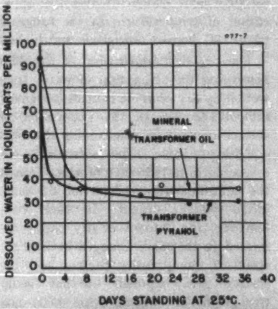


Figure 7. The dehydration of wet insulating liquids as a result of moisture absorption by dry cellulosic insulation immersed therein

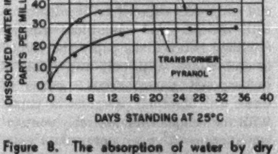


Figure 8. The absorption of water by dry insulating liquids in contact with cellulosic insulation previously exposed to humid air

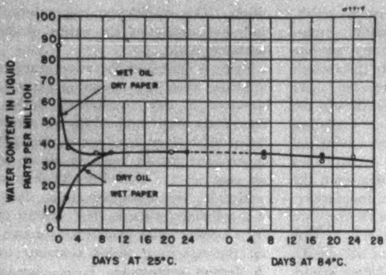


Figure 9. The characteristics of the moisture equilibrium set up between mineral transformer oil and the impregnated cellulose insulation

wet paper in dry mineral transformer oil are plotted together. After the common equilibrium has been established, each setup is heated from a 25- to an 84-degree-centigrade liquid temperature. The equilibrium is substantially unchanged. This behavior is of real importance in transformer practice for it indicates the danger which results when moisture is allowed to be dissolved by the insulating liquid. When once the moisture is absorbed by the impregnated cellulose, unless it be present in abnormally large amounts, it is released again only by drastic treatment which involves the application of temperatures in the range of 95 to 100 degrees centigrade and higher.

The practical application of the data describing the absorption of water from wet oil by dry cellulose is made extremely difficult because of the importance of surface contact in determining the final equilibrium value. This is demonstrated in a series of tests in each of which is used the same amount of oil having the same initial water content. In each test, however, the amount of dry paper immersed in the oil is changed both in respect to the quantity present and the surface area exposed. In one setup two grams of vacuum-dried 0.001-inch kraft paper were immersed in the oil so that the total surface area of 1.50 square feet was entirely bathed by the liquid. In a second setup, 40 grams of the same vacuum-dried paper with a total surface area of 30 square feet were assembled in a cylindrical-shaped roll and immersed in the oil so that only the outer roll surface and ends were exposed to direct contact with the oil. An equilibrium was established between the paper and the oil in each case. With only two grams of well-exposed paper, 0.0082 grams of water was absorbed per gram of paper. With the compact roll assembly containing 40 grams of paper, only 0.0014 gram of water was absorbed per gram of paper.

The Dehydration of Insulating Liquids

In the data already presented, it will be observed that in no instance has the presence of initially dry paper been able to reduce the water content of the dielectric liquid to zero. An equilibrium is ultimately set up which recalls the equilibrium generally recognized to exist between the water content of paper and of air. In the drying of cellulosic insulation, it is necessary to upset this equilibrium which is done in common practice by the use of heat and vacuum. The fact that an equilibrium also exists between water-in-liquid and water-in-cellulose indicates clearly that under some conditions as for example in the data of figure 8, the cellulose may impart water to the liquid. In like manner, an improperly dried blotter paper used in the filter-press treatment of the dielectric liquid, although removing suspended particles, may even lead to an increased oil-dissolved water content. It is further obvious that unless completely dried filter papers are successively supplied in the filtering treatment, the equilibrium ultimately established may prevent the lowering of the water content of the liquid to the desired safe value.

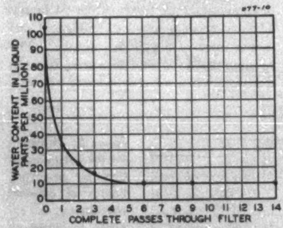


Figure 10. The dehydration of transformer Pyranol by means of paper filtration at 25 degrees centigrade with frequent changes of the filtering papers

Used papers were replaced at the time of each test value indicated

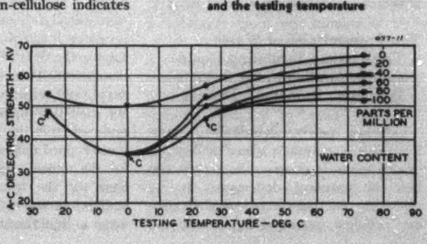
Water in solution in mineral insulating oil or other dielectric liquid is removed only by careful and severe treatment. Figure 10 illustrates the difficulty of removing dissolved water from transformer Pyranol using a standard seven-inch blotter press. Despite the extensive use of new, dry filter papers, it was impossible to lower the dissolved water content of the Pyranol below ten parts per million.

A modified filter press has been designed[8] which incorporates frames much deeper than those of the usual plate and frame press. These deeper frames give spaces which are filled with 80/300-mesh fullers earth. In the standard seven-inch press, sufficient capacity is provided for about one gallon of the fullers earth. With such a "fullers earth" press, equipped with well-dried blotter papers of the usual type, complete liquid drying is more easily obtained. Using 30 gallons of Pyranol containing 116 parts per million of dissolved water, and a filter containing fullers earth previously dried at 200 degrees centigrade, the water content of the Pyranol in parts per million is decreased as follows:

Initial water content	116
After one pass through filter	9
After one-half hour circulation through the filter	0

It is customary in some instances to

Figure 11. The rapidly applied a-c dielectric strength of mineral transformer oil as a function of its dissolved and suspended water content and the testing temperature



Points marked C indicate that the oil contains suspended as well as dissolved water. All dielectric strength tests were taken with a 0.20-inch gap setting degas mineral oil and other insulating liquids immediately after filter press or centrifuge treatment by passing the material through a vacuum chamber in the form of a thin film or spray. The assumption is usually made that such deaeration rapidly and effectively eliminates the dissolved water. This is not necessarily true. Mineral oil, even to the point where the dissolved air content is reduced to less than one per cent may still retain substantially large amounts of dissolved water. This was demonstrated in one form of commercial degassing equipment using mineral transformer oil containing 70 parts per million of water. After the degassing treatment, the oil still contained 46 parts per million of dissolved water.

A Practical Method for the Quantitative Estimation of Dissolved Water

A simple method for estimating the amount of water in solution in an insulating liquid involves the determination of its cloud point. Carefully carried out, the cloud point indicates that temperature below which the water in solution exceeds the saturation value of the liquid and separates with the formation of faint cloud. This water separation becomes pronounced and forms a turbid suspension in the liquid as the temperature is further lowered. This test procedure is limited to those materials free or substantially free from wax so that the formation of a cloud due to wax separation is avoided in the range of temperature of interest in dissolved-water determinations. Since, in general, the dielectric liquids in common use are of the naphthenic type of mineral oil or are synthetic benzenoid compounds free from wax separation on cooling, this limitation is not of serious significance.

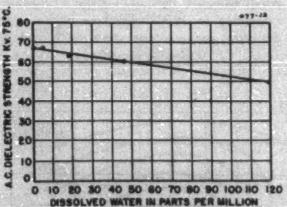


Figure 12. The effect of dissolved water on the rapidly applied a-c dielectric strength of mineral transformer oil tested at 75 degrees centigrade

All dielectric strength tests were taken with a 0.20-inch gap setting

The cloud-formation temperature relation to the liquid dissolved-water content is obtained from figure 1 for mineral transformer oil and from figure 2 for transformer Pyranol. The dissolved-water contents shown in these figures are those of the water-saturated condition for the corresponding temperatures. These temperatures are, therefore, the cloud-point values since further increase in water content or a further decrease in temperature results in the formation of a visible cloud.

The difficulty in the cloud-point test is that, except for the highest water contents, the test involves refrigeration with accompanying frost formation on the walls of the containing vessel and the necessity of protecting the liquid under test from water pick up due to frost formation on its surface. A suggested form of test apparatus utilizes a clear vacuum bottle within which the liquid under test, contained in a clear-glass stoppered tube, is cooled at a standardized rate in accordance with principles of the method of test for the cloud and pour point of mineral oils, described under designation *D*97-34 of the American Society for Testing Materials.

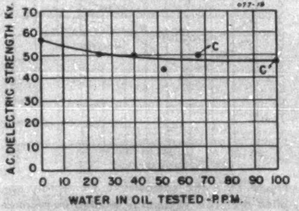


Figure 13. The rapidly applied a-c dielectric strength for mineral transformer oil tested at 25 degrees centigrade as a function of its dissolved and suspended water content

Points marked C indicate that the oil contains suspended as well as dissolved water. All dielectric strength tests were taken with a 0.20-inch gap setting

The Effect of Dissolved Water on the A-C Dielectric Strength of Insulating Liquids

Repeated researches have indicated that the dangerously low dielectric strengths normally attributed to the presence of water in commercial insulating liquids are to be associated as well with the accidental presence of suspended solids such as dust particles, fibers, and the like.[1-4] It is undoubtedly true that mineral oil or other insulating liquid may contain appreciable amounts of dissolved water and, in the absence of suspended impurities, still retain what is commercially regarded as a good dielectric strength. This results in the fact, already indicated, that the dielectric-strength test as commercially applied does not give good protection against the insidious penetration of moisture through the liquid to the hygroscopic cellulosic insulation immersed therein. Irrespective of its effect on the dielectric properties of the liquid, unlimited penetration of moisture and its sorption by the cellulose must be avoided if satisfactory commercial operation of high-voltage apparatus is to be obtained.

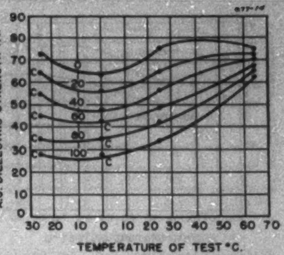


Figure 14. The effect of temperature and dissolved and suspended water in the a-c rapid applied dielectric strength of transformer Pyranol

Points marked C indicate that the oil contains suspended as well as dissolved water. The figures on the curves indicate the total water content of the liquid. All dielectric strength tests were taken with a 0.20-inch gap setting

Because of the unavoidable variation in water content during the handling and testing of an insulating liquid, it has been most convenient to determine the dielectric strength of the liquid for at least four water-content values covering the range of interest and from the smooth line (average) relation established, to select definite values of water content for comparison and further technical analysis. Thus, for example, the 75-degree-centigrade data of figure 11 are based on the smooth-line relation of figure 12 which describes the dielectric strength of mineral transformer oil at 75 degrees centigrade as a function of its dissolved-water content, the values indicated being experimentally determined. In general, the basic experimental data correlating the dielectric strength and the water content of the insulating

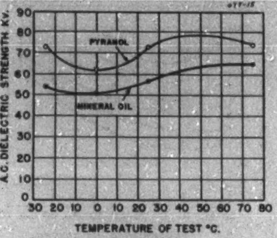


Figure 15. The comparative a-c rapidly applied dielectric strength of dry transformer Pyranol and dry mineral transformer oil

All dielectric strength tests were taken with a 0.20-inch gap setting

liquid are eliminated for reasons of brevity. The analyses which are shown are based on selected points taken from the smooth-line relation correlating the experimentally determined values.

The dielectric test results were obtained using brass electrodes meeting the requirements of the American Society for Testing Materials, designation *D*117-33. The gap distance used throughout was 0.20 inch. Temperatures recorded are the liquid temperature values at the time of test. In all instances, no dielectric-strength readings were made until the liquid had been at the testing temperature for several hours. The water-content values given include suspended as well as liquid dissolved moisture. The presence of suspended water is indicated.

The rapidly applied dielectric strength has been determined in accordance with American Society for Testing Materials, designation *D*117-36, in which the voltage is evenly raised to breakdown at the rate of three kilovolts per second.

To define adequately the effect of dissolved and suspended water on the dielectric strength of an insulating liquid, it becomes evident at once that the temperature of test must be carefully specified. Practically speaking, a wide range of testing temperatures must be covered for liquid treated insulations are often used in high-voltage dielectric practice specifically because of the stability of their dielectric properties over a wide range of operating temperatures. In these tests, temperatures from −25 to +75 degrees centigrade have been studied.

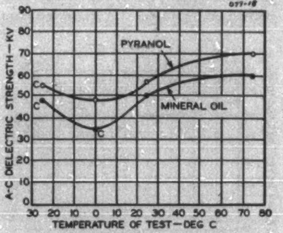


Figure 16. The comparative a-c rapidly applied dielectric strength of mineral transformer oil and transformer Pyranol, each containing 40 parts per million of dissolved water

Points marked C indicate that the oil contains suspended as well as dissolved water. All dielectric strength tests were taken with a 0.20-inch gap setting

It has been pointed out in a previous publication[8] that the dielectric strength of mineral insulating oil as affected by temperature varies from oil to oil. With some oils, notably the viscous cable oils, the dielectric strength falls with temperature increase above room temperature. With mineral oils of the transformer type, the dielectric strength increases to a maximum with temperature increase above room temperature. The dielectric strength-temperature relation has been considered to reflect the solubility characteristics of dissolved air. In this study of the dielectric strength of mineral oil as affected by the dissolved and suspended water content, available data are limited to the behavior of air-saturated mineral transformer oil prepared from Gulf Coast crude and having a viscosity at 37.8 degrees centigrade of approximately 58 seconds Saybolt Universal. With such an oil, the effect of temperature increase on the dielectric strength is a function of its water content. The dielectric strength increases most rapidly with temperature increase from 25 to 75 degrees centigrade when the water content of the oil is held at low value. This is illustrated in the data of figure 11.

In any study of the effect of water on the dielectric strength of mineral oil, difficulty is at once experienced because of water separation as the temperature is lowered. This limits the study of the effect of dissolved water to a narrow range of water contents as the lower temperatures are investigated. Thus, as is shown in figure 12, even with a water content as large as 120 parts per million, clear solutions are obtained at 75 degrees centigrade and the decrease in dielectric strength with increased water content is easily demonstrated. At 25 degrees centigrade, however, the limit of water solubility as indicated in figure 1 for

Figure 17. The rapidly applied direct-voltage dielectric strength of mineral transformer oil as affected by tem perature and its dissolved and suspended water content

Points marked C indicate that the oil contains suspended as well as dissolved water. All dielectric strength tests were taken with a 0.20-inch gap setting

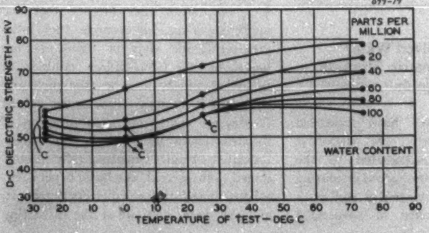
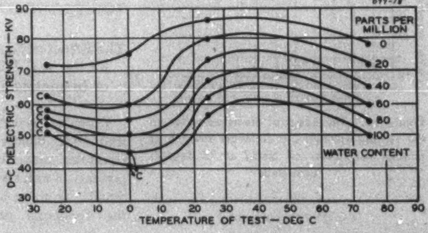


Figure 18. The rapidly applied direct-voltage dielectric strength of transformer Pyranol as affected by temperature and its content of dissolved and suspended water

Points marked C indicate that the oil contains suspended as well as dissolved water. All dielectric strength tests were taken with a 0.20-inch gap setting



normally refined transformer oil is about 70 parts per million. The higher water contents of figure 11 are, therefore, cloudy suspensions. It is only with wet oil free from suspended water that a clear effect of dissolved water on the dielectric strength can be demonstrated. The dielectric strength for oil containing suspended water is independent of the water content within the limits explored (figure 13). For tests at lower temperatures similar results are obtained.

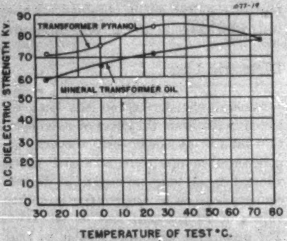


Figure 19. The comparative dielectric strength of dry mineral transformer oil and dry transformer Pyranol tested under direct-voltage application

All dielectric strength tests were taken with a 0.20-inch gap setting

Synthetic, noninflammable transformer liquids of the Pyranol type have already been described as possessing a maximum dielectric strength at about 50 degrees centigrade.[10] As illustrated in figure 14 this is true for such liquids only when containing small amounts of dissolved moisture. With increased water content the dielectric strength rises continuously with the increasing temperature of test.

Insulating liquids of the Pyranol type, as shown in figure 14, are characterized by a continually decreasing dielectric strength with increased water content. No abnormality in the relation is observed at the point of cloud formation, indicative of water separation from solution.

The Pyranol type of transformer insulating liquid in normally dry condition possesses a dielectric strength which is superior to that of mineral transformer oil. Figure 15 compares the dielectric strength of these two typical transformer liquids as affected by temperature when dry. Figure 16 compares the dielectric strengths of the two liquids each containing 40 parts per million of dissolved water.

The Effect of Dissolved Water on the D-C Dielectric Strength of Insulating Liquids

Under rapidly applied direct voltage, transformer insulating liquids behave in a manner similar to that already described for a-c breakdown. Figure 17 describes the d-c breakdown of mineral transformer oil as a function of temperature and water content. The general resemblance to the a-c breakdown values of figure 11 is marked. The effect of dissolved water is most clearly demonstrated in the 75-degree-centigrade tests. And again, as in the alternating-voltage breakdown, the formation of a cloud in the oil tends to obscure the effect of further water additions.

Figure 18 describes the d-c dielectric-strength characteristics of transformer Pyranol. As in the a-c breakdown data of figure 14, the effect of water content on the dielectric strength is marked.

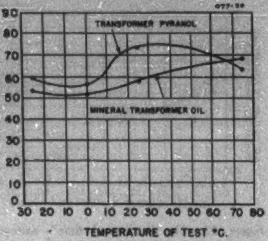


Figure 20. The comparative dielectric strength of mineral transformer oil and transformer Pyranol each containing 40 parts per million of dissolved and suspended water and each tested under rapidly applied direct-voltage application

All dielectric strength tests were taken with a 0.20-inch gap setting

Figures 19 and 20 compare the d-c breakdown characteristics of mineral transformer oil and transformer Pyranol. Figure 19 compares the materials in the dry condition. Figure 20 compares the materials each containing 40 parts per million of water. The dielectric strength of transformer Pyranol is generally higher than the dielectric strength of mineral transformer oil except at 75 degrees centigrade, at which temperature the two liquids appear of equivalent dielectric strength under direct-voltage tests.

The Time-Voltage Relation

The time-voltage characteristic applying to an insulating liquid is a property of difficult determination because of the sharp "knee" which is obtained in the relation at a relatively short time interval. That part of the time-voltage relation which parallels the time axis and which designates the voltage that can be

Figure 21. The time–alternating (effective) voltage relation for transformer Pyranol as affected by the dissolved and suspended water content

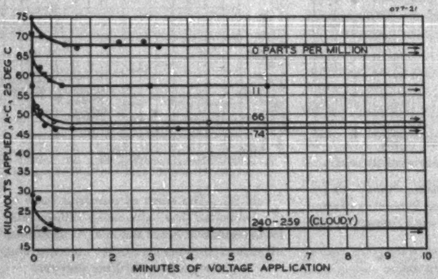
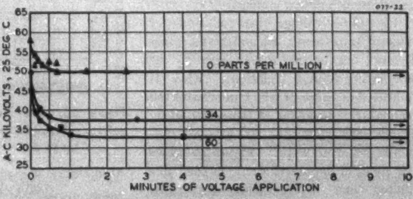


Figure 22. The time–alternating (effective) voltage relation for mineral transformer oil as affected by the dissolved water content

An arrow indicates that repeated applications of the voltage shown gave no dielectric breakdown within the time limitations of the test.

All dielectric-strength tests were taken with a 0.20-inch gap setting



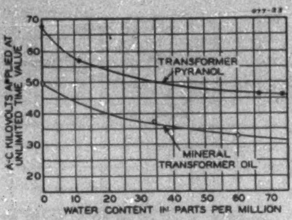


Figure 23. The "unlimited time" voltage value for transformer Pyranol and mineral transformer oil as affected by the water content of the liquid

All dielectric-strength tests were taken with a 0.20-inch gap setting

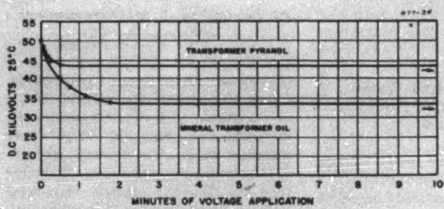


Figure 24. The time – direct-voltage relation for transformer Pyranol and mineral transformer oil each containing 60 parts per million of dissolved water

An arrow indicates that repeated applications of the voltage shown gave no dielectric breakdown within the time limitations of the test. All dielectric-strength tests were taken with a 0.20-inch gap setting

applied without fear of breakdown within the experimental time limit, is called the "unlimited time" value of voltage. In general, the maximum time allowed for breakdown in the work described has been limited to ten minutes. The electrodes used were duplicates of those employed in the rapidly applied test investigations and were spaced 0.20 inch apart. All tests were at 25 degrees centigrade. Both direct and alternating (effective) voltages have been studied.

Figures 21 and 22 describe the time-voltage (a-c) relation for transformer Pyranol and mineral transformer oil as affected by the dissolved and suspended water content. Figure 23 compares the effect of dissolved water on the "unlimited time" voltage values for mineral oil and Pyranol. The relations established are of marked similarity.

The direct-voltage–time relation for mineral transformer oil and transformer Pyranol bear strong resemblance to the alternating-voltage relation. This is indicated in figure 24 which illustrates the comparative behavior of these insulating liquids each containing 60 parts per million of dissolved water. Figure 25 shows the effect of dissolved water on the "unlimited time" direct voltage for transformer Pyranol over the range of water contents of practical interest.

The Relation of the Rapidly Applied to the "Unlimited Time" Breakdown Voltage

In the use of insulating liquids it is most convenient and reliable to gauge the dielectric strength by means of a carefully controlled rapidly applied alternating-voltage test. The determination of the "unlimited time" voltage is difficult and time consuming. For both transformer Pyranol and mineral transformer oil, the "unlimited time" voltage falls to approximately 70 per cent of the usual rapidly applied dielectric strength value as the water content of the liquid is increased at 25 degrees centigrade. The relation is illustrated in figure 26.

The D-C/A-C Ratio of Dielectric Strength

The relation between the rapidly applied d-c and the a-c (effective) dielectric strengths of mineral transformer oil and of transformer Pyranol has no fixed value. The ratio obtained for mineral transformer oil varies erratically between 0.89 and 1.58 as the temperature of test and the water content of the oil are changed. For transformer Pyranol, the range in d-c/a-c ratio value is between 0.89 and 1.37.

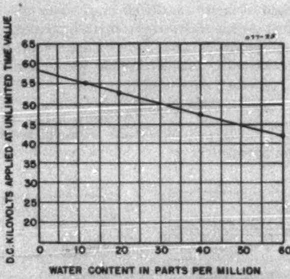


Figure 25. The "unlimited time" direct-voltage value of transformer Pyranol as affected by its dissolved water content

All dielectric strength tests were taken with a 0.20-inch gap setting

When the "unlimited time" voltage breakdown value is studied, a more consistent behavior of the d-c/a-c relation is observed. Figure 27 illustrates the ratio for transformer Pyranol using the "unlimited time" direct and alternating (effective) voltage values illustrated in figures 23 and 25. A ratio value linearly decreasing with the increased water content of the Pyranol is obtained. These tests were taken at 25 degrees centigrade.

Dielectric Loss

Mineral transformer oil has been selected to illustrate the effect of water solution on the dielectric loss. Several experimental setups were arranged which allowed the oil to be exposed at 25 degrees centigrade to air having relative humidity values of 24 per cent and 65 per cent. The apparatus used was similar to that already described under the paragraph entitled "The Hygroscopicity of Dielectric Liquids." Power factor and resistivity measurements were made at 25 degrees centigrade. The power-factor measurement was at 60 cycles under 3,000 volts. The resistivity was measured after the application of 500 volts direct current for one minute. The electrode gap setting in all cases was 0.10 inch.

Figure 28 illustrates the power-factor results obtained. From the dry-oil condition the power factor rises rapidly with increased water content to reach a maximum stable value at about 15 parts per million of dissolved water. Figure 29

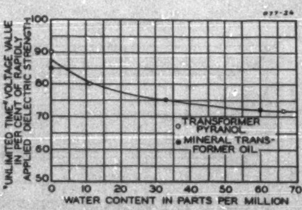


Figure 26. The effect of dissolved water on the "unlimited time" alternating-voltage value for transformer Pyranol and mineral transformer oil expressed in per cent of the corresponding rapidly applied dielectric strength

indicates a similar behavior for d-c resistivity measurements.

In considering the data of figures 28 and 29 it must be kept in mind that the oil tested was new oil uncontaminated by foreign materials or by oxidation products, the effect of which would necessarily be determined by their chemical and physical properties.

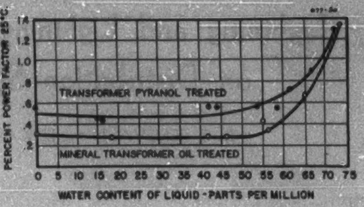


Figure 30. The power-factor characteristics of vacuum-dried kraft paper insulation as affected by water absorption after impregnation with transformer Pyranol and with mineral transformer oil

Cellulosic insulation is usually considered to be very susceptible to dielectric change caused by water. The presence of conducting salts and hygroscopic organic material serves to explain this marked susceptibility. Figure 30 describes the 30-degree-centigrade 60-cycle power-factor change in kraft-paper pads immersed in new mineral transformer oil and in new transformer Pyranol which are each exposed to humid air under conditions similar to those applying in the experimental setup of figures 28 and 29. The kraft-paper pads were originally well dried under a pressure about 0.3 millimeter of mercury at 100 degrees centigrade after which they were impregnated with the dry liquid. Although the data of figure 31 give no indication of the amount of moisture absorbed by the insulation, the test period covered several weeks and was sufficiently long for the insulation to reach an equilibrium with the water content of the surrounding liquid. Figure 30 leads to the conclusion that liquid-dissolved water does constitute a hazard to the successful maintenance of low-loss impregnated cellulose insulation. With new and unused insulating liquids, the 25-degree centigrade 60-cycle power factor of the impregnated cellulose insulation rises rapidly as the dissolved-water content of the impregnating liquid approaches and exceeds approximately 50 parts of dissolved water per million.

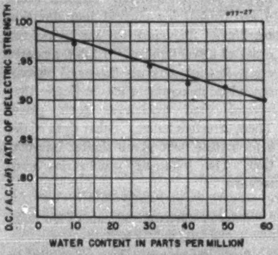


Figure 27. The 25-degree-centigrade "unlimited time" d-c/a-c (effective) voltage ratio of dielectric strength for transformer Pyranol as affected by its dissolved water content

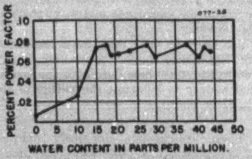


Figure 28. The effect of dissolved moisture on the 60-cycle power factor of mineral transformer oil measured at 25 degrees centigrade

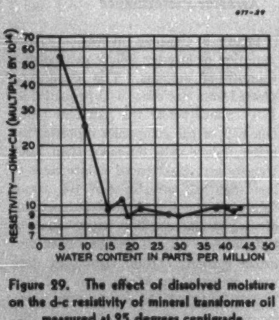


Figure 29. The effect of dissolved moisture on the d-c resistivity of mineral transformer oil measured at 25 degrees centigrade

The effect on the power factor of liquid-immersed cellulosic insulation produced by moisture absorption becomes more pronounced as the temperature is raised above 50 degrees centigrade. This is illustrated in the data of figure 31. In this instance, carefully dried and impregnated kraft-paper insulating pads were immersed for 21 days at 70 degrees centigrade in transformer Pyranol, over the surface of which was maintained a layer of water about one-half inch deep. The sorption of water by the treated cellulose greatly increases the power factor at the higher testing temperatures.

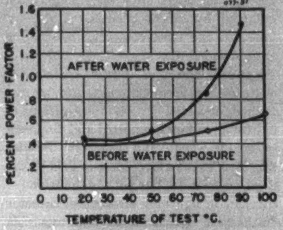


Figure 31. The effect of water absorption on the power factor temperature relation for transformer-Pyranol-impregnated kraft-paper insulation

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Lightning and Lightning Protection on Distribution Systems

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THE CONCEPTIONS of lightning and of lightning discharge currents through arresters have been altered and enlarged by recent studies. Until not long ago the most complete information available on the magnitude and wave shape of lightning surges on electric systems has been that obtained by the cathode-ray oscillograph investigations of lightning voltages, reported and discussed before the AIEE,[1] and elsewhere. Besides this there exists a large mass of data on the crest magnitudes but not the durations of currents in lightning strokes and arrester discharges. Based on the data available two conclusions have been generally held: one, that in a small percentage of discharges, the currents may be high, perhaps 65,000 amperes or more; and two, that the total duration of the discharge might be of the order of 100 microseconds, but not much more. Lightning-arrester design was based principally upon these conclusions. Arresters available during the past six or eight years have in general been able to discharge high currents of the durations cited without damage. In the field, the over-all experience with such arresters has been good. Throughout the country the average failure rate of Autovalve distribution arresters manufactured during that period has been less than a quarter of one per cent per year. However, in rural locations in some parts of the country failure rates of three to four per cent have occurred. This is considered excessive. The variation in failure rates led to the suspicion, several years ago, that there are characteristics in arrester discharge currents that had not been taken into account and which are themselves influenced by such factors as lightning exposure, stroke characteristics, ground resistance, and system characteristics. It is true that to some extent the significance of the data may be obscured by failures from abnormal system voltages and by the performance of obsolete arresters, but these factors were not sufficiently weighty to explain the observations.

The purpose of this paper is to discuss the present state of our knowledge of this matter resulting from the observations and investigations of the authors and their colleagues. We do not propose to provide a complete answer at this time to the questions raised because insufficient field data exist. The authors hope that this paper will stimulate others to give the subject study. It is expected that a considerable volume of informative data will be available after another season's research with "fulchronographs," the recently developed surge wave recorders.

Inspection of Arresters

A systematic inspection of Autovalve arresters removed from service was undertaken several years ago by our associates. Arresters that had failed in service as well as arresters which had not were examined. A surge discharge of high crest or of appreciable duration leaves a distinctive mark on the series-gap electrodes. This serves as an indication of crest current and duration if not obscured by power current. Crest currents above 3,000 amperes of short duration (less than 100 microseconds) leave marks like splashes. A low current of a few hundred to a thousand amperes of long duration (1,000 or more microseconds) produces a small pit with definite evidence of burning. Currents of high amplitude and long duration cause local melting of metal. A calibration of the appearance of the surge tracks as related to current magnitude and duration has been made. Typical results are shown in figure 1*a*.

Comparisons of electrodes from arresters that had seen service were made with the laboratory calibrations. Figure 1*b* shows representative field electrodes. These are especially significant because all but number 5 came from arresters installed on a line struck by lightning while it was unenergized. There is thus no question of masking by power current even in the cases where the arresters failed. Typical currents of high crest and short duration appear in 1, 2, and 3. The crests are of the order of 50,000 amperes and the durations approximately 50 microseconds. Number 2 has additional marks indicating other surges of lower crests. The elements of these arresters were not injured. Records 4 and 5 are typical low-current long-duration markings. They indicate currents of the order of a few hundred to 1,000 amperes crest lasting several thousand microseconds; moreover they indicate that several such discharges took place. These arresters failed. Other data of the same nature have given similar information. The following conclusions were reached:

1. Arrester discharge currents of low magnitude and long duration occur.
2. Such discharges are more likely to cause arrester failure than very high currents of short duration.
3. As far as arresters in service are concerned, either type of discharge may occur independent of the other.
4. It is important that field data be collected on the magnitude and wave shape of the discharge currents that prevail and which arresters must meet for a reasonable life expectancy under severe conditions; and to correlate these data with the factors that appear to influence the nature of the discharge currents in service.

These conclusions definitely pointed to a revision of our thinking about lightning-arrester discharge currents. Other observed circumstances strengthen this opinion.

Statistical Data on Arrester Performance

Figure 2 gives an indication of the variation of arrester failure rates over a number of systems. This curve was plotted from statistical data presented by L. G. Smith before the AIEE in 1937.[2] One-half of the companies reporting arrester failure data on more than 1,000 transformers experienced failure rates of less than 0.3 arrester per 100 transformers, but a few reported failure rates of two or more. The paper shows a ratio of four to one between rural and urban systems. Other significant data were presented by McEachron and McMorris in 1938.[3] Many crest measurements without duration of arrester dis-

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The authors gratefully acknowledge the encouragement, guidance, and suggestions of L. R. Ludwig through these studies, as well as the important contributions made by A. M. Opsahl, W. G. Roman, F. B. Johnson, and W. B. Berkey; the aid given by C. F. Wagner and G. D. McCann in regard to field lightning; and the patient collection of a mass of laboratory data by J. L. Clark and C. M. Lear.

[1] For all numbered references, see list at end of paper.

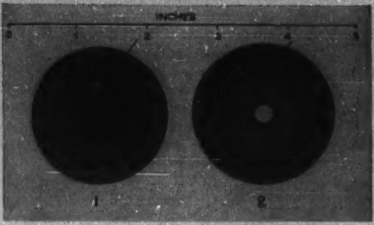


Figure 1a. Surge marks on lightning-arrester gap electrodes obtained in laboratory tests

1—57,000 amperes crest; 12 microseconds to crest; 26 microseconds total duration

2—725 amperes crest; 10 microseconds to crest; more than 4,000 microseconds total duration

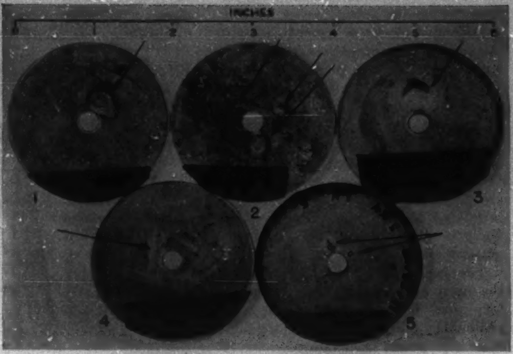


Figure 1b. Surge marks left by lightning discharges through arresters in the field

1—High short current
2—Several discharges of high short currents
3—High short current
4—Several discharges of moderate current. Several hundred amperes, but of long duration, probably more than 4,000 microseconds
5—Several discharges of moderate long current; 500 and 1,000 amperes, several thousand microseconds

All plates but number 5 from arresters on unenergized line

Arresters of 1, 2, and 3 were electrically undamaged. Arresters of 4 and 5 failed

charge currents are reported there and the occurrence of arrester failures is apparently not related to their magnitudes or to their relative frequency of occurrence on different systems in different parts of the country. Figure 7 of that paper is of considerable interest. It compares the frequency of occurrence of crest currents of various magnitudes on two different systems. Although the Detroit data indicate a greater prevalence of high currents, the observed failures of arresters are higher in Georgia. Low currents occur more often in Georgia than in Detroit. McEachron and McMorris report that 68 per cent of the observed arrester failures took place in Georgia although only 34 per cent of all the records were obtained there.

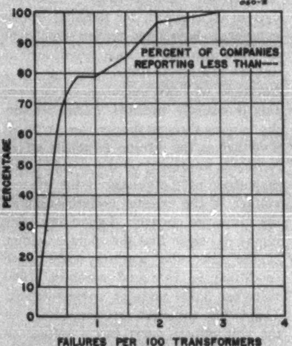


Figure 2. Statistical data on the relation between number of companies reporting arrester failure rates on more than 1,000 transformers

Number of companies considered, 33

Data culled from reference 2. Includes all protective circuits, average of 1934, 1935, and 1936 reported data

Stroke Characteristics

Photographic studies of strokes by several investigators, for instance Schonland and Collens, and Walter, indicated the probability that strokes may not only be multiple but also may involve moderate or low-current components of considerable duration. Stekolnikov and Valeev[4] in 1936 made records of lightning currents to a captive balloon. A rotating klydonograph used by them indicated that the duration of current flow lay between 2,600 and 10,000 microseconds. McEachron's work on the Empire State Building[5] also gave evidence of this phenomenon. Last summer, Wagner and Beck[6] obtained a complete record of the current in a direct stroke to the Cathedral of Learning of the University of Pittsburgh which definitely showed both high short and low long components. The oscillograph studies on lines had not indicated that this occurred on systems, but the authors suspect that because of the sensitivity limitations of the cathode-ray oscillograph the low long component may have been present but not revealed.

There is definite evidence that the long low components will frequently, if not always, appear in strokes to tall conductors. It is probable that the characteristics of strokes to lines are different from those to high objects but there is sufficient evidence to be convincing that long-duration components appear also to some extent on lines when struck by lightning.

New Recording Instrument

The available measurements of stroke currents cannot be translated into arrester duty until a significant volume of wave-shape data on strokes and arrester discharges in the field has been collected. This had not been done for lack of a recording device sufficiently low in cost to be installed in considerable numbers. The invention of the fulchronograph by Wagner and McCann described by them in a contemporary paper has made the needed device available.

It is evident that crest currents only without the important factor of duration did not give a complete understanding of arrester duty. The work has been begun. In co-operation with a number of utilities, many fulchronographs were installed during the summer of 1939, and already a considerable number of records has been obtained.

The fulchronograms of discharges through arresters in service are not yet of the desired technical significance. No records have been obtained accompanied by arrester failure. Records falling toward the expected extremes of high current or of long duration have not been secured. They do indicate, however, that arresters sometimes are called upon to handle, and do so successfully, considerable blocks of current (figure 3) appreciably in excess of the existing AIEE standard test. The paucity of heavy surges is not unexpected, inasmuch as experience with fixed link records as well as with arresters indicates that harmless currents are much more frequent. Some typical fulchronograms are shown in figure 3. These are all records of discharge through individual phase legs of arresters. Numerous records have been made of discharges of less than 1,000 amperes and short duration.

The observations and tests discussed in the foregoing show that the occurrence of long-tailed moderate-current surges accounts for the abnormally high failure rates experienced in certain rural regions on the basis that in these regions arresters must discharge such surges more frequently. The ability of the De-ion protector such as is used in the *CSP* transformer, to handle currents of long duration probably accounts to a large extent for its low failure rate in service.

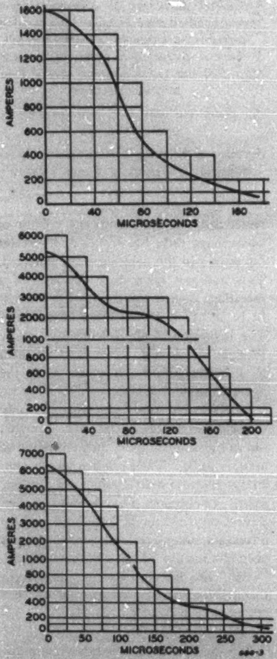


Figure 3. Typical records of discharge currents through single-phase legs of lightning arresters in service, obtained with the fulchronograph

Effect of System Characteristics

Low-current discharges through arresters are fairly common. This is demonstrated, for instance, by published data.[3] Figure 3 of the reference indicates that among 100 rural arrester installations, discharges of the following crest magnitudes (no data on duration) may be expected.

Amperes exceeding:	500	1,000	2,000	5,000	10,000	15,000
Discharges per year:	40	33	20	7	3	2

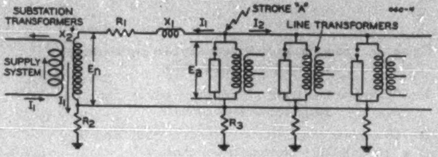
If the comparatively numerous low-current discharges through arresters were all or to a major extent of the long duration which might be expected from the observed stroke characteristics, laboratory tests on all types of distribution arresters would lead us to expect much higher arrester failure rates than those experienced and published.[2] Since many strokes evidently have these long low-current components there must be drainage effects present in many instances that relieve arresters installed on systems of some of this current.

Line flashovers from steep-front strokes to the line at points remote from arresters will by-pass a large proportion of the surge current. The effects of soil resistivity both on the degree of current drainage and perhaps on the character of the stroke itself may partly explain the difference in failure rates. In general, it has been found that where soil resistivity is high, arrester failures are more frequent. In addition to this, however, variations in arrester failure rates between locations resembling each other in insulation levels and soil resistivity point to another factor.

On grounded-neutral systems, both substation and distribution transformers are in parallel with the arresters and provide possible paths to ground for part of the current in long surges. A study was made of the current drainage characteristics of these shunt paths. The following discussion covers a typical example to illustrate the effects of system characteristics.

Figure 4. Surge-current drainage paths in parallel with distribution arresters

For I_1 see figure 5, and I_2 see figure 6



E_a—Arrester counter voltage

E_n—Alternating voltage to neutral

To use figure 5, calculate a-c short-circuit current at arrester nearest stroke with sufficient accuracy by using $R=(R_1+R_2)$ and $X=(X_1+X_2)$ (60 cycles), where X_1 is taken as 0.75 ohm per mile and X_2 is the substation transformer plus supply-system leakage reactance. Variation of R_3 through a 10–50 ohm range does not change I_1 appreciably

In figure 4 is shown a long single phase circuit with distribution transformers and arresters, fed by a substation transformer. A stroke to the first arrester will discharge initially through it and possibly also through adjacent arresters depending on the surge front. With a steep-front stroke close to, or a slow-front stroke remote from an arrester, line flashover will not occur and all of the stroke current is carried by the arrester or arresters discharging first. The stroke current flowing into the system is assumed, for purposes of calculation, to have a low-current component of 1,000 amperes of such length that it may be considered a direct current. For durations of 1,000 microseconds or more this is entirely justified. This current, discharging through the arrester, produces a voltage at the arrester terminals, which in turn is impressed on the system. The magnitude of the voltage is the counter voltage of the arrester corresponding to the discharge current. Laboratory tests (figure 11) show that the counter voltage of the Autovalve arrester under these conditions is, in round numbers, 6,000 volts per 3,000 volts of arrester rating. This is not the crest arrester voltage on the volt-ampere curve but a voltage on the descending part of the curve, near the so-called "cut-off". The initial voltage while carrying high current can be neglected because of its short duration. For purposes of calculation, then, it is assumed that 6,000 volts per 3,000 volts of arrester rating are applied continuously to the resistance and inductance of the

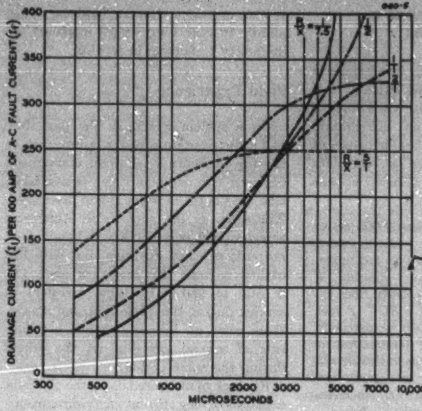


Figure 5. Surge current drained from arrester through line conductors and substation transformers

(Current I_1, in figure 4)

These curves based on $E_a=2.5E_n$ which is an arrester counter voltage of 6,000 on a 2,400-volt circuit or 18,000 on a 7,200-volt circuit

Since the range of a-c fault current (I_f) on a feeder is generally known, the drainage current I_1, which is proportional to it, was plotted accordingly. To use the curves, calculate I_f from R, X (60 cycles) and E_n of the figure 4 system, and use the appropriate R/X ratio curve in plotting the leakage current I_1 versus time

circuits through the supply and distribution transformers. These were found to have entirely different characteristics.

The substation transformer has connected to its receiving winding the supply system of low impedance. The current I_1 of figure 4 to the substation ground will build up through the inductance of the line, the leakage inductance of the transformer and the supply source, that is, in the ordinary a-c short-circuit path. In figure 5, I_1 is plotted per 100 amperes of feeder fault current for several values of the ratio R/X_{60}. These curves apply to higher voltage as well as 2,400-volts-to-neutral systems because the arrester counter voltage is proportional to the arrester rating. With small conductors, ($R/X=2/1$ to $5/1$, the drainage current to the substation ground is soon limited by line resistance.

The current I_2 flows in the other drainage path through the distribution transformers to ground. Since the secondary load impedances are high this current must build up through the magnetizing path. To determine the growth of current I_2, a $1^1/_2$-kva distribution transformer was subjected to suddenly applied continuous potential and the current measured by an oscillograph. Figure 6 shows the increase of current in terms of full load rms current of the transformers with 6,000 volts continuous applied to 2,400-volt windings or 18,000 volts applied to 7,200-volt windings. Until the iron begins to saturate at about 1,000 microseconds the current flow is imperceptible but after this it increases rapidly. The 1,000 microseconds applies under the conditions of test with no flux in the iron at the instant of application of the voltage. If the lightning voltage is applied at the instant the flux is zero, the curve applies as it stands. If the voltage appears when the flux is at its peak in the same direction, the time is much less, possibly zero. If it appears when the flux is at its opposite peak, the time will be much greater up to about twice 1,000 microseconds. To consider the most severe conditions it is assumed that 2,000 microseconds elapse before I_2 starts to flow.

The total current, I_1 plus I_2, that is drained away from arresters on typical 2,400- and 7,200-volt distribution systems is shown in figure 7. Up to 2,000 microseconds the drainage may be through the substation transformers only. Curves 1 and 2 show that for strokes near substations (3,000 to 1,000 kva, three phase) current is drained away rapidly. This also applies to a less extent on short heavy city feeders as shown in curve 3. Suburban and heavy rural circuits are represented in curves 4 and 5. Curve 6 applies to a typical rural circuit, while curve 7 represents extremely light rural distribution.

From curves 1, 2, and 3 of figure 7, it is apparent that for 1,000-ampere surges of long duration, the arrester discharge current will be reduced to zero in from 130 to 700 microseconds in city locations but for typical suburban distribution as indicated by curve 5, the arrester current will persist for some 2,500 microseconds where it is abruptly limited by drainage of current through the line transformers. The shape of curve 5 shows also that the suburban arresters are subjected to a higher average current.

On light rural systems the situation is the most unfavorable. The small size of the supply transformer and the high resistance of the line prevent much drainage to the substation. Fifteen miles of number 8 copper has a resistance of 53 ohms, which would pass only 340 amperes at the 18,000-volt counter voltage of a 9,000-volt arrester usual on a 7,200 volt system. Furthermore, the light branch fuses on these circuits are blown easily. With blown fuses, only the limited capacity of the distribution transformers can be depended upon for drainage, so conditions on grounded-neutral rural lines may be even worse than shown by curve 7.

In this connection it is suggested that the drainage of appreciable amounts of surge current through transformers may explain the occasional blowing of light transformer fuses with arresters connected on the line side of the fuses and no discernible evidence of flashover on the transformers.

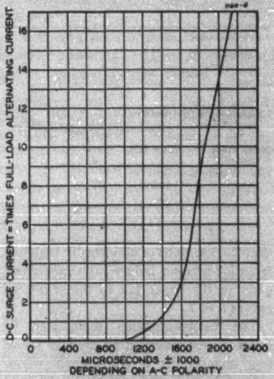
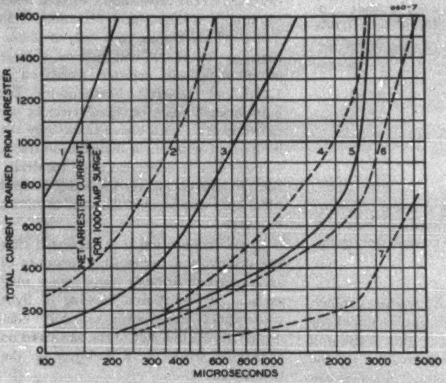


Figure 6. Surge current drained from arrester through magnetizing path of line transformers

(Current I_2 in figure 4)

Based on applying direct voltage of 2.5 times rms alternating voltage rating of transformer, that is, $E_a=2.5E_n$ (figure 4) which is an arrester counter voltage of 6,000 on a 2,400-volt circuit or 18,000 on a 7,200-volt circuit. Load impedance is so high that secondary was considered open-circuited

While there are other factors already enumerated to be considered, experience has shown that the failure rate of arresters increases progressively with conditions represented by the ascending curve numbers of figure 7. We would also expect from this analysis that arrester failure rates on free-neutral systems should be higher than on grounded-neutral systems. It is further significant that the locations of abnormally high distribution-arrester failure rates mentioned early in the paper fall in the region of curves 5 to 7.



Below 2,000 microseconds, all current flows to the substation. (Current I_1 calculated from figure 5). Above 2,000 microseconds, the line transformers begin to saturate and I_2, calculated from figure 6, increases sharply. Actually, this may occur at any time from zero to 2,000 microseconds, depending on the instantaneous magnitude and polarity of the a-c magnetic flux in the iron at the time of the stroke. Refer to table below

Figure 7. Total current drained from arresters through transformers of typical distribution systems

	Curve Number:							
	1	2		3	4	5	6	7
Voltage to neutral	2,400	7,200 or	2,400	2,400	7,200	2,400	7,200	7,200
Substation transformer kva per phase	1,000	1,000 or	333	1,000	333	333	333	50
Size line conductor	None	None	None	No. 00	No. 4	No. 2	No. 4	No. 8
Miles from stroke to substation	0	0	0	2	5	5	20	15
Line transformer kva assumed all at one point	0	0	0	0	300	200	100	75

The fulchronograms reproduced in figure 3 were recorded on arrester phase legs installed at a transformer whose neutral is grounded. The circuit characteristics are such that for the assumptions made in figure 7, the current drained from the arresters should be in the region of curve 2. This curve predicts an arrester discharge duration of not much more than 300 microseconds. The fulchronograms of figure 3 appear to corroborate the calculations. Apparently, all of the evidence available to date supports the conclusions.

Application of Conclusions to Arrester Construction

The observations and investigations discussed in the foregoing explain to a large extent, at least, the variations in arrester failure rates on distribution systems as well as the probable causes of high failure rates. Thus far, the data presented are relative because not enough information on the actual wave shapes of discharge currents through arresters are at hand. Nevertheless, the conclusions appeared of sufficient importance two years ago to warrant an active investigation of arresters' ability to handle long discharges with the object of increasing this ability, without sacrifice of any of the high current capacity or protective characteristics. After all, the significance of the conclusions lies in whether they can be translated into a new and better arrester. Experience with an arrester tailored to these new concepts has been favorable. It gives additional corroboration and clarification to the aspects already discussed. The extremely low failure rate experienced with these arresters indicates that an increase in the ability to take long surges increases the immunity to failures in service.

Field Experience

On a system which, in the past, has had a distribution-arrester failure rate at the top of figure 2, a direct comparison between two arrester types is available. One type is the new one under discussion. The other is the superseded Autovalve arrester, with equal ability to handle high currents of short duration, but with less ability to take the long low ones. In a total of 950 of the new arresters in service during the 1939 lightning season, there were no failures, against a failure rate of several per cent on the others during the same period.

For lack of measured data, the ultimate demands in long current-carrying capacity are not yet known. Nor is it known exactly what can be done economically to meet what those ultimate demands may turn out to be, or to what degree the new arrester meets them. It will not eliminate failures entirely. However, on the basis of laboratory tests and field experience, it is expected that even in the unfavorable locations the failure rate will be low.

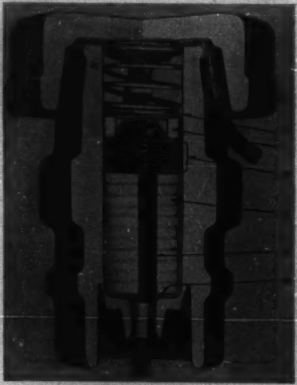


Figure 8. 3,000-volt Autovalve distribution lightning arrester, cutaway section

1—Porcelain-insulated main gap
2—Gap electrode with preionizing tips
3—Quenching gaps
4—Autovalve element with white insulating coating
5—Insulating coating cut away from element for purpose of illustration
6—Terminal plate, ground lead, and ferrule soldered together

The first field-trial installations were made in 1939, to gather operating experience before the design was adopted for production. Several thousand units were placed in locations where previous failure rates have been higher than normal. Two hundred fifty were in service throughout the 1938 season. No failures occurred. By the end of 1938 some 1,200 were installed. Before the spring of 1939, the number was 4,000, and this had more than doubled by the fall. There has been a total of four failures; one of these was definitely caused by system voltage. The other three occurred late last summer on the same system near the ends of lines. Inspection of these three arresters indicated that they were caused by lightning. Thus the lightning failure rate has been 0.043 per cent compared to the quarter of a per cent mentioned in the first paragraph of this paper.

Construction

The principal factor in determining the ability of the arrester to discharge long surges is the valve element. Its failure can be thermal, or it can be caused by a function involving current density, voltage gradient, and duration of discharge. In long-duration discharges the latter cause is more weighty than the thermal. The recognition of this was important for it pointed out a means of making a distribution arrester of large current-carrying capacity without increasing its cross section. If the length of the block is doubled and its composition adjusted so that the total voltage across it during discharge is the same as in the case of the shorter element, the voltage gradient is halved. This reduction in voltage gradient effects a large increase in the ability of the elements to handle discharges of long duration. The use of such an element in the arrester is the major reason for its large discharge ability. Other detail features permit the utilization of the valve material to its full capabilities; such as a newly developed insulating coating and a particular design of terminal for making contact with the valve blocks.

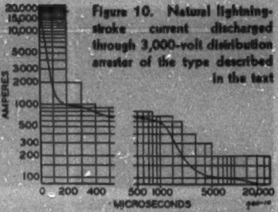
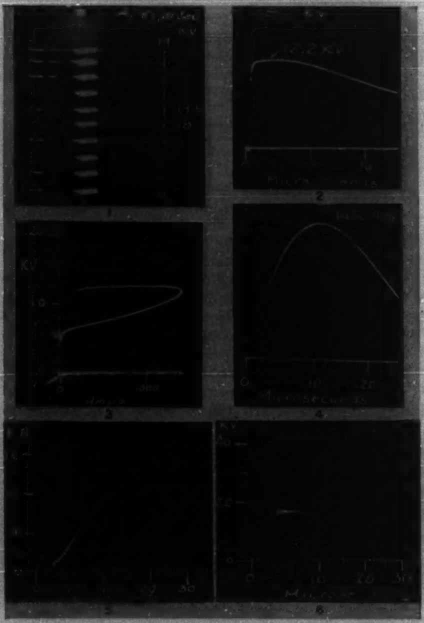


Figure 10. Natural lightning-stroke current discharged through 3,000-volt distribution arrester of the type described in the text

The current was derived from a lightning stroke to the Cathedral of Learning Building in Pittsburgh.[6] The current was recorded by a fulchronograph. After this test, the arrester was checked in the laboratory and found unchanged in its characteristics



1—Successive impulse breakdown tests on a 3,000-volt arrester. Rate of rise of test voltage 40 kv per microsecond. No time deflection used in oscillograph. Voltage deflection from left to right. Vertical line marked 19 is crest of 60-cycle breakdown

2—Volt-time curve of same arrester on AIEE test wave shown in 4

3—Volt - ampere characteristic of same arrester, test current as in 4

4—Ampere - time characteristic of test current used in 2 and 3

5—Ampere - time characteristic of 9,000-ampere test current used in 6

6—Volt-time characteristic of 3,000-volt arrester on test wave shown in 5

Figure 9. Impulse characteristics of new distribution arrester

The series gap structure consists of a quenching gap, item 3 in figure 8 with high 60-cycle interrupting ability, and a porcelain-insulated line or main gap, items 1 and 2. The high interrupting ability of the gap provides a margin of safety against failures from high system voltage. The use of mica spacers in the quenching gap and of a special main gap electrode, item 1, provides preionization of the gap spaces and produces low and consistent impulse breakdowns, as illustrated in oscillogram 1 of figure 9, which also shows the discharge characteristics of the arrester on the basis of AIEE standards. The favorable impulse ratios obtained with this construction permit the incorporation of a high 60-cycle breakdown, thereby providing additional margins against dynamic voltage failures.

During the development of this arrester, many tests were made with moderate discharge currents of long duration as well as with high currents. A novel test was completed when a lightning stroke occurred to a measuring station on the roof of Pittsburgh University's Cathedral of Learning.[6] The entire stroke current was discharged through one of the new distribution arresters, connected in the stroke-current circuit, and was recorded by a fulchronograph. This current is shown in figure 10. Subsequent laboratory tests on the arrester showed that its condition had not been altered by the discharge. As far as the authors are aware, this is the first time that an arrester has deliberately been put in the path of a stroke and successfully discharged the full stroke current. The performance of the arrester on laboratory surges of long duration and of high magnitude are shown in figures 11 and 12. Oscillogram 1 of figure 11 is typical in shape of the surge used in routine testing of valve elements to insure the use in complete arresters of only

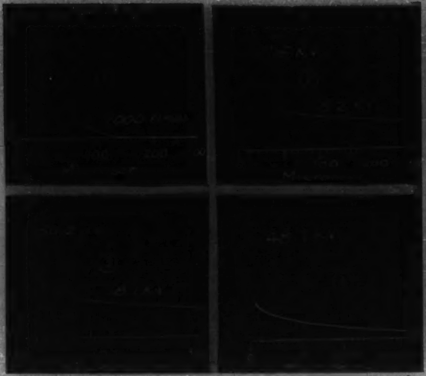


Figure 11. Performance of distribution arrester on long surge

1—Discharge current through arresters used in recording 2, 3, and 4

2—Volt-time characteristic of 3,000-volt arrester with discharge current of 1

3—Volt-time characteristic of 9,000-volt arrester with discharge current of 1

The points on 2 and 3 marked 6.2 and 18.1 kv are the voltages corresponding to 1,000 amperes in 1. These are the basis for assuming 6,000 volts per 3,000 volts of arrester rating in the derivation of figures 5, 6, and 7

4—Volt-time characteristic of 9,000-volt arrester with long time scale. This oscillogram shows that there is voltage across the arrester even after 4,000 microseconds, indicating that the arrester is still discharging current. In 1, the current is not discernible after 150 microseconds because of the current scale used

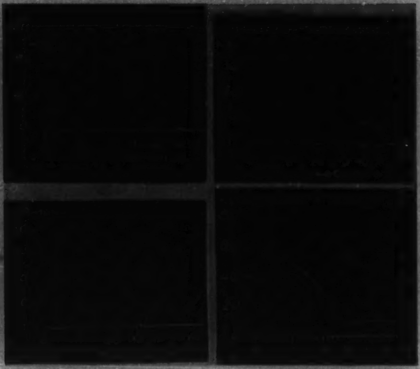


Figure 12. Performance of distribution arrester on high discharge currents

1—High current discharge through 3,000-volt arrester

2—Volt-time characteristic of 3,000-volt arrester with discharge current of 1

3—High current discharged through 9,000-volt arrester

4—Volt-time characteristic of 9,000-volt arrester with discharge current of 3

The difference in current between the 3,000- and 9,000-volt arresters is caused by the difference in the counter voltage of the two. Both were tested in the same surge-generator circuit. The voltage across the 9,000-volt arrester for the current in 1 is three times the voltage recorded in 2

elements able to handle long-tailed discharges.

This test surge used on distribution arrester elements resembles the recorded discharge currents of figure 3, but is longer in duration. The routine test is made while the arresters are excited at rated 60-cycle voltage. The adoption of such a test procedure is perhaps as important a development as the new construction used in the arrester.

Other characteristics have been given due weight in the development, such as safeguards against lockouts by failed arrester on low-capacity systems, and the avoiding of interference with radio reception. Discussion of these is without the scope of this paper. However, the authors wish to say a few words on the matter of radio interference.

Radio interference has not been a problem in arresters constructed during the past eight years. Laboratory tests with standardized radio-noise-measuring technique are factors in the development of any arrester. However, a considerable amount of attention is being directed to this subject as well as to so-called ionization currents, synonymous with radio-noise currents. For this reason it is to be desired that the committees concerned with this question adopt standards of permissible microvolt levels to which the industry will subscribe and adhere. In this connection, it may be of interest that in the arrester discussed in this paper the occurrence of low ionization currents at a voltage below actual arrester breakdown but above its rating is promoted and utilized to achieve low and consistent impulse breakdown.

Summary

The text of this paper concerns itself almost entirely with one aspect of the problem of distribution circuit protection, that of arrester discharge currents and the lightning-arrester failure rate. There are other phases of the problem such as the important one of protection to equipment. At this time, however, it is believed that the most important problem is the matter of eliminating as far as possible the failures of protective devices even under severe operating conditions. It is hoped that the work described in the paper has contributed to the solution of this problem, and there is some reassuring evidence that it has, contained for instance in the field records of an arrester designed to meet conditions disclosed by the studies already outlined.

Table I. Summary of Arrester Characteristics

Arrester rating, volts	3,000	6,000	9,000
60-cycle breakdown, volts rms	14,000	24,000	35,000
Impulse breakdown, volts crest*	20,000	40,000	58,000
Discharge voltage at:			
1,500 amperes†	12,500	25,000	37,500
5,000 amperes†	15,000	30,000	45,000
10,000 amperes†	17,500	35,000	52,500
20,000 amperes†	18,500	37,000	55,500

The above values are average.

* On front of 50 to 100-kv-per-microsecond test wave.

† 10 by 20-microsecond current wave.

Much research has been done on lightning, particularly on the lightning stroke. This will no doubt result, in time to come, in important new concepts of protection. However, the present practical aspect of the protective problem is concerned with the discharges through lightning protective devices in service, and not with the currents in direct strokes. For this reason, the authors attach the greatest importance to an accumulation without delay of a volume of statistical data on the magnitude and wave shape of such discharge currents, to permit a definite evaluation of the present means of protection, the economic possibilities,

Watt-Hour-Meter Performance With Power Rectifiers

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Synopsis: This paper gives the results of tests made principally to determine the effect of 6- and 12-phase mercury-arc power-rectifier loads on the performance of typical polyphase watt-hour meters. Schematic diagrams of the connections used and oscillograms of the rectifier voltage and current waves are included. The results indicate that the effect to be expected will be well within one per cent.

IT IS well known that mercury-arc power rectifiers introduce harmonics in the alternating supply current and voltage waves [1-4] and this has been thought to affect adversely the accuracy of watt-hour meters on circuits including rectifiers. The subject has been investigated previously, notably by Dannatt,[5] but interest in it is still current. It was considered highly desirable, therefore, to make more comprehensive tests, utilizing the best obtainable equipment and methods, to determine the actual effect of commercial types of 6- and 12-phase rectifiers under approximate conditions of service on the performance of typical polyphase watt-hour meters used with instrument transformers. At the suggestion of W. C. Wagner, a var-hour meter (reactive-volt-ampere or rva-hour meter) was included; also included was a portable power-factor meter.

In these tests, the performance of five watt-hour meters was determined at various loads, first with one of the two rectifiers and second with a sine-wave supply under corresponding three-phase conditions. The differences between the meter accuracies thus obtained are a measure of the effect of the rectifier and are referred to the sine-wave results as a standard. A similar procedure was followed for the var-hour and power-factor meters.

The paper first summarizes the test results, then describes briefly the apparatus tested and the related theories of performance, outlines the procedure followed, and discusses the data obtained. The ratings of the apparatus are given in table I and part of it is shown in figures 1 and 2. Schematic diagrams of the test connections are shown in figures 3 and 4 and the measurement equipment in figure 5.

Summary of Results

The results of the watt-hour meter tests are given in table II, which shows that the maximum change in watt-hour meter performance due to the effect of the rectifiers is less than 0.5 per cent. Table II gives the detailed effects or the differences between the accuracies obtained with the rectifiers and with a sine-wave supply at corresponding points for the five watt-hour meters tested. The accuracies so obtained in the 6-phase rectifier tests are included in table III and are plotted in figure 8; the 12-phase rectifier results are plotted in figure 9. In general, the results obtained with the two rectifiers and the two types of meters are similar in magnitude. In both tests, the rectifier effect tends to decrease the meter speed at light load. At higher rectifier outputs, the 6-phase effect tends to increase the meter speed, whereas the 12-phase effect becomes negligible.

Three representative oscillograms taken during the 6-phase rectifier tests are shown in figure 6 and the analyses are given in table IV, part 1. This table includes an analysis of the oscillogram shown in figure 7 for a 12-phase rectifier. The maximum harmonics occur with the 6-phase rectifier, but the harmonic power is small, as indicated in table V.

The results of the var-hour meter tests are given in table III and are plotted in figure 10. The maximum change shown in var-hour meter performance due to the 6-phase rectifier is less than one per cent. The rectifier effect tends to increase the meter speed at light load and to decrease it at higher outputs.

Table III also gives the results of the power-factor meter tests, together with the instrument readings from the watt-hour meter tests. Three values of power

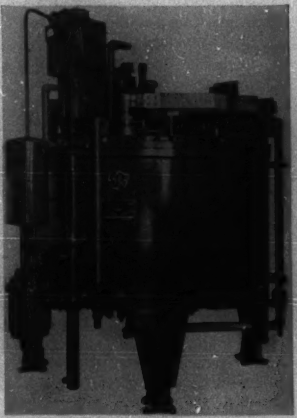

Figure 1. Six-phase mercury-arc power-rectifier tank rated type RHW, 1,675 kw, 515 volts

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The authors desire to acknowledge the assistance of G. F. Gardner and of many other of their associates in carrying out the tests.

1. For all numbered references, see list at end of paper.

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and the prediction of the failure rates of protective devices under the conditions of service that are encountered.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

factor are given for each pair of tests as follows:

(*a*). Power-factor-meter reading

(*b*). Power factor calculated from the formula

$$\text{power factor} = \text{polyphase watts/voltamperes} = P/\sqrt{3}EI \quad (1)$$

(*c*). Vector power factor calculated from the formula

$$\text{vector power factor} = \text{polyphase watts} \div \sqrt{\text{watts}^2+\text{vars}^2} = P/\sqrt{P^2+Q^2} \quad (2)$$

Formulas 1 and 2 are based on American Standards Association definitions; they are here restricted to balanced circuits, as is (*a*).

With the sine-wave supply, the power-factor-meter readings (*a*) in table III agree fairly well with the two corresponding calculated values of power factor (*b*) and (*c*) in each case. With the 6-phase rectifier, the expected good agreement is shown between the power-factor-meter readings (*a*) and the corresponding calculated values of vector power factor (*c*). Good agreement is not expected between the power-factor-meter readings (*a*) and the corresponding calculated values of power factor (*b*) based upon the ASA definition; the maximum difference shown is 0.030 (=0.976–0.946) or about three per cent: this difference is a function of the harmonics present.

The rectifier effect represented by the difference between (*a*) and (*b*) may be expressed by the ratio (*a*)/(*b*), which here has a maximum value of 1.032 (=0.976/0.946), or as (*c*)/(*b*). The lowest power-factor values are those determined from equation 1.

Conclusions

The rectifier current and voltage waves obtained in the tests are believed to be fairly representative of those encountered in practice.

The maximum effect of such rectifier waves on the performance of watt-hour meters should be well within one per cent; this conclusion is based upon all of our experience to date. The corresponding limit for a var-hour meter should be two per cent.

The effect of a 12-phase rectifier on the power factor should be less than the three per cent (ratio 1.032) obtained with the 6-phase rectifier. For both 6- and 12-phase rectifiers, the ratio (*a*)/(*b*) should aproximate the ratio

$$\frac{\text{effective value of balanced line currents}}{\text{effective value of fundamental component}} \quad (3)$$

when the harmonics in the balanced voltage waves are small as in these tests. The maximum value obtainable for ratio (3) with a 6-phase rectifier is 1.045 and it is 1.010 with a 12-phase rectifier. Thus, the maximum differences to be expected between the readings of a power-factor meter and the power factor calculated from equation 1 with such rectifiers should be within five per cent and two per cent, respectively.

The significance of the terms "power factor" and "reactive power" when harmonics exist in the system current and voltage waves is a matter of opinion[5–8].

The results upon which table II is based are believed to be correct within ±0.2 per cent.

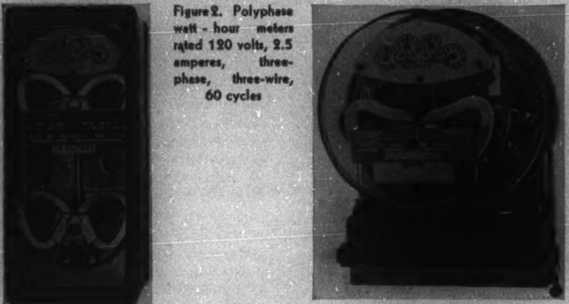


Figure 2. Polyphase watt-hour meters rated 120 volts, 2.5 amperes, three-phase, three-wire, 60 cycles

A—Type DS-19

B—Type V-3-A

Mercury-Arc Power Rectifiers

A 6-phase rectifier tank is shown in figure 1. The auxiliary equipment includes a main power-supply transformer in one or three units connected to convert 3-phase power to 6- or 12-phase power, an interphase transformer, vacuum pumps, and suitable control devices.[3,4]

The function of a complete rectifier is to convert a-c power to d-c power, usually for electric traction or electrochemical purposes. In so doing, it draws nonsinusoidal currents from a system generating sinusoidal voltages. These currents, in flowing through the system impedances, create nonsinusoidal voltage drops, which combine with the line voltages to give nonsinusoidal voltages at the rectifier terminals. The nonsinusoidal currents are composed of the fundamental and various harmonics, the orders of which depend on the number of rectifier phases; the magnitudes are an inverse function of the orders.[1] The nonsinusoidal voltage drops have the same harmonics, but the harmonic magnitudes depend on the magnitudes of the system impedances as well as on the magnitudes of the currents.[2] The formulas for the order and magnitude of the harmonic currents are as follows:

$$\text{Order of harmonic } n = mp' \pm 1 \quad (4)$$

$$\text{Per cent harmonic } h = 100/(mp' \pm 1) = 100/n \quad (5)$$

where

m = any number from 1 to infinity
p' = number of rectifier transformer secondary phases

The maximum obtainable percentage values of the harmonic currents due to the rectifier occur at light load and these estimated values are shown for 6- and 12-phase rectifiers in table IV, part 2*A*, up to the 25th harmonic, the fundamental being taken as 100 per cent; the orders of the harmonics also are shown. It should be noted that the magnitude of the harmonics decreases as their order increases and that harmonics common to both rectifiers, such as the 11th and 13th, have identical respective values. In practice, these magnitudes will ordinarily be somewhat less than those listed. Also, due to various causes, small amounts of the 5th, 7th, 17th, and 19th harmonics will be present with 12-phase rectifiers. For a given rectifier, the percentage harmonics will decrease with load, but the actual harmonic currents will increase; in other words, the harmonic components do not increase as fast as the fundamental.

Different rectifiers may be compared as to the relative amounts of harmonics expected in the input currents at full load by means of the load coefficient. The formula is:

$$\text{Rectifier load coefficient} = IX/E_s \quad (6)$$

Table I. Ratings of Apparatus Tested

	Units	Type	Cycles	Phases	Kva	Kw	Volts	Amp
Mercury-arc power rectifiers								
6-phase rectifier	1	*RHW*	60 60	6 6		1,675 2,233*	515 515	3,250 4,333*
Main transformer	3	*H*	60	1	1,130		13,200	
Interphase transformer	1	*H*	180/360					
12-phase rectifier	1	*RDW*	60	12		2,000	625	3,200
Main transformer	1	*HTD*	60	3	3,370		14,200	
Interphase transformer	1	*H*	180/360					
Meters and instrument								
Two-element watt-hour meter	3	*DS*-19	60	3	0.52	($K = 0.6$)	120	2.5
Two-element var-hour meter	2	*V*-3-*A*, *BE*1	60	3	0.52	($K = 0.6$)	120	2.5
	1	*DS*-19	60	3	0.52	($K = 0.6$)	120	2.5
Double autotransformer	1	*MC*-1	60	3			115/115	
Power-factor meter	1	*P*-3		3	0.87		100	5

* Extended rectifier rating.

where

I = d-c load current per group of main-transformer secondary windings

X = reactance of circuit in which commutation takes place

E_0 = crest voltage to neutral of main-transformer secondary winding

The effect of IX/E_0 at full load in reducing the light-load harmonic percentages is shown in table IV, part 2B for assumed values of 0.04 and 0.08: the latter is considered representative of many rectifiers. For example, take the 13th harmonic. For both rectifiers, "39" corresponds to 0.08, which means that 39 per cent of the light-load harmonic of 7.7 per cent will be present; this gives 3 per cent for the 13th harmonic: the corresponding value for $IX/E_0 = 0.04$ is 5 per cent. The percentage of harmonic present tends to decrease as the order of harmonic increases; the decrease for the 5th harmonic is small: conversely, the percentage increases with decreasing values of IX/E_0.

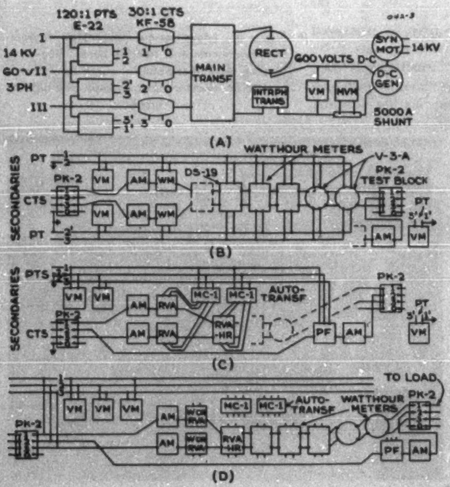
Figure 3. Schematic measurement circuits for rectifier tests

A—Rectifier input and output

B—Rectifier supply for watt-hour meter tests

C—Rectifier supply for var (rva)-hour meter tests

D—Sine-wave supply for check tests


120:1 PTS
E-22
30:1 CTS
KF-58
I
14 KV
60~II
3 PH
III
1
2
2'
3
3'
1'
1'
0
2
0
3
0
MAIN TRANSF
RECT
600 VOLTS D-C
SYN MOT
14 KV
D-C GEN
INTRPH TRANS
VM
MVM
5000A SHUNT
(A)
SECONDARIES
PT
CTS
PK-2
VM
AM
WM
DS-19
WATTHOUR METERS
V-3-A
PK-2 TEST BLOCK
PT
AM
VM
(B)
SECONDARIES
PTS
CTS
PK-2
VM
AM
RVA
MC-1
AUTO-TRANSF
RVA-HR
PF
AM
PK-2
PT
VM
(C)
TO LOAD
PK-2
VM
AM
W OR RVA
MC-1
AUTO-TRANSF
WATTHOUR METERS
RVA-HR
PF
AM
(D)

The term X in IX/E_0 represents the sum of the system reactance and that of the rectifier transformer; the system reactance (and the losses therein) ordinarily is less than ten per cent of the total. The harmonic power to be measured, therefore, is but a small part of that due to the rectifier, which in turn is but a small part of the fundamental power. The harmonic and fundamental powers have opposite signs. If the system voltage itself contains some of the harmonics expected from the rectifier, however, the net harmonic power may be positive instead of negative.

Ratio (3) is a function of the harmonics in the input currents. The maximum value of 1.045 for this ratio with a 6-phase rectifier is obtained when all possible harmonics attain the maximum possible magnitude; table IV, part 2, includes those up to the 25th. The maximum value of 1.01 is similarly obtained with a 12-phase rectifier.

Watt-Hour Meters

One of the type *DS*-19 watt-hour meters tested is shown in figure 2*A* and one of the type *V*-3-*A* meters in figure 2*B*. Both types have the same rating and are designed for three-phase three-wire service, usually with instrument transformers. The switchboard-type *DS*-19 meter employs two electromagnetic structures and two disks mounted on a common

Figure 4. Schematic instrument and auxiliary circuits

A—Potential and current supply—load control for check tests

B—Meter-disk revolution counter

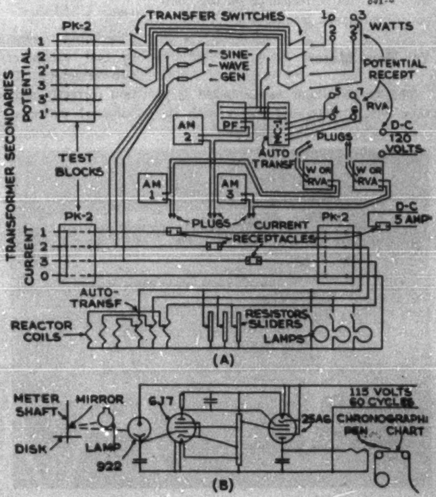

TRANSFORMER SECONDARIES
POTENTIAL
PK-2
1
2
2'
3
3'
1'
TRANSFER SWITCHES
SINE-WAVE GEN
1
2
3
2
WATTS
POTENTIAL RECEPT
5
7
4
6
RVA
D-C 120 VOLTS
AM 2
PF
MC-1
PLUGS
AUTO TRANSF
W OR RVA
W OR RVA
TEST BLOCKS
AM 1
AM 3
PLUGS
CURRENT RECEPTACLES
PK-2
D-C 5 AMP
CURRENT
PK-2
1
2
3
0
AUTO-TRANSF
REACTOR COILS
RESISTORS SLIDERS
LAMPS
(A)
METER SHAFT
MIRROR
DISK
LAMP
922
6J7
25A6
115 VOLTS 60 CYCLES
CHRONOGRAPH PEN
CHART
(B)

Table II. Effect of Rectifiers on Watt-Hour-Meter Performance (Difference Between Rectifier and Sine-Wave Results)

	Rectifier						Watt-Hour Meters; Per Cent Difference (Referred to Sine Wave)				
			Input (P-3 Instruments)				DS-19			V-3-A	
			(Average of Three)								
Test Number	Output (Per Cent)	Load Coefficient IX/E_0	Volts	Amp	Three-Phase Watts	Power Factor $(P/\sqrt{3}EI)$	1	2	3	4	5
6-phase rectifier											
1	21.5	0.015	116.9	0.571	109.9	0.950	−0.1	−0.1	0	−0.1	−0.3
2	57.5	0.044	116.6	1.515	289.3	0.946	−0.1	−0.1	−0.1	−0.1	−0.3
3	86.9	0.066	117.1	2.279	436.6	0.944	+0.1	+0.1	+0.1	+0.1	−0.1
4	113.6	0.086	117.9	2.973	570.3	0.939		+0.1	+0.2	+0.1	−0.1
5	135.8	0.105	118.3	3.568	684.6	0.936	+0.2	+0.2	+0.2	+0.1	−0.1
6	154.2	0.120	118.1	4.078	777.8	0.933	+0.1	+0.2	+0.2	+0.1	−0.1
12-phase rectifier											
1	18.2	0.008	118.2	0.551	108.4	0.961	−0.2	−0.3	−0.4		−0.3
2	35.4	0.015	118.0	1.061	210.0	0.989	+0.1	+0.1	+0.1	+0.1	+0.1
3	62.6	0.027	118.0	1.850	367.8	0.973	0	0	0	0	0
4	83.3	0.036	117.3	2.482	491.5	0.974	0	0	−0.1	0	0
5	92.8	0.041	116.2	2.812	550.8	0.973	0	0	−0.1	0	−0.1

shaft. The bottom-connected type *V*-3-*A* meter employs a more recent construction, in which both electromagnets act on a common disk. Interference is rendered negligible by the use of magnetic shields and by laminating the disk, thereby segregating the fluxes and eddy currents.

Tests made here and elsewhere have shown that such induction-type meters tend to become slower as the applied frequency increases. Thus 60-cycle meters would be slow at 300 cycles; in other words, they would not register fifth harmonic power correctly and larger errors would be obtained with higher harmonic powers. In some cases, correlation has been shown[5,10,11] between the performance of a meter on distorted waves and its performance on the component harmonic frequencies.

Var-Hour Meter

The var(rva)-hour meter tested is a type *DS*-19 watt-hour meter with its two potential circuits supplied from a type *MC*-1 double autotransformer instead of directly from the two line-to-line system voltages. By means of suitable taps, the autotransformer lags each voltage 90 degrees in order to measure the reactive component of the energy.[9] A similar unit supplied two indicating watt(var) meters to measure the reactive power.

The active power in a balanced three-phase three-wire sine-wave circuit may be determined from the formula

$$\text{watts} = \text{line-to-line volts} \times \text{line amperes} \times \sqrt{3} \times \cos\theta = \sqrt{3}EI \cos\theta \qquad (7)$$

where θ is the angle between the voltage to neutral and the line current. Similarly, the reactive power may be obtained from the formula

$$\text{vars} = \sqrt{3}EI \sin\theta \qquad (8)$$

The former quantity may be measured by two wattmeters and the latter by two varmeters. The varmeters will function correctly also on circuits with unbalanced currents, but neither formula can be used as the line currents are unequal and θ is meaningless. The wattmeters will function correctly on any sort of three-wire circuit.

Equations 7 and 8 may be used for balanced circuits and "θ" may be taken to represent the "angle" between voltage and current when the current waves contain harmonics. However, each wattmeter then measures the product of its sine-wave voltage and the in-phase component of its current. Since the fundamental component is smaller than the total current, the correct power factor (by ASA definition) or cos "θ" obtained from equation 1 will be smaller and "θ" will be larger than if no harmonics were present. Therefore, sin "θ" and the calculated reactive power will be larger than the measured value, the difference

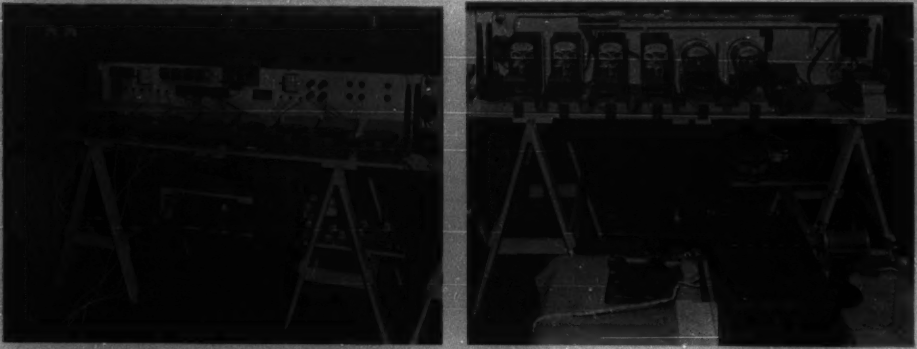


Figure 5. Measurement equipment—indicating instruments and transfer panel, sine-wave loading devices and control—watt-hour meters, revolution counters, chronograph and oscillograph

being a function of ratio (3). Also the vector power factor or cos θ determined from equation 2 will be the same as that read on a power-factor meter and both will be greater than the value determined from equation 1 in the ratio (3).

The foregoing discussion will still be pertinent when the harmonics in the voltage waves are small. However, it has been shown[8] that correct measurement of reactive power is impossible when the harmonics in both the voltage and current waves are appreciable.

Power-Factor Meter

The type *P*-3 portable power-factor meter tested is designed for use on balanced three-phase three-wire circuits only and has one current coil, which is connected in series with the "common" line. There are two potential coils mounted at an angle on the shaft and the two potential circuits are connected from the other two lines to the "common" line. Essentially, two wattmeters are combined in one instrument without a control spring, so the deflection depends on the ratio of the two torques, which is a function of the power-factor angle θ. The scale is marked in power factor and the range is from 0.5 lag through 1.0 to 0.5 lead.

Test Connections and Procedure

The tests described herein were made in connection with the standard acceptance tests on the two rectifiers. The measurement equipment is shown in figure 5. The meter accuracies were determined first with one of the two rectifiers and second with the sine-wave supply. The correct three-phase watt (or var) values were obtained in each test from the readings of two single-phase watt (or var) meters, which were checked with the d-c supply by means of potentiometers. The correct power factor was calculated from equation 1, which requires the readings of eight different instruments.

CONNECTIONS

The connections used are shown schematically in figures 3 and 4. Figure 3*A* shows the general arrangement of the rectifier input and output circuits and indicates the feed-back scheme adopted for acceptance tests on large rectifiers. The small numbers on the instrument transformer secondary leads correspond to similar numbers in figure 3*B*, which shows the arrangement for the watt-hour meter tests. The arrangement for the

Table III. Watt-Hour, Var-Hour, and Power-Factor Meters (Six-Phase Rectifier and Sine-Wave Results)

	P-3 Instruments					(a)	(b)	(c)		Watt-Hour Meters Per-Cent Accuracy					Var-Hour Meter Per Cent	
	(Average of Three)		Three-Phase							DS-19			V-3-A			
Test Number	Volts	Amperes	Volt-Amperes	Watts	Vars	Power-Factor Meter	Power-Factor $\left(\frac{P}{\sqrt{3}EI}\right)$	Vector Power-Factor $\left(\frac{P}{\sqrt{P^2+Q^2}}\right)$	Reactive Factor $(Q/\sqrt{3}EI)$	1	2	3	4	5	Accuracy	Diff.*
Input to 6-phase rectifier (through instrument transformers)																
1	116.9	0.571	115.7	109.9			0.950			100.1	99.9	100.1	100.0	99.8		
1a	117.0	0.592	119.9		28.52	0.977		0.968	0.238						98.1	+0.2
2	116.6	1.515	305.9	289.3			0.946			100.0	100.0	99.9	99.9	99.8		
2a	116.4	1.508	304.0		67.16	0.976		0.974	0.221						99.4	−0.1
3	117.1	2.279	462.3	436.6			0.944			100.1	100.1	100.1	100.1	99.9		
3a	117.0	2.266	459.4		106.5	0.971		0.972	0.232						99.7	−0.4
4	117.9	2.973	607.1	570.3			0.939				100.1	100.2	100.1	99.9		
4a	117.8	2.956	603.2		152.4	0.965		0.966	0.253						99.5	−0.4
5	118.3	3.568	731.4	684.6			0.936			100.2	100.2	100.2	100.0	99.9		
5a	118.1	3.474	710.6		186.7	0.962		0.965	0.263						99.3	−0.6
6	118.1	4.078	833.9	777.8			0.933			100.1	100.1	100.2	99.9	99.8		
6a	119.3	4.083	843.6		233.4	0.959		0.958	0.277						99.1	−0.8
Supply from sine-wave alternator (direct) Check Number																
1	116.6	0.585	118.2	114.0			0.964			100.2	100.0	100.1	100.1	100.1		
1a	116.8	0.585	118.3		33.61	0.972		0.959	0.284						97.9	
2	116.1	1.501	302.0	293.0			0.970			100.1	100.1	100.0	100.0	100.1		
2a	116.2	1.503	302.4		69.27	0.975		0.973	0.220						99.5	
3	117.6	2.197	447.6	433.7			0.969			100.0	100.0	100.0	100.0	100.0		
3a	119.0	2.215	456.5		108.9	0.972		0.970	0.239						100.1	
4	118.4	2.942	603.3	579.7			0.961			100.0	100.0	100.0	100.0	100.0		
4a	118.4	2.946	604.1		160.5	0.963		0.964	0.266						99.9	
5	117.5	3.564	725.4	694.3			0.957			100.0	100.0	100.0	99.9	100.0		
5a	117.7	3.567	727.1		198.0	0.963		0.962	0.272						99.9	
6	117.4	4.038	820.9	782.7			0.953			100.0	99.9	100.0	99.8	99.9		
6a	117.7	4.037	823.2		232.4	0.959		0.959	0.282						99.9	

* Difference between rectifier and sine-wave results; this is the rectifier effect: the watt-hour-meter differences are in table II. Power-factor columns (*a*), (*b*), and (*c*) are explained in the text of the paper.

A—At 22 per cent output

B—At 114 per cent output

C—At 156 per cent output

Figure 6. Oscillograms showing wave shapes of voltage and current, six-phase mercury-arc power rectifier rated type RHW, 1,675 kw, 515 volts

Curve *A*—Secondary current in line 1

Curve *B*—Secondary current in line 2

Curve *C*—Secondary current in line 3

Curve *D*—Secondary voltage across lines 1–2

var-hour and power-factor meter tests is shown in figure 3*C*. In *B* and *C*, meters not under test are indicated by dotted lines. Figure 3*D* indicates the arrangement for the sine-wave supply, which constituted the standard test condition for all meters.

The necessary connection changes were made by means of single- and double-throw potential switches, type *PK*-2 potential and current test blocks, and potential and current receptacles and plugs. These are shown in figure 4*A*. In the rectifier tests, the magnitude of the currents through the various instruments and the six meters (not shown) was determined by the load on the rectifier. In the sine-wave checks, the corresponding currents and power factors were obtained by means of the variable noninductive and inductive loads shown at the bottom of figure 4*A*. The line-to-line voltages were set at approximately the desired values by means of remote generator field control.

The method of automatically counting the revolutions of the six meter disks is shown in figure 4*B*. A small mirror is fixed on each meter shaft to reflect the light from a small lamp to photoelectric tube 922. This tube operates the coil of a suitable pen through a tube network. Each pen records each revolution of its meter on the moving chronograph chart, which also is marked by two pens (one at each side of the chart) energized every second from a standard clock.

PROCEDURE

For a test run on the watt-hour meters with the rectifier supply, the chronograph chart was started and allowed to record the meter revolutions for a period of eight minutes, during which the two indicating wattmeters were read every ten seconds, making a total of 48 readings each. Readings were taken every 30 seconds on each voltmeter and ammeter, giving a total of 16 readings per instrument. Thus one observer was required for each wattmeter, one for the three voltmeters, and one for the three ammeters, making a total of four.

Next a test run was taken on the var-hour and power-factor meters and the chronograph chart was permitted to record the meter revolutions for five minutes, during which a total of 30 readings were taken on each of the two varmeters and on the power-factor meter and ten readings were taken on each voltmeter and ammeter. An extra observer was required for the power-factor meter.

The watt- and var-hour meter tests just described were checked by corresponding tests with the sine-wave supply, the secondary voltage, current, and power-factor values due to the rectifier load in each case being duplicated as closely as possible. The actual running time for the four tests at each load was 26 minutes.

Finally, the indicating watt (or var) meter readings taken in the a-c tests were checked with the d-c supply. Reversed readings were taken at each point, using two deflection potentiometers, one for voltage and the other for current, as standards.

After completing the tests, the readings of each indicating instrument were av-

Figure 7. Oscillogram showing wave shapes of voltage and current at 100 per cent output, 12-phase mercury-arc power rectifier rated type RDW, 3,000 kw, 625 volts

Curve *A*—Secondary voltage across lines 1–2

Curve *B*—Secondary current in line 1

Table IV. Actual and Estimated Percentages of Harmonics in A-C Input Waves

Rectifier						Per Cent Harmonic									
				Input (Average of Three)		Harmonic Order and Frequency in Cycles Per Second									Oscillogram Figure Number
Test Number	Phases	Output (Per Cent)	Load Coefficient IX/E₀	Kv	Amp	1st 60	5th 300	7th 420	11th 660	13th 780	17th 1,020	19th 1,140	23d 1,380	25th 1,500	
(1) Harmonic Analysis of Oscillograms															
Secondary current															
1	6	22	0.015	14.00	17.15	100	16.2	9.9	6.8	6.4	3.9	2.3			6A
4	6	114	0.086	14.12	89.20	100	17.7	8.8	5.1	3.9	1.8	0.6			6B
6a	6	156	0.120	14.29	122.5	100	16.7	8.2	4.2	2.7		3.6			6C
Secondary voltage															
1	6	22	0.015	14.00	17.15	100	1.5	2.2	0.8	1.3	2.3				6A
4	6	114	0.086	14.12	89.20	100	1.7	1.1	2.2	2.8	0.6				6B
6a	6	156	0.120	14.29	122.5	100	2.9	3.6	2.3	1.8	2.1				6C
Secondary current															
†	12	100	0.058	14.00	126.0	100	1.4	0.5	5.0	3.8	0.4	0.1	0.9	0.7	7
Secondary voltage															
†	12	100	0.058	14.00	126.0	100	0.2	0.1	1.3	1.1	0.2	0.1	0.3	0.1	7
(2) Estimated Percentages of Harmonics in Current Waves Due to Rectifier															
(A) Light-load values (up to 25th harmonic)						Maximum Obtainable Per Cent Harmonics**									
	6	0-20	*			100	20	14.3	9.1	7.7	5.9	5.3	4.4	4.0	
	12	0-20	*			100			9.1	7.7			4.4	4.0	
(B) Full-load values						Per Cent of Light-Load Values									
	6	100	0.04			100	94	88	74	65	48	38	25	24	
	6	100	0.08			100	88	77	52	39	24	22	21	20	
	12	100	0.04			100			74	65			25	24	
	12	100	0.08			100			52	39			21	20	

* The load coefficient has a negligible effect on the magnitude of the harmonics at light load.

** Based on an inductive load; for a noninductive load, the fifth harmonic value is higher (= 22.6) but the other harmonic values are lower (or unchanged), so the maximum possible composite value is practically unchanged.

† Test made on 3,000-kw rectifier.

eraged and corrected for scale error. To obtain the three-phase volt-amperes, the three corrected voltmeter readings in each test were averaged, as were the three ammeter readings and these average values E and I were used in the formula.

$$\text{three-phase volt-amperes} = \sqrt{3}EI \qquad (9)$$

In each watt-hour meter test, the polyphase watts were obtained by adding the corrected readings of the two wattmeters and these indicated watts constituted the standard of reference in determining the meter accuracies. The recorded meter watts were determined from the total revolutions recorded on the chronograph chart and the corresponding elapsed time. The accuracy formula is

$$\text{per cent accuracy} = \text{recorded watts/indicated watts} \times 100 \qquad (10)$$

A similar procedure was followed for the var(rva)hour meter, the formula being

$$\text{per cent accuracy} = \text{recorded vars/indicated vars} \times 100 \qquad (11)$$

In these tests, however, the polyphase vars were obtained from the difference between the two varmeter readings. The reactive factor (sin "θ") was determined from the formula

$$\text{reactive factor} = \text{polyphase vars/volt-amperes} = Q/\sqrt{3}EI \qquad (12)$$

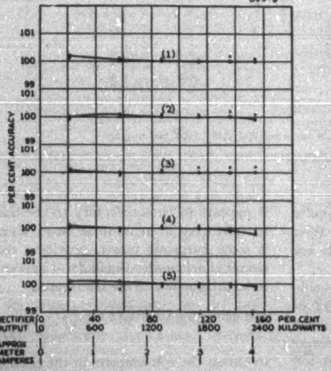


Figure 8 (left). Watt-hour-meter accuracies with six-phase rectifier and with sine-wave supply

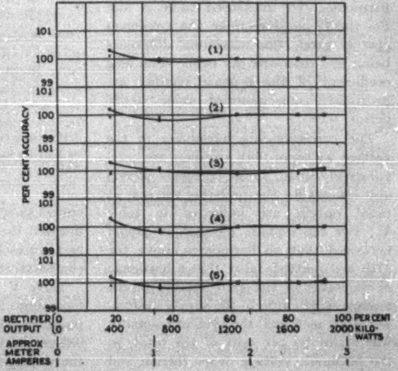
Figure 9 (right). Watt-hour-meter accuracies with 12-phase rectifier and with sine-wave supply

Curves 1, 2, 3—Type *DS*-19 meters

Curves 4, 5—Type *V*-3-*A* meters

Circles—Sine-wavesupply

Crosses—With rectifier load



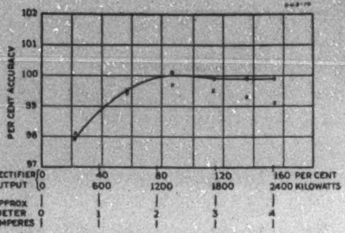


Figure 10. Accuracy of type DS-19 var (rva)-hour meter with six-phase rectifier and with sine-wave supply

Circles—Sine-wave supply
Crosses—Six-phase rectifier

Power factor (cos θ) was read directly on the power-factor-meter scale and for comparison was calculated from equation 2.

No corrections for the watts or vars were necessary because of the instrument transformers in the rectifier tests, since the meters were compared directly with the instruments in the secondary circuits.

Discussion of Results

Wave Shapes

A comparison of the current wave shapes in figure 6*A* at light load on the 6-phase rectifier with the theoretical waves, as shown by the magnitudes of the several harmonics in table IV, parts 1 and 2, indicates that the test harmonics are less than the theoretical ones by a maximum of about four per cent.

A much better agreement is found between the current wave shapes in figure 6*B* at rated output on the 6-phase rectifier and the theoretical waves based upon table IV, part 2*B*. The calculated harmonics check the test harmonics closely, the maximum difference being about two per cent for the seventh harmonic: the measured value is low. For the 12-phase rectifier, the agreement also is good, the maximum difference being less than one per cent. The load coefficient of the 6-phase rectifier approximates one assumed value (0.086 versus 0.08).

The corresponding harmonics in the voltage waves of both rectifiers are comparatively small, so the harmonic powers also are small; the probable maxima at rated output are indicated in table V. For the 6-phase rectifier, it should be noted that all harmonics above the 17th are omitted; also, that a harmonic in the order of the 49th appears in the voltage waves, due to the supply system. For both rectifiers, the system reactance was about 20 per cent of the total, so the harmonic voltage drops therein and the resulting harmonic powers are somewhat greater than would ordinarily be expected in service.

Meter Performance

The watt-hour-meter per-cent accuracies listed in table III have been corrected for minor errors in meter adjustment. For example, the actual test per-cent accuracies obtained for meter number 5 at 2.5 amperes in the sine-wave checks for both rectifiers were the same: 100.32, so 0.32 was subtracted from all test values. This procedure is equivalent to adjusting each meter to register correctly at its rated current of 2.5 amperes, in so far as comparative values are concerned. As just indicated, all results were carried out to the nearest 0.01 per cent and then rounded off to the nearest 0.1 per cent. The accuracy curves shown in figures 8 and 9 are quite flat and the differences between rectifier and corresponding sine-wave points are comparatively small.

Table V indicates that the probable maximum harmonic power for the 6-phase rectifier at rated output is about 0.6 per cent of the fundamental power; the corresponding values at 22 and 156 per cent output are 0.7 and 0.9 per cent, respectively. These apparent differences are not sufficient to account for the differences in effect on the meter speed. Although the phase relations of the several components of the harmonic power and therefore the true harmonic power itself at any output were not determined, there is no reason to expect that the net harmonic power changes sign as the rectifier output increases. In other words, there appears to be no correlation between the harmonic power and the rectifier effect. The probable maximum is less than 0.2 per cent for the 12-phase rectifier. It seems logical to conclude first, that the harmonic power due to 6- and 12-phase rectifiers will be comparatively small in practice and second, that their effect on the accuracy of modern watt-hour meters will be negligible.

Var-Hour and Power-Factor Meters

The var-hour-meter results are given in table III. The accuracy curve shown in figure 10 is fairly flat at the higher rectifier outputs, but it turns down toward light load; the differences between rectifier and sine-wave points are, in general, greater than for the watt-hour meters.

Table III also gives the power-factor-meter readings (*a*) and these agree fairly well with the corresponding values (*c*) calculated from equation 2. In fact it appears to be satisfactory to use this formula even if, as in these cases, there is some harmonic power, providing the circuit is balanced. Similarly, it appears to be satisfactory to use the formula

$$\text{volt-amperes} = \sqrt{\text{watts}^2 + \text{vars}^2} = \sqrt{P^2 + Q^2} \quad (13)$$

to obtain the volt-amperes in the circuit,

Table V. Probable Maximum Harmonic Power at Rated Output

Order of Harmonic	6-Phase Rectifier* Per Cent of Line-to-Line Voltage	6-Phase Rectifier* Per Cent of Line Current	6-Phase Rectifier* Relative Harmonic** Volt-Amperes	12-Phase Rectifier* Per Cent of Line-to-Line Voltage	12-Phase Rectifier* Per Cent of Line Current	12-Phase Rectifier* Relative Harmonic** Volt-Amperes
1	100.0	100.0	10,000.0	100.0	100.0	10,000.0
5	1.7	17.7	30.1	0.2	1.4	0.3
7	1.1	8.8	9.7	0.1	0.5	0.1
11	2.2	5.1	11.2	1.3	5.0	6.5
13	2.8	3.9	10.9	1.1	3.8	4.2
17	0.6	1.8	1.1	0.2	0.4	0.1
19				0.1	0.1	0.0
23				0.3	0.9	0.3
25				0.1	0.7	0.1
Totals			10,063.0			10,011.6

* Harmonic values taken from table IV (1), 6-phase rectifier values from test number 4.

** Relative harmonic volt-amperes at an assumed power factor of unity equals relative harmonic power in watts.

when not otherwise available. The ratio measured/calculated volt-amperes as obtained from equations 9 and 13, respectively, should approximate ratio (3) whenever the harmonics in the balanced voltage waves are small. This also applies to the ratio of power factors calculated from equations 2 and 1.

The average value of ratio (3) in 6-phase rectifier test numbers 1, 4, and 6*a* is 1.021 (from table IV, part 1). This compares with 1.025, which represents the average ratio for the same three tests between the two calculated power factors (table III, (*c*) and (*b*)). It appears that the voltage harmonics do not exert an appreciable effect.

It has been emphasized that the type *P*-3 power-factor meter is for use on balanced circuits only. In these tests, the circuit balance cannot be determined from the tables, as the averages of the voltages and currents instead of the individual values are given to conserve space. In general, excellent balance was obtained in the six-phase rectifier tests and in the corresponding sine-wave check tests, the maximum difference between any of the three voltages or between any of the three currents being in the order of one per cent.

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Transient Analysis of Symmetrical Networks by the Method of Symmetrical Components

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Synopsis: The canonical equations of a general, linear, bilateral symmetrical network of *n* meshes on which are impressed arbitrary mesh voltages are subjected to a Laplacian transformation. The transformed equations are then subjected to a symmetrical component transformation. By the use of these transformations the transient behavior of an *n* mesh symmetrical network subject to impressed arbitrary voltages and initial currents and charges is readily obtained. The solution of the usual determinantal equation is avoided.

SINCE the introduction of the method of symmetrical components into electrical engineering by Fortescue in his classic paper,[1] there have been many extensions and expositions of the method.[2,3] It appears however, that no systematic exposition of the use of symmetrical components in the study of transients in linear, constant, bilateral symmetrical networks has appeared. It is the purpose of this discussion to present such an exposition. The methods of the Laplacian transformation and matrix algebra as presented in references 4 and 5 respectively will be assumed.

I. The Canonical Equations

Let us consider a general, linear, constant, bilateral, lumped *n*-mesh network. Let the *n* mesh currents have the instantaneous values $(i_1 \ldots i_n)$ and let there be *n* voltages $(e_1 \ldots e_n)$ acting on the contours of the *n* meshes. The canonical equations of such a general *n*-mesh circuit may be written in the form:

$$(e) = [Z](i) \tag{1}$$

Where (*e*) and (*i*) are columnar matrices whose elements are the *n* mesh voltages and currents respectively and $[Z]$ is a square matrix of the *n*th order whose elements are the various self and mutual impedance operators of the form:

$$Z_{rs} = L_{rs}d/dt + R_{rs} + S_{rs}\int_{-\infty}^{t} (\;)dt \tag{2}$$

and L_{rs}, R_{rs}, S_{rs} are the coefficients of self- and mutual inductance, resistance, and elastance of the system. The impedance operator matrix $[Z]$ has the important property of being symmetrical about the principal diagonal. This property follows from the relations:

$$L_{rs} = L_{sr} \tag{3}$$

$$R_{rs} = R_{sr} \tag{4}$$

$$S_{rs} = S_{sr} \tag{5}$$

II. E-Symmetric Systems

Many networks of practical importance have the property that their impedance operators satisfy the relations:

$$Z_{11} = Z_{22} = \ldots. = Z_{nn} = Z_0 \tag{6}$$

$$Z_{sr} = Z_{jk} = Z \text{ for } r \neq s,\ j \neq k \tag{7}$$

that is, the impedance matrix of these systems has the form:

$$[Z] = \begin{Vmatrix} Z_0 & Z & Z & Z \ldots Z \\ Z & Z_0 & Z & Z \ldots Z \\ \ldots & \ldots & \ldots & \ldots \\ Z & Z & Z & Z \ldots Z_0 \end{Vmatrix} \tag{8}$$

Such networks have equal self-impedance operators among the several meshes and also equal mutual impedance operators between the meshes. Networks exhibiting this type of symmetry are called *E*-symmetric and are of great technical importance. Symmetrical polyphase systems are of this type.

III. The Laplacian Transformation of the Canonical Equations

Following the method of (5) part II, we subject the canonical matrix equation

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1. For all numbered references, see list at end of paper.

(1) to a Laplacian transformation as follows:

Let

$$I_j(p) \div i_j(t) \qquad (I) \div (i) \tag{9}$$

$$E_j(p) : e_j(t) \qquad (E) \div (e) \tag{10}$$

that is, for each element of the current matrix (I) and the voltage matrix (E) of (1) we introduce its Laplacian transform. Then the matrix equation (1) is transformed into:

$$[Z(p)](I) = (E) + p[L](i^0) - [S](q^0) \tag{11}$$

where:

$$(i^0) = \begin{Vmatrix} i_1^0 \\ i_2^0 \\ \cdots \\ i_n^0 \end{Vmatrix} \tag{12}$$

and ($i_1^0 \ldots i_n^0$) are the initial mesh currents at $t=0$ in the system,

$$(q^0) = \begin{Vmatrix} q_1^0 \\ q_2^0 \\ \cdots \\ q_n^0 \end{Vmatrix} \tag{13}$$

($q_1^0 \ldots q_n^0$) represent the n initial mesh charges at $t = 0$.

$$Z_{mn}(p) = L_{mn}p + R_{mn} + S_{mn}/p \tag{14}$$

$$[L] = [L_{mn}] \text{ the inductance matrix} \tag{15}$$

$$[S] = [S_{mn}] \text{ the elastance matrix} \tag{16}$$

IV. Transformation to Symmetrical Components

In the theory of symmetrical components,[1] the transformations are effected by the use of the nonsingular, symmetric matrix:

$$[C] = [C_{rs}] \tag{17}$$

where

$r = 1, 2, \ldots n$
$s = 1, 2, \ldots n$

$$C_{rs} = a^{-(r-1)(s-1)} \tag{18}$$

$$a = \epsilon^{j2\pi/n} \tag{19}$$

The transformation matrix $[C]$, is thus a square matrix of the nth order with the important properties that:

$$[C]' = [C] \tag{20}$$

where $[C]'$ is the transpose matrix of $[C]$

$$[C]^{-1} = \frac{1}{n}[\text{conj } C] \tag{21}$$

where $[C]^{-1}$ is the inverse of $[C]$ and $[\text{conj } C]$ is the conjugate of $[C]$. The rows of $[C]$ are the sequence operators of Fortescue.

Let us now multiply both sides of the equation (11) on the right by $[C]^{-1}$. We then obtain:

$$[C]^{-1}[Z][C][C]^{-1}(I) = [C]^{-1}(E) + p[C]^{-1}[L][C][C]^{-1}(i^0) - [C]^{-1}[S][C][C]^{-1}(q^0) \tag{22}$$

Now let us introduce the notation:

$$(E)_s = [C]^{-1}(E) \tag{23}$$

$$(I)_s = [C]^{-1}(I) \tag{24}$$

$$[Z]_s = [C]^{-1}[Z][C] \tag{25}$$

$$[L]_s = [C]^{-1}[L][C] \tag{26}$$

$$[S]_s = [C]^{-1}[S][C] \tag{27}$$

Then equation 22 may be written in the form:

$$[Z]_s(I)_s = (E)_s + p[L]_s(i^0)_s - [S]_s(q^0)_s \tag{28}$$

V. Transformation of the E-Symmetric System

Let us consider the transforms of the equations of the E-symmetric system of part II. In this case, the impedance matrix has the form given by (8). Computation of the symmetrical component matrix of the E-symmetric system by the equation (25) gives:

$$[Z]_s = \begin{Vmatrix} [Z_0+(n-1)Z] & 0 & 0\ldots & 0 \\ 0 & (Z_0-Z) & 0\ldots & 0 \\ \cdots & \cdots & \cdots & \cdots \\ 0 & 0 & 0\ldots & (Z_0-Z) \end{Vmatrix} \tag{29}$$

that is, the symmetrical component transformation matrix $[C]$, transforms the impedance matrix of the E-symmetric network into the diagonal form (29).

Let:

$$(I)_s = \begin{Vmatrix} I_{1s} \\ I_{2s} \\ \cdots \\ I_{ns} \end{Vmatrix} = [C]^{-1} \begin{Vmatrix} I_1 \\ I_2 \\ \cdots \\ I_n \end{Vmatrix} \tag{30}$$

and, similarly introduce the columnar matrices:

$$(E)_s = (E_{rs}) = [C]^{-1}(E_r) \tag{31}$$

$$(i^0)_s = (i_{rs}^0) = [C]^{-1}(i_r^0) \tag{32}$$

$$(q^0)_s = (q_{rs}^0) = [C]^{-1}(q_r^0) \tag{33}$$

then the equation (28) separates into the n scalar equations:

$$[Z_0+(n-1)Z]I_{1s} = E_{1s} + p[L_0 + (n-1)L]i_{1s}^0 - [S_0+(n-1)S]q_{1s}^0 \tag{34}$$

$$(Z_0-Z)I_{rs} = E_{rs} + p(L_0-L)i_{rs}^0 - (S_0-S)q_{rs}^0 \tag{35}$$

$r = 2, 3, \ldots n$

We then have immediately:

$$I_{1s} = E_{1s}/[Z_0+(n-1)Z] + p[L_0+(n-1)L]i_{1s}^0/[Z_0+(n-1)Z] - [S_0+(n-1)S]q_{1s}^0/[Z_0+(n-1)Z] \tag{36}$$

$$I_{rs} = E_{rs}/(Z_0-Z) + p(L_0-L)i_{rs}^0/(Z_0-Z) - (S_0-S)q_{rs}^0/(Z_0-Z) \tag{37}$$

$r = 2, 3, \ldots n$

Now if we let:

$$(i)_s \div (I)_s \tag{38}$$

that is:

$$i_{rs} : I_{rs} \tag{39}$$

$r = 1, 2, \ldots n$

We may compute the i_{rs} quantities from (36) and (37). We also have the relations:

$$(I) = [C](I)_s \tag{40}$$

and

$$(i) : (I) \tag{41}$$

Hence if we premultiply (38) by $[C]$ we obtain:

$$[C](i)_s \div [C](I)_s = (I) : (i) \tag{42}$$

or finally,

$$(i) = [C](i)_s \tag{43}$$

and we have the various currents explicitly.

VI. Examples

Let us consider the application of the above general theory to specific examples.

Discharge of a Capacitor in One of Two Mutually Influencing Circuits

Suppose that we have two similar circuits each with self-induction L_0 and elastance S_0, but negligible resistance, and that the capacitor in one carries an initial charge q^0. The initial currents in the system and the charge on the capacitor of the second circuit are supposed to be equal to zero. The coefficient of mutual induction is M. The problem is to determine the subsequent behavior of the system.

In this case we have:

$$[Z] = \begin{Vmatrix} Z_0 & Z \\ Z & Z_0 \end{Vmatrix} \tag{44}$$

with:

$$Z_0 = L_0p + S_0/p \tag{45}$$

$$Z = Mp \tag{46}$$

Since we have no impressed electromotive forces and no initial currents, the equations (36) and (37) for this circuit reduce to the following:

$$I_{1s} = -S_0q_{1s}^0/(Z_0+Z) \tag{47}$$

$$I_{2s} = -S_0q_{2s}^0/(Z_0-Z) \tag{48}$$

in view of the fact that $n=2$ and $S=0$.

The transformation matrix C and its inverse for this case is given by:

$$[C]=\begin{Vmatrix}1 & 1\\ 1 & -1\end{Vmatrix} \tag{49}$$

$$[C]^{-1}=\frac{1}{2}\begin{Vmatrix}1 & 1\\ 1 & -1\end{Vmatrix} \tag{50}$$

we thus have:

$$\begin{Vmatrix}q_{1s}^0\\ q_{2s}^0\end{Vmatrix}=\frac{1}{2}\begin{Vmatrix}1 & 1\\ 1 & -1\end{Vmatrix}\begin{Vmatrix}q_1^0\\ 0\end{Vmatrix}=\frac{1}{2}\begin{Vmatrix}q_1^0\\ q_1^0\end{Vmatrix} \tag{51}$$

since the second circuit does not have an initial charge. To simplify the operations, let us write:

$$M=L_0b \tag{52}$$

$$S_0=L_0c^2 \tag{53}$$

We then have the following equations for I_{1s} and I_{2s}:

$$I_{1s}=-c^2pq_{1s}^0/[p^2(1+b)+c^2]=-\frac{1}{2}c^2q_1^0p/(1+b)[p^2+c^2/(1+b)] \tag{54}$$

$$I_{2s}=-\frac{1}{2}c^2q_1^0p/(1-b)[p^2+c^2/(1-b)] \tag{55}$$

If we now make use of the basic transform:

$$p/(p^2+w^2)\div\sin(wt)/w \tag{56}$$

we obtain:

$$i_{1s}=-\frac{1}{2}\frac{cq_1^0}{\sqrt{1+b}}\sin(ct/\sqrt{1+b}) \tag{57}$$

$$i_{2s}=-\frac{1}{2}\frac{cq_1^0}{\sqrt{1-b}}\sin(ct/\sqrt{1-b}) \tag{58}$$

The actual currents in the two circuits i_1 and i_2 are now obtained from the symmetrical-component currents i_{1s} and i_{2s} by the transformation:

$$\begin{Vmatrix}i_1\\ i_2\end{Vmatrix}=\begin{Vmatrix}1 & 1\\ 1 & -1\end{Vmatrix}\begin{Vmatrix}i_{1s}\\ i_{2s}\end{Vmatrix} \tag{59}$$

or hence:

$$i_1=-\frac{1}{2}cq_1^0[(1/\sqrt{1+b})\sin(ct/\sqrt{1+b})+(1/\sqrt{1-b})\sin(ct/\sqrt{1-b})] \tag{60}$$

$$i_2=-\frac{1}{2}cq_1^0[(1/\sqrt{1+b})\sin(ct/\sqrt{1+b})-(1/\sqrt{1-b})\sin(ct/\sqrt{1-b})] \tag{61}$$

We thus obtain the current in the two meshes without solving for the roots of the characteristic equation of the system which in this case is one of the fourth order.

APPLICATION TO THREE-MESH NETWORK

Let us consider an E-symmetric three-mesh network. Let each mesh have a mesh inductance L_0 and mesh elastance S_0. Let the three meshes be coupled together by equal mutual inductance coefficients M.

The network will be considered inert at $t=0$, that is, the mesh currents and mesh charges are zero at $t=0$. At $t=0$, a steady electromotive force E is impressed on mesh one. The problem is to determine the subsequent currents in the three meshes.

For this case, the impedance matrix of the network is given by:

$$[Z]=\begin{Vmatrix}Z_0 & Z & Z\\ Z & Z_0 & Z\\ Z & Z & Z_0\end{Vmatrix} \tag{62}$$

with:

$$Z_0=L_0p+S_0/p \tag{63}$$

$$Z=Mp \tag{64}$$

The general equations (36) and (37) in this case reduce to:

$$I_{1s}=E_{1s}/(Z_0+2Z) \tag{65}$$

$$I_{2s}=E_{2s}/(Z_0-Z) \tag{66}$$

$$I_{3s}=E_{3s}/(Z_0-Z) \tag{67}$$

Now, since the electromotive force impressed in mesh number 1 is steady, we have:

$$\begin{Vmatrix}E_{1s}\\ E_{2s}\\ E_{3s}\end{Vmatrix}=\frac{1}{3}\begin{Vmatrix}1 & 1 & 1\\ 1 & a & a^2\\ 1 & a^2 & a\end{Vmatrix}\begin{Vmatrix}E_1\\ 0\\ 0\end{Vmatrix}=\begin{Vmatrix}E_1\\ E_1\\ E_1\end{Vmatrix} \tag{68}$$

where:

$$a=e^{j2\pi/3} \tag{69}$$

Hence we have:

$$I_{1s}=\frac{1}{3}E_1p/[p^2(L_0+2M)+S_0] \tag{70}$$

$$I_{2s}=\frac{1}{3}E_1p/[p^2(L_0-M)+S_0]=I_{3s} \tag{71}$$

Now if we let:

$$w_1=\sqrt{S_0/(L_0+2M)} \tag{72}$$

$$w_2=\sqrt{S_0/(L_0-M)} \tag{73}$$

then, in view of the transform relation:

$$p/(p^2+w^2)\div\sin(wt)/w \tag{74}$$

we obtain the following expressions for the transforms of I_{1s}, I_{2s}, and I_{3s}.

$$i_{1s}=\frac{1}{3}E_1\sin(w_1t)/\sqrt{S_0(L_0+2M)} \tag{75}$$

$$i_{2s}=\frac{1}{3}E_1\sin(w_2t)/\sqrt{S_0(L_0-M)}=i_{3s} \tag{76}$$

The actual three mesh currents of the system are now obtained by the equation:

$$\begin{Vmatrix}i_1\\ i_2\\ i_3\end{Vmatrix}=\begin{Vmatrix}1 & 1 & 1\\ 1 & a^2 & a\\ 1 & a & a^2\end{Vmatrix}\begin{Vmatrix}i_{1s}\\ i_{2s}\\ i_{3s}\end{Vmatrix} \tag{77}$$

Carrying out the matrix multiplication, we obtain:

$$i_1=\frac{1}{3}E_1\sin(w_1t)/\sqrt{S_0(L_0+2M)}+\frac{2}{3}E_1\sin(w_2t)/\sqrt{S_0(L_0-M)} \tag{78}$$

$$i_2=\frac{1}{3}E_1\sin(w_1t)/\sqrt{S_0(L_0+2M)}-\frac{1}{3}E_1\sin(w_2t)/\sqrt{S_0(L_0-M)}=i_3 \tag{79}$$

We thus obtain the solution to our problem in a most simple manner. It may be remarked that had we proceeded in the conventional operational manner, we would have been forced to determine the roots of a sixth order algebraic equation in order to apply the Heaviside expansion formula. A transformation to symmetrical components avoids all this labor.

VII. Conclusion

The general equations by which the transient analysis of multimesh E-symmetric networks may be carried out have been developed. The introduction of symmetrical component and Laplacian transformations enormously simplify the analysis of this type of networks. Since E-symmetric networks are of great technical importance in many branches of engineering, it is hoped that this contribution to their theory will be of value. The method may be readily applied to all linear dynamical systems exhibiting this type of symmetry whether electrical or mechanical.

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

The Accuracy of Watt-Hour Meters on Intermittent Loads

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Synopsis: A watt-hour meter on intermittent loads of a particular character may have a registration error. The magnitude of error is a function of the meter design, the periodicity of the intermittent component of load, ratio of intermittent component to total load, character of intermittent component, power factor of load, and adjustment of the friction compensation.

The registration characteristic of a meter on most types of intermittent load can be predicted from the acceleration and deceleration curves for the meter disk.

THE characteristics of watt-hour meters are dependent upon many diverse factors interrelated in a complex way. The design of the magnetic circuits, the mechanical features of the meter, and the various corrective elements to a large degree are responsible for these diverse factors. Once in service, however, the characteristics of a meter are relatively fixed, except as influenced by the remainder of the circuits with which it is associated.

The nature of the load which is to be measured by the meter may have an important effect on the characteristic performance of the meter. Loads may vary continually in magnitude or be intermittent with the period of cyclic variation from a few cycles to many minutes or hours. Such intermittent loads may cause factors which are usually of relatively minor importance to have considerable influence on the meter accuracy. Since these factors are interrelated, it is difficult to separate them experimentally, and only their combined effects have been studied.

In the usual application of watt-hour meters, the intermittent nature of the load will affect only those factors which depend upon the magnetic effects produced by the current elements or series coils, the potential element being energized at all times, thus making its effects constant. However, some adjustments of the meter which are independent of the series coil may affect the meter performance because of the interdependence of those factors and their influence on the common sections of the magnetic circuit.

The results of this study are given in curve form and are representative of several meters operating under the same load conditions. A careful study of the starting and stopping characteristics was made photographically. These results were verified by actual load tests.

Results of Study With Discussion

The registration of a watt-hour meter is a direct measure of the total angular travel of the meter disk. This angular travel, in turn, is a function of the angular velocity of the disk and time. The angular velocity of the disk for a given load is dependent upon the mechanical and geometrical factors of the moving system and the summation of the various component torques (driving and retarding) acting on this moving system.

It has been shown that the mechanical and geometrical factors for a given meter are essentially constant. As indicated in the appendix, the various component torques are constant when the load on the meter is constant, but for a variable load, the various component torques do not remain constant during the transitory periods. Certain torque factors which are present during the acceleration period of the meter disk may not be present during the deceleration period. The relative magnitudes of these changing torques will determine the meter accuracy during the transition period from one load value to another.

Figure 1 shows the accelerating and decelerating characteristic curves for a typical modern meter.* These curves are representative of those obtained from a stroboscopic photographic recording of the meter-disk travel during the period of change from rest to constant angular velocity and from constant angular velocity to rest. It will be noted that the slope of the constant-velocity sections changes in accordance with the different loads. Data for these curves were obtained by scaling angular travel of the meter disk from successive photographs of the disk obtained by means of a stroboscopic camera. The time interval between successive photographs was controlled accurately. The time or angular travel necessary for a disk to attain constant velocity was obtained by drawing a tangent to the curve at a slope corresponding to the velocity attained.

In an ideal meter in which no torque component is a function of higher than the first power of the disk angular velocity (appendix) and in which the ultimate disk velocity is directly proportional to the load, the time required to attain any given per cent of ultimate velocity is not a function of the load. In a practical meter, however, these conditions are not fulfilled although they are approximated closely. The position of the anticreep slots at the start of the acceleration period, the flux density in the various portions of the magnetic circuit, reactions of disk currents on the distribution of the flux in the various paths, and the variation of the friction torque with disk velocity are some of the factors which can produce small effects in a practical case. The accurate determination of the time required to attain steady-state condition is not necessary, an approximation being sufficient to indicate the character of intermittent loads which may give rise to registration errors. The graphical method of determining the point of tangency has inherent errors larger than the probable error due to the causes listed above.

The time required for the disks of meters of different designs to reach a constant angular velocity differs, the limits being approximately 0.2 second and 0.4 second. The time required for the disk to reach rest from the constant angular velocity corresponding to rated load is from 0.3 to 0.4 second. These variations between meters of different designs can be attributed to the fact that the components of torque are not in the same proportions in the different meters. It should be noted, however, that the time required is of interest only in the examination of a load cycle. The accuracy with which the meter integrates a load over a cycle depends upon the total disk travel during the cycle and not upon the time required for that travel.

If a constant load is placed on a meter

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* Both the modern and the older design of meters were used in the experimental tests.

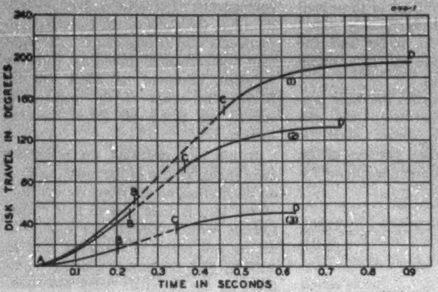


Figure 1. Acceleration characteristics of a typical watt-hour meter for various loads at unity power factor

A-B—Acceleration period
B-C—Constant-velocity period
C-D—Deceleration period

Curve 1—300 per cent load
Curve 2—250 per cent load
Curve 3—100 per cent load

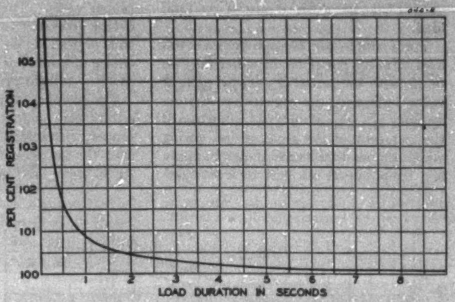
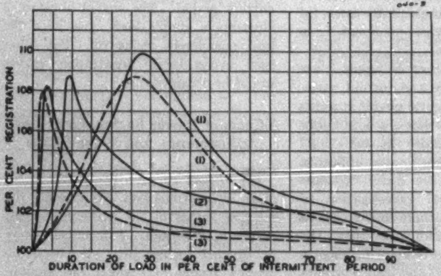


Figure 2. Effect of load duration on watt hour meter accuracy for 200 per cent load a unity power factor

a sufficient time to allow the meter disk to start from rest and attain constant angular velocity and then is removed for the length of time required for the meter disk to return to rest, the total angular travel of the disk should be a direct measure of the energy involved in the load cycle. This total disk travel should be equal to the product of the constant velocity and the time the load is applied if this registration is a correct measure of the energy. During the period of acceleration up to the velocity corresponding to the load value, the disk has lost a definite number of degrees of travel since it must start from rest and its average velocity during this accelerating period is less than the constant value corresponding to this load. After the load is removed, the disk decelerates to rest and gains a definite number of degrees of travel. If the number of degrees lost during the acceleration period does not equal the number gained during the deceleration period the meter registration is in error. This error in registration will be positive or negative in accordance with too large or too small a gain in disk travel at the end of a load cycle compared to the beginning. It will be indicated later that this error is influenced by the design of the meter and its adjustments for constant load accuracy.

If the load exists for a longer time than is necessary for the disk to attain the velocity corresponding to the load, the disk will travel at constant velocity for the time in excess of that required to reach this load velocity. The disk travel to rest after the load is removed will be the same for a given disk velocity regardless of the time the meter has been running at this velocity. The error in registration (degrees of travel) will be the same for each load cycle, but the per cent error in total registration will approach that for a constant load.

Figure 3. Comparison of predicted and test curves of watt-hour meter accuracy for intermittent loads of different periods



Predicted curves are in solid lines
Test curves are in dotted lines

Curve 1—8-cycle period
Curve 2—35-cycle period
Curve 3—100-cycle period

The effect of the length of the constant velocity period on the total registration of the meter is shown in figure 2. This curve is obtained from curves such as given in figure 1. The curve of figure 2 is for approximately 200 per cent load, unity power factor on the meter. As the constant-velocity interval is increased for a given load, the per cent error is reduced. It will be noted that for load durations less than one second, the meter error, caused by intermittent load, is one per cent or more and, for very short duration loads, will exceed five per cent. If the load duration is greater than four seconds, the intermittent load cycle causes an error less than one-fourth per cent. This error is within the limits of commercial meter accuracy.

For practically all types of intermittent loads, the approximate registration characteristics of the meter may be predicted, from the acceleration and deceleration curves (figure 1) for the meter. These characteristics should be at the power factor and with the same wave form as would be encountered in the circuit under consideration. For any load-variation cycle in which the load decreases to zero the approximation may be very close if the acceleration curves are known for each value of load and the deceleration curve is known for the greatest value of load to be considered in the group of loads studied. The degree of approximation (in predicting the registration characteristics) for other load cycles depends upon the relative value of the variable and fixed components of the load. The approximation is least accurate when the fixed component becomes a major part of the total load.

For a simple case of approximately full load of unity power factor either fully on or off, with the period of the load cycle of value indicated, the registration characteristics of a typical meter as a function of the per cent of the total period during which the load exists are shown in figure 3. These curves are derived from characteristics such as given in figure 1. Curves from experimental data for the same meter in a circuit with a load having periods of 8 and 100 cycles are shown for comparison.

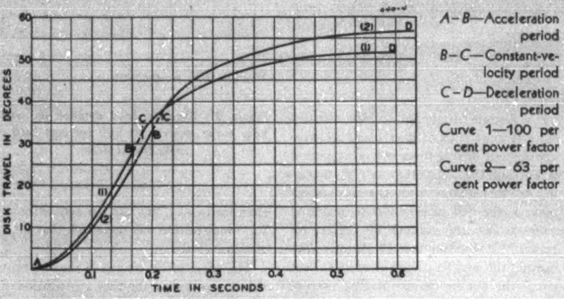


Figure 4. Acceleration characteristics of a typical watt-hour meter for 200 per cent load at two different power factors

With the characteristic curves for the meter available, for the loads chosen, such curves as those in figure 3 may, for simple load cycles, be predicted with ease. Beginning with the disk at rest the acceleration disk-travel curve is drawn for the length of time corresponding to the "on" portion of the load cycle. This curve is then extended by use of the deceleration disk-travel curve from the point on that curve having the same slope as the acceleration curve at the end of the first "on" portion. The deceleration curve is used for a time corresponding to the "off" portion of a load cycle. The acceleration curve is then applied as before except that the velocity at the end of the previous "off" period must be used to determine the initial conditions for the new "on" period. Successive "on" and "off" conditions must be assumed until the disk travel in any single load cycle becomes a constant value. This travel is compared with the proper value (constant velocity times the "on" time) and the registration noted as compared to the proper value. The number of these load cycles necessary to have "steady state" will depend upon the meter and the choice of load cycle and may be as many as 10 or 15 load cycles.

For complex load cycles the same procedure can be followed except that the proper characteristic curves must be used for the acceleration portions of the travel curve, and deceleration curves for partial removal of loads should be available. None of these deceleration characteristics for partial removal of loads has been included in the data of this investigation.

The experimental check was obtained by applying intermittent loads of a definite nature and comparing the actual registration of the meter for a known number of cycles "on" with the registration of the meter for the same number of cycles "on" for a continuous load of the same magnitude, power factor, and wave form. These check load cycles were chosen to correspond to those for which the errors were predicted from characteristic curves.

The predicted curves of figure 3 are for sinusoidal voltage and current variations. The experimental circuit currents correspond closely to that of a continuous welder. The harmonics in such load currents may be expected to influence the registration of the meter to some extent. Experimental investigation of this factor by the authors in the case of continuous loads indicates that the effects of such proportions of harmonics as are present in those load currents will not introduce an appreciable error in registration. The influence of this factor on the registration of a meter on intermittent loads should be very nearly the same as for constant loads having the same wave shape since the same torque factors would be affected by the harmonic components. For loads of short periodic duration, the effect of the corrective adjustments also is important. The light load compensation should be carefully adjusted, otherwise its effect will influence the shape of the registration curve. See paragraph (*b*) of the next section.

It will be noted that the maximum error in registration of the meter, used as an illustration, occurs when the duration of the load is from two to four cycles. Certain welder controls are commonly set for this period of operating time in order to give the proper working conditions for the materials being processed. It will be noted that the magnitude of the predicted maximum error is nearly independent of the period of load. This is borne out by the experimental results. Tests have indicated that, in the older designs of meters, this error is greater than in the more modern meter designs. This, no doubt, is caused by a different relationship between the various torque components in these classes of meters.

In some of the meters tested, when adjusted within the limits of accepted commercial accuracy, this maximum error was as great as 20 per cent. More than normal care in adjusting the meters resulted in the marked reductions in the maximum error.

The registration error due to intermittent load must approach zero as the acceleration portion of the load cycle approaches zero or 100 per cent of the load cycle. At some other value of the duration of load the error must be a maximum. The per cent of the intermittent period at which the maximum error occurs depends upon the design of the meter and the length of the intermittent period. The curves of figure 3 illustrate a simple load cycle with the load completely on or off. For other load cycles other registration curves would be obtained.

Effects of Power Factor, Light-Load Adjustments, and Wave Shape

(*a*) *Power Factor*. The power factor of an intermittent load will influence the relative values of the accelerating and decelerating torques. This influence may be divided further into (1) effects on driving torque, (2) effect on friction-compensating torque, and (3) effect on current-damping torque. The exact influence of power factor on each is not known quantitatively but it may be shown (see appendix) that these three torque components may be affected. The driving torque per ampere will be decreased as the power-factor angle is increased. This in turn may affect indirectly the friction compensation because of a new resultant flux per unit of driving torque. The new resultant flux may be shifted in time phase relative to the original flux, which may cause a change in the magnitude of the friction-compensating torque.

The effect of power factor on the current-damping torque is a function of current-coil flux. The flux will increase as the current increases but in a proportion which is dependent on the properties of the magnetic circuit. The effect at normal current values will be approximately in proportion to the current change per unit of power.

The net effect of small changes in power factor alone on the intermittent-load characteristic of the meter is ordinarily a minor one, because of the small net changes which occur in the total torques. Figure 4 shows the starting and stopping characteristic curves for a meter at two widely different power factors. The curve at the lower power factor indicates a longer time required to reach constant velocity from rest for the same load but the decelerating periods are the same. The elapsed time for a complete start-stop cycle is greater at the lower power factor and as a result the total travel of the disk is greater for the complete cycle. This increased time for a complete start-stop cycle will affect the meter performance on very short interval loads where the meter does not reach the true load velocity. For the loads of longer duration the error in starting and stopping is decreased and it is quite possible to have a meter with a combination of torque values which will give correct registration at some low power factor. Depending upon the adjustments of a meter, low lagging power factor loads may cause the registration error to become negative.

(*b*) *Light-Load Adjustments.* The curves which have been shown (figures 1, 2, 3, and 4) have been determined with the light-load adjustments as carefully adjusted as possible for all the meters used in these tests. The influence of this light-load compensation and how it can affect the meter performance on intermittent load is indicated by the registrations at three different positions of the light-load compensator for the same meter load. This influence is indicated in figure 5. The three points on the curve for this periodic load (three cycles on and five cycles off) are for the compensator positions of (1) ready to creep backward, (2) mid-position of compensator, and (3) ready to creep forward. The mid-position of the friction compensator corresponds very closely to the actual position used when this meter was in the original test circuits. Several test runs were made with the compensator at various positions and for intermittent loads of different periodic durations. It can be inferred from figure 5 that the component of torque from the compensator will be of great importance for loads of short periodic duration but will be less important as the period becomes longer. It is believed that more than ordinary care should be exercised in adjusting the compensator if the meter is to be used on a circuit which has a transitory load of major importance and of comparatively short periodicity.

(*c*) *Wave Shape.* Care was exercised in making comparisons between the performances for different load durations, that the wave shapes of the current and voltage were constant. The study of the effects of wave shape is separate and apart from this load study, but tests have been made which show that wave shape will influence the registration of a meter and for that reason only performance curves for the same wave shapes were compared in determining the meter accuracy.

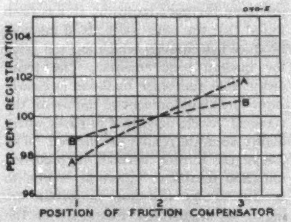


Figure 5. Effect of friction-compensator position on watt-hour meter accuracy for intermittent loads of short duration

A–A—One cycle on and seven cycles off
B–B—Three cycles on and five cycles off

Conclusions

1. The stroboscopic photographic data of the meter accelerating and decelerating characteristics can be used to predict with a good degree of accuracy the performance characteristics on most types of intermittent load. This is indicated by the comparison of the predicted and the actual test curve shown in figure 3.

2. The relative effects of the various torques influence the registration of a meter. If the speed of the disk is low, as in the present-day meter, the retarding torques will maintain a fairly uniform relationship. The current-damping torque has less relative effect in the modern meter and as a result, the accuracy on intermittent load has been improved.

3. The character of the load cycle, that is, the time the load is on and the time the load is off, influences the registration of the meter. Tests indicate that the conditions for maximum error occur at two to four cycles of accelerating period. This maximum error may be as much as 20 per cent. The off period apparently is not so important.

4. The adjustment of the light-load compensation is extremely important and care must be taken to be certain that there is minimum friction in the moving system of the meter, in order that friction-compensation-torque value may be minimized. If the adjustment is such that the meter is on the verge of creeping, an error up to 30 per cent may be obtained.

5. The variation of an intermittent load may be of a small magnitude in comparison with the average value of the load. In this case the influence of the load variation will be negligible.

6. Tests indicate that the load power factor influences the registration error. However, the damping effect of the current flux is relatively small in the modern meter, and the power factor of the load does not affect the meter accuracy on intermittent loads as much as in meters of older design.

7. The increased overload rating and increased ratio of driving torque to the moment of inertia of the moving element have decreased the accelerating time for a given load change. This improvement has decreased the meter error on intermittent load from the values found in the older designs of meters.

8. For metering loads which are continually transitory in nature, it is suggested that present-day meters be used and that a careful study of the load be made. Loads, such as electric welder and flashing electric-sign loads, should be carefully studied before being metered as a single load on a meter.

Appendix

When a watt-hour meter is recording a constant load at a constant speed, the total driving torque equals the total retarding torque. The relationship can be expressed in equation form as:

$$(T_D+T_L)-(T_M+T_P+T_C+T_F)=0$$

where

T_D = driving torque produced by the interaction of the potential-coil and current-coil fluxes
T_L = driving torque of the light-load (friction) compensator
T_M = retarding torque of the permanent magnets
T_P = potential-flux retarding torque
T_C = current-flux retarding torque
T_F = retarding torque of friction

In order to indicate the relative importance and effect of each term the controlling factors of each torque are indicated. Thus, the torque equation becomes:

$$k_1 f[\underbrace{(f)\phi_P\phi_C \sin\alpha}_{1}+\underbrace{(f)\phi_P{}^2 \sin\beta}_{2}]-$$
$$\frac{k_1}{2}f_d[\underbrace{(f)\phi_M{}^2}_{3}+\underbrace{(f)\phi_P{}^2}_{4}+\underbrace{(f)\phi_C{}^2}_{5}]-$$
$$\underbrace{[k_f(f)f_d+F_0]}_{6}=0$$

Part 1 = T_D expressed as a function of potential flux, current flux, and the angle of displacement between them
Part 2 = T_L expressed as a function of the square of the potential flux and the angle of displacement between the resultant flux and potential flux
Part 3 = T_M expressed as a function of permanent-magnet flux
Part 4 = T_P expressed as a function of potential flux
Part 5 = T_C expressed as a function of current flux
Part 6 = T_F expressed as a function of disk speed plus a friction-torque component independent of speed
k_1 = constant which includes the geometrical constant, gravitational constant, etc.
f = circuit frequency
f_d = rotational speed of the meter disk
k_f = frictional constant for the disk
F_0 = friction constant

During the interval of transition from one load value to another, the torque equation must contain another term. This term, which may be called the transition torque, may be a driving or a retarding torque and will be of sufficient magnitude to balance the torque equation at every instant. The transition torque will be a retarding torque during an increase in load and a driving torque during a decrease in load. The rate of change of the transition torque during a transition interval will depend upon the changes in the other components in the torque equation. Since the retarding torques are a function of disk speed and the meter registration is $\int f_d dt$, the meter registration for loads which are for the most part transitory in nature, will depend on the effect of the transition torque.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Induction-Motor Characteristics at High Slip

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THE practical work of inventing the polyphase induction motor, by Nikola Tesla and Galileo Ferraris, was followed by farseeing analytical studies of motor operation. Masterly theoretical analyses of the induction-motor circuit were made by B. A. Behrend, Alexander Heyland, and others. These men, through their insight and understanding, were able to devise ingenious methods for theoretically predetermining operating characteristics of induction motors by employing readily determined constants. For many years these methods of predetermination were accepted and used in practical work. However, as knowledge has expanded and a higher degree of accuracy becomes requisite, it has been found that the older methods of analysis produce inaccuracies which are too great for modern requirements. The trend away from the older methods is clearly shown by abandonment of the use of the classical "circle diagram" in cases where highly accurate results are desired. The use of the former methods of predetermination results in inaccuracies due to several factors, the most important being: (*a*) unjustified treatment of the exciting current, (*b*) the assumption that circuit parameters are constant when they are not, and (*c*) failure to consider the stray load loss. The errors resulting from wrong assumptions may not prove serious for light loading of the motor, but discrepancies increase as load and slip become greater.

A study is made in this paper of the factors which produce rotor losses and corresponding torque in induction motors. A method of accurately analyzing this problem is given, based upon elemental principles and including important effects which have been omitted from previous studies. In order that correct principles may be applied and accurate results obtained in practical determinations, a complete test procedure with required calculations is outlined. The principles developed are illustrated by tests on a selected motor. However, these principles are general in nature and are not limited in their application by the type or size of induction motor. In approach and general treatment of the subject the analysis differs from previously accepted methods. This difference is pronounced in two important respects. In the first place, the analysis gives consideration to the effects of stray load loss. In order to predict results the laws of variation of this loss are investigated and it is shown that it varies as the square of both current and motor speed. The fact that this loss plays a highly important part in the production of motor torque at all higher values of slip is made clearly evident. Secondly, a study is made of the rotor-resistance loss, showing that variations in the value of rotor resistance become a most important factor in such considerations. In this connection, the rotor effective resistance is studied and its variation with the frequency of the rotor circuit is determined. The manner in which the resulting loss operates to produce motor torque is shown clearly by the analysis. Finally, the net over-all torque is derived by combining the separate components. The results show great discrepancies when compared with conventional values.

Investigation of induction-motor characteristics was made over a range of slip extending from zero up to a value of 200 per cent. Accurate determinations show that the actual torque of a motor at high slip may have a value of two or three times that calculated by conventional methods. Discrepancies of this nature have been noted by other investigators[1] and are of such magnitude as to demand

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[1] For all numbered references, see list at end of paper.

attention. In modern applications a motor may be reversed by plugging, at which instant the slip is 200 per cent. In such reversing cycles both the heating of the motor and the torque developed throughout the cycle become important considerations. The extent of the heating is determined by the magnitude of the losses and the time required to reverse the motor, the latter depending upon the torque developed. The magnitude of instantaneous torque also becomes an important factor in shaft design. In addition, the results of the investigations with which this paper is concerned have a direct application in problems of polyphase unbalance. In applying the symmetrical-component method of analysis, the action of the negative-sequence current is made truly predictable through the application of the principles herein deduced.

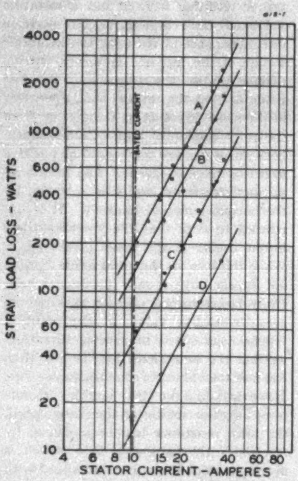

Figure 1. Curves of stray load loss as a function of stator current for different rotor speeds

Curve	Rotor Speed in Per Cent of Synchronous Speed	Rotor Frequency (Cycles Per Second)
A	100	120
B	80	108
C	50	30 and 90
D	25	75

Experimental Investigation

In order to make a practical study of motor characteristics, tests were made on an ordinary squirrel-cage induction motor. To insure that the effects of stray load loss would not show themselves in undue proportion, a motor was selected in which this loss was reasonably low, being 2.08 per cent of full-load input at rating. It was fortunate that considerable test information was already available, this motor having been used in previous tests.[3,4] The motor was rated at ten horsepower, 550 volts, 10.3 amperes, three phase, 60 cycles, and 1,750 rpm. The stator had 48 teeth with semiclosed slots, and the rotor 57 teeth with coffin-shaped slots.

The motor was directly connected to a calibrated d-c machine by a flexible coupling. Power from a three-phase a-c source, having balanced voltage which could be adjusted to any required value, was applied to the stator. Instruments were so arranged that both electrical power input and output could be accurately measured. Measurements were taken at several values of current while the speed was held constant and observed by the use of a stroboscope. Motor current was adjusted to any desired value by control of the applied three-phase voltage. Measurements were made over a range extending from rated current to between three and four times this value. The stator resistance was measured by a resistance bridge immediately following each run, and the friction and windage loss of the two coupled machines was measured at the same time by driving through the d-c machine. Complete runs were made for speeds of 100, 80, 50, and 25 per cent of synchronous speed in the reverse direction to the stator field, for zero speed with locked rotor, and for 25, 50, and 80 per cent of synchronous speed in the same direction as the stator field.

Stray Load Losses

For all points at synchronous speed in the reverse direction values of stray load loss were calculated by the reverse-rotation method.[4] Each point was obtained by taking the difference between the net mechanical power input to the rotor and the net electrical power input to the rotor received from the stator. The results are shown by curve *A* of figure 1 where the loss is plotted as a function of stator current on logarithmic cross-section paper. Curves *B*, *C*, and *D* in this figure show the stray load loss for speeds of 80, 50, and 25 per cent of synchronous speed respectively. The three latter curves are obtained in the same manner as curve *A* except that care is taken to subtract the appropriate value of net rotor power supplied by the stator for each speed. These amounts are 0.8, 0.5, and 0.25 times the rotor power received from the stator for corresponding speeds.

The black points of curve *C* were obtained by operation of the machine at 0.5 speed in the same direction as the stator field (that is, for ordinary motor action). In this case the stray load loss is obtained by subtracting the measured value of net mechanical power developed from one-half of the rotor power received from the stator. The close agreement between values obtained in this manner with those obtained from reverse rotation at the same rotor speed indicates the degree of accuracy of the reverse-rotation method at this particular speed. This substantiates the assumption that the stray load loss is not affected by the direction of rotation.

Curves *A*, *B*, *C*, and *D* of figure 1 all have a slope of two indicating that the stray load loss varies as the square of the current for all values of speed.

In order to determine the law of variation of stray load loss as a function of motor speed, values of the loss at different speeds were taken from the curves of figure 1 at the point of rated current. In figure 2 these quantities are plotted as a function of motor speed on logarithmic cross-section paper. The fact that the resulting curve has a slope of two indicates that the stray load loss varies as the square of the rotor speed for constant current.

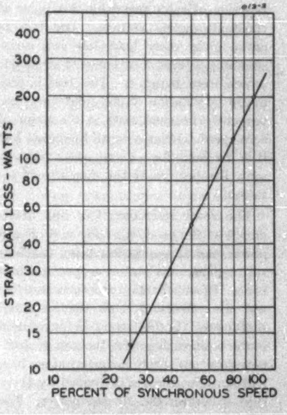

Figure 2. Stray load loss as a function of speed at rated current

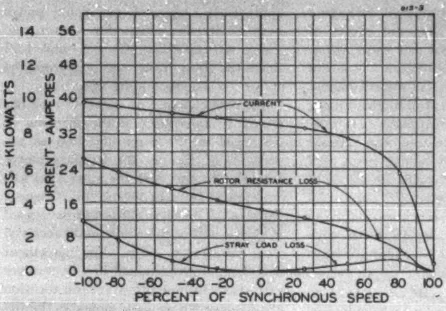

Figure 3. Stator current and losses at one half voltage as function of rotor speed

Motor Characteristics at One-Half Voltage

Since it was impossible to obtain measurements at full voltage for the speeds selected without damaging the motor due to overheating, the characteristics of the motor were first obtained at one-half rated voltage. The top curve of figure 3 shows measured stator current as a function of rotor speed for this value of constant applied voltage. The bottom curve gives stray load loss for corresponding speeds and currents as determined from figure 1. This loss is produced by rotation of the motor and consequently becomes zero at the point of zero speed. Being a tooth-frequency loss it is the same for a given speed and current, irrespective of the direction of rotation.

The rotor resistance loss was determined as the product of slip and air-gap power, the air-gap power being taken as stator input less stator copper and iron losses. In addition the rotor resistance loss was also calculated from the measured shaft power. In this latter case for rotation in the positive direction, the sum of shaft-power output, friction and windage loss, and stray load loss was divided by (1 — slip) to obtain the air-gap power. For rotation in the reverse direction the sum of friction and windage and stray load loss was subtracted from the mechanical power input to the shaft and the difference divided by (slip — 1) for obtaining air-gap power. The fact that the values of the rotor resistance loss obtained from the stator electrical power input and from the mechanical shaft power showed good average agreement indicated that the iron loss due to the stator leakage flux was of negligible importance for these calculations.

The rotor resistance loss thus obtained is of the nature of an *effective* resistance loss and includes all components of loss that occur as a result of the slip-frequency rotor currents. In addition to the slip-frequency copper loss component, there may be components of eddy-current losses in the rotor circuit and slip-frequency iron losses caused by the air-gap and rotor-leakage fluxes. This effective resistance loss of the rotor is shown by the middle curve of figure 3.

Each loss produces its own value of torque. The rotor effective resistance loss results in a force from the reaction of slip-frequency currents in the rotor and the air-gap field rotating at synchronous speed. Thus the torque can be calculated from the power loss and the difference in speed of the rotor and the synchronous field (that is, the slip speed). The dashed-line curve of figure 4 shows the torque resulting from the rotor effective resistance loss. At small values of slip a small loss produces a large torque while at high slip it takes a large loss to produce the same torque. The torque resulting from the rotor resistance loss is the only component of torque which in the past has been considered in conventional calculations. The curve of rotor resistance loss shown in figure 3, and corresponding torque of figure 4 differ from customary calculations in that they are derived from actual measurements, instead of being calculated from a value of rotor resistance which is usually considered as constant.

The stray load loss produces a drag on the rotor which in this respect is similar to friction and windage effects. Thus the torque resulting from it has a negative value over the complete range of positive directional speeds, reducing the available torque of the motor. However, during reverse rotation the torque from this loss tends to stop the motor, thus operating in the same direction as the torque from the rotor resistance loss. Consequently both quantities are considered as positive over this range of speeds. The torque resulting from stray load loss is shown by the bottom curve of figure 4. It is important to note that the stray-load-loss torque occurs at the speed of the rotor. While this loss (as shown in figure 3) does not appear large it may produce a relatively larger torque. If for example we consider relative values at the point of synchronous speed in reverse direction, we find the stray load loss is less than one-half the value of rotor resistance loss. However, while the stray load loss results from forces occurring at the rotor speed, the rotor resistance loss is produced by reaction with the stator field which is moving at double synchronous speed with respect to the rotor. Since the torque for any rotor loss varies inversely as the speed of the force reaction producing it, the difference in speed results in torque from the stray load loss which is nearly equal to that from the rotor resistance loss at this point.

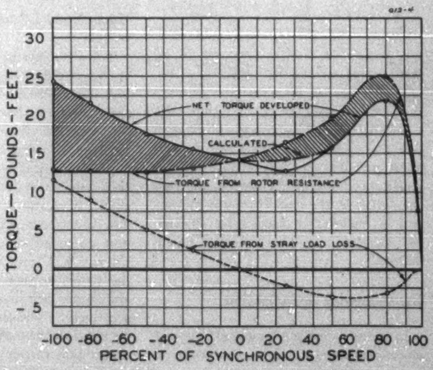

Figure 4. Torque curves for one-half-voltage operation

The combination of the torque from stray load loss with that from the rotor resistance loss gives the net torque available to drive the rotor. Stray load loss causes a reduction in torque over the range of direct rotation which is shown by the shaded portion to the right of the point of zero speed in figure 4. For reverse-rotation operation the torque from stray load loss causes a marked increase in the net torque tending to stop or reverse the motor. This effect is shown by the shaded portion at the left of the diagram. It is significant that the measured shaft torque, after being corrected for friction and windage, gives a net value which is in complete agreement with the resultant of the two components, with the exception of a short portion of the curve between zero and one-half speed in the positive direction. The discrepancy over this range is considered to be the result of torque effects caused by harmonic components of flux. The full-line curve marked "net torque developed" in figure 4 was obtained from measured output.

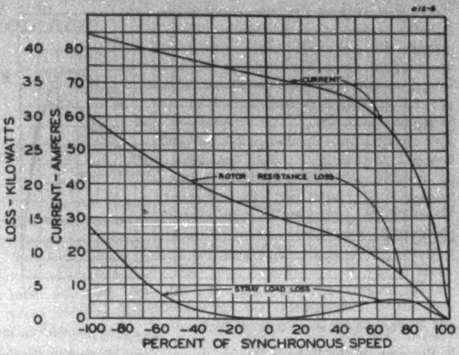

Figure 6. Curves of current and losses at full voltage

Rotor Effective Resistance

All losses which occur at slip frequency in the rotor have been considered as rotor *effective* resistance loss. This loss was found to vary as the square of the current for a given frequency but to have different values for the same current at different frequencies. This indicates that the effective resistance of the rotor is not constant. Its value for one phase of an equivalent wye-connected rotor was obtained by dividing the total loss by three times the square of the rotor current. The equivalent effective resistance for each phase of the rotor circuit is shown by the curve of figure 5. Since the total loss includes among its components hysteresis and eddy-current iron losses due to the rotor leakage flux, it could not be expected that the effective resistance would remain constant at different rotor frequencies.

The curve shows that the rotor resistance increases appreciably at higher frequencies, being about 1.68 times as large at 120 cycles (synchronous speed in reverse direction) as it is at very low frequencies. At zero speed (locked rotor) the resistance has risen to 1.18 times its lowest value.

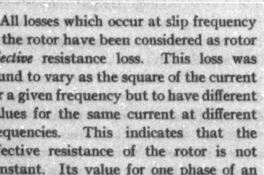

Figure 5. Curve of rotor resistance per phase as a function of frequency in the rotor circuit

It was found that the resistance could be expressed by a mathematical equation of the following general form

$$R_{\text{eff}} = a + b\epsilon^{mf}$$

where R_{eff} is rotor effective resistance in ohms per phase, f is frequency in cycles per second, ϵ is the base of natural logarithms, and a, b, and m are constants. Supplying values of the constants for this particular case gave the equation

$$R_{\text{eff}} = 0.74 + 0.103\epsilon^{0.0156f}$$

Characteristics at Full Voltage

Knowing the value of rotor effective resistance and the laws of variation of stray load loss with current and frequency, motor characteristics at full voltage can be easily calculated if current values can be determined. The latter were obtained by extrapolation of current-voltage curves taken at constant speed. A slight curvature in such curves, extending up to a point of approximately twice rated current, indicated small effects due to tooth saturation. For higher values of current the curves become straight lines permitting accurate extension to the point of rated voltage. Current values obtained in this way are shown by the top curve of figure 6. The rotor resistance loss then was calculated using values taken from figure 5, and the stray load loss was obtained by extrapolation of curves of figure 1. The resultant loss curves are also shown in figure 6.

Torque values corresponding to each of the losses were obtained in the manner previously described for the one-half voltage characteristics. The resultant net torque curve was then found by combining the torque components as in figure 4. This net torque developed at full voltage is shown by the full-line curve of figure 7. Stray load loss has a more pronounced effect in altering the torque curve at full voltage than at one-half voltage due to the fact that this loss and its corresponding torque vary as the square of the current. The full-voltage torque curve is obtained on the assumption that both rotor and stator resistance remain unchanged from heating by the high currents, which would not be the case if such currents were maintained for more than a very short period. This torque curve is quite accurate except over the range of motoring speeds extending from zero to approximately one-half synchronous speed. Over this range harmonic fluxes may cause changes in torque which are difficult to predict.

In order that this speed-torque characteristic might be compared with the curves calculated in the conventional way from constants measured under locked-rotor conditions the latter are also given. The dotted-line curve marked "theoretical—*A*" is derived from circuit parameters measured at zero speed with current corresponding to the point of rated voltage. Consequently it agrees with the actual curve at this point. The dashed-line curve labeled "theoretical—*B*" is computed from locked-rotor circuit parameters taken at the point of rated current. The rotor effective resistance was the same for these two cases, but the combined leakage reactance was larger for the case of rated current. This latter difference was due to magnetic saturation

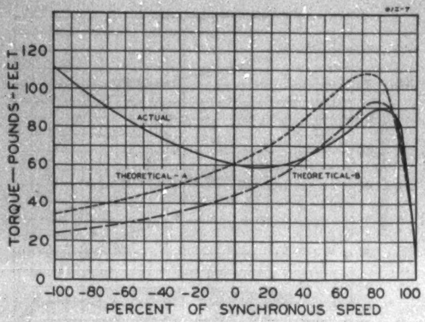

Figure 7. Comparison of actual torque to be expected with torque curves computed from locked-rotor measurements

in the path of the leakage flux as above mentioned. This difference in value of leakage reactance produces very different theoretically computed speed-torque curves. However, the important fact to note is the pronounced difference between the actual torque and that obtained by calculations from locked-rotor data. At synchronous speed in the reverse direction the actual torque developed is more than three times that computed by conventional methods.

A New Accurate Method

The ability to obtain accurate characteristics of induction motors without actually measuring them is most desirable. It is evident that if such predetermination is to be made it will require more information than can be obtained from locked-rotor and no-load tests. The effects of stray load loss and the variation of rotor effective resistance as a result of change in rotor frequency must be taken into consideration. These effects may be included readily by adopting the following procedure.

In addition to the customary locked-rotor and no-load tests two additional tests become necessary. These are a reverse-rotation test and an ordinary load run. The reverse-rotation test has been fully specified in a previous paper[4] which should be referred to for a complete description. This test, which is made at synchronous speed in reverse direction, should include data over the range of current from rated value up to as high an amount as can safely be used without overheating the machine. The test will give stray load loss as a function of current at synchronous speed. By application of the law that this loss varies as the square of current and speed its value can be calculated for any required condition. The rotor effective resistance loss can be readily obtained by taking twice the air-gap power for points of test. Knowing the resistance loss for given values of current the rotor effective resistance can be calculated readily at double frequency. A current-voltage curve can also be plotted, which when extended as a straight line above the point of saturation will give the current for rated voltage.

The locked-rotor test would be made as in the past over as large a range of current as possible. The results should be shown in a current-voltage curve which may be extended as above to the point of rated voltage. Impedance per phase can be calculated from voltage and current at this point. Then by using the average value of input effective resistance a value for total inductive reactance can be obtained. Measurement of stator resistance will permit determination of the rotor resistance by separation.

An ordinary load run should be made in which the slip is measured with high accuracy. Output power is not an important measurement as air-gap power can be obtained by subtracting stator copper and iron losses from measured input power. Multiplication of air-gap power by slip will give the rotor resistance losses. Rotor current is found by vector subtraction of the exciting component from the stator current. Rotor resistance per phase is then computed from rotor losses and rotor current.

We now have three points of rotor effective resistance, that is, points at low rotor frequency, locked rotor, and double frequency. This will permit the drawing of a curve of rotor resistance similar to that of figure 5. It now remains only to find current values for rated voltage in order to determine rotor resistance loss and stray load loss. Current at no load and at rated load has been obtained by measurement. Also, values of current are available for the locked-rotor point and for that of synchronous speed in reverse direction. This leaves points of current between that of zero speed and full load to be determined. Values of rotor resistance over this range are available from the curve. By assuming values of slip and dividing the corresponding rotor resistance by it we obtain the equivalent circuit secondary resistance. The total input resistance is obtained by addition of primary resistance to secondary equivalent resistance. This may be combined with the input reactance obtained from the locked-rotor test to find the total input impedance. It has been found that leakage reactance remains nearly constant over this range of speeds, but any slight variation therein is not serious because of the greater importance of the rotor equivalent resistance. Knowing the equivalent impedance, current values can then be calculated for any given speed. As the current curve appears to be almost a straight line between zero speed and synchronous speed in reverse the complete current curve can now be drawn. Loss curves can be determined and corresponding torque curves obtained. Resultant torque is then found by combination of the component values. The experimental work necessary to obtain accurate determinations as above described is not increased greatly over that required for conventional determinations, the main additions being the reverse-rotation test and the measurement of slip for a corresponding value of power input under load. Calculations are simple and straightforward with the separate steps following in a logical manner.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Control of Inductive Interference to Telegraph Systems

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FELLOW AIEE

THE tendency of power-supply and distribution systems to cause extraneous currents to flow by induction into telegraph lines, has presented a diversity of problems which are of concern to both interests. No one solution is found to be of general application; rather it ordinarily is necessary that each situation be made the subject of individual study.

In different instances it may be most economical to control the interference by attention to the design of the power system, or by choice of routes for each line, or by mitigative equipment applied directly to the telegraph system. The present paper will be limited in scope to the third class of control methods; it will describe various special devices which may be applied in different cases to the telegraph circuit for reducing the power interference, with indications of their accomplishments and of their limitations.

The discussion herein is in particular reference to interference which tends to continue during normal working of the power systems, although some of the devices referred to are also effective in removing those more severe surges which occur at times of power line or equipment failures. Extraneous voltages are induced where power and telegraph lines parallel, and tend to reduce the capacity and the dependability of the telegraph. An exposure one mile in length at a separation of a few hundred feet may result in a small or a severe interference dependent upon the nature of the power system and type of telegraph circuits. The voltages are caused by the magnetic field created by the power system, which interlinks the telegraph wires. The electric field from the power line is seldom of importance.

Usually the fundamental frequency of the power system, 25, 50, or 60 cycles, is of chief concern. Sometimes the third harmonics of these create interference. In our experience no cases have occurred where harmonics higher than the third have created interference to physical telegraph circuits. The interference is most severe where some part of the power current flows in the earth, as may be the case when neutral points are grounded, or as always occurs in electric-railway practice.

As distinct from power practice the telegraph circuits ordinarily are not worked at any standardized frequencies. The assigned speed of each telegraph depends upon the amount of business between the terminuses; economical considerations require that where the business is sufficient to justify, the speed shall be the maximum practical working capacity of the line between the points.

The consideration that in this country there are many situations where large volumes of telegraph business exist between cities widely separated, has created a strong incentive to develop the maximum telegraph line speeds. As part of this objective Western Union has endeavored to survey every practical method for minimizing the effects of power induction.

Review of Theory of Telegraph Transmission

The transmission of intelligence by telegraph requires that current, controlled in accordance with a code and representing the message to be sent, shall be impressed on the line and shall be correctly interpreted at the distant end of the line. The discussion herein will be in particular reference to the multiplex printing telegraph system, which serves for the more important message circuits and which utilizes probably the most efficient available code. However, the fundamental problems of line transmission are the same in all telegraph systems.

In the multiplex each letter or figure is represented by a code character consisting of five units of time. During each time unit there is applied to the line either positive or negative battery as indicated in figure 1, the battery ordinarily being of 160 volts. No space occurs between consecutive characters. A measure of the speed of the circuit is the "dot frequency" or the "fundamental signaling frequency" of the circuit, which is the reciprocal of the time required for two consecutive units in the code character.

During its passage along the line the telegraph signal becomes rounded and the current becomes weakened. The practical working circuit speed is limited by the amount of weakening of the signal which can be tolerated in the presence of such extraneous currents as then exist. Any induced currents from external sources are of course superposed on the received telegraph current, and amounts of induction exceeding about six volts are sufficient to cause a measurable impairment of the telegraph.

In field measurements of telegraph transmission it is customary to examine the current sent and received, and the rate of change of the current between reversals. However, for analytical purposes it often is useful to regard the circuit as an a-c transmission system. While telegraph transmission would appear superficially to be wholly different in principle from that of power supply, there is only this basic difference, that while the power supply system need transmit one frequency only, the telegraph circuit must be capable of sending a band of frequencies the lower limit of which is zero frequency and the upper limit of which is somewhat above the dot frequency.

If in the telegraph transmitter a given combination of signals is set up and is

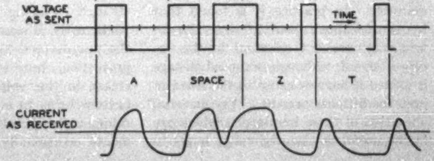

Figure 1. Typical signals in three-channel multiplex printing telegraph system

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repeated over and over, the combination may be analyzed by well-known methods into the sum of a number of sinusoidal waves of definite frequencies. In figure 2 are shown the frequencies contained in certain signal combinations when repeated again and again. Values above 200 cycles are not shown, although actually they continue with decreasing amplitude to infinite frequency.

Commercial telegraph traffic, of course, is made up of random reversals of voltage rather than of recurring signal combinations. In strictly random traffic the frequency spectrum is found to be continuous, that is, it contains substantially *all* frequencies from zero upwards, rather than certain discrete frequencies.

If the telegraph circuit were to transmit all frequencies without reduction in amplitude and in correct phase, the received signal would be identical to that sent. In practice the upper frequencies are reduced or removed in traversing the line, and the effect on received signal shape is to cause the rounding previously referred to. A certain degree of such rounding is not objectionable and may be desirable. In practice it is found that frequencies above about 1.6 times the dot frequency are not essential in the received signal, although some advantage is gained if harmonics up to the third or even the fifth are present. Any material alteration of those frequencies which are in the useful or essential range tends to cause distortion or mutilation of the received signals.

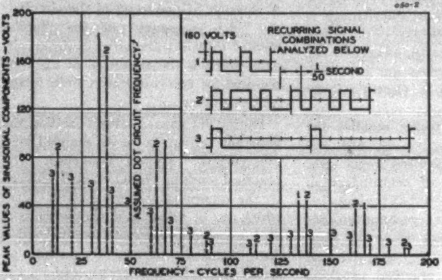


Figure 2. Examples of analyses of repeated telegraph signals into their sinusoidal components

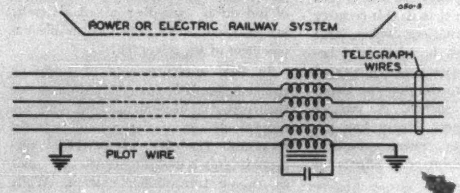


Figure 3. Multiwire neutralizing transformer

Devices for Separating Power Induced Voltages From Telegraph

Once extraneous voltages have been induced from a power system into telegraph, their removal is difficult only because of the need for minimizing any direct impairment to the telegraph. Equipment for removing the interference must not introduce any material added impedance into the telegraph nor remove any essential components of the telegraph currents, otherwise its objective is defeated. Among telegraph circuits carried along a common route there ordinarily is some tendency to induce currents known as "cross fire" into each other; the equipment for removing power interference must not increase such cross fire.

Interference of course can be removed by replacing the customary ground-return telegraph circuit by a metallic telegraph, using two line wires instead of one. Or a carrier telegraph may be substituted. Placing the wires in cable, particularly if armored, is effective for the purpose and also affords mechanical protection, but is expensive. An increase in the voltage of the telegraph battery helps to a small extent in overriding induced voltages. Such arrangements occasionally may be justified to reduce power interference, but hardly can be considered to be a general solution as a co-ordinative measure.

Devices for removal of induced power voltages are of two general classes. In the first a pilot wire set aside for the correction, is placed on the pole line carrying the telegraph wires and extends throughout the length of the exposure to power. The second class of devices depends upon tuning to remove the sinusoidal power current.

Mitigative Systems Including a Pilot Wire

The pilot wire is exposed to the same induced voltage as the telegraph wires. This is conducted by the pilot wire into suitable equipment which in turn applies to the telegraph wires a voltage to neutralize the interference.

NEUTRALIZING TRANSFORMER

Illustrated in figure 3, this is a relatively simple device which under suitable conditions is fairly effective. In one form it was used early by Professor C. F. Scott. All wires on the pole line may be corrected simultaneously, all transformer coils in the multiwire transformer being wound on the same iron core.

As at present developed the step-up ratio is only slightly above unity, and the inductance of the primary winding is tuned to the disturbing frequency. To prevent partial saturation of the core by currents in the telegraph wires, an air gap is included in the core. The correction of voltages up to 200 in an exposure 20 miles in length, has been obtained with a transformer weighing 500 pounds, in as many as 34 wires carried on the same pole line. The removal of power interference is nearly perfect unless limited by conditions such as local earth potentials at the grounding points, but the system tends to add cross fire between the various telegraph circuits.

As in all other correcting systems it is necessary to examine the direct effect of the transformer on the telegraph currents. The transformer adds impedance in each telegraph wire, but more important is the cross fire induced between

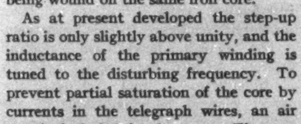


Figure 4. Neutralizing transformer for correction of two frequencies

the various wires. These effects may be controlled by holding to a low value the resistance of the primary circuit. That is, a mutual impedance exists between each pair of telegraph circuits, which may be considered to consist of an inductance shunted by the capacitor and by the primary circuit. The resistance of the latter including the wire and its earth connections should not exceed 40 ohms and preferably should be lower. To this end there have been used pilot wires as large as number 3 Brown and Sharpe gauge, with earth connections made of a dozen paralleling grounding pipes. The limitation in resistance obviously sets a limit on the length of exposure in which a multiwire neutralizing transformer can be used economically, where high-speed telegraph circuits operate.

If the telegraph circuits are worked at low speed, or if a separate pilot wire is provided for each telegraph circuit, the limitation as to length becomes less difficult. Individual line neutralizing transformers have been used in exposures up to 60 miles in length using a pilot wire of number 9 Brown and Sharpe gauge in a relatively low-speed telegraph circuit, for correction of 25-cycle interference.

The neutralizing transformer may be used for simultaneous correction of several frequencies. An arrangement in service for removing 25- and 75-cycle interference is shown in figure 4, and includes, in addition to the transformer, a second capacitor and an added reactor.

The above transformers were designed for use during normal working of the disturbing power or railway system. A unique application has been made in a five-mile exposure of an electrified railway to circuits of a burglar-alarm system, where frequent railway short circuits caused false alarms by induction. In this case the induction reached values over 100 volts which were interrupted in about one cycle through the operation of railway circuit breakers. A transformer was provided which could adjust itself sufficiently quickly to correct the transient voltage, by tuning to 25 and 50 cycles through the use of two coil-capacitor units shunting the primary coil, and was effective in neutralizing the abrupt severe surges of voltage.

NEUTRALIZING GENERATOR

The neutralizing generator formerly has been in service use but is now superseded by the system next to be described. It will be discussed briefly because of the light it throws on the general problem.

Various arrangements of this have been developed, of which one is shown in figure 5. A multiwire transformer is energized by means of an a-c generator, and counterbalances the induced voltages both in telegraph wires and in the pilot wire. Any residual voltage in the latter is rectified in a mechanical rectifier driven synchronously with the generator, and after amplification serves to correct the voltage of the generator and the speed of the driving motor until the correct compensation is effected.

The system is effective in correcting interference, but is sluggish in adjusting itself when changes occur in the induced voltage. All a-c components of the telegraph signals must flow through the generator armature, and in order to minimize the addition of impedance in the telegraph wires and the tendency to cause cross fire between them, the impedance of the armature must be held to a low value. The pilot wire may be of the wire size ordinarily used for telegraphy, and the system is practical for exposures much longer than is the multiwire neutralizing system previously described.

NEUTRALIZING AMPLIFYING SYSTEM

This system is probably the most valuable of those available. It has made it possible to maintain practically normal operation in over 260 telegraph wires which are exposed to interference at 25 cycles from the electrified zone of a large eastern railroad, where voltages approaching 200 volts are induced. The system is also in use elsewhere for correcting interference at 60 cycles.

The neutralizing amplifying system is illustrated in figures 6 and 7. As with systems previously described, a transformer serves to impress on each telegraph wire a voltage to counteract the disturbing voltage induced in line wires. In this case the transformer is energized by a vacuum-tube amplifier. The voltage induced in the pilot wire is also opposed by the voltage in one coil of the transformer, and the residual is impressed upon the amplifier. The ratio of the potentiometer is not the same as the turns ratio of the opposing coil with respect to the line coils. Through adjustment of each of these ratios it is possible to obtain a step-up action which provides neutralization approaching 97 per cent. Since the system is aperiodic, it is able to neutralize sudden surges of voltage such as caused by power-system short circuits, as well as continuous interference within the voltage limitations of the equipment.

The manner of providing for the passage of the useful telegraph currents is unique. It might appear that the arrangement would impress a high impedance upon the telegraph currents, and would tend to add cross fire between circuits. The circuit, however, is such that a current is caused to flow in the transformer coil in the output of the amplifier, which (corrected for turns ratio) is equal to the sum of the telegraph currents in all line coils, thus cancelling any flux in the transformer due to telegraph currents. If the current in the output coil departs from the amount to

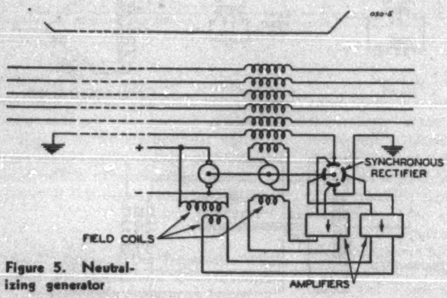


Figure 5. Neutralizing generator

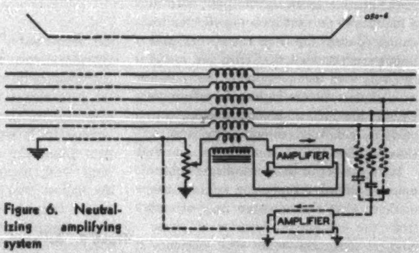


Figure 6. Neutralizing amplifying system

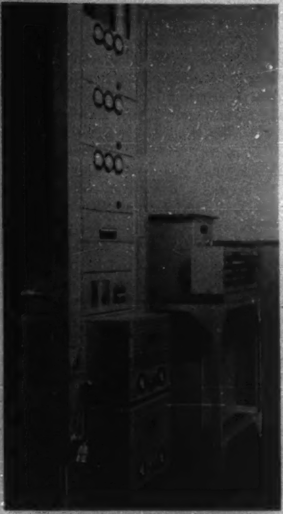


Figure 7. Neutralizing amplifying system for correction of 150 volts at 25 cycles in 60 telegraph wires

balance the telegraph currents, a residual voltage is immediately impressed upon the input of the amplifier tending to correct that condition.

A complication occurs in connection with the cross fire which normally occurs between telegraph wires carried on a given pole line. If the cross fire between all circuits including the pilot wire were equal, this system would also be effective in reducing the cross fire. Actually it is impracticable to locate the pilot wire in a manner to prevent its being affected by enhanced cross fire from a few wires, and unless compensated that cross fire would interfere with the operation of the system as a whole. Where high-speed telegraph circuits are present a correction for this crossfire condition is necessary and is shown in dotted lines in the figure.

With one set of the neutralizing equipment as shown, a limitation exists in the distance that the exposure may extend from the equipment, because of phase changes along the wire. The efficiency tends to decrease where the distance exceeds 20 miles in the case of 60-cycle interference, or where it exceeds 40 miles with 25-cycle interference. These distances are reduced if the telegraph wires are in cable. These limitations are removed by utilizing two sets of equipment, one at or beyond each end of the exposure, in which case the pilot wire may be 200 miles or more in length.

An arrangement somewhat similar in principle to the one illustrated was first shown by H. M. Trueblood of the Bell Telephone Laboratories. In its present form the system was developed here by W. D. Cannon.

Mitigative Systems Dependent Upon Tuning

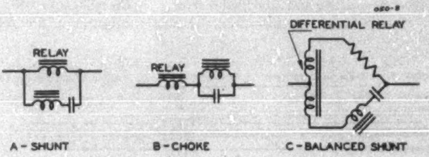
Many combinations of reactors and capacitors have been proposed with the view to reducing or removing extraneous sinusoidal currents by tuning. The expense of a pilot wire is thus avoided. The tuning removes those same frequencies from the useful telegraph currents and thus may impair the operation of the telegraph circuit by an amount dependent upon the relative frequency of the telegraph and the interfering frequencies, and upon the breadth of the tuning. A description of a small number of tuned devices will suffice to illustrate their practical value in mitigating interference.

Resonant Shunts and Resonant Chokes

A simple resonant shunt or choke, as shown in figures 8*A* and 8*B*, was introduced early for removal of interference. It may remove 95 per cent of the interference, but because of its coincident removal of useful telegraph currents it can be used only where the dot telegraph frequency is less than about half of the interfering frequency. This greatly restricts its use in modern systems of telegraphy. The effectiveness of the coil increases with its size; the above statements are based upon the use of an inductor weighing 20 pounds.

While simple resonant devices cannot be used for removal of 25- or 60-cycle interference from high-speed telegraph circuits, the resonant shunts are effective in such circuits where the interference results from power harmonics at 180 cycles. For application where conditions permit, the resonant shunt is placed in parallel with the working telegraph relay. The resonant choke is convenient for use in "way" telegraph circuits where a number of stations operate on the same line, since one choke may serve where several of the resonant shunts would be required.

Figure 8. Resonant devices for reducing interference

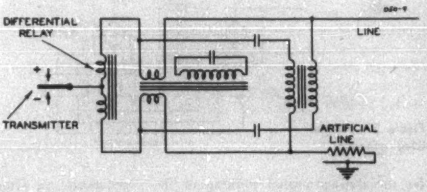


Balanced Double-Resonant Network

A balanced arrangement shown in figure 8*C* was proposed by E. Blakeney and R. E. Chetwood, which in theory may remove all of the interference. The resistor is adjusted to equal the a-c resistance of the coil and capacitor at resonance. Advantage may be taken of the arrangement to increase the value of the inductance and reduce the amount of capacitance, with the result that the impairment to the telegraph is much reduced.

That arrangement has been little used, but a development from the same principle is the balanced double-resonant network shown in figure 9. The theory of this is rather involved, but it may be considered that the a-c resistance of a double-resonant shunt is balanced against that of the resonant choke combination in a manner to remove nearly all interference. Partly through the circuit design and

Figure 9. Balanced double-resonant network



partly through an increase in the weight and effectiveness of the inductors, a much improved performance results. Each inductor is designed to offer the highest ratio of reactance-to-resistance obtainable within practicable size limits. The cores are of silicon steel and contain air gaps. This device is wholly practical, and with some moderate impairment of the telegraph currents it may be used where the telegraph dot frequency approaches nine-tenths of the interfering power frequency. Typical plots showing the efficiencies of various tuned devices in removing interference are shown in figure 10.

The effectiveness of the balanced double-resonant network is dependent upon the power frequency remaining nearly constant. Failures of the device in removing interference have occurred because of variations in the frequency of the disturbing power system.

Synchronous Selective Neutralizing System

The arrangement just described provides nearly the practical limit of sharpness of tuning which may be secured with direct use of coil-capacitor tuning. It is possible to obtain selectivity many times closer through the use of mechanical tuning, or by an arrangement which involves rectification, a delay network, and reconversion to alternating current. A system of this type is indicated in figure 11. With such tuning as plotted in figure 10, the amount removed from the telegraph frequency spectrum is so narrow, presuming that random telegraph traffic is transmitted, that the impairment to the telegraph signals is not serious, and the telegraph dot frequency is not limited to values below the disturbing frequency as was the case with other tuned devices.

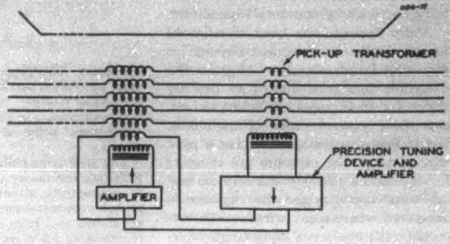


Figure 11. Synchronous selective neutralizing system

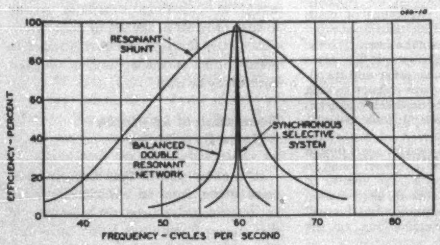


Figure 10. Efficiency of removal of sinusoidal currents by tuned devices

Table I. Telegraph Dot Frequencies Which Conflict With Operation of 60-Cycle Synchronous Selective Neutralizing System

Two-channel multiplex	25.0.....27.3.....33.3 37.5
Three-channel multiplex	37.5.....40.9.....45.0 50.0.....56.2
Four-channel multiplex	50.0.....54.6.....60.0 66.7.....75.0

The values listed include only those which occur within the customary range of operation of the stated equipment.

In practice a complication occurs because the telegraph signals are not wholly random. Certain characters, for example the letter "e", the space between words, etc., occur more frequently than others. When one multiplex channel is idle a blank is transmitted repeatedly, and at times circuit testing is done with a recurring set of characters. When telegraph signals containing such combinations are analyzed into their sinusoidal components (see section on telegraph theory) it is found that certain frequencies are more prominent than others and if one of those frequencies should nearly coincide with the interfering frequency there is impairment of signals and possible cross fire between circuits.

The values of those prominent frequencies have been computed by setting up combinations of signals likely to occur. For a two-channel multiplex it is found that the prominent frequencies are given by the formula $nF/5$, where F is the dot telegraph frequency and n takes all integer values from one to infinity. For the three-channel multiplex the formula is $2nF/15$, and in the four-channel multiplex it is $nF/10$. An exception is that frequencies of $2F$, $4F$, $6F$, etc., are not present, and frequencies near those values are of little importance.

There are, therefore, limitations upon the telegraph circuit speeds which can be worked to best advantage on a line where a synchronous selective neutralizing system is in use. A list of the telegraph dot frequencies to be avoided under these circumstances, as determined from the above formulas, where the interference is at 60 cycles, appears in table I.

Provision for passage of the useful telegraph currents is made in the same manner as in the neutralizing amplifying system previously described. The system is of course quite complex, and has the further disadvantage that a time interval of the order of 20 seconds is required for it to readjust itself for changes in amount of induced voltage, this sluggishness being a corollary of the extraordinarily sharp tuning. During that interval the interference remains partly uncorrected. Arrangements are incorporated for automatic adjustment of the frequency of the tuning to follow normal changes in the frequency of the power system. Interference from two unsynchronized power systems cannot be neutralized by a single set of this equipment.

Discussion and Conclusions

This discussion of methods for mitigating interference obviously has been quite one-sided, since due to limitations of space there has been no discussion of matters of design of the power system which may influence the interference, yet in many cases the best solution will call for some modification of the plans of the latter. Such matters are dealt with in various other publications. In general those features should receive consideration with every exposure, since the only procedure that can be justified is that in which the greatest total benefit is obtained with a minimum total expenditure.

Many devices have been investigated for application to telegraph circuits for removing interference, and of those the preceding has been limited to descriptions of a small number sufficient to indicate their general range of usefulness. Neutralizing systems embodying a pilot wire through the exposure are effective for all classes of telegraph service, and are most useful where the exposure is short and where many wires can be corrected with one set of equipment. Systems dependent upon tuning are frequently unsuitable because of a greater tendency to cause direct detrimental effects to the telegraph currents, but find application in some circumstances where pilot-wire methods would be uneconomical.

But because of the cost, of the added complexity in telegraph circuits already highly complex, and of the weakening of the useful signaling currents in some cases, there are often objections to the use of such correcting equipment in exposures causing moderate or small amounts of power induction. In each instance there is some minimum value of induced voltage below which the application of neutralizing devices is not justifiable. To the telegraph company the most serious type of exposure is that which creates small interference only—a conclusion which follows from the fact that such exposures are far more numerous than the more severe ones, and their damaging effect upon telegraph service is to some extent additive.

However, the application of neutralizing devices to the telegraph circuits is proper where conditions are such that the creation of an exposure causing material induced voltages is justifiable, and fortunately it is possible to state that through the use of different methods in different cases there have been few exposures which during normal conditions of the power system, are so severe individually as to effect any serious impairment to telegraph service.

Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Harmonic Theory of Noise in Induction Motors

WAYNE J. MORRILL
MEMBER AIEE

Synopsis: This paper presents a theory of noise generation particularly adapted to use with small motors. Since single-phase motors predominate in the small-motor field, a single-phase point of view has been employed.

The underlying philosophy of the paper is to reduce the theory to simplest terms and to concentrate attention upon the most probable sources of noise. While the results are shown as a large collection of numbers representing force waves the development steps of the theory are really simple and easily understood.

The resultant air-gap flux wave in an induction motor is the difference between the fundamental stator flux plus its harmonics and the fundamental rotor flux plus its harmonics. The difference between the two fundamental fluxes is usually less than the larger one and often less than either one but the harmonics, with few exceptions, are neither of the same velocity nor space order and, therefore, do not add or subtract from each other. It follows that the resultant flux wave has more harmonics of much greater per-unit amplitude than does either the stator or rotor flux wave alone. It is the interaction of two large harmonic flux waves of slightly different space order and traveling at high frequency with respect to each other that causes high-frequency magnetic-force waves of long space pitch which are effective in producing noise.

The theory has been used for some time in noise investigations and has been found to bear out test results on a large number of motors. It is hoped that its presentation will not only prove useful to others but will encourage further contributions to the understanding of noise.

THE present-day problems which confront the designers of electric motors have been to a considerable extent complicated by the necessity for producing motors of greater and greater quietness. Since the pressure of economics precludes the possibility of obtaining quietness by the introduction of massive structures, ways must be found to obtain desired levels of quietness with the same or lighter structures than those previously used.

There have been a number of papers on noise of electrical machinery which have undertaken to deal with the problem of producing quiet motors from a scientific designer point of view. Many of these papers have been unsatisfactory to those engineers who desire always to have a sound and rigorous theory against which to check data, form conclusions, and make predictions. Much of the work which has been presented on noise has been of an experimental nature and has been presented at least semiempirically. Other work has been presented which, experience indicates, appears to be based upon unnecessarily complicated conceptions.

It appears that a sound theory having a simple philosophy and starting from a few simple fundamental assumptions would at this time contribute greatly toward progress in the art of noise reduction. It is the purpose of this paper to present such a theory and to suggest how it may be used.

Generation of Air-Borne Noise By a Motor

It can be shown that the air-borne noise arising from an electric motor has as its principal source the vibration of the stator frame and yoke structure. The natural and correct assumption is that the magnetic forces set up in the air gap of the motor cause the frame structure to be distorted periodically with time and that the external surface of the frame in turn beats the air causing air-borne noise.

From the above conception of the mechanism of noise production it follows that a complete exposition of motor noise phenomena would include not only a method for determining the character, magnitude, and frequency of the disturbing forces set up in the air gap but also a means for determining the response of the stator structure to the disturbing forces.

Unfortunately, experience with tests on many motors indicates that there is sufficient variation in the response characteristics of apparently identical stator structures (assembled in complete motor)

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Chart I. List of Largest Noise Force Waves

Stator Harmonic	Rotor Harmonic	Noise Frequency	Length of Magnetic Force Pole
N_f or M_f	R_f	$f[R(1-s)-1-s]$	$\frac{\lambda}{R-N}$ or $\frac{\lambda}{R-M}$
N_f or M_f	R_b	$f[R(1-s)+1-s]$	$\frac{\lambda}{R-N}$ or $\frac{\lambda}{R-M}$
N_f or M_f	T_f	$f[T(1-s)-1+s]$	$\frac{\lambda}{T-N}$ or $\frac{\lambda}{T-M}$
N_f or M_f	T_b	$f[T(1-s)-3+s]$	$\frac{\lambda}{T-N}$ or $\frac{\lambda}{T-M}$
N_b or M_b	R_f	$f[R(1-s)+1-s]$	$\frac{\lambda}{R-N}$ or $\frac{\lambda}{R-M}$
N_b or M_b	R_b	$f[R(1-s)+3-s]$	$\frac{\lambda}{R-N}$ or $\frac{\lambda}{R-M}$
N_b or M_b	T_f	$f[T(1-s)+1+s]$	$\frac{\lambda}{T-N}$ or $\frac{\lambda}{T-M}$
N_b or M_b	T_b	$f[T(1-s)-1+s]$	$\frac{\lambda}{T-N}$ or $\frac{\lambda}{T-M}$

to leave little hope that in the near future a reasonably exact expression for stator response can be presented. Calculations of stator response do have their place in a noise investigation but the results obtained thus far have been more qualitative than quantitative.

Because of the, as yet, unsatisfying results which have come from efforts to predetermine frame response characteristics no effort is being made in this paper to discuss this point. It is felt better to confine our attention to the subject of the magnetic forces causing noise and leave for the future the subject of the accurate calculation of the stator response. By presenting now that which is known for certain, effort on the part of others may be stimulated which will result in further advances in the art.

The theory will be based upon the assumption of an air gap having uniform length, constant permeance, and windings which are distributed in the usual way in finite numbers of slots. Such assumptions can be well justified in small electrical machinery where substantially closed slots are employed and where the problem of noise reaches its most acute form. In larger motors having open slots it is felt that many of the conclusions obtained by this theory will still be valuable because accompanying the use of open slots is the use of greatly increased air gaps which to a considerable extent minimize such phenomena as may arise from effects due exclusively to permeance variations. Another reason why the conclusions arrived at from this magnetomotive-force constant-permeance theory may be useful where the permeance is not really constant lies in the fact that analysis has shown that most of the phenomena arising from tooth and slot permeance variations are identical in every way with associated phenomena arising from harmonics of magnetomotive-force caused by windings placed in the same slot structure.

Development of Theory

It is well known that, in addition to the fundamental air-gap fluxes produced by the currents in the rotor and stator windings, harmonic fluxes are also produced. With symmetrical windings harmonic fluxes, with certain exceptions, may exist having a number of poles corresponding to any odd multiple of the fundamental number of poles. "Good" winding distributions generally limit the magnitude of harmonics of low order but with a given number of uniformly spaced slots nothing can be done about the magnitudes of the tooth harmonics because the "distribution factor" for the tooth harmonics is identical with that for the fundamental flux. With any given slot combination and fundamental number of poles the magnitudes and orders of the tooth harmonics are definitely determined in relation to the fundamental flux. There always exist an infinite series of tooth harmonics having magnitudes and orders as shown below.

$$\text{Order of harmonic} = \frac{\text{harmonic poles}}{\text{fundamental poles}} = 2Kn \pm 1 \quad (1)$$

$$\text{Amplitude of harmonic wave} = A_1 \frac{1}{2Kn \pm 1} \quad (2)$$

In the above equations

P_1 = fundamental number of poles
A_1 = amplitude of fundamental flux wave
n = number of teeth per pole
K = any integer

It will be noticed that the tooth harmonics go in pairs and that if there are nine teeth per pole in a 4-pole motor the largest pair of tooth harmonics are the 17th and 19th harmonics. These harmonics will have respectively 17 and 19 times as many poles as the fundamental flux and their amplitudes will be $^1/_{17}$ and $^1/_{19}$ that of the fundamental. The next largest harmonics will be the 35th and 37th and after these the 53d and 55th, etc.

There may be times when the second or third largest tooth harmonics will be important in producing noise but for the present only the largest tooth harmonics will be considered. The method outlined can be employed for any number of harmonics.

For the sake of clarity and to give a mental picture of the relative magnitudes involved, figure 1 has been prepared showing (dotted) the stair-step wave of flux produced by a four-pole winding in a 36-slot stator. Superposed is shown (solid) the fundamental component and the first pair of tooth harmonics corresponding to the stair-step wave. Both the harmonics as well as the fundamental wave of flux should be visualized as pulsating with time and, if it be assumed that figure 1 was drawn when the waves were maximum, the effect of the pulsation is exactly duplicated by dividing each pulsating wave into two oppositely gliding components each having half the amplitude shown.

If the forward and backward components be used there are now six stator flux waves to be considered. If the subscript "1" be always associated with the stator and the subscripts "N" and "M" are each associated with a member of and represent the order of the first pair of tooth harmonics, the forward and backward components of each wave may be expressed as shown below.

$$\left.\begin{aligned}
W_{1f} &= A_{1f} \cos\left[x\frac{\pi}{\lambda} - \omega t\right] \\
W_{1b} &= A_{1b} \cos\left[x\frac{\pi}{\lambda} + \omega t\right] \\
W_{1Nf} &= A_{Nf} \cos\left[Nx\frac{\pi}{\lambda} - \omega t\right] \\
W_{1Nb} &= A_{Nb} \cos\left[Nx\frac{\pi}{\lambda} + \omega t\right] \\
W_{1Mf} &= A_{Mf} \cos\left[Mx\frac{\pi}{\lambda} - \omega t\right] \\
W_{1Mb} &= A_{Mb} \cos\left[Mx\frac{\pi}{\lambda} + \omega t\right]
\end{aligned}\right\} \quad (3)$$

The forward component of fundamental stator flux acts on the rotor winding to produce a forward wave of rotor currents which in turn produces a forward wave of rotor fundamental flux moving at slip frequency with respect to the rotor. Associated with this forward fundamental

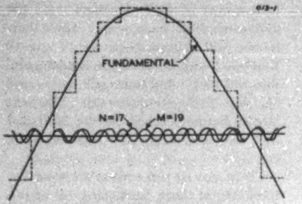


Figure 1. Fundamental and first tooth harmonics

flux is an infinite series of rotor tooth harmonic fluxes. Unlike the stator fluxes, the rotor fluxes, arising as they do from the action of a gliding wave of stator flux having constant magnitude, are not pulsating. They glide forward and backward on the rotor (forward with respect to the stator) with constant magnitude and at slip frequency with respect to the rotor.

The rotor tooth harmonics have poles and amplitudes as shown below:

$$\text{Order of rotor harmonic} = \frac{\text{harmonic poles}}{\text{fundamental poles}} = \pm\frac{2KB}{P_1}+1 \quad (4)$$

$$\text{Amplitude of rotor harmonics} = A_{2f}\left(\frac{1}{\pm\frac{2KB}{P_1}+1}\right) \quad (5)$$

In the above equations:

P_1 = fundamental number of poles
A_{2f} = amplitude of forward fundamental rotor wave
B = total number of rotor bars
K = any integer

When $K=0$ in equation 4, the first harmonic (the fundamental flux) is obtained and the positive sign indicates that the direction of motion of the fundamental flux wave with respect to the rotor is considered positive. A negative sign such as always occurs with the lesser-poled harmonic of each pair of tooth harmonics indicates that that harmonic is moving around the rotor in a direction opposite the motion of the fundamental flux. Throughout this discussion R indicates the negatively moving rotor tooth harmonic and the ratio of its poles to the poles of the fundamental. T will indicate the positively moving rotor harmonic and the ratio of its poles to those of the fundamental.

The backward component of fundamental stator flux is also responsible for producing a backward component of fundamental rotor flux gliding backward on the rotor at a frequency $(2-s)$ times the stator frequency. Corresponding tooth harmonics are also produced.

If only the fundamental and first pair of tooth harmonics be considered at this time there are in all six rotor fluxes. Neglecting the difference in time phase between stator and rotor currents, these six fluxes may be expressed with respect to the rotor as shown.

$$\left.\begin{aligned}
W_{2f} &= A_{2f}\cos\left[x_2\frac{\pi}{\lambda}-\omega st\right]\\
W_{2b} &= A_{2b}\cos\left[x_2\frac{\pi}{\lambda}+\omega(2-s)t\right]\\
W_{2Rf} &= A_{Rf}\cos\left[Rx_2\frac{\pi}{\lambda}+\omega st\right]\\
W_{2Tf} &= A_{Tf}\cos\left[Tx_2\frac{\pi}{\lambda}-\omega st\right]\\
W_{2Rb} &= A_{Rb}\cos\left[Rx_2\frac{\pi}{\lambda}-\omega(2-s)t\right]\\
W_{2Tb} &= A_{Tb}\cos\left[Tx_2\frac{\pi}{\lambda}+\omega(2-s)t\right]
\end{aligned}\right\}\quad(6)$$

With respect to the stator the rotor waves of (6) can be written:

$$\left.\begin{aligned}
W_{2f} &= A_{2f}\cos\left[x\frac{\pi}{\lambda}-\omega t\right]\\
W_{2b} &= A_{2b}\cos\left[x\frac{\pi}{\lambda}+\omega t\right]\\
W_{2Rf} &= A_{Rf}\cos\left[Rx\frac{\pi}{\lambda}-\omega[R(1-s)-s]t\right]\\
W_{2Tf} &= A_{Tf}\cos\left[Tx\frac{\pi}{\lambda}-\omega[T(1-s)+s]t\right]\\
W_{2Rb} &= A_{Rb}\cos\left[Rx\frac{\pi}{\lambda}-\omega[R(1-s)+2-s]t\right]\\
W_{2Tb} &= A_{Tb}\cos\left[Tx\frac{\pi}{\lambda}-\omega[T(1-s)-2+s]t\right]
\end{aligned}\right\}\quad(7)$$

The flux at every point in the air gap of the motor and at every instant of time is exactly representable as the sum of the fundamental fluxes and all the harmonic fluxes. Using those which have been considered we may write:

$$B_\delta = \left[\begin{aligned}&W_{1f}+W_{1b}+W_{1Nf}+W_{1Nb}+W_{1Mf}+\\&W_{1Mb}+W_{2f}+W_{2b}+W_{2Rf}+W_{2Rb}+\end{aligned}\right] W_{2Tf}+W_{2Tb} \quad (8)$$

The radial force density at any point in the gap due to B_δ is, according to Maxwell:

$$\frac{F}{A}=\frac{1}{72{,}130{,}000}B_\delta^2 \text{ pounds per square inch} \quad (9)$$

For purposes of noise study it is desirable to work with the components of the force density, which components are obtainable by the expansion of the square of the right member of (8). The expansion consists of the square of each term in (8) plus twice the product of each term with each other term. A complete expansion of even so few as the 12 terms chosen leads to 78 terms which are rather clumsy to handle by conventional means. In the appendix is given a simple shorthand means for performing the expansion and there has been prepared a multiplication table showing results for the case of a four-pole 36–48 combination motor.

At this time it is of interest to perform one of the multiplications in order to discuss the results obtained. In the case of the N forward stator tooth harmonic and the R forward rotor harmonic the product is:

$$\Delta F_{(Nf)(Rf)} = A_{Nf}A_{Rf}\cos\left[Nx\frac{\pi}{\lambda}-\omega t\right]\times \cos\left[Rx\frac{\pi}{\lambda}-\omega[R(1-s)-s]t\right] \quad (10)$$

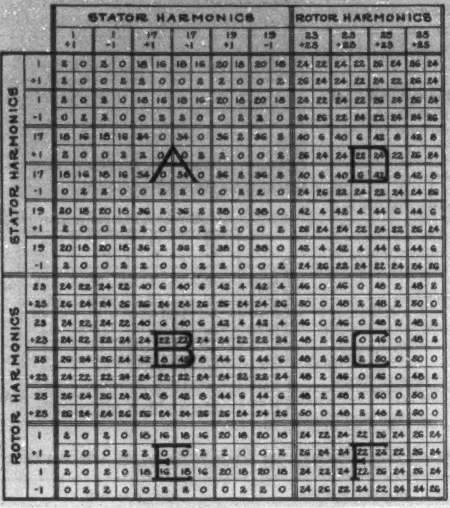
$$\Delta F_{(Nf)(Rf)} = A_{Nf}A_{Rf}\times \left\{\cos\left[(R-N)x\frac{\pi}{\lambda}-\omega[R(1-s)-1-s]t\right]+ \cos\left[(R+N)x\frac{\pi}{\lambda}-\omega[R(1-s)+1-s]t\right]\right\} \quad (11)$$

It will be seen from (11) that the product of the two flux waves is representable as two force waves, the upper wave having a long wave length and the lower one having a very short wave length.

Chart II. No-Load Noise Forces for Four-Pole 36–48 Motor

Stator Harmonic	Rotor Harmonic	Frequency (Cycles)	Amplitude (Pounds Per Square Inch)	Length of Magnetic Force Pole (Inches)
17_f	23_b	1,440	0.032	0.46
19_f	23_b	1,440	0.0286	0.685
17_b	23_b	1,560	0.032	0.46
19_b	23_b	1,560	0.0286	0.685
17_f	25_b	1,320	0.0294	0.46
19_f	25_b	1,320	0.0264	0.685
17_b	25_b	1,440	0.0294	0.46
19_b	25_b	1,440	0.0264	0.685

Chart III. Magnetic Force Waves for 36–48 Combination



		STATOR HARMONICS												ROTOR HARMONICS							
		1 +1		1 -1		17 +1		17 -1		19 +1		19 -1		23 +25		23 +23		25 +23		25 +25	
STATOR HARMONICS	1	2	0	2	0	18	16	18	16	20	18	20	18	24	22	24	22	26	24	26	24
	+1	2	0	0	2	2	0	0	2	2	0	0	2	26	24	24	22	24	22	26	24
	1	2	0	2	0	18	16	18	16	20	18	20	18	24	22	24	22	26	24	26	24
	-1	0	2	2	0	0	2	2	0	0	2	2	0	24	26	22	24	22	24	24	26
	17	18	16	18	16	34	0	34	0	36	2	36	2	40	6	40	6	42	8	42	8
	+1	2	0	0	2	2	0	0	2	2	0	0	2	26	24	24	22	24	22	26	24
	17	18	16	18	16	34	0	34	0	36	2	36	2	40	6	40	6	42	8	42	8
	-1	0	2	2	0	0	2	2	0	0	2	2	0	24	26	22	24	22	24	24	26
	19	20	18	20	18	36	2	36	2	38	0	38	0	42	4	42	4	44	6	44	6
	+1	2	0	0	2	2	0	0	2	2	0	0	2	26	24	24	22	24	22	26	24
	19	20	18	20	18	36	2	36	2	38	0	38	0	42	4	42	4	44	6	44	6
	-1	2	0	0	2	2	0	0	2	2	0	0	2	24	26	22	24	22	24	24	26
ROTOR HARMONICS	23	24	22	24	22	40	6	40	6	42	4	42	4	46	0	46	0	48	2	48	2
	+25	26	24	24	26	26	24	24	26	26	24	24	26	50	0	48	2	48	2	50	0
	23	24	22	24	22	40	6	40	6	42	4	42	4	46	0	46	0	48	2	48	2
	+23	24	22	22	24	24	22	22	24	24	22	22	24	48	2	46	0	46	0	48	2
	25	26	24	26	24	42	8	42	8	44	6	44	6	48	2	48	2	50	0	50	0
	+23	24	22	22	24	24	22	22	24	24	22	22	24	48	2	46	0	46	0	48	2
	25	26	24	26	24	42	8	42	8	44	6	44	6	48	2	48	2	50	0	50	0
	+25	26	24	24	26	26	24	24	26	26	24	24	26	50	0	48	2	48	2	50	0
	1	2	0	2	0	18	16	18	16	20	18	20	18	24	22	24	22	26	24	26	24
	+1	2	0	0	2	2	0	0	2	2	0	0	2	26	24	24	22	24	22	26	24
	1	2	0	2	0	18	16	18	16	20	18	20	18	24	22	24	22	26	24	26	24
	-1	0	2	2	0	0	2	2	0	0	2	2	0	24	26	22	24	22	24	24	26

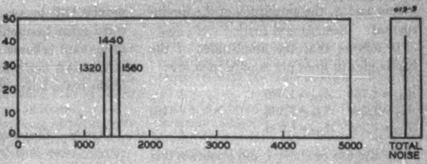


Figure 3. Noise spectrum taken on a four-pole (36–48) motor

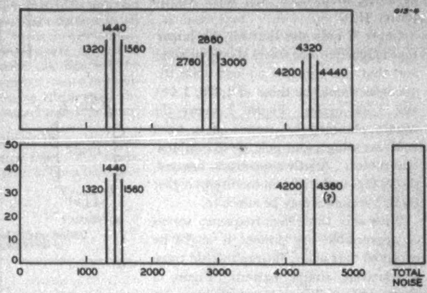


Figure 4

Top—Calculated noise-force spectrum on a four-pole (36–48) motor
Bottom—Noise spectrum taken on a four-pole (36–48) motor

In general, very short force waves will not be important because the stator will be very stiff to short-wave distortion. Long waves of force, however, will cause a relatively large response and therefore will be important in producing noise. The pitch of the long force wave is:

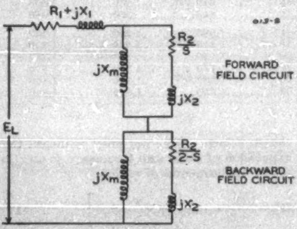


Figure 2. Equivalent circuit for a single-phase motor

$$\lambda_{(Nf)(Rf)} = \frac{\lambda}{R-N} \tag{12}$$

The frequency of this wave is:

$$f_{(Nf)(Rf)} = f[R(1-s)-1-s] \tag{13}$$

and this frequency is, of course, the frequency at which the air-borne noise will be emitted.

A study of the various products involved shows that those products most likely to be responsible for noise are the ones of long pitch produced by the multiplication of a stator harmonic with a rotor harmonic. There are 16 such products and they will be listed here for future reference (chart I). In appendix I a complete listing of the product terms is given but the simplifying assumption has been made that $s=0$. It may at times be desirable to have available the expression for noise frequency when $s \neq 0$.

It will be noted that the frequency does not change when the N_f stator harmonic is substituted for the M_f but the force wave length does.

Sample Force-Wave Calculation

A specific calculation will now be introduced to show how the force waves may be calculated for a particular motor.

Assumptions

Gap diameter	=3.5 inches
Stacking length	=2 inches
Stator slots	=36
Rotor slots	=48
Fundamental poles	=4
Maximum fundamental density	=30,000 lines per square inch
Maximum load current	=2 (no load current)

The order of the largest stator tooth harmonics may be calculated by means of equation 1.

$$\left.\begin{aligned} N &= 17 \\ M &= 19 \end{aligned}\right\} \tag{14}$$

The order of the largest rotor tooth harmonics may be calculated by means of (4).

$$\left.\begin{aligned} R &= 23 \\ T &= 25 \end{aligned}\right\} \tag{15}$$

At no load the forward field rotor currents are negligible (see figure 2) and the forward-field rotor tooth harmonics listed on chart I will be ineffective in producing noise. In the backward field, while the rotor and stator fundamental fluxes neutralize each other, as shown by the low voltage which exists across the backward field at no load, it will be assumed that because of poor distribution factor and spiral the harmonics exist practically undiminished.* On the basis of this assumption the stator harmonic fluxes and the backward-field rotor harmonic fluxes are subject to the same ampere turns as is the fundamental flux; consequently they will exist in their normal magnitudes (A_1/h) where h is the order of the harmonic and A_1 the amplitude of the fundamental. [See (2) and (5).]

---

* Note: If desired the rotor secondary characteristics to each stator harmonic can be carefully calculated and an exact determination of the diminution of the stator harmonic fluxes determined. The stator winding will have almost no diminishing effect on rotor harmonics.

It follows that the amplitudes of the harmonics in lines per square inch are:

$$\left.\begin{array}{ll} A_{17f}=1{,}765 & A_{19b}=1{,}580 \\ A_{19f}=1{,}580 & A_{23b}=1{,}305 \\ A_{17b}=1{,}765 & A_{25b}=1{,}200 \end{array}\right\} \quad (16)$$

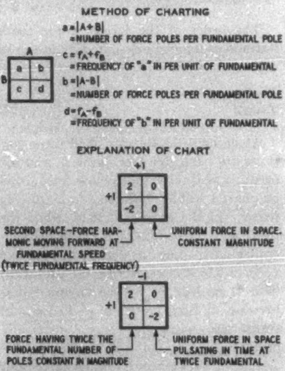
By the use of chart I and equation 9 it is now possible to list maximum force densities, frequencies, and wave lengths (chart II).

Chart II indicates that all the important magnetic-force waves should be small and that the principal no-load noise frequencies should be those of 1,320, 1,440, and 1,560 cycles. Figure 3 shows the results obtained by means of a noise analyzer using a four-pole 36–48 combination motor. Another spectrum, figure 4, shows that under certain conditions higher tooth harmonics may be effective.

With only three high-frequency waves of appreciable size present it might be expected that an oscillograph would trace a relatively simple pattern. Figure 5 shows two oscillographs which bear out the idea of pattern simplicity. These were taken using a velocity pickup and a high amplification to the oscillograph.

Figure 6 gives noise spectrums taken on four-pole motors of other slot combinations. These may be compared with chart VII.

Chart IV


METHOD OF CHARTING
A
B
a
b
c
d
a=|A+B|
=NUMBER OF FORCE POLES PER FUNDAMENTAL POLE
c=f_A+f_B
=FREQUENCY OF "a" IN PER UNIT OF FUNDAMENTAL
b=|A-B|
=NUMBER OF FORCE POLES PER FUNDAMENTAL POLE
d=f_A-f_B
=FREQUENCY OF "b" IN PER UNIT OF FUNDAMENTAL
EXPLANATION OF CHART
+1
+1
2
0
-2
0
SECOND SPACE-FORCE HARMONIC MOVING FORWARD AT FUNDAMENTAL SPEED (TWICE FUNDAMENTAL FREQUENCY)
UNIFORM FORCE IN SPACE. CONSTANT MAGNITUDE
-1
+1
2
0
0
-2
FORCE HAVING TWICE THE FUNDAMENTAL NUMBER OF POLES CONSTANT IN MAGNITUDE
UNIFORM FORCE IN SPACE PULSATING IN TIME AT TWICE FUNDAMENTAL

The force waves under load will be larger since, as indicated under "assumptions", the maximum load current is twice the no-load current, all the stator harmonics and backward rotor harmonics will be twice as large. The forward rotor harmonics which do not exist at no load become almost as large as the others and not only are the magnitudes of the noise forces increased but the character of it is changed. (See the discussion of no-load versus load noise in appendix I.)

Chart V. Graphical Explanation of Chart
Two-Pole Motor Assumed

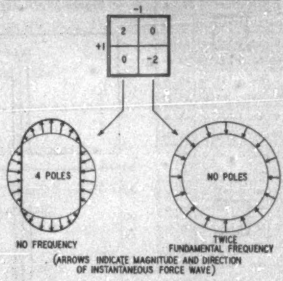

-1
+1
2
0
0
-2
4 POLES
NO POLES
NO FREQUENCY
TWICE FUNDAMENTAL FREQUENCY
(ARROWS INDICATE MAGNITUDE AND DIRECTION OF INSTANTANEOUS FORCE WAVE)

Chart VII. Analysis of Major High-Frequency Force Waves With a 32- and a 36-Slot Stator

Stator	Rotor	Magnetic-Force Poles			Noise Frequency		
32	33	2	6	10	870	990	1,110
	34	4	12		900	1,020	1,140
	35	2	6	14	930	1,050	1,170
	36	0	8	16	960	1,080	1,200
	37	2	10	18	990	1,110	1,230
	38	4	12	20	1,020	1,140	1,260
	39	6	14	22	1,050	1,170	1,290
	40	8	16	24	1,080	1,200	1,320
36	28	8	16	24	720	840	960
	29	6	14	22	750	870	990
	30	4	12	20	780	900	1,020
	31	2	10	18	810	930	1,050
	32	0	8	16	840	960	1,080
	40	0	8	16	1,080	1,200	1,320
	41	2	10	18	1,110	1,230	1,350
	42	4	12	20	1,140	1,260	1,380
	43	6	14	22	1,170	1,290	1,410
	44	8	16	24	1,200	1,320	1,440
	48	16	24	32	1,320	1,440	1,560

Appendix I

In the calculation of magnetic force waves in the course of actual design work it is desirable to have a systematic method of work. This section of this paper describes such a method.

If time-phase relations are not important the equation for a magnetic flux wave as given in equations 3 and 7 expresses for us the following:

Amplitude
Order (pitch, distribution)
Frequency
Direction

If we deal with tooth harmonics the relation expressed in (2) and (5) is valuable in connecting amplitude with pitch so that if the pitch is known the amplitude is known. In such event only the pitch need be defined. If frequency be given a sign the symbol for frequency may be used to express both frequency and direction.

Chart VI. High-Frequency Noise Terms, 36–48 Slot Combination

ROTOR TOOTH HARMONICS	STATOR TOOTH HARMONICS 17 +1	17 -1	19 +1	19 -1	
23	6	6	4	4	a
+25	24	26	24	26	
23	6	6	4	4	b
+23	22	24	22	24	
25	8	8	6	6	c
+23	22	24	22	24	
25	8	8	6	6	d
+25	24	26	24	26	

If we make these simplifications in our present case we may express any traveling wave by two numbers.* It is convenient to make these numbers "per unit" numbers as shown below. (By "per unit" is meant the expression of order and frequency as a ratio to the fundamental of each.)

$$W_a=\boxed{\frac{18}{-2}}=\cos\left[18\times\frac{\pi}{\lambda}+2\omega t\right] \qquad (17)$$

If the upper number in the box be the order of the flux harmonic (the number of poles of the wave per fundamental pole) and the lower number the ratio of the harmonic frequency to the fundamental frequency, the wave W_a is seen to have 18 times the fundamental number of poles and to be moving backward (because of the minus sign) at twice fundamental frequency.

The conventional expression for the wave is also given in (17) and it can be seen that the numerical coefficients of the conventional expression are used in the new nomen-

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*NOTE: It is obvious that, if desired, a four-number system could be similarly set up representing phase relation, order, magnitude, and frequency-direction. For our present purpose the simplification is desirable.

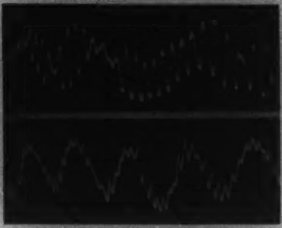


Figure 5. Cathode-ray oscillograms of stator-yoke vibration, 36–48 combination

clature except that the sign of the frequency term is reversed.

If it is desired to multiply two waves using the convention it is merely necessary to add and substract numbers of the same significance. For example:

$$W_b=\begin{vmatrix}\frac{14}{4}\end{vmatrix}=\cos\left[14\times\frac{\pi}{\lambda}-4\omega t\right] \qquad (18)$$

$$W_a\times W_b=\left[\begin{array}{l}\frac{1}{2}\cos\left[32\times\frac{\pi}{\lambda}-2\omega t\right]+\\ \qquad\frac{1}{2}\cos\left[4\times\frac{\pi}{\lambda}+6\omega t\right]\end{array}\right] \qquad (19)$$

$$\underset{b}{\begin{vmatrix}\frac{14}{4}\end{vmatrix}}\times\underset{a}{\begin{vmatrix}\frac{18}{-2}\end{vmatrix}}=\underset{c}{\begin{vmatrix}\frac{32}{2}\end{vmatrix}}+\underset{d}{\begin{vmatrix}\frac{4}{-6}\end{vmatrix}}=\underset{e}{\left|\begin{array}{c|c}32&4\\\hline 2&-6\end{array}\right|} \qquad (20)$$

Chart VIII. Rotor Subharmonics

STATOR SLOTS	ROTOR SLOTS	SUBHARMONIC POLES SUBHARMONIC FREQ.			
32	33	6	6	2	2
		+15.5	+17.5	−15.5	−17.5
	34	8	8	0	0
		+16	+18	+16	+18
	35	10	10	2	2
		+16.5	+18.5	+16.5	+18.5
	36	12	12	4	4
		+17	+19	+17	+19
	37	14	14	6	6
		+17.5	+19.5	+17.5	+19.5
	38	16	16	8	8
		+18	+20	+18	+20
	39	18	18	10	10
		+18.5	+20.5	+18.5	+20.5
	40	20	20	12	12
		+19	+21	+19	+21
36	40	12	12	4	4
		+19	+21	+19	+21
	44	20	20	12	12
		+21	+23	+21	+23
	48	28	28	20	20
		+23	+25	+23	+25

In equation 20 a horizontal addition is made to obtain *c* from *a* and *b*. A horizontal subtraction is made (in the direction to make the upper figure positive) to obtain *d*. The two waves are written together as at *e*.

If the two waves W_a and W_b are flux waves their product according to previous discussion is a pair of moving waves of force tending to bend the stator yoke into as many nodes as there are waves of force.

The present resultant force waves are seen to have 32 poles and 4 poles and to be moving respectively at twice fundamental frequency forward and at six times fundamental frequency backward. It will be noted that the numerical coefficient $^1/_2$ is dropped for simplicity in view of its existence in all similar product terms. A further simplification will be made for our present purpose in that after multiplication the sign will be dropped from the frequency terms since direction of rotation has no effect upon airborne noise in which we are primarily interested.

It was shown previously that a complete expression for the force waves of a motor would multiply each term of both the stator and rotor Fourier series flux expression by itself and by each other term to form a second-order series of force waves. A multiplication form has been prepared and the operations performed for a 36–48-slot four-pole combination assuming no-load operation (chart III).

Inspection of the chart will show that when stator waves act together only "0" and "2" frequencies (0 and 120 cycles) exist. It is also apparent that when rotor waves act together high-frequency terms are produced (about 3,000 cycles) but the pitches of the waves are so short that it is extremely doubtful whether the resultant stator distortion could be detected.

All of the waves found in "*E*" and "*F*" will be found in "*A*" and "*B*" respectively and "*D*" will be found to be a replica of "*B*". Attention may therefore be concentrated upon section "*B*" of the chart when high-pitch noise is of interest.

If waves of extremely short pitch be discarded as being relatively ineffective we find ourselves with the same waves given in chart VI.

It is of interest to note that the four terms laterally adjacent to "*b*" and "*d*" result from forward-field rotor current and are, therefore, absent at no load and large under load. Terms adjacent to "*a*" and "*c*," however, are the result of backward-field rotor current and are large at no load as well as under load. The results of the analysis are statistically tabulated below in relation to the number of poles of force and the frequencies produced.

16 Poles	24 Poles	32 Poles
No-load noise forces (cycles)		
1,440	1,320	1,320
1,560	1,440	1,440
	1,440	
	1,560	
Additional-load noise forces (cycles)		
1,320	1,320	1,440
1,440	1,440	1,560
	1,440	
	1,560	

If motor noise is measured at no load it would be expected that the 1,440-cycle and 1,560-cycle noises would predominate since the stator is much stiffer to 24-node and 32-node bending than to 16-node bending. Under load the 1,320-cycle noise should become pronounced, though, due to slip, it will be somewhat displaced in frequency.

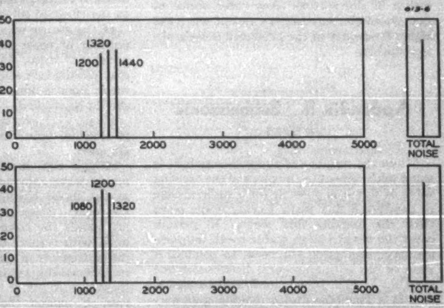
All no-load noises should be reduced practically to zero by polyphase operation or the use of a proper capacitor. The introduction of a balancing capacitor (effective only under load) should be accompanied not only by a reduction in noise level but by the elimination of the highest of the three tooth frequencies, causing a change of about 120 cycles in the mean pitch of the high-frequency noise. Two-phase operation should show this clearly.

Chart VII has been prepared giving principal magnetic-force waves for all the possible slot combinations for a satisfactory re-

Figure 6

Top—Noise spectrum taken on a four-pole (36–44) motor

Bottom—Noise spectrum taken on a four-pole (32–40) motor



frigeration-machine motor assuming both 32- and 36-slot stators.

It is of special interest to notice that whenever force waves of zero poles occur there exist "geared" waves of the same number of poles on the rotor and stator. The existence of such waves is accompanied by standstill locking and high-speed rotating subharmonic waves of fundamental distribution and high-frequency tooth-harmonic interference. (Discussed briefly later.)

A two-pole magnetic force tries to displace the entire rotor thus producing shaft vibration, a four-pole force tries to distort the stator yoke into the form of an ellipse, etc. It is to be assumed that, other things being equal, a magnetic force of many poles is to be preferred to one of few poles; because of the greater effective yoke stiffness when the wave length is short. Any force wave having less than eight poles is likely to be bad for noise.

Conclusions

It must be concluded that when a 32-slot stator is used the rotor is limited to 36 slots or 40 slots or more. If 36 slots are used it is questionable whether the locking and high-frequency interference (torque) noises can be kept from becoming a nuisance; spiral might control them. Above 40 slots there seems little reason to confine the slot combination to even slots if advantage is to be obtained by going to an odd number.

With a 36-slot stator the rotor possibilities are 28, 32, 40, 44, and higher. The remarks concerning the 36-slot rotor with a 32-slot stator apply to the 32- and 40-slot rotors with a 36-slot stator. Any rotor above 44 slots should be satisfactory except when the higher frequency of the produced noise is objectionable.

Appendix II. Subharmonic Waves

When the number of squirrel-cage bars is small with respect to the pitch of the exciting wave of flux it is possible for a squirrel cage to produce a flux wave having fewer poles than the exciting flux wave. In certain cases the always large stator-tooth harmonics may react upon the rotor to produce a wave of flux of fundamental distribution but rotating at such speeds as 70,000 to 80,000 rpm in a four-pole motor operating at normal 1,725 rpm speed. Such subharmonic waves become very important in the noise of a motor and must be taken into account. The procedure after determining the flux waves present is exactly as already described except that the subharmonic waves are included in the calculation along with the fundamental and tooth-harmonic waves.

Determination of Subharmonic Waves

Subharmonic waves may be determined as Chapman outlined in his book on induction motors by use of the equations:

$$S=\frac{2BK}{P_1}+h=\text{order and direction of subharmonic (direction referred to direction of } h \text{ with respect to rotor)} \quad (21)$$

$$f_s=\pm 1-\frac{2BK}{P_1}=\text{stator referred frequency in per-unit of fundamental frequency (single-phase stator)} \quad (22)$$

In the above equations:

B = total number of rotor bars
K = any positive or negative integer ($K=-1$ usually most important)
P_1 = number of fundamental poles
h = order of a stator harmonic (use plus sign always)

The magnitude of a subharmonic depends upon the rotor impedance as well as the linkage of the rotor and the stator in so far as the generating stator harmonic is concerned. If the stator harmonic is of short pitch, as are most tooth harmonics, a slight amount of spiral greatly reduces the mutual and reduces the subharmonic. Even without spiral the amplitude will usually be found to be less than $A_1(1/h)^2$. Since the calculation of subharmonic magnitudes is somewhat involved it is felt that an outline of the method is not warranted here. The intent of these remarks is merely to call attention to the effect of subharmonics.

If the stator winding is "good" and the number of rotor slots is greater than the number of stator slots by $2P$, subharmonics may usually be neglected. If the number of rotor slots is less than this subharmonics should be studied.

Chart VIII lists subharmonics for 32- and 36-slot four-pole stators when various rotors are used.

A plus sign in (21) indicates a subharmonic wave moving in the same direction as the stator harmonic, h, with respect to the rotor. Since at normal forward operating speeds the rotor is always moving forward with respect to any stator harmonic, the movement of the stator harmonic with respect to the rotor is backward. Under these conditions, if the sign of (21) is negative, it means that the subharmonic is moving in the opposite or forward direction with respect to the rotor and, of course, forward also with respect to the stator. Since a plus sign for a frequency number in chart VIII indicates a wave moving forward with respect to the stator, chart VIII would therefore have a positive frequency number when (21) is negative.

Nomenclature

A_1 = amplitude of fundamental stator flux wave
A_{1f} = amplitude of fundamental rotor forward wave
A_{NF} = amplitude of the N forward tooth harmonic
B = total number of rotor bars
B_s = total density in the air gap at the point x
f = frequency of primary excitation
K = any integer
n = number of stator slots per pole
N = largest stator-tooth harmonic
M = second largest stator-tooth harmonic
P_1 = fundamental number of poles
R = largest (least poled) rotor-tooth harmonic
s = per unit slip
T = second largest rotor-tooth harmonic
x = distance in inches measured peripherally around the air-gap surface
ΔF = force density (pounds per square inch)
λ = fundamental pole pitch (inches)

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Current Transformers and Relays for High-Speed Differential Protection, With Particular Reference to Offset Transient Currents

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Synopsis: Up to the present time, the only data generally available to protection engineers for determining the performance of current transformers have been the ratio and phase-angle curves of the current transformers in question. More detailed analysis of important high-speed differential protection problems has shown that these data are hopelessly inadequate for the purpose of determining the performance of current transformers during the starting period when the current wave may be fully offset. If the time constant for decay of the d-c component exceeds 0.05 second, large errors of transformation may be expected if standard current transformers are used. While it is possible to make special designs that will not saturate because of the d-c component in the current,[1] such designs are usually considerably oversize and expensive. Furthermore, space requirements and economy sometimes dictate that current transformers already installed be used if at all possible, even though it is known that their performance will be far from perfect. The problem then becomes one of determining the actual performance of existing current transformers, so that a suitable relay scheme may be chosen. It is the purpose of this paper to discuss the solution of this problem, and to present approximate methods of calculation not hitherto available. Attention is also given to the proper interpretation of the calculations with respect to relay operations, together with supporting test results.

THE PROBLEM of differential protection of generating-station busses is one which almost invariably requires calculation of current-transformer performance for asymmetrical currents, because of the long time constant of the generators. Figure 1 shows schematically a six-circuit bus protected by a simple overcurrent relay. The principle of differential protection indicates that, for an external fault at X, the current, i_1 through the relay should be zero. This is based upon the assumption that all the current transformers involved will perform perfectly. Inequalities are apparent at once, however, since it is seen that the transformer in circuit F_6 carries the total fault current, whereas the others each carry only a fraction of the total. The d-c component in the primary current, particularly troublesome when large generators are near the fault, aggravates the inequalities much more severely than is immediately obvious. For example, when conditions are severe, instantaneous peak values of i_1 may be of the order of 100 amperes when the symmetrical component of the total fault current is 100 amperes rms in terms of the secondary circuit.

The false differential current, i_1, which may exist when an external fault occurs at point X, is equal to the difference between the sum of the magnetizing currents of all the current transformers carrying current into the bus, and the magnetizing current of the current transformer carrying the total fault current away from the bus. (This can be derived from figure 1 by expressing each of the secondary currents in terms of their respective primary currents less the magnetizing currents of their respective current transformers, and solving for i_1, remembering that the sum of the currents entering the bus is zero for an external fault.) This forms the explanation for the inadequacy of ratio curves for this type of problem. It diverts the interest of the protection engineer at once from the question, "What will the secondary current be?" to the question, "What will the magnetizing current be?"

The authors have found that many protection engineers tend to underestimate the relative saturating quality of the d-c component of the asymmetrical current. Some have felt that, given a definite maximum instantaneous peak value for an asymmetrical current, it was only necessary to provide a current transformer whose performance would be satisfactory on symmetrical currents having the same peak values. This is definitely not true. For these reasons, the authors believe that the discussion immediately following is in order prior to the detailed mathematical treatment. This discussion is intended to convey a conception of the mechanics of d-c saturation in a form kept as graphical as possible.

The Mechanics of Saturation

A convenient way to analyze the performance of the current transformer of figure 2A is by means of the equivalent diagram as shown in figure 2B. With a given value of primary current, i, the values of primary-winding resistance and leakage reactance are of no consequence, hence these values are not indicated in the final diagram for analysis (figure 3). Also, for convenience, the secondary-winding resistance and leakage reactance are lumped with the burden and shown as R_2 and L_2. The mutual inductance between primary and secondary windings is shown by L_1, through which flows the magnetizing current, as indicated by i_1. It is possible to provide for the core loss current by including a suitable resistance in this branch, but this refinement is not necessary to provide sufficient accuracy for the problem at hand. This diagram is usable on the basis that the currents are expressed in terms of per cent of full-load current. Further, the diagram is usable

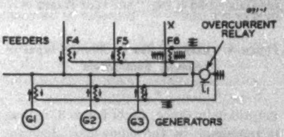

Figure 1. Simple overcurrent differential protection of a six-circuit bus

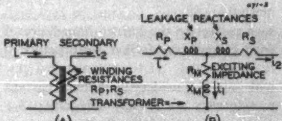

Figure 2. Diagram of current transformer and its equivalent circuit

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1. For all numbered references, see list at end of paper.

in that, electrically, it satisfies the requirement that the primary current minus the magnetizing current must equal the secondary current. The physical significance of the current, i_1, flowing through the mutual inductance, L_1, of figure 3, is that it represents in the equivalent diagram the vector difference between the primary and secondary currents in the actual transformer.

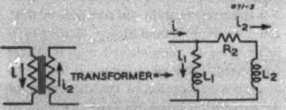
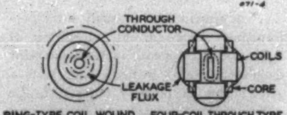
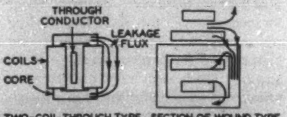


Figure 3. Simplified diagram of current transformer



RING-TYPE COIL WOUND ON TOROIDAL CORE. LEAKAGE FLUX DOES NOT ENTER THE CORE

FOUR-COIL THROUGH TYPE. SOME LEAKAGE FLUX ENTERS AND FLOWS IN PART OF THE CORE



TWO-COIL THROUGH TYPE. A GOOD DEAL OF LEAKAGE FLUX FLOWS IN THE ENDS OF THE CORE

SECTION OF WOUND TYPE. A GOOD DEAL OF LEAKAGE FLUX FLOWS IN OVER HALF OF THE CORE

Figure 4. Leakage flux in different types of current transformers

Inspection of figure 3 will show that the mutual impedance, L_1, must be kept high with respect to the burden impedance, if good current-transformer performance is to be expected. If the symmetrical current only is considered, standard current transformers are usually adequate. Actual asymmetry of the fault current requires consideration of a new and very important factor. The d-c component of the primary current must also divide between the parallel branches of figure 3. Since, to all practical purposes, there is very little impedance to direct current in the magnetizing branch, L_1, of the parallel circuit, the flow of direct current in this branch is retarded only by the inductive voltage generated by the time rate of change of the direct current, as it builds up in this circuit. The voltage which must be generated is determined by the voltage drop across the burden, R_2 and L_2, caused by the d-c component flowing through it. If the d-c time constant is long, this direct voltage must be maintained for a relatively long time, and it can only be done by a continuously increasing current through L_1 at a relatively high time rate of change. This must be done in one direction only, without the advantage of positive and negative half-cycles characteristic of alternating current. This means that the knee of the saturation curve for the inductance, L_1, may be reached within a few cycles, or within a fraction of a cycle in severe cases. When the iron core saturates, the effective value of the inductance, L_1, decreases to a fraction of the normal value. When this occurs, the ability of the mutual inductance to retard the flow of direct current through it collapses, so that practically all of the d-c component goes through L_1. This condition will continue until the d-c component has subsided, after which the transformer can again perform with its normal a-c characteristics. It is of interest and importance to note that during the time that the magnetizing branch, L_1, remains severely saturated by direct current, it also offers much less impedance to the flow of alternating current; consequently, it is to be expected that the percentage of the a-c component which is transmitted to the secondary will be materially less. These conclusions are borne out by oscillographic records which show that, when a current transformer is failing to reproduce faithfully the d-c component in the secondary, it is also breaking down in a-c ratio, even though its performance may be quite accurate on the same value of symmetrical current.

Derivation of Formulas

Mathematical analysis of the current-transformer performance is based on the same equivalent circuit, shown in figure 3, and in fact, has already been worked out by Marshall and Langguth.[1] Their paper gives rules for keeping the flux density below the saturation value and for calculating performance, provided the transformer does not saturate, but, of course, cannot be used to indicate performance in cases where there simply is not room for the resulting design, or where existing transformers must somehow be used. The following analysis will be devoted to methods for calculating the performance, after saturation is reached. Important savings in space, as well as in weight and expense, may result if the analysis shows that current transformers which are far from perfect may still be adequate.

GENERAL CONSIDERATIONS

Returning to figure 2*B*, it must be recognized that although the sum of the leakage reactances, X_p and X_s, may be nearly constant and represent definite flux linkages, the effective division between X_p and X_s is different for every load condition, and they are individually practically impossible to determine.

Most problems deal with the "through" or "bar" type of current transformer, which can be so designed that the leakage flux which enters the core is negligible, and the following theory has, therefore, been worked out on the basis that the secondary leakage reactance is zero. The theory cannot be indiscriminately applied to wound-type transformers nor to through-type transformers with coils only on two legs of the core (see figure 4) without preliminary investigation of the relative amount of leakage flux.

The theory described assumes that the reactance of the burden is also zero. The justification for this is difficult to establish rigorously, but it can be readily estimated that once the d-c component of current is established (as it is during the first half-cycle) the inductance in the burden will present no obstacle to maintaining it, while the resistance in the burden acts against the direct current with a more or less continuous counter (*IR*) voltage. The

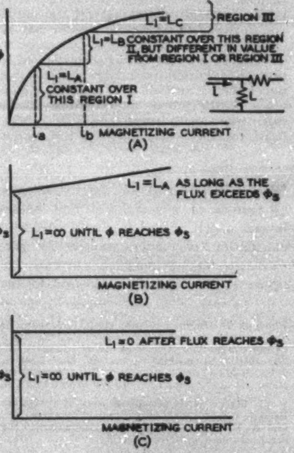


Figure 5. Approximations of the saturation curve

The true saturation curve may be approached as closely as may be desired theory has been worked out and calculations made with inductance in the burden, and it has been found that it makes very little practical difference in the result if the inductance is neglected, at least for burden power factors of 80 per cent or more. Tests confirm this.

The value of the assumption that $L_2=0$ is that the numerical work of calculation is approximately halved. After an example has been worked out, the importance of this becomes obvious. In the short-cut method to be described later, the secondary inductance may be included, but it can be shown that it can be neglected with negligible error.

The final assumption is that the equivalent resistance of the magnetizing branch of the network is zero. Correctness of this assumption is more difficult to prove, but actual results indicate that it is always practically true.

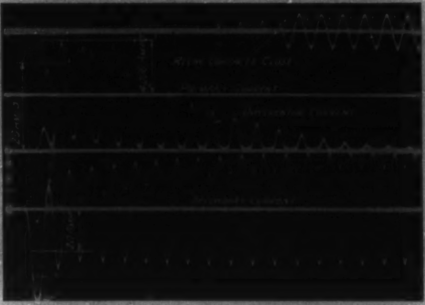


Figure 6A. Oscillogram showing that nonsaturating current transformer (primary current) balanced against current transformer that saturates (secondary current) gives a false differential current

Primary current, 4,000 amperes rms, $T=0.075$, ratio 1,000/5, test current transformer core area 1.69 square inches, secondary resistance 1.05 ohms

FUNDAMENTAL EQUATIONS

The equivalent network to be analyzed is thus very simple, figure 3, and may be analyzed exactly as Marshall and Langguth have previously done, except that $L_2=0$.

The equation for the difference current will be the same as given by Marshall and Langguth (equation 15, corrected) in their paper, except to neglect L_2.

$$i_1=\frac{1}{R_2T_1}\epsilon^{-t/T_1}\int R_2i\epsilon^{t/T_1}\,dt+C\epsilon^{-t/T_1} \qquad (1)$$

where $T_1=L_1/R_2$.

If the circuit is closed at the moment to get a fully offset primary current:

$$i=I\,(\cos\omega t-\epsilon^{-t/T}) \qquad (2)$$

Substituting for i and performing the indicated integration gives:

$$i_1=I\left[\frac{\cos\,(\omega t-\Delta)}{\sqrt{1+(\omega T_1)^2}}-\frac{T}{T-T_1}\epsilon^{-t/T}\right]+C_1\epsilon^{-t/T_1} \qquad (3)$$

$$\Delta=\tan^{-1}\,\omega T_1$$

$$T_1=\frac{L_1}{R_2}$$

This equation is perfectly general, and is the one on which the calculation of exciting current will be based. Its limitation in actual use is that L_1 (and T_1) are assumed to be constant; in the actual problem the solution must be carried out into the region of saturation, and L_1 (and T_1) will vary.

Step-by-Step Solution

THREE-ZONE SATURATION CURVE

Any saturation curve can be represented as closely as may be desired by a number of straight lines. This makes it possible to consider the inductance constant in a given region, as in figure 5*A*. With such an approximate curve, the magnetizing current may be calculated by means of equation 3. The procedure is to begin at time, $t=0$, setting $L_1=L_A$, $i_1=0$, evaluate C_1, and calculate i_1 at various values of t. It must be remembered that each time that i_1 enters a new region, figure 5*A*, the value of L_1 must be changed correspondingly, and a new value of C_1 obtained. This procedure gives good results, but is very laborious, and quite impractical for most problems. Further simplification is desirable.

TWO-ZONE SATURATION CURVE

It will usually be found that the exciting current will be negligible in its effect on the differential relay until the upper region of the saturation curve is reached. This justifies the assumption of a more simple shape of the saturation curve, as in figure 5*B*. The advantage of this shape is that L_1 is infinite until the "saturation" flux is reached, and the boundary conditions at the same time, t_1, at which ϕ_s is reached are: $t=t_1$, $i_1=0$. Returning to equation 5 in the paper by Marshall and Langguth, the flux will be (letting $L_2=0$ and writing I_s for N_pI_p/N_s)

$$\phi=\frac{10^8I_s}{N_s}\left[\frac{R_2\sin\omega t}{\omega}+R_2T\epsilon^{-t/T}\right]+C \qquad (4)$$

The procedure of calculation will be:

1. Calculate the flux variation beginning at time, $t=0$, $\phi=0$. Evaluate C accordingly, and calculate ϕ at several intervals of time, t, as necessary to determine the time, t_1, at which the flux will reach the "saturation" value, ϕ_s.
2. The new boundary conditions to be used now in equation 3 will be $t=t_1$, $i_1=0$. Evaluate C_1 and calculate i_1 at suitable intervals of time. Inspection of the equation shows that i_1 may be maintained at a positive value for a considerable length of time if the values of t, C_1, and T are suitable.
3. However, i_1 will eventually pass through zero. When it does at time, $t=t_2$, we must return to the equation 4 for ϕ and substitute the boundary conditions, $t=t_2$, $\phi=\phi_s$. The variation of ϕ must then be calculated. Usually, it will return to the "saturation" value of ϕ_s within a fraction of a cycle, say at time, t_3.
4. At time, t_3, $i_1=0$, determine a new value of C_1 and continue calculation of i_1.

This procedure might appear to be fully as difficult as that of the first method. Actually, it is much easier, and usually gives results which are not appreciably different.

If circuit constants are such that the exciting current (paragraph 2 above), is maintained at a positive value for a number of cycles, equation 3 shows that this exciting current is composed of a sinusoidal component with two superimposed d-c components. Unless the top of the saturation curve has an appreciable curvature, there will be few harmonic components in the exciting current. The variation of flux may occur over a relatively short zone along the top of the saturation curve, which may be nearly straight. Of course, the actual flux variation is always along a superimposed hysteresis loop, and the equivalent degree of curvature with the resulting harmonic components cannot be practically determined on any exact basis. However, tests show that the actual differential current may often be very nearly sinusoidal.

FLAT-TOP SATURATION CURVE

A still more simple method is to assume that the saturation curve has a flat

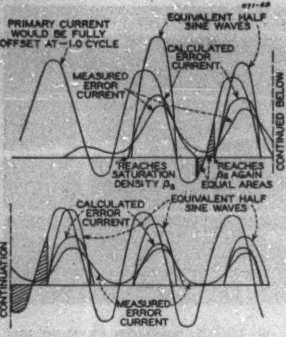


Figure 6B. Check of calculated error current against measured error current of figure 6A

The lower section of the figure is a continuation of the upper section. The "calculated error current" was calculated by the step-by-step method, equation 3 and sloped-top saturation curve, and the "equivalent half sine waves" were calculated using equation 12

Constants for calculated error current: $I=28.3$, $T=0.075$ ωL_1 corresponding to slope of top of saturation curve$=1.37$, $R_2=1.05$, $A=1.69$ square inches

top, so that L_1 will be zero after saturation (figure 5*C*) and the current equation will become:

$$i_1=I(\cos \omega t-\epsilon^{-t/T})$$

This is the same as the expression for the primary current, and shows, as would be expected, that if the saturation is complete at density, ϕ_s, there is no further reproduction of current in the secondary circuit until the time during the cycle when the primary current again passes through zero. At this time, the flux will tend to decrease below ϕ_s and the primary current will, thereafter, be perfectly reproduced until the flux once more increases to ϕ_s.

Figure 6*B* shows the exciting current as measured, and as calculated according to the sloped-top, and the flat-top saturation curve, compared with test values, for a given example. It can be seen that the calculated values according to both methods are in fair agreement with the test values.

Approximate Formulas

All these methods involve a great deal of numerical work. They are entirely too difficult for use in making the large number of preliminary estimates of performance which must be made during the study of a system of protection for a given bus layout. A still more simple solution is based on some rather bold assumptions. The justification of these assumptions will be shown to be two-fold:

1. The assumptions will be such that the final result will be pessimistic.
2. The results will agree sufficiently well with actual tests to be generally useful.

Returning to figure 6*B* it will be seen that the measured exciting current exhibits the following characteristics:

1. The area under the test current wave is very nearly the same as the area under the calculated current wave.
2. The exciting current wave form is approximately that of a half-wave rectified current.

These characteristics are shown in a large number of oscillograms, some of which are shown in figures 6*A*, 10*A*, and 10*B*.

From these considerations we will make the following assumptions:

1. The exciting current can be calculated and dealt with on the basis that it will always be a half sine wave.
2. The magnitude of it can be calculated on the basis that the area under the half wave will equal the area under the exciting current wave calculated by the previously described method.

The meaning, and the advantage of assumption 2 will be apparent from the consideration that the area of the exciting current wave can be determined fairly simply from the fundamental equation:

$$N\frac{d\phi}{dt}10^{-8}=R_2i_2$$

$$N\phi 10^{-8}=10^{-8}\int N d\phi=R_2\int i_2 dt \qquad (5)^*$$

and the consideration that ϕ will be at the saturation value at both beginning and end of a cycle, and that if the integration is from beginning to end, $N\phi$ must, therefore, be zero. The equation then becomes simply:

$$R_2\int_{t=(n+1)T_0}^{t=nT_0} i_2 dt=0 \qquad T_0=\frac{1}{f} \qquad (6)$$

However, the actual integral of an asymmetrical primary current over a cycle is obviously not zero. The meaning of this is that the whole value of the integral of primary current is exciting current, and will flow through the differential circuit. This integral is easily evaluated and can be shown to be (see appendix I):

$$\int_{t=(n+1)T_0}^{t=nT_0} i dt=IT_0\epsilon^{-nT_0/T} \qquad (7)$$

for any given cycle, n. Now, if the area under the exciting wave is $IT_0\epsilon^{-nT_0/T}$ according to (7), the magnitude of its peak value can easily be determined, if the assumption is maintained that the exciting current will appear as a half sine wave. The area under a half sine wave is (see appendix I):

$$A=\frac{PT_0}{\pi} \qquad (8)$$

where

P = peak value
T_0 = time of one cycle

If the area is given by (7), the peak value can be obtained from (8) and (7).

$$P=\frac{\pi A}{T_0}=\pi I\epsilon^{-nT_0/T} \qquad (9)$$

This expression for value of any exciting current peak at the nth cycle can be very quickly evaluated.

Determination of the time at which the saturation value of flux is reached can be made from equation 4. This equation cannot be solved for t as it stands, but if we make the pessimistic assumption that the sinusoidal component is always at its maximum value—

$$\frac{R_2\sin\omega t}{\omega}=\frac{R_2}{\omega}$$

then the equation can be solved for t, giving

$$t=-\frac{T}{0.434T_0}\log\left[1-\frac{\beta_s NA}{10^8R_2TI}+\frac{1}{\omega T}\right] \qquad (10)$$

If an impedance, Z_D, is connected into the differential circuit, it obviously will reduce the differential current. If the resistance were zero, the effect of inductance would be to reduce the variation in current, but to allow about the same magnitude of d-c component. If the inductance were zero, the resistance would continuously oppose the current, reducing it at every point on the wave.

If the differential current is calculated on the basis that the inductance in the differential circuit is negligible, the calculated value will certainly be somewhat too high, and the result will be in error on the conservative side. Referring to figure 9, we can rewrite (5)—

$$e_s=\frac{Nd\phi}{dt}10^8=R_2i_2-R_Di_1 \qquad (11)$$

Considering, as before, that ϕ will have to be the same, and equal to ϕ_s at the end of each cycle, it can be shown that (appendix II) the differential current will be a fraction of the value calculated from the assumption that the resistance in the differential circuit is zero, or

---

* $L_s\frac{di}{dt}$ may be included in this equation, but it can be shown that the resulting integral L_si will be negligible for any usual problem.

$$P = \pi I \epsilon^{-nT_0/T} \frac{R_2}{R_D+R_2} \text{ (peak amperes)} \qquad (12)$$

The methods used may seem to lack mathematical rigor. Indeed they do, but the assumptions are all uniformly pessimistic, and cannot well cause errors which destroy the practical value of the result. Comparison with tests show that the results are sufficiently accurate to be useful.

Relay Characteristics

The operating characteristics of relays are usually given in terms of symmetrical sine-wave quantities. Also, when current-time curves are taken, the current is usually left on until after the contacts have closed and the time determined. Consideration of the characteristics of the expected differential current reveals at once that it is not always sinusoidal nor symmetrical, and that it does not necessarily last for a length of time sufficient to cause the relay contacts to close, depending, of course, upon the time setting of the relay being used. For the differential problem, additional data are needed. These cover relay performance on altered wave forms involving various amounts of d-c and current-time curves wherein the current is applied only for a limited time. The latter curves will be termed "impulse curves" for convenience.

It is impossible to duplicate in the relay testing laboratory all of the possible currents which may occur in the differential circuit when all the variations between applications and between different possibilities for one application are considered. The best that can be done is to take data of a general nature which may be approximately interpreted for the particular application. Consideration of a large number of test oscillograms lead to the following conclusions:

1. The differential current for two or more cycles may be fairly represented in many cases by a fully and continuously offset sine wave. These few cycles, then, represent those test conditions wherein the harmonics present in the differential current are of minor consequence. The condition is easily set up by passing both steady-state direct current and sine-wave alternating current through the relay coil at the same time.

2. Subsequent cycles of the differential current may be fairly represented by half-wave rectified current. This approximation is sufficiently accurate until the differential current has subsided to a point where it is negligible in comparison with the original peak values.

A comparison of a standard current-time curve with impulse curves is given in figure 8 for a standard-type *COH* overcurrent relay. Each of the four curves was taken with the same tap and time-lever setting. The abscissas are plotted in terms of peak amperes rather than rms amperes in order that the approximate formula (9) or (12) for differential current may be most useful. Curve *a* is the usual current-time curve where the current is not interrupted until after the contacts close. For curve *b*, however, sine-wave 60-cycle current was applied only for the length of time shown by the time scale, and then interrupted. After the current was interrupted, the contacts closed due to kinetic energy stored in the disk. The total elapsed time from the application of current until the contacts closed is not shown by the curve at all. Thus, this curve is an "impulse curve", indicating a product of current and time which will store just sufficient kinetic energy in the relay disk to cause the contacts to close. Curve *c* is a similar impulse curve, except that half-wave rectified current was used. Curve *d* is a similar curve for a fully offset sine wave, where the direct current through the relay was made equal to 1.414 times the a-c sine-wave amperes. Curves *c* and *d* compared with curve *b* show the effect of a d-c component. Also, for the same peak-current values, curves *c* and *d* have less rms and 60-cycle component values. Curves *c* and *d* differ from each other because of the proportion of the direct current being different and because there are harmonics in the half-wave current.

Confirming Tests

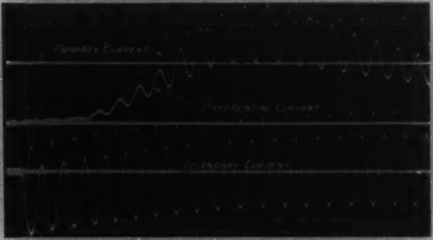
The approximate methods of calculating the current-transformer performance, as given, and the proper interpretation of the results in terms of impulse characteristics of the relays would have no value, of course, if it were not possible to verify the predictions made by means of suitable tests. Confirming tests have been made with results as given below.

Test Setup

Figure 9 shows the essential details of the test setup which was used. The circuit for demagnetizing the current transformers between tests is not shown. The nonsaturating current transformer shown was liberally designed so that it would not saturate under any of the test conditions. For this reason, the oscillographic record of *i* was taken as the true picture, in terms of the secondary, of the primary current. At the same time, the current, *i*, may be taken to represent the total output of any number of current transformers carrying current into a bus for an external fault, as in figure 1, on the basis that these current transformers do not saturate. This is a legitimate simplification of test setup, since nonsaturation of the input current transformers gives the maximum difference current, i_1, for a given saturation of the outgoing current transformer. Thus, the most pessimistic condition is arrived at at once with a decided simplification of test.

The resistor, R_2, was used to obtain various degrees of saturation of the test current transformer. The resistor, R_D, was

Figure 7. Test oscillogram showing error current with high percentage of direct current



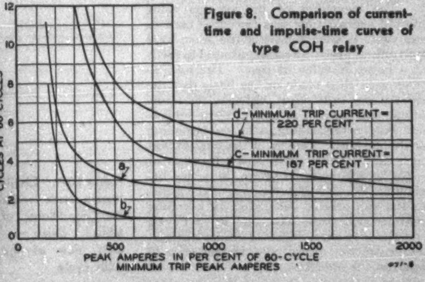


Figure 8. Comparison of current-time and impulse-time curves of type COH relay

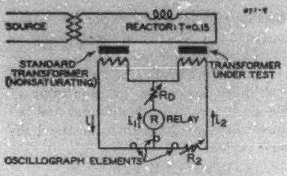


Figure 9. Diagram of test circuit

used to explore the possibility of preventing the false operation of a simple overcurrent differential relay by this means, and to check the accuracy of equation 12 showing the reduction in differential current expected.

As usual in tests of this kind, the current wave shape which can actually be obtained is not the perfect shape assumed in the theory, and the test results have to be properly interpreted to take the actual variation into account. The actual wave departs from the theoretical as follows:

1. It is never perfectly offset.

2. The output from a power transformer will contain not only a d-c component in proportion to the primary d-c component, but also a secondary d-c component determined by the constants of the secondary circuit. As a result, the secondary current, if initially offset on one side of zero, always becomes slightly offset on the opposite side before the transient finally disappears. The apparent decrement toward the end of the transient is higher than at the beginning, and higher than would be predicted from the true time constant L/R of the circuit.

3. There seems to be a higher decrement over the first cycle of current, probably due to the contact not being solidly closed initially.

The test results were, therefore, analyzed as follows:

1. The time constant was determined by plotting the logarithm of the average value of current. When the time constant departed to an appreciable extent from the true value (toward the end of the transient) the test and calculation do not agree, and this fact was simply accepted as unavoidable.

2. The logarithm of the average value plotted against time was projected back to the value it should have if the wave were fully offset, and it was always found to occur at a negative value of time. The calculations were then made on the basis that the current was fully offset, but that the transformer flux started at zero at some equivalent time corresponding to the real beginning of the current. That is, if the current was such that it would have been fully offset at -1.0 cycle (one cycle before the actual beginning) the time of saturation of the transformer can still be calculated by assuming that the flux starts from zero at $t=1$ cycle and the successive values of differential current calculated using $n+1$ instead of n in equations 9 and 12.

Table I

$I=28.3 \quad R_1=1.05 \quad R_D=0.38$
P = Peak Value of Differential Current

n_{osc}*	n_{eff}†	$P=\frac{1.05}{1.43}\pi I\epsilon^{-nT_s/T}$	P Test Figure 10A	Calculated Cycles From Figure 8c: From Calculated P	Calculated Cycles From Figure 8c: From Test P	Reciprocals**: Column 5	Reciprocals**: Column 6
0	1		2.04				
Saturation occurs here							
1	2	41.4	17.9	3.2	4.8	0.310	0.208
2	3	33.3	18.36	3.5	4.7	0.286	0.213
3	4	26.6	16.0	3.9	5.4	0.256	0.185
4	5	21.3	13.8	4.2	6.3	0.238	0.159
5	6	17.1	11.6	5.0	7.9	0.200	0.127
6	7	13.7	9.88	6.5	9.5	0.154	0.105
7	8	11.0	8.31	8.4	12.5	0.119	0.080
8	9	8.75	6.75	12.0	18.0	0.083	0.055
9	10	7.02	5.64	18.0	24.0	0.055	0.042
10	11	5.67		24.0		0.042	
						1.743	1.174

* n_{osc} denotes the number of the cycle on the oscillogram.

† n_{eff} denotes the number of the cycle on counting from time, $t=-1.0$ cycle where the current would have been fully offset.

** The reciprocals indicate a percentage of the energy required to operate the relay. Thus, if three cycles of a given peak current are required to close the contacts, then each peak delivers one-third of the total energy needed.

This sort of interpretation is awkward and not as straightforward and clear as might be desired, but is hardly to be avoided. If an a-c generator is used to supply the current, the difference between transient and subtransient reactance will reduce the initial a-c component, with the result that a wave which is initially fully offset may be more than fully offset during the second or third cycles, which will also require interpretation. It is obvious that the possible impulses of differential current can safely be calculated on the basis that the a-c component is maintained at the value corresponding to the subtransient reactance. Therefore, whether the current source is a generator or a transformer, the calculated differential current will always be somewhat higher than the actual value, providing that the current is based on the subtransient reactance of the generator.

The perfect current transformer used as a standard had about ten times the cross section of core and about one-fifth the resistance in its circuit of the transformer under test, and calculation showed that its flux density never exceeded the value corresponding to maximum permeability, so that it can safely be considered to be perfect. The other transformers in an actual differential protection circuit may not be perfect. If they are not, the actual differential current will be even less, and a perfect transformer balanced against a poor one represents a worst possible condition.

Taking all these considerations into account, a large number of tests were made and compared with calculations.

Test Results

The first set of tests was made with negligible resistance in the differential

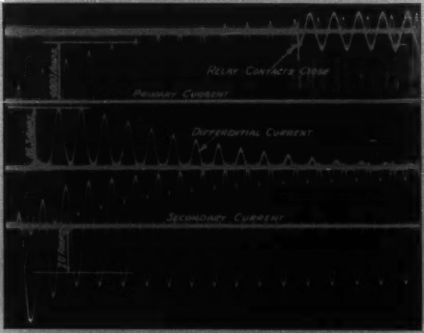


Figure 10A. Oscillogram made under the conditions of table I. Relay contacts close at the 12th cycle

circuit. The differential current was calculated according to equations 3 and 4, and a very good agreement obtained (figure 6). It was also calculated according to equation 12, and it can be seen that the equivalent half sine waves are much larger than test values to begin with, but are in fairly good agreement with test after the first few cycles.

The next step was to introduce resistance into the differential circuit. With a burden resistance of 0.82 ohm and a differential resistance of 0.38 ohm, the oscillogram of figure 10*A* was taken. The primary current lacked one cycle of being fully offset; that is, it would have been fully offset at −1.0 cycle. The time to saturation, calculated by equation 10, is 0.95 cycle. Taking this as 1.0, the equivalent time at which saturation would occur is actually about 2.0 cycles, and the successive calculated peaks (equation 12) of differential current are given in table I with the test values scaled from figure 10*A* for comparison.

The number of cycles which each peak would have taken to operate the relay (taken from figure 8) are given in columns 5 and 6 of table I, and the reciprocals of the numbers of cycles in columns 7 and 8. The sums of these columns are 1.74 and 1.17 respectively, and both indicate that the relay should operate. The relay actually operated about once in six times of application of current, and always operated when the current was offset as in figure 10*A*.

When the differential resistance is increased to 0.75 ohm, the oscillogram, figure 10*B* results and the corresponding peaks are given in table II. The sum of the reciprocal column based on calculated current indicates that the relay should operate. Actually, the relay could not be made to operate.

These tests indicate a considerable factor of safety. A large number of tests and calculations have been made, some of which show no factor of safety at all, and one test showed an actual operation when the calculated sum was only 0.8. This test indicates that if nonoperation is to be guaranteed, the sum of the reciprocal column should be held to something less than 0.8. A sum of 0.5 has been taken as a very safe value. If saturation occurs during the first two or three cycles, consideration of the possible differential current shows that each peak will have to last longer than one-half cycle and will, therefore, have a smaller peak value. Therefore, a larger factor of safety can be counted on if saturation occurs very soon than if it occurs later.

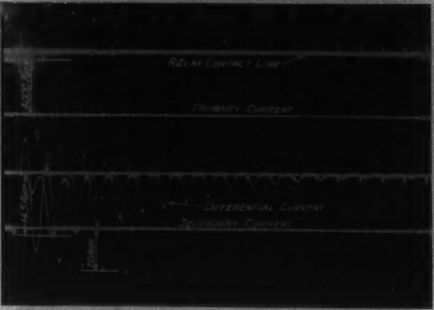


Figure 10B. Oscillogram made under the conditions of table II. Similar to table I, except that $R_D=0.75$ ohm. The relay does not operate

The test results which can be presented here are necessarily limited, so that they cannot adequately cover the entire range of possibilities. A large number of tests have been made, however, with sufficient results to justify the methods covered in this paper and are sufficiently general to warrant the following discussion.

In figure 10, for example, it is noted that the first few cycles of differential current are composed almost entirely of a d-c component and a sine-wave component. Obviously, its form is different from half wave, but the half-wave impulse curve of the relay was used in predicting relay operation. A better prediction could have been made by using impulse curve 8*d* for the first few cycles. However, curve 8*d* shows that the combined direct current and sine wave is less effective in operating the relay, and it is thus concluded that the use of curve 8*c* errs on the conservative side. From the point of view of simplification, as well as insuring that errors will always be on the conservative side, it is better to use curve 8*c* only, neglecting curve 8*d*.

Table II

$I=28.3 \quad R_1=1.05 \quad R_D=0.75$
P=Peak Value of Differential Current

nose	n_{eff}	$P=\frac{1.05}{1.8}\pi I\epsilon^{-nT_0/T}$	P Test Figure 10B	Calculated Cycles From Figure 8c: From Calculated P	Calculated Cycles From Figure 8c: From Test P	Reciprocals: Column 5	Reciprocals: Column 6
0	1		4.5				
		Saturation occurs here					
1	2	32.8	14.3	3.6	6.0	0.278	0.167
2	3	26.4	13.9	3.8	6.4	0.263	0.156
3	4	21.1	11.6	4.2	8.0	0.238	0.125
4	5	16.9	10.0	5.0	9.5	0.200	0.105
5	6	13.6	8.6	6.4	12.0	0.156	0.083
6	7	10.9	7.5	8.4	15.3	0.119	0.066
7	8	8.73	6.35	12.0	20.6	0.083	0.049
8	9	6.95	5.28	18.0		0.056	
						1.393	0.751

Other Relays

The tests described were made with a simple overcurrent relay. In many cases, such a relay is not adequate for bus differential protection. The type *CA*-6 relay, which is a ratio-differential relay of the variable-ratio type, may be used where the requirements are more severe. With this relay, the same method of calculation of the peak values of the differential current may be used, of course. In considering the impulse characteristics, however, the effect of through fault current restraint is very important and helpful in allowing a much greater degree of current transformer saturation without causing a false tripping operation for a through fault. The impulse curves for the type *CA*-6 relay are quite similar to curve *c* of figure 8, except that they are moved considerably to the right, even with moderate restraint values.

Conclusions

1. The methods of approximate calculations outlined provide a tool for the analysis of current-transformer performance which has not been available previously.

2. The resulting predicted current in differential relay circuits is sufficiently accurate

for determining relay schemes and settings because the approximations which it has been necessary to make have been uniformly made on the pessimistic side.

3. The use of impulse curves of relays is of material value in reducing the amount of safety factor to allow because of unknown factors.

4. It is felt that the agreement between calculated results and test results, while not perfect, is sufficiently close to warrant the use of the method by protection engineers. Because of the variations noted, however, it is recommended that a safety factor of about 2 should be applied in relay work.

Appendix I

Evaluation of

$$\int_{t=(n+1)T_0}^{t=nT_0} i\,dt$$

$$i=I(\cos\omega t-\epsilon^{-t/T})$$

$$\int_{t=(n+1)T_0}^{t=nT_0} i\,dt=$$

$$I\left[\frac{\sin\omega t}{\omega}+T\epsilon^{-t/T}\right]_{t=(n+1)T_0}^{t=nT_0}=$$

$$IT\left[\epsilon^{\frac{-nT_0}{T}}-\epsilon^{\frac{-(n+1)T_0}{T}}\right]=$$

$$IT\epsilon^{\frac{-nT_0}{T}}\left[1-\epsilon^{\frac{-T_0}{T}}\right]$$

T/T_0 must be at least 3 in any problem of importance, because if the transient has a time constant less than three cycles long, it may usually be neglected. Hence, T_0/T will be less than 0.33 and if we write the series for $\epsilon^{-T_0/T}$:

$$\epsilon^{-T_0/T}=1-\frac{T_0}{T}+\left(\frac{T_0}{T}\right)^2\cdot\frac{1}{\underline{|2}}\ldots\text{etc.}$$

and

$$\frac{1}{\underline{|2}}\cdot\left(\frac{T_0}{T}\right)^2$$

and higher terms can be neglected.

Hence:

$$\int_{t=(n+1)T_0}^{t=nT_0} i\,dt=IT_0\epsilon^{-nT_0/T}$$

Evaluation of area under a half sine wave of peak value P:

$$\text{Area}=P\int_{t=0}^{t=\pi/\omega}\sin\omega t\,dt$$

$$=P\left[-\frac{\cos\omega t}{\omega}\right]_0^{\pi/\omega}$$

$$=\frac{2P}{\omega}=\frac{2P}{2\pi f}=\frac{PT_0}{\pi}$$

If the area of the integral

$$\int_{t=(n+1)T_0}^{t=nT_0} i\,dt\doteq IT_0\epsilon^{-nT_0/T}$$

is to be set equal to the area under the equivalent half sine wave, PT_0/π:

$$\frac{PT_0}{\pi}=IT_0\epsilon^{-nT_0/T}$$

the equivalent peak value is $P=\pi I\epsilon^{-nT_0/T}$.

Appendix II

Derivation of equation 12, considering effect of resistance in differential circuit: Continuing from equation 11, and following the same reasoning as in going from equation 5 to equation 7:

$$e_s=N\frac{d\phi}{dt}10^{-8}=R_2i_2-R_Di_1$$

or

$$N\frac{d\phi}{dt}10^{-8}=R_2i_2-R_Di_1$$

$$N\phi=N\int d\phi=0=R_2\int i_2dt-R_D\int i_1dt$$

$$\int i_2dt=\frac{R_D}{R_2}\int i_1dt$$

but

$$i_1=i-i_2$$

and

$$\int i_1dt=\int i\,dt-\int i_2dt=\int i\,dt-\frac{R_D}{R_2}\int i_1dt$$

Then

$$\int i_1dt=\frac{R_2}{R_2+R_D}\int i\,dt$$

As in appendix I, if we express the area,

$$\int_{t=(n+1)T_0}^{t=nT_0} i_1dt=\frac{PT_0}{\pi}$$

the equivalent half sine wave and

$$\int_{t=(n+1)T_0}^{t=nT_0} i\,dt=IT_0\epsilon^{\frac{-nT_0}{T}}$$

we have

$$\frac{PT_0}{\pi}=\frac{R_2}{R_2+R_D}IT_0\epsilon^{\frac{-nT_0}{T}}$$

and

$$P=\frac{R_2}{R_2+R_D}\pi I\epsilon^{\frac{-nT_0}{T}}\qquad(12)$$

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Electric Braking for Railroad and Urban Transit Equipment

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THE PURPOSE of this paper is to outline the present status of electric braking in the railroad and urban transit fields and to point out possible solutions of future braking problems particularly those arising from increasing speeds.

General

The science of train braking has progressed steadily with the development of railroad transportation. Improvements in air brakes along with improvements in motive power and rolling stock have made possible the steadily increasing speed and length of trains; in fact, the railroad industry as we know it today. With the exception of a few railway electrifications where regenerative electric braking has been applied, this entire development of braking on the railroads of this country has been, until very recently, that of the compressed-air system.

As the speed and length of trains have increased, the braking problem has become more and more difficult of successful solution. In freight-train operation, the problem is accentuated more by the length than the increased speed and has been one more of control than of dissipation of energy. This is because maximum speeds have been restricted both by tonnage handled per locomotive unit and by the design of rolling stock used. On the other hand, length of trains has increased to such an extent, it has been difficult to transmit the braking impulse from the locomotive to 100–150 cars in such a way as to avoid undesirably variable response, rough slack action, and damage to equipment and lading. A program is now under way involving application of improved brake equipment to approximately 2,000,000 freight cars to eliminate these conditions.

In passenger equipment, the problem of braking is more affected by increased speed than increased train length. A 20-car train is about the maximum handled and most trains are further restricted by traffic requirements and station-platform limitations. The use of spring buffers and other means of either controlling or eliminating slack, simplify the problem. The use of electropneumatic brakes on unit trains effects instantaneous transmission of the braking impulse to every car. The difficulty arises from the peak speeds now encountered—100 miles per hour or more—where desired retardation rates, braking ratios, shoe pressures, and rate of energy dissipation greatly exceed past requirements for safe and economical operation.

Associated with the high-speed deceleration problem is the requirement of higher-speed operation on down grades where, in mountain territory, railroad profiles require miles of continuous operation descending two per cent grades, imposing a heavy load on the brake equipment from the standpoint of continuous energy dissipation that is almost directly proportional to the higher speed.

These problems are serious and much research and development work is being done by the air-brake manufacturers to cope with them. Tied in with these problems is the question of the deleterious effect many claim high-speed braking has on wheel life and performance. This has led to experiments with various types of nontread friction brakes.

In the urban transit field schedule speeds have also been increased with a corresponding increase in braking rates as well as acceleration. While speeds are much lower than for heavy railroad operation, the requirement for more rapid acceleration is, nevertheless, an important factor. At the same time relief from excessive heating of the wheel treads by mechanical braking is essential to economical maintenance and safe operation.

Unlike heavy railroad operations, urban vehicles rarely require braking on long grades but are concerned principally with frequent and rapid decelerations for stops. Dynamic or rheostatic braking systems and electromagnetic track brakes are now being extensively applied to urban transit vehicles.

The successful dissipation of large amounts of energy is not the only problem in high-speed braking. Due to the high retardation rates required to hold stopping distances within prescribed limits, it is desirable to take advantage without wheel sliding of all of the wheel-rail adhesion possible throughout the complete deceleration. Opposed to this, is the fact that the braking force is directly affected by many transient phenomena, which cause it to change many times during a deceleration. Not only does this make necessary the use of devices to alter the braking ratio during a maximum deceleration, but also, because these phenomena are likely to differ in magnitude on different axles in the train due to condition of wheels, shoes, brake rigging, etc., the brake application should be such as to prevent wheel sliding on the axle most susceptible to it. Devices are now being tried which anticipate wheel sliding on each axle and automatically ease the brake shoe pressure when wheel sliding is imminent.

A precept which has governed the design of new braking equipment is that improvements must be of such a nature that they will function in complete harmony with existing apparatus and thus preserve the enormous investment in equipment already in service.

The foregoing remarks give a rough idea of the magnitude and complexity of the braking problem, particularly with respect to high-speed passenger operation. The following discussion will attempt to show the part which various forms of electric braking are playing in the railroad picture and what effect they may have on the further development of means to cope successfully and economically with this increasingly difficult problem of high-speed braking.

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Braking a train from high speed to standstill usually takes the form of unproductive liberation of heat by the dissipation of kinetic energy created during acceleration. Braking on a heavy downgrade at constant speed produces the same result by dissipating potential energy created by hauling the train uphill previously. The natural function of a wheel appears to be to roll under load and not act as a medium for the dissipation of excessive amounts of energy at excessively high rates such as encountered frequently in the retardation of modern trains. Even though wheels have been employed at this task with considerable success, the demands have reached a point where other means are being considered for the dissipation of this energy.

Regenerative Electric Braking

The ideal braking system would be one which would transform into useful work all available kinetic and potential energy in the train not absorbed by normal train friction. As this would require a system of 100 per cent efficiency, it is obviously impossible but from a practical standpoint, such a system for constant-speed retardations is approached by regenerative electric braking which is available for most forms of straight electric locomotives, and is one of the several advantages which railroad electrification in general makes available.

This type of braking has been used on mountain electrifications exclusively since the benefits have been greatest in retarding heavy trains while negotiating long steep grades. Wherever used, this system has been very successful and has resulted in the following advantages:

Saving of approximately 15 per cent of total power required without regeneration, and 60 per cent on two per cent grade sections.

Elimination of brake shoe, wheel, and brake rigging trouble with attendant reduction in maintenance costs.

Smooth, continuous braking effort over long periods eliminating surges in speed with resultant wear and abuse of equipment.

Increased safety because the air brake is held in reserve so that in case of emergency stops, the air-brake system is charged and wheels and brake shoes are cool.

Increased comfort to passengers due to constant speed on grades and absence of noise due to grinding of shoes on wheels.

With this system, the braking power is limited by the driver weight of the locomotive and the capacity of the traction motors. In operation involving retardation of the train downgrade at constant speed, due to the fact the braking effort necessary is equal to the required tractive effort to haul the train up the grade minus twice the train friction, modern regenerative locomotives are capable of handling with leeway, any train downgrade which they can handle up the same grade. Grade sections also frequently include heavy curves which are fully compensated and this factor further reduces the braking necessary to hold a descending train.

Take for example a passenger train of 1,000 tons gross weight hauled by a 3,000-volt d-c locomotive recently put into service. The locomotive weighs 185 tons with 135 tons on drivers. The motoring and braking characteristics are shown in figures 1 and 2 respectively. The tractive effort required to handle this train up a two per cent grade is 47,000 pounds or 17.4 per cent adhesion, which gives a balancing speed of 50.3 miles per hour in the highest-speed motor combination. Retarding this same train down a two per cent grade requires 31,000 pounds braking effort or 11.5 per cent adhesion, which is attainable up to 67.5 miles per hour.

Track conditions, particularly curvature, will, of course, govern speeds in this type of operation, but the foregoing example shows the possibility of regenerative electric braking with a modern high-powered locomotive with respect to high-speed operation in heavy-grade territory. In this example, additional electric braking power is available if required, and with the air-brake system backing up the regenerative brake, safety is assured.

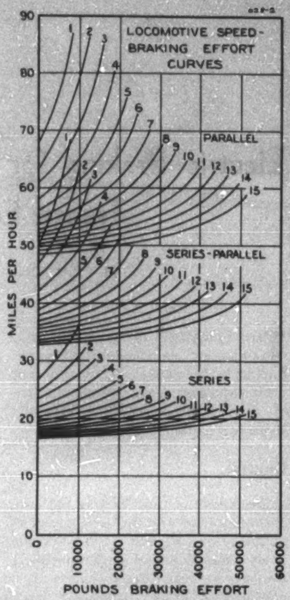

Figure 2. Speed-braking effort curves for a 185-ton six-motor electric passenger locomotive with regenerative braking

The regenerative electric brake can be used to advantage sometimes for light decelerations, the extent depending upon the grade, weight of train, and required decelerating rate. The bunching of slack and the smooth, quiet application of retarding effort is both beneficial to equipment and, in passenger service, a distinct advantage particularly to patrons in sleeping cars.

For retardation cycles approaching a full service or emergency application with air equipment, the regenerative brake, which is available on the locomotive alone, is entirely inadequate because of limitations in both driver weight and electrical capacity. For such decelerations, it is necessary to take advantage

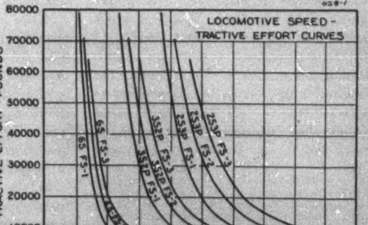

Figure 1. Speed-tractive effort curves for a 185-ton six-motor electric passenger locomotive

of the entire adhesive weight of the train. To appreciate this fully, consider the conditions involved in decelerating a 1,000-ton train from 80 miles per hour to standstill on level track.

At 80 miles per hour this train possesses a kinetic energy of approximately 428,000,000 foot-pounds, ignoring the rotational energy of wheels and axles. With a full service application of the air brakes, the deceleration will be at approximately two miles per hour per second average to standstill. On the basis of the train resistance cancelling the rotational energy during deceleration, which is essentially true, the average braking effort during the retardation cycle would be 182,000 pounds and the *average* dissipated horsepower 19,400. Taking the powerful locomotive represented by figures 1 and 2, this corresponds to a wheel-rail adhesion of 67 per cent and a braking horsepower 277 per cent of the maximum available.

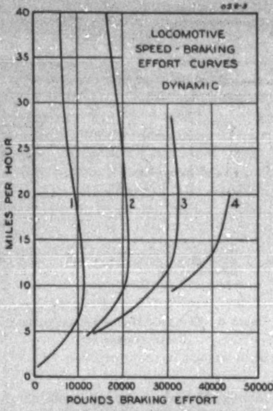

Figure 3. Speed-braking effort curves for a self-propelled locomotive with dynamic braking

In an emergency application at three miles per hour per second average deceleration, the average braking effort becomes 273,000 pounds, and the average dissipated horsepower, 29,100, corresponding to 101 per cent adhesion and 415 per cent of the maximum available braking horsepower of the locomotive.

These decelerations, which are not based on unusually severe conditions of either maximum speed or weight, give another insight into the magnitude of the energy to be dissipated and the resulting forces involved in the braking of high-speed modern trains.

Dynamic Electric Braking

Self-propelled locomotives using electric drive such as the Diesel-electric and steam-electric types can be equipped with dynamic braking which does not differ essentially from regenerative electric braking except that the energy must be dissipated in some manner on the locomotive rather than returned to the distribution system.

In some respects the dynamic electric brake has advantages the d-c regenerative brake does not have, in that the dynamic brake is flexible as to its characteristics. There are two types of braking characteristics, the holding brake and the stopping brake. The former has variable braking effort at constant speed and the latter, constant braking effort at variable speed. The regenerative electric brake on d-c equipment is of the holding-brake type inherently because it is tied down to constant voltage operation. The dynamic electric brake, however, can be designed to produce any desired characteristic by suitable arrangement of circuits, excitation, and external resistor values.

Figure 2 shows the characteristic curves of the regenerative electric brake on a d-c locomotive. The relatively flat shape means the locomotive can descend a variable profile at practically constant speed, automatically adapting itself to the changes in braking effort required.

Figure 3 shows a dynamic braking characteristic that is vertical and ideal for applying constant braking effort automatically throughout a deceleration. This can be approached with the regenerative brake in figure 2, but only by a skilled operator through constant manipulation of the braking controller, including shifting the motor combinations when required.

The 5,000-horsepower steam-electric locomotive built for the Union Pacific system, has a unique application of dynamic electric braking (figure 4). The unusual feature is the method of dissipation of electric energy generated during braking. The energy is delivered to a resistor consisting of sections of seamless stainless-steel tubing, through which water is circulated from the main power plant, the water being heated and in heavy braking duty partially converted into steam. The outlet of the resistor is connected to a separator from which hot water is returned to the hot well and steam to the main condenser.

The electric brake may be used alone or in conjunction with the air brakes. When used alone or with the air brake up to 48 pounds brake-cylinder pressure, the output is limited automatically to 4,800 kw to protect the motors from continuous overload down long mountain grades. From 48 to 100 pounds brake-cylinder pressure, indicating a stop is imminent and the demand for power limited in time, the output of the electric brake is graduated automatically up to 7,200 kw at the 100-pound cylinder pressure which corresponds to an emergency application on a modern electropneumatic straight air equipment as used on streamlined trains.

To date there have been no applications of dynamic braking to Diesel-electric locomotives in main-line service, although there is no reason why such braking cannot be applied. Experience with this type of braking on the Union Pacific steam-electric locomotive has emphasized the advantages of the system in retardations at constant speed or moderate decelerations, as well as safety at higher speeds in downgrade operation.

On a Diesel-electric the energy can be absorbed in resistors and the heat ultimately released to atmosphere through a system utilizing the engine cooling water and radiators. As alternatives, air-blast resistors might be used or resistors cooled by a separate fluid system employing a synthetic liquid of desirable characteristics with respect to specific

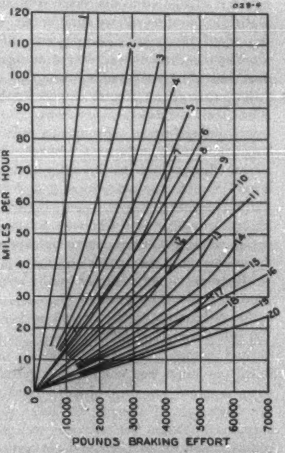

Figure 4. Speed-braking effort curves for 5,000-horsepower steam-electric locomotive (dynamic braking)

heat, boiling and freezing points, dielectric strength, inflammability, etc.

A miniature edition of such a dynamic brake was put in service recently on a 400-horsepower 20-ton Diesel-electric rack-rail locomotive for pushing a 12-ton observation car up and retarding it down the Manitou & Pikes Peak Railway which negotiates Pikes Peak in Colorado. The braking problem on this nine-mile road is severe because the average grade is 16 per cent and the maximum 25 per cent and absolute reliability is essential. The use of dynamic braking, in which power is dissipated in air-cooled resistors in the locomotive allows full control of the speed throughout the descent with the dynamic brake alone except at stopping when this brake fades out.

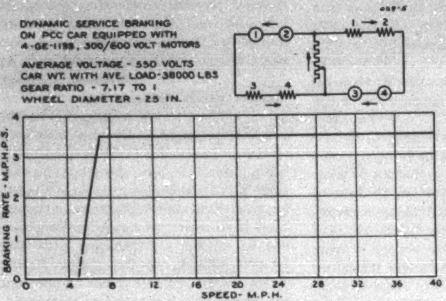

Figure 5. Dynamic service braking curve for PCC car

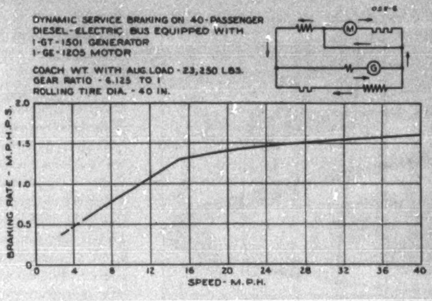

Figure 6. Dynamic service braking curve for Diesel-electric bus

The use of an electric brake would eliminate immediately all problems arising from sliding-friction phenomena, wheel slipping, and temperature changes in shoes. Because of its prompter action, fewer variable characteristics, and flexibility, it is effective more quickly than the air brake and can be used as well to assist the conventional brakes where high-speed demands exceed their capacity.

Electric braking so far has hardly been tried in the streamliner field where speed is the outstanding factor. Today we hear 100 miles per hour talked of freely as a normal nominal top speed just as 60 miles per hour was commonly referred to as a measuring stick of high speed a few years ago. Tomorrow 100 miles per hour maximum on the rails may well be common, and we shall be looking at higher speed levels again as indicative of the trend of ultramodern railroad equipment. Even today, the modern self-propelled locomotive with electric drive is geared for a peak speed of 120 miles per hour. As these top speeds increase, the braking problem becomes more and more acute. The kinetic energy in a train at 140 miles per hour is about double that in the same train at 100 miles per hour. As this trend to higher speeds develops, the necessity for new braking methods will become increasingly pronounced. The electric brake is a potential candidate to meet this necessity; in fact, it may open the door to ultrahigh-speed operation, for without suitable braking equipment, such operation is impractical.

Electric Braking in Urban Transit Service

In recent years, electric braking also has been used in urban transit service, which is quite different from heavy main-line railroad operation. Urban service involves relatively light vehicles, such as the Electric Railway Presidents' Conference Committee or "PCC" car, the single-motor trolley coach, and the gas-electric or Diesel-electric bus.

The PCC car utilizes entirely new propulsion equipment to obtain high rates of acceleration and braking, and resilient wheels which have rubber inserts between the wheel tire and hub to reduce wheel and rail noise. This design was a factor in the adoption of a braking system comprised of dynamic braking, magnetic track brakes, and conventional air-operated wheel brakes properly blended by a single brake pedal.

During a typical service stop of a PCC car weighing approximately 38,000 pounds with seated load and decelerating at $4^3/_4$ miles per hour per second, the traction motors contribute $3^1/_2$ miles per hour per second of dynamic braking and the magnetic track brakes $1^1/_4$ miles per hour per second deceleration from 40 miles per hour down to approximately 7 miles per hour. At this speed the dynamic braking fades out rapidly and the air brakes are automatically engaged to assist the magnetic track brakes in maintaining the braking rate to standstill. A feature of this braking sequence is the fact that the air brakes are called for at the same pedal by which dynamic braking is established, but the air brakes are held off automatically so long as the dynamic braking builds up to the desired rate. Thus, if the dynamic brakes should fail to build up for any reason, the air brakes would be available immediately to assist the magnetic track brakes in braking the car. Emergency stops at a rate of 7 to 8 miles per hour per second are obtained by increasing the dynamic braking to 4 miles per hour per second and the magnetic track brakes to 3 or 4 miles per hour per second deceleration, by depressing the brake pedal to its lowest position. An infinite number of intermediate braking rates between one mile per hour per second and emergency rate are available by depressing the brake pedal a corresponding amount.

Dynamic braking was selected as the primary brake on PCC cars because it is entirely independent of the trolley power in case of dewirement and because it requires relatively little control equipment in addition to the accelerating control, much of which is also used during the braking cycle. In addition, an emergency dynamic braking position is provided on the manually operated motor reverser. When the reverser is placed in this position, the traction motors are short-circuited to provide a very effective dynamic emergency brake if all other brakes should fail.

Each magnetic track brake consists of a large electromagnet mounted on a long, narrow shoe that is suspended parallel to and directly over the rail between the two wheels on each side of each truck. Four such magnet shoes are mounted on a car. When the electromagnet coil is energized, the shoe is pulled against the rail by its own magnetic action and the resultant friction of the brake shoe on the rail exerts a braking effort on the car. The relative effect of this retarding force is dependent upon the magnetic strength of the electromagnet coil, which is controlled by depressing the brake pedal. The track-brake coils are energized from a storage battery in order to make this brake also independent of the trolley power in case of dewirement. This brake is not usually cut off before standstill is reached, nor does it fade out at low speeds. However, provision is made to release it when the brake pedal is latched down to continuously engage the air brakes for prolonged parking, in order to eliminate needless drain of energy from the battery.

The use of electric braking as the primary brake on PCC cars eliminated the problem of using air-operated wheel brakes for service stops at high rates of deceleration with the attendant dissipation of large amounts of heat in the car wheels. In fact, some of the newest cars use a spring-applied clasp brake to supplement the electric brakes at low speeds and for parking, and thus eliminate entirely the use of air-operated wheel brakes. The same principles of electric braking utilized so successfully on PCC cars have recently been applied and have materially simplified the problems of equalization and prompt response of braking effort on all axles of a multisection subway-elevated train (figures 5 and 6).

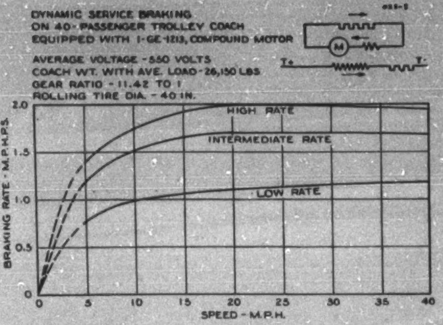

Figure 8. Dynamic service braking curve for single-compound-motor trolley coach

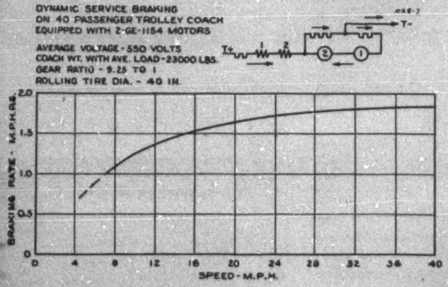

Figure 7. Dynamic service braking curve for two-motor trolley coach

Electrically propelled automotive-type urban transit vehicles, particularly the trolley coach, have presented a more serious braking problem than mechanically driven busses, due to the higher rates of acceleration and deceleration demanded from electric drive and due to the free-wheeling characteristic of electric drive in which there is no engine compression to assist the wheel brakes. The use of higher ratios (power-to-weight) with electric drive in the last few years, coupled with the fact that the physical size of wheel brakes has reached practical limits, led to the introduction of the compound-wound trolley-coach motor and automatic control equipment designed especially for regenerative and dynamic braking and speed control, and to the development of new series-wound motors and control equipments designed especially for dynamic braking.

It is characteristic of the compound-wound motor that, if the shunt-field strength is increased while motoring at a given speed, the motor, driven by the inertia of the load, begins to regenerate and exert a braking effort on the load until a lower balancing speed corresponding to the stronger shunt-field strength is attained, at the same time returning power to the line.

This principle of shunt-field control is utilized with the modern compound-wound trolley-coach motor to provide a number of balancing speeds over the upper half of the vehicle's speed range, with regenerative braking automatically obtained during deceleration from one balancing speed to another or while holding the coach at any one of these balancing speeds during descent of a grade.

The compound-wound motor with the shunt-field winding separately excited from the trolley has ideal characteristics for dynamic braking (figure 8). By opposing the series and shunt fields, a differential action is obtained which prevents the imposition of excessive strains on the rear axle or gearing. Dynamic braking and air braking are applied by a single pedal. Maximum dynamic braking is obtained at approximately one-half pedal travel; and at the same time a small amount of air is admitted to the brake cylinders. Further movement of the pedal maintains maximum dynamic braking until approximately 85 per cent of the air braking is being applied. Dynamic braking is then reduced to the minimum and 100 per cent air braking applied. This operating sequence permits maximum utilization of dynamic braking without applying excessive braking to the rear wheels during an emergency stop.

Paralleling the development of the compound-wound motor is a new series-wound motor for trolley coaches which has been designed especially for dynamic braking. This development was made possible largely by the introduction of a practical, automatic acceleration control which has been adapted for use during dynamic braking automatically to commutate the braking resistance in series with the motor to hold the desired acceleration rate. An infinite number of intermediate braking rates between the minimum and the maximum are available merely by depressing the brake pedal a corresponding amount. The same brake pedal also controls the air brakes, which may be applied simultaneously with the dynamic braking to provide higher braking rates than are possible with dynamic braking alone, or to assume the braking duty at low speeds below which the dynamic braking automatically fades out.

The past year or two has also advanced the application of electric service braking to existing cars and trolley coaches of older design. The scheme most universally employed for this purpose consists essentially of connecting load resistors across the motor armatures and connecting the armatures in series with the motor fields in such a manner that, when the fields are excited from the trolley, the generated voltage of the motor armatures opposes the applied trolley voltage. This connection results in a differential action which automatically increases the field strength as the armature voltage decreases with vehicle speed in order to maintain a substantially constant rate of deceleration over a wide vehicle speed range. This form of dynamic braking is advantageously applied to older-type cars and trolley coaches having nonautomatic control and excess motor capacity.

Dynamic electric holding brakes were practically standard equipment on the earliest gas-electric busses with dual-motor drive, as these busses had only two-wheel mechanical brakes. However, the holding brake as applied to these vehicles was not designed to assist the wheel brakes during a service stop, but was intended merely for holding the bus at a predetermined speed on some specific grade. Since the introduction of the modern single-motor equipment for gas-electric or Diesel-electric busses was subsequent to the universal adoption of four-wheel air brakes for railless urban vehicles, the dynamic holding brake has been retained only where unusually long or severe grades are encountered, complete reliance being placed on the air brakes in most cases.

However, increased size of busses and installation of larger engines, as well as a demand for higher schedule speeds, have recently promoted the development of a fully automatic electric service brake to supplement the air brakes on modern gas-electric and Diesel-electric busses. This form of dynamic braking utilizes power obtained from the main generator to excite the fields of the traction motor, the armature of which has a braking resistor connected across it. The amount of motor field excitation taken from the generator is automatically regulated to maintain an approximately constant braking rate over the entire speed range of the bus. The electric braking may be applied whenever the engine throttle is returned to its idling position, in order to simulate the retardation effect of engine compression, or it may be applied by initial movement of the brake pedal. It is automatically cut off at low bus speeds. Test installations indicate a material improvement in the life and maintenance of the wheel brakes on Diesel-electric busses equipped with this type of dynamic service brake.

In all cases where modern electric service braking has been applied to supplement wheel brakes, the life and maintenance of the wheel brakes on electrically propelled vehicles has been equal to or better than the life and maintenance of wheel brakes on similar mechanically-driven equipment, in spite of the higher schedule speeds and higher deceleration rates normally used on electrically-propelled vehicles. The use of higher rates of electric braking on trolley coaches and on electrically-driven busses is not recommended, however, because the braking must all be done on the rear axle and would thus cause the rear wheels to assume a very major portion of the total braking duty. This would cause the vehicle braking characteristics to approach the obsolete performance standards of two-wheel brakes which, for obvious reasons, the industry saw fit to discard years ago.

Conclusions

Operating experience in recent years clearly establishes the importance of electric braking as a supplementary system both in heavy railroad operation and for urban transit. A continued demand for higher speeds is to be expected and the limitations of mechanical brakes without excessive weights and costs have already been indicated. Thus, electric braking may easily augment the increasingly drastic requirements of electric modern motive power.

Of the several types of electric brakes discussed, regenerative braking is available for electrified railroads or trolley-coach lines while dynamic or rheostatic brakes, eddy-current brakes, and magnetic track brakes can be applied as well to self-propelled vehicles. Under heavy braking conditions, regeneration returns appreciable amounts of power to the line. Exceptionally favorable conditions such as a three per cent continuous grade may return regenerated power amounting to as much as 70 per cent of that used for motoring.

Dynamic braking delivers electrical energy to resistors where it is dissipated in the form of heat. Eddy-current brakes accomplish the same result by use of a self-contained independent device attached to the motor frame and coupled to the armature shaft. Eddy-current brakes are operating successfully on rapid transit equipment but in general the extra cost discourages their general use. Dynamic brakes on the other hand make use of the existing motor as a generator and usually the resistor equipment normally used for acceleration.

Magnetic track brakes while simple and comparatively inexpensive are only justified where rapid and frequent decelerations are absolutely essential to the performance of a given schedule. Present applications are limited to PCC cars and rapid transit trains.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Network Coupling by Means of Static Electronic Frequency Changers

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FELLOW AIEE

DURING the last few years several attempts have been made, especially by some foreign railways, to find ways and means to feed their 11,000-volt $16^{2}/_{3}$-cycle railway trolley system directly from a commercial 50-cycle power source by means of a simple device, or then to have the locomotives equipped with such apparatus which would permit using power at the latter frequency fed directly to the trolley line. In other words, it was felt that many advantages could be gained if the trolley line could be fed, directly, or over a simple converter, from any commercial power network which would be adjacent to the railroad's right of way. This desire led to the development of the 50-cycle series commutator motor by the Siemens Schuckert Company, a 50-cycle converter cascade motor by the Krupp Company, and to the converter locomotive by Von Kando. It also led to the design by several companies in Europe of a locomotive with a built-in mercury-arc rectifier for converting the 50-cycle power fed directly to the trolley line into direct current suitable for standard railway motors.

Furthermore, studies were made using a commutatorless motor with a mercury-arc rectifier. The development of this motor, having no commutator but using a rectifier for commutation and able to operate on commercial frequency, was, however, abandoned in its early stages, as far as its use on a locomotive went. However, it may be said that since single-anode tank rectifiers of large capacity are now available, a more economical connection for the commutatorless motor could be employed than was considered when the first investigations were made.

The operation of these various systems proved successful from a technical point of view, but they were found less advantageous from an economical and maintenance standpoint.

At this time, as briefly referred to above, still another system permitting the use of power of conventional frequency was under careful consideration, namely, using a rectifier on the locomotive converting 50-cycle power fed directly to the trolley system into direct current and supplying it to standard railway motors. Finally four locomotives of this design were built. Three of them are operating on the Hoellental railroad system. This system was originally projected for a trolley voltage of 15 kv but had to be raised to 20 kv in order to compensate for the high inductive drop at 50 cycles. The locomotives were equipped with standard series motors, a rectifier and transformer, and devices for voltage control, etc. The 50-cycle power supplied to the trolley system from a conveniently available power line is stepped down and converted into direct current by the transformer and rectifier on the locomotive.

However, before the studies undertaken by four companies in connection with these locomotives were very far advanced, it was found that the grid control, which at first thought appeared extremely simple, became quite involved. One company even abandoned the idea of controlling the voltage by means of grids and introduced tap changing under load equipment for regulating the anode voltage of the rectifier, and thus in turn the direct voltage supplied to the motor.

Simultaneously with this development, attempts were made to develop a static frequency changer using grid-controlled mercury-arc rectifier-inverters (electronic rectifier-inverters). This method would also make it possible to take power from any commercial network, convert it and feed the trolley system with a frequency suitable for a-c railway motors.[1–6] Such devices would be static and some of them would permit flexible coupling between a three-phase 60-cycle, and, for instance, single-phase 25-cycle trolley systems. Tests[6] with such frequency changers were started over six years ago, and after extensive experiments with sets of a capacity of 500 to 1,000 kw, a commercial installation was finally erected near Basel, Switzerland, feeding power into the $16^{2}/_{3}$-cycle trolley system of the Wiesenthal Railroad, Germany, from a 50-cycle network.[8,17,18]

The object of this paper is to discuss such methods of frequency changing using electronic rectifier-inverters which would make it possible to transform three-phase power of 60 or 50 cycles into 25 cycles, the frequency commonly used by the railroads in the United States. The other systems as used today in Europe are briefly referred to and discussed only where they have a bearing on the systems suitable for obtaining 25-cycle power.

Rotating Versus Static Frequency Changers

The phenomena taking place during conversion of electric energy from one frequency to another will be discussed briefly in order that the arguments in favor of methods using electronic converters can be easily followed, and so that some of the theoretical considerations put forward in several articles referred to below can be fully appreciated.

We may recall the fact that the energy furnished by a three-phase network, with a symmetrical three-phase voltage system, normally supplies a constant power flow in spite of the fact that in each individual phase the voltage and current change according to a sine wave. Power from a single-phase network, however, pulsates with twice the frequency of the induced voltage and current, which, as is well known, is an inherent characteristic of such systems. The instantaneous power demands of the two systems are not equal. Therefore, the supply of power from a network of commercial frequency to a single-phase railway system, necessitating conversion of energy from 60 to 25 cycles, means the electrical coupling of two power systems, each of which has quite a different characteristic of its natural flow of energy.[9]

Let us first analyze the power relation in case a three-phase single-phase motor generator set is used for coupling such systems. The power absorbed by the motor from the three-phase 60-cycle system shall be assumed to be constant and is illustrated by T in figure 1*b*. T is the summation of the power furnished by each phase X, Y, W, of the three-phase system. The power demand of the single-phase network on the three-phase network is shown in figure 1 by curve S, which in case of a mechanical frequency changer will always be active. In other words, in such a frequency changer no reactive power can be transmitted from one to the other system, and the same condition holds true

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1. For all numbered references, see list at end of paper.

for a circuit as shown in figure 2. From figure 1*a* it can be seen that the single-phase energy *S* pulsates about a mean value *T* with a frequency equal to twice that of the frequency of the current *i* and voltage *e* of this system. The mean power of the single-phase system must be equal to *T* as the loads of the two machines average the same over one cycle, but, as said above, are not the same at each instant of the cycle. It will therefore be seen that during certain intervals (*a*), for instance, single-phase energy equal to the horizontal hatched area will have to be supplied from a source other than the three-phase system, while during the intervals (*b*), the same amount of energy has to be absorbed and stored in some way or other in order that the three-phase energy supply may be constant (equal to *T*). In other words an energy reservoir will be necessary to balance the available and required power. Curve S_1 shows, for a few cycles, the magnitude of energy which has to be taken care of at each instant by certain means (accumulators) capable of absorbing and freeing energy.

In the case of a motor generator set the rotating masses will automatically take care of the excess energy during period (*b*) and release it during period (*a*). However, it must be realized that this will only take place if the speed varies or deviates from the mean value *N* as shown in curve *n* (figure 1*a*). In other words, during the interval (*a*), the speed of the rotating masses is being advanced, while during interval (*b*) the speed will decrease over the mean value *N*.

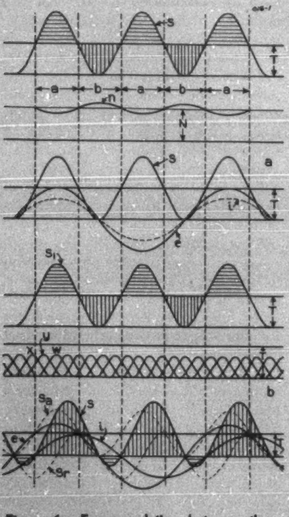


Figure 1. Energy relation between three-phase, single-phase systems of frequency converters

Therefore, a rotating frequency changer (motor generator set) is able, with its normally inherent rotating masses, to take care of the surplus, or lack, of energy as compared to the mean value *T*, since the rotating masses permit storing and supplying energy. The three-phase motor will, therefore, draw constant energy from the 60-cycle network in spite of the pulsating character of the single-phase load, but the speed will fluctuate slightly with the frequency of the pulsating single-phase load.

The power supply for conversion of three-phase to single-phase current by means of an electrically controlled rectifier-inverter is illustrated in figure 1*b*. The power demand *S* oscillates at twice the single-phase frequency as above, *T* being the mean value and S_a the active and S_r the reactive component of power demand from the single-phase supply. Both were taken to be equal, representing a power factor of approximately 0.7. In the case of a frequency changer using an electrically controlled rectifier-inverter, no inherent means are available to permit storing and releasing of power at the frequency impressed by the single-phase load characteristic. Therefore, with such equipment the power oscillations are transferred to the three-phase network, which will be loaded with currents departing from sine waves and of frequencies foreign to the network frequency. This will result in an increase in reactive power, harmonics, and a negative component in the three-phase current system which will in turn affect the efficiency factor.

The above condition can only be corrected if means are used to take care of the differences between the instantaneous power demands of the systems as is done inherently in the case of a mechanical frequency changer. Such energy-storing equipment can be built up using a battery of capacitors and reactors (a resonant circuit). However, due to the fact that the intermediate d-c circuit of such an electronic converter, see figures 3 and 4, has a superimposed alternating current of 360 cycles (if a 6-phase electronic converter is used) and 720 cycles (if 12 phases are used), the smoothing or storing equipment may have to be of fairly high capacity. The use of capacitors and reactors forming a resonant circuit, even if of large capacity, introduces very little additional losses, as the energy can be kept oscillating between capacitors and reactors, consuming very little power. In case it is found advisable that the single-phase power energy oscillations may partly be transmitted back to the three-phase system, in other words, if *T* may vary, then the capacity for the storing equipment (capacitors and reactors) becomes relatively small. However, due to the recent developments of static capacitors for power-factor-improving equipment, the cost of such storing equipment is no longer prohibitive, and enough capacity may be provided to obtain a constant energy flow from the three-phase network as shown by *T*.

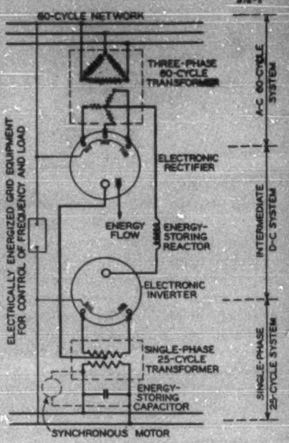


Figure 2. Fundamental circuit diagram for an electronic frequency converter with intermediate d-c step

Electronic Frequency Changer With Intermediate D-C Step

Flexible Indirect Coupling Between Systems

The simplest method for effecting a flexible coupling of a-c systems of different frequencies by means of electronic rectifiers and inverters is shown in figure 2. This system consists of a rectifier, a reactor, and an inverter. Three functions are necessary: First, the a-c power of 60 cycles is rectified into d-c power; second, the d-c power is inverted into 25-cycle single-phase power, and third, the difference between the instantaneous power demand of the single-phase system and the three-phase supply has to be balanced by an energy accumulator, which may consist of a reactor and a capacitor. The reactor

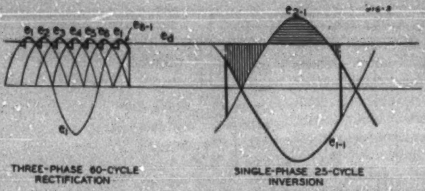


Figure 3. Relationship between single-phase voltage and intermediate direct voltage obtained from six-phase system

e_1, e_2, e_3, e_4, e_5, e_6, e_1, etc., represent phase voltages of the 60-cycle six-phase system. e_{1-1} and e_{2-1} represent voltage waves of the 25-cycle single-phase full-wave system. e_d represents the intermediate direct voltage of the rectifier inverter

is inserted between the rectifier and inverter, and takes care of the differences of the direct voltages e_{6-1} and e_{2-1}, figure 3. The voltages e_{6-1} and e_{2-1} refer to a six-phase rectifier system and a single-phase inverter system. For further data on rectifier and inverter operation, see bibliography.[13,14,26,28] The above arrangement does not allow reactive power to be fed back to the three-phase system, and only active energy can be transferred from the three-phase to the single-phase network. The reactive power would therefore have to be taken care of by a static or a synchronous condenser connected to the single-phase system.

The first two steps, namely, converting power from alternating current to direct current and from direct current to alternating current, have been done separately in many installations during the last few years and a great deal of experience was recently gained during extensive field as well as factory tests in connection with inversion of direct current to alternating current by means of grid-controlled rectifiers.[11,21,23] These tests have demonstrated that the flexibility between two a-c networks is fully maintained and no instability resulted no matter how large and sudden a change in frequency took place.

It may therefore be realized that at this time a frequency-changing set of great reliability can be built by utilizing the experience gained in some of the high-voltage rectifier and inverter installations briefly referred to later on.

Electronic Frequency Changer Without Intermediate D-C Step

(a). Flexible or Rigid Direct Coupling of Systems

The converter referred to above could be built up by using either single-anode or multianode tank rectifier and inverter; the one discussed below necessitates the single-anode tank arrangement. Figure 4 shows the basic circuit diagram of such equipment, consisting of a transformer and 12 single-anode grid-controlled rectifier-inverters; one set having their cathodes and the other set their anodes connected to one side of the single-phase system. Each pair of tanks connected to the same secondary winding constitutes a switch, in other words they permit current to flow in either direction. Let us assume that the grids of tubes R_1 permit current to flow while all other anodes are blocked, then the transformer winding 1 is connected to the single-phase system. After a certain period this set of tubes is blocked and set R_2 is released, thus phase 2 of the transformer may supply current. After another period of time the next phase, 3, is brought into play, and then 4, 5, etc. Figure 5 shows the voltage of each phase, and below the arc current which is commutated from each anode to the anode of the succeeding phase as the impressed alternating voltage of this phase becomes more positive than that of the phase carrying the arc. It would have to be arranged, therefore, that the grid-control equipment permits the flow of current according to the time interval t_1 to t_{14}. This results in sections of voltages e_1 to e_6 being made available and joined to the heavy-line curve which becomes the voltage e_s of the single-phase system. The flow of current through the individual tubes can be seen from the second curve of figure 5 for the case where the single-phase voltage and current are in phase.

The frequency ratio between e_1 to e_6 and e_s as shown in figure 5, upper curve, is about 3:1. However, as can be seen from the voltage curves below, any frequency ratio may be obtained, as any number of sections e_1 to e_6, or e_1 to e_6+e_1, or e_1 to $e_6+e_1+e_2$, etc., of the transformer phase voltages can be built up to produce the

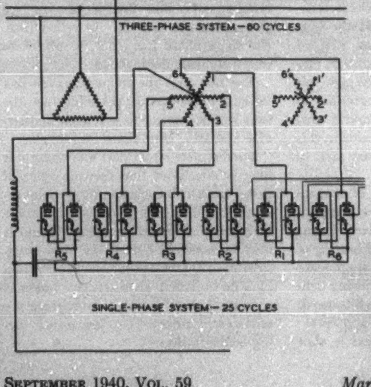


Figure 4 (left). Fundamental circuit diagram of an electronic frequency converter

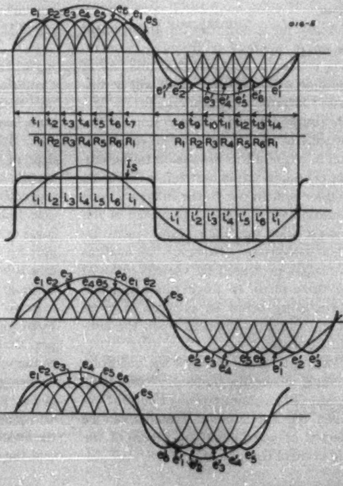


Figure 5 (right). Formation of single-phase voltage and current waves for different frequency ratios in an electronic frequency converter

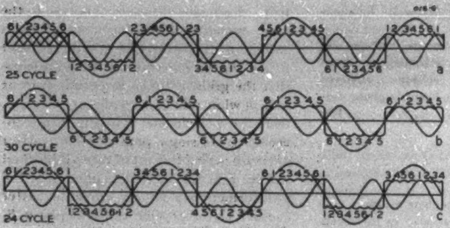


Figure 6. Diagram showing the firing of anodes for various frequency ratios in a flexible frequency converter

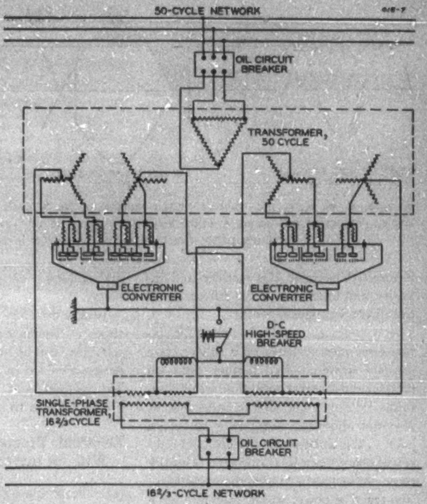


Figure 7. Fundamental circuit diagram for a rigid frequency converter

desired single-phase voltage e_s. A further consideration of these curves will show also that a change in the frequency of the three-phase voltage still permits obtaining a constant single-phase frequency. The flexibility of this type of frequency changer with electronic equipment will be analyzed still further below using figure 6.

As is well known the firing of the anodes can be controlled by means of grids in such rectifier tubes and therefore the voltage curve e_s in figure 5 could be made to resemble more closely a sine wave by retarding or accelerating the firing of some of the anodes.

The scheme[3,4] of employing a transformer with a modified secondary winding (see figure 4, system 1′, 2′, 3′, 4′, 5′, 6′) cannot be used unless the ratio of the frequencies is for instance 3:1, as is the case in Europe between the conventional 50-cycle power and the $16^2/_3$-cycle railway systems. Furthermore, this permits only a rigid coupling between the two systems. For these reasons no further consideration will be given to these methods in this paper.

Analyzing still further the arrangement shown in figure 4 of frequency conversion, it is quite apparent that this type of equipment and others which will be discussed later do provide a completely flexible coupling between two systems. For the sake of brevity no detailed consideration is given in this discussion to the subject of commutation between sections of a rectifier system nor to the influence upon commutation in case a potential is impressed upon the single-phase system by other converters. However, an analysis of conditions caused by various ratios of the three-phase to single-phase frequencies is given below for purpose of determining the duty imposed upon the individual anodes.

Part *a* of figure 6 shows the condition existing when the frequency ratio is 60 to 25. The commutation of current between the two rectifier systems can be effected at any instant by means of the grid-control equipment.

Part *b* illustrates the condition when the frequency ratio is 60 to 30. Here we see that each of the six anodes fires only once during the active period of either system; further, we also see that the same anodes bear the entire burden of each commutation, and each of the others always carried the current during the same part of the active cycle of its system. From this it is obvious that the duty on the anodes within one of the systems cannot be equally distributed, and the anode subjected to the most severe duty will govern the rating of the rectifier.

Part *c* indicates the relationship existing when the frequency ratio is 60 to 24. Conditions in this instance are more favorable than for the ratio of 60 to 30, but it does have some undesirable characteristics. Investigation shows that the burden of commutation is now divided equally among four of the six anodes employed in each system, and although this does limit the capacity of the rectifier to some value less than it would have if used only for rectification, the reduction in this case is less than for the frequency ratio of 60 to 30.

From the above discussion of the unusual frequency ratios it may at first appear that the ratio of 60 to 25 is very near to the undesirable condition of loading of anodes encountered when it approaches 60 to 24. However, before drawing definite conclusions it might be well to recall the reason for developing such apparatus; that the coupling be entirely flexible, that is, that the frequency ratio may be a variable. With this in mind let us consider what determines the frequency of the two systems; in the case of the three-phase system it is without exception dependent upon the speed of the prime movers driving the generators, while the single-phase system may or may not include rotating equipment. The three-phase systems are well known to maintain a constant frequency; on the other hand, traction systems do allow relatively large variations, the reason being that if compared to the supply capacity, great power demand fluctuation occurs. In the case where no rotating equipment is supplying energy to the single-phase system, its frequency will be a function not only of the active power requirements, but also of the reactive power demand as well. In either case, the single-phase frequency is subject to relatively wide fluctuations.

The fact that the frequency ratio is a variable eliminates the possibility that the unit will be subject to sustained undesirable operating conditions.

Furthermore, from part *b* of figure 6 it may be concluded that the conversion of power from a 50-cycle to a $16^2/_3$-cycle system (3:1) does not provide a favorable utilization of the rectifier-inverter and it may be concluded from the above consideration that more favorable operation may be obtained when 60-cycle power is converted into 25-cycle power, since the anodes are subjected to a lower duty during commutation.

(b). RIGID INDIRECT COUPLING OF SYSTEMS

Figure 7 shows the diagram of connections of such a type of frequency changer with the electronic converters and the three-phase and single-phase transformers. This system has, however, a serious inherent disadvantage—namely, the coupling is not flexible and therefore any change in frequency on one system will automatically affect the frequency of the other. However, in spite of this drawback such equipment has been installed to supply a small railroad with $16^{2}/_{3}$-cycle current. The set is rated 3,600 kva continuous load, 2,520 kw (power factor 0.7) 4,000 kva one-half hour, and 6,000 kva, one minute, and has been in commercial operation since December 1936. The unit furnished the entire load for over a year for this branch railroad which was up to then supplied by means of two motor generator sets, each rated 2,100 kva. Therefore, these two motor generator sets were used only for standby units, but it was contemplated to have one working in parallel with the static frequency changer as soon as this method of conversion has proved satisfactory and could then be reconnected in such a way that it could work in parallel with any other converter, whether of the mechanical or another electronic type. The original setup as indicated above was such that the frequency of the single-phase output was a function of the frequency of the three-phase supply system. In other words, the coupling of this system as originally used was not flexible and parallel operation of different units could not be accomplished without using a connection similar to the one which will be explained below. The frequency changer was supplied by a 45-kv 50-cycle transmission line, and therefore the rectifier transformer was provided with a 45-kv delta primary winding. The voltage of the intermediate d-c system of the frequency changer was chosen at 2,000 volts and does not depend upon the voltage of either the three-phase or the single-phase network.[8,17,18,19,20]

The grid control of this unit is such that a slight inaccuracy in the adjustment of the firing period may make the unit completely inoperative. Furthermore, this method of frequency changing is very limited, as stated before, as it does not provide a flexible coupling, produces an unequal loading of the anodes, and necessitates a tank with an abnormal number of anodes. The advantages which may be stressed are long firing time of the anodes, and the possibility of furnishing wattless power from one network to the other.

Two things, however, were definitely demonstrated by the installation of this type of static frequency changer, namely, that it requires less space and has less weight, both of which reduce the cost of the building and foundations. Furthermore, the efficiency at light load was considerably higher than the one which could be obtained with a mechanical converter.

It may be mentioned that right from the beginning considerable difficulties were experienced due to the fact that the communication systems were exposed to the trolley, as well as feeder system, both of which showed considerable current and voltage wave distortion from the rectifier-inverter. This situation was corrected by means of filters. Further on will be outlined some schemes and methods which may be employed in order that the inductive co-ordination of power and telephone systems may be obtained for large installations of static frequency changers without using filters.

(c). FLEXIBLE INDIRECT COUPLING BETWEEN SYSTEMS

The static frequency changer discussed below has recently been perfected and is based on a somewhat different principle than the arrangements described above. It attracted considerable attention among European engineers, especially due to the fact that it permits flexible coupling of two a-c systems of any frequency, without appreciable distortion of the current and voltage waves. Such a frequency changer was recently put in commercial operation for coupling an a-c three-phase 50-cycle system with a single-phase $16^{2}/_{3}$-cycle trolley system, converting as much as 3,000 kw. A similar electronic converter has been placed in operation, of a capacity of 1,600 kw, coupling a 50-cycle three-phase system with another three-phase system which has a frequency of 42 cycles. Both installations have already given several thousand hours of satisfactory service.[8,10,24]

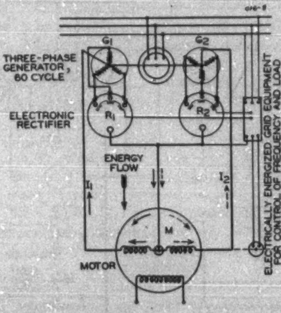

G1
G2
THREE-PHASE GENERATOR, 60 CYCLE
ELECTRONIC RECTIFIER
R1
R2
ENERGY FLOW
I1
I2
M
MOTOR
ELECTRICALLY ENERGIZED GRID EQUIPMENT FOR CONTROL OF FREQUENCY AND LOAD

Figure 8. Simplified circuit diagram for demonstrating flexibility of electronic frequency converter

Let us therefore describe this system more fully by means of a simplified circuit, figure 8. This circuit may have no practical value, but will permit ready analysis of all the phenomena with which we shall be concerned.

The scheme represented in figure 8 comprises two a-c synchronous generators, G_1 and G_2, feeding a two-winding d-c motor (M) through a connection without appreciable resistance and through rectifier tubes R_1, R_2, each tube being equipped with control grids. Neglecting the voltage drop in the connections and in the rectifier, the generator, let us say, induces a voltage E somewhat higher than the back electromotive force of the motor. Energy will then flow from the generator to the motor if the grids are energized so that the anodes of the rectifiers are permitted to carry current. Let us assume that one electric valve, R_2, is blocked and therefore current will flow only through the left-hand winding of the motor, bringing it up to speed in the direction indicated by the heavy arrow. The motor will operate in this direction as long as current I_1 produced by generator G_1 can flow through R_1. At a certain instant of time the grids of rectifier valve R_1 are negatively energized by the auxiliary grid equipment, current I_1 is prevented from flowing, and the motor comes to a stop, see figure 8. However, at the same time the grids of R_2 are de-energized and since generator G_2 impresses a potential E_2, current I_2 will flow through the right-hand winding of motor M and bring this motor up to speed; however, in the opposite direction. It can therefore be seen that by impressing a negative potential on grids of rectifier R_1 and then on grids of rectifier R_2, this motor will reverse at a frequency equal to the frequency of the voltage applied to the grids of these two rectifier valves. The frequency of the reversal of motor M is independent of the frequency of generators G_1 and G_2—in other words, independent of the frequency of the voltage applied to the anodes of rectifiers R_1 and R_2. Further, it can be seen that the frequency of reversal of motor M is independent of the frequencies of generators G_1 and G_2 whether these generator frequencies are the same or not.

Let us now apply the above reasoning by following the circuit of figure 9, and we shall find that with a simple control device for energizing the grids, power of a 60-cycle frequency can be converted into single-phase energy of 25-cycle frequency if the grids of the anodes of the left-hand unit and those of the right-hand unit are energized consecutively with potentials of opposite polarity. The reversal frequency of the grid potentials would have to be equal to the frequency of the single-phase output, namely, 25 cycles. In this arrangement, however, instead of a reversal of direction of rotation, a reversal of voltage on the primary of the single-phase transformer takes place, which is at the rate of 25 cycles if the grids are controlled from the single-phase 25-cycle network.

A more specific arrangement of the grid control is shown on the left-hand side of figure 9. The potential of the grids is controlled by three mechanical contacting devices m, m_1, and m_2, and is supplied by a battery. The devices m_1 and m_2 which are driven in synchronism with the frequency of the three-phase system therefore energize the grids at the same frequency as the anodes of each group, S_1 and S_2, of the rectifier-inverter. However, contactor m supplies the battery potential to each of the contactors m_1 and m_2, or, in turn, to each group of anodes S_1 and S_2, according to the speed of motor M_2. This motor, as can be seen, is driven from the 25-cycle system and, if of the synchronous type, will energize the grids of each group negatively through the battery and contactor segment m once during each revolution or, if a two-pole motor is used, at the frequency of 25 cycles. If the frequency of the single-phase system changes, then the speed of rotation of contactor m changes, and accordingly the interval during which the brush is in contact with the segment. The groups S_1 and S_2 may therefore furnish current for shorter or longer intervals which will be reflected in the frequency of the energy transferred to the single-phase system. Therefore, the frequency of the energy transferred to the primary winding of the single-phase transformer from the three-phase network follows the frequency of the impulses of the contactor m, and since this device is driven by a synchronous motor M_2, energized by the single-phase system, it follows the frequency changes of this system. A completely flexible coupling of the two systems is therefore effected.

The same results can be obtained by using electronic instead of mechanical control equipment, see figure 10. Comparing this with the mechanical control shown in figure 9, the circuit of the electronic control can easily be analyzed. A 12-anode rectifier with electrically controlled grids is connected to the secondary windings of a 60-cycle transformer. The energy-storing equipment comprises a reactor and a battery of capacitors. The control devices consist of grid-controlled tubes and 60- and 25-cycle auxiliary transformers.

Relative Advantages and Disadvantages of Different Methods of Frequency Changing

The discussion below will deal especially with the system shown in figure 10, or similar ones, and only touch lightly on other methods of frequency changing.

From the foregoing it could be seen that the system as shown in figure 2 with an intermediate d-c step does not make it possible for the three-phase network to take care of the reactive power of the single-phase trolley system. Synchronous or static condensers would have to be connected to the trolley system, see figure 2, or then the reactive power would have to be furnished by rotating converters connected in parallel with the electronic converters. Furthermore, in order to obtain successful operation, a reactor has to be connected into the d-c link for reasons given in the second section of this paper. This method, however, permits transferring energy in either direction and may show some additional advantages after more experience has been obtained with d-c transmission of power. Furthermore, this method provides a completely flexible link between the two systems, has a high conversion efficiency (since a high intermediate direct voltage can be chosen making the arc losses negligible), and possesses effective regulation and protective features by means of energized grids.

Such systems for conversion of three-phase power into direct current and back into another three-phase system have been operating in the United States and Europe for many years. These systems make use of intermediate direct voltages of 3,000, 15,000, and up to 48,000 volts, respectively.[11,21,22,27] Although these systems are three-phase to three-phase conversions, still they deserve careful study when considering new frequency-changing equipment for railroad electrification.

Figure 9. Circuit diagram of electronic frequency converter showing mechanical grid-control equipment

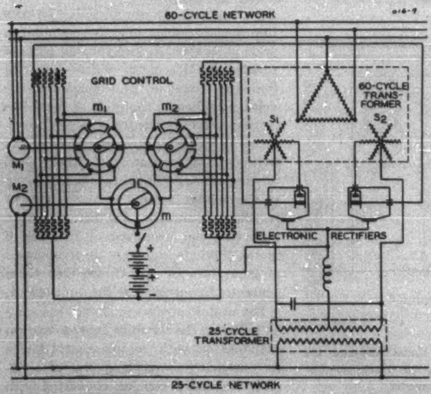
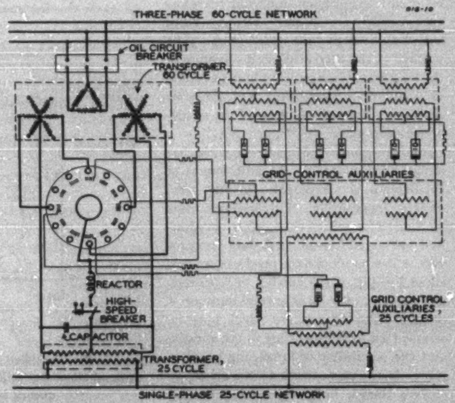


Figure 10. Circuit diagram of electronic frequency converter showing static electronic grid-control equipment



The direct-coupled system (no intermediate step), although it can be made flexible, did not receive much attention in the initial tests nor was such equipment installed on some of the European railway systems. The output voltage for the rectifiers is determined by the trolley voltage and will therefore be fairly high for all existing electrified $16^2/_3$- or 25-cycle lines, and the anodes of this rectifier as well as of the inverter are subjected to a high commutation voltage.

The indirect-coupled systems permit the selection of the most suitable rectifier-inverter voltage in order to utilize the anodes to the fullest degree as the single-phase transformer ratio can be chosen accordingly. Both methods, shown in figures 7 and 10, have been utilized and are therefore given further consideration below. The investigations were made based on a set for 5,000 kw, a power factor of 70 per cent, and conversion of power from 60 to 25 cycles at conventional alternating voltages. The rating of 5,000 kw for the electronic converter sets was chosen in order to be in line with the rating of the present single-phase feeder transformers as used on one of the main electrified lines. Both sets are static and permit the same ease of starting, and may therefore be applied in the same way in connection with electrified lines.

The rectifier-inverter tanks consist of several anodes equipped with energized grids and their necessary control devices. The first method of this kind, see figure 7, necessitates an abnormal number of anodes per tank and an exceedingly accurate control device. Furthermore, the transformer connections are quite abnormal and require a relatively complicated design. The other indirect-coupled system makes use of normal rectifier-inverter tanks with grids, a 60-cycle three-phase transformer with double six-phase secondary, and interphase transformers, both being of the same types as used in standard rectifier installations, and a 25-cycle transformer, single-phase with mid tap.

It can therefore be realized that this last-mentioned system deserves careful consideration. However, some of the main characteristics of both these indirect-coupled systems will be discussed below. Since most of these systems are flexible they have to be compared with motor generator sets which permit elastic coupling of two networks.

Voltage Regulation

From figure 11*a* it can be seen that the voltage regulation is similar to the regulation of a standard transformer. Curve 3 shows the regulation of a transformer, and curve 1 that of a 5,000-kw frequency changer, while curve 2 gives the voltage regulation of a motor generator set. It may be pointed out, however, that should this voltage drop, of about seven per cent at full load, be objectionable, a curve similar to number 2 could be obtained by means of additional grid-control devices, as shown in figure 10. This voltage regulation is naturally due to the drop in both of the transformers, in the reactors, and the arc drop of the rectifier-inverter. As pointed out above, the voltage of the intermediate system can be chosen independently of the voltage of the a-c systems, and therefore if chosen at 2,500 volts, for instance, the regulation due to the rectifier-inverter arc drop would only amount to about one per cent at 5,000-kw load.

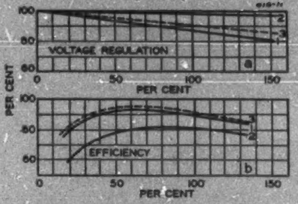


Figure 11. Performance characteristics of a typical electronic frequency converter of 5,000-kw rating compared with a transformer and flexible rotating converter of equal rating

It may be pointed out that the voltage regulation of such a frequency changer is not affected by fluctuations of frequency nor by the phase relation between the voltages of the two networks, as is the case, for instance, in a synchronous motor generator set.

Efficiency

Not only is the efficiency of such a set higher at full load compared to a motor generator set, but it becomes relatively higher at lower loads. From figure 11*b* the efficiency of such a unit, curve 1, can be compared with the efficiency of a motor generator set, curve 2, as well as with that of a transformer, curve 3. The arc drop of the rectifier-inverter is very low and therefore the efficiency of such a frequency changer is also practically the same as that of a transformer, which can couple two networks of different voltages, but naturally cannot couple two networks of different frequencies.

It may be mentioned here that efficiency measurements on some of the actual installations to which reference was made previously reached as high as 93 per cent at full load. Curve 2 refers to a motor generator set of a design which permits flexible coupling of the two systems.

Due to the fact that in the installations referred to above the load factor is relatively low, about 25 per cent, the average difference in efficiencies between the motor generator set and the rectifier-inverter amounts to about 15 per cent. In other words, the losses are reduced very appreciably by this new type of converter. This, as pointed out above, is mostly due to the fact that the efficiency of such a type of frequency changer maintains a very high value, even at low loads.

Power Factor

A certain amount of wattless power has to be supplied by an external source to a frequency changer of the electronic type. This is required for the magnetization of the transformers and partly also on account of the phase displacement existing between the voltage and the current in this type of equipment, especially if grid voltage control is employed. In case of a system as shown in figure 7, if the energy-storing equipment is omitted, the power factor would probably only amount to about 60 per cent. In other words, a very high component of wattless energy would have to be furnished from the a-c supply system. However, with an arrangement such as shown in figure 10, a power factor of as high as 95 per cent can be obtained. Analysis shows that economically it will be feasible to equip such a set with an energy-storing equipment. However, it may be pointed out that in case the capacitors should fail for any reason, the frequency changer would not be affected in its operation. However, it would then adversely affect the three-phase system, by drawing additional wattless power, and causing an unbalance of the three-phase system.

Protective Equipment

In all considerations referred to above, reference was made repeatedly to grid-control equipment in connection with regulating the valve action of the rectifier and inverter, as well as regulating the voltage. It may be pointed out, however, that the grids may be used for another purpose also, namely, to protect the equipment in case of abnormal operating conditions and disturbances on the line as well as short circuits and backfires.[28] Extensive tests in many installations have proved that the electrically energized grids are able to interrupt the current flow in a tank such as proposed for the above frequency-changer set. The equipment required for this purpose consists of relatively inexpensive devices which permit energizing the grids of the inverter with a negative potential in case of sudden disturbances. Recent tests have shown that this time can be confined to less than one-sixth of a cycle. Naturally this equipment will not eliminate breakers completely but may make it possible to use switching devices of lower rating.

FEEDER AND TROLLEY VOLTAGES

It can easily be realized that since any ratio between primary and secondary voltage on both transformers can be chosen, no additional equipment will be necessary in case the single-phase 25-cycle system would require a higher voltage than commonly used. In the case of a motor generator set, it would probably not be advisable to generate directly a voltage of 25,000 volts or higher, but instead a transformer could advantageously be connected between the single-phase generator and the trolley system.

SPACE REQUIREMENTS

Present installations have shown that a considerable saving in space can be obtained with this type of frequency changer. Furthermore, the cost of foundations and building can be greatly reduced, similarly as in any mercury-arc-rectifier installation.

FLEXIBILITY OF A STATIC FREQUENCY-CHANGER SET

As pointed out repeatedly, a set as shown in figure 10 can operate without losing its stability even in case of severe frequency variations on either of the two systems, or during heavy load swings.

TELEPHONE INTERFERENCE

As mentioned in the introduction, the installation of one of these frequency changers caused relatively serious disturbances in the adjacent telephone lines. However, it must be realized that this installation was fed from a relatively small power source, and therefore the wave-shape distortions due to the rectifiers were transferred to the a-c networks without appreciable diminution. For large railway systems, where many units would be used, it may be possible to take care of this situation by establishing such phase relations between the different units that a multiple-phase system could be obtained, as was done in one of the largest rectifier plants recently installed in this country.[23]

Conclusion

It can thus be seen that there are several methods which permit coupling of networks of different frequencies with static electronic devices. The first system, using an intermediate d-c step, has a considerable drawback in that no wattless power can be transmitted from one system to another. However, it has proved very successful for coupling three-phase networks.

The direct-coupled system, although it does not have the above-mentioned disadvantage, necessitates that the output voltage of the rectifier-inverter be equal to the single-phase voltage and requires that single-anode rectifiers be used.

The indirect-coupled system, on the other hand, permits choosing any output voltage for the rectifier-inverter, allows the exchange of wattless power from one system to another, and permits full flexible coupling. The methods used in Europe, necessitate a 3:1 ratio between frequencies, and cannot be applied to the electrified railway systems in the United States where frequency conversion from 60 to 25 cycles, or a 2.4:1 ratio is necessary. However, the conversion of frequencies, as is necessary in this country, may subject the anodes of the rectifier-inverter to a more favorable load cycle than in case of the frequency ratios required in Europe.

In order to give a proper comparison between the characteristics of these converters of the electronic type with those of the rotating converters at present used, a rating of 5,000 kw was selected for ready reference.

It could be seen from the foregoing that the main advantages of an electronic frequency changer are high efficiency, very simple operation, no necessity for special starting equipment or high starting load, instantaneous starting up of the equipment, small weight of the equipment, especially of the control equipment, which reduces costs for housing and foundation.

The disadvantage of such converters, in so far as can be seen at present, is the distortion caused by them on the three-phase supply network, unless somewhat large and costly energy-storing devices are employed.

It may also be pointed out that the present installations referred to should make it possible to develop in the future frequency changers of this type for very large capacities which may also become a very useful means for the conversion of power for present electrified systems now using 60-to-25-cycle motor-generator converters.

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Circuit Interruption by Air Blast

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Synopsis: By using the prestored energy of compressed air for the twofold function of circuit interruption and circuit-breaker operation, a new and original oilless circuit interrupter has been evolved. This air-blast circuit breaker has a guillotine-like arc-interrupting characteristic and can be made practically immune to the adverse effects of circuit recovery voltage.

The historical background of oilless circuit breakers is discussed with particular reference to European designs. The electrical and mechanical design of air-blast circuit breakers for indoor use and the factors affecting that design, are described in some detail.

IN VIEW of the desire to eliminate oil in electrical apparatus evidenced by the electrical power industry, research activities conducted during the past decade have been directed toward the development of a satisfactory current-interrupting principle, not involving the use of oil, but capable of application over the same ranges of current, voltage, and interrupting-capacity steps as the oil circuit breaker.

Fundamental Requirements

It has been recognized that to be acceptable to American power engineers, it is essential that any oilless circuit breaker must be compact, simple in operation, reliable, economical, and easily adaptable to the various forms of circuit connection and mounting at present obtaining for the oil circuit breaker. In addition, the desirability of obtaining an increase in interrupting efficiency has been recognized.

Development of Air-Blast Breaker

It is economically advisable and expedient that American manufacturers should avail themselves wherever possible of the operating experience and evolutionary test data appertaining to successful European designs, for, by so doing, much development time and expense can be eliminated, resulting in ultimate savings to the user. This procedure has been followed in the case of the air-blast breaker here described, thereby resulting in an initial American design from which outstanding reliability of operation has been obtained. A typical indoor air-blast circuit breaker of American manufacture is shown in figure 1.

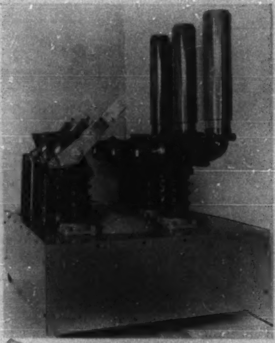

Figure 1. Type AB indoor air-blast circuit breaker, 1,200 amperes, 15,000 volts, 500,000-kva interrupting capacity having active operating parts enclosed

European air-blast circuit breakers, of both axial and cross-blast designs, have been found to be subject to certain limitations so far as their ability to interrupt high currents is concerned. In addition, these limitations are aggravated when high rates of recovery voltage obtain.

In view of the mechanical advantages and operating experience of a certain European design, it was thought desirable to so improve the design of the interrupting chamber as to increase the maximum interrupting current ability of the breaker to the point where it would be adequate for use on American power systems. To this end, considerable laboratory research was conducted.

In the typical European type of axial air-blast breaker interrupting chamber, the compressed air approaches an orifice through a converging port, and exhausts through a diverging throat. In many European designs, too, the interrupting contact is a part of the isolating contact. This arrangement makes for inefficiency of the interrupting air blast as will be shown later in the paper.

As the result of extensive research, it was found that the arrangement of interrupting chamber shown in figure 2 has a greater interrupting efficiency, particularly at high currents, than the European type of converging-diverging chamber.

It will be noted from figure 2 that the American development incorporates a structure having parallel walls. Redesign of the cooling chamber to obtain increased air velocity, and the introduction of concentric metallic cylinders within the arcing chamber, produced further improvement. Main current-carrying contacts, as distinct from the arcing contacts, were also added.

Operating Characteristics

The design here described capitalizes on the mechanical operating experience and reliability of the European design and, using the prestored energy of compressed air for both circuit-breaker operation and circuit interruption, produces a practically constant circuit-interrupting characteristic, with the result that arcing times of one-half cycle or less are consistently produced throughout the interrupting range of any particular breaker, as shown in figure 3. The typical interrupting test oscillogram, shown in figure 4, shows the short arcing time obtained with this design.

As a consequence of the short arcing time, contact burning is very low and many repetitive operations can be made without contact renewal.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

It has also been found that the interrupting action can be so controlled as to be independent of system characteristics, since it is possible to control the rate of rise of recovery voltage across the interrupting contacts immediately following arc interruption by means of the automatic insertion of resistors which prevent the occurrence of high-frequency transients and reduce the instantaneous values of generator electromotive force appearing at the moment of arc interruption.

Axial-Blast Application

Tests have proved that when using an axial-blast form of interrupting chamber, there is an optimum distance of break between the arcing contacts which produces a maximum interrupting effect. Figure 5 shows the relationship between megavolt-amperes interrupted and arcing-contact separation, with air pressure and port area constant. Obviously, it is desirable that in any air-blast circuit-breaker design, the arcing contact separation should be that which provides maximum interrupting efficiency.

Separation of Interrupting and Isolating Functions

It is evident that, since in indoor air-blast circuit breakers the maximum interrupting efficiency is achieved with a short arcing-contact separation of only one-half inch, considerations of circuit isolation require a co-operating disconnecting means. This necessitates an initial arcing contact break sufficient for the maximum interrupting efficiency, immediately followed by an appropriate isolation break, in series, opening under no-load conditions.

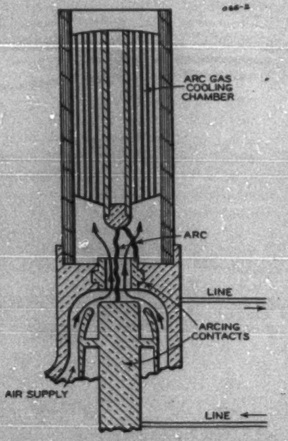


Figure 2. Typical air-blast interrupting chamber—American design

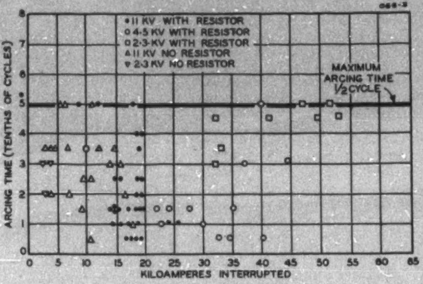


Figure 3. Graphical representation of interrupting-test data showing arcing time in tenths of cycles

The use of an independent interrupting contact also permits a very speedy opening of the arcing contacts during interruption, since the inertia of these contacts is relatively negligible when compared with the energy available in the compressed air. The isolating means takes the form of a disconnect switch, and is operated by the same pneumatic energy that opens the interrupting contacts and interrupts the circuit.

Compressed air used for operation of the contacts constitutes a pneumatic co-ordinating device which acts as a positively functioning mechanical interlock of unlimited flexibility. The relative diameter and volume of the operating devices, and of the air ducts leading to them, provide a superior timing means with which to insure proper sequential operation.

Closing Capabilities

Completion of the circuit through the breaker during closing, is achieved by means of the closing of the disconnecting or isolating contacts only, since, except during the interrupting process, the arcing contacts are held closed by the action of heavy springs.

Actual contact, during closing, is made between the movable disconnecting or isolating blade contact and a laminated wedge-type stationary contact. Burning, during closing, is very slight. Even when closing against currents of 140,000 rms amperes and above, there were only slight evidences of scorching or pitting.

The reason for the low contact-burning characteristics of the air-blast breaker during closing is in the very high speed of closing obtained. In order to burn a given quantity of contact material a definite amount of energy is required. Since time is a factor in the energy equation, and since, due to the high closing speed, the period of arcing during closing is extremely short, the breaker can be closed against very high currents without appreciable damaging effects, and with practically no heating or pitting of the contacts. It is especially to be noted that no blast of air on the contacts is required during the closing operation.

Separation of the interrupting and closing functions between two sets of contacts divides the contact deterioration consequent upon these functions; thereby lengthening the over-all contact life of the breaker.

Trip-Free Interrupting Contacts

As in the air-blast breaker the interrupting contact is free to move independently of, but co-operatively with the isolating or disconnecting means, it is essentially trip free in its position and relation to the disconnecting contacts. This fact is of great advantage, especially in the higher-voltage outdoor breakers where extremely rapid interrupting and reclosing times are required.

Breaker Arrangement

A typical design of indoor air-blast breaker is illustrated in figures 6 and 7. A compressed air tank (1), fitted with a main air blast valve (2), forms the foundation on which the active parts of the breaker are mounted. The arcing chambers (3) and disconnect movable members (5) are supported by hollow insulator columns attached to the tank assembly. The arcing chambers exhaust into cylindrical mufflers. Operation of the disconnects is achieved through insulated link (6) and shaft (7), connected to air pistons (8) and (9). Valves (10) and (11) control the air supply.

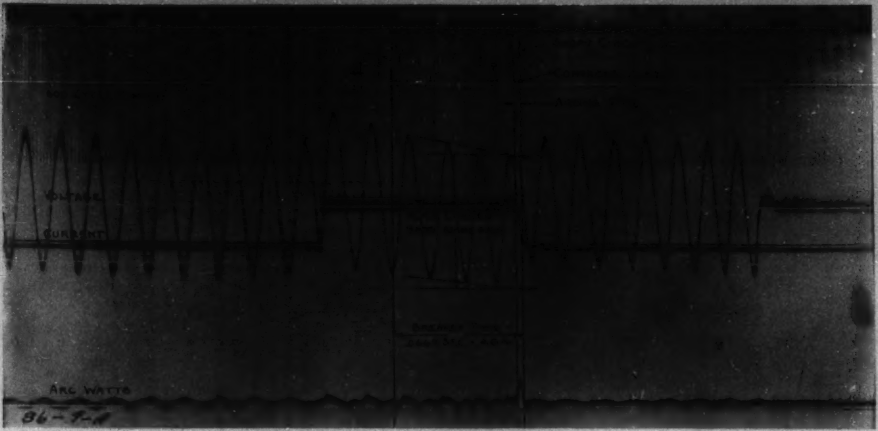


Figure 4. Type AB air-blast-circuit-breaker interrupting-test oscillogram

CO operation at 192,000 kva at 11 kv interrupted by a breaker having a 250,000-kva interrupting rating

Operation

Opening

When opening, electrical or manual actuation of control valve (11) results in opening of the air-blast valve (2), admitting air at 135 pounds or 215 pounds per square inch gauge pressure into the interrupting chambers.

Movable arcing contact (14), normally held closed by the action of heavy springs, is attached to a piston. As air pressure builds up within the interrupting chamber, the piston is depressed, withdrawing contact (14) the required distance away from the stationary arcing contact and air-blast orifice (13), drawing an arc between the contacts. As the tubular stationary contact is the only outlet for the compressed air, a blast of high-velocity air is simultaneously impelled across the initial arcing zone, resulting in limitation of arc-energy release and preventing reignition after the first current zero.

Simultaneously with the admission of air to the interrupting chamber, air pressure is applied to piston (9) resulting in opening of the disconnect contacts (5), under no load, as soon as arc interruption is complete.

Immediately the disconnecting contacts open, an auxiliary contact interrupts the circuit to valve (11), closing the valve. This cuts off the air supply to valve (2) and permits the air remaining on the reverse side of this valve to escape. The main valve then closes under the influence of a heavy spring and the pressure of air remaining in the tank; pressure in the arcing chamber falls and results in reclosing of the arcing contact due to spring pressure.

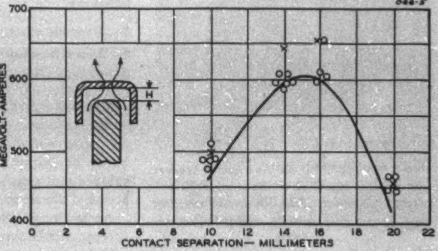
The air required for arc interruption causes pressure in the breaker air tank to fall. Since the tank is permanently connected to a compressed air storage system, the supply of air within the tank is quickly replenished.

Closing

When closing, electrical or manual actuation of control valve (10) causes compressed air to be admitted to the closing cylinder (8), resulting in the closing of the disconnecting switches. As the arcing contacts are already held closed, the closing of the disconnecting switches completes the circuit.

Figure 5. Relation between interrupting-contact separation and interrupting ability in megavolt-amperes

Circles—Effective interruption
Crosses—Interruption with remnant arc
(Air pressure and port area constant)



A simple mechanical interlock insures that all switching operations, once initiated, will be carried to completion. A mechanical trip-free device permits immediate reopening of the disconnecting switches in the event of their being closed against short circuits, even though air pressure may still be exerted upon closing piston (8).

Reliability of Operation

The successful air-blast breaker has to be more than an interrupting device. It must have adequate insulation, and must be able to carry its rated current continuously without exceeding prescribed temperature limitations. Also, like all circuit breakers, it must be capable of operating many times a day without failure of mechanical parts, and yet, on the other hand, be capable of operating satisfactorily after having stood for a period of time without being operated. Several years of experience in the manufacture and operation of air-blast circuit breakers have provided knowledge of the proper materials to use in mechanical parts, such as pistons, valves, valve seats, piping, and interlocks; giving assurance that the breaker can accomplish repetitive operations, as well as function satisfactorily, when infrequently operated.

Apart from the two solenoid control valves for closing and tripping the breakers, there is only one valve which must seal against the air pressure. This is the main air-blast valve, which is a simple composition valve, easily accessible. The method of mounting this main air-blast valve is such that the air pressure in the storage tank acts on the valve to secure tightness.

General Factors Affecting Design

The most efficient circuit breaker is the one in which, all other things being equal, the rate of dielectric recovery between the arcing contacts, immediately following the first passage of the current wave through zero, after parting of the contacts, is always greater than the rate of recovery voltage rise across the contacts. Under these circumstances, restriking of the arc is impossible and the maximum period of arcing is one-half cycle.

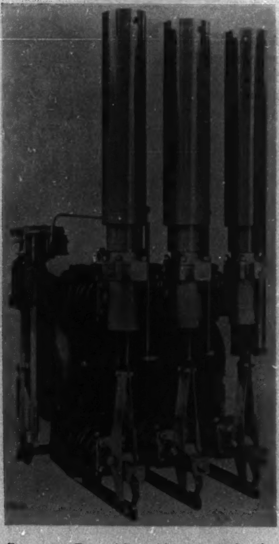


Figure 6. Type AB air-blast circuit breaker with vertical mounting arrangement, 600 amperes, 15,000 volts, 500,000-kva interrupting capacity—equipped with arc-paralleling resistors

In designing a circuit breaker, there are two lines of attack by which maximum interrupting efficiency may be obtained. First, by the production of a high rate of dielectric recovery and second, by limiting the rate of rise of the recovery voltage. These two factors are used in the design of air-blast circuit breakers with such marked success as consistently to produce arcing times of one-half cycle or less.

Tests have proved that there are four factors which influence the interrupting capacity of high-voltage air-blast circuit breakers. These are:

1. Air pressure
2. Port area
3. Series breaks
4. Automatic insertion of resistors

The first three of these factors contribute toward control of dielectric recovery. The fourth factor produces a measure of control over the rate of rise of recovery voltage. The proper correlation of such of these factors as may be required produces air-blast circuit breakers of almost any desired interrupting capacity in the sizes usually required. This holds true for a wide range of interrupting capacities and for all standard voltage ratings up to and including 220,000 volts.

Factors Affecting Dielectric Recovery

AIR PRESSURE

Dielectric recovery between the arcing contacts of air blast breakers is assisted by three different air-pressure characteristics. These characteristics result in the following:

1. Dynamic effect
2. Dielectric effect
3. Cooling effect

Dynamic Effect. Initial opening of the arcing contacts is effected by means of the air pressure itself. An arc is drawn between the contacts, but is immediately enveloped and centralized by the dynamic action of the air under high pressure.

An arc may be visualized as an incandescent core of concentrated electrons surrounded by an incandescent envelope of ionized gas. Due to the dynamic effect of the compressed air, the arc is centralized and its length controlled. This contributes toward limitation of arc energy release.

During the process of arc extinction the air pressure rapidly forces the arc products and any ionized gas into an arc gas cooling chamber, and so produces extremely rapid scavenging and deionization of the arc path.

Dielectric Effect. That the dielectric value of air increases due to compression, is well known. When under a pressure of about 135 pounds per square inch, the dielectric value of air is approximately equivalent to that of high-grade oil. Due to the passage of this high-dielectric medium between the contacts an inherently rapid build-up of dielectric is obtained.

Cooling Effect. The compressed air usually employed, is under a pressure of either 135 pounds or 200 pounds gauge per square inch. It is obvious that the sudden release of such pressures in the arc zone, and the instantaneous expansion of the air, will create intense cooling action. The cooling action deionizes the arc and limits its conducting ability by reducing its cross section.

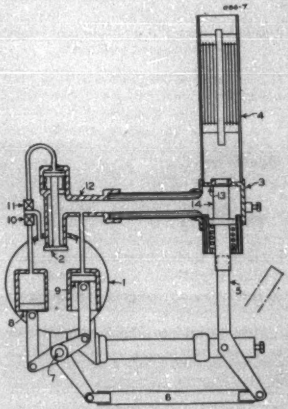


Figure 7. Typical type AB air-blast-circuit-breaker pole section

Due to the inherent characteristics of an a-c system, the cross section of the arc diminishes as the current approaches zero. This causes an increase in the relative cooling effect of the air blast immediately before current zero.

Total Effect of Air Pressure. The combination of a high-velocity high-dielectric cooling medium produces the maximum interrupting effect and is responsible for the short interrupting time of one-half cycle or less, experienced with most air-blast breakers.

Due to the short arcing time, and the low release of arc energy, contact burning is greatly reduced. Consequently, long contact life is obtained, even when interrupting high currents. Many thousands of interruptions are obtainable without the necessity of contact renewal.

Many interrupting tests with varying short-circuit currents, and at different air pressures, have proved that a definite relation exists between interrupting capacity and air pressure. As shown in figure 8, the increase in interrupting capacity is almost proportional to the increase in pressure.

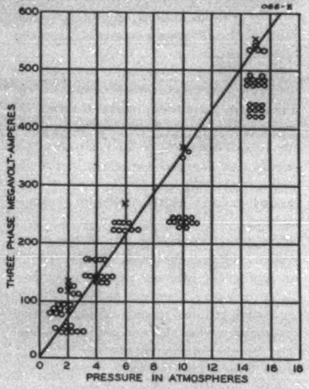


Figure 8. Relation between air pressure and interrupting ability in megavolt-amperes

Circles—Effective operations
Crosses—Ineffective operations
(Port area constant)

PORT AREA

Since a definite control is obtained by means of the dynamic, dielectric, and cooling effects of the air blast, it follows that these effects will be increased if the volume of air made available is increased, provided that the air pressure remains constant.

By increasing the area of the port, increased interrupting capacity is obtained as shown in figure 9. The proportional relationship between the area of the port and the interrupting capacity there shown has been verified by a large number of tests.

SERIES BREAKS

As the recovery of dielectric between the arcing contacts depends more or less directly on the pressure of the air, it follows that, provided air pressure remains constant, an increased measure of dielectric recovery should be obtainable by connecting a number of breaks in series.

Tests have proved that while an improvement in interrupting capacity can be obtained by the use of series breaks, the increase in capacity is not in direct proportion to the increase in the number of breaks as is shown in figure 10.

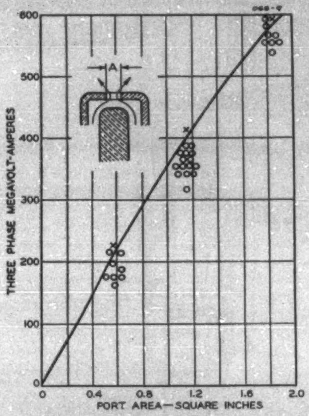


Figure 9. Relation between port area (orifice) and interrupting ability in megavolt-amperes

Circles—Effective operations
Crosses—Ineffective operations
(Air pressure constant)

There are practical limitations to the use of series breaks. Mechanical design factors, insulation difficulties, and costs increase with each additional break. It is also obvious that the expenditure of air must increase with each additional break.

The electrostatic relations which cause unequal voltage distribution across series breaks, increase with the number of breaks. In the case of the indoor air-blast breakers here described, one break per phase is sufficient in all cases.

Factors Affecting Rate of Rise of Recovery Voltage

INSERTION OF RESISTORS

On circuits where the impedance consists largely of reactance, the power factor at the moment of arc interruption may be low. Due to the lower power factor, current zero occurs at a time when the generator electromotive force would normally be in the vicinity of its crest value, so that, at the moment of interruption, the voltage across the contacts rises rapidly to a value corresponding to the generator electromotive force.

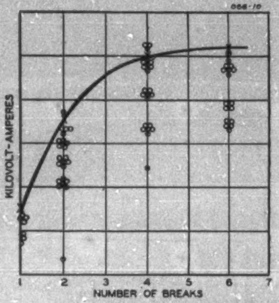


Figure 10. Relation between number of breaks in series and kilovolt-amperes interrupted

Circles—Effective operations
Crosses—Ineffective operations
(Air pressure and port area constant)

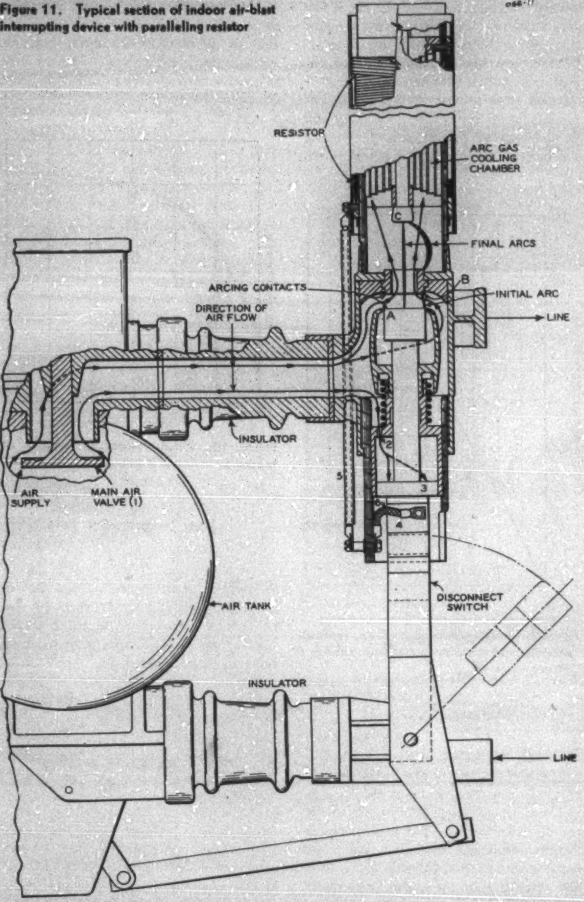
The rate at which the voltage rises is affected by the characteristics of the circuit under interruption, and also is dependent upon the instantaneous value of the generator electromotive force at current zero.

Upon interruption of the short circuit, the stored energy in the system tends to discharge, since a-c circuits are composed of both inductance and capacitance. The well-known inductive kick from coils of many turns, such as field coils, illustrates one type of circuit discharge. The snapping discharge of a capacitor illustrates another. An interruption on a large power system may produce both of these kicks in magnified form, and in an infinite variety of phase relations.

When the generator electromotive force re-establishes itself, immediately upon interruption of the short circuit, the inductive and capacitive discharge effects are superimposed upon the characteristic form of the normal-frequency electromotive force in the form of a high-frequency transient which may rise to a value as much as twice or more the instantaneous value of the generator electromotive force. This transient is usually a combination of various frequencies which depend upon the natural frequency of different circuit combinations obtainable in the system interrupted.

The rate of rise of the combined recov-

Figure 11. Typical section of indoor air-blast interrupting device with paralleling resistor



ery voltage depends upon the instantaneous value of the generator electromotive force, the amplitude of the superimposed transient, and upon the frequency of the transient.

It has long been known that the rate of rise of recovery voltage can be beneficially influenced by inserting a resistor into the interrupting circuit. In the air-blast breaker limitation of the rate of recovery voltage rise is accomplished by the use of resistors inserted into the circuit by means of the action of the arc itself.

Indoor air-blast circuit breakers having 150,000-kva interrupting rating and higher are usually equipped with paralleling resistors. The method of connecting and mounting the resistor is shown in figure 11. It will be seen that the resistor is noninductively wound around the muffler housing.

As the arcing contacts *A* and *B* separate, an arc is drawn between them and the centralizing action of the air-blast forces the arc into contact with the probing electrode *C*, thereby automatically inserting the resistor in parallel with a portion of the arc during the first half cycle of arc.

As the fault current decreases after reaching its maximum value during the first half cycle, the resistance of the arc increases rapidly, so that the current tends to follow the alternative path from (4) through the resistor in series with the section of the arc from the electrode *C* to the stationary arcing contact *B*. The section of the arc between the moving contact and the electrode then becomes unstable and goes out.

The section of the arc between the electrode *C* and the stationary contact *B* is momentarily maintained after the main arc from the moving contact *A* to the electrode *C* is extinguished, but the fault current is greatly limited due to the high value of the resistance connected in series with the arc.

When the low-current arc between the electrode and the stationary contact is extinguished at or near current zero, improvement in power factor has taken place due to the insertion of the resistance, resulting in a marked reduction in the instantaneous value of the generator electromotive force at the moment of interruption.

The reduction in the rate of rise of recovery voltage obtained by the use of automatically inserted resistors is illustrated in figure 12, which shows cathode-ray oscillographic records of the voltage appearing across the contacts of an air-blast breaker during and after interruption. The upper oscillogram shows the voltage condition when no resistor was used and the lower oscillogram shows the improvement obtained by the use of resistors.

It is apparent that the rate of rise and the maximum value of recovery voltage is much less when an automatically inserted resistor is used. High-frequency transients are practically eliminated and the voltage immediately following arc interruption, follows generally the normal sinusoidal wave form. A representative oscillogram of an interrupting test made on an air-blast breaker equipped with resistors is shown in figure 13.

Summary

It is evident from the foregoing description that the air-blast breaker has several very desirable characteristics.

1. It is an oilless breaker.
2. It can be applied over practically the same range of interrupting-capacity steps and voltages as the oil circuit breaker, both indoor and outdoor.
3. The prestored energy of compressed air is used, not only for circuit interruption but also for opening and closing contacts, and for proper sequential timing.

4. Its current-interrupting range is such as to meet the maximum interrupting-current requirements on each interrupting step.

5. It gives control over the magnitude and rate of recovery voltage rise.

6. Short arcing time permits fast interrupting time.

7. Separation of interrupting and isolating contacts enables fast reclosing.

Conclusion

The air-blast breaker is a scientific product which is a direct result of the availability of high-power circuit-breaker testing laboratories. Its performance under all kinds of conditions, has been checked and rechecked; its operating characteristics and limitations have been determined; and operating security has been assured by the application of adequate safety factors and interlocks.

As a result of the extensive research conducted during the evolutionary process, it is possible to predetermine the performance of this air blast circuit breaker by means of a judicious combination of air pressure, port area, series breaks, and arc paralleling resistors. And so there emerges from the testing laboratory, a unique oilless circuit breaker of simple, sturdy and compact design, which is applicable to practically all current, voltage, and interrupting ratings. Comprehensive and reliable test data enable its adaption to almost any particular set of system characteristics within its rating, and prove its ability to render satisfactory service under the most arduous demands of American power systems.

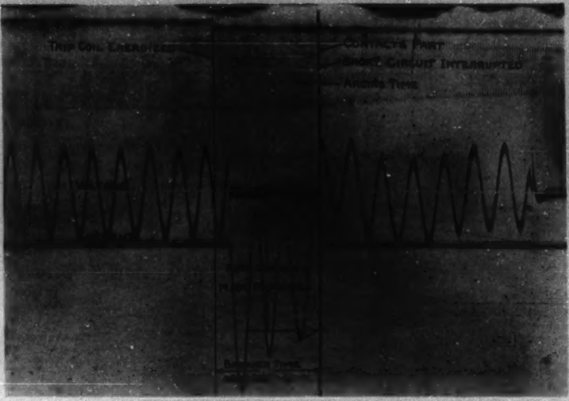


Figure 13. Oscillographic record of interrupting test of a 1,200-ampere 15-kv 500,000-kva interrupting capacity air-blast circuit breaker, when interrupting 429,000 kva at 11 kv. Arcing time 1/600 (0.00167) second

Acknowledgment

During the successful development of the air-blast circuit breaker, reliance has been placed on the co-operative efforts of many individuals. Acknowledgment is here made of valuable contributions from the staffs of the circuit-breaker testing laboratories and engineering departments of the Allis-Chalmers Manufacturing Company and of the Brown Boveri Company.

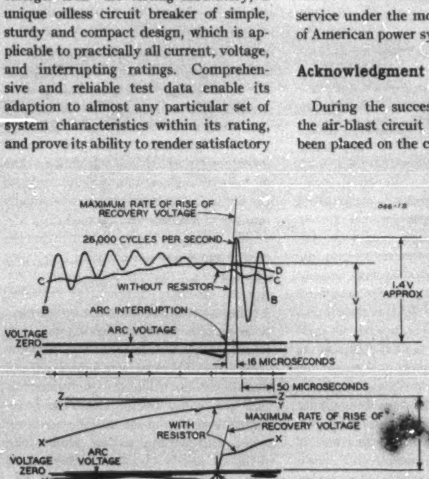


Figure 12. Cathode-ray oscillogram showing prevention, by resistor, of high-frequency transients

Upper section, without resistor, shows transient with frequency of 26,000 cycles. Record starts at *A*, continues to *B*, reappears at *B* (left), continues to *C*, reappears at *C* (left), terminates at *D*. Arc voltage decreased to zero from peak value at interruption point. High-frequency transient appeared; voltage reaching 1.4 times normal maximum sinusoidal value in the short time of 15 microseconds. Oscillations gradually decrease to *D*

Lower section, with resistor, record starts at *W*. Resistor limited voltage peak before arc interruption. Rate of recovery voltage rise greatly reduced after interruption. Voltage-wave rise rate tapered off, following approximately sinusoidal form through *X* and *Y* to *Z*. Maximum voltage value not reached until 750 microseconds

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

The Cross-Air-Blast Circuit Breaker

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Synopsis: The designs of European air-blast circuit breakers are not wholly satisfactory for operation in the United States as they do not handle high enough currents, cannot economically be built for high enough interrupting capacities, and do not conform to United States installation arrangements.

Accordingly, a new type of air cross-blast circuit breaker has been developed which does conform to the above requirements. The commercial design of this circuit breaker is discussed in a companion paper "Design and Construction of High-Capacity Air-Blast Circuit Breakers" by Messrs. H. E. Strang and A. C. Boisseau, and the present paper analyzes the theory of performance of the foreign and cross-blast circuit breakers, and presents evidence from a number of sources which throw light on the method of operation. The circuit breaker is found to operate predominately on the displacement theory. The nature of its performance makes it much less sensitive to recovery rate and high currents and much more economical in air consumption than air-blast breakers previously available.

FOR a number of years air-blast circuit breakers have been built in Europe. These designs have been watched closely; individual breakers have been imported and tested. An air-blast circuit breaker of purely domestic design is now being put upon the market. This circuit breaker is described in a companion paper by Messrs. H. E. Strang and A. C. Boisseau (*Transactions pages 522–7*).

Inasmuch as this design differs completely from foreign practice, it is interesting to examine the reasons for this pronounced difference, and the inner workings of the new device. Figure 1 shows diagrammatically the typical Continental design, which may, for the sake of identification, be described as radial blast. It consists of a male contact and a segmental female contact through which air is discharged when the circuit breaker is opened. The air stream envelopes the male contact on its way to the nozzle. It thus tends to cool the arc and blow the arc products away from the male contact and out through the nozzle. Due to the fact that the arc is extinguished by an independent source of power, the air blast, the performance of such a circuit breaker is highly reproducible and it is an excellent design for analytical study.

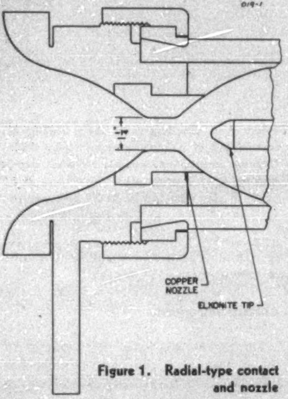

Figure 1. Radial-type contact and nozzle

A considerable number of tests has been made on nozzles corresponding approximately to the Continental designs. Figures 2*a*, *b*, and *c* give the results of such tests. In figure 2*a*, it is shown that with voltage recovery rate and air pressure constant, the current-interrupting capacity is proportional to nozzle diameter. In figure 2*b* it is shown that with voltage recovery rate and nozzle diameter constant, the current interrupted is proportional to air pressure. In figure 2*c*, it is shown that with pressure and nozzle diameter constant, current-interrupting capacity is inversely proportional to recovery voltage rate between limits. This inverse proportionality fails at high currents, therefore, there is a maximum current which can be cleared no matter how low the rate of rise of recovery voltage may be. The design of figure 2*c* corresponds to a Continental rating of 400,000 kva at 10 kv or 23,000 amperes. Examination of the figure shows that at this rating the recovery rate must be quite low, 150 volts per microsecond at 120 pounds per square inch and about 500 volts per microsecond at the breaker's rated pressure, as compared with a figure of 1,800 for the usual testing station. It further appears that the kilovolt-ampere rating must be sharply reduced if the operating voltage is below 8,000 volts no matter how low the recovery voltage may be.

Since United States circuit-breaker standards call for interrupting current ratings as high as 60,000 amperes for a 250,000-kva circuit breaker, it is apparent that the Continental design would have to have a much smaller rating if it were to be applied in any standard manner in this country. Since the nozzle diameter increases as the first power of the current to be interrupted, while the air requirement goes up as the square of the nozzle diameter, the larger nozzle required to meet United States conditions would have a very greatly increased air consumption and would not be an economical design. A satisfactory design for the United States should be able to handle considerably higher recovery rates and at the same time should be able to handle maximum currents with a minimum of air consumption.

Accordingly the cross-blast design described in the Strang-Boisseau paper has been evolved. Figure 3 is a cross section of the interrupting unit. This consists of a pair of contacts located in an arc chute of insulating material. Air is introduced at one side of this chute. The air velocity is, therefore, at right angles to the arc stream and blows the arc against a number of barriers also of insulating material. In effect the arc is caught in the grip of a multiple shear having a movable member consisting of jets of air and stationary members of insulating material. At current zero the arc is cut up into sections and the arc products blown away (through the arc chute) into a muffling structure. This structure lends itself well to observation. All of the usual magnetic and cathode-ray measurements can be recorded and the chute can be made of transparent material so that the behavior of the arc can be observed by high speed photography. The usual high speed cameras operating up to 2,000–3,000 frames per second do not provide enough observations close to current zero. However, a special camera was developed which is capable of a maximum speed of 120,000 frames per second.[13] Since the arc phenomena in the cross-blast chute last for periods between a half and one cycle, the camera was slowed down so as to take 60,000 frames per second, that is, its total of

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Paper 40-19, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940. Manuscript submitted November 13, 1939; made available for preprinting December 12, 1939; released for final publication March 27, 1940.

D. C. PRINCE, manager of the commercial engineering department, General Electric Company, Schenectady, N. Y., was chief engineer of the switchgear department of the company at Philadelphia, Pa., at the time the paper was presented; J. A. HENLEY is with Metropolitan Vickers Company, Ltd., Manchester, England, and W. K. RANKIN is with the General Electric Company, Philadelphia, Pa.

[13] For all numbered references, see list at end of paper.

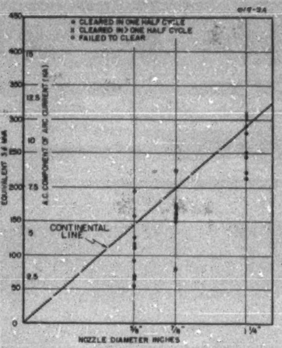

Figure 2a. Variation of rupturing capacity with nozzle diameter at 120 pounds per square inch; radial-type nozzle

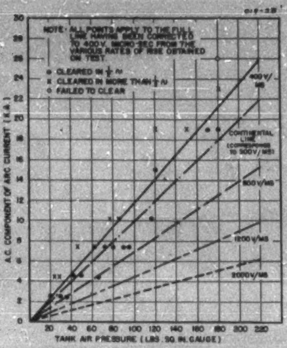

Figure 2b. Variation of rupturing capacity with air pressure; radial-type nozzle

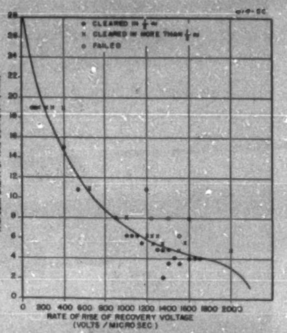

Figure 2c. Variation of rupturing capacity with rate of rise of recovery voltage at 120 pounds per square inch; radial-type nozzle

1,000 frames is distributed over one cycle. With this device, exposures occur at the rate of one every $16^2/_3$ microseconds, or one every 0 degree 20 minutes of arc.

Figure 4 shows some typical enlargements of arcs taken with this camera.

Although much valuable work has been done in which the voltage recovery characteristics of circuits have been assessed on the basis of the recovery rate, or the steepest tangent passing through the origin, it has nevertheless been appreciated from the earliest days of the conception that precise work along this line involved a comparison of the entire voltage recovery curve of the circuit with the curve representing recovery of insulation strength of the interrupting device. For certain other interrupting devices, notably expulsion protector tubes, curves of insulation strength recovery have been determined and published. In the present instance, therefore, an attempt was made to secure as complete a curve as possible between the recovery strength of the circuit breaker and time. In order to secure this curve the circuit breaker was subjected to varying recovery voltage rates high enough to cause the arc to restrike. A cathode-ray oscillogram of such a test shows the voltage first rising across the contacts and then collapsing at the point where a restrike occurs. The value of voltage at which collapse occurs and the associated time provides a point in the time curve of recovery of insulation strength. See appendix *C*.

The solid line of figure 5 is a plot of a recovery characteristic with a number of experimental points shown. Over a considerable range it appears that a straight line through the origin fits the observed points as closely as any other line. It therefore, appears that the conception of recovery rate, that is, a proportionate increase in strength against time, is a useful parameter for this type of circuit breaker. As the recovery voltage rises corresponding to an increasing dielectric strength in the path previously occupied by the arc, however, the point is reached where a breakdown can take place between the contacts over a new path. There is, therefore, a saturation condition. The curve bends over at some value representing the breakdown of the gap. This saturation characteristic appears as a matter of experiment in the solid-line curve of figure 5.

In figure 5, most of the experimental points correspond to a pressure of 3 pounds gauge or 18 pounds absolute. The dashed curve is calculated from the experimental solid curve for 20 pounds gauge or 35 pounds absolute. Two test points are given corresponding to this pressure. The highest points of these two curves are at 40 and 91 kv respectively, or a ratio of approximately 2.3. From the curve in figure 6, the rod-gap dielectric strengths corresponding to the two pressures and adjusted to the proper temperatures are 61 kv and 103 kv, whose ratio is 1.7. From the random nature of breakdown phenomena this is probably as close an agreement as one can expect from so few tests.

Below ten microseconds no experimental points on the solid curve appear. Some discussion will be offered as to the probable shape of the portion of the curve below ten microseconds. However, consideration will first be given to the straight portion of the curve between t_3 and t_4. This part of the curve appears to be consistent with the displacement theory of operation, that is, it is assumed that at current zero an unimpaired wedge of dielectric is driven across the arc path. This wedge increases in thickness at the rate of fluid flow and the product of that rate and the dielectric strength of the wedge gives a measure of recovery strength across the gap:

$$v/t = d/t \times v/d$$

where

v = voltage
d = distance
t = time
v/t = rate of rise of recovery voltage
v/d = dielectric strength
d/t = dielectric velocity

This equation has been evolved for the case of liquid dielectric such as oil, which is relatively incompressible and constant as to dielectric strength. With air which

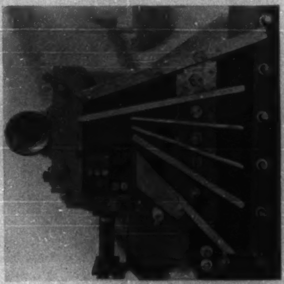

Figure 3. Cross section of interrupting unit of cross-blast air circuit breaker

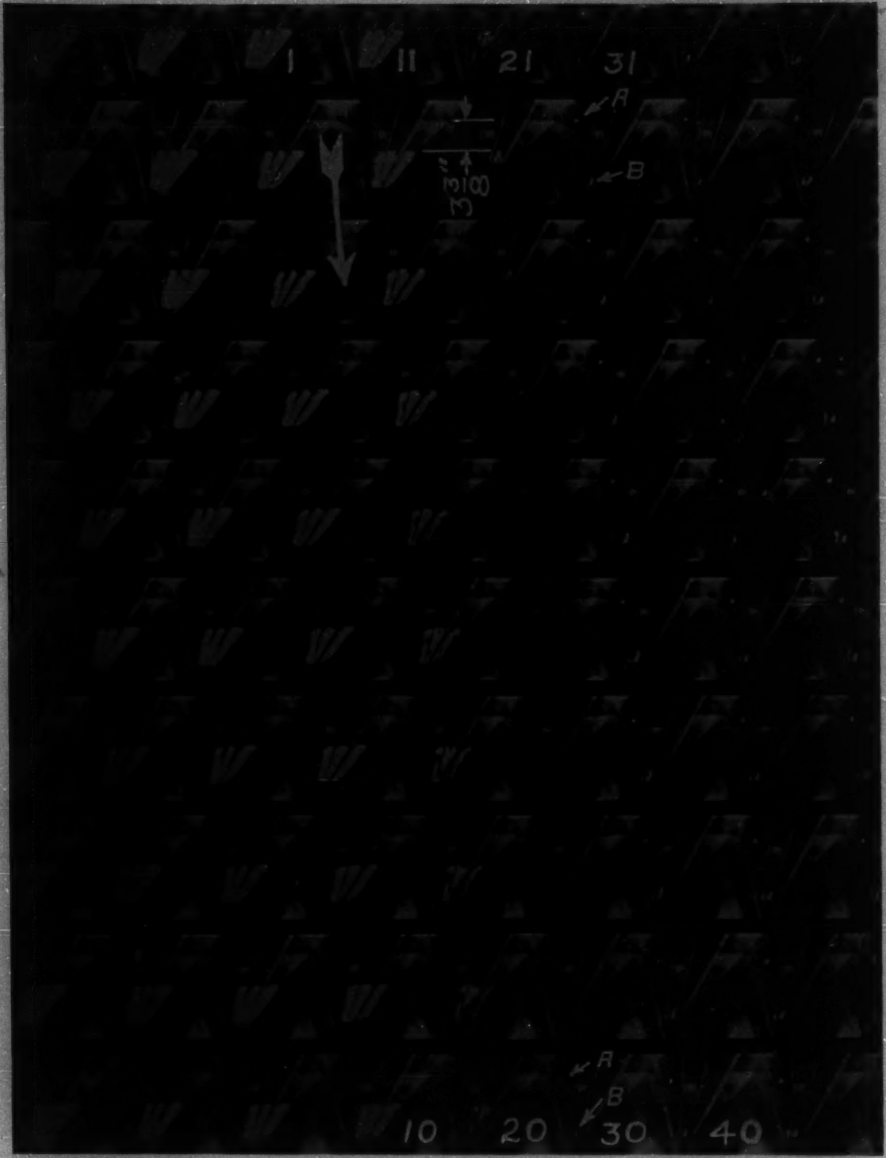

Figure 4. Photographs of the arc in a cross-blast circuit breaker taken with high-speed camera at 60,000 frames per second

Frames 1 to 25 inclusive are photographs of the arc which is decreasing as it approaches current zero in frame 23. Frame 30 shows the first breakdown, at *B*, from the moving contact to a timing gap at *A*. The breakdown of this timing gap is recorded on the cathode-ray oscillogram and makes it possible to locate the current-zero photograph. In frame 32 these auxiliary arcs appear with greater intensity. Frame 22 shows the $3^{3}/_{8}$-inch depth of the cooling plates obeys the gas laws and has a dielectric strength which is variable with pressure, the matter of determining the proper dielectric strength and velocity to use is somewhat more complicated than with oil. The curve of figure 6 has already been offered showing the variation of dielectric strength with pressure. The amount of air discharge through an orifice is expressed by the equation:

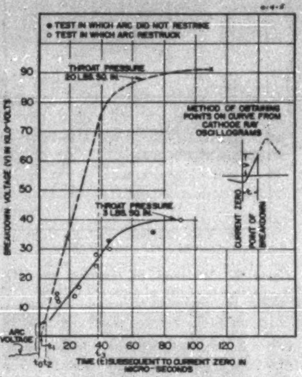

Figure 5. Variation of recovery strength of cross-blast breaker with time

$$M = Ap\sqrt{\frac{\gamma g}{RT}}$$

where

M = mass discharge in unit time
A = effective orifice area
P = absolute pressure in orifice
T = absolute temperature in orifice
R = universal gas constant
$\gamma = \frac{cp}{cv}$
g = acceleration of gravity

This equation holds provided the receiver pressure is less than 0.53 times pressure in the tank supplying the orifice. Under these conditions, the discharge will be at the velocity of sound. This condition is met by the 20-pound curve of figure 5. In the air at high velocity, the temperature will have fallen in accordance with the equation

$$T_2 = T_1\left(\frac{P_2}{P_1}\right)^{\frac{\gamma-1}{\gamma}}$$

to −71 degrees centigrade, P_1 being 135 pounds absolute.

At this pressure and temperature the impulse dielectric strength will be 103,000 volts per inch, assuming that the gap between the ionized columns is approximately equivalent to a rod gap. At a temperature of −71 degrees the velocity of sound is 938 feet per second. The data is taken under such conditions that the contacts are separated far enough to bring in to play one barrier in the arc chute. A wedge of air will be driven in on each side of this barrier so that the rate of rise of dielectric strength should be determined by multiplying the dielectric strength just obtained by the velocity of sound and by 2. The resultant figure of 2,320 volts per microsecond is well within the limits of experimental error as compared with the slope of 2,000 volts per microsecond on the dotted curve.

In the deionization or cooling theory of arc interruption the arc space is cooled and deionized. (Doctor Suits[11] has shown that these two phenomena are substantially the same at high pressure.) This cooling causes an increase in arc voltage. Prior to current zero the arc voltage is measured and it is a reasonable assumption that immediately subsequent to current zero it would require that amount of voltage or a greater amount to sustain an arc. Practically, the required voltage should be greater for two reasons. In the first place, some cooling will have taken place during the voltage reversal no matter how quickly that has occurred. In the second place, the reversal must be accompanied by the formation of a new cathode. This cathode effect, however, is small in a high-voltage design such as that under discussion. The curve of figure 5 would, therefore, be expected to start at zero time t_0 at a height corresponding approximately to the arc drop prior to current zero. During the time that the cooling is taking place this curve will rise until when the cooling has become complete the dielectric strength will increase at the rate of interposition of new dielectric. The transition from the curve to the straight line, the slope of which represents the rate of interposition of the new dielectric, is designated as t_2. It is interesting to see what evidence can be obtained regarding the time t_0 and t_2 and what effect it has upon the performance of the circuit breaker.

In this way the recovery characteristic of this circuit breaker can be divided into two parts. One part between t_0 and t_3 in which a cooling phenomenon takes place and the other period subsequent to t_2 in which the operation is on a displacement basis. During the cooling period, displacement cannot be operative because during that period some conductivity exists, and the current flowing through that conductivity disrupts the fresh dielectric as fast as it enters the arc space. As soon as unimpaired dielectric has been interposed, that fresh dielectric will sustain the entire recovery voltage. That part of the arc path still conducting will in effect be short-circuited by that conductivity. The two phenomena, therefore, would not take place simultaneously, but are present, one prior to time t_2 and the other subsequent to it. During the time t_0 to t_1 there must be some conductivity. If that conductivity is appreciable, it should represent a resistance shunted across the contacts, and should alter the curve of the voltage transient. The voltage transient of the test station is known both through calculation and tests of oil circuit breakers. See appendix *A*

An examination of the tests on the cross-blast air circuit breaker indicates conductivity only at currents above 6,000 amperes and at rates of rise of recovery voltage above 2,500 volts per microsecond. The largest observed conductivity was quite small, resulting in a reduction of the recovery transient peak of only 15 per cent. Qualitatively, if cooling is playing a part in the operation of the circuit breaker, one would expect to find some relationship between clearing and the arc voltage immediately prior to the last current zero, that is, it should be apparent that the circuit breaker is getting ready to clear by a rise in arc voltage prior to the current zero, at which point the clearing actually takes place.

Table I shows a number of tests made at varying currents and pressures together with the arc voltages immediately prior to the last current zero. There seems to be a purely random relation between the

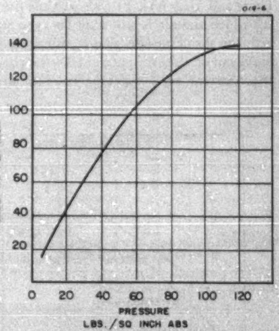

Figure 6. Variation of dielectric strength of air with pressure; Impulse breakdown of two-centimeter rod gap in nitrogen at 25 degrees centigrade

arc drop just prior to clearing, so that any effect of cooling on the actual clearing appears to be a secondary phenomenon.

Further qualitative evidence on this subject was obtained by substituting other gases for air. It is known that a small amount of carbon tetrachloride vapor will greatly increase the breakdown strength of air. Accordingly, carbon tetrachloride vapor was added and it was found that the performance corresponded to that which would exist at double the dielectric strength of pure air in accordance with the displacement theory. Since it was conceivable that this increase was due in part to the increased cooling effect from added carbon tetrachloride, a proper proportion of CO_2 was added to give approximately the same increase in the cooling effect but no measurable increase in the performance was detected.

Messrs. I. Kesselring and F. Koppelman[7,8] have proposed an interesting theory on the basis of which the time required to cut the arc path can be calculated for a radial-blast breaker. They assume that the current density in the arc stream is a constant. Under these conditions and with the round arc stream used by them the diameter of the arc is proportional to the square root of the current, a relationship which they claim to have been confirmed by photographs. In the process of cutting off the arc path, the interrupting air proceeds with some velocity. As long as this velocity is greater than the rate at which the arc diameter decreases, the unimpaired air will follow closely the receding boundary of the arc. However, with the diameter changing in accordance with the square root of current, the rate of decrease of the arc diameter approaches infinity as current zero is approached. There will, therefore, be a short time during which the air is not able to keep up with the shrinking arc, and it will not actually cut off the arc space until some time after current zero. If enough data are available this interval during which the arc path is not definitely cut off by displacement is calculable.

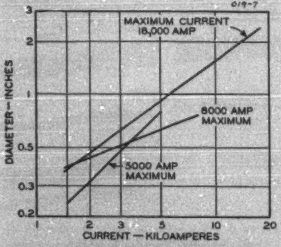

Figure 7. Variation of arc diameter with current, cross-blast circuit breaker

Doctor C. G. Suits has calculated on theoretical grounds that the current density with forced convection is not absolutely constant but that it should vary in such a way that the arc diameter will vary as the 0.77 power of the current, instead of the 0.5 power as assumed by Messrs. Kesselring and Koppelman.

The high-speed photographs provide an experimental measure of the actual diameter of the arc at least as far as it is shown by its effect on the photographic negative.

Figure 7 has been taken from measurements of arcs at three different currents, peaking at 5,000, 8,000, and 18,000 amperes. From these curves the following three formulas can be deduced:

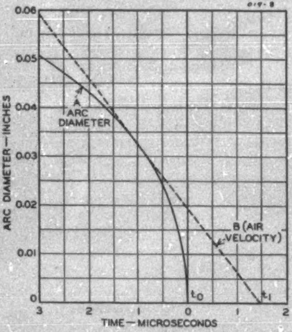

Figure 8. Variation of arc diameter with time, 8,000-ampere arc, cross-blast circuit breaker

With a maximum of 5,000 amperes

$d = 0.000288i^{1.04}$ inches

With a maximum of 8,000 amperes

$d = 0.021i^{0.4}$ inches

With a maximum of 18,000 amperes

$d = 0.00163i^{0.746}$ inches

All of these curves represent measurements during the decay of the arc.

Figure 8 is such a curve of arc diameter against time, drawn to scale with a corresponding curve of air velocity shown tangent to it. If we assume that the separation of the current zero and the arrival of the air indicates the time t_0t_1, then those times for the three currents observed are 0, 1.6, 0.0107 microseconds. From the pronounced variation in exponent derived from figure 7, one is left in doubt whether there is a change in constants with increase in current or whether the three sets of values are due to observational errors. More data should throw light on this point. All three values yield cutoff times so short that it is apparent that one would not expect cooling phenomena to be in evidence except under conditions of a high rate of rise. For instance the least arc voltage from table I is 2,900 volts and the longest time above is two microseconds, giving a rate of rise of 1,450 volts per microsecond.

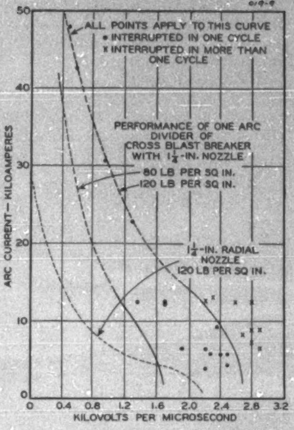

Figure 9. Variation of rupturing capacity with rate of rise of recovery voltage

This gives rise to the question of what needs to be done in the case of an application where the rate of rise of recovery voltage is extremely rapid. There are two alternatives. Either the ability of the breaker may be increased by an increase in air pressure, air volume, or the number of arc barriers introduced into the chute; or arrangements may be made to shunt the contacts with some impedance which will reduce the rate of rise. Both of these expedients can be taken. In order to secure the effect of a resistance shunt, an electrode can be located between some pair of barriers and connected through a resistance to the stationary contact. With such an arrangement the recovery voltage appearing between the contacts after the arc has been interrupted breaks down the gap to this electrode inserting the resistance in shunt with the arc path while any current flowing through the resistance is interrupted by further movement of the contact admitting air to other sections of the arc chute. In the appendix *B* will be found a simple method of calculating what

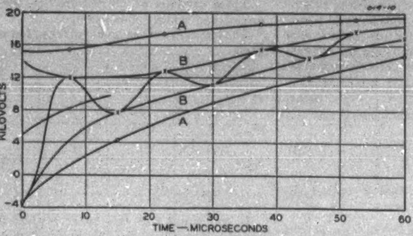

Figure 10. Envelopes of recovery-voltage oscillation, cross-blast circuit breaker

Curve *B* at reduced air pressure

the proportions of such resistors should be. From this point on, it is merely a matter of economics and design whether the recovery rate of any given circuit should be taken care of by a change in the air delivery or by the application of a shunt resistor.

The ability of the cross-blast design to handle recovery rate is much greater than that of a radial blast, particularly at heavy current.

Figure 9 is a curve of current variation with recovery rate corresponding to figure 2*c*. The curve of figure 2*c* has been superimposed and dotted for reference. In the case of the radial blast, all of the air heated by the arc and the arc products must be expelled through the same opening as the air blast. Consequently any current will reduce to some extent the air delivered. In a cross-blast design air is throttled by an orifice before entering the arc chute. As long as the back pressure is less than the critical value 0.53, the amount of air delivered will not change with current over a considerable range. The interrupting ability of the circuit breaker should, therefore, not change as much over a corresponding range and this is shown clearly in figure 9.

Some falling off in the recovery rate ability is noted, but this fall is not nearly as rapid as with the radial nozzle. At high currents gas evolved by decomposition of the chute structure begins to be added to the air discharge in such volume that it adds appreciably to the interrupting capacity of the chute. The dotted portion of the curve in figure 9 represents an extrapolation of the actual test curve beyond the point at which it was possible to secure failure to locate the curve exactly. At high currents enough gas is evolved from the chute structure to interrupt the circuit without any air supply. The extrapolation of this curve, therefore, represents ability known to be present in the interrupting structure but not necessarily the maximum capacity of it. The full-line curves of figure 9 correspond to clearing in one cycle with only one barrier in action. As the contact continues its travel two or more barriers come into action with resultant increase in interrupting ability as shown by the crosses on the figure. These points are not comparable directly with the points for one barrier since no limit in interrupting capacity was reached. It will be noted that at say 1,000 volts per microsecond the cross-blast chute interrupts over four times as much current with the same air consumption or conversely, a radial nozzle to have the same interrupting capacity would be 4.3 times as big in diameter and consume 18 times as much air.

As mentioned at the beginning of this paper, it is hoped that the foregoing will give a clear idea of the reasons why, for low-voltage designs particularly, it has been necessary to develop a new type of interrupting air-blast structure. The behavior of that structure has been analyzed and it has been shown under most conditions to operate on a displacement basis. The small area in which operation is by cooling has also been defined. For heavy currents, the ability of this design, instead of decreasing toward zero volts, actually rises until at the highest currents the circuit is interrupted even without the aid of the air blast.

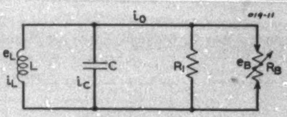

Figure 11. Test circuit

Table I. Interrupting Tests

Test Number	Arc Amperes, RMS	Arc Duration (Half Cycles)	Arc Length (Inches)	Reactor Tap Number	Kilocycles Per Second	Applied Air Pressure (Lbs Per Sq In.)	Peak Arc Voltage (Maximum Kv)	Peak Recovery Voltage (Kv)	Rate of Rise of Recovery Voltage (Volts Per Microsecond)
186	1,950	0.9	0.9	6.6/0	26	40	3.4	29	1,710
187	1,920	0.8	0.8	6.6/0	26	30	3.4	30	1,770
188	1,970	0.9	1.0	6.6/0	26	30	2.5	32	1,900
190	1,950	2.7	3.6	6.6/0	26	23	5.1	32	1,900
188	1,970	3.7	4.9	6.6/0	26	18	6.2	32	1,900
191	1,920	4.2	4.5	6.6/0	26	7	5.4	31	1,830
194	4,100	2.2	3.0	2.7/0	40	28	5.7	25	2,280
192	4,100	3.0	3.8	2.7/0	40	25	5.1	25.5	2,320
193	4,100	2.4	3.5	2.7/0	40	25	4.3	25.2	2,300
203	5,700	1.9	2.8	1.7/0	45	47	4.5	22	2,240
196	5,400	2	2.5	1.7/0	45	45	6.2	20.6	2,100
199	5,400	1.1	1.3	1.7/0	45	40	2.8	20.6	2,100
201	5,900	2.5	3.2	1.7/0	45	27	3.9	21.3	2,170
197	5,600	3.1	4.5	1.7/0	45	23	5.2	20.6	2,100
202	5,900	3.1	3.7	1.7/0	45	13	3.4	21.3	2,170
204	8,500	1.1	1.1	0.93/0	70	45	3.4	15.2	2,420
206	8,300	2.5	2.3	0.93/0	70	40	3.4	13.8	2,200
205	8,300	2.2	2.7	0.93/0	70	25	3.4	12	1,910
210	11,100	1.5	1.8	0.47/0	100	58	2.9	9.6	2,180
209	11,300	1.5	1.6	0.47/0	100	55	3.4	8.3	1,880
211	10,800	1.8	2.1	0.47/0	100	33	2.9	8.2	1,860
213	10,600	4.4	4.7	0.47/0	100	15	2.9	7.7	1,750

Appendix A. Air-Circuit-Breaker Leakage Current*

It has been pointed out that the interruption process can be divided into two periods: one during which a path of finite resistance exists between the electrodes; and a second characterized by infinite resistance but finite dielectric strength. This fact is well illustrated by a series of tests from which the actual magnitude of the leakage resistance and current are determined. It was found that at constant recovery rate, current, and voltage, air pressure could be reduced until a leakage current flowed after the current zero.

Figure 10 shows the envelopes of the recovery-voltage oscillation as taken from cathode-ray-oscillograph records of two tests taken at the same voltage and with the same circuit. Test *B* was made at considerably reduced air pressure.

The complete circuit employed in the tests consisted of a generator and an external reactance in series with the breaker. Both generator and reactance had considerable distributed capacitance to ground, consequently the recovery voltage was composed of two frequencies, one relatively low, and the second, due to the presence of the series reactance, of the order of 60,000 to 100,000 cycles per second.

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* Prepared by Robert M. Bennett.

For the purpose of analysis of the high-frequency oscillation, this circuit can be generalized as indicated in figure 11, in which L represents the inductance and C the equivalent capacitance to ground of the external reactance. R_1 represents the ordinary damping resistance of the circuit and might have been placed in series with the inductance without altering the final conclusions. R_B represents the breaker resistance. The vibration modulus of the high-frequency oscillation is:

$$p=-\frac{1}{2\frac{R_1R_B}{R_1+R_B}C}\pm\sqrt{\frac{1}{\left(2\frac{R_1R_B}{R_1+R_B}C\right)^2}-\frac{1}{LC}} \qquad (1)$$

Since $2\frac{R_1R_B}{R_1+R_B}C \gg LC$ the angular frequency of the oscillation is

$$\omega=\sqrt{\frac{1}{LC}}$$

and C may be determined, the inductance being known. The envelope of the high-frequency oscillation will be of the form

$$y=\epsilon^{-\frac{R_1+R_B}{2R_1R_BC}t}$$

and this envelope plotted on semilog paper has the slope

$$D=-\frac{R_1+R_B}{2R_1R_BC} \qquad (2)$$

Figure 12 shows the magnitude of the envelope of the recovery voltage during two tests, test B being at reduced air pressure.

These curves are direct evidence of the occurrence of displacement during the operation of the breaker. Curve A made at high air pressure shows that at all times after the current zero, the damping of the circuit was due to a constant value of resistance, hence the breaker resistance must have been infinite.

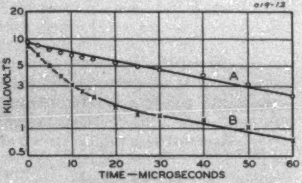

Figure 12. Magnitude of recovery voltage envelopes

Curve *B* at reduced air pressure

Curve B which indicates a greater rate of damping for 15 microseconds becomes parallel to curve A showing that in this case also displacement occurred although the air pressure and velocity were greatly reduced. The slope of curve A is given by $D=-\frac{1}{2R_1C}$ and the value of R_1 is found from the relation

$$R_1=-\frac{1}{2DC} \qquad (3)$$

If R_1 is known it is possible to find R_B from equation 2 above, that is

$$R_B=-\frac{R_1}{1+2R_1DC} \qquad (4)$$

The above method of calculation is useful for determining the breaker resistance after the first peak of the recovery voltage. For values previous to this peak the exact value of the voltage envelope is in doubt and the breaker current can be determined as follows:

Referring to figure 11

$$e_L=-L\frac{di_L}{dt}$$

and the slope of the inductance current is given by[8]

$$\frac{di_L}{dt}=\frac{-e_L}{L}$$

The capacitor current is found from the relation $i_C=-C\frac{de}{dt}$ consequently both i_C and i_L can be drawn if any single value of inductance current can be determined. Since it is evident that i_0 must be zero when $e_B=0$, it is possible to determine the inductance current at this instant by $i_L=i_0-i_C=-i_C$.

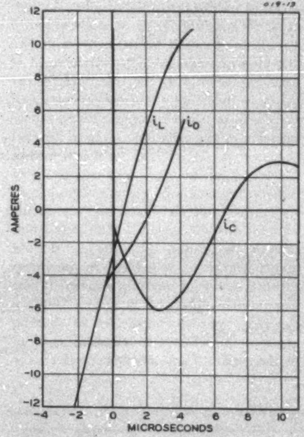

Figure 13. Construction of currents, test B

These currents as plotted for the low-pressure air test are shown in figure 13.

The resistance of the breaker is then found from

$$\frac{R_1R_B}{R_1+R_B}=\frac{e_B}{i_0}$$

or

$$R_B=\frac{1}{\frac{i_0}{e}-\frac{1}{R}} \qquad (5)$$

It was found that this latter method is useful only until the time of the first recovery voltage peak as the mechanics of constructing the inductance current introduce considerable error.

In figure 14 are shown the complete current, voltage, and resistance characteristics for the air breaker operating with low air pressure.

Points marked with circles are calculated from equation 5 and points marked with crosses from equation 4.

Appendix B†

Calculation of a Resistor for Controlling Voltage Recovery Rates

The ohmic value of resistance to limit the recovery rate to a given value is given by the equation:

$$R=\frac{\frac{de}{dt}\times10^6}{\frac{di}{dt}}=\frac{\frac{de}{dt}\times10^6}{2\sqrt{2}\pi fI}=$$

$$\frac{\frac{de}{dt}\times10^3\times V\times\sqrt{3}}{2\sqrt{2}\pi f\times kva}=3.25\frac{de}{dt}\times\frac{V}{kva}\text{ (for 60 cycles)}$$

where

R = the ohmic value of the resistance
$\frac{de}{dt}$ = the specified limit of recovery rate in volts per microsecond
$\frac{di}{dt}$ = the limit of the rate of change the instantaneous value of short-circuit current just prior to interruption
f = system frequency
I = maximum available interrupting current in rms symmetrical amperes
V = application voltage in line-to-line volts
kva = interrupting duty in kilovolt-amperes, three phase

This value of resistance should be used on each pole.

The amount of active material required may be based on the allowable temperature rise for one operation on the assumption that all of the heat developed remains in the active material. If the breaker is properly designed, the maximum duty will be about one-quarter cycle at $1^1/_2$ times leg voltage. The amount of active material is then given by:

$$M=\frac{Q}{h\Delta T}=\frac{950\times.75\times(\text{kv})^2}{240h\,\Delta T\times R}=2.97\frac{(\text{kv})^2}{h\cdot\Delta T\cdot R}$$

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† Prepared by W. F. Skeats.

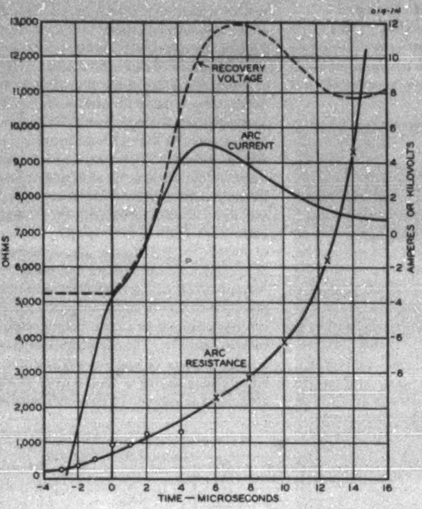

Figure 15. Breakdown in cross-blast air circuit breaker

3.7 microseconds per millimeter

Figure 14 (left). Variation of leakage current and resistance with time

Figure 16 (right). Breakdown of an arc which has been cooled to the point of interruption

A—Line voltage
B—Arc voltage
C—Current

where

M = active material required in pounds for each pole
Q = heat generated in resistance in Btu
h = specific heat of material; Btu per pound per degree Fahrenheit
ΔT = allowable temperature rise for one operation, Fahrenheit
R = resistance in ohms
kv = application voltage in line-to-line volts

Appendix C

The type of breakdown that occurs in the air-blast breaker is of particular interest because it indicates the nature of interruption to be similar to the breakdown of a dielectric. A cathode-ray-oscillograph record of such a breakdown is shown in figure 15.

The recovery voltage rises at a rate which is in accordance with circuit characteristics and then collapses to zero voltage in less than two microseconds.

This failure of the cross-blast air circuit breaker was intentionally caused by reducing air pressure and increasing the rate of rise of recovery voltage. The character of the failure is similar to that of the disruptive breakdown of a rod gap in air.

As there is nothing shown in the oscillograph record which would indicate progressive breakdown, we conclude that it was occasioned by the overstressing of the cold air of the blast which had been inserted between the arc path ends after current zero. In other words, there was an interruption by displacement but breakdown occurred due to the fact that the recovery voltage rose faster than the dielectric strength.

In contrast with this sharp breakdown the slowness of thermal breakdown is illustrated by the magnetic oscillograph record figure 16.

It will be noted that there were attempts to interrupt which were partially successful and finally the arc was extended to a point where successful interruption was obtained. The rate of rise of recovery voltage is less than ten times faster than the 60-cycle voltage wave which is less than 40 volts per microsecond and indicates that its rate of rise was limited by leakage current.

The collapse of voltage T_1 to T_2 required approximately 2,000 microseconds which indicates clearly that it was progressive in character and was caused by a gradual decrease in the resistance of the arc path.

These two cases have been chosen as they are typical of the extremes. Figure 15 is the disruption breakdown following displacement with no deionization phenomena visible. Figure 16 is the progressive breakdown from a reversal of the deionization process with no displacement in operation.

Cases may be found where displacement and deionization by cooling may both play a part in arc interruption; however, in the cross-blast air circuit breaker, displacement will account completely for interruption at low currents and be a predominating factor even for the high currents and recovery rates.

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Magnetic "De-ion" Air Breaker for 2,500—5,000 Volts

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Synopsis: The theory of quenching an arc by deionizing the normally conducting arc stream, utilizing a gas blast produced by means of an intense transverse magnetic field, is elaborated. Application of design principles dictated by this theory has made it possible to develop a new form of air circuit breaker for 2,500–5,000-volt service. These breakers are completely described, the theory of the De-ion interrupters is discussed, and test results are submitted. The problem of adequately enclosing the breakers is considered and it is shown that excellent interruption can be obtained in an enclosure of small over-all dimensions.

INTERRUPTION of 2,500- and 5,000-volt circuits capable of producing fault currents equivalent to 150,000 kva, by breakers using air as the interrupting medium, is not readily accomplished without special arc-quenching chambers. In open air impractically long arcs are necessary to clear circuits supplied at these voltages. Comparatively simple air circuit breakers have been built utilizing magnetic blowout means for lengthening the arc in an interrupting chamber of reasonable dimensions, but even with the constricted path available for the arc in such chambers, it is necessary to lengthen the arc to approximately five feet in order to achieve interruption. The energy dissipated in such an arc is necessarily high because of the long arc length and consequent high arc voltage. Interruption in such cases is not achieved primarily by establishing a process of rapid deionization in the vicinity of current zero, but depends rather upon building up the arc voltage to such an extent that restriking after an early, forced, current zero does not take place, even though intensive deionization is not provided during the time of substantially low recovery voltage following the current zero.

It is usually necessary to mount air circuit breakers for 2,500–5,000-volt applications in compact metal-clad switchgear. Consequently, efficient interruption with a minimum of arc energy, size of interrupting chamber, and formation of ionized gases is required. These features call for the development of arc-interrupting means which will open the circuit using a comparatively short arc, relatively low energy input, and effective deionization at the first current zero. In the next sections a circuit breaker is described which has these qualities and which is shown to be adequate for 150,000 kva at 2,500 or 5,000 volts when mounted in enclosures of small dimensions.

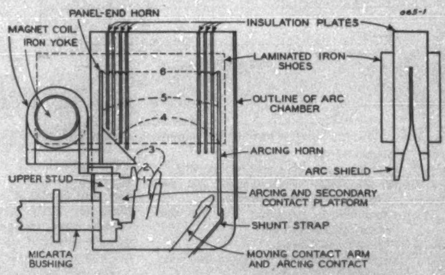

Figure 1. A diagrammatic sketch showing partial sections of the magnetic De-ion interrupter

The dotted lines 1, 2, 3, 4, 5, and 6 indicate progressive positions in arc interruption

De-ion interrupter

To interrupt an a-c circuit with minimum dissipation of energy, a switch is required which does not substantially impede the flow of current until it normally reaches zero, and which quickly takes on insulating properties thereafter. This means appears to be more advantageous than depending upon a high arc voltage and the associated high energy input to effect interruption. Synchronization of interruption with a current zero may be obtained by separating switch contacts and forming an arc which must be controlled to develop dielectric strength, following the current zero, more rapidly than voltage is impressed by the circuit.[1] This deionization action may be accomplished in a number of ways—for example, by the use of a blast of gas produced by decomposing a gas-forming material,[2] or by the use of a plurality of copper plates with short arcs between them to take advantage of the spectacular dielectric recovery at the cathode of an arc.[3] A more recent method which employs a magnetically produced gas blast for deionization was presented by Ludwig and Grissinger in their paper "A New High-Capacity Air Circuit Breaker".[4] An experimental review of the various interrupting means available indicated that this latter method was most suitable for interruption of high currents at 2,500 and 5,000 volts, because of its simplicity, effectiveness, and freedom from undesirable flame and noise.

Application of the magnetic-deionizing principle results in constructing an interrupting chamber as shown diagrammatically in figure 1. The chamber consists of a large number of non gas-forming insulating plates having V-shaped slots which are spaced apart and placed at right angles

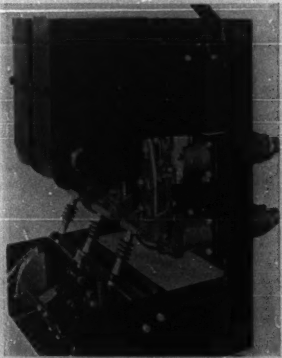

Figure 2. A right view of a 2,000-ampere magnetic De-ion breaker for 2,500–5,000-volt service

Rated interrupting capacity 150,000 kva. Right-pole arc chamber removed

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Paper 40-65, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940. Manuscript submitted November 10, 1939; made available for preprinting December 20, 1939; released for final publication March 27, 1940.

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to the arc path. During operation an arc is drawn between separating contacts, and is then magnetically moved toward the closed top of the slots in the insulating plates. At this position the presence of a strong magnetic field imparts to the electrons in the arc an upward component of velocity. They in turn bombard gas particles and produce an actual blast of gas perpendicular to the arc, which action requires the arc continuously to ionize fresh gas in considerable quantities. When current zero is reached this action continues in a sufficient degree effectively to deionize the plurality of short lengths of the arc near the edges of the plates. Dielectric strength is consequently established.

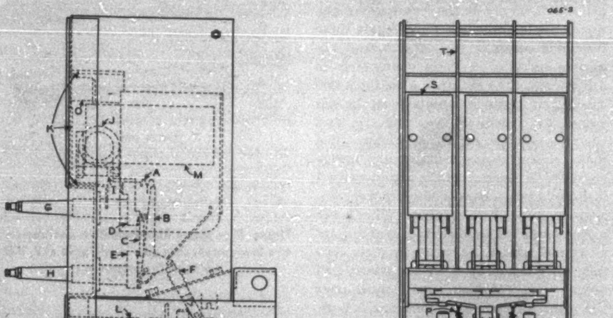


Figure 3. Front and side views of the new De-ion circuit breaker for 150,000 kva interrupting capacity

A—Arcing- and secondary-contact platform
B—Moving-contact arm
C—Main-contact bridge member
D—Upper contact stud
E—Lower contact stud
F—Insulating push rod
G—Upper stud bushing
H—Lower stud bushing
I—Male adapter of magnet coil which engages the female connector of the panel-end horn of the arc chamber
J—Magnet coil
K—Panel barriers
L—Solenoid mechanism
M—Magnet shoes
N—Removable operating handle
O—Insulating magnet bushing
P—Accelerating spring
Q—Air dash pot
R—Shunt trip
S—Removable arc chamber
T—Interphase barriers

The insulating plates are made of a material which does not form gases in order to prevent unnecessary display of ionized flame in the air space above the interrupting chamber. Flame from the arc core must pass upward between the closely spaced plates for a distance of several inches before reaching free air space. During this passage contact with the cool walls of insulating material deionizes the flame so that upon reaching the free air space beyond the chamber there is no danger of external flashover of the arc chamber or to metal parts of the enclosure in which the breaker is mounted.

Figure 4 (left). Oscillogram of a 1,900-volt 39,000-ampere rms single-phase interruption

A—Arc voltage measured at breaker terminals

B—Travel record

C—Current through breaker

Figure 5 (right). Oscillogram of a "closing-opening" single-phase interruption with 1,800 volts and 38,500 amperes rms

A—Voltage at breaker terminals

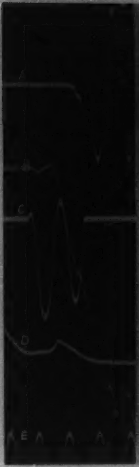
B—Current through breaker

C—Travel record

This principle of deionization is applicable to a wide range of voltage. The magnetic-field strength, thickness of the plates and the space between the plates, and the shape of the slot determines the volts per plate at which a given current can be interrupted. The number of plates used may be suitably chosen along with the other parameters to make the interrupter adequate for specified current and voltage ratings.

In the case of the 600-volt air breaker previously described[4] sufficient field strength to give the required volts per plate was obtained by the use of iron plates built in above the insulating plates and in the same plane with them. However, for the higher-voltage circuits iron has been added in the form of a laminated structure about the chamber, and a multiturn coil is connected to provide a very intense field. The iron structure is shown diagrammatically in figure 1.

The figure also shows an outline of one of the deionizing plates. The slot into which the arc is forced by the magnetic field is long and quite narrow at the top. When the current in the arc is high during most of the half cycle preceding interruption, the arc cannot reside in the uppermost portion of the slot because the cross-sectional area required by its core is large compared to that available in the upper slot region. The effect of this construction is to permit the arc to operate with a low voltage drop until just prior to current zero, and thus the energy dissipated in the interruption is reduced. The arc voltage, as shown on the oscillograms in figures 4 to 9, indicates the effectiveness of the slot design in limiting the arc voltage. The design of the slot differs from that of lower-voltage breakers which are also applied for d-c interruption.



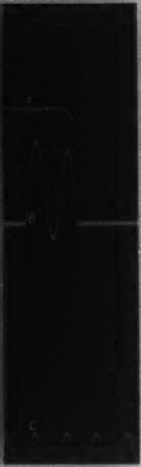


Figure 6 (left). Oscillogram of a 3,000-volt 27,500-ampere-rms single-phase interruption

A—Arc voltage measured at breaker terminals
B—Travel record
C—Current through breaker
D—Shunt-trip-coil current
E—60-cycle timing wave

Figure 7 (right). Oscillogram of a 4,000-volt 22,000-ampere-rms single-phase interruption

A—Arc voltage measured at breaker terminals
B—Current through breaker
C—60-cycle timing wave

Plates approximately one-eighth inch thick are found experimentally to be most suitable. The space between the plates may be varied from one-eighth to one-fourth inch, depending on the magnitude of the current to be interrupted and the voltage. An arc length of $9^1/_2$ inches between horns at the two ends of the chamber is sufficient for interruption of 150,000 kva at either 2,300 or 5,000 volts. Rms voltages as high as 550 volts per inch or 140 volts per plate have been interrupted when the short-circuit current was approximately 22,000 amperes. An interrupting device of this type remains substantially constant in kilovolt-amperes and it is found that higher currents can be opened at lower voltages or that higher voltages can be cleared satisfactorily if the current is less.

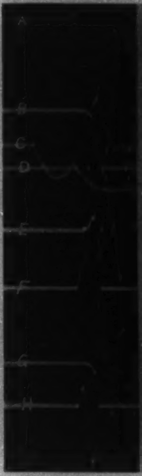
In order to make all plates effective in the longer chutes for the higher circuit voltages, arcing horns are necessary at both ends of the chamber. These consist of copper bars placed vertically at each end of the chute. The lower terminals of the panel-end horn are sloped toward the arcing contacts of the breaker to facilitate transfer of the arc to them. The horns are slotted to release gas pressure which would tend to prevent intimate contact between the arc and the horn at the moment of transfer. The multiturn coil at one end of the chute is electrically connected between the arcing contact at that end and the horn. Therefore, the coil does not pass current except after the contacts have parted and the arc terminal has transferred to the horn, thus introducing the coil into the circuit.

Air-Breaker Construction

Breakers for 150,000 kva can be constructed in such reasonable dimensions that one can be mounted above another if desired, and therefore the draw-out type of design is particularly advantageous. The fabricated steel frame, as shown in figure 2 and figure 3, serves to support the four component subassemblies of the breaker, and has wheels attached at the bottom for withdrawal from a metal cubicle. The subassemblies are the contact structure, electric mechanism, arc chutes, and barrier system.

The contact structure of an air breaker has two functions to perform, that of carrying the load current and of drawing the arc which serves as the interrupting element. It is important to segregate these functions carefully in order to save the current-carrying or main contacts from any effects of the arcing. Also, the interrupting or arcing contacts must be designed to withstand the magnetic and thermal stresses placed on them. The main contacts are self-aligning bridge members of copper and the actual contact is made between bars of silver-nickel composition brazed to copper. The lower main contact is protected from arcing by a parallel shunt. The upper main contact is shunted by both secondary and arcing contacts which open in turn after the main contacts have parted. A horizontal overlapping barrier system isolates the main contacts from gases thrown downward by the arc.

The necessary relative motion between the two upper contacts and the main contacts is obtained by limited travel of the upper contacts on the stationary-contact side of the switch. The upper contact springs are enclosed by a platform member which has limited travel, and are thereby well protected from burning. An inverted U shunt is also enclosed within this platform member and is designed so that the magnetic force set up by the short-circuit current presses the secondary contacts tightly together in addition



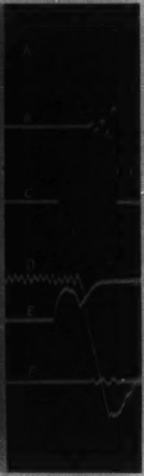


Figure 8 (left). An oscillogram of a three-phase interruption at 2,300 volts, 124,000 kva

A—Travel record
B—Arc voltage of phase 1 to neutral, 1,640 volts
C—Shunt-trip-coil current
D—Breaker current of phase 1, 27,500 amperes rms
E—Arc voltage of phase 2 to neutral, 1,640 volts
F—Breaker current of phase 2, 39,000 amperes rms
G—Arc voltage of phase 3 to neutral, 1,640 volts
H—Breaker current of phase 3, 26,000 amperes rms

Figure 9 (right). Representative oscillogram of a low-current shot on a single pole at 2,300 volts, 630 amperes rms

A—60-cycle timing wave
B—Arc voltage across breaker terminals
C—Current through breaker
D—Travel record
E—Shunt-trip-coil current
F—Indicator wave

to the springs. Parallel-path protection is thus provided for the main contacts to prevent them from opening any portion of the interrupted current. Tungsten-alloy contacts are provided for the secondaries and arcing tips.

Insulation of the contact structure and mechanical support is obtained by Micarta bushings through which the electrical connection is carried to the rear of the breaker.

The electric operating mechanism together with trip-free levers comprises a unit which is mounted horizontally in the base of the breaker. The mechanism actuates a cross bar which is mechanically connected to each moving contact arm by an insulating operating rod. The principal advantage of the mechanical construction is high speed, which results in only a half cycle of arcing at most currents.

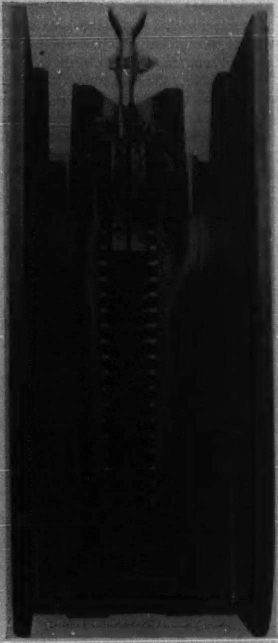
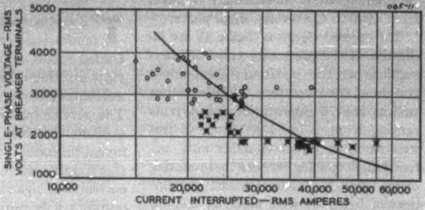


Figure 10. Arc chamber which was used during 27 interruptions at maximum short-circuit currents and also for 76 interruptions at currents of 14,000 amperes and below. The insulating plates are in satisfactory condition for further service

The arc chamber previously described is withdrawn from the breaker by pulling forward and using the laminated iron field structure as a support.

The function of the barrier system is to isolate electrically the live phases from each other and from the metal enclosures, particularly during the interrupting period when free ionized flame tends to expedite flashover. Obviously, the requirements of the barrier system are lessened if the interrupting chamber is made efficient so that only a small quantity of flame is allowed to escape. An enclosed space is provided by the barrier system above each arc chamber for the discharge of hot gases, which would otherwise build up pressure above the arc in the chute and hinder its interrupting ability. In this space the gases mix with cool air, and residual flame is suppressed. Muffling action is also secured by the barriered space. The barrier structure is built as a unit of insulating material and is supported from the breaker frame at the top.

Figure 11. Magnetic De-ion circuit breaker single-pole interruption tests



Crosses indicate tests made on a single arc chamber whose insulating plates were spaced one-fourth inch apart

Circles indicate tests made on a similar arc chamber whose insulating plates were spaced one-eighth inch apart

Test Results

Interrupting tests have been made on the breaker through a voltage range of 1,900 to 4,000 volts single phase, using the maximum plant capacity of the Westinghouse high-power laboratory. Figure 4 is an oscillogram showing the interruption of 39,000 amperes rms at 1,900 volts across one pole of the breaker. The equivalent three-phase kva is 150,000. The voltage element connected across the breaker terminals shows that the arc drop remains comparatively low until nearly the end of the single half cycle of arcing.

Figure 5 shows a closing-opening operation at the same voltage and with the same plant setting. The crest value of the current following closure was 89,000 amperes, and the rms current interrupted was 38,500 amperes.

Figure 6 shows the interruption of 27,500 amperes rms at 3,000 volts single phase. In figure 7, the interruption of 22,000 amperes rms at 4,000 volts single phase is illustrated. Both oscillograms show less than a half cycle of arcing.

A three-phase interruption record is illustrated in figure 8. The line-to-line potential in this case was 2,300 volts. The total kilovolt-amperes interrupted was 124,000, and the rms current opened by the center phase was 39,000 amperes rms. This test was made with the breaker completely enclosed in a metal cubicle which was at ground potential. Demonstration such as flame was entirely absent, and noise was at an acceptably low level.

Interrupting tests have been made throughout the current range with 2,300 volts across a single pole. The time of interruption increased to 20 cycles at 22 amperes, and then decreased to one-half cycle at 9 amperes. All interrupting tests were made with only reactance in the circuit, and consequently at low power factor. The typical oscillogram of figure 9 shows an interruption of 630 amperes rms at 2,300 volts single phase.

The arc chamber is not damaged by high-current short circuits. Figure 10 shows a photograph looking up into an arc chamber which had been used during 27 interruptions at the maximum short-circuit current of the laboratory, and in addition 76 interruptions of currents of 14,000 amperes and below at 2,300 volts single phase. The oscillograms of figures 4, 5, and 9 and phase three of figure 8 were made using this De-ion chamber.

In figure 11 points are plotted to semilog scale of currents interrupted at various single-phase voltages. Sixty-one test points are shown, based on currents interrupted up to 55,000 amperes rms at 1,900 volts, and voltages as high as 4,000 volts, single phase. A curve corresponding to 150,000 kva is also drawn, and the number of successful operations above the curve and throughout the voltage range indicate a good margin of safety in the breaker over this rating. This figure also indicates that the kilovolt-amperes which can be interrupted is substantially constant from 1,900 to 4,000 volts, single phase.

The crossed circles of figure 11 are indicative of the previously noted 27 interruptions at the maximum short-circuit current of the laboratory. These tests as well as the 76 interruptions of currents ranging from 9 to 14,000 amperes rms were made on an arc chamber with one-fourth-inch spacing between the insulating plates, figure 10. The 34 circled interruptions of figure 11 were made on this arc chamber using similar insulating plates spaced one-eighth inch apart.

Conclusions

The high efficacy of the new De-ion circuit breaker to function at and above 150,000 kva with 2,500 to 5,000 volts demonstrates the practicability of the interrupter utilizing a gas blast which is produced by means of an intense transverse magnetic field. This interrupter is particularly desirable because the noise and demonstration usually found in air-circuit-breaker practice is greatly reduced. The ionized gases and other by-products of arc interruption are sufficiently controlled by the interrupter that unusual barrier arrangements are not needed. The new De-ion air circuit breaker is a particularly compact and sturdy device for use in modern clad switchgear, or for separate mounting.

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2. The Expulsion Fuse, J. Slepian and C. L. Denault. AIEE Transactions, volume 51, March 1932, page 157.

3. Structural Development of the De-Ion Circuit Breaker, R. C. Dickinson and B. P. Baker. AIEE Journal, February 1929.

4. A New High-Capacity Air Breaker, L. R. Ludwig and G. G. Grissinger. AIEE Transactions, volume 58, 1939, page 414.

Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Design and Construction of High-Capacity Air-Blast Circuit Breakers

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THE air-blast principle of circuit interruption is a recent introduction to the United States, although interrupting devices utilizing compressed air have been available in Europe for a number of years. The service performance of European breakers has been satisfactory, but inherent limitations prevented adoption in this country where large values of fault current and high rates of rise of recovery voltage are the rule rather than the exception. Exhaustive tests on forms of available air breakers heretofore available have shown that approximately 40,000 amperes represents the maximum current that can be safely interrupted at generator voltage, and then only under conditions of moderate rate of rise of recovery voltage. This fact alone made it essential, if air breakers were to be utilized, to develop an entirely different type, since applications in this country have commonly required oil breakers with interrupting ratings up to 60,000 amperes at 15 kv. Even this value of current does not represent the maximum requirement, since an increasingly strong demand exists for ratings at least up to 100,000 amperes at 15 kv or 2,500,000 kva.

It was believed that the principle of arc interruption by means of compressed air was sound, particularly for the higher interrupting ratings, so a development program was undertaken to overcome the previous limitations. Breakers with interrupting ratings as high as 1,500,000 kva (60,000 amperes at 15 kv) have already been built and tested, and design tests have been made which assure the availability of breakers of higher ratings when required by the industry. Two breakers rated 500,000 kva have been in service since early in November 1939, on the Consolidated Edison system. A number of 250,000-kva breakers of this same general type have been supplied for use on systems of the American Gas and Electric Company.

Theory of Operation

The principle of interruption of the air-blast breaker is similar in many respects to the principles that have been thoroughly substantiated by both service experience and factory test with the oil-blast breaker. Fundamentally the theoretical basis of operation of the breaker is the displacement of the arc stream and the replacement of the arc products with insulation at a rate which will prevent break down of the dielectric by the recovery voltage. As applied to the air-blast breaker a stream of air under pressure displaces the arc stream and at the zero point of the current wave, purges the interrupting chamber of the hot gases. The flow of air across the separating contacts then provides the necessary dielectric strength at the required rate to prevent breakdown by the recovery voltage. A more complete exposition of this theory as applied to the air-blast breaker is contained in a companion paper by Messrs. Prince, Henley, and Rankin (*Transactions pages 510–17*).

Design Features

The actual design features of the breaker may be broken down into four major components in order to facilitate description:

1. Arc chute and movable contact
2. Air-blast valve
3. Operating mechanism
4. Control valve

The arc chute is a box structure of simple, compact design combined with great mechanical strength. Within this structure are the interrupting chamber, stationary contacts, cooling plates, arc barriers, and exhaust vent. Figure 1 illustrates the arrangement of the various parts of a typical chute designed for a 15-kv breaker with an interrupting rating of 500,000 kva. This chute is mounted in the breaker cell on porcelain insulators, and arranged to be easily removable for inspection.

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H. E. Strang is managing engineer, large oil circuit breaker division, General Electric Company, Philadelphia, Pa.; A. C. Boisseau is with the same company.

The section at the right containing the fixed contacts, is the chamber in which the actual interruption of the arc occurs. The sides consist of blocks of insulation material with a contour designed to force the blast of air directly across the arc stream. A false top barrier containing an opening with relatively small clearances, through which the movable contact passes, forms the top while the back plate of the arc chute is also the back of the interrupting chamber and contains the entrance orifice for the air blast. The fixed-contact assembly is at the bottom of the chute, centrally located between the two insulating blocks.

Arc barriers placed at the exit of the interrupting chamber divide this portion of the interior of the chute into four passages each equipped with cooling plates. A fifth passage which also contains a cooler exists between the false and actual top barriers. All of these passages exhaust into a vertical vent where additional cooling members are installed.

The orifice in the back plate of the arc chute is located so that the normal flow of air is directly across the fixed contacts and in the plane of the longitudinal axis of the tips of the fingers. This flow of air forces the arc between the streamlined side members of the interrupting chamber and into the exit passages at the front of the chamber. In order to obtain short arc duration and minimum deterioration of parts, approximately 15 cubic feet of free air at 150 pounds initial pressure are required for a closing-opening operation on a triple-pole 15-kv 500,000-kva breaker while a 1,500,000-kva breaker requires approximately 25 cubic feet for such an operation.

The arc barriers are for the purpose of cutting the arc into sections and securing a maximum rate of increase in dielectric strength of the arc path. These barriers are of different thickness in order to equalize deterioration under the various interrupting conditions within the rating of the arc chute. The area of the passage enclosed by the barrier is proportional to the current to be interrupted while the number of passages is determined by the maximum rate of rise of recovery voltage. For 15-kv applications, tests have shown that normal interruptions up to full rating usually utilize only the first passage. The additional passages provide adequate factors of safety where high rates of rise of recovery voltage may be encountered and for that reason three barriers are used in the 15-kv arc chutes to form four exhaust passages.

The coolers are composed of stacks of thin copper plates located in the plane of the exhaust. These perform the function of cooling the exhaust gas to a point where it will not cause restrike on the outgoing side of the chute. The size and number of these stacks are determined by the current-interrupting rating.

The passage above the false top barrier allows the moving contact to be withdrawn without the external discharge of hot gases. It provides a chamber in which the time for the contact to pass through is sufficiently long to permit the air flow to completely purge the arc chute following circuit interruption, before uncovering the opening in the top barrier. The action within the section formed by the false and top barriers provides what is in effect a turbulent seal to prevent discharge around the moving contact without the necessity of a complex mechanical seal. The moving contact then finishes its stroke at which time it is completely withdrawn from the arc chute, thus providing the positive insulation of a clear air-break distance between open contacts. This also facilitates contact inspection, since the blade and its arcing tip are readily visible from the front of the cell when the breaker is open.

The exhaust gases are collected in a vent pipe of insulation material, where they are permitted to further cool and expand. Complete cooling and elimination of objectionable noise are assured by the final set of cooling plates located at the upper end of the vent pipe, where discharge takes place in the open air.

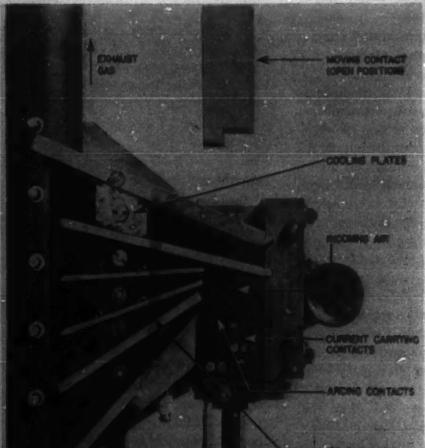

Figure 1. Cross section of arc chute of 500,000-kva breaker

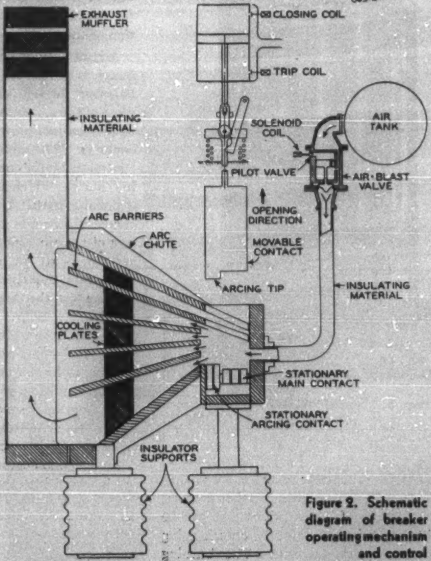

Figure 2. Schematic diagram of breaker operating mechanism and control

The stationary contacts consist of a suitable number of primary fingers to carry the normal rated current without excessive heating and secondary fingers consistent with the maximum current to be interrupted. The primary fingers are of the short-wipe line-contact silver-surfaced type that have been employed on various types of oil breakers for a number of years. These fingers are spring loaded with one end in contact with the stationary block and the other in contact with the movable blade, assuring a high-pressure silver-to-silver connection with the complete elimination of flexible shunts.

The secondary contacts consist of heavy copper fingers, surfaced on the burning area with a silver-tungsten alloy to minimize deterioration and prolong the life of the entire stationary contact assembly. These fingers are provided with a copper shunt to eliminate burning caused by vibration at the instant the movable blade engages the stationary contact, and facilitates current transfer on the opening operation. The secondary fingers are arranged to engage first and break last and, thereby, prevent burning on the primary contacts. An arcing plate has been incorporated in the design to further increase the number of interruptions that can be obtained without seriously deteriorating the stationary contacts. The burning surfaces of this plate are surfaced with a silver-tungsten alloy and are so placed that the arc is immediately transferred to it by the blast of air so the majority of the arcing occurs at this point instead of on the secondary fingers. All of these contacts and the stationary arcing plate are mounted as a unit assembly, which is easily removable from the chute for inspection or renewal.

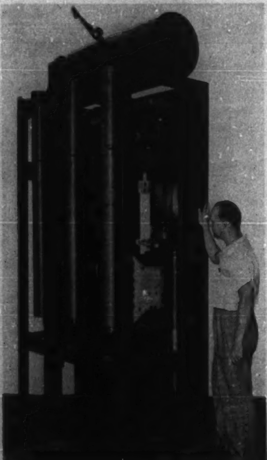

Figure 3. Typical 500,000-kva breaker complete in steel cell

The movable contact is a flat blade of copper, completely silver-surfaced, and equipped with a silver-tungsten alloy arcing tip. Silver-surfaced fingers, similar to the primary fingers, are mounted on the movable contact guide to provide a connection between the blade and entrance bushing. This method of making connections between movable and stationary current-carrying members is simple and compact, eliminating flexible leads or pigtails. Adequate tests have proved it to be satisfactory under all possible operating conditions.

In order to arrive at a satisfactory design of arc chute, numerous tests were made on various configurations and in many cases the action that takes place within the chute during interruption, was photographed.* The following description of the operation is, therefore, based on analysis of these high-speed photographs, as well as magnetic and cathode-ray oscillograms, which studied jointly, were of considerable aid in understanding the underlying theory and actual operation of the air-blast breaker.

Air, at a tank pressure of 150 pounds gauge, is introduced into the interrupting chamber through an orifice in the back plate of the arc chute and is so timed as to be present just prior to the parting of the contacts. The direction of flow of air is controlled by the contour of the interrupting chamber which also confines the arc to a definite path. Under this controlling condition, the flow of air forces the lower root of the arc from the tips of the secondary fingers to the surface of the arcing plate and is effectively prevented from restriking on the finger contacts by the positive flow of air. Arcing occurs on the secondary fingers for only a small fraction of the total arcing time and it is, therefore, possible to form a very accurate estimate of the condition of the stationary contacts from an inspection of the movable blade, which is fully exposed when the breaker is in the open position.

As the contacts move further apart the arc is forced into the first passage of the chute and lengthens rapidly. At the first current zero the stream of air removes the heated gases from between the contacts, placing a stream of clean air under pressure across the normal arc path. If the separation of contacts is sufficient for the dielectric strength of the air to withstand the recovery voltage, the arc has been successfully cleared. Inasmuch as the air in the interrupting chamber is under pressure, its dielectric strength is considerably greater than under normal atmospheric conditions and is sufficient to prevent restriking of the arc with only a short separation of the contacts.

If the contact separation at the first current zero has not been great enough to withstand the recovery voltage, the arc restrikes and interruption will occur at the next current zero. As the contact separation increases, additional barriers are brought into action forcing the arc to follow a path of rapidly increasing length. The speed of separation of the contacts is approximately three inches per cycle during this part of the stroke and by the second current zero from parting of the contacts, the separation is adequate to prevent the recovery voltage from causing the arc to restrike.

The movable contact is finally completely withdrawn from the chute, and during its passage through the last section, the gases are swept from the arc chute, and the air blast is shut off before uncovering the opening in the top barrier.

During the closing operation a blast of air is started across the contacts just prior to the time they meet. This very effectively prevents the arc from striking on the current-carrying parts and sweeps clear of the chute any gases which may be formed when the arcing contacts meet.

Blast Valve

For the arc chute to function properly, it is essential that a reliable valve be used to control the admission of the air blast. A high-speed differential-type solenoid-pilot operated valve has been designed for this purpose. The valve consists essentially of a bronze body with integrally cored air passages, a piston-operated main valve, and a solenoid-operated pilot valve. Poppet-type valves in conjunction with moderately soft seats have been utilized for their simplicity, speed of operation, and low leakage factor. It is of primary importance that the pressure drop of the valve be kept as low as possible and every effort has been made to provide a straight-line flow with a minimum of obstructions. Under intermittent flow conditions at a rate of 7,800 cubic feet of free air per minute, the pressure drop through this valve

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* "The Cross-Air-Blast Circuit Breaker", D. C. Prince, W. K. Rankin, and J. A. Henley, *Transactions pages 510–17.*

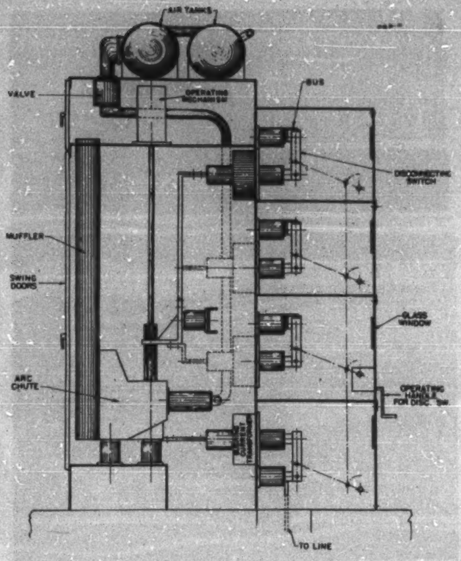

Figure 4. Application of air-blast breaker to metal-enclosed cubicle arrangement, complete with bus, disconnecting switches, and current transformers

is only about 10 pounds with an initial tank pressure of 150 pounds gauge. High speed of operation has also been obtained as the over-all operating time, from energizing the pilot valve control to complete opening of the main valve, is less than 2 cycles on a 60-cycle basis.

The blast valve is opened by energizing a solenoid, unbalancing a spring-loaded pilot valve. This allows the air to escape from above the piston at a greater rate than can be supplied by a small bleed hole from the high-pressure system. The unbalance in pressure on the two sides of the piston causes the piston to move, lifting the main valve from its seat. At the end of the piston stroke a small valve in the piston face is automatically opened, permitting a rapid flow of air to the area above the piston, but not sufficient to influence the operation as long as the pilot valve is open. This arrangement assures rapid closing of the pilot valve. With the valve in the closed position, the air under pressure holds it closed and prevents leakage.

Operating Mechanism

Mechanical operation of the contacts of this breaker is by means of a pneumatic mechanism, utilizing air pressure for both the closing and opening strokes.

Essentially the operating mechanism consists of two units, a double-ended driving cylinder and a straight-line linkage mechanism. Figure 2 shows a schematic diagram of the mechanism and control-valve equipment. The piston is directly connected to the straight-line linkage mechanism through a pair of slotted links which permit a short stroke of the piston before motion is transmitted to the contacts. To close, air pressure is applied to one end of the cylinder until the breaker is completely closed and latched in this position by a prop. On completion of the stroke, a mechanically operated switch opens to de-energize the closing control-valve solenoid.

The blast of air on closing is controlled by an interlock switch which both energizes and de-energizes the solenoid on the blast valve pilot at a predetermined point in the stroke.

The opening operation is practically the reverse of the closing operation. Air is applied to the opening side of the piston, and the first part of the motion provided by the slotted link arrangement is utilized to remove the latch. The accelerating springs rapidly bring the movable contact up to speed which is maintained by the air pressure until the blade passes the false barrier in the arc chute. At this point motion is decelerated by the action of a special dashpot, before allowing the operation to be completed. As in the case of the closing operation, air completes the opening stroke and is then cut off by a mechanically operated switch.

At the point where the blade is passing through the last section in the arc chute, an interlock switch opens to de-energize the blast of air. The mechanically operated switches used in the control of the mechanism and blast valve are directly

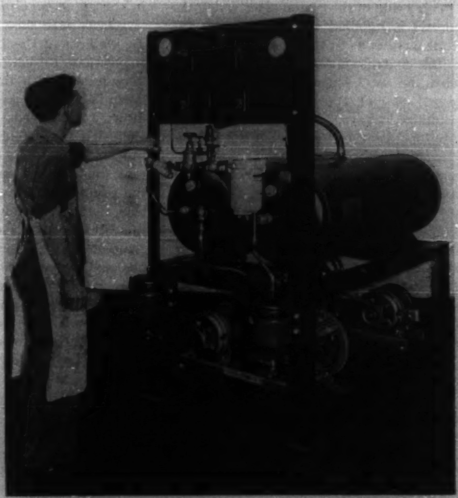

Figure 5. Typical compressor equipment for battery of 12 500,000-kva breakers

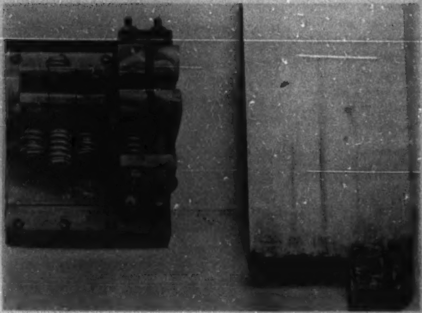

Figure 6. Condition of stationary and movable contacts following test

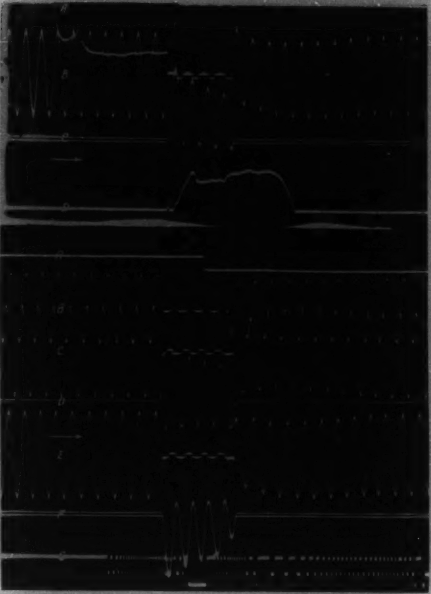

Figure 7 (right). Oscillogram of three-phase closing-opening test, interrupting 680,000 kva at 14,500 volts

connected to the mechanism. They are easily and accurately adjusted by movable cam surfaces and are of a simple and rugged construction.

Control Valves

On closing the operation is controlled by energizing the closing control-valve solenoid to admit air to the closing end of the cylinder after which the remainder of the operation is performed automatically by the sealing-in circuits and interlock switches. The opening stroke is initiated by energizing the blast-valve pilot since it is of primary importance to insure air being present at the contacts at the time of their parting. The blast-valve control is energized and motion of this valve actuates the valve which admits air to the opening side of the piston after which the control is handled automatically in the same manner as on the closing stroke.

The control valves used in conjunction with the mechanism are of the spring-biased poppet type with soft seats to minimize leakage. In order to prevent a false operation of the mechanism in case leakage does occur, the valves are so arranged that the passage to the cylinder is open to the atmosphere when the valves are in the closed position.

The closing and opening valves are interlocked in such a manner that the opening valve takes precedence over the closing valve under all conditions and provides the necessary trip-free operation of the mechanism. The operating mechanism is, therefore, fully trip free since an opening operation may be initiated at any time during the closing stroke.

General Arrangement

Figure 3 shows a typical 500,000-kva breaker mounted in a steel cell, with both terminal connections located at the bottom, and with the mechanism and air storage tank mounted at the top. A bottom-and-back arrangement can also be made in this same type of structure. Conclusive tests have shown that the interrupting element of this breaker is insensitive to position; the contacts may be mounted horizontally with the blast either up or down, or the moving contact may be drawn downward. Therefore, where local structural conditions require, an arrangement may be made with the storage tank and mechanism at the bottom, and the terminals at the top or the top and back. Figure 4 shows the application of this breaker to a metal-enclosed cubicle complete with disconnecting switches, bus, and current transformers. This construction provides a ready means of interlocking the gang-operated disconnecting switches with the cell doors, to permit entrance to the breaker cells only when the parts are safely disconnected from both bus and line.

Air Supply

In the case of these high capacity breakers, each breaker will have its own individual air storage tank, containing a sufficient supply for two closing-opening operations at full interrupting rating. A central compressor and storage system may be located at any convenient remote point, and connected by a manifold and nonreturn valves to the breaker unit tanks. These nonreturn valves assure the retention of sufficient air at each breaker for two operations in the event of failure of the central system.

The size and nature of the central compressor and storage plant will be determined by the number of breakers to be supplied, and the probable frequency of operation. The 500,000-kva breaker uses only about 15 cubic feet of free air per round trip operation. In one typical installation of these breakers, compressors rated 5 cubic feet per minute are used. These are operated by two-horsepower motors so with both the main and auxiliary compressors connected to operate, their running time will be about $1^1/_2$ minutes per breaker operation. Figure 5 shows this compressor and storage equipment, adequate for 12 500,000-kva breakers, for average central-station service. Two 5-cubic-feet-per-minute compressors are used, either one or both of which may be selected to hold the primary tank pressure automatically within selected limits.

For indoor service, the moisture content of the air is of concern only in so far as it might cause sticking of the valves, since it has no effect on the interrupting performance of this type of breaker. To secure the required degree of dryness in the air, therefore, this system compresses to 250 pounds pressure in the primary tank where the air is cooled, dropping most of the moisture content. An automatic reducing valve feeds the secondary tank at 150 pounds pressure with dry air, which has been found to have a dew point below zero degrees Fahrenheit, which is entirely safe for indoor service. In addition to the automatic pressure controls for the compressor motors, the usual water pumps, oil traps, and safety valves complete this part of the equipment.

Each individual breaker storage tank is equipped with a pressure switch, which at any predetermined low pressure can be arranged to perform one of three functions:

1. Sound an alarm
2. Trip the circuit breaker
3. Lock the breaker against tripping

The type of service on which the breaker is used, will generally determine which of the three alternatives should be selected. This switch is equipped with a manually operated by-pass, so that the operator may elect to trip the breaker to clear normal load current, which requires only very low air pressure, when the air in the tank might not be adequate for full interrupting rating performance.

Tests

A very comprehensive series of interrupting tests was brought to a climax in August 1939 by a demonstration of the 500,000-kva breaker to a large group of interested engineers, at the high-capacity testing station at Schenectady. The breaker was subjected to both opening and closing-opening tests over a range from 10 per cent to 150 per cent of interrupting rating. Table I summarizes the results of these tests, indicating that the longest arcing time was 0.5 cycle and the longest breaker operating time 6.0 cycles.

Table I. Three-Phase Tests at 14,500 Volts

		Current, Amperes Maximum Phase					
		Maximum Loop					
Test Number	Duty	Peak Value	RMS Value	Initial in Arc, RMS	Kva, Three Phase	Cycles From Trip Impulse to Interruption	Arc Length (Cycles)
1	O	4,400	2,800	2,100	53,000	5.0	0.05
2	O	12,000	7,000	5,400	135,000	5.4	0.1
3	O	32,000	19,500	13,500	340,000	5.0	0.05
4	O	48,000	29,000	19,000	480,000	5.8	0.2
5	O	55,000	32,000	25,000	630,000	5.9	0.3
6	CO	48,000	29,000	20,000	500,000	5.9	0.3
7	CO	80,000	49,000	29,000	780,000	6.0	0.5

Table II

		Current, Amperes					
		Maximum Loop					
Test Number	Duty	Peak Value	RMS Value	Initial in Arc, RMS	Kva, Three Phase	Cycles From Trip Impulse to Interruption	Arc Length (Cycles)
1	CO	166,000 197,000 105,000	98,000 112,000 68,000	44,000 48,000 48,000	350,000	4.9	0.80 1.05 0.45
2	O	163,000	97,000	78,000		3.8	0.70

Additional tests were made on this same breaker following this series, making a total of some 20 operations, 13 of which were at or above full rating. Following this complete series, the contacts shown in figure 6 were still in condition to permit making many more tests without attention.

Figure 7 is a typical oscillogram taken during this series of tests, and shows a three-phase closing-opening operation at 27,000 amperes, 14,500 volts, or slightly above full rating of the circuit breaker.

One of the unique features of this type of interrupting device is its high current-closing ability. The blast of air during the closing operation effectively delays the striking of the arc until the contacts have practically touched, and then promptly clears away the gases which are generated. Table II, test 1, shows the results of a typical three-phase closing-opening test at 4,200 volts in which the breaker successfully closed against a peak current of 197,000 amperes and then tripped and cleared. Test 2 was an opening test at 4,200 volts across a single-pole unit, in which the breaker cleared a current of 78,000 rms amperes, initial in arc.

In the course of making tests for design information, at reduced pressure levels, or with experimental timing of blast-valve opening with respect to contact separation, occasional operations were encountered when the breaker did not clear, and in due course of time the fault was removed by the station back-up breaker. In such cases arcing continued within the interrupting chamber, melting contact parts, and destroying some of the material of the arc chute. However, no damage resulted to the cell or enclosing structure, either by disruptive forces or by arcing. In all cases the breaker was again ready for service inside of two hours after merely replacing contact parts and barriers.

Conclusions

Oilless circuit breakers for indoor service are now available for the first time in interrupting ratings completely covering the range of oil breaker ratings. A number of high-capacity units has been placed in service, and a 1,500,000-kva 15-kv triple-pole unit has been built and completely tested. A review of the design and tests on the air-blast breaker brings out the following:

1. Complete elimination of fire hazard
2. Positive operation, with arcing time closely approaching the irreducible minimum
3. Contacts and arcing compartment built for long life, and minimum deterioration
4. Arcing contact exposed for ease of inspection
5. Flexibility of mounting arrangement, permitting either bottom, back, or top connections
6. Space requirements equal to, or less than correspondingly rated oil breakers, which is of importance in modernization of old stations or extensions of existing switchhouses
7. Simplicity and compactness of application to steel cubicle construction, which is easy to install, safely interlocked, and having all design features co-ordinated
8. High inherent current-closing ability

Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

A New 15-Kv Pneumatic Circuit Interrupter

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Synopsis: The demand for oilless circuit breakers of large interrupting capacity to meet the requirements of American practice directs attention toward an improved compressed-air circuit breaker which materially extends the present limitations as exemplified by continental designs.

A brief historical review of the trend toward oilless-circuit-breaker development is given. The comparison is made of the available methods of circuit interruption in air. The particular applicability of compressed air is stressed and a new circuit interrupter is described which can reach an interrupting capacity of 1,500,000 kva at 15,000 volts. Among the outstanding features of this interrupter are extremely high operating speed and rapid interruption with minimum energy dissipation. Test results are given indicating the adequacy of this type of design.

THE circuit-breaker art, almost from the time of its inception, has depended upon the use of oil as an interrupting medium in the larger and higher-voltage units. The choice of oil was based upon its excellent insulating properties and its remarkable arc-quenching ability. Oil, and its vapor, can be ignited, however, and thus can constitute a serious fire hazard, as the result of abnormal circuit-breaker operation. This factor has long been recognized, but practical operation of electrical systems has demonstrated that oil fires directly chargeable to circuit-breaker failures have been negligible. Recently, however, growing demand on the part of operators and development of the principles of oilless circuit interruption in Europe have combined to strengthen the interest in oilless circuit breakers in this country.

The first major contribution in the oilless breaker field was the development of the De-ion air breaker in 1928.[1, 2] Prior to this time, air circuit breakers had been used for many years with unquestionable success in lower-voltage circuits in which limited interrupting capacity was sufficient. The De-ion breaker, however, interrupted an arc in air using entirely new principles and so efficiently that it has been constructed for voltages up to 23 kv and for interrupting capacities as high as 750,000 kva at 15 kv or 1,500,000 kva at 23 kv. Twenty-three kv is sufficient for virtually all indoor applications in this country. This breaker is completely self-contained and operates without an auxiliary air system or the need of a replenishable interrupting fluid.

Greater interrupting capacities are continuously being required, and the economic range of the De-ion breaker will not prove adequate for all requirements. For higher interrupting capacities to 1,500,000 kva at 15 kv other forms of oilless breakers take on new attractions.

The departure from oil in Europe began with the introduction of both compressed-air and water breakers in 1929 and 1930.[3–6] The new trend was accelerated there because of the high price of oil and

Figure 1. Self-generated-gas-blast circuit breaker (shown in open position)

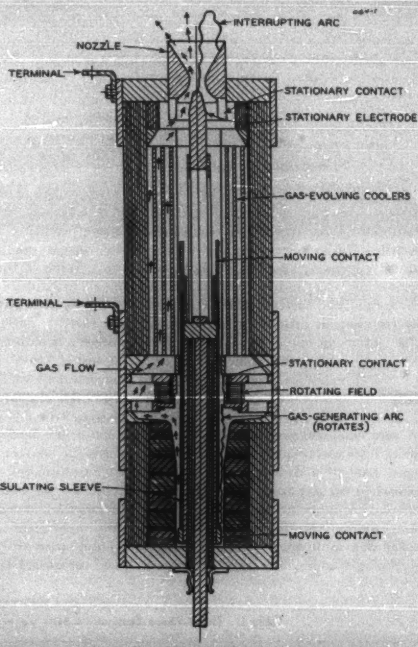

Figure 2. Oscillogram of test on self-generated-gas-blast circuit breaker showing interruption of 30,000 amperes at 13.2 kv

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1. For all numbered references, see list at end of paper.

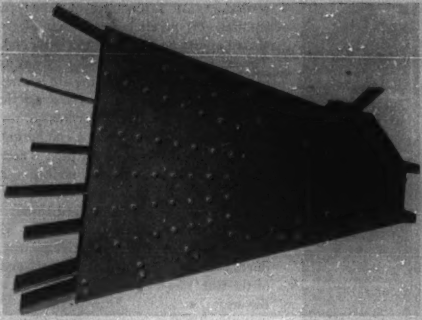


Figure 3. Experimental chute for compressed-air breaker

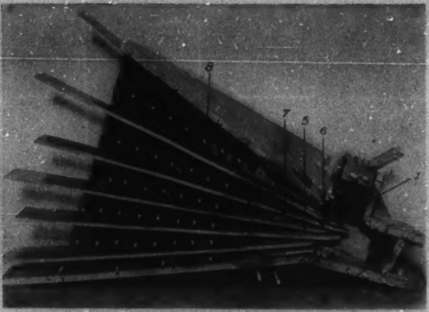


Figure 5. Experimental chute for compressed-air circuit breaker after extended series of tests

metals and because the limited capacities for which compressed air or water circuit breakers could then be built were sufficient to meet European requirements. Considerable development has been done in Europe with both types of breakers. The field of application of oilless breakers in Europe is becoming extensive and for some manufacturers is including the higher voltage and outdoor types.[7-9] In America, by contrast, the immediate demand is limited to indoor oilless breakers and although the European developments may influence the American trend, the more limited interrupting capacities which they find acceptable will not be sufficient to meet our requirements. This is especially true since European designs do not reach 1,000,000 kva, and American practice requires 1,500,000 kva or more at generator voltage.

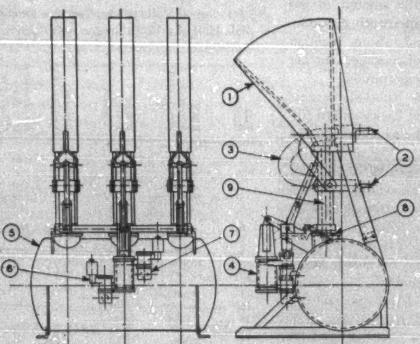


Figure 4. Compressed-air circuit breaker with vertical chute

1—Chutes
2—Terminals
3—Moving contacts
4—Operating cylinder
5—Storage tank
6—Closing valve
7—Opening valve
8—Air valve for chute
9—Insulated air tube

Comparison of Principles of Air Circuit Interruption

In choosing a suitable operating principle for application to air circuit interruption, an experimental review of available methods was made. The results of this review will be described to present the background of reasons for the choice of interrupter finally reached.

COMPRESSED-AIR ARC EXTINCTION

In Europe, variations of the nozzle type of interrupter have become most popular. They consist of an orifice through which an arc is drawn or blown and then subjected to a very intense longitudinal blast of air. For the lower ranges of current and at system capacities where the voltage recovery rate after arc extinction is not too great, this type of interrupter is quite satisfactory. As the current or voltage is increased the air requirements mount, until at 1,000,000 kva, pressures in excess of 200 pounds per square inch are required. The nozzle type of interrupter is, therefore, not appealing.

SELF-GENERATED-GAS-BLAST BREAKERS

In order to overcome the necessity of an external air-supply system, the blast of gas may be obtained by the decomposition of suitable materials. By selecting the proper material and arrangement, very high pressures of suitable gases may be obtained, and the energy to produce the gas pressure may be extracted from the arc itself during short circuit.

Boric-acid fuses and De-ion protector tubes are excellent examples of devices operating on this principle. In Europe, circuit breakers have been produced commercially where a synthetic resin is used for producing the gas blast.[13] Two self-generated-gas-blast breakers have been developed by the company with which the authors are associated—one utilizes boric-acid in close proximity to the arc-drawing contacts with suitable boric-acid-lined arc chutes into which the arc is driven by the expelled arc gases. The other is a two-arc interrupter as shown in figure 1. One arc is established through a conventional orifice and subjected to a longitudinal blast of gas in quite the same manner as used in continental compressed-air circuit breakers. The other arc is drawn in a space closely surrounded by gas-producing walls. This arc is not relied upon to supply interrupting ability except for low values of current, but to supply the gas blast for the first mentioned arc.

The interrupter consists of three essential elements—the lower part which houses one set of contacts, and the gas-forming elements; the central part which cools the hot gases and as a result of the

Figure 6. Magnetic oscillograms from table I

Fault current (amperes)—from left to right:
1,350 12,700 35,000 47,800 51,800

Recovery voltage (kv)—from left to right:
13.2 12.4 12.4 12.7 12.1

energy thus extracted, liberates more gas and eventually delivers the thoroughly cooled and deionized gas to the interrupting arc (which is located at the top of the interrupter) in such quantity and at sufficiently high pressure to handle very heavy currents.

Table I. Opening Tests on an Experimental Compressed-Air Circuit Breaker

13.2 Kv Initial Voltage Applied to a Single-Pole Unit of a 15-Kv Circuit Breaker; 60 Cycles

Test Number	RMS Amperes Interrupted	Arcing Time (Cycles)	Recovery Voltage (Kv)	Air Pressure in Tank (Lbs/Sq In.)
1	1,390	0.505	12.7	105
*2	1,350	0.35	13.2	105
3	2,680	0.20	12.8	150
4	2,620	0.20	13.0	150
5	2,660	0.45	13.0	150
6	4,220	0.20	12.7	150
7	5,180	0.25	12.9	150
8	4,950	0.35	12.9	150
9	10,400	0.45	12.8	150
10	10,000	0.15	12.5	150
11	7,900	0.40	12.8	150
*12	12,700	0.40	12.4	150
13	16,300	0.25	12.8	150
14	16,600	0.10	12.7	150
15	19,800	0.60	12.7	150
16	23,000	0.45	12.7	150
17	22,600	0.35	12.8	150
18	28,700	0.615	12.8	150
*19	35,000	0.505	12.4	150
20	40,600	0.6	12.5	150
*21	47,800	0.15	12.7	150
*22	51,800	0.25	12.1	150
23	32,700	0.15	12.4	150
24	31,900	0.15	12.3	150
25	41,400	0.35	12.3	150
26	30,650	0.505	12.4	150
27	23,800	0.70	12.4	125
28	23,000	0.75	12.5	125
29	20,650	0.60	12.6	125
30	27,800	0.25	12.4	125
31	23,800	0.75	12.4	125
32	36,700	0.40	12.2	125
33	43,000	0.40	12.2	125
34	33,100	0.505	12.4	125

* Magnetic oscillograms of these tests are shown in figure 6.

In this type of design the gas-forming parts must be shaped to supply sufficient gas for interruption at low currents without having excessive gas generation and pressure at high currents. Since the gas is obtained from solid surfaces in proximity to an arc, repeated operations produce erosion that affects the amount of gas generation and the interrupting characteristics. These considerations, together with other factors, discouraged further work on this type of construction.

The interrupter has handled currents up to 48,000 amperes at 7.6 kv and to 30,000 amperes at 13.2 kv as shown by the oscillogram in figure 2. The arcing time for the high values of current usually did not exceed two half cycles yet pressures as high as 1,000 pounds per square inch were recorded. While this pressure may seem high, extrapolation from compressed-air requirements for this type of nozzle interrupter are quite comparable. The ratings desired, however, appear to be beyond the capacity which can be reached with this principle.

MAGNETICALLY PRODUCED AIR BLAST

For many years magnetic fields have been used to extinguish arcs by blowing them into some type of arc chute which lengthens and constricts the arc. Recently a new means of effecting interruption with a magnetic field has been described.[3,4] An interrupter of this type consists of parallel insulating plates spaced apart and slotted at the lower end. The arc is driven into this slot by means of a magnetic field. When the arc reaches the upper portion of the slot the magnetic field exerts a force on the electrons in the gas stream and gives them a component of velocity transverse to the arc. These electrons in turn bombard neutral gas particles, with the final result that a blast of gas is caused to flow upward through the arc path. When current zero is reached this blast of gas effectively introduces a strong deionizing action at the end of the slot, which effectively extinguishes the arc.

Within the last two years two types of lower-voltage breakers have been developed using this interrupting principle. The first type was for 600 volts and below and the second for potentials of 2,500 and 5,000 volts. The lower-voltage devices have interrupted currents as high as 120,000 amperes and the 2,500-volt interrupter satisfactorily clears currents up to 50,000 amperes.

The effectiveness of this principle is not sufficient for the interruption of currents as high as 60,000 amperes at 15,000 volts.

Compressed-Air Interrupter

A more effective air stream can obviously be produced by utilizing compressed

Table II. Opening Tests on an Experimental Compressed-Air Circuit Breaker

13.2 Kv Initial Voltage Applied to a Single-Pole Unit of a 15-Kv Breaker; 60 Cycles

Test Number	RMS Amperes Interrupted	Arcing Time (Cycles)	Recovery Voltage (Kv)	Air Pressure in Tank (Lbs/Sq In.)
1	1,370	0.45	13.1	150
2	1,310	0.45	13.2	150
3	2,740	0.35	13.1	150
4	3,020	0.15	13.2	150
5	3,920	0.35	13.1	150
6	4,550	0.45	13.1	150
7	4,950	0.35	13.0	150
8	10,600	0.35	12.8	150
9	10,700	0.65	13.0	150
10	11,100	0.40	13.0	150
11 12 13	Approximately 12,000	No oscillogram		150
14	27,800	0.35	12.7	150
15	23,800	0.45	12.7	150
16	23,400	0.35	12.6	150
17	34,600	0.35	12.6	150
18	26,600	0.35	12.4	150
19	23,900	0.45	12.3	150
20	39,800	0.65	12.2	150
21	50,100	0.60	12.2	150
22	31,000	0.50	12.2	150
23	40,000	0.60	12.4	150
24	30,200	0.50	12.3	150
25	51,800	0.85	12.3	150

Table III. Opening and Closing-Opening Tests on an Experimental Compressed-Air Circuit Breaker

13.2 Kv Initial Voltage Applied to a Single-Pole Unit of a 15-Kv Circuit Breaker

Test Number	Operation	RMS Amperes Interrupted	Arcing Time (Cycles)	Recovery Voltage (Kv)	Air Pressure in Tank (Lbs/Sq In.)
1	O	1,380	1.1	13.0	150
2	O	1,280	0.5	13.3	150
3	O	2,890	0.3	13.2	150
4	O	3,440	0.3	13.2	150
5	O	2,690	0.3	13.1	150
6	O	4,660	0.2	13.2	150
7	O	4,460	0.3	13.1	150
*8	O	6,200	0.4	13.1	150
9	O	9,000	0.9	13.0	150
10	O	7,500	0.5	13.0	150
11	O	7,000	0.2	12.6	150
12	O	16,000	0.3	12.6	150
*13	O	15,800	0.3	12.7	150
14	O	9,600	0.7	12.8	150
15	O	20,600	0.6	12.4	150
16	O	23,400	0.6	12.6	150
17	O	22,800	0.7	12.2	150
*18	O	29,000	0.7	12.4	150
*19	O	40,000	0.4	12.6	150
20	O	40,600	0.4	12.5	150
21	O	44,400	0.4	12.0	150
22	O	44,400	0.6	12.5	150
23	CO	1,270	0.6	13.0	150
24	CO	1,280	0.7	13.1	150
25	CO	2,650	0.5	13.0	150
26	CO	2,530	0.5	13.0	150
27	CO	2,570	0.5	13.0	150
28	CO	4,320	0.6	13.0	150
29	CO	3,980	0.6	13.0	150
30	CO	4,260	0.6	13.0	150
31	CO	7,520	0.6	12.8	150
32	CO	7,520	0.6	12.8	150
33	CO	7,450	1.2	12.8	150
34	CO	11,300	0.3	12.6	150
35	CO	11,500	0.7	12.4	150
36	CO	17,000	0.3	12.0	150
37	CO	Approx. 20,000	No oscillogram	No oscillogram	150
38	CO	Approx. 20,000	No oscillogram	No oscillogram	150
39	CO	20,800	0.5	11.4	150
40	CO	Approx. 25,000	No oscillogram	No oscillogram	150
†41	CO	25,000	0.5	11.2	150
42	CO	27,500	0.6	11.3	150

*Magnetic oscillograms and cathode-ray oscillograms of these tests are shown in figures 7 and 8 respectively.

†Magnetic oscillogram of this test is shown in figure 9.

air in the type of arc chamber just described. In this case the magnetic field is no longer necessary. Tests were made with this structure which indicated greatly increased interrupting capacity. An external air-supply system must be provided of course, but in view of the extremely high interrupting ratings which can be achieved in very small space, the use of compressed air in conjunction with an arc chamber of the type described appeared to be the most feasible method of proceeding toward the design of a breaker having sufficient interrupting capacity to meet all indoor requirements. A comparison of the interrupting ability of a compressed-air device built in this manner, with known types of compressed-air breakers utilizing nozzles for interruption, indicated that an air stream perpendicular to the arc path in conjunction with insulating plates was much superior.

The contacts and surrounding walls are arranged so that the arc is blown by the air stream from its point of inception against suitable insulating splitters. There the arc remains practically stationary and substantially perpendicular to the gas blast throughout the arcing period. During this time the gas is allowed to escape around the sides of the arc, freely enough to prevent building up of pressure sufficient to interfere with the continuity of the arc stream and at the same time sweeps the excess ionization from around the arc out into the space beyond the splitters. Thus, as the current zero approaches, the residual ionization associated with previous high current has been removed without high arc voltage, and the time between current zero and restored voltage becomes adequate for interruption even though the voltage recovery rate is high.

The next important requirement is to cool and deionize the gases sufficiently before allowing them to escape beyond the splitters, to prevent flashover on the outside. Further cooling is necessary for the higher kilovolt-ampere ratings to prevent excessive external demonstration. Both of these problems are solved by suitable coolers and mufflers described later.

Aside from the considerations of intimate association of the gas pressure, arc behavior, and splitter performance, there are other practical reasons that favor the cross-blast type of construction over the longitudinal blast. These considerations are: (1) the ability with a blast of air directed at right angles to the direction of contact separation to sweep the arc quickly from the contacts, thereby permitting both main and arcing contacts to remain in the chute; (2) the ability to increase the cross-sectional area of the main current-carrying member within wide limits, without affecting any of the parts

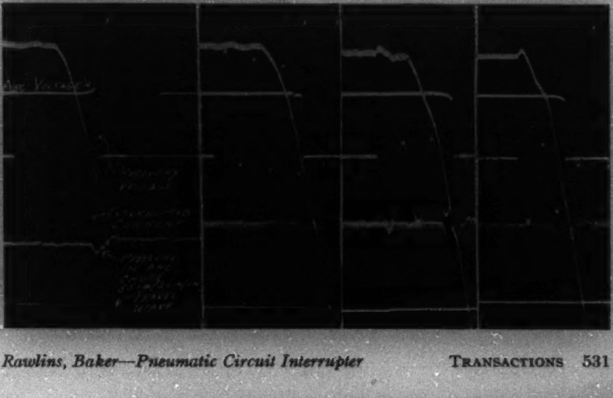
Figure 7. Magnetic oscillograms from table III

Fault current (amperes)—from left to right:

6,200 15,800 29,000 40,000

Recovery voltage (kv)—from left to right:

13.1 12.7 12.4 12.6



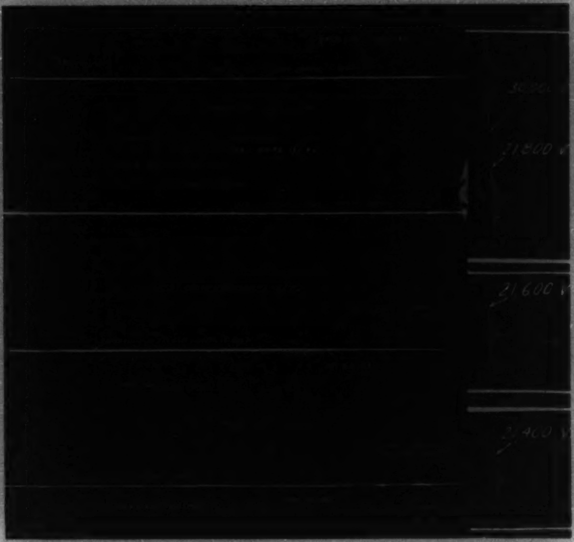


Figure 8. Cathode-ray oscillograms corresponding to those of figure 7

involved in arc extinction. These two factors simplify the construction of the breaker and are of particular importance on the heavier current units of the powerhouse class.

An experimental arc chute as shown in figure 3 was built along these lines for test. This form of interrupter is flexible and can be adapted to a breaker design as shown in figure 4, or it can be disposed horizontally, depending on the application requirements. Figure 5 shows an open view of this same chute after having successfully interrupted 45 short circuits at currents varying from 1,000 to 50,000 amperes at 13,200 volts as listed in table III. During the tests the interrupter was in a horizontal position on the breaker. After these tests the contacts were found to be in excellent condition. There was some erosion of the splitters as may be seen, however, this erosion was not sufficient to render them unfit for further service.

Referring to figure 5, (1) is the stationary contact from which the moving contact (2) is withdrawn pulling an arc along the path (3). This arc under the action of a blast of air from the orifice (4) is driven into intimate contact with the splitters (5) along the path (6). The arc gases expelled from the throat impinge first on the copper buffer plates (7) which perform the first cooling and deionizing action on the arc gases. The partially cooled and deionized gases then pass on through the cooling and diffusing screens (8) that cool and diffuse the arc gases effectively preventing external flame and disturbance.

Test Results

Tables I, II, and III show the results of three typical series of single-phase tests using the arc chute shown in figure 3. During these tests the pressure in the tank varied between 105 and 150 pounds per square inch. The pressure in the arc chute was measured with an instantaneous recording telemeter. This pressure was negligible except during the arcing period. The effect of the arc acting as an impediment to the free escape of air caused a temporary build-up in pressure back of the arc which in some cases almost equalled the tank pressure.

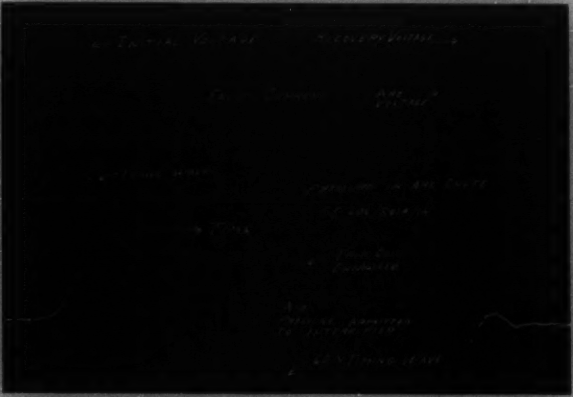
The arcing time was generally less than that corresponding to one-half cycle of current. Sometimes when the current was highly asymmetrical arcing would continue over one entire current loop which would lengthen the time to over one-half cycle. The breaker rarely passed up an opportunity to clear if the contacts were parted one-half inch or more when a current zero occurred.

Tables I and II show currents ranging from approximately 1,000 amperes to 52,000 amperes with 13.2 kv across a single-pole unit.

Figure 6 shows typical oscillograms taken from table I at 1,000, 12,700, 35,000, 48,000, and 53,000 respectively. The oscillograms from table II are all very similar to the ones shown in figure 6.

Table III shows a series of 45 tests covering the same current range. On some of these tests cathode-ray oscillograms were obtained, these together with the magnetic oscillograms shown in figures 7 and 8 are of interest in proving that the operation of the breaker is consistently fast and dependable even on a circuit with a natural oscillatory frequency of 28,300

Figure 9. Typical oscillogram of closing-opening tests



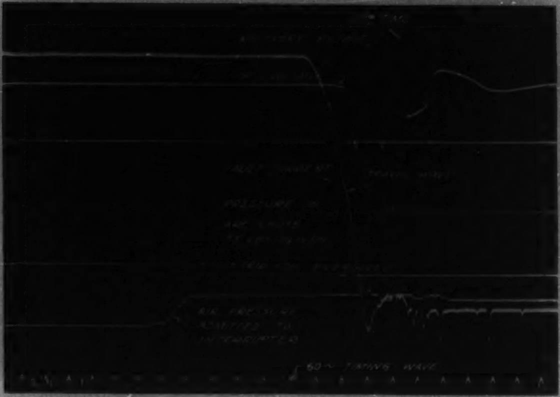


Figure 10. Oscillogram showing interruption of 65,300 rms amperes at 13.2 kv, single phase

cycles per second, and a circuit transient recovery voltage rate of 2,200 volts per microsecond. On this circuit the test transient recovery voltage rate was 1,380 volts per microsecond as shown for test 19. The transient recovery voltage rate is given as the slope of the tangent to the recovery voltage wave. It is obtained by dividing the maximum voltage by the time of one-half cycle of natural frequency and multiplying by 1.15. This method is used because the recovery transient consists essentially of a single sinusoidal component.

Figure 9 shows a typical closing-opening test. The ability of this breaker to close in on high current without showing any evidence of distress is noteworthy.

Figure 10 shows an oscillogram of the highest current interrupted. It was obtained during a fourth run similar to the one shown in the tables. This film shows satisfactory interruption of 65,300 amperes rms at 13.2 kv, single phase.

These interruptions were obtained with negligible external demonstration in the form of ionized flame. Noise accompanying the interruption is held to acceptable limits by the arc chamber design as shown. A particularly significant feature of all tests is the high mechanical speed of the breaker contacts. It is possible to separate these contacts two inches in one-half cycle. This high speed is the effect of properly using an air mechanism to operate the breaker. Very high opening and closing forces can be exerted on the piston which is moved in the cylinder by the compressed air itself to actuate the contacts. Springs which would give comparable operating speeds would represent a nearly impossible mechanical design. It is also a simple matter properly to control the air cylinders to stop the contacts and their associated operating rods without undue jar on rebound at the end of the opening stroke.

A compressed-air interrupter of the type described appears to be particularly rugged and to handle repeatedly interruption of the highest currents without excessive damage. The low energy input into an interrupting chamber of this type which is the function of careful design to minimize the arcing time, to approximately one-half cycle, and to keep the arc drop low during the arcing period, is primarily responsible for reliability without excessive erosion.

Table IV. Summary of Information From Cathode-Ray Oscillograms

Test Number	Frequency (Cycles/Sec)	Crest Voltage	Test Transient Recovery Voltage Rate (Volts/Microsec)	Circuit Transient Recovery Voltage Rate (Volts/Microsec)
*8	13,500	30,000	920	1,050
*13	23,500	21,800	1,150	1,820
*18	24,400	21,600	1,210	1,890
*19	28,300	21,400	1,380	2,200

* Magnetic oscillograms and cathode-ray oscillograms of these tests are shown in figures 7 and 8 respectively.

Conclusions

The search for improved forms of dry-type circuit interrupters suitable for interruption of 1,500,000 kva, at 15,000 volts, indicates that the use of a compressed-air stream transverse to an arc is the most effective. Currents as high as 65,300 amperes have been interrupted at 13,200 volts single phase with a contact separation of approximately two inches, which can be mechanically obtained in one-half cycle. A breaker utilizing these new principles of interruption makes possible new standards of system protection because of its high operating speed, complete freedom from oil and associated fire hazard, and remarkably high interrupting capacity in terms of the space required by the breaker.

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

What Wood May Add to Primary Insulation for Withstanding Lightning

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ON power-transmission lines, wood, serving certain structural functions, simultaneously may provide supplementary insulation against lightning if properly chosen and applied. However, to sustain the power-frequency voltage, adequate primary insulation in the form of porcelain insulators or the like is essential.

These facts have been rather well known for some time, but in degree the ability of wood to withstand lightning has not been thoroughly determined. In the laboratory study described two years ago,[1] this phase was partially covered. It is the purpose of this paper to describe the results of a testing program in the same laboratory, treating of all factors which it was felt could affect the lightning or impulse strength of wood.

Nothing relating to the 60-cycle strength of wood will be given here, for, as outdoor insulation against sustained power-frequency voltage, wood, with its internal moisture, is generally considered quite ineffective. For briefly applied surges, such as lightning or switching transients below flashover voltage, the moisture content is not damaging to the wood. However, for sustained 60-cycle voltages, the electrolytic nature of the moisture allows leakage currents which ultimately cause burning and charring of the wood. As intimated above, it is the purpose of effective primary insulation in series with the wood to check this action.

It will be shown below that the degree to which wood will supplement porcelain against lightning depends almost entirely upon its length and internal moisture, and little upon its creosoting treatment, cross section, or kind. Considerable test data were obtained in this study and numerous curves and tables developed directly therefrom. In the interest of brevity and clarity, practically all of these original curves are omitted here and only so-called "working curves" which were derived from the originals, are included. Accordingly, most of the kilovolts of insulation plotted involve actual supplementary wood insulation values to be added directly to the impulse kilovolts of the associated insulators. Likewise, ratios and percentages are used for expressing the effects of certain wood properties upon the insulation strength of wood.

Nature of Test Equipment and Data

The various stages of the 3,000,000-volt impulse generator at the Barberton high-voltage laboratory of the Ohio Brass Company were used in these tests. The generator itself was always adjusted for the standard $1^1/_2$x40-microsecond wave.

For tests on the wood specimens alone, voltage was applied between braided metallic bands tightly wrapped about the wood. This was felt to simulate electrically most hardware conditions in service. In the case of a combined insulator and wood test, voltage was applied to the conductor attached to the insulator while the braided band was adjusted along the wood member and grounded. For all tests, the specimens themselves were suspended at sufficient distances from nearby grounded objects to eliminate interference with the electrostatic fields being studied.

On practically all specimens, complete volt-time curves were taken with both positive and negative polarities, cathode-ray oscillograms being secured for every impulse. To allow compactness of presentation, however, curves are given showing flashover voltages at only the two-microsecond and "critical" (formerly termed "50-50" or "minimum") points. It is felt that the two-microsecond point represents direct-stroke conditions of average severity while the critical flashover values allow computations for induced voltages and other long-wave conditions.

Flashover Voltages of Structure Parts

As pointed out above, any structure design computations made from information in this paper require the addition of the supplementary wood values from certain curves to actual insulator flashover values. Figures 1 and 2 provide the necessary flashover data of a pin-type and a suspension insulator for use in typical examples in this paper.

Figure 3 affords rod-gap data for assisting in designing so-called "horn gaps" for wood members. As will be shown by later examples, such gaps can be made to protect parallel crossarm or pole paths if adjusted to have lower flashover strengths. The proper crossarm and pole data for this comparative study is provided in the curves of figures 4 and 5. No measurable difference was found for surges of the two polarities so that the curves of these two figures represent the average of all data. The data from figures 4 and 5 are only for comparison with other parallel flashover paths, such as air gaps or insulators, and they are not to be added to other insulation in series.

Flashover Voltages Added to Porcelain by Wood

In figures 6 to 9 are given the supplementary impulse insulation strengths afforded porcelain insulators by different lengths of various wood members. For each case the flashover data from the curve *must be added* to the corresponding flashover data of the insulator chosen in order to have the total lightning or impulse strength of the combination. The supplementary insulation values which

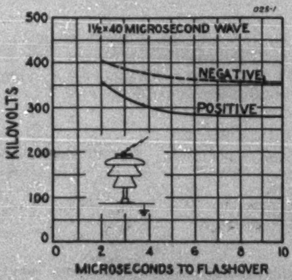

Figure 1. Impulse volt-time curve of 69-kv pin-type insulator

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Paper 40-25, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940. Manuscript submitted November 8, 1939; made available for preprinting December 14, 1939.

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Crossarm and pole specimens used in many of the tests were from the Georgia Power Company, Mississippi Power Company, New England Power Service Corporation, and Ohio Power Company. The special creosoted planks with known moisture contents were prepared by the Wood Preserving Corporation under the direction of W. P. Arnold.

[1] For numbered reference, see end of paper.

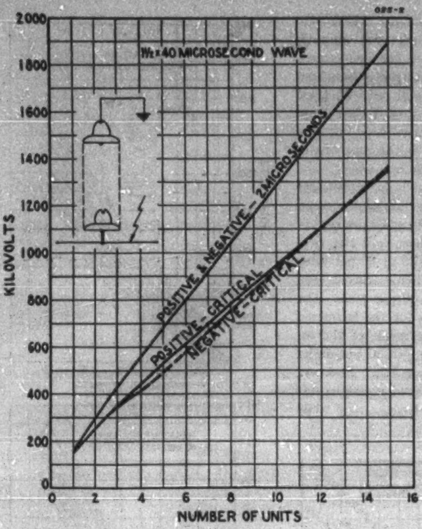


Figure 2. Impulse flashover voltages of 10 by 5$^3/_4$-inch suspension insulators

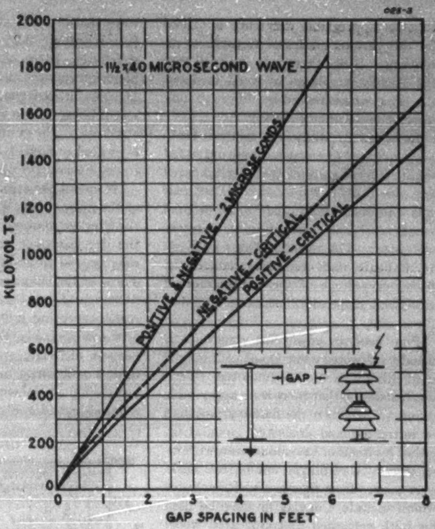


Figure 3. Impulse flashover voltages of rod gaps

pole sections add to pin-type crossarm combinations (figure 6) are shown in figure 10.

All wood insulation values in figures 4 to 10 inclusive, except for cedar and newly creosoted pine poles in figure 5, are based on tests on wood with an average moisture content of about 15 per cent.

Data, described in the paper referred to above,[1] indicated that the total insulation strengths of insulator and wood combinations could be calculated by multiplying the sum of the individual flashover strengths by certain constants. Subsequent and more numerous data, however, tend to prove that more accurate calculations can be made by the use of curves such as given in figures 6 to 10. Also, an analysis of the electrostatic conditions involved tends to bear this out. For example, the effect of changing the constants of one member, such as by lengthening indefinitely the crossarm supporting the insulator, cannot always allow the actual flashover voltage to be the same percentage of the sum of the insulator and arm flashover voltages.

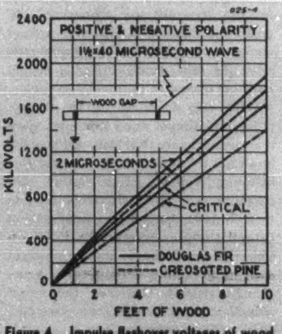


Figure 4. Impulse flashover voltages of wood crossarms

Factors Influencing Impulse Insulation Values

Cross Section

Previously published data[1] indicated that the impulse flashover voltage of wood decreases as its cross-sectional area is increased. This subsequent investigation, however, shows that certain other factors have greater effects and that cross-sectional area is of secondary importance.

Another related factor which may affect the flashover voltage is the distribution of the wood cross section, as exemplified by solid and multiple-plank crossarms with equal cross-sectional areas. The ratios in figure 11 are the result of flashover tests upon a number of combinations of insulators and creosoted pine timbers. The latter were first tested as six by eight-inch solid crossarms and then sawed and retested as three by eight-inch double plank crossarms. The average ratio for the positive critical flashover is about 0.96 and that for the negative is about 1.02, indicating that as far as the impulse flashover voltage is concerned, there is little to be gained by the choice of one type of construction over the other. However, a definite advantage is gained with the double-plank crossarm in that a lightning stroke damages only one member and leaves the other intact for supporting the line.

Creosote Treatment

Since wood used in power-line structures may be creosoted, it is desirable to know the effects on flashover voltages of the various kinds of creosote, degrees of retention, and methods of treatment. A number of flashover tests were made on three-foot sections of newly creosoted green pine crossarms in series with three 10 by 5$^3/_4$-inch suspension insulators. Light creosote with retentions of eight and ten pounds per cubic foot of wood, and heavy creosote with a ten-pound retention were used in the preparation of the test specimens. Results of these tests, some of which are shown in figure 12, indicate that no appreciable difference is caused by the various grades of creosote or retentions. Still other tests indicate that the flashover voltage of wood is about the same when treated by different commercial creosoting processes.

In order to study the relative flashover voltages of creosoted and untreated wood, several pairs of specially prepared dry pine timbers were tested. Each pair was obtained from the same original plank, and only one of each pair was creosoted. The results, shown in figure 13, indicate that creosoting reduces the flashover voltage of the combination of wood and suspension insulators only 6 or 7 per cent.

Pine used in power-line structures is usually creosoted while Douglas fir may be used either untreated or creosoted. Various tests show that there is no appreciable difference between the flashover voltages of untreated and creosoted Douglas fir, either by itself or in combination with insulators. Accordingly, all of the flashover curves from figures 4 to 10 inclusive which include Douglas fir may be used for both creosoted and untreated wood.

RAIN

Since lightning is usually accompanied by rain, it seemed desirable to know the effects of rain on the impulse insulation of wood. It had been reported previously[1] that rain may reduce the impulse flashover of wood structures by as much as 50 per cent.

In order to determine whether such a reduction is caused by rain in the air, and on the insulator and wood surfaces, or by the moisture penetration into the wood, a special series of tests was made. A dry Douglas-fir crossarm was completely covered with several coats of lacquer to keep out moisture and subjected to dry and wet flashover tests. The wet tests were made using standard AIEE rain (precipitation 0.2 inch per minute; resistivity, 7,000 ohms per inch cube). Similar tests were made upon unlacquered Douglas fir, red elm, and red oak, these woods being chosen because of their dense structure. The results of these tests are presented in figure 14 as the ratio of wet to dry flashover voltage.

As shown, the critical flashover voltages of the unlacquered woods are reduced on the average to about 65 or 70 per cent of the dry flashover values, and the two-microsecond to about 75 or 80 per cent. Those of the lacquered crossarms showed a change of less than 10 per cent. The ratios exceeding unity for the lacquered arm are probably the result of the increased dielectric strength of air under rain conditions. Since the flashover value of each unlacquered crossarm was reduced considerably by the rain while that of the lacquered arm was practically unchanged, it seems logical to presume that the penetration of moisture into the wood is the fundamental cause of the observed reduction.

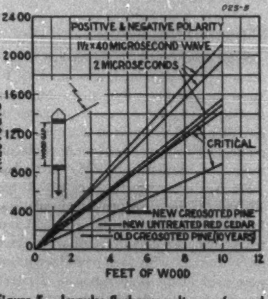


Figure 5. Impulse flashover voltages of wood poles

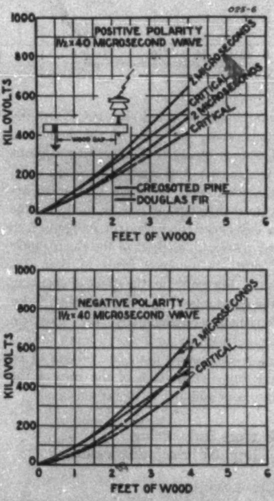


Figure 6. Impulse insulation added to pin-type insulators by crossarms

The effect which the duration of rain has upon the impulse insulation of wood and insulators is indicated in figure 15. The tests here were made on four-unit insulator strings suspended from four-foot sections of both untreated Douglas-fir and creosoted-pine crossarms. The greatest reduction in flashover voltage seems to occur within a very few minutes after the rain starts, negligible change resulting thereafter.

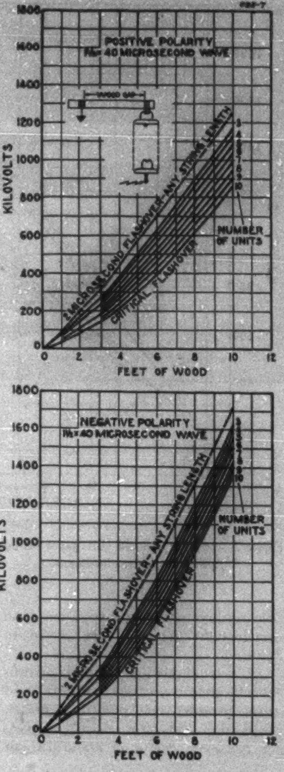


Figure 7. Impulse insulation added to suspension-insulator strings by wood crossarms (creosoted pine or Douglas fir)

While rain definitely reduces the impulse insulation of wood structures, there is always a chance that the wood may not be wet at the instant it is subjected to a lightning voltage. Furthermore, the standard rate of precipitation used in these tests far exceeds the usual rate of natural storms. Accordingly, it is possible that the flashover voltage may not decrease as rapidly as indicated in figure 15. In general, therefore, the actual reduction factor for rain for a given condition appears to be a matter of probability.

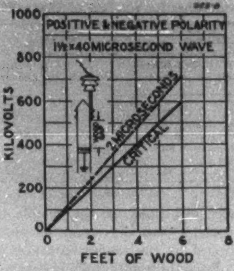


Figure 8. Impulse insulation added to pin-type insulator by pole sections (creosoted pine)

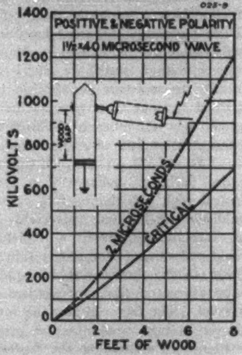


Figure 9. Impulse insulation added to suspension-insulator strings by pole sections (creosoted pine)

Moisture Content

The results of the various rain tests indicated that moisture penetration into the wood was responsible for the reduction of its impulse flashover voltage. Furthermore, it had been observed that green wood has a lower flashover voltage than equivalent dry wood. Accordingly, it seemed desirable to conduct a systematic investigation of the effects of internal moisture upon the impulse flashover voltage of wood.

Figure 16 gives results of flashover tests on four-unit strings of 10 by $5^3/_4$-inch insulators suspended from four-foot sections of creosoted-pine crossarms with moisture contents varying from 12 to 93 per cent. Similar tests were made on the same crossarms without insulators. Other tests were also made on similar arrangements of untreated Douglas-fir crossarms with controlled moisture contents.

The results of this study are presented in figure 17 as the ratio of the flashover voltage at any moisture content to the flashover voltage at 15 per cent moisture content. The latter is usually considered the average for dry wood in moisture equilibrium with the atmosphere.

When wood with internal moisture is placed under electrical stress, electrolytic conduction may occur within it. This might influence the electrostatic field of the wood, and therefore, its flashover voltage in combinations with insulators. The degree to which this would take place may vary with the different woods because of their internal cellular structures. This may account for the difference in behavior of the several woods in the curves of this paper. The amount of voltage reduction introduced by internal moisture also may be dependent upon the duration of the transient, as time is required for electrolytic conduction. This may be a possible explanation of why the two-microsecond flashover voltage (figure 17) is influenced less than the critical by the internal moisture.

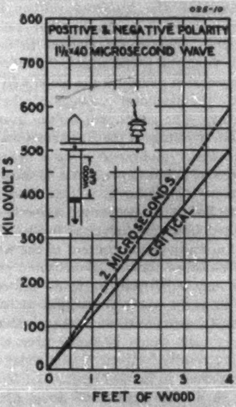


Figure 10. Impulse insulation added to combinations of pin type with crossarm by sections of poles (creosoted pine)

From figure 5, the critical flashover voltage of eight-foot sections of newly creosoted pine poles is about 720 kv while that of old poles is about 1,200 kv. The ratio of these flashover voltages is about 0.60, which corresponds to a moisture content of about 33 per cent on the creosoted pine curve in figure 17. The newly creosoted pine poles were presumably green wood while the old poles were fairly dry, having been in service for several years. A moisture content of 33 per cent is a reasonable value for green wood and, accordingly, it would seem that the curves shown for crossarms (figure 17) may be satisfactory for poles as well.

As shown by the curves of figure 17, an immediate increase of impulse insulation strength may be secured by predrying wood to its equilibrium value before it is installed on transmission-line structures. On the other hand, the insulation value of green wood will increase gradually to the same level as its internal moisture approaches equilibrium with that of the atmosphere. Accordingly, the choice between predried and green wood appears to be a matter of economics.

Spread of Data

All curves presented in figures 4 to 10 inclusive represent the average of numerous data points, which lie within ±15 to ±20 per cent of the average. These rather wide limits are believed to be the result of inherent structural variations within the wood itself which influence its impulse flashover voltage.

Critical flashover tests were made on a group of about 60 newly creosoted pine poles, each pole being subjected to the same test procedure. These flashover voltages ranged from 490 kv to 836 kv, averaging about 675 kv. The spread of data with respect to the average was as follows:

Spread of Data in Per Cent	Number of Test Values Included	Per Cent of Total Number of Test Values Included
± 5	25	41
±10	41	67
±15	48	79
±20	56	92
±25	60	98
±28	61	100

In the above table 92 per cent of all

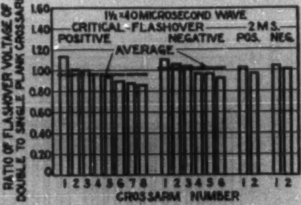


Figure 11. Comparison of impulse flashover voltages of single- and double-plank crossarms with pin-type and suspension insulators

Table I. Protective Horn Gaps on Creosoted Pine Crossarms for Six Feet of Green Wood

	Positive		Negative	
	Critical	Two-Microsecond	Critical	Two-Microsecond
Flashover of dry wood (figure 4) in kv	840	1,080	840	1,080
Assumed moisture content in per cent	35	35	35	35
Reduction factor (figure 17)	0.57	0.77	0.57	0.77
Flashover of green wood in kv	480	830	480	830
Rod gap having flashover equal to that of green wood (figure 8) in inches	0.29	832	25	32

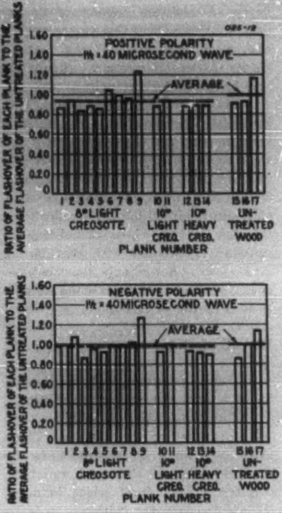


Figure 12. Effects of various creosotes and retentions upon impulse flashover voltages of pine crossarms with suspension-insulator strings

the tests are included in a ±20 per cent spread while a ±25 per cent spread includes all but one test. Similar distributions of test values would probably be found for various combinations of insulators and wood, although the spread in the above tabulation of green-pole data is probably as great as would ever be found.

When the curves of figures 4 to 10 inclusive are used for estimating the flashover voltage of a given structure, the result obtained will represent the average flashover voltage of a large number of similar structures. For example, if tests were made on 100 similar structures the average of these test values would probably agree quite closely with the estimated average and 90 to 95 per cent of these tests would lie within a ±20 per cent band.

Application of Curves to Typical Problems

General Procedure

By means of the curves in this paper it is possible to estimate the various impulse flashover voltages of numerous combinations of insulators and wood.

The first step is to obtain the flashover voltage of the insulator, either from figures 1 or 2 or from other reliable sources. The second step is to determine from the proper curve the kilovolts of insulation *added* by the wood and *add* it to the flashover voltage of the insulator. The result will be the average flashover voltage of the given combination of insulator and wood with a moisture content of about 15 per cent. If the wood is green, this flashover voltage should be multiplied by the proper reduction factor, selected from the curves of figure 17 for the known moisture content of the wood. The moisture content of creosoted green pine may vary from 30 to 60 per cent, and in cases where the actual moisture content is unknown, an average value of 35 per cent would probably give satisfactory results. Green fir and cedar can be expected to be no higher than 35 per cent in moisture content. Incidentally, this may account for the flashover voltage curve of new cedar poles in figure 5 being higher than that of new creosoted pine poles.

In case the flashover voltage under rain conditions is desired, the flashover voltage, estimated for the combination of insulator and dry wood, should be multiplied by the proper reduction factor. From the results presented in figures 14 and 15, it would appear that a factor of 0.65 to 0.70 for critical flashovers and 0.75 to 0.80 for two-microsecond flashovers of either polarity would be suitable reduction factors due to the rain.

Tests previously discussed have indicated that the reduction in impulse flashover voltage caused by rain is due primarily to moisture penetration into the wood and not to a reduction in the impulse flashover voltage of the insulators or the surrounding air. Therefore, it seems a reasonable presumption that the impulse flashover voltage of a green crossarm with a high moisture content should not be lowered appreciably by rain. Accordingly, both a moisture-content reduction factor and a rain reduction factor should not be applied to the estimated flashover voltage for dry wood. The factor giving the lower flashover voltage would be the proper one to use in estimating the flashover voltage under adverse conditions.

Examples

The following examples are presented to illustrate several applications of the curves in this paper and to demonstrate the agreement of estimated with actual test values.

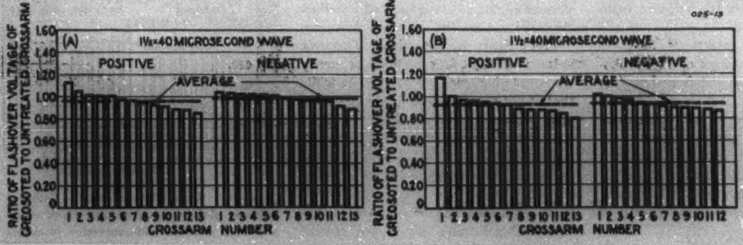


Figure 13. Effect of creosote treatment of wood upon its impulse flashover voltage with and without insulators

(*A*)—Five feet of creosoted pine crossarm only

(*B*)—Five feet of creosoted pine crossarm with five 10 by $5^3/_4$-inch suspension insulators

Example 1. In this example the dry and wet impulse flashover voltages of a 69-kv pin-type insulator and four feet of creosoted pine crossarm are determined.

Positive Critical

Flashover of insulator (figure 1)	260 kv
Insulation added by wood (figure 6)	410 kv
Estimated flashover, dry	670 kv
Actual test value, dry	690 kv
Ratio of estimated to actual	0.99
Reduction factor due to rain	0.65
Estimated flashover, wet	435 kv
Actual test value, wet	445 kv
Ratio of estimated to actual	0.98

The negative critical, and positive and negative two-microsecond flashover voltages may be estimated in a similar manner.

Example 2. In this example the estimated voltage is for phase-to-phase flashover between conductors supported by two 33-kv pin-type insulators on the same 22-inch creosoted pine crossarm with two wood sections between phases such as results from the use of metal crossarm braces. When an impulse is applied between conductors, each combination of insulator and wood section is subjected to an impulse of opposite polarity from the other. Accordingly, the procedure is to estimate the positive and negative critical flashover voltages for one insulator and wood section, add these two estimates together, and multiply by a factor of 0.85. The latter is chosen because tests have indicated that when two such combinations are placed in series the resulting flashover voltage averages about 0.85 of the sum of the flashover voltages of the individual combinations. Similarly, a factor of 1.00 has been determined for the two-microsecond flashover. There is evidence that the same procedure will prove satisfactory for strings of suspension insulators in place of pin type.

Critical Flashover

Positive critical flashover of pin type	200 kv	
Insulation added by wood (figure 6)	165 kv	
Total, positive		365 kv
Negative critical flashover of pin type	255 kv	
Insulation added by wood (figure 6)	130 kv	
Total, negative		385 kv
Sum, positive and negative		750 kv
Reduction factor	0.85	
Estimated flashover, positive or negative	640 kv	
Actual test value, positive	620 kv	
Ratio of estimated to actual, positive	1.03	
Actual test value, negative	650 kv	
Ratio of estimated to actual, negative	0.98	

The method of estimating used in this example naturally gives the same value for both polarities and actual tests indicate that there is little difference between the positive and negative flashover voltages of this combination of wood and insulators.

Example 3. In order to avoid shattering of crossarms when struck by lightning, parallel horn gaps are sometimes used which have shorter spacings than the sections of wood being protected. The intention is to have the flashover voltage of the horn gap somewhat lower than that of the wood in parallel with it, so that flashover will occur over the gap and not along or in the wood.

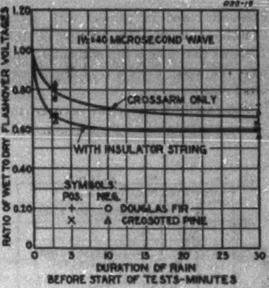


Figure 15. Effect of standard AIEE rain duration on critical impulse flashover voltage of suspension-insulator string and wood crossarm

Using the rod gap curves of figure 3 and the crossarm curves of figure 4, it is possible to estimate horn-gap spacings which will protect wood. Table I illustrates how such an estimate is made.

To protect the crossarm, it is evident that a 25-inch gap would be the longest permissible. In a series of tests run on six-foot sections of four creosoted pine crossarms, the protective gaps were found to be 24, 24, 25, and 27 inches, which are in good agreement with the above estimates.

Example 4. Horn gaps are occasionally used on poles in series with the down-lead to increase the impulse insulation of the pole top with respect to ground. A single pole gap with a 30-inch spacing is oc-

A—Douglas fir, lacquered, test 1
B—Douglas fir, lacquered, test 2
C—Red elm, untreated, test 1
D—Red elm, untreated, test 2
E—Red oak, untreated, test 1
F—Red oak, untreated, test 2
G—Douglas fir, untreated, test 1

Test 1—Four feet of wood plus a four-unit string of 10 by 5¾-inch suspension insulators

Test 2—Four feet of wood plus one 69-kv pin-type insulator

Figure 14. Effects of rain upon impulse flashover voltages of combinations of wood and insulators

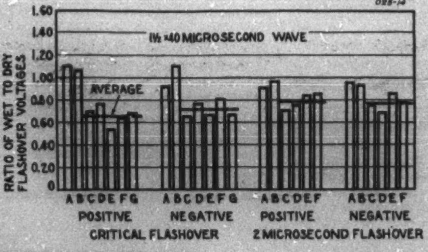
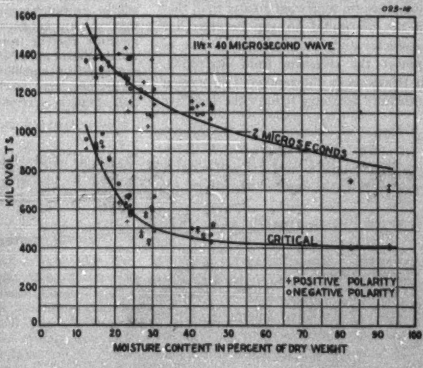


Figure 16. Effects of moisture contents of wood upon impulse flashover voltages of wood crossarms with suspension-insulator strings



casionally used in parallel with about 94 inches of wood. The example in table II shows that such a gap adequately protects the wood, even if it is newly creosoted pine.

The excellent agreement between the actual test values and the flashover voltage of the equivalent rod gap indicates that rod gap curves may be used for estimating horn-gap flashover voltages with a fair degree of accuracy.

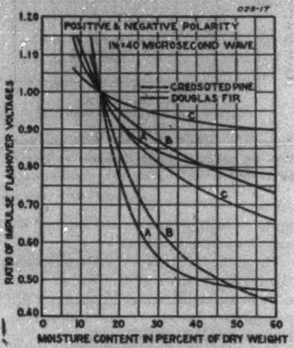


Figure 17. Ratio of impulse flashover voltages for various moisture contents relative to 15 per cent moisture

A—Critical flashovers, crossarms with suspension insulator strings

B—Critical flashovers, crossarms alone

C—Two-microsecond flashovers, crossarms alone or crossarms with suspension insulator strings

Example 5. In H-frame wood structures it is sometimes desirable to have the flashover voltage of the insulator string and crossarm somewhat greater than that of the horizontal air gap between line conductor and down-lead on the pole to protect the crossarm from shattering. In this example, the number of suspension insulators necessary to force the flashover to occur on the horizontal gap is determined (table III).

Table II. Thirty-Inch Protective Pole Gaps on 94 Inches of Newly Creosoted Green-Pine Poles

	Positive		Negative	
	Critical	Two-Microsecond	Critical	Two-Microsecond
Flashover of 94 inches of pole (figure 5) in kv	700	1,200	700	1,200
Flashover of 30-inch rod gap (figure 3) in kv	500	770	570	770
Actual test value in kv	485		570	

Table III. Required Number of Suspension Insulators on Six Feet of Creosoted Pine Crossarm to Force Flashover on Six-Foot Air Gap to Down-Lead

	Positive		Negative	
	Critical	Two-Microsecond	Critical	Two-Microsecond
Flashover of six-foot rod gap (figure 3) in kv	1,120	1,850	1,270	1,850
Flashover of crossarm with the following number of suspension insulators (figures 2 and 7) in kv:				
6 insulators	1,130	1,510	1,300	1,720
7 insulators	1,190	1,630	1,350	1,840
8 insulators	1,230	1,750	1,410	1,960
9 insulators	1,280	1,870	1,460	2,080
10 insulators	1,340	1,990	1,520	2,200

From table III, it is evident that the nine-unit insulator string is the shortest causing flashover voltages higher than all corresponding voltages of the rod gap. Accordingly, it should be expected to give the desired crossarm protection. In a series of actual tests on creosoted pine crossarms, a ten-unit string was required to cause flashover along the desired path.

Conclusions

1. Properly selected and controlled wood may add appreciably to the lightning insulation of a line. The magnitude is more or less in proportion to the wood length and may be estimated from curves presented in this paper.

2. Almost without exception, the lower the moisture content of wood the higher will be its supplementary lightning insulation value; in particular, reducing the moisture content from an average green-wood value of about 35 per cent to an average moisture equilibrium value of about 15 per cent may nearly double the insulation strength of creosoted pine structures, and raise that of fir structures by about one-fourth. The increase for various moisture contents may be estimated from figure 17.

3. During a rain storm, the lightning insulation strength of structures utilizing wood as supplementary insulation may suffer reductions to as low as 65 per cent of the dry flashover voltage within two or three minutes after the start of rainfall; further reduction, with continuation of the rain, is inappreciable.

4. The supplementary lightning insulation strength of wood is not appreciably altered by the grade of creosote, degree of retention, or creosoting process used in its treatment.

Reference

1. Lightning Strength of Wood in Power Transmission Structures, Philip Sporn and J. T. Lusignan, Jr. AIEE Transactions, volume 57, 1938, pages 91–101.

Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Analysis of Bushing-Current-Transformer Performance

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Synopsis: This paper is a presentation and discussion of the more important results obtained in a series of extensive tests made on bushing current transformers. In addition to the determination of the ratio and phase-angle characteristics, tests were made to determine the division of current in interconnected secondary circuits for conditions simulating actual faults in the primary circuit. It is also shown that most of the results obtained from the tests may be readily calculated from the excitation-current data.

IN the application of protective relays for transmission and high-voltage distribution substations, it is vitally important to give careful consideration to the ratio and phase-angle characteristics of the bushing current transformers used as accessory equipment. This has long been recognized, but the necessity for a wider scope of understanding of these factors and more information as to the methods of taking them into account has increased during recent years on account of the performance requirements of modern relaying schemes designed to provide more effective assurance of uninterrupted service. Furthermore, where there are multiple interconnected secondary circuits with totalizing phase and residual burdens such as result, for example, in a bus differential scheme of protection, there is the additional problem of determining the current distribution in the various branches of the secondary circuit for both balanced and unbalanced fault conditions.

The manner in which the ratio and phase relations of the secondary current to the primary current are affected by the components of current required for the excitation of the core and to supply the losses for steady-state conditions is well known,[1] but it is necessary for design and operating engineers to have more complete data than heretofore available, and to be provided with suitable methods of calculating the over-all performance under various conditions which may exist in the field. Similar information is also required regarding the performance during transient conditions but this phase of the problem is beyond the scope of this paper.

In order to obtain the necessary data regarding the performance of a particular relaying scheme, a series of extensive laboratory tests was made on a specific type of multiratio bushing current transformer for various secondary conditions. The purpose of this paper is, first, to present the more important results of these tests, and second, to show that the results so obtained can be calculated with reasonable accuracy from the in-phase and quadrature components of the excitation curves.

Some information regarding the wave-shape distortion of the secondary current by reason of the excitation component was also obtained and data regarding this phase of the tests are given in this paper.

Description of Transformers and Classification of Tests

The current transformers upon which the tests were conducted are quite similar in design to those in general use, consisting of a circular laminated core of silicon steel, a secondary winding with taps for different turn ratios, and a single-turn primary formed by the stud of the oil circuit breaker passing through the center of the core. By proper selection of the taps, turn ratios from 10 to 1 up to 120 to 1, with the exception of 70 to 1 and 110 to 1, may be obtained. In order to obtain better operating characteristics, these transformers were designed specifically to have larger core volumes than had previously been used.

For the purpose of this paper, the tests that were made may be classified and briefly outlined as follows:

(*a*). Ratio and phase-angle tests for different turn ratios and burdens.

(*b*). Single-circuit tests to determine the magnitude and phase relation of the current in each portion of the interconnected secondary circuits, and under conditions equivalent to a line-to-ground fault. The tests were made for three different turn ratios and for several different burdens.

(*c*). Double-circuit tests, similar to the preceding single-circuit tests.

(*d*). Miscellaneous tests, such as excitation curves of the transformers, the taking of oscillograms during the tests, and the determination of the burden characteristics.

Test Equipment and Procedure

Inasmuch as a high degree of accuracy such as is required for billing metering was not deemed necessary for the intended purposes, practically all of the tests were carried out with portable voltmeters, ammeters, wattmeters, and previously standardized instrument transformers.

The primary bus currents were obtained from low-voltage loading transformers, the primary voltage of which was controlled by a 25-kva single-phase induction regulator. Although this voltage regulator was overloaded during some of the tests, involving the higher burdens, and caused some distortion of the primary voltage and current waves, there is good reason to consider, as will be shown later by the closeness of calculated values with actual test values, that the results of the tests were not affected seriously by reason of the distortion that was introduced in the primary currents of the transformers. It is believed that the very presence of distortion for the higher current values actually aided in arriving at some important conclusion as to the amount of distortion introduced in the secondary current by reason of the large third harmonic component required by the excitation component of the primary current.

All of the oscillograms shown in the accompanying figures were obtained from a cathode-ray oscillograph, having a photographic attachment. A number of check oscillograms, however, were taken with a magnetic type of oscillograph and it was found that there was a substantially close agreement of the waves obtained with the two different types of oscillographs.

Burdens

Three different types of burdens were used during the tests: (1) variable nonsaturating burdens capable of carrying secondary currents up to 80 amperes; (2) nonsaturating burdens of lesser carrying capacity for the portions of the interconnected secondary circuits in which the secondary current did not exceed 10 amperes; and (3) saturating burdens made up of relay coils, compensator coils, and other iron-core reactive devices. It was the intention at first to use actual relays for the saturating burdens, but it was found that the windings could not stand the heavy secondary currents for the period of time

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J. A. Gebelein is with the Consumers Power Company, Jackson, Mich., and J. A. Elzi is with the Commonwealth and Southern Corporation, Jackson.

[1] For all numbered references, see list at end of paper.

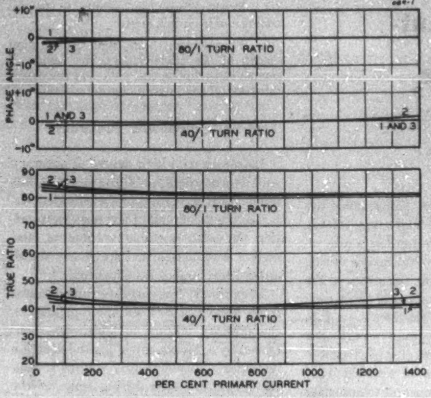


Figure 1. Phase-angle and ratio characteristics

Nonsaturating burdens, 40/1 and 80/1 turn ratios

Curve 1—18 volt-amperes, 0.5 power factor, single transformer

Curve 2—50 volt-amperes, 0.5 power factor, single transformer

Curve 3—50 volt-amperes, 0.5 power factor, two transformers in series

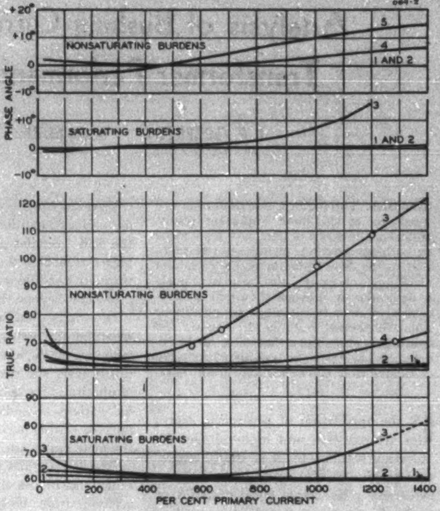


Figure 2. Phase-angle and ratio characteristics

Saturating and nonsaturating burdens, 60/1 ratio

Curve 1—18 volt-amperes, 0.5 power factor

Curve 2—50 volt-amperes, 0.5 power factor

Curve 3—200 volt-amperes, 0.5 power factor

Curve 4—100 volt-amperes, 0.5 power factor

required to take the various instrument readings.

Single-Phase Ratio and Phase-Angle Characteristics

Figures 1 and 2 show the ratio and phase-angle characteristics for three different turn ratios—namely 40:1, 60:1, and 80:1. There does not appear to be much necessity to enter into any detailed analysis of these curves, excepting to call attention to the fact that while in this type of current transformer there is a greater deviation from true ratio and phase angle at values of current near rated current than is permissible in instrument transformers, the reverse is true for the higher values of current. Relay performance is usually associated with current magnitudes well above the rated current of the transformer, and from this standpoint the bushing current transformer is superior to an instrument type. There are, however, cases where relays are required to operate at values of current approaching full-load current, so that the entire performance curve is of importance. The curves also bring out the fact that, particularly for relatively heavy burdens at poor power factor, the ratio and phase-angle characteristics are much better for the higher turn ratios than they are for the lower ones, and a real advantage can frequently be gained by using the higher turn ratios.

In order to show to what extent the ratio and phase-angle performance has been improved by using the extra-volume cores, it would undoubtedly be interesting to present at this point some corresponding curves for transformers having smaller volumes. Unfortunately, such data are not readily available but an equation has been derived from the test data which expresses the approximate relation between a change in volume from a reference volume and the ratio of the primary current to the secondary current. This equation, together with its derivation, is given in the appendix.

A number of tests were made on two transformers with their secondaries connected in series, curves of which are given in figure 1 and show that for burdens of less than 50 volt-amperes and with turn ratios above 40:1, there is little gain in actual accuracy, at least for the primary currents that have been considered. From the test results, and also from the equation that has been deduced for the volume-ratio relation, it appears that the ratio curves that will be obtained for two transformers with their secondaries in series for a certain burden, are approximately the same as is obtained with one transformer with half that burden.

The curves so far discussed apply to nonsaturating burdens. In figure 2, curve 3, the ratio and phase-angle curves are shown for a 200-volt-ampere saturating burden, on the basis of a five-ampere rating. During the tests, a complete record was kept of the volt-ampere burden that the saturating burdens imposed upon the secondary and it was found that a nominally rated 200-volt-ampere saturating burden actually imposed the equivalent of a 110-volt-ampere nonsaturating burden at 55 amperes secondary current. This change in the relative value of the burden will explain why the curve for a 200-volt-ampere saturating burden corresponds very closely to the curve for the 100-volt-ampere nonsaturating burden. Inasmuch as the burdens obtained in actual installations have in general saturating characteristics of widely varying degree, it may be important in some cases that these saturating characteristics be known and taken into consideration.

It is, of course, to be understood as previously stated that the test results apply only for steady-state conditions. Some information regarding the performance of current transformers under transient conditions, particularly for primary currents within the rating of the transformers, has been published.[1-3] In conducting the tests described herein, due precautions were taken to eliminate any residual magnetization of the cores which might affect the results of the tests.

Calculation of Ratio and Phase-Angle Characteristics

It is generally accepted that in the case of standard types of instrument transformers operating below the saturation point, the ratio and phase-angle characteristics can be calculated from the excitation characteristics and give results that are in substantial agreement with actual test data.

The symmetrical form of the core of the bushing transformers and the practically uniform distribution of the secondary winding, at least for the higher turn ratios, gave reason to consider that it should be possible similarly to calculate their ratio and phase-angle characteristics. This is very desirable because of the expense and equipment necessary to make actual tests, especially at the higher current values.

Figure 3 shows the in-phase and quadrature components of the excitation current as obtained in the test made on the secondary side with 40:1, 60:1, and 80:1 turn ratios. In these excitation tests, the current values were measured by dynamometer ammeters. The voltages for the lower values were measured with a combination of a high-resistance rectifier voltmeter and ratio box. This combination was checked for each reading against a dynamometer voltmeter and care was taken to insure that the check test was made under practically the same voltage wave form as in the actual test. The relative proportion of in-phase and quadrature components of the excitation current was obtained by calculation from the watt loss measurements and the volt-ampere values. The power measurements were made with wattmeters having high-resistance voltage circuits and the readings were duly corrected for the losses in the test instruments themselves, the amount of which was very considerable at the lower values of excitation current.

The procedure for calculating the ratio and phase-angle characteristics from the excitation data is well known and fairly simple. Starting with a definite value of secondary current, the voltage necessary for a specific secondary burden is calculated. For accurate results the leakage reactance of the secondary winding itself should be taken into consideration in calculating the required secondary voltage. For the transformers under consideration the combined effect of the secondary resistance and leakage reactance was approximately 0.1 ohm and is practically constant for the three turn ratios of 40:1, 60:1, and 80:1. Knowing the voltage required, the corresponding excitation components are obtained from the excitation curve and added vectorially to the secondary current and the resultant multiplied by the turn ratio to obtain the corresponding primary current. Five or six such calculations for different secondary currents are generally sufficient to obtain the ratio curve. The encircled points shown in figure 2 indicate the calculated values as obtained by this procedure. If the calculations are made by the rectangular-component method, with the induced voltage taken as the reference axis, the phase angle between the secondary current and the primary current reversed can be determined.

Inasmuch as bushing current transformers are in general provided with a number of taps on the secondary winding, in order to obtain various turn ratios, it was considered desirable to determine whether the excitation curve for a certain turn ratio would be applicable for other turn ratios. For conditions below saturation, the excitation current may be considered approximately inversely proportional to the square of the turn ratio, but for conditions in the saturation region, this relation does not hold true, and hence in using the excitation curve of a particular turn ratio for calculating the values for some other turn ratio, it is found necessary to modify the procedure for calculating the excitation components.

Designating as N_1, the turn ratio for which the excitation curves are available and as N_2 the turn ratio for which the corresponding curves are desired, the first step is to obtain the excitation values for voltage values equal to $E \times N_1/N_2$ and the second step to multiply the values so ob-

Figure 3. Excitation curves

Points for 40/1 ratio calculated from 60/1-ratio curve

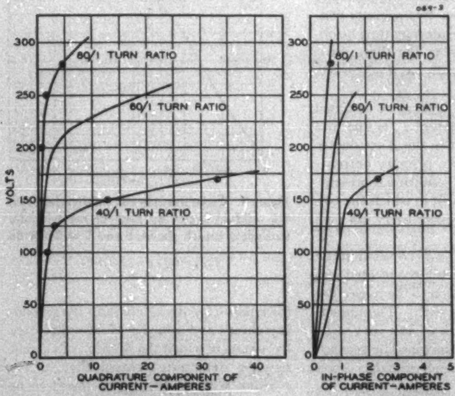
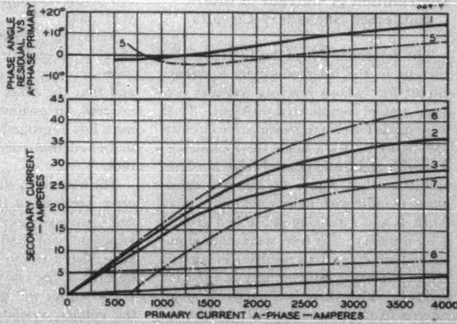


Figure 4. Current division for circuit shown in figure 6a

Nonsaturating burdens; 200 volt-amperes, 0.5 power factor residual circuit; 18 volt-amperes, 0.5 power factor each phase circuit

Zero current in primaries *B* and *C*:
Curve 1—Phase angle
Curve 2—*A*-phase secondary
Curve 3—Residual
Curve 4—Average *B* and *C* secondary

300 amperes in primaries *B* and *C*:
Curve 5—Phase angle
Curve 6—*A*-phase secondary
Curve 7—Residual
Curve 8—Average *B* and *C* secondary



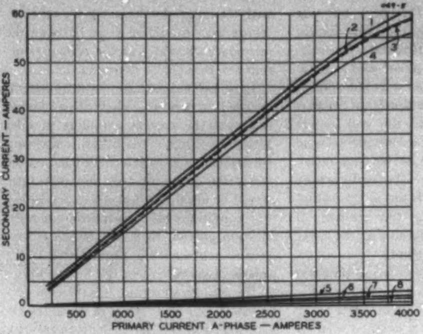


Figure 5 (left). Current division for circuit shown in figure 6c

Nonsaturating burdens; 50 volt-amperes, 0.5 power factor totalizing residual circuit; 18 volt-amperes, 0.5 power factor each phase circuit; 18 volt-amperes, 0.5 power factor each phase totalizing circuit; zero volt-amperes each circuit residual

Zero current in all primaries except phase *A*

Curve 1—*A*-phase circuit number 1 secondary

Curve 2—*A*-phase totalizing

Curve 3—Residual circuit number 1

Curve 4—Totalizing residual

Curve 5—Residual circuit number 2

Curve 6—*B* or *C* phase totalizing secondary

Curve 7—*A*-phase circuit number 2 secondary

Curve 8—Average *B* and *C* secondary circuits 1 and 2

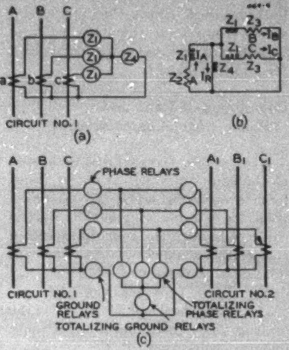


Figure 6 right). Typical circuits involving interconnected secondary circuits

tained by N_1/N_2. The result gives the excitation components for the N_2 turn ratio at E volts. The encircled points in figure 3 show the calculated values of the excitation components for the 40:1 and 80:1 turn ratios obtained from the 60:1 excitation test curves by the above procedure.

Distribution of Current in Interconnected Secondary Circuits

The determination of the distribution of current in the interconnected secondary circuit for various relaying schemes presents a more complicated problem. Tests were made for two representative circuits, one, figure 6*a*, for a single three-phase circuit with phase and ground relay burdens, and the other, figure 6*c*, for two three-phase circuits with individual phase relay burdens and totalizing phase and ground relay burdens. The tests were made simulating fault conditions resulting in current in one primary conductor only and also for faults in which there is a relatively small current in the two unfaulted conductors. The results of these tests are shown by the curves of figures 4 and 5.

In addition to providing information as to the distribution of current for the particular current transformers and burdens tested, a study of the results has been of assistance in arriving at analytical methods of calculating the distribution of current in similar circuits from the excitation characteristics of the current transformers and the impedance of the various burdens. It may be safely assumed that whatever current will flow through the idle secondaries will be in the nature of an excitation current and that no appreciable power or apparent power will be transformed to the respective primary windings of the two transformers. In other words, the impedance offered by each of these idle secondary windings is the same as the excitation impedance. In the case of primary currents in each of the three phases, a solution is obtainable by making calculations for each primary current separately, assuming no current in the other two primary conductors. The final result may then be obtained by superimposing the three separate solutions numerically or, if necessary, vectorially.

Referring to figure 6, let it be assumed that the secondary current I_A is due to a given primary current in the A conductor and that the other two primary currents are zero, the assumed condition representing a line to ground fault in which the two unfaulted line conductors carry no current.

The current I_A will be distributed in the other portions of the interconnected circuit in inverse proportion to the respective impedances of each branch. The impedance offered to the flow of any part of I_A by each of the two otherwise-idle secondary windings will depend primarily upon the voltage available at their respective terminals and their excitation characteristics. If the available voltage at the terminals of the windings can be determined, the value of the corresponding current may be obtained from the secondary excitation current and voltage relation. Therefore, in order to analyze the distribution of current in the interconnected circuit, it is necessary to determine the relative value of this voltage with respect to the current that may flow. Such a relationship may be deduced very readily for the simple circuit shown in figure 6*a*. For a more involved circuit, as for example that shown in figure 6*c*, the solution may be obtained by successive approximations. Although a rigorous solution may undoubtedly be formulated for any interconnected secondary circuit and a family of curves plotted by means of which the division of current may be determined graphically, such a solution will not usually be found to be

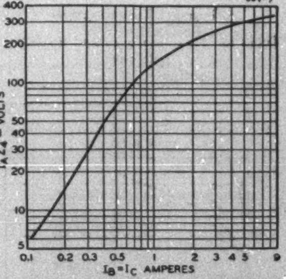


Figure 7. Curve showing current diverted from the residual circuit by each of the two idle secondary branch circuits B and C of figure 6b

$$I_A Z_4 = I_B(Z_1 + 2Z_4) + E_s$$

necessary because of the uncertainty of the accurate determination of other factors, as for example, the value of the fault current itself. It is for this reason that it is considered that certain other assumptions are justifiable.

In the treatment which follows, the assumption is made that if the power factor of all the secondary burdens be considered to be approximately 0.5, then the secondary current and the excitation current through the idle secondary windings may be added numerically instead of vectorially and thereby lead to a simplification of the problem and still give results that appear to be sufficiently accurate for the intended purposes. With the assumptions that have been mentioned, the following analysis has been made using figure 6*b*, which is the simplified circuit for figure 6*a*.

Legend

Z_t = total impedance imposed on the *A* secondary winding

I_A = secondary current in the *A* secondary winding due to primary current in phase *A*. Primary current in phases *B* and *C* equal to zero

E_1 = total voltage induced in the *A* secondary winding

E_2 = voltage available at terminals of branch circuits

E_3 = voltage available at terminals of idle secondary windings

The other symbols used below are shown in figure 6*b*.

$$Z_t=(Z_1+Z_2)+(Z_1+Z_3)Z_4/D \qquad (1)$$

$$D=Z_1+Z_3+2Z_4 \qquad (2)$$

$$E_1=I_AZ_t \qquad (3)$$

$$E_2=E_1-I_A(Z_1+Z_2)=I_A(Z_1+Z_3)Z_4/D \qquad (4)$$

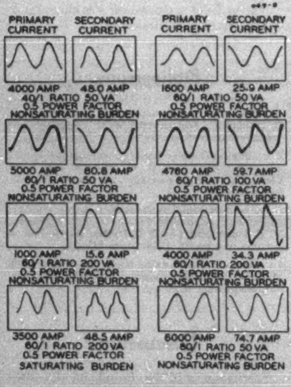
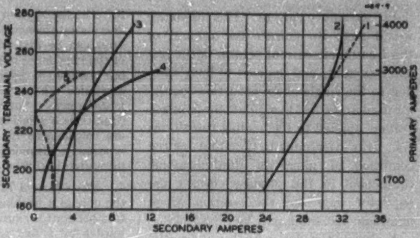


Figure 8. Oscillograms of typical primary and secondary current wave shapes

Figure 9. Relation of fundamental and third harmonic components of secondary current

200-volt-ampere nonsaturating burden, 60/1 turn ratio



Curve 1—Secondary current, equivalent sine wave

Curve 2—Secondary current, 60-cycle fundamental

Curve 3—Secondary current, third harmonic

Curve 4—Excitation current, third harmonic on secondary basis

Curve 5—Third harmonic in primary current on secondary basis

$$I_B=I_C=I_AZ_4/D \qquad (5)$$

$$I_R=I_A(Z_1+Z_3)/D \qquad (6)$$

$$E_3=E_2-I_BZ_1=I_AZ_3Z_4/D \qquad (7)$$

Substituting E_3/I_B for Z_3 in equation 7 and simplifying, gives:

$$I_B(Z_1+2Z_4)+E_3=I_AZ_4 \qquad (8)$$

Since the values I_B and E_3 are related by the excitation curve, new curves may be drawn for $I_B(Z_1+2Z_4)+E_3$ versus I_B and from such a curve the value of I_B corresponding to the value I_AZ_4 may be found.

Figure 7 shows such a curve and it is derived from the excitation curve for the 60:1 turn ratio. The burden values Z_1 and Z_4 are taken as 0.8 and 8.0 ohms respectively, the same as were the values of the burdens used in the interconnected secondary circuit in the test for which curves are shown in figure 4. A curve closely approximating curve 4 in figure 4 can be obtained by means of the curve, figure 7.

In the case of the double interconnected circuit shown in figure 6*c*, the best procedure appears to be to start with a first approximation of the current in the residual circuit and proceed to the other branches, using a voltage and current relationship as was done for the simple circuit. In connection with this particular interconnection, attention is called to the possibilities that the amount of current diverted away from the totalizing residual circuit by the A_1 branch circuit may reach values equal to or even greater than the residual current itself if the burdens of the phase relays and circuit ground relays are low compared to the totalizing ground relays.

Wave Distortion of Secondary Current

The excitation component of current introduces not only ratio errors and phase-angle displacement but also affects the secondary-current wave shape. During the tests, numerous oscillograms were taken of the primary current, the secondary current, and the secondary induced voltage. Additional oscillograms were also obtained of the excitation current. Most of those which showed considerable distortion were analyzed for the fundamental, the third, the fifth, and the seventh harmonic components. A number of these oscillograms are given in figure 8.

The theory of wave distortion by transformers in general and its effect on relay operation has been discussed in previous publications[1,4] and only a brief discussion of the test results is given here. It will be noted from the oscillograms that the distortion is very small for the low burdens. In the case of a 50-volt-ampere burden and with 60:1 turn ratio, the distortion in the secondary current does not become noticeable until the primary current reaches a value of 5,000 amperes, or 16.7 times the normal primary-current rating. In the case of the 200-volt-ampere burden, distortion begins at about 1,200 amperes, or four times the normal rating.

The curves in figure 9 which show the actual values in amperes of the fundamental and the third-harmonic component in the secondary current for different values of secondary-terminal voltages, were obtained by analyzing the oscillograms made during the tests on the 60:1 turn ratio and a 200-volt-ampere nonsaturating burden. Curve 4 in this figure was plotted from the excitation-current waves obtained during the excitation-current tests on the 60:1 turn ratio. The dashed curve number 5, shown in this figure, represents the difference between the third harmonic component actually required for excitation (on a secondary basis) and the amount of the third-harmonic component in the secondary current. This difference would seem to indicate the amount of third-harmonic current present in the primary current, and appears to have been due to the single-phase induction regulator used in the test to regulate the primary voltage applied to the loading transformers. It is interesting to note that for the condition of minimum third-harmonic component in the primary current the regulator was near the position where minimum distortion may be expected.

Analyzing the matter further, it appears tenable to state that the amount of the third-harmonic component in the secondary current is approximately equal to the vector difference of the third harmonic actually present in the primary current and the amount of third-harmonic current actually required for excitation, all values reduced to a secondary basis. Hence, the further deduction that if the fault current during a line-to-ground fault is sinusoidal, then the amount of third-harmonic component present in the secondary current should be equal to the third-harmonic component required for excitation. Although this would be true only if the burdens are nonsaturating, nevertheless, it would appear from the analysis of some of the oscillograms that with saturating burdens, the amount of the third harmonic is not altered very greatly from that with nonsaturating burdens, excepting that there is a different phase displacement of the third-harmonic component with respect to the fundamental which results in a more peaked secondary-current wave for saturating burdens than is the case for nonsaturating burdens.

As to the fifth and higher harmonics, no attempt was made to correlate the results, mainly because the analyzation of the waves for such harmonics could not be carried out within a reasonable degree of accuracy.

Conclusions

Although this series of tests was made on a specific set of bushing current transformers, an analysis of the results leads to certain conclusions which are applicable to this general type of transformer. These may be summarized as follows:

1. Bushing current transformers having cores of adequate volume and reasonably high turn ratios have characteristics which are very desirable from the standpoint of relay application and in some cases may be superior to instrument-type transformers.

2. The performance characteristics of bushing current transformers can be determined with reasonable accuracy from the in-phase and quadrature components of the excitation current.

3. The distribution of current in circuits involving interconnected transformer secondaries can be arrived at by a consideration of the impedance of the burdens in the various branches of the secondary circuit and the excitation characteristics of the current transformers. For various primary circuit conditions a solution is obtainable by making calculations for each primary current separately, assuming no current in the other primaries, and then combining results.

4. The effect of increasing the volume of the core of a bushing transformer by a certain per cent is equivalent to decreasing the burden by a like per cent. Likewise, the use of two bushing transformers in series is equivalent to reducing the burden on one to half the total burden.

Appendix

True Ratio Versus Change in Core Volume

The relation of the true ratio versus change in core volume, is a function of the excitation current required for a particular burden. If the volume is increased by changing the axial dimension and the radial dimensions remain the same, the magnetic cross section will be increased correspondingly and hence for a given number of turns in the secondary winding and for a given excitation current, the flux and the voltage induced by the flux, will be increased in the same proportion. The iron losses will of course enter into consideration but it can be shown that the effect is negligible.

For instrument transformers which generally operate below the knee of the saturation curve, factors other than the true ratio, in general, determine the core dimensions. In the case of bushing current transformers with low ampere-turn ratings, the ratio accuracy is one of the important factors which determine the core dimensions. Inasmuch as this discussion is concerned mainly with reference to conditions in the saturated region, it is of interest to analyze the relationship of the true ratio with respect to changes in the core volume, for such conditions only.

For such an analysis, the portion of the excitation curve above the knee may be represented by an equation of the form $I=k_1E/(1-k_2E)$. Now if V_1 represents a reference volume and V_2 the volume for which the ratio characteristics are desired, then if the change in volume from V_1 to V_2 is a change in the axial dimension only, the respective excitation currents, of the two volumes, will be:

For volume V_1,

$$I_{s1}=k_1E_{s1}/(1-k_2E_{s1}) \qquad (9)$$

For volume V_2,

$$I_{e1}=k_1E_{s2}V_1/(V_2-V_1k_2E_{s2}Z) \qquad (10)$$

The values E_{s1} and E_{s2} may be replaced by the respective products $I_{s1}Z$ and $I_{s2}Z$ in which I_{s1} and I_{s2} are the respective secondary currents and Z is the impedance of the secondary burden.

For the purpose of this analysis, the secondary burden is considered to have a power factor of 0.5 and on this basis the assumption is made that the excitation-current component and the secondary current are close enough in phase to permit adding their values numerically instead of vectorially. The respective true ratios for the two volumes are then given by equations 11 and 12.

$$R_1=N\left(1+\frac{k_1Z}{1-k_2ZI_{s1}}\right) \qquad (11)$$

$$R_2=N\left(1+\frac{k_1ZV_1}{V_2-V_1k_2ZI_{s2}}\right) \qquad (12)$$

Substituting $I_{p1}=R_1I_{s1}$ and $I_{p2}=R_2I_{s2}$ in the two preceding equations, and equating the two for $R_1=R_2$, gives the following relationship of the primary currents:

$$I_{p2}=I_{p1}+\frac{(V_2-V_1)}{V_1}\,\frac{R_1}{K_2Z_1} \qquad (13)$$

Hence, if the corresponding values of I_{p1} and R_1 are known for volume V_1, the corresponding primary current I_{p2}, for which the true ratio is equal to R_1, may be calculated. The value of the constant k_2 may be determined from the excitation-current curve.

The conclusion that may be deduced from curves plotted by means of equation 13 may be stated in various ways but it would seem that the following would best serve the purpose, namely, that the gain in ratio accuracy that is obtained by increasing the axial dimension of the core by a certain percentage is approximately the same as would be obtained with the original volume by reducing the value of the burden the same percentage.

Ratio Versus Burden Impedance

For approximate determination of the ratio curve for a burden of Z_2 ohms impedance, if the curve is known for a burden of Z_1 ohms and of the same power factor as Z_2, the following equation has been found to give sufficiently accurate results for conditions in the saturating region.

$$I_{p2}=I_{p1}+\left(\frac{Z_1-Z_2}{Z_2}\right)\frac{R_1}{KZ_1}$$

The value of the constant k is the same as in the preceding equation 13.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

A Proposed Method for the Determination of Current-Transformer Errors

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CURRENT TRANSFORMERS for relay service must be able to function with reasonable accuracy under very high overload conditions, and it is therefore necessary that their accuracy for such high currents be verified. However, in performing the usual accuracy test at very high currents, certain difficulties are encountered—the principal ones being:

(*a*). The difficulty of providing large amounts of power at large current values and bus-bar type wiring required. The kilovolt-amperes required at 20 times normal current is 400 times that at normal current.

(*b*). The necessity for standard burdens capable of withstanding heavy currents.

(*c*). The necessity for taking the meter readings very rapidly—within a few seconds—in order to avoid overheating of the current-transformer windings.

This paper proposes an indirect method for determining the accuracy characteristics—phase angle and ratio—of a current transformer for both normal and abnormal currents, whereby the difficulties of test mentioned above are obviated, with no sacrifice of essential accuracy. It consists in the determination by test of the impedance and of the excitation characteristics of the transformer and calculating from them the ratio and phase-angle errors of the transformer.

In this respect the proposed indirect method, as a method of calculation of the *current* regulation of the transformer, falls in identically the same category as the standard procedure for determining the voltage regulation of a power transformer indirectly from impedance measurements.

Equivalent Circuit of a Transformer as Reduced to 1:1 Ratio

The errors of a current transformer arise from the fact that not all of the primary current is transformed, a component (I_e) being used to excite the core. This may be seen more clearly by referring to the conventional equivalent circuit of a transformer as shown in figure 1. In this diagram *R* and *X* are respectively the resistance and the reactance of the external burden, and r_1, r_2, x_1, x_2, respectively the resistances and reactances of the primary and secondary windings.

The exciting current is conceived of as occasioned by a load in a fictitious third winding. From the equivalent circuit it is easily seen that the value of the by-passed exciting I_e depends upon the value of the induced voltage E_2, and it follows therefore that the characteristics of a current transformer are defined when the values of r_1, r_2, x_1, and x_2 are known and the exciting current I_e is given as a function of E_2.

For if these values are known, then it follows from the equivalent circuit that:

$$I_2 = I_1 - I_e \tag{1}$$

and:

$$E_2 = I_2\{(r_2+R)+j(x_2+X)\} \tag{2}$$

and consequently the ratio $|I_2|/|I_1|$ and the phase angle ($\angle I_2 - \angle I_1$) can be determined as illustrated below.

Determination of the Leakage Reactances

Of course r_1 and r_2 can be measured directly across each winding. In order to be able to measure the primary and secondary leakage reactance voltages directly it is necessary that there be only load currents in the windings and no exciting current—that is, the primary and secondary ampere turns must be exactly equal and opposite. This condition can be realized in a 1:1 ratio transformer by connecting the primary and the secondary windings in series opposition and forcing the desired current through them.[1] The voltages measured across each winding are then caused respectively by the "primary" and "secondary" internal impedances of the transformer. Knowing the current, the leakage reactances x_1 and x_2 can be calculated.

To insure equality of primary and secondary ampere turns in transformers other than 1:1 ratio, some expedient must be introduced. One such method is illustrated in figure 2. This figure shows a bushing-type current transformer. However, the scheme can be used for any current transformers provided they do not incorporate methods of compensation other than the regular turn compensation. In figure 2, *B* is the transformer of which it is desired to determine the secondary leakage reactance; *A* is an auxiliary current transformer which is used to balance the primary and the secondary ampere turns of the transformer *B*.

The auxiliary "balancing" current transformer *A* is wound with suitable taps and its ratio of transformation is chosen to be *identical* with that of current transformer *B* under test. The auxiliary current transformer *A* has also an exploring winding *N*4 which is connected to a vibration galvanometer.

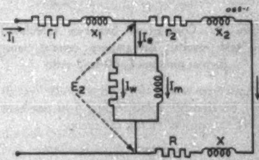

Figure 1. Equivalent circuit of a current transformer

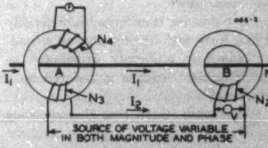

Figure 2. Method for determining the secondary impedance of a current transformer

The primary winding of the transformer to be tested is connected in series with the primary winding of the balancing transformer and to a source of voltage supplying the primary current. Likewise the secondary windings of the transformers are connected in series and to a source of voltage supplied by a phase shifter.

The phase and magnitude of the current supplied by the phase shifter are ad-

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[1] For all numbered references, see list at end of paper.

justed until the flux in the balancing transformer is zero. This is determined by a zero reading on the vibration galvanometer. The value of I_2 is measured by an ammeter and the voltage V by a voltmeter, across the secondary of the transformer B. The latter is proportional to the internal secondary impedance of the transformer.

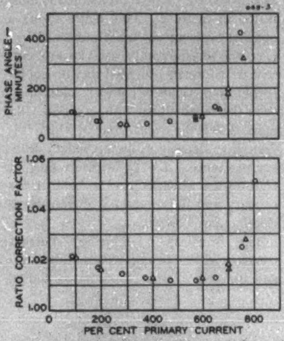

Figure 3. Comparison between calculated and test results, 300-ampere current transformer number 1, 60-to-1 ratio

Bushing type with distributed secondary winding; burden 50 volt-amperes, 100 per cent power factor, 60 cycles

⊙—Calculated △—Test

The balancing transformer is preferably of ring form with its primary and secondary windings uniformly distributed so that magnetic leakage is reduced to a minimum.

It has been stated that the balancing transformer of figure 2 needs to be of the same ratio as the current transformer under test. To fulfill this requirement it is necessary to know the secondary number of turns of the current transformer. There are several ways in which this can be obtained by test,[2] but the description of these methods falls outside the scope of this paper. Of course the simplest way is to obtain this information directly from the manufacturer.

In these tests, two methods were used for obtaining the variable source of voltage:

(*a*). By deriving this voltage from a conventional phase shifter (a wound-rotor polyphase induction motor will serve)

(*b*). By the use of two generators the shafts of which are connected mechanically. The stator of one of the generators can be turned with respect to the other

The voltmeter V needs to be of very high resistance. A good plan is to use a copper-oxide voltmeter having an internal resistance of about 1,000 ohms per volt. The Tinsley-Gall a-c potentiometer can also be used to read the above voltage.

Variations of Secondary Reactance With Secondary Current of the Current Transformer

Theoretically the division of the leakage reactance of a transformer varies with the secondary current. The reason for this is that changes in permeability of the core at various densities affect differently the reluctances of the external and internal parts of the magnetic circuit—neither the iron magnetic circuit nor the air magnetic circuit, which are in parallel, having uniform section or length. It is found, however, by actual test that these changes are quite limited and not large enough to affect considerably the accuracy which can be obtained by the proposed method of test. Actual test results of the secondary reactance at two or more values of the secondary current are tabulated under the item of "Test Results".

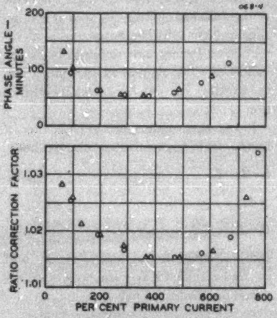

Figure 4. Comparison between calculated and test results, 300-ampere current transformer number 2, 60-to-1 ratio

Bushing type with secondary winding concentrated on approximately one-fourth of the circumference of the core; burden 50 volt-amperes, 100 per cent power factor, 60 cycles

⊙—Calculated △—Test

Determination of Magnetic Properties of Core Material

In order to determine the performance of a given transformer under definite conditions of load, it is now necessary to obtain values of the watt and wattless components of the exciting current (I_e) corresponding to the voltage E_2 which is applied across the secondary terminals of the transformer. It should be noted from the equivalent circuit of figure 1 that theoretically to obtain the actual value of I_e corresponding to the induced voltage E_2 there should be applied across the terminals of the transformer a somewhat higher voltage; but in general the voltage drop caused by the current I_e flowing through the internal impedance of the transformer is very small and can be neglected.

In the measurements of watts and exciting current, the fundamental components of these quantities must be taken. In the tests made for the preparation of this paper, a Tinsley-Gall a-c co-ordinate potentiometer was used.[3] Other methods can be used when this equipment is not available. For instance, the fundamental component of the exciting current can be obtained by means of a separately excited wattmeter, the separate excitation furnished by a phase shifter.

Calculation of Current-Transformer Characteristics From Test Results

If the magnetic properties of the core of the current transformer and the leakage reactance of the secondary winding are known, the ratio and phase angle for any secondary current or burden on the current transformer can be calculated quite accurately.

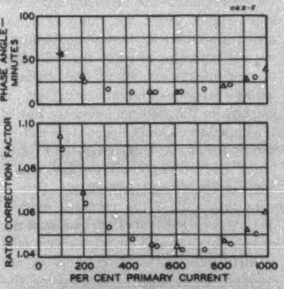

Figure 5. Comparison between calculated and test results, 200-ampere current transformer number 3, 40-to-1 ratio

Bushing type with secondary winding concentrated on approximately one-sixth of the circumference of the core; burden 50 volt-amperes, 100 per cent power factor, 60 cycles

⊙—Calculated △—Test

Agnew[4] gives the following simplified formulas for calculating the errors in current transformers:

$$\text{ratio } (R) = \left(n + \frac{M \sin \phi + F \cos \phi}{I_2}\right) \frac{1}{\cos \theta} \quad (3)$$

$$\tan \theta = \frac{M \cos \phi - F \sin \phi}{n I_2} \quad (4)$$

where:

I_2 is the secondary current
n is the ratio of number of secondary to primary turns
θ is the phase angle of the current transformer
ϕ is the phase angle of the secondary current with respect to the induced voltage
M is the magnetizing component of the core exciting current
F is the corresponding loss component of exciting current (due to hysteresis and eddy losses in the core)

In order to obtain M and F it is simply necessary to multiply the wattless volt-amperes (VA) and the watts (W) respectively by:

$$\frac{n}{E_s}$$

After obtaining the internal secondary impedance of the given transformer and its excitation characteristics by the use of formulas 3 and 4 it is relatively a simple matter to determine the ratio and phase angle under any conditions of current and of burden. The method will be illustrated by an example and the results compared with test results obtained by a conventional method of testing.

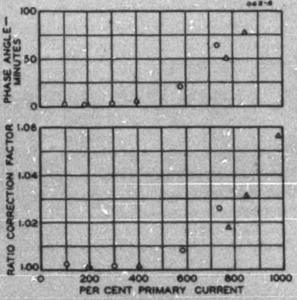

Figure 6. Comparison between calculated and test results, 100-ampere current transformer number 4, 20-to-1 ratio for 4,500-volt circuit

Wound-type current transformer; burden 50 volt-amperes, 50 per cent power factor, 60 cycles

⊙—Calculated △—Test

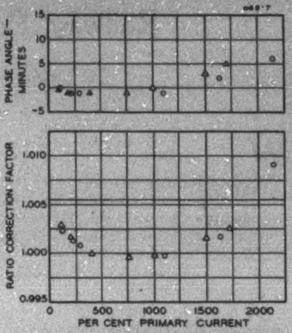

Figure 7. Comparison between calculated and test results, 50-ampere current transformer number 5, 10-to-1 ratio for 69,000-volt circuit

Wound-type current transformer; burden 50 volt-amperes, 50 per cent power factor, 60 cycles

⊙—Calculated △—Test

Sample Calculation

To illustrate the method of test and the necessary calculations to determine the current-transformer errors, the following example is given:

Characteristics required on a bushing-type current transformer rated 300/5 amperes, having a concentrated secondary winding. Core dimensions—inside diameter = 8 inches, outside diameter = 10 inches, height = 3 inches
Secondary turns = 60

External burden = 50 volt-amperes at 100 per cent power factor

Balancing transformer consisting of a bushing current transformer having a core of the following dimensions: inside diameter = 16 inches, outside diameter = 20 inches, height = 3 inches. Secondary turns = 60 uniformily distributed over the core. Exploring winding of 100 turns—also uniformily distributed.

(a). DETERMINATION OF SECONDARY REACTANCE

Test made at five amperes, 60 cycles
Voltage read across the secondary of the current transformer under test (at balance) = 1.70 volts
Measured secondary resistance of the transformer = 0.14 ohm
Calculated secondary reactance of the transformer = 0.31 ohm

(b). TEST ON THE MAGNETIC CHARACTERISTICS OF THE CORE

For the sake of brevity only a few points are given:

E	W	I_e	F	M
60.12	20.64	0.775	20.58	41.70
70.32	29.62	1.31	25.50	74.4

(c). CALCULATION OF THE CHARACTERISTICS OF THE CURRENT TRANSFORMER

External burden—50 volt-amperes at 100 per cent power factor
$R = 2$ $X = 0$
Internal burden—$r_2 = 0.14$, $x_2 = 0.31$
Total secondary impedance:

$R + r_2 = 2.143$
$X + x_2 = 0.31$
$Z = 2.16$

$$\cos \phi = \frac{2.14}{2.16} = 0.99$$

$$\sin \phi = \frac{0.31}{2.16} = 0.143$$

E	I_2	M sin ϕ	M cos ϕ	F cos ϕ	F sin ϕ	R	θ
60.12	27.8	5.98	41.3	20.4	2.94	60.96	78′
70.32	32.56	10.68	70.36	25.26	3.66	61.14	2°, 1′

Table I. Measured Secondary Impedances

Transformer Number	r_2	X_2	At I_2 (Amp.)	Remarks
1	0.132	0.002	5	
2	0.143	0.31	5	
3	0.083	0.274	2.3	
3	0.085	0.276	4.55	
3	0.084	0.274	7.83	
4	0.29	0.249	2	
5	0.52	3.65 3.48 3.30 3.19	2.68 4.79 7.3 8.7	Transformer compensated (Wilson method)
5	0.52	2.79 2.79 2.76	2.67 5.25 9.40	Tranaformer uncompensated

These results are compared with conventional test in figure 4.

Test Results and Discussion

The following transformers were tested:

1. Figure 3—bushing-type current transformer, wound core, secondary winding uniformly distributed. Ratio 300/5. Inside diameter of core = 8 inches
2. Figure 4—bushing-type current transformer, wound core, concentrated secondary winding—otherwise same as above
3. Figure 5—bushing-type current transformer, wound core, concentrated secondary winding. Inside diameter of core = 18 inches. Ratio 200/5
4. Figure 6—wound-type current transformer for 4,500-volt circuit. Ratio 100/5
5. Figure 7—wound-type current transformer for 69,000-volt circuit. Ratio 50/5. This transformer can be compensated according to the Wilson method. Tests were made with and without compensation

From table I it is noted that the secondary reactance of an annular core with distributed secondary winding is negligible, which confirms the findings of other investigators.[5] Table I and figures 3 to 7 also show that from a practical standpoint the secondary reactance is constant and a measured value at normal current can be used for overcurrent calculations, provided the transformer does not incorporate any compensating scheme other than simple turn compensation. With certain compensating schemes an estimation of this reactance can be obtained by testing at several points and extrapolating. This value can be used when errors are small.

The curves in figures 3, 4, 5, 6, and 7 show good agreement between test values and calculated values up to saturation of the core. The results obtained above saturation in either method (test or calculation) are somewhat indefinite. The method used for the testing recognizes only fundamental values of the primary and secondary currents. At these points above saturation, distortion is unavoidable. It is difficult to duplicate this distortion by calculation, and furthermore there is some doubt whether results in this region are of much value since some relays respond to fundamental values, some to effective values, some to peak values, and others to intermediate values. It should be pointed out, however, that overcurrent tests are more often used to compare the performance of two or more different current transformers and for such purposes the proposed method of indirect testing would appear satisfactory.

Conclusion

A method of testing current transformers has been described which eliminates a number of serious difficulties encountered in the usual methods of testing and is applicable with satisfactory accuracy under practically all current and burden conditions.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Railway Power Supply

PHILIP TORCHIO
FELLOW AIEE

While all industries depend on the use of power few are more vitally affected by the circumstances of its use than the transportation industry. In this industry power not only represents a large item in the total cost of operation but, by the varied forms and ways in which it can be applied, exercises an important influence in the creation of conditions favorable to more efficient movement of freight and passengers, improvement in the standards of service and resultant promotion of the business.

The determination of the type of power most advantageous in a specific case presents a multiplicity of problems which require detailed study by persons who possess extensive practical experience. In a general way, however, all engineers recognize the fact that, where electric power must be utilized in small quantities in widely separated locations, it is more economical to install the generating plant at the point of use. As these locations become more numerous and are grouped together, better results can be secured by generating the power centrally and distributing it to the individual users. Similarly, in generating power for the movement of trains, where they are limited in number and size the conventional method of using a steam locomotive or other power unit on each train is followed with satisfactory results. As the trains increase in number and size a point is reached where it becomes far more economical and satisfactory from every standpoint to install the power-generating facilities at a central location and supply the current to motors on the trains through a distribution system.

In any specific case the railroad executive will have to co-ordinate and evaluate all the gains possible from electrification but, in general, past experience has shown that it may be considered as a means of moving trains in dense-traffic territory at no additional cost and in many cases at considerable saving, while at the same time providing the public with faster schedules and more reliable service and the management with increased capacity of plant. The full benefits of a railroad electrification usually are not secured until after it has been in service for a sufficient length of time to enable the management, through education, training, and supervision, to take full advantage of the improved tool with which it is supplied.

As further experience is secured with railroad electrification the first cost and operating expenses should be materially reduced by using the same intensive methods of supervision and development which, in the past, have produced so much improvement in the service and reduction in the cost of steam operation. As these results are obtained, electrification will doubtless extend to portions of railroads which, under present conditions, are not considered to be suitable projects for this method of handling transportation; the intensified study which many power companies are giving to the subject of railway supply will help to make these results possible.

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Changing Aspects of Power Supply

When electricity first became available for traction, it found a ready field of application in the transportation of passengers in cities and towns and a new industry was soon developed in which it was used exclusively for motive power on account of its remarkable flexibility in handling such traffic. In the case of existing transportation systems, however, the substitution of electricity for steam encountered the serious obstacle that it would require large new investments, involving the addition of wayside structures, equipment and conductors, possible changes in track and facility provisions, and a complete change in locomotive equipment. For this reason the railroads found it more advantageous to continue their long-established policy of improving the economy of steam locomotives, designing different types for specific services and, more recently, of adopting internal-combustion engines for high-speed light passenger trains, for yard switching, etc. Nevertheless, in certain situations other weighty considerations made it desirable or necessary to adopt electric traction. This was the case in the early electrifications of important terminals serving considerable suburban passenger traffic and, later, where heavy freight traffic and steep grades, or the carrying of freight and passenger traffic in competition with other means of transportation, made the change to electric traction profitable.

In the foregoing developments, the role of the utilities in connection with the supply of power for traction, has passed through two different phases:

Many years ago, when electric traction was first introduced for urban rapid transit and, subsequently, for the more important steam-railroad terminals, the electric utilities were not sufficiently developed to supply advantageously throughout their territories, the large quantities of special current required for traction. Consequently the transportation companies, as a rule, installed their own plants and extensive distributing systems.

Later, because of their rapid growth, the utilities had greatly expanded their generating and distributing facilities and found themselves in a position to supply power to the transportation companies more advantageously than the latter could produce it. This change in conditions prompted the consummation of many contracts for the supply of power.

Type and Scope of Contracts

We shall subdivide these contracts into two types:

First—Those covering the supply of power to railways operating their own generating and distributing plants.

Second—Those covering power sales to railways to be electrified without power plants of their own.

A utility, in dealing with these two classes of contracts, is confronted with distinctly different problems. In the first case it has to prove to the prospective customer that its service will be more advantageous than can be obtained from his old plant even if it is improved by an economical modernization. In the second case the utility has to prove that its service will be more advantageous than the customer can obtain from a heavy investment in modern generating and distributing plant to provide the requirements of the road to be electrified.

First Case

Where the railways owned good-sized 25-cycle plants for generating their power requirements, and the utilities had considerable 25-cycle capacity installed for their own use, the parties were able to evolve special workable power arrangements based on interconnecting the railways' plants with the utilities. The less efficient railway plants were then operated for a minimum number of hours as in the case of the utilities' own generating capacity of older vintage, thereby realizing material operating savings which were equitably shared by both parties. In addition, the railways were relieved of the necessity of generating power, which must be a side issue for them since they are basically engaged in providing transportation, rather than in producing and distributing power economically.

These special power arrangements were dictated by the desirability of making full use of the unexpired economic life of the railways' plants without the necessity of large expenditures for their modernization, as is being successfully done on the utility systems, with assured increasing loads, by the installation of topping turbines. The railways are handicapped in carrying out a modernization program because it is generally difficult for them to dispense with a substantial part of their generating facilities during the protracted period required for reconstruction, and, if the construction plans are so worked out that the existing equipment is not disturbed the cost of the new facilities is likely to be excessive. Furthermore, since their rebuilt plants, in some cases, must be operated at the relatively poor traction load factor, they are not in a position to make the best use of the modern equipment and the operating savings may not be sufficient to counterbalance the added fixed charges. The utilities, by contrast, are in a position to modernize plants and to operate them advantageously for base-load generation, using the railway plants for emergency or standby service thereby filling the former role of the modernized plants. The old railway plants then readily fit in as a part of a co-ordinated program.

There is no standard form for such arrangements because the details to be covered vary greatly in each situation. Special provisions of the contracts usually are similar to those for straight power sales contracts hereafter described. The only essential requisite is that the net results must be such that the railways obtain the service at a lower cost than they could secure for themselves by modernizing their plants in order to raise their efficiencies to the standard afforded by the advancement in the art; and that the utilities obtain sufficient revenue to cover all operating expenses and fixed charges, including amortization of any new investment, plus allocated general expenses, taxes, and a fair share of the savings derived from the bulk production of the increased output. These savings will ultimately accrue to the benefit of the general customers in reduced rates.

Second Case

Contracts for the sale of power to traction companies not owning power plants are not essentially different from the usual form of "demand and energy rate" contracts and include such items as:

term of the contract—character of energy—points of delivery—use to be made of the energy—price of "demand" and "energy" charges—price adjustment for changes in cost of fuel, when power is produced by steam—definition of billing demands—service connections—billings and payments—etc.

In addition such important contracts also contain appropriate provisions, including:

dates for beginning specified partial services and full service—clauses to insure diligence in providing regular and uninterrupted service—access to records—testing of measuring instruments—arbitration—reduction of rates if more favorable rates are given other customers under substantially similar conditions.

In the preparation of each contract, the various provisions are carefully drafted to conform to the unique requirements of the case. Certain distinguishing characteristics of railway contracts, and the considerations on which they are based, are as follows:

(*a*). **A relatively long period of time is specified for the term of the contract and its renewals, if any.**—These contracts being for large amounts of important service, it is obvious that the parties must have a relatively long period of guaranteed operation of the facilities provided for the service. Contracts are rarely written for less than 10 years and usually cover longer periods of operation up to 20 years with provision for extension of time should the utility make substantial additions to its facilities for the railway's use during the initial period.

(*b*). **A guarantee of payment for a minimum amount of service each year is usually included.**—The utility must have a guaranteed minimum payment in order to cover the overhead charges and provide, during the term of the contract, for reasonable amortization of the special investment made.

(*c*). **Unusual methods of determining the monthly maximum demand for billing are used.**—The operation of a railroad is subject to occasional large variations of maximum demand from causes extraneous to the normal operation. These fortuitous conditions should not be allowed to penalize the railroad unduly. Hence the average of the three highest clock-hour maximum demands in each month has been adopted in a number of contracts as the billing demand. Furthermore, in cases of emergency, the large drafts of power following accidents or abnormal and unforeseen increases in traffic for short periods of time are usually furnished by the utilities, if they have the capacity available, without increasing the billing demand.

(*d*). **A provision is included which requires that each monthly billing demand shall not be less than a specified percentage of any previous billing demand.**—This supplements the requirements of (*a*) and (*b*) and takes into account the special characteristics of the railway's fluctuation in business from year to year. Except for some of the urban lines this fluctuation is very large, and if the railways were to pay for a high percentage of any previous demand when the traffic has shrunk considerably, their income would be diminished when vitally needed. For this reason they prefer that the unit demand charge be figured high enough to compensate the utility fully during the term of the contract, and that the guaranteed percentage of any previous billing demand which determines the yearly payment for "demand charge", be made more proportional to the amount of maximum load taken during each year.

(*e*). **The practice as to whether the railway or the utility should furnish and operate the machines required to deliver direct or single-phase current is not uniform.**—In the case of railroads electrified during the last few years the tendency has been to have the utility furnish, house, and operate such apparatus and supply energy directly to the trolley or transmission lines. In such cases provisions are made in the contract price for a proper compensation to cover the operating costs, conversion losses, and fixed charges including amortization. Clauses are also included for transferring ownership to the railway in the event of termination of the contract, at a sale price proportionate to the unamortized investment.

(*f*). **The utility, in establishing these differences in the terms of various contracts, carefully analyzes the effect of each variation and accordingly provides price differentials which will avoid discrimination among customers.** The general clause on reduction of rate if more favorable rates are given other customers also protects each purchaser in respect to these special provisions.

Underlying Economic Factors

The utilities are justified in making the foregoing departures from their standard forms of power contracts in the case of electrified railways, since experience has shown that this valuable business could not be secured without giving the underlying economic factors full consideration from the standpoints of both the railways and the utilities. We have already made occasional references to these factors but it is also important to explain more comprehensively how they affect the two industries.

LOAD FACTOR

In general the utilities have a much higher use of their maximum demand than the railways which means that they are able to render more service per kilowatt of plant capacity. This is caused by the great variety of their customers and the resulting time diversity of maximum demand and use of the service. Furthermore, although the railway's load factor is, in general, less favorable than the utility's, when the two loads are superimposed with their diversity in time of respective maximum demand, the combined load factor is improved and conditions are created for realizing substantial economies in plant investment. These investment economies are, of course, further enhanced by a saving in the amount of spare capacity which would be required if the railway were to install and operate an independent generating system for the lines to be electrified. Similar economies may also be realized in connection with the power transmission system. Therefore, if the railway electrification loads are supplied by a heavily-interconnected utility industry these factors will produce economic results to the advantage of both the railways and the utilities.

RATE OF GROWTH

The utilities with their continuous growth in load, in contrast with the more or less stationary load of a railroad for a given situation, are in a better position to continue to improve their operating economies, as they have done in the past.

These economies were realized in part from the fact that the quantities of power handled by the electric utilities became so large that it was possible for them to install generating and transmission units of large capacity with correspondingly lowered unit costs, reduced operating labor costs, and improved efficiencies. At the same time, the utilities were enabled to produce the major portion of their power requirements from their newer and more efficient units, by operating them at high load factors and gradually relegating their older plant to usage for peak or standby service.

For illustration, the graph, figure 1, gives the distribution of capacity and output according to the date of installation of units for a large system during the year 1934. It shows that of the total capacity, the 20 per cent installed before 1919 was used to give less than 2 per cent of the total output and the 50 per cent installed before 1926 was used to give less than 28 per cent of the total output while the other 50 per cent installed between 1926 and 1934 and incorporating the latest advancements in the art as to the size and efficiency of machines, higher steam pressure, higher steam temperature, more efficient boilers,

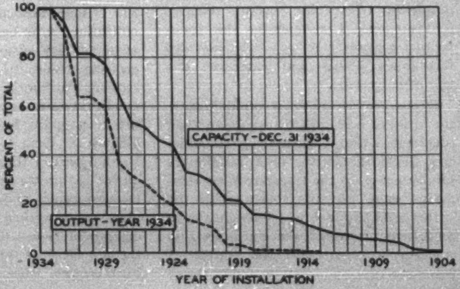

Figure 1. Total generating capacity installed in or prior to indicated year compared with energy generated by corresponding units during the year 1934

and all other station improvements generated 72 per cent of the total output. This progress in securing higher economies is a continuous one as long as the industry is growing.

SOURCES OF POWER

In contrast with the foregoing favorable situation of the utilities in supplying power, it is evident that large-scale electrifications under present-day conditions, if served with power by the railways themselves would either require several suitably located and relatively small power plants, which necessarily would involve high unit investment and operating costs, or alternatively would require excessive investment in transmission facilities in order to distribute over a widespread area from some centrally located plant. In either case a relatively high amount of reserve capacity would be needed to insure the reliability of the service. The advantageous position of the electric utilities with their large and economical plants at many load centers, and large-capacity transmission facilities generally available at other load centers not directly served by generating plants, is obvious.

Status of Electrification

Notwithstanding the fact that steam railroads have had the possibility of using electric traction available to them for many years, they have not found it feasible to utilize this form of power except for the few special cases to which we have referred. The reason for this has been stated to be the large new investment which would be required. It would be appropriate to add practical illustrations of the net results of operation before and after electrification, taking account of the changes in capital investments and all other factors affecting the net results. Unfortunately most of the comparisons available to the public deal in generalities or apply only to a specific phase of the subject. Furthermore, any comparison would be entirely dependent on the circumstances of the case, and would vary widely between different roads, or divisions of the same road, depending on traffic, profile, speed, and many other conditions.

Comprehensive data on the results of electrification have been published for the Chicago, Milwaukee, and St. Paul Railroad, and a recent analytical study of all the factors affecting the total net results of the Italian Railways may be found in Mr. Carli's report to the 1938 World Power Conference on "Comparative Results of Operations of Italian Steam and Electric Railways for the Year 1936–37" (*Elettrotecnica*, April 25, 1939). From the latter we can draw the conclusion that, apart from other important advantages *unrelated to the operating costs*, their savings by the use of electric traction are mainly due to the high cost of coal in Italy and would disappear entirely if their cost of coal were the same as in the United States. These conclusions are arrived at by a comparative analysis of Mr. Carli's tabulation, for the steam and electric systems, of all expense items that relate to the supply of power and operation of the transportation department, including interest charges on investment in locomotives and power equipment.

The aggregate expense per ton mile of steam traction was 17 per cent higher than for electric traction. Unfortunately Mr. Carli's study compares steam traction with electric under great differences in density of traffic; the electric lines with only one-third the mileage of the steam lines, carried $17^1/_2$ billion ton miles against $21^1/_2$ billion ton miles carried by the steam lines. This fact greatly favors the electric-traction system, and seriously limits the absolute value of the comparisons, although the relative value of the conclusions reached above are not disturbed. In analyzing the items of expense we find that the cost of coal was of the order of $8.70 per short ton. The equivalent cost for the class I railroads in this country was about $2.43, this low average price being partly due to the Interstate Commerce Commission accounting system which excludes from fuel expenses any cost or charges for its transportation on the reporting company's lines. But even if a liberal allowance were made for such charges, the American price per ton of fuel would have been less than one-half that of the Italian Railways. If the price in Italy had been on the same relative basis as in America the excess cost of steam traction would have been eliminated entirely.

The electric lines handled the 17.5 billion ton miles with an average consumption of 43 watt-hours per ton mile or a total of 750 million kilowatt-hours. Of this total the railways generated about one-fourth in four hydroelectric stations owned by the State, and purchased three-fourths from the utilities at 42 high-voltage delivery points. These results give a practical confirmation of the conclusions reached under the heading of "Underlying Economic Factors" and emphasize the fact that such economies are secured jointly with an infinitely greater insurance of continuity of service which is provided, not only by the large number of high-voltage delivery points, but also by the numerous generating plants back of them, which insure the supply of the utilities under abnormal conditions of operation.

Future Possibilities

Up to 1937 the railways in the United States had only electrified to the extent of consuming 1,627 million kilowatt-hours, which is estimated to be less than $2^1/_2$ per cent of the total business. This would indicate that, under present conditions and prevailing prices of coal, electricity has not demonstrated definite advantages over steam traction except in a very few special cases where other considerations unrelated to operating costs might have influenced the change or where heavy traffic conditions and relatively high operating speeds prevail. We need considerably more analytical information to clarify the problem as to why the largest industry in this country has not adopted electric traction more widely. For the present, therefore, it is impossible to make any sound forecast of its probable growth. We must, however, bear in mind that changes in this country are apt to be rapid and powerful and that the utilities may eventually find in the railways a field for steadily expanding their power sales. The theoretical demand for such services, if all the railways were to be electrified would be of the order of three-fourths the present output of the entire central-station industry in the country. Only a third of this potential total would be as large as the present consumption of all residential consumers.

There are many obstacles in the way of further electrification which are not related to the cost of electric power but, to the extent that this cost influences the whole problem, the utilities have demonstrated that the railways can buy electricity from them more advantageously than they can secure it in an alternative way. In securing this business the utilities would be benefiting all their customers, because the economies realized from service to the railways ultimately accrue to the advantage of all users of electricity.

Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Overcurrent Performance of Bushing-Type Current Transformers

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Synopsis: Conventional methods used for defining and comparing current-transformer performances are commonly based on their ratio and phase-angle characteristics. For years, these characteristics have been continuously improved, particularly with respect to the requirements for metering service. However, from the standpoint of circuit protection, current-transformer application becomes more involved and as a result, increased attention has been given it.

A better knowledge of transformer performance is necessitated by the requirements of new relays and protective schemes. Attention to the influence of the d-c component in short-circuit transients is especially necessary when using high-speed relays. Also, in differential connections, the necessity for equivalent matching of saturation characteristics is essential. These are illustrations of the types of problems which ratio and phase-angle characteristics alone do not solve. The analysis of open-circuit saturation curves has proved of immense value in the solution of these problems.

This paper gives results of investigations on bushing-type transformers that indicate the proper choice of analysis procedure and the limitations of the various methods for determining performance under overcurrent operating conditions.

THE PURPOSE of this paper is to present the results of an investigation on the scope and limitations of saturation and loss curves in the application of bushing current transformers to relaying problems involving the overcurrent performance. The investigation covered the method of taking such data, the methods of calculation and the accuracy of ratio and phase-angle performance as calculated from it, and the application of the data to simplification of relaying problems.

The use of voltage-saturation and watt-loss data to analyze the performance of magnetic circuits has always afforded a means of predetermining the performance of devices associated with such circuits. Generators, transformers, and similar devices have their basis of design on these known characteristics of the magnetic material used. This leads to simplicity of investigation for a given set of conditions, and provides information which otherwise could be obtained only with the conditions peculiar to the investigation.

Within certain limitations it should be possible to formulate a practical analysis of the overcurrent performance of bushing-type current transformers in terms of the induced voltage and associated losses in a specified magnetic circuit independent of the operating conditions. The latter would be specified in terms of ratio, secondary burden, and primary-current range. These characteristics, if given in a desirable form, should be as suitable to the application engineer as they have been to the design engineer, and should find a wide range of usefulness.

Transformer Performance Characteristics

The conventional method for defining and comparing the performance of current transformers has been their ratio and phase-angle characteristics. Considerable information has been published relative to the improvement of these characteristics, particularly over the ranges of load currents and burdens encountered in metering service. These characteristics, particularly ratio, have been extended to cover the higher ranges encountered in relaying service.

With the advent of new relays and protective schemes the application of current transformers has become more involved, and the use of the conventional performance data has often proved inadequate for successful solution of certain types of these problems. Recently much progress has been made in application of high-speed relays[2,3] with the attending problem of transient performance by the use of open-circuit saturation curves. Many have found this criterion of great benefit in the solution of problems involving the overcurrent performance of bushing-type current transformers.

Saturation and Loss Data and Vector Diagram

A theoretical consideration of the vector diagram of the current transformer figure 1, which is the same as that for the conventional voltage transformer, shows that in general the primary constants of resistance and leakage reactance do not influence the performance, except by virtue of the indirect influence of the primary upon the secondary leakage reactance.[1] The difficulty of separating these leakage reactances is well known, and any effective use of the open-circuit saturation curve for calculating performance requires a knowledge of the approximate value of this reactance. For through-type current transformers with a ring-type core, symmetrically spaced, distributed secondary winding, and reasonably spaced primary conductor, the secondary leakage for low and medium core densities is sufficiently small that it can be neglected.[1,4] The usual bushing-type current transformer fits these requirements and hence the open-circuit saturation curve and equivalent circuit should lend themselves to the simplification of many problems involving the knowledge of its performance.

Method of Measuring Excitation Characteristics

Of the numerous methods of obtaining saturation and loss data on current transformers, the direct measurement of these quantities on the secondary winding with the primary open-circuited has been chosen for its simplicity, flexibility, and economy. It has been found when making such measurements that the wave shapes of the currents and voltages, and the types of measuring instruments used have a direct bearing on the results. Previous experience indicated satisfactory calculations of transformer performance could be made from saturation and loss data taken by impressing sine wave voltages and measuring the rms voltages and currents.

As it is often difficult to determine and maintain wave form when obtaining saturation and loss data, tests were made to determine the influence of wave shape. A 600/5 multiratio bushing current transformer for a 138-kv oil circuit breaker was tested; first, using sine-wave impressed voltage, and second, circulating sine-wave exciting current. As illustrated by figure 2, both voltage and current are essentially sine wave for core densities below saturation, while either one or the other, or both, must be distorted when the core becomes saturated. The results obtained throughout the complete range of core densities are shown in figure 3. These curves also indicate, when using sine-wave

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[2] For all numbered references, see list at end of paper.

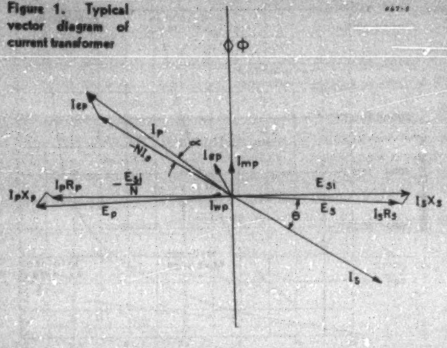


Figure 1. Typical vector diagram of current transformer

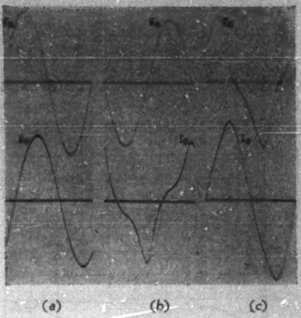


Figure 2. Typical oscillograms of current-transformer saturation characteristics

(a)—Sine-wave voltage and current—medium flux density

(b)—Sine-wave voltage—saturated density

(c)—Sine-wave current—saturated density

exciting current, the difference in results from measuring the voltage with a repulsion-iron voltmeter which reads rms voltage, and the Rectox voltmeter which reads the average voltage multiplied by the form factor 1.11.

It will be noted that there is quite close agreement between the sine-wave voltage and sine-wave current excitation curves, when the voltage, in the latter case, is measured with a Rectox voltmeter. This is in accordance with accepted practice, as it is known, that for a distorted voltage wave having the same average value as a given sine wave voltage, the maximum flux is equal. The equal values of maximum flux represent equivalent flux conditions independent of wave shape.

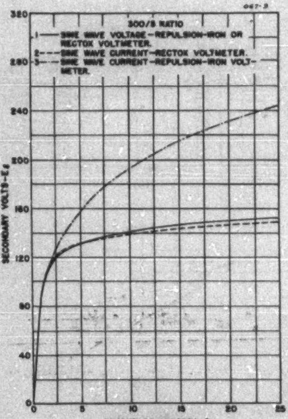


Figure 3. Saturation characteristic curves taken at sine-wave voltage, and at sine-wave current

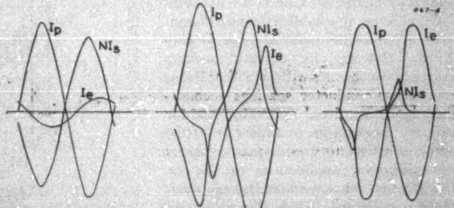
Under operating conditions the primary current of the transformer is essentially a sine wave. Through the lower-density range the exciting currents, secondary induced voltages, and burden currents are all approximately sine waves. In the saturated region all of these quantities are somewhat distorted. However, the errors introduced by these harmonics are relatively unimportant unless they are large, and then only when the total exciting current is an appreciable portion of the primary current. These conditions are illustrated by the oscillograms of figures 4*a*, *b*, and *c*.

It appears from a practical operating viewpoint that saturation tests may be made independent of wave form when using rms ammeters and voltmeters responding substantially to average voltage but with an rms sine-wave calibration. Such data will give sufficient accuracy for most performance calculations.

Effects of Primary and Secondary Winding Locations on Performance

In an effort to determine the effect of and variations in the secondary leakage reactance of bushing transformers at high values of overload currents, conventional ratio and phase-angle tests were made on several oil-circuit-breaker transformers. The standard multiratio transformer has the total secondary winding well distributed over the core, but the various lower ratios are obtained by means of tapping portions of the secondary winding, resulting in relatively poorer distribution on the low and intermediate ratios.

From a theoretical viewpoint the variation of secondary leakage with the position of secondary winding is increased with closer proximity of the primary return conductor, with higher magnitudes of primary current, with greater asymmetry of the secondary winding, and with increased core diameters. In this investigation the transformers were tested with the approximate spacing of the primary conductor as encountered in practice, and with two different positions of secondary winding with respect to the primary return as shown by positions *AA* and *BB* of figure 9. Position *AA* represents the middle axis of the secondary winding in a relation to the return conductor which gives the condition of mini-



(*a*)—Medium flux density

(*b*)—High flux density

(*c*)—Saturated flux density

Figure 4. Reproduced oscillograms showing wave shape of primary, secondary, and resultant exciting currents with a secondary burden of 50 volt-amperes, 100 per cent power factor

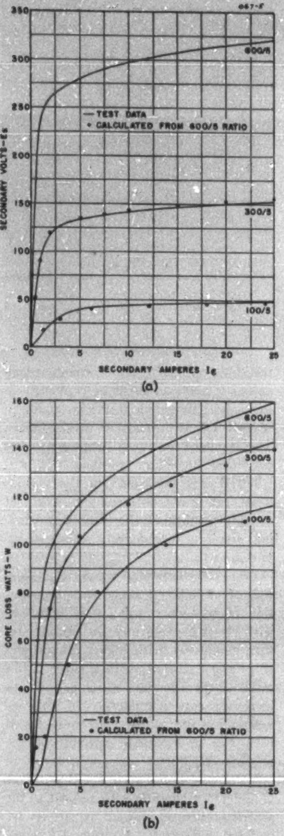


Figure 5 (left). Saturation and watt loss characteristics of a current transformer

(*a*)—Voltage versus exciting current

(*b*)—Watt loss versus exciting current

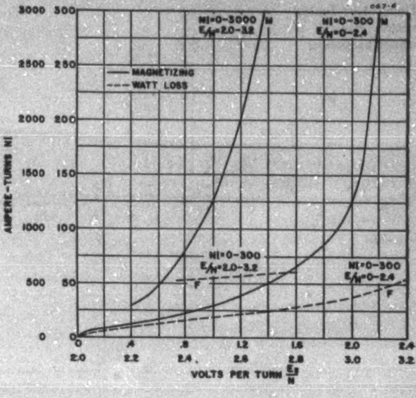


Figure 6. Ampere-turn versus volt-per-turn characteristics of bushing current transformer of figure 5

mum coupling, and likewise position *BB* gives a maximum coupling.

These tests, typical results of which are shown by figures 9*a*, *b*, and *c* are not sufficient in scope to establish definite boundaries for all of the parameters previously listed. However, for standard bushing current transformers with core dimensions and return conductor spacings encountered in conventional apparatus, and for primary currents up to 25,000 or 30,000 amperes, satisfactory results in calculations involving relay applications can be obtained when the secondary leakage reactance is neglected. This is particularly true when it is observed that most of such applications use performance data based on nonsaturating burden impedances, which is, at times, coarsely modified to meet actual operating conditions.

Various Forms of Saturation and Loss Curves

The saturation and loss data taken under the conditions previously outlined result in a set of data as shown in figures 5*a* and *b* for various ratios. The data thus taken can be shown in several different forms, and may be converted into similar data for any other ratio using the same magnetic core. Two forms are indicated by the curves of figures 5 and 6, and the methods which can be used for these conversions are given in appendix II. The first form gives voltage and watt loss versus exciting current in secondary terms for a given ratio, while the second form gives the core loss and magnetizing components on an ampere-turn versus volts per turn basis which is independent of the ratio.

Attention is directed to the curves in figure 5, where the full-line curves are plotted test data, while the indicated points on the 100/5 and 300/5 ratios are conversions from the 600/5 data using equations 10, 12, and 13 of appendix II. These curves indicate that very satisfactory transformations from one ratio

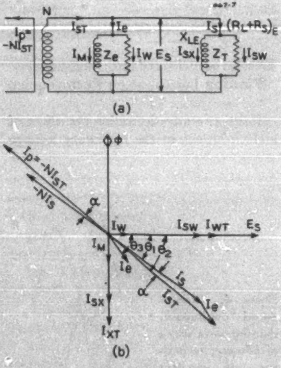


Figure 7

(*a*)—Equivalent circuit of current transformer on secondary base

(*b*)—Vector diagram of (*a*)

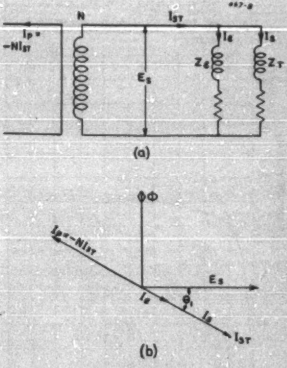


Figure 8

(*a*)—Simplified equivalent circuit of current transformer on secondary base

(*b*)—Vector diagram of (*a*)

to another may be made, the greatest source of variation coming from the use of commercial rather than precision testing facilities. Figure 6 is another form of expressing the data as calculated by equations 4 to 8 of appendix II. In this form they may be considered the master curves since they give the fundamental data which can be used to calculate the saturation and loss curves, and other calculations based on these data for any ratio using the same core. Since bushing current transformers have only the single primary turn, the ordinates for the magnetizing and loss ampere turns of figure 6 could be conveniently called primary amperes.

Calculation of Ratio and Phase-Angle Performance

The formulas for the calculation of transformer performance from the saturation and loss curves are given in appendix III. These calculations may be divided into two classes; the first giving both ratio and phase angle, and the second giving approximate ratio. In this connection it is interesting to note the theoretical effect of burden power factor on the phase-angle error of the transformer, as illustrated by figure 7. For lagging burdens the maximum possible phase angle error for any degree of saturation is less than 90 degrees and decreases with decreasing burden power factor, but in practice seldom exceeds 30 or 40 degrees. With unity or leading power factor burdens the phase-angle error may approach 90 degrees. Hence, for burdens having very lagging power factors the second method of calculation gives good results.

The agreement between the first method of calculation and test data is indicated in figures 9*a*, *b*, and *c* which show the comparison between calculated and actual test performance. As indicated, there is a difference between the results which occur for the following reasons:

1. Neglect of secondary leakage reactance which has been previously mentioned.

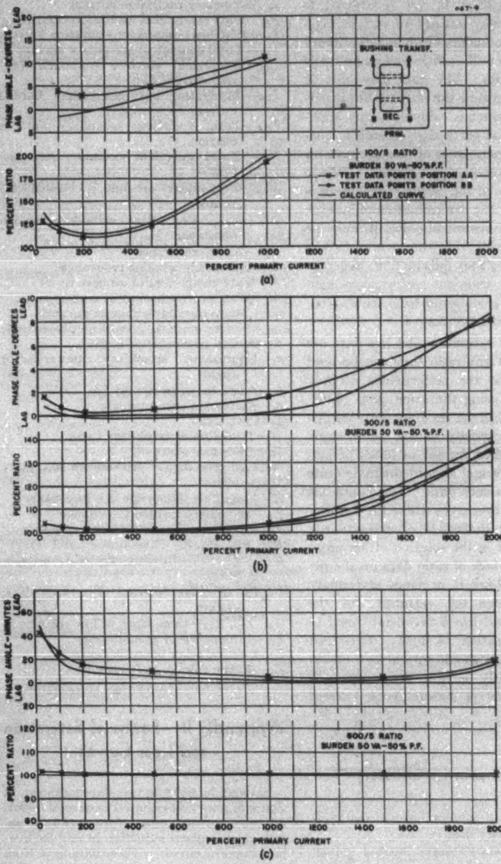


Figure 9 (left). Ratio and phase-angle calculated and test characteristic curves of current transformer

(*a*)—For 100-to-5 ratio

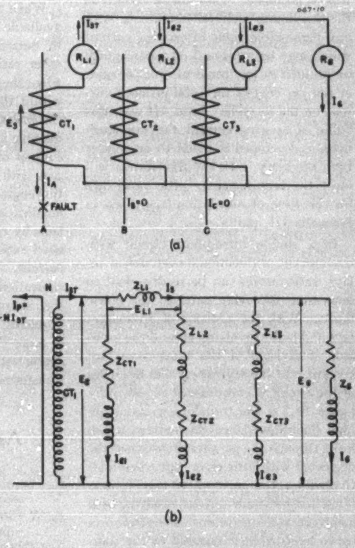
(*b*)—For 300-to-5 ratio

(*c*)—For 600-to-5 ratio

Figure 10

(*a*)—Typical three-phase relay circuit

(*b*)—Equivalent circuit for (*a*)



2. Neglect of harmonics, particularly with respect to their effect on the phase-angle error. The null test method used to obtain the phase angle compares the fundamental-frequency components of the secondary and primary currents, while the method of calculation has assumed all quantities are of fundamental frequency.

3. The order of accuracy of test data, including both saturation and loss data, as well as ratio and phase-angle data. Test errors could be reduced to a minimum by increasing the precision of the testing facilities. However, the work described herein was done with ordinary commercial testing equipment in order to observe the limitations which might be imposed on the use of saturation and loss data in practical applications.

Application to Relay Problems

In a large number of applications it is only necessary to determine if the degree of linearity of the transformer performance with a given burden over a specific range of primary current is within required limits. A glance at a saturation curve such as shown in figure 5*a* enables one to estimate quickly the limits over which approximately linear performance will be obtained. This can be defined as the maximum voltage obtainable with any permissible value of exciting current which may be expressed on a percentage basis, such as two per cent, five per cent, or ten per cent of the total primary current on the secondary base. If with this value of exciting current the secondary voltage developed is equal to or greater than necessary, then the transformer will meet the requirements. The procedure for this form of calculation is outlined in appendix III, part *c*.

In a simple three-phase circuit with overcurrent or directional overcurrent relays, ratio curves can be readily used to determine the relay settings for phase faults. With the addition of ground relays and faults involving ground the problem of division of the total secondary current into the several parallel branches of the circuit is encountered as shown by figure 10. If the transformer ratios are high (high impedance) the current which leaks through the parallel circuits may be neglected with little error, but when relatively low ratios are involved this problem often becomes quite serious, resulting with incorrect relay operation. The saturation curve lends itself admirably to the solutions of problems of this nature as outlined in appendix IV. It will be evident that additional refinements can be made on this method by taking into account the vector relationships and all of the circuit constants. Again, in cases involving the paralleling of transformers of two or more circuits, similar problems of leakage of the secondary current through the multiple circuits and the effective burdens are encountered.

Conclusions

From the foregoing it is evident that the open-circuit a-c saturation and loss data will be extremely useful in the field of relay application. It can be utilized to determine relay settings for various magnitudes of secondary burdens and primary currents, as well as ratio and phase-angle characteristics. The data may also be used to determine the range of linear performance, which is valuable in any application, and is particularly useful in comparing characteristics of transformers for differential protection schemes. Where a knowledge of the exciting impedance is required for the determination of leakage currents in branch circuits, it may be easily obtained from the saturation and loss curves. This information, like that required for transient performance, cannot be readily obtained from the conventional performance curves.

When the saturation and loss data are available on any ratio they can be used to determine the performance on any other ratio using the same core. Likewise, they can be used for any burden within the capacity of the transformer; the latter when defined in terms of permissible error and current range, is quite evident from such data. This is not the case with ratio curves where the application engineer, in the field, often is seriously handicapped in the solution of his problems by the lack of ratio data on the desired ratio, burden, or range of primary current. Since the versatility of the saturation and loss curves lends itself to the simplification of many diversified problems, they should appeal as a means of more clearly defining the overcurrent characteristics of bushing-type current transformers.

Appendix I—Glossary

E_{si}—Secondary induced internal voltage—secondary base
E_{si}/N—Secondary induced internal voltage—primary base
E_p—Primary terminal voltage
E_s—Secondary terminal voltage
E_s/N—Volts per turn
E_{sk}—Secondary voltage of transformer at a specified exciting current
I_sX_s—Secondary leakage reactance drop
I_pX_p—Primary leakage reactance drop
I_sR_s—Secondary winding resistance drop
I_pR_p—Primary winding resistance drop
I_s—Secondary burden current
I_{ek}—Permissible value of excitation current for a secondary voltage E_{sk}
NI_{ST}—Total secondary current—primary base
NI_S—Secondary burden current—primary base
I_{ep}—Core excitation current—primary base
I_{wp}—Core excitation watt component of current—primary base
I_{mp}—Core excitation wattless component of current—primary base
I_p—Primary current
I_e—Secondary excitation current
I_W—Watt component excitation current
I_M—Wattless component excitation current
I_{ST}—Total equivalent secondary current
I_{SX}—Equivalent wattless component burden current I_S
I_{SW}—Equivalent watt component burden current I_S
I_{XT}—Total equivalent secondary wattless current
I_{WT}—Total equivalent secondary watt current
X_{LE}—Equivalent secondary burden reactance
$(R_L+R_S)_E$—Equivalent secondary burden resistance
R_L—Burden resistance
R_S—Secondary winding resistance
F—Watt component of ampere turns
M—Wattless component of ampere turns
T_n—Times normal voltage or current
α—Primary-secondary current phase angle
θ—Burden power-factor angle
θ_1—Equivalent secondary power-factor angle
θ_2—Angle between E_s and I_{ST}
θ_3—Excitation current angle
ϕ—Core flux
N—Transformer secondary turns
W—Core loss watts
NI—Ampere turns—primary or secondary base
Z_T—Total secondary burden impedance
Z_S—Secondary burden impedance
Z_e—Open-circuit transformer impedance
R_{L1}, R_{L2}, R_{L3}, R_G—Line and ground relays
CT_1, CT_2, CT_3—Bushing current transformers
I_A, I_B, I_C—Line currents of a three-phase system
Z_{L1}, Z_{L2}, Z_{L3}—Impedance of line relays
Z_G—Impedance of ground relay
I_G—Ground-relay operating current
E_G—Relay voltage at I_G current

Appendix II—Forms of Saturation and Loss Curves

Saturation and loss test data as shown in figure 5, may be presented in several forms, all based on the ampere-turn excitation required for a given induction in any particular magnetic circuit. These data have been taken with open-circuit primary, reading the impressed voltage and watts loss at specified values of secondary circulated current. The induced voltage is taken equal to the impressed voltage, since the resistance drop of the winding is of small magnitude and becomes negligible when considered in its vector relation to the impressed voltage. The watt loss of the secondary winding is appreciable in comparison to the core loss and the observed data has been corrected to take this into account. Hence:

$$E_{st}=E_s \tag{1}$$

At a given voltage E_s for N turns, the exciting current is I_e, figure 5*a*, and the exciting volt-amperes are:

$$E_sI_e \tag{2}$$

At this current I_e the core loss is W watts, figure 5*b*. The angle between E_s and I_e as shown by figure 7*b* is θ_3 and is expressed:

$$\theta_3=\cos^{-1}\frac{W}{E_sI_e} \tag{3}$$

The exciting current is divided into its magnetizing and loss components I_M and I_W respectively:

$$I_M=I_e\sin\theta_3 \tag{4}$$

$$I_W=I_e\cos\theta_3 \tag{5}$$

The data may then be interpreted in values independent of the secondary winding.

$$\text{Induction}=\frac{E_s}{N}\text{ volts per turn} \tag{6}$$

$$\text{Excitation}=NI\text{ ampere turns} \tag{7}$$

The excitation in ampere turns is the same on either primary or secondary turn basis, and since the bushing transformer has only one primary turn it follows that:

$$I_{mp}=NI_M=M$$

and

$$I_{wp}=NI_W=F \tag{8}$$

By the use of equations 4 to 8 a set of data for a given ratio and core as represented by figure 5*a* and *b* may be converted into the general form as given by the curves in figure 6.

Another convenient form of interpreting these test data as obtained on one ratio N_1 to another ratio N_2 is shown by the curves for the 100 to 5 and 300 to 5 ratios in figures 5*a* and *b;* and is derived on the following basis:

For a given voltage E_{s1} and loss W_1 on turn basis N_1 with exciting I_{e1}:

$$N_1I_{e1}=N_2I_{e2} \tag{9}$$

Therefore:

$$I_{e2}=\frac{N_1}{N_2}I_{e1} \tag{10}$$

Likewise the induced voltage per turn:

$$\frac{E_{s1}}{N_1}=\frac{E_{s2}}{N_2} \tag{11}$$

Therefore:

$$E_{s2}=\frac{N_2}{N_1}E_{s1} \tag{12}$$

and

$$W_2=W_1 \tag{13}$$

By the use of equations 10, 12, and 13 the curves for the 100-to-5 and 300-to-5 ratios as shown by the indicated points in figures 5*a* and *b* are calculated and the agreement with test values is well illustrated.

Appendix III—Calculation of Performance Characteristics

(a) Ratio and Phase Angle

The calculations are based on the equivalent circuit and diagram figure 7, which refers all quantities to the secondary side and includes consideration of the vector relationships. Assume a burden Z_B at a power-factor angle θ, current I_S, and ratio N. The total burden Z_T includes the secondary winding resistance R_S. Hence:

$$Z_T=(R_B+R_S)+jX_B \tag{14}$$

$$\theta_1=\cos^{-1}\frac{R_B+R_S}{Z_T} \tag{15}$$

Also the secondary voltage required:

$$E_s=I_SZ_T \tag{16}$$

For this voltage the excitation current I_e is required and obtained from the saturation and loss curves. These currents are then separated into their watt and wattless components, and added vectorially as indicated in the equivalent circuit and vector diagram of figure 7, with the following equations:

$$I_{SW}=I_S\cos\theta_1 \tag{17}$$

$$I_{SX}=I_S\sin\theta_1 \tag{18}$$

The values for I_W and I_M are obtained from the curves of figure 6 for the induction E_s/N.

Therefore:

$$I_W=\frac{F\text{ (from figure 6)}}{N} \tag{19}$$

$$=I_e\cos\theta_3\text{ (from figure 5}a\text{)}$$

and

$$I_M=\frac{M\text{ (from figure 6)}}{N} \tag{20}$$

$$=I_e\sin\theta_3\text{ (from figure 5}a\text{)}$$

$$I_{WT}=I_{SW}+I_W \tag{21}$$

$$I_{XT}=I_{SX}+I_M \tag{22}$$

$$I_{ST}=\sqrt{I_{WT}^2+I_{XT}^2} \tag{23}$$

$$I_p=NI_{ST} \tag{24}$$

$$\text{Per cent ratio}=\frac{I_p(100)}{I_SN} \tag{25}$$

Substituting (24) in (25) and collecting terms:

$$\text{Per cent ratio}=\frac{I_{ST}(100)}{I_S} \tag{26}$$

$$\text{Per cent primary current}=\frac{I_p(100)}{\text{Rated primary current}} \tag{27}$$

Phase angle is:

$$\alpha=\theta_2-\theta_1 \tag{28}$$

$$\text{where }\theta_2=\tan^{-1}\frac{I_{XT}}{I_{WT}} \tag{29}$$

and θ_1 is given by equation 15.

(b) Simplified Method

The calculations are based on the equivalent circuit and diagram, figure 8, and assume all quantities to be scalar values in phase with the secondary current. These quantities are referred to the secondary side. The justification for this method is based on the fact that relaying burdens usually have low power-factor angles approaching the excitation angle. Thus the calculated results will indicate poorer ratio performance than obtained in service, and will disregard the phase-angle performance. Under these conditions:

$$E_s=I_SZ_T \tag{30}$$

where Z_T is obtained from equation 14.

The total secondary current is:

$$I_{ST}=I_S+I_e \tag{31}$$

where I_e is a value of exciting current obtained from a saturation curve similar to figure 5 for a given ratio N at voltage E_s. The primary current, per cent ratio, and per cent primary current are than obtained by equations 24, 26, and 27 respectively.

(c) Linear Performance Range

The determination of the linear performance range of a current transformer may be ascertained from a saturation curve by assuming an allowable percentage P of maximum primary current I_p for the core excitation current I_{ek}. The exciting current on the secondary base becomes

$$I_{ek}=\frac{I_p}{N}\times\frac{P}{100} \tag{32}$$

The value of voltage on the saturation curve corresponding to the current I_e for a given ratio N, yields a developed voltage E_{sk}. Comparing the burden voltage E_s at rated secondary current with the value E_{sk} indicates the number of times normal T_n, that the transformer will function linearly, or

$$T_n=\frac{E_{sk}}{E_s} \tag{33}$$

With I_p taken as the maximum primary current and $5N$ as the normal primary-current rating, then if, $T_n>I_p/5N$, the transformer provides linear performance over the considered range. If $T_n<I_p/5N$, this indicates that the ratio error is greater than the permissible assumed value P per cent and requires recalculation on basis of lower primary current.

Appendix IV—Application of Saturation Curve to Typical Relay Problem

A typical problem involves the relay protection of a three-phase circuit with grounded neutral using phase and ground-current relays as shown in figure 10. Assume line-to-ground fault current in phase *A*, and no current in phases *B* and *C*, then the equivalent circuit for the three-phase circuit is as shown in figure 10*b*. Current transformers CT_2 and CT_3 in series with their respective line relays form leakage-current paths in parallel with the ground relay which may appreciably reduce the current I_G in the relay.

Assume the ground relay tap setting requires an operating current I_G, and following the practice outlined in appendix III, part *b*, wherein all quantities were considered scalar and in phase, the following procedure may be used to determine the approximate value of primary current required to operate the relay.

$$E_G = I_G Z_G \tag{34}$$

Since Z_{L2} and Z_{L3} are usually small compared to the transformer impedance, they may be neglected thus avoiding the complication of using a cut-and-try method to determine the total value of $(Z_{L3}+Z_{CT3})$. The result of neglecting the impedance of these relays will be in favor of the ground relay.

$$I_{e2} = I_{e3} \tag{35}$$

These values may be obtained from a saturation curve such as shown in figure 5*a* for the voltage E_G on a ratio N.

$$I_S = I_G + 2I_{e2} \tag{36}$$

$$E_{L1} = I_S Z_{L1} \tag{37}$$

$$E_s = E_{L1} + E_G \tag{38}$$

It then follows that the required excitation current I_{e1} for CT_1 may be obtained as previously noted for voltage E_s.

Then

$$I_{ST} = I_S + I_{e1} \tag{39}$$

and

$$I_p = NI_{ST} \tag{40}$$

This last equation gives the approximate primary current required to obtain the ground relay current I_G.

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Power Supply for Railroad Electrification and Fundamentals of Power Contracts

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THE PURPOSE of this paper is to indicate and clarify the status of the American railroad as a major factor in the generation and use of power, the extent to which steam-railroad electrification has become an element therein, the effect of electric-power costs on the extension of railroad electrification, and the desirable features of power contracts in connection with such electrification.

Railroads as Producers and Users of Power

Before the advent of the electric street and interurban railway and the use of electricity for domestic and industrial lighting and power purposes, the railroads of the country were the large producers and users of power, of course developed in many relatively small steam-locomotive units. That power business has grown until in 1937 the aggregate indicated horsepower of locomotives in service on the 136 class I railroads (roads with operating revenues of more than $1,000,000 a year) amounted in round figures to 100,000,000 horsepower. This compares with an aggregate installed capacity of all utility plants—hydro and steam—of approximately 50,000,000 horsepower.

Extent of Railroad Electrification

There are approximately 250,000 miles of railroads in the United States of which 235,000 miles are in the class I group. Of this total only 1.3 per cent have been electrified (6 per cent in the rest of the world). This, however, represents a considerable mileage, namely 2,965 route miles (6,950 miles of track) distributed over 24 railroads. The main reason for this relatively slow progress is the high capital cost of electrification because of which operating savings do not result in a sufficiently quick payout to virtually compel electrification.

Advantages of Electrification

Electrification permits operating accomplishments that are not possible with steam power; it increases the capacity of a given railroad thus frequently saving large expenditure for additional trackage or terminal facilities; it adds to the attractiveness of the service and the comfort of the public; and through operating savings usually more than pays fixed charges. Thus it is conspicuous in being a type of railroad betterment that provides many desirable facilities and at the same time "pays its way." Among the outstanding operating benefits are great reliability of operation, high degree of availability and serviceability of the traction units, and the almost unlimited amount of power that may be made available for the movement of traffic. Underground terminals and connecting tunnels would be impracticable without electrification and capacity limitations of long mountain tunnels are overcome thereby.

Today probably the greatest demand on transportation is for service and the above advantages, together with the high operating speeds attainable, make electrification a valuable factor in enabling railroads to meet this requirement.

Operating Savings Due to Electrification

The major elements of operating cost, wherein savings by electric power as compared with steam are effected, include:

Locomotive repairs. These are considerably lower under electric operation and constitute one of the largest items of saving. Enginehouse expenses, inspection, etc., may be included in this general group and in these substantial savings are effected.

Transportation costs enter into the picture—these savings are substantial when the improved operation with electric power can be fully realized and the service can be rendered with a smaller number of larger train units.

Miscellaneous and indirect savings usually aggregate a substantial sum.

Cost of electric power versus locomotive fuel is an important item. Under some conditions this cost makes the project unattractive and becomes a controlling consideration from the standpoint of operating costs and savings.

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Advances in the Art

The major advances in the art of power production as affecting efficiency and cost have been in the conversion of the heat energy in fuel into mechanical energy, where the efficiency has always been relatively low. The efficiency of conversion of mechanical into electrical energy is already high and little improvement has occurred or can be expected here.

During the period of great improvement in boilers, furnaces, and prime movers due to more efficient combustion methods, higher pressures, and superheat and other advances in the design of steam-electric generating plants parallel progress has been made in the efficiency of the steam locomotive. Thus in the past 15 or 20 years, there has been a reduction of about 25 per cent in the coal consumption on steam railroads per transportation unit. In the same period, as shown by the Federal Power Commission's statistics, there has been a reduction of nearly 50 per cent in coal burned per kilowatt-hour in steam-electric power plants of the utility companies. These figures indicate that in so far as efficiency of conversion of fuel into mechanical energy is concerned, the stationary plant still has the advantage.

Public-Utility Supply Versus Railroad Generating Plants

In the early days of railroad electrification, including that of terminals and certain city transit lines, it was found economical or necessary for the railroads to build their own power plants. The characteristics of the supply—frequency, phase, amount and fluctuating character of loads, etc.—and the rates quoted by utilities for such supply indicated in many cases that substantial savings would result from the railroad building its own plant. An important part of this advantage is that the railroad plant is built to produce the kind of electric power required without conversion from 60 cycles and it can be conveniently located with respect to the railroad's load center. Latterly, some of these plants have been leased or otherwise taken over by the utilities and purchased-power contracts substituted for railroad generation. In connection with most of the larger and more important railroad electrifications, power in whole or in part is now purchased from the utility companies.

Characteristics of Railroad Load

All railroads, both in the United States and abroad, use for traction purposes current of other characteristics than 60 cycles three phase that now is the general standard in this country for commercial power systems. This means that converting equipment of some kind is necessary to produce either direct current or 25-cycle single-phase current required for railroad operation. Development of the static converter type of equipment for frequency changing may result in increased efficiency of conversion and ultimately in lower cost of such equipment.

On the shorter railroad electrifications, such as for terminal operation or for mountain-grade lines, because of heavy train units and other operating conditions the demand on the supply system fluctuates widely. In some cases also the peak loads frequently occur at irregular and unpredictable times. These, together with the characteristics of the current, are adverse conditions which tend to make the supply by the utility of such requirements somewhat difficult and relatively costly.

On the other hand, where the railroad electrification is extensive and the traffic dense and comprised of a large number of train units, the fluctuations are not unduly severe and the load factor is relatively high; also the heavy demands frequently occur at off-peak times with reference to the utilities' commercial load. These features are favorable and should tend to reduce the cost of supplying energy.

Supply From Several Utilities

On extensive electrifications where the electrified lines cover hundreds of miles of route and cut across territory normally served by a number of different utilities, it is usually necessary for the railroad either to choose between such utilities and provide its own transmission lines in addition to substations and trolley system or to enter into agreements with two or more of the utilities. In the latter case, the railroad's entire requirements must be covered and the rates should be so applied that the railroad pays on the basis of its system load factor; otherwise it will be penalized in regard to load factor due to dissimilar conditions in the different areas traversed.

Table I

Service Operated	Miles of Route Operated	Miles of Track Operated	Average Monthly Demand*	Load Factor (Per Cent)
Suburban only	130	400	37,000	33–34
Through passenger service added	180	600	60,000	46
Same, with through passenger service extended	350	1,200	76,000	48
Same, with through freight added	380	1,370	120,000	55
Same, with further extension of through passenger and freight service	675	2,190	141,500	62

* Based on three maximum clock hours per month.

In this connection it is interesting to note the effect the extension of electrification has had on the load factor which in turn affects the over-all cost of energy as illustrated by table I.

This table shows, as is well understood, that the load factor of the supply to purely suburban electrified lines is low because of the heavy morning and evening travel and relatively light load at other times of the day.

When this suburban service is supplemented by through passenger traffic or by both passenger and freight, there is a substantial improvement in load factor. When the through passenger and freight traffic becomes so extensive and heavy as to form the major portion of the load an excellent load factor results. Thus in the table the average monthly load factor gradually increased from 33 per cent for suburban traffic only to 62 per cent after the entire program of suburban and through electrification was carried out. If there had been no suburban electrification the average load factor due to through passenger and freight service would have been of the order of 75.

If the electrification had been divided into say three main sections the load factor for each section would have been lower and the sum of the peak loads or demands on which separate billings would be based would be of the order of eight to nine per cent higher than the actual system peak loads. This in the case above cited would have represented an additional power cost to the railroad of more than $200,000 a year.

The supply of power by several utilities at the railroad's system load factor can be made effective in either of two ways:

1. By the railroad installing transmission lines of sufficient capacity to interchange power and energy between the different areas, thus enabling each utility actually to deliver its share or quota of the total demand and energy requirements, through the railroad transmission lines, or,

2. By the utilities jointly contracting with the railroad and with each other to supply

the portion of the demand and energy actually drawn by the railroad in each area and combining or pooling the demand and energy and jointly billing the railroad.

Such utilities should be interconnected and operated in parallel on the 60-cycle side so as to maintain synchronism effectively and thus avoid momentary interruptions in the supply when trains pass from one area to another. Most of the utilities are already normally interconnected for their own operating and economic purposes and their lines often parallel important trunk lines of railroads in territory where the traffic is sufficiently heavy to warrant electrification. In some localities portions of the railroad right of way are utilized for transmission lines of the utility. Joint use of railroad electrification structures is also feasible in some cases.

It is desirable from the standpoint of cost of special equipment that the number of supply points should not be unduly multiplied. Broadly speaking, the power-supply system—railroad and utility—should be so planned as to involve the minimum total capital expenditure by the parties consistent with the requirements of the service.

In considering electric traction railroads are confronted with the high capital cost and are usually desirous of shifting to the utilities as much as possible of the cost of power-supplying equipment. Locomotives are at present usually purchased through equipment trusts, the money being borrowed at favorable interest rates largely independent of the credit status of the railroad. If a similar plan could be devised for financing the roadway power equipment at favorable rates, greater progress in railroad electrification might result.

Cost of Electric Energy

It is generally believed that a public utility engaged in generating and supplying electric power to all sorts of customers should be able to produce power at lower cost than can be done by a railroad in its own power plant or plants; also that utilities are willing to supply the railroads on the basis of actual cost, including a reasonable return on investment and, therefore, there should no longer be any question as to whether a railroad about to electrify should purchase its power or generate it. There are a number of considerations, however, that enter into this question and which have an important bearing upon the railroad's decision to buy power from the utilities on such cost basis. Amongst these are the rates covering fixed charges on capital investment which the utilities may find necessary to use in arriving at cost and which may for a number of reasons be relatively high.

Such factors coupled with the railroad's ability to generate in its own plant, conveniently located, the form of energy required to operate its trains, may in certain cases more than offset the lower cost to the utility of generating its standard form of electrical energy and then converting it for railroad use.

The trend in the average cost per kilowatt-hour for purchased energy has been downward for the past ten years or more in respect to residential, commercial, and interurban and street-railway business. There was no definite downward trend in the cost of energy purchased for steam-railroad electrification until 1935 and since then the reduction appears to have been mainly due to improved load factor or some other special conditions rather than the lowering of rates.

Power Contracts

Experience has shown that power contracts as between railroads and electric utility companies in order to be generally satisfactory to the railroads should provide for the following railroad requirements:

(*a*). The contract should be for a long term—say 20 years with renewal options—so as to amortize on a low rate basis the cost of special equipment. A longer term than 20 years would be permissible if the rates for demand and energy are on a "cost" basis thus assuring to the railroad the benefits of advances in the art.

(*b*). The utility should preferably, although not necessarily, provide and own the special equipment and supply the power at the phase, frequency, and possibly the voltage required for railroad operation. This would minimize the investment by the railroad but fixed charges on same should be on a low basis comparable with rates applicable to the railroad's own capital expenditures.

(*c*). It is preferable to the railroad for the utility or group of utilities to supply power at a sufficient number of points in the electrified zone to take advantage of the utilities' networks to reduce investment costs by the railroad and at the same time further assure reliability of the supply.

(*d*). While the railroads would like to purchase power on a flat or sliding rate per kilowatt-hour, much as they purchase fuel, it has so far been impracticable to work out such a basis and separate demand and energy rates are customarily used.

(*e*). Demand charges should be on a monthly basis and should be established by averaging three or more clock-hour peak loads in the month. In selecting such peak hours, those caused by abnormal and emergency conditions should be excluded. System peaks rather than sectional peaks should be used in determining the maximum demand. When railroad peaks regularly occur at utility off peak times such as between 9:00 p.m. and 7:00 a.m., this should be reflected in a fair way in the demand rate.

(*f*). The energy charge should be based on the summation of meter readings at all points and is properly subject to correction for fluctuations in cost of fuel. The demand and energy charges may be on agreed rates or they may be based on the actual cost to the utilities within upset or over-all limits, the elements of such cost being defined, the rate used in billings in any yearly period being based on the actual cost for the preceding year.

(*g*). Where a substantial portion of energy is generated in hydroelectric plants of either the contracting utility or its affiliates and is furnished on a cost basis a suitable correction factor should be applied because of the high first cost and low operating cost of such plants.

(*h*). Power contracts should be as simple as possible as to form and should eliminate all unnecessary conditions and stipulations that might lead to misunderstandings and disputes. No guarantee as to load factor should be required of the railroad, nor should guarantees as to the minimum demand charge following a previously established maximum be required. While minimum loads of two-thirds or three-fourths of the previously established demands have been stipulated in some railroad contracts, it is preferable from the railroad's viewpoint to eliminate such clauses. Wide variations are inherent in the nature of the traffic on trunk-line railroads and are quite beyond the control of the management. They should be understood and recognized by the utility but should not appear in the contract.

In conclusion, the writer would express the hope that the downward trend in the cost of generating and distributing electric power will presently be reflected in the rates offered for railroad electrification and will be on a scale which will make the cost of power one of the major items of saving by electrification. It is also the hope that at the same time ways may be found materially to reduce the capital cost of electrification. If these things can be accomplished, important pending projects may be undertaken with every assurance of attractive financial as well as operating results. In that case, power companies operating in the vicinity of newly electrified railroad lines would be assured of large additional blocks of load, and the best interest of two important groups of public servants would be mutually advanced.

Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Restoration of Service on a Metropolitan Power System

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THE purpose of this paper is to outline procedures and discuss pertinent problems that arise when restoring service to a metropolitan system following a major shutdown. In the discussion, consideration is given to the fundamental plan of the system, pointing out the inherent features in the design which prevent such failures. Several cases of trouble which have been the cause of service curtailment have been discussed and the service restoration procedure outlined. A brief outline of the essential procedure is given for two types of service restoration.

The conclusions and procedures discussed in this paper are the result of experience gained from failures which have occurred on the Duquesne Light Company system. It is recognized that the resultant procedures and conclusions may not be applicable to other systems. However, this paper does discuss many conditions which are common to all systems and therefore this paper may be helpful in formulating a service restoration procedure.

Description of Facilities for Supplying Load Areas

The power sources of the Duquesne Light Company system are comprised of three steam generating stations and two medium-capacity interconnections. The Colfax power station, which is located 18 miles east of Pittsburgh on the Allegheny River, delivers its 262,500-kw capacity to the 66-kv transmission system. The Brunot Island and Reed power stations which are located 4 miles north of Pittsburgh on the Ohio River, have a total capacity of 236,500 kw; use a common switch house; and, in general, supply the 11-kv and 22-kv load for the downtown Pittsburgh area and the industrial section adjacent thereto. All the power stations are directly connected to the 66-kv transmission system which acts as a high-capacity tie between the generating stations.

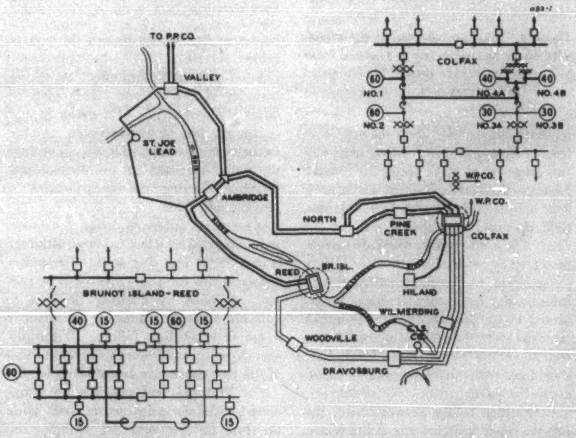

Figure 1. Schematic diagram of the Duquesne Light Company system

Inserts show schematic bus diagram of power stations

The 66-kv transmission system and the power stations normally have a running order that provides two segregated power systems which are referred to as the north and south rings. The 66-kv transmission system is essentially a ring surrounding the major part of the Pittsburgh territory having a secondary ring extending north and west along the Ohio River. This scheme provides a duplicate source to each of the substations located on the transmission ring. The sectionalizing of the system into a north ring and a south ring is accomplished by reactors between respective bus sections at the power stations (figure 1).

There are eight step-down substations on the transmission ring which feed definite load areas. In general, these stations feed isolated load blocks but are provided with secondary ties which are used for emergency power transfers. These substations are the source of the 22-kv feeds to the distribution stations supplying 4-kv service in the congested areas. They also supply the street railways and the high-voltage industrial service. In general, each one of these substations comprises a complete unit of the distribution system (figure 2).

The downtown Pittsburgh area and the industrial area adjacent to it are supplied from the Brunot Island switch house and are not directly dependent upon the 66-kv transmission ring. This area is supplied by a 4-kv radial system and a low-voltage network. The network system has four

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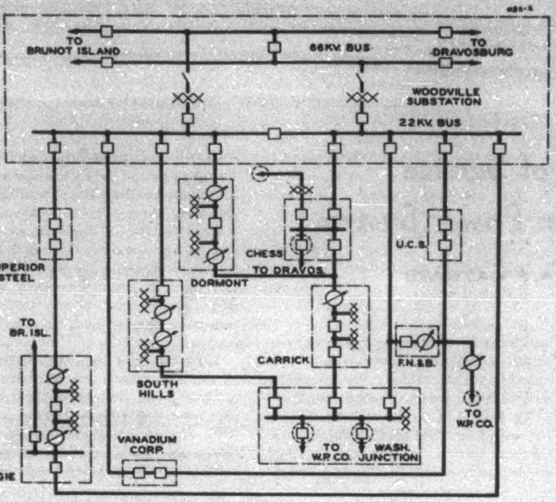


Figure 2. Schematic diagram of the Woodville 66-kv ring substation and related transmission circuits

separate areas, two areas having 4-kv feeders from downtown substations, and two areas fed at 11-kv from the Brunot Island switch house. Each 11-kv network area has its power source from bus sections operating on opposite sides of the ring. The feeds to these areas are further protected by connecting individual feeders to separate bus sections. The 11-kv cables which feed other services are connected to the Brunot Island bus in the same manner. The failure of a single bus section in the switch house, therefore, should not result in interruption of customers' service (figure 3).

In addition to the network load, the Brunot Island switch house is the source for various street-railway stations, and numerous high-voltage customers. The combined load in this 16-square-mile area reaches a maximum of 100,000 kw, 25 per cent of the system load. Due to the high load density in this area and the relatively short transmission distance, this service is supplied at the generator voltage.

The fundamental plan of the bulk power system involves its segregation into two main sections, commonly referred to as the north ring and the south ring. This provides ties between generating sources over two distinct and separate paths; and, due to the method of connecting the halves at the power sources through reactors, permits a fault to be immediately isolated with minimum effect on the remainder of the system. This plan reduces the probability of a major system shutdown due to transmission trouble. In the event of a ring split, the reactors in the power stations can be shunted out, thereby relieving the transformers of possible overloads.

A complete failure of a ring substation should only affect a limited area, although it may result in a ring split. The power requirements for these areas can be partially supplied by utilizing tie lines to adjacent areas, thus limiting the effect of a major substation failure. Transmission capacity is provided to cover the loss of one ring substation transformer bank.

The Brunot Island switch house is operated with two main bus sections, using the bus tie breakers to provide the emergency back up. Further protection is provided at the Brunot Island switch house by the isolated-phase construction.

Power-Station Auxiliaries

The layout of the auxiliary system follows, in general, the same plan as the transmission system. Two sources are supplied the duplicate equipments on each of the major generating or boiler units. One of these feeds usually has its source direct from the unit itself, while the remaining equipment has its source from other units or transformer banks connected to the opposite side of the transmission ring. Auxiliary power sources are provided by separate steam-driven station-service generators, station-service generators driven from the shafts of main units, or station-service transformer banks. This provides combinations consisting of either a house generator and a station-service transformer or station-service transformers supplied from opposite sides of the transmission ring. The separate voltage sources minimize the loss of auxiliaries due to disturbances on the transmission system. Station-service generators are available at the Brunot Island and Colfax power stations to assist in the initial start-up of these stations. The Brunot Island station-service generator, while not located in the Reed power station, is available for use at that location.

The major part of the station-service power supply is equipped with voltage-transfer devices which transfer the unit auxiliaries to a normal-voltage source in event of failure of the regular power supply. The voltage transfer eliminates the necessity of other types of reclosing. Simultaneous reclosing devices, provided to energize the auxiliary busses after a complete outage, do not appear advisable in many cases due to the characteristics of the auxiliary drives.

In addition to the general plan of sectionalizing all the main power sources into two sections, much effort has been expended in improving the bus construction and clearances, installing faster relays and circuit breakers, co-ordinating insulation, providing gas barriers and fire protection, and improving test and maintenance procedure, all with the intent to decrease the probability of failures.

With the ring substations responsible for the service of eight major load areas, and the Brunot Island switch house covering the requirements for downtown Pittsburgh and adjacent areas, the 66-kv breakers at the ring substations, or the 66-kv breakers at the power stations, have adequate capacity to restore these load areas.

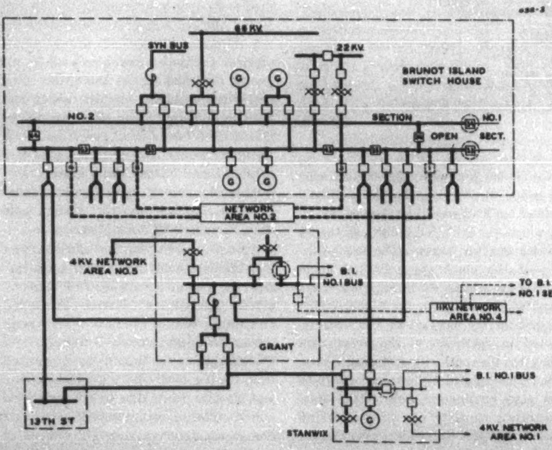
The low-voltage network presents a somewhat different problem and a master control switch has been provided at Brunot Island to reclose simultaneously any combination of breakers feeding each of the network areas. This simultaneous reclosing of each network area prevents de-energizing sections of the Brunot Island switch house for the purpose of reclosing the network through a bus-tie breaker. The simultaneous-reclosing scheme was easily applied due to the use of spring-operated breakers which eliminated consideration of control difficulties involved during simultaneous reclosure of solenoid-operated breaker mechanisms.

Major Operating Experiences

In discussing the operating experiences of the Duquesne Light Company which have involved an outage to a major section of the system, a brief outline of the type of loads served is necessary in order to visualize the response of the customers to service restoration.

The residential and commercial load, which is mainly lighting, is approximately 25 per cent of the total system load. The growth of refrigeration and air conditioning appear to be factors which will influence restoration of this service in certain areas. The industrial service, which comprises about 53% of the system load, includes much rotating equipment provided with low-voltage interlocks, the use of which prevents instantaneous pickup upon restoration of service. The remaining load is mainly street-railway service, which is supplied by synchronous converters or motor generator sets. This equipment is sensitive to system disturbances and generally requires manual restoration, except for a few unattended automatic stations. It is not unusual for a noticeable decrease in system load to occur following a severe voltage disturbance, due to the loss of synchronous equipment. It is believed that this condition is more favorable to rapid restoration of service than residential and commercial load, which approaches a resistance characteristic.

Figure 3. Schematic diagram of typical Brunot Island section and related transmission circuits



The major hazards which would involve a serious outage on the system fall into three classes:

1. *Electric Failures at Vulnerable Points on the System.* These involve failures either of bus sections in the power stations, transformer failures, or ring substation failures. These failures are the most frequent type of hazard, but, except for one or two cases which occurred many years ago, they have not caused other than a momentary disturbance to the system. The loss of one main generating unit is not considered a serious hazard as adequate spare capacity is normally available at all times.

2. *Loss of Transmission Facilities Due to Storms, Hurricane, or Sleet.* This condition would severely interfere with service but fortunately very little trouble has been experienced in the Pittsburgh district due to these causes. The geographical location precludes any great probability of a hurricane or tornado impairing the transmission system. The hazard from sleet is relatively great in this area. Severe sleet storms have not involved any important transmission circuits since 1925, although they have occurred frequently immediately adjacent to the Duquesne Light territory. Electrical storms are severe in this locality and have caused most of the major transmission failures.

3. *The Flood Hazard.* The flood hazard has been recognized and the system facilities have been installed to provide continuous service beyond what appeared to be a record flood. However, in March 1936 the most severe flood in the history of Pittsburgh occurred and resulted in an almost complete shutdown of the system; all but 2,500 kw of the entire system load was dropped. In the Pittsburgh district a flood does not present any serious hazard to the main transmission system, although it does affect many parts of the system serving industrial customers and domestic customers. However, due to flood conditions, these customers could not utilize the service if available. The generating stations, being in the river valleys, are subjected to the full brunt of the flood wave.

The most severe outage the system has experienced, except in the flood, occurred on Sunday evening, September 25, 1927, at 7:17 p.m. as a result of instability. Extending over a period of 31 minutes following the start of this disturbance, severe hunting existed between power stations and between generating units within the power stations. In order to restore stability to the system it was necessary to sectionalize the system, each area being fed by a separate generating unit with the load so proportioned as to come within the unit's capacity. The switching incidental to this split up was performed during the instability and resulted in immediate recovery following its completion. This disturbance resulted from a combination of factors which involved operation of the system below the normal voltage level while transformer taps were being changed to raise the voltage level of the 66-kv transmission system. This resulted in lower generator voltages which, combined with an error in the load forecast as a result of the changeover from daylight saving time, resulted in the instability. It is not expected that this condition will occur again from these causes due to the rigid maintenance of generator voltages and the operation of adequate reserve capacity.

There have been within recent years two major disturbances resulting from transmission-line failures which have upset the normal distribution of load in the transmission ring. On both occasions the 66-kv transformers at Brunot Island were tripped by the overload relays, isolating more load on the station than could be carried.

One major outage due to a transmission failure occurred on July 3, 1929. The primary cause of this disturbance was the failure of a ground wire on a four-circuit river crossing between the Wilmerding and Dravosburg substations on the south ring, splitting the south ring. This operation interrupted the normal power flow through this section which supplied part of the south-ring substations and the south-ring feed to the Brunot Island 11-kv bus. The interruption caused the power flow to seek a path across the reactor bus at Colfax along the north ring, through the 66-kv transformers at Brunot

Island across the 12-kv reactor bus and out through other 66-kv banks to the remaining substations on the south ring.

This redistribution of load caused severe overloads on the transformers connected to the north ring at Colfax and Brunot Island. The overload relays on the Brunot Island transformers connected to the north 66-kv ring tripped this bank, leaving 130,000 kw of load isolated on the Brunot Island station with 100,000 kw of capacity operating at 50,000-kw load. Due to the inability of the station to carry the entire load the frequency dropped to 56 cycles, reducing the load to approximately 110,000 kw. In a short time the steam pressure dropped from 190 pounds to 145 pounds and the voltage dropped from 11,600 volts to 10,200 volts. As a result of the low steam pressure the Brunot Island output decreased to 92,000 kw, indicating that it was necessary to quickly drop part of the connected load at this station in order to prevent the steam pressure from reaching a level where the station would have to be completely shut down. This is particularly important at this station, due to steam-driven auxiliaries for the boiler room and turbine room. The 66-kv transformers to the south ring and the transformers feeding the 22-kv transmission system were opened, relieving the station of all but the local 11-kv and low-voltage network load. The frequency and voltage recovered to normal and the station load settled at 75,000 kw.

With the frequency back to normal the station was immediately paralleled with Colfax through the north 66-kv ring and the 22-kv and 66-kv transformers reclosed. There were no difficulties experienced in picking up the load which was a result of service restoration. The nearness of this disturbance to noon hour resulted in a smaller total load than existed previous to the disturbance. The possibility of a simultaneous failure of any four-circuit transmission line is considered to be so remote that no precautions are taken to provide for this contingency.

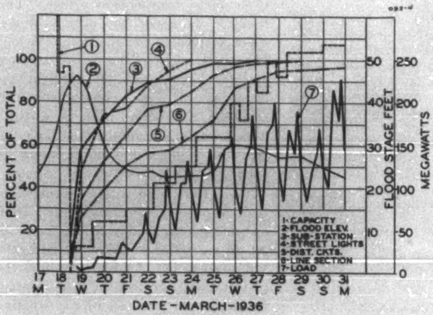


Figure 4. System condition following the 1936 flood

Another major disturbance due to a transmission failure occurred on August 13, 1935, at 9:11 a.m. This occurred at approximately the same relative location on the north ring as the south-ring disturbance just described and involved a double-circuit tower line. The conditions preceding this disturbance were abnormal, in that a 60,000-kw unit at the Reed power station was out of service for maintenance. Enough capacity was operating in the Brunot Island power station to protect against the loss of one of the 66-kv transformer banks, thereby providing backup for the Brunot Island local load. However, lightning struck this two-circuit transmission line and the resulting relay operations split the north 66-kv ring, routing the feed to the Valley district through the Brunot Island 12-kv bus and 66-kv transformers on the north ring.

Immediately preceding this disturbance the Brunot Island station output was 20,000 kw with 55,000 kw boiler capacity in service. The local Brunot Island bus load was 64,000 kw, 44,000 kw of which was being fed in through the 66-kv transformer banks. The sectionalizing of the north ring diverted about 40,000 kw additional through the south ring, which, added to the 44,000 kw required for the local 12-kv bus load, resulted in operation of the overcurrent relays on the south-ring 66-kv transformers. This isolated 104,000 kw of load on the same bus section with 55,000 kw of generating capacity operating at 20,000 kw. Under this tremendous overload the steam pressure dropped from 190 pounds to 100 pounds within one minute, and to 60 pounds within another half minute. A maximum load of 64,000 kw was observed on the Brunot Island station immediately after the stations had separated.

Immediately following the tripping of the south-ring transformer breaker at Brunot Island, the Brunot Island-Ambridge 66-kv lines were opened, relieving the generators at this plant of 40,000-kw load, in an attempt to restore the frequency in order to synchronize with Colfax. This load was immediately reclosed on the Colfax power station. After this relief, the Brunot Island generator load continued to decrease to 35,000 kw and then slowly decreased to about 22,000 kw, due to loss of steam pressure and failure of steam-driven auxiliaries. The frequency at this time was below 50 cycles, that being the lowest frequency scale observed and the bus voltage decreased from 11,600 to 7,500 volts. At this time, the Brunot Island 11-kv bus was de-energized and all generating equipment disconnected from the bus.

De-energizing the Brunot Island 11-kv bus resulted in a complete outage to the 23,000-kva low-voltage network load and all of the 11-kv and 22-kv transmission in the area adjacent to downtown Pittsburgh. Various 11-kv loads were sectionalized at Brunot Island during the disturbance and service was restored to the network and the remaining loads by energizing the 11-kv bus from Colfax through the north 66-kv ring transformers at Brunot Island. Preceding the restoration of network service, the Colfax power station load was 137,500 kw. Immediately upon re-energizing the Brunot Island station, which involved only the low-voltage network and a few 11-kv services, the Colfax load increased 56,500 kw to 194,000 kw. In the intervening period the remaining circuits, which had been sectionalized in an effort to relieve the Brunot Island load, were reclosed. By 9:42 a.m. the Colfax station load had increased to 252,000 kw. During this interval the generators at Brunot Island were synchronized on the system and brought up to 29,000-kw load.

At one interval shortly after service had been restored the station operators reported a total system load of approximately 282,000 kw between the usual 15-minute reading interval. This peak existed for approximately 5 minutes and was thought at the time to have resulted from the loss of diversity in refrigeration and elevator loads due to the extended period of the outage and some excess load due to simultaneous re-establishment of service by the high-voltage industrial customers.

These two cases of trouble involved a complete outage to relatively large blocks of load and yet in both cases when service was re-established there was no evidence that the generating equipment was unable safely to handle the load.

Since this last disturbance, generation and spare capacity has been allocated to the several load areas in such a manner that not only is the system protected for loss of generating capacity but each major load area is protected against loss of either generator or transmission capacity, except in the case of four-circuit tripouts.

There have been two other recent major electric failures which involved bus sections at the Colfax power station. In one case a 60,000-kw unit was completely crippled, due to a three-phase short circuit which destroyed an entire 12-kv bus section. The unit operating setup prevented these failures from interfering with the operation of the balance of the station, except for the voltage disturbances during the fault.

The most serious outage the system has ever observed started on March 18, 1936, as a result of the all-time record flood on the Allegheny and Monongahela Rivers. The flood stage reached a maximum elevation of 46.2 feet which was 36.2 feet above the normal river level. The highest previous flood, since the settlement in Pittsburgh, occurred, in March 1907, at which time the flood reached a level of 39.7 feet. Serious trouble was first encountered at a 39-foot stage about 4:00 a.m. on March 18. Preceding this time a few transmission and distribution circuits had been lost and most of the high-voltage customer stations in the low-lying industrial districts had been taken out of service at the customers' requests. Shortly after this flood water began to involve major substations and equipment. In rapid succession the 66-kv north-ring transformers at Brunot Island; the 11-kv control board at the Stanwix substation which feeds part of the downtown Pittsburgh district; and all system transmission and distribution services on Neville Island and adjacent districts along the Ohio River were removed from service. During this period the communication circuits were failing. By early morning contacts had been lost with many substations, the Brunot Island and Reed power stations, and the dispatching headquarters for the downtown distribution division and the substation division.

The operating problem during this period consisted of isolating faulted equipment and maintaining what service was possible on the remaining lines. About 7:00 a.m. the flood had reached the circuit-breaker mechanisms of the 66-kv line breakers at Colfax power station and the 66-kv and 22-kv line breakers at Brunot Island, making it impossible to operate any of the outdoor switching equipment and crippling the protective-relay system. However, this equipment was not removed from service due to failure of the control. When necessary, switching was done manually from boats. The system load reached its peak Wednesday at about 8:00 a.m. and decreased steadily after that time due to the flooding of customers' equipment. The loss of load on the system due to the effects of the flood reduced the system demand below the available capacity of the generating stations so that up until the time of shutdown there was adequate power for all essential services that were outside of the flooded areas.

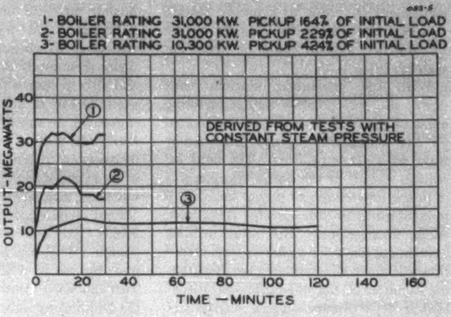


Figure 5. Typical stoker boiler response curves—constant steam pressure

Although the low-voltage-network areas in downtown Pittsburgh were practically all flooded, service was available until the system shut down. The submarine-type equipment withstood the flood with an insignificant amount of damage.

The Brunot Island station was the first generating station to abandon operation. The Reed power station was taken out of service at 11:20 a.m. due to flooding of the electric-bay basement. The turbine room was not flooded.

The Colfax power station continued to operate until 3:50 p.m. although a leak in the foundation which was adjacent to a field rheostat required removing from service a 40,000-kw unit at 11:37 a.m. This leak and some minor damage to the electrical equipment was repaired and the unit was about ready to return to service when it was necessary to shut down the entire station.

It was known for some time previous that there was little chance of averting a general shutdown. The 66-kv transmission system had been sectionalized so that the opening of a single breaker would drop a block of load. By this means the shutdown could be accomplished in an orderly manner without undue risk to the equipment at Colfax. It was also thought best to drop the load by opening a single beaker in order to forestall damage to customers' equipment which might result if operated at low voltage and frequency. The authorization to shut down was given at 3:47 p.m. following a rupture in one of the foundation walls in the Colfax power station. The dropping of sectionalized load areas proceeded and as the Colfax station load was reduced a generating unit was disconnected from the system by opening the main transformer breakers.

The first unit was off the line at 3:50 p.m., three minutes after the shutdown authorization was given and the last unit was off the line at 3:58 p.m., 11 minutes after the initial order. The station-service generators had been flooded by water entering the air ducts and were not available for some time preceding the shutdown. The total system load dropped was approximately 110,000 kw. This procedure was executed in remarkable fast time without any operating errors or any other event which in any way damaged any of the generating equipment or exposed the operating personnel to undue risk. All main equipment was de-energized before the flood water reached the insulation.

At this time several 22-kv transmission lines, which had previously been transferred to adjacent power companies, were carrying a load of approximately 2,500 kw, which saved the system from a complete shutdown.

The problem facing the operating organization at this time involved complete de-energizing of all the substation control equipment in order to conserve station service batteries until such time as the transmission system could be again energized.

With the generating stations shut down, the only possible way of restoring a few of the most essential services rested on the availability of the interconnections with other utilities. The need for station service at the power stations was of prime importance to permit immediate re-establishment of dewatering operations. While arrangements with other utilities were being made for power supply, attention was given to organizing the dispatching facilities of the substation and distribution departments. These dispatchers were moved to offices adjacent to the system operator to eliminate telephone congestion. These moves did not retard the rehabilitation work as no action could be taken until the flood waters had receded about ten feet. In the meantime the management formulated a "service priority list" to designate the order by which service was to be re-established. The list follows:

1. Communication
   - (*a*). Police and fire signal systems
   - (*b*). Telephone communication, including telegraph
2. Essential service, including cities, towns, and townships
   - (*a*). Water pumping
   - (*b*). Hospital service
   - (*c*). Dairies
   - (*d*). Bakeries
3. Street service
   - (*a*). Municipal street lighting
   - (*b*). Street cars (curtailed service only)
4. Newspapers
5. Domestic electric lighting
6. Commercial light and power
7. Industrial

Startup From System Shutdown

Before discussing any plans or sequence of starting up a system, it should be recognized that the cause of shutdown necessarily dictates the method of re-establishing service. With this thought in mind, two types of shutdowns will be discussed. The first case will have particular reference to the flood failure, which, briefly, is a type where total generating capacity is curtailed and cannot be immediately restored. A second case is assumed whereby some unforeseen electrical failure causes a complete shutdown, with the power sources, transmission system, and substations immediately available for service re-establishment upon isolation of the faulty equipment.

The system was shut down in an orderly manner in spite of the communication difficulties. This was due in part to instructions to all substations not to attempt reclosure in event of circuit failures and also to open and clear all circuits in event of loss of power supply. Prior to shutdown, arrangement was made with a local radio station to broadcast switching instructions to the Colfax power station, in the event of complete telephone failure, but these arrangements were not used.

First consideration was given to establishing station service to the Colfax power station. This was accomplished at 6:33 p.m. by use of a 22-kv interconnection with the West Penn Power Company to the Dravosburg substation. This station then energized Colfax through a 66-kv line and one of the main transformer banks. These banks were without their water-cooling facilities and were operated intermittently to prevent overheating.

The 66-kv interconnection with the Pennsylvania Power Company at Valley substation was energized next at 8:04 p.m. and Colfax transferred to this source. The West Penn Power Company tie was unavailable due to flooding of both the Colfax and Springdale switchyards.

Due to the flooding of the 11-kv cable potheads at the Brunot Island switch house, service could not be restored at this station until the cable terminals could be replaced, although a source of feed was available. The cable operation was complicated by the flooding of the 11-kv grounding-bank connections. The Brunot Island plant was completely flooded so that normal service facilities could not be used to dewater the plant.

The next major step was restoration of the Bell Telephone facilities in the downtown area served from the 11-kv network. This customer had a multiple-unit vault, one cable of which originated at Grant, and energizing the network feeder, all network transformers were stripped from the network feeder and the street main fuses opened at the Bell vault. The pickup of this service cannot be overemphasized, as its effect was immeasurable in insuring communication channels both for civic and company use.

On March 19, 1936, all load that had been restored was carried by the 66-kv interconnection at Valley and numerous 22-kv interconnections with the West Penn Power Company, each source serving isolated load blocks. At approximately 9:30 a.m. on this date, many substation busses and transmission lines were energized to facilitate service restoration to water-pumping stations, and other essential loads, most of which were served at 11 kv or 22 kv. At 12:15 p.m. additional capacity was added to the system by closing the Colfax-Springdale tie line to the West Penn Power Company. All load at this time was confined entirely to essentials, and the total system output limited to about 40,000 kw. In order to limit the service pickup to the priority list, a gigantic task was being performed by the distribution department in stripping the necessary circuits of non-essential load in order to limit the output to the interconnected capacity available.

On March 20, service was broadened to include some residential load, mostly rural, for the re-establishment of water supply. A total of about 30,000 kw was added in this manner, bringing the system output to about 74,000 kw.

Meanwhile, the power stations department was working to restore generating capacity, but was handicapped in many ways. At Brunot Island, for instance, the station was badly flooded and had to be dewatered. To accomplish this, a flood pump had to be moved in by a crane which required 500-volt d-c service. As the rotary converters at the Brunot Island station were inundated, service to the crane could not be established until a rotary converter was made available. By the time this was accomplished station service was available from the switch house through temporary leads.

The Reed power station, after drying out the coal-handling and circulating-pump equipment, was restored to service. It was necessary, however, to inspect and clean all the electrical connections in the Brunot Island switch house before power could be utilized from this unit. The boiler plant at Reed was kept on a live bank during the entire period, by permitting the coal reserve in the bunkers to drop to the floor and manually firing through the end doors, utilizing natural draft. The boilers supplied steam for a boiler feed pump which was temporarily modified to pump the seepage from the turbine room basement.

To fully appreciate the problem at Colfax, requires a brief description of the plant at the period immediately following shutdown. The flood water, while not affecting the turbines or generators, completely inundated the turbine-room basement crippling the main-unit auxiliaries entirely. In the switchyard, water had reached the 66-kv breaker mechanisms for the 66-kv lines and the West Penn Power Company tie line and had damaged practically all control cable.

At the time of the flood a 30,000-kw unit was being overhauled. All the auxiliary equipment below flood level that could be removed was moved above flood level thereby providing enough motors on another 30,000-kw unit to replace the flooded equipment. This made it possible to start the first 30,000-kw unit at 4:10 a.m. on March 22 about 84 hours after the shutdown.

In contrast to the other plants, Colfax had station service shortly after the shutdown. The boiler plant was completely down, and the first stoker-fired boiler placed in service about 39$^1/_2$ hours after shutdown.

At 5:15 p.m., March 23, the Reed number 1 unit was returned to service, increasing the system capacity. At this time the scope of pickup was expanded to include all available residential load. All distribution circuits outside of the flood area were cut in, and part of the load which had been stripped from circuits was restored. The pickup for this day amounted to about 50,000 kw.

The estimated demand at this point was about 20,000 kw in excess of capacity. This condition was set up to improve the load factor and with the intent of dropping any excess load over the peak period. During this period emergency overload ratings were in effect on all operating equipment.

The addition of generating capacity to the system required special relay settings and bus setups to protect adequately the equipment involved. These setups were too numerous to mention, but it is important to note that all equipment energized had some sort of protection provided. The system at this time was still segregated into several isolated load blocks with the largest block carried by the Duquesne Light Company stations. All of the available interconnections were still in use.

The load pickup was made along well-defined lines. As generating capacity was made available, a program was devised to bring the system peak load up to the available capacity. This was accomplished by preparing an hour-by-hour forecast of system load for the next day. As the system proper was still isolated into load areas, the load to be added was tabulated under the area affected and a total determined for the forecast. The aggregate of the areas gave a forecast for the system which was surprisingly accurate. Since the essential loads available were already connected, the forecast program assisted in determining the amount of excess power available in an area that could be prorated to the next succeeding service class on the priority list. On March 24 and 25 many industrial customers were reconnected on a restricted basis. As the generator capacity was increased, the isolated load blocks fed by various interconnections were transferred back to normal and by March 26, all services connected were on an unrestricted basis. At this date, however, there were many customers without service in the flooded areas.

After energizing the 11-kv bus at Brunot Island, the testing and energizing of the low-voltage network began. The transformers were cleared on all 11-kv feeders and the feeders tested individually from Brunot Island. With the feeders out, all transformer units unaffected by the flood were closed and the feeders closed at Brunot Island on a dead bus. The bus and network were then energized by closing an adjacent bus tie. It was necessary to conduct these tests with several cables and all available transformers to provide sufficient short-circuit current to clear a network fault.

Immediately following the return to service on an unrestricted basis on March 26, the emergency routing was rapidly converted into a heavy maintenance program. Figure 4 summarizes the progress of service restoration.

Station Pickup Capacity

There are several definite limitations that must be observed in determining the amount of load that can be picked up instantly and yet protect the equipment.

1. The load block that is to be picked up must fall within the overload capacity of the turbine and the steaming capacity of the boilers. The electrical equipment can usually withstand any load the turbine can deliver for a short time. Failure to observe this limitation will result in stalling the system.

2. The load pickup should not be so sudden or be such a large part of the available capacity as to cause priming of boilers with the danger of water carry-over damaging turbine blading.

3. The rate of load pickup on a turbogenerator which has just been rolled and synchronized must be held within the limits set by the manufacturer in order to prevent damage to the blading. This limit is usually about five per cent of the rating per minute. If the shutdown has been of short duration, the turbines which were carrying load preceding the disturbance usually will not have a rate of pickup limitation.

The boiler capacity in general sets the load conditions. Tests on individual boilers show that instantaneous changes in the steaming rate can only be realized when accompanied by a pressure drop using the accumulator effect of the boiler for the sudden increase in output. The curves (figure 5) show the rate at which the steaming rate is increased by an increase of the combustion rate. Powdered-coal boilers respond as shown and can hold the rating after the initial pickup. Stoker boilers are more uncertain in their performance due to the variation in condition of the fuel bed. A stoker boiler with a light fuel bed can produce a high rating for a few minutes but may not be able to hold the output until such time as the fuel bed can be built up for operation at the higher rating. This may require at least one-half hour before the ultimate rating is available.

Repeated tests have indicated that the following pickup ratings can be met in everyday operation. The standby boilers or boilers counted as having reserve capacity should be operated at not less than 15 per cent of their normal rating in order to keep the fuel beds ready for additional load. With this requirement met the boilers are then able rapidly to increase their output to 250 per cent of the initial output, provided the maximum rating of the boilers is not exceeded. The boiler spare capacity is based on this pickup ratio and the same ratio would apply for restoration of system load.

Theoretical studies have been made of boiler performance under these emergency conditions, but these studies, in general, indicate the optimum condition rather than the practical aspect.

One of the definite limitations in the pickup rate is personnel. An emergency load pickup greatly increases the attention required from the boiler operator; for instance, the feed water control is made semiautomatic to reduce the drum level as a preventative of priming, the dampers require resetting, and additional fans may be required. The coal feed is increased, the powdered-fuel boiler burners may require relighting if the auxiliary power supply was lost during the load drop. All these duties must be attended to manually and the rate at which the operating organization can accomplish these moves determines the degree by which the optimum rate of pickup can be reached.

Upon the occurrence of a major shutdown the several power stations immediately take such measures as may be required to hold what load is left or to protect the equipment. In general, the safety of the equipment takes precedence over system service, since once a disturbance has occurred any abnormal risks to equipment may result in further trouble which will require days instead of minutes to remedy. During the emergency the electrical switchboard operator becomes the key man in the stations, contacting the system operator, and advising the turbine and boiler-room operators of the system conditions and other information as fast as it is available.

The system operator is advised as soon as possible of the general conditions within the plant and furnished an estimate of the pickup capacity and the time required to perform this pickup. During this interval the station conditions will be returned to normal as fast as possible but no attempt will be made to restore service until all the elements contributing to service restoration are under control. Upon the sounding of the emergency alarm, certain picked maintenance men, who have had operating experience report to the turbine room, boiler room, and control room to augment the operating personnel and assist in the extra duties caused by the emergency. In event of loss of communication with the system operator, each plant will attempt to limit the load by opening lines, in order to retain reasonable steam pressures and prevent a general failure of the generating stations.

Experience so far has indicated that conditions on the transmission system and the time interval required by the system operators to co-ordinate the emergency activities retard the rate at which service is restored well within the limits of the power stations.

In order to start up a system which has been shut down, a plan or program must be provided. If, for instance, the flood hazard is great, a flood program is necessary. On the other hand, if shutdown is the result of failure unforeseen, the plan of pickup is determined by the remaining available facilities and the possible setup ensuing at the time. When a failure occurs, the operating personnel goes on its own, the load dispatchers, switchboard operators, and so on down the line must accept and share the responsibility of performing the duties required. Recognition must be made of existing rules governing operation such as overload ratings, equipment protection, and rate of pickup, and all these factors co-ordinated. In conclusion, past experience has proved the need for a pickup program. The major considerations necessary for making two types of plans for start-up are tabulated below and have proved invaluable in the restoration of service.

I. PREDETERMINED SHUTDOWN CAUSED BY FLOOD

Curtail service in an orderly manner, saving all equipment possible. This expedites the start-up.

Provide system operator with elevations of all equipment in flood area referred to river elevations.

Provide operating departments with detailed instructions giving the required work and material location for protection or salvage at incremental flood stages.

Establish communication facilities. It is necessary to enlarge existing facilities to care for increased traffic. Also emergency radio channels and adequate messenger service are needed. It is essential that boats and automobiles be provided for the messengers.

Provide station service to power stations. Standby interconnections, small oil-engine generator sets for lighting, and small power uses are essential. Steam-driven auxiliaries with stoker boilers are also desirable.

Provide a "priority of service schedule" with the necessary operating instructions to effect immediate restoration of service for public safety or health.

Provide a loading program based on the "priority schedule". This is based on the amount and character of the various service classes. Utilize restriction of service during emergency, either by municipal proclamation or appeal by company executives in order to widen scope of service restoration with limited capacity.

Establish operating personnel routine. It is necessary to provide a training program as part of operating routine to prepare for floods. During the emergency it is necessary to insure adequate food, safe water, and provide shelter and in some cases, clothing for employes. In addition it is essential that an immediate checkup be made of home conditions for those employes retained on duty.

Observe strict limits on total working hours to protect employes from sickness and injury.

II. SHUTDOWN CAUSED BY FAILURES NOT PREDETERMINED

Make a survey of system conditions immediately following a major shutdown of this type. This provides sufficient time to collect thoughts and avoids rash moves which may further damage equipment and thereby complicate service restoration. This further provides sufficient time to determine the status of the power plants and locate the failure.

Provide a plan of restoration. The plan will depend on the facilities available for operation. It is extremely desirable to maintain some firm boiler capacity to initiate the restoration plan. If possible, parallel a part of the generating capacity with available interconnections, and back feed power to the interconnection to establish firm plant capacity. When possible, parallel generating plants through the transmission system to increase the pickup ability. Restrict the rate of service restoration to avoid recurrent curtailment, and if required, segregate the system into load blocks which, when added, will not be in excess of the available turbine or boiler plant capacity. Co-ordinate the rate of service restoration with actual conditions of boilers and other essential power-plant equipment.

Establish a routine for operating personnel. Complete co-ordination is necessary between the load dispatchers and the switchboard operators. Provide plans for augmenting this personnel during emergency, and these plans should be predetermined and included in the operating routing. In conclusion, place complete confidence in the operating personnel to get out of trouble; they are intimately acquainted with their specific phase of operation, and can do the job best.

Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Restoration of Service From Large Metropolitan System After Complete Shutdown

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Synopsis: This paper sets forth briefly two general procedures that may be followed in starting the system of the Philadelphia Electric Company after a complete shutdown, and also gives some experiences relating to re-establishing service. There is also included an outline of some of the major principles of design followed in the development of the system which are important in minimizing the chance of a complete system shutdown and in facilitating restoration of service in the event of a complete outage.

It is believed that the problem of expeditiously restoring service from a system in the event it is completely interrupted may be divided essentially into two parts. The first has to do with operating routines which require that the operating men are thoroughly conversant with the many things that they are required to do at such times so that upon instructions from the system operator various activities in connection with restoring service will be carried out with little or no confusion. Needless to say, the process of restoring service is much easier when the layout, both in the stations and in the connections between stations that comprise the backbone of the system is not complicated and has a high degree of uniformity.

The second part of the problem of restoring service has to do with the maximum size of the block of load that must be picked up at one time, which if very large may cause trouble due to automatic opening of circuit breakers or difficulties due to the steaming rate of boiler plants not increasing rapidly enough.

Fortunately, in the case of the Philadelphia Electric Company, there are no single blocks of load large enough to make this a real problem. The largest of the three networks in the central business district of the City of Philadelphia has a peak demand of only about 10,000 kw. The load of the Pennsylvania Railroad Company, while slightly exceeding 100,000 kw, can be split into two parts for the purpose of re-energizing. In this case it is believed that the load will build up to the figure of approximately that before the interruption occurred over a period of several minutes as the trains get under way. The load of the Philadelphia Rapid Transit Company, which is of the order of 100,000 kw, is supplied from a number of different locations and therefore presents no problem in restoring service.

For the reasons outlined in the preceding paragraph, this paper will concern itself mainly with principles of design followed and development of the system in order to facilitate restoration of service, and to the operating procedures followed in order to re-establish service as quickly as possible.

Description of Bulk Power System

The bulk power system of the Philadelphia Electric Company (see figure 1) is that of a divided arrangement of the 66-kv and 220-kv transmission facilities into two backbones of the system; parallelism of the two distinct high-voltage systems being effected on the 13-kv busses through the transformers at the major load centers of the Philadelphia Electric Company and on the 66-kv ring bus of Christiana substation (Wilmington) and the 66-kv busses of Deepwater station. At Conowingo hydro station, the divided arrangement of the 220-kv transmission facilities extends to and includes the 13-kv generator busses.

The major load centers of the Philadelphia Electric Company are the steam generating stations and large high-voltage step-down substations. From these load centers energy is transmitted to distribution substations and to customers having high-voltage service. Customers of this type are also supplied from distribution substations.

The distribution of the generating facilities of the company among the major load centers is such that a satisfactory balance of the generating capacity and area load in each load center is obtained. The comparison of generating facilities and load of each load center is given in table I.

The capacity of the transmission facilities between all load centers, generating, and high-voltage step-down transformer stations, is adequate to make generating capacity of one area available to another for economical system operation or as reserve coverage in providing for the possible loss of generating equipment on any part of the system.

Principles of Design

Some of the general principles of design that are followed are enumerated in the following paragraphs:

1. Generating capacity is provided as close to the load areas as economics and other conditions make practicable, or in other words it is the desire to make each load area as nearly self-sustaining as possible under the circumstances encountered. In the case of generation by hydroelectric plants this is obviously not possible, due to the location of suitable sites being some considerable distance from the load.

2. Simplicity of layout. It has been the practice of this company for a great many years to avoid complication, both in the stations and in connections between stations, in view of the many advantages thus to be obtained.

Table I. Generating Capacities of the Various Load Centers and Approximate Amounts of Load That May Be Supplied Through Each Center

	Generating Capacity (Kw)	Area Load (Kw)
Richmond	255,000	180,000
Delaware	183,000	137,000
Chester	126,000	130,000
Deepwater	58,000	40,000
Schuylkill	179,000	170,000
Paschall		20,000
Conowingo	252,000	3,000
Plymouth Meeting		25,000
Westmoreland*		100,000
Barbadoes Island	45,000	75,000
Total	1,098,000 kw	900,000 kw

* Westmoreland load area included here as in Conowingo load area for the purpose of restoration. Normally it is part of the Richmond-Delaware area.

3. The system is so sectionalized as to minimize the effects of faults. As pointed out, the portion of the system operating at 66 kv is operated to all practical purposes as two separate 66-kv systems, which are interconnected at certain points mentioned earlier in the paper. The operation of the 66-kv system in this manner not only gives lower short-circuit duty on switchgear, etc., but minimizes the disturbance produced under such fault conditions. The sectionalizing of the system is further planned so that failure of any one locality should not reduce the capacity available to carry the load in this particular area below that required for the load involved.

4. Quick clearance of faults. Very much has been done, particularly in recent years, in reducing the length of time required to

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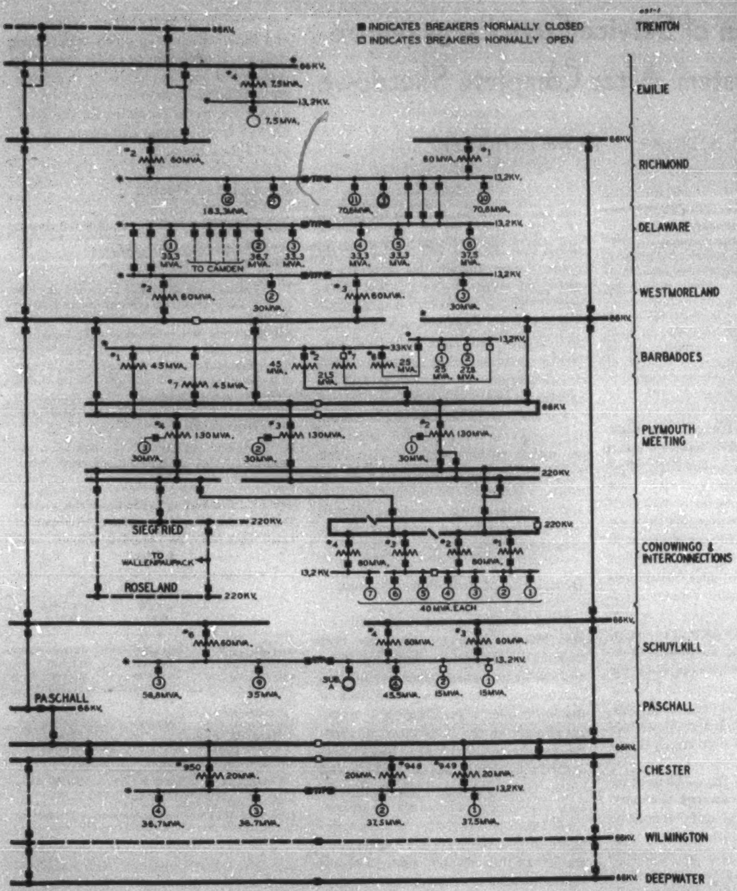
■ INDICATES BREAKERS NORMALLY CLOSED
☐ INDICATES BREAKERS NORMALLY OPEN
TRENTON
EMILIE
RICHMOND
DELAWARE
WESTMORELAND
BARBADOES
PLYMOUTH MEETING
CONOWINGO & INTERCONNECTIONS
SCHUYLKILL
PASCHALL
CHESTER
WILMINGTON
DEEPWATER
66KV.
13,2KV.
33KV.
220KV.
*4
7.5MVA.
7.5MVA.
*2
60MVA.
*1
60MVA.
12
183,3MVA.
11
70,8MVA.
10
70,8MVA.
1
33,3 MVA.
TO CAMDEN
2
36,7 MVA.
3
33,3 MVA.
4
33,3 MVA.
5
33,3 MVA.
6
37,5 MVA.
*2
60MVA.
2
30MVA.
*3
60MVA.
3
30MVA.
*1
45MVA.
*7
45MVA.
45 MVA.
*2
21,5 MVA.
*7
*8
25 MVA.
1
25 MVA.
2
27,8 MVA.
*4
130MVA.
3
30MVA.
*3
130MVA.
2
30MVA.
*2
130MVA.
1
30MVA.
SIEGFRIED
TO WALLENPAUPACK
ROSELAND
*4
*3
*2
*1
80MVA.
80MVA.
7
6
5
4
3
2
1
40 MVA. EACH
*6
60MVA.
*4
60MVA.
*3
60MVA.
3
58,8MVA.
9
35MVA.
SUB. A
4
45.5MVA.
2
15MVA.
1
15MVA.
PASCHALL
*950
20MVA.
*948
20MVA.
*949
20MVA.
4
36,7MVA.
3
36.7MVA.
2
37,5MVA.
1
37.5MVA.

Figure 1. Bulk power system of the Philadelphia Electric Company

In addition to the generating capacities shown in this figure the company has available a capacity of 62,000 kva in the Deepwater plant, which is owned jointly with the Atlantic City Electric Company. At the locations marked with an asterisk busses not normally energized have been omitted for the sake of simplicity. Certain stations of other utilities and the lines connecting with them are shown with broken lines

clear faults on those portions of the system where quick clearing is important. This involves the use of pilot-wire relays on 66-kv cables, differential protection of busses and transformer banks, and for certain applications circuit breakers that will clear a fault within eight cycles (0.13 second).

5. Isolation of equipment. Considerable attention is being given to the arrangement of equipment in switch houses, provision of barriers, etc., to provide a proper degree of isolation in the event of oil fires, explosions, etc. There is now under way complete modernization of the Schuylkill switch house which changes the layout from the present double 13-kv sectionalized bus to a six-section sectionalized ring bus with modern switchgear at the sectionalizing points and at several other important locations. It is believed that a layout of this type should do much to minimize the amount of capacity affected in the event of a fire or explosion. In the case of outdoor substations considerable attention is given to the location and spacing of equipment so that in the event of a failure it will not affect other equipment which is used for reserve purposes.

6. Interconnection. The most important of the interconnections of the system of the Philadelphia Electric Company with other companies is the 220-kv interconnection with the Pennsylvania Power and Light Company and the Public Service Electric and Gas Company of New Jersey. This interconnection not only provides advantages in the way of economy of operation, generator reserve, etc., but is extremely useful in the event of an emergency.

Starting Generating Station From Shutdown State

It has been indicated that each load area of the Philadelphia Electric Company system is adequate unto itself in generating capacity. Thus restoration of the system load should a complete shutdown occur may be a matter of starting each individual station, restoring the area loads and then paralleling the sections of the system to return to normal operation.

The restoration of each generating station is usually dependent on the supply to the auxiliary light and power busses. This supply, in the event of any station shutdown, may be provided by an electrical feed from an outside source. Failing this, the thermal capacity of the boilers in most of the stations is such that it would be possible to start up the auxiliaries and even a main unit. Thus, steam-driven d-c sets can furnish power for stoker drive and for emergency lighting; a Duplex circulating-pump set can be operated as a generating unit of 200-kw capacity and supply stoker sets, and fans and steam-driven oil pumps can be operated to restore oil-fired boilers. In one station a house turbogenerator is available to carry auxiliary load.

In instances of an individual station shutdown, however, an electrical feed from an outside source would usually be available for supply to the station auxiliary light and power bus and other than the usual starting measures should be unnecessary.

The supply to the electrical auxiliaries of Richmond station is indicated in figure 2. The electric auxiliaries in the other steam stations are supplied from transformer banks stepping down from a 13.2-kv bus and there are no transformers connected to the generator leads and no house turbogenerator.

Restoring the Station Load

When an operating bus has been subjected to an interruption, the bus is cleared; that is, all bus circuit breakers are opened. As the generating sources are restored to the bus, the outgoing lines are gradually restored and at no time will the capacity of these lines exceed that of the restored generating capacity. The operators in distribution and industrial substations restore their load gradually not exceeding the capacity of the lines made available to them.

Speed of Load Restoration

The restoration of the load should not proceed too fast for the boiler operators to handle. Delay may arise, however, if the dead busses are not cleared immediately after the interruption, as the simultaneous energizing of a number of large transformers and an appreciable amount of load from one source may cause a severe drop in voltage. This might result in the loss of auxiliaries such as boiler feed pumps and fans. As an instance, at the time of the complete system shutdown of April 4, 1937, the Schuylkill, Chester, and Wilmington areas were simultaneously closed to Deepwater station within a few minutes after the initial outage. The load of these areas just previous to the outage was approximately 90,000 kw. During the interruption, Deepwater station continued to supply the load of the Atlantic City Company and of the Du Pont Manufacturing Company, the combined load being approximately 40,000 kw. When the transformer equipment and considerable of the load of the three areas were simultaneously restored on Deepwater station, the voltage surge caused the loss of a boiler feed pump and the drop in steam pressure delayed the restoration.

In the initial step of restoration, the load of Deepwater station increased from 40,000 kw to 100,000 kw, Deepwater thus picking up all but 30,000 kw of the normal load of the three areas. The steam pressure at Deepwater station fell to 930 pounds, and 19 minutes elapsed before normal pressure, 1,300 pounds, was restored. The load of the three areas required 20 minutes to return to normal.

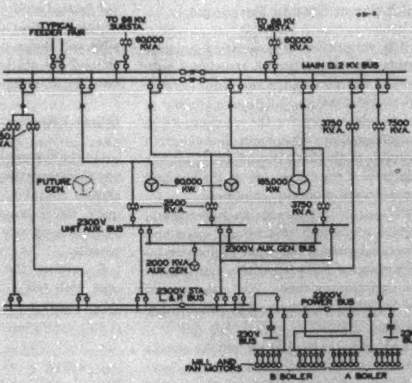

Figure 2. Auxiliary power layout for Richmond station

This station is the only one on the system of the Philadelphia Electric Company that has a steam-driven house generator

If the bus of each load area had been cleared, the restoration could have proceeded in successive steps rapidly effected as pilot lights indicated the lines had been re-energized.

Operating Procedures

Either of two methods may be followed in starting up the system after a complete shutdown and the choice of method may depend on the normal system load at the time of outage and the generating capacities that had been in operation just previous to the disturbance.

If, for instance, the shutdown occurs in the early morning hours, when Richmond, Schuylkill, and Deepwater stations had been operating at minimum loading, and Conowingo hydro station had been delivering maximum output, the procedure would be as follows, all stations having cleared their dead busses when the outage occurred.

1. Conowingo hydro station; bring all hydro units to synchronous speed.

1*a*. Deepwater station will energize lines to Christiana substation.

1*b*. Christiana substation will energize lines to Chester station and restore its 11-kv load.

1*c*. Chester station will energize its 66-kv busses and 13-kv bus from the Christiana substation 66-kv lines and restore its load.

2. If pilots are not received on 220-kv lines at Conowingo one minute after outage, all units will be closed to bus and both 220-kv lines to Plymouth Meeting substation will be energized.

3. Plymouth Meeting substation will restore the 66-kv lines to Westmoreland substation and Barbadoes Island station, and restore 33-kv lines.

4. Plymouth Meeting substation will parallel with the 220-kv interconnection.

5. Westmoreland substation will energize 66-kv lines to Richmond and Schuylkill stations.

6. Westmoreland substation will restore its 13-kv lines including the Delaware station tie lines.

7. Richmond station will restore its 66-kv and 13-kv lines including the Delaware station tie lines.

7*a*. Delaware station will restore its load on the first group of tie lines to receive a pilot, and then parallel the second group of tie lines.

8. Schuylkill station will energize the 66-kv lines to Chester station and its 13-kv lines.

9. Chester station will restore the system to parallel operation by synchronizing the Schuylkill station and Christiana substation 66-kv lines.

10. Other generating capacities that had been in operation may then be restored.

If the shutdown occurs during a heavy load period the restoration may be effected by each generating station restoring the load of its own area. When this is done, the sections of the system will be paralleled to establish normal operation. In such outages, there is one important rule; namely:

"Clear the dead bus."

As has been mentioned, this means that all circuit breakers of the dead bus must be opened. In restoring the lines and apparatus, this practice will avoid a repetition of the fault without being aware of its location and will prevent a voltage disturbance and the confusion that may arise if too much load is re-energized when the first unit is closed to the bus. The control of the return of the load also may be more satisfactorily maintained in this manner.

Initiative of Station Personnel

The restoration of generating capacity within the station to full speed and voltage is a function of the operating personnel of that station. The availability of an outside supply for the starting of the station auxiliary equipment, however, is a function of the load dispatcher, as is the direction of the energizing of the bus and the restoration of outgoing lines.

Co-operation between the boiler room, turbine room, and switchboard operators of necessity and through the realization by each group of their respective functions is of automatic response. The personnel of the boiler room knows that the station output must be restored to what it was before the disturbance and they also know what their part in the program must be. The personnel of the turbine room are also familiar with their responsibilities and this is also true of the switchboard operators, for the operations all of these men must perform in an emergency are similar to their everyday functions.

It is in the location, inspection, and isolation of faulted equipment that a high degree of initiative is very essential. The confusion that usually attends this type of station failure is usually the cause of undue delay in restoring the station to operation. The electrical supervision must first of all observe the essential safety regulations and see that none of the equipment in the area being inspected is energized until all men have left the scene. His inspection is primarily to determine what equipment may be re-energized and what switching must be done to isolate the faulted equipment. The performance of this function as expeditiously as possible will mean usually that the restoration of the station will be effected without any unnecessary delay.

Other Effects of Shutdown

On the system of the Philadelphia Electric Company the procedure in starting up after a complete shutdown does not require any changes in relay settings or alterations of any nature in the protective scheme.

Regarding the possibility of interference with telephone and signal systems, this is present for the duration of the fault and, in some of the past occurrences, has resulted in trouble on the circuits of a railroad company but has had no apparent effect on the leased telephone communication circuits used by the company.

Conclusion

The possibility of a complete shutdown of the system of the Philadelphia Electric Company is extremely remote, particularly as a result of changes effected in operation and design during the last two years.

The starting up after a complete shutdown, if it should occur, may be effected by starting each individual station separately as the generating capacities and the load of each major distributing center approaches a balance.

The restoration of all but the 25-cycle railroad load should usually be effected within five minutes, and the entire system within ten minutes, provided the functioning of the operating personnel is satisfactory.

So far as can be foreseen at the present time, it is expected that the design of system extensions will follow the same general principles now in use, except that sectionalized ring busses will likely be used more generally in the future instead of double busses.

Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

The Design and Operation of a Metropolitan Electrical System From the Viewpoint of Possible Major Shutdowns

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Synopsis: The bulk power system of The Detroit Edison Company is designed and operated in five power-plant load areas. These load areas are "loose linked" to prevent uncontrollable trouble in any one of them causing a total shutdown.

The internal design of the load areas is arranged to permit of ready sectionalizing in event of serious area shutdowns, thereby permitting the load to be picked up in small pieces. Such sectionalizing is practicable because the distribution lines are largely radial, that is, are not connected together beyond the substation.

A limited amount of experience data are given on the behavior of generators under instantaneously applied loads.

The rationale of emergency operating is developed and the special instructions outlined.

DESPITE the efforts of central-station operators and designers to the contrary, there occasionally occurs a complete shutdown of an entire major electrical system or a large section of one. Unless, in the design and the operation of the system, proper account has been taken of such a contingency, it may prove very difficult to again "start up" the system and unnecessarily long service outage may result.

Accordingly, it was felt that a symposium covering the design and operating practice of several large systems with respect to "starting up" would prove helpful to those facing such a problem. It is as a part of such a symposium that this paper is presented.

In any particular situation the difficulty of starting from complete shutdown, turns in the last analysis on the degree to which the load itself may be divided into discrete pieces, small enough to be picked up by the available generating capacity.

General

On a system where the load consists largely of primary or secondary networks it is desirable, in the interest of economy, to make the individual blocks of load or "pools" as large as can, with certainty, be picked up by the generating capacity available and by the number of feeders which the operators can simultaneously close onto the busses. In the design of such a system it is necessary to have a fairly accurate idea of the performance of generators under suddenly applied loads or overloads, and also how the load may be expected to vary with reduced voltage and perhaps reduced frequency. While some testing and a great deal of calculating have been done, the most reliable knowledge must of necessity come from the actual experiences of systems under emergency conditions and one of the principal purposes of this symposium is to bring forth reliable experience data of that kind.

On the other hand, in a system employing radial distribution lines beyond the substation, that is, lines which are not connected together, the load is already divided into relatively small pieces which can, if necessary, be picked up one at a time in detail.

Accordingly, the approach to the "start up" problem is determined by the character of the load area served and becomes a question of designing to meet the particular situation.

With the exception of a square mile or so, the territory of The Detroit Edison Company lends itself to the use of radial overhead distribution lines. Very few of the radial circuits have loads in excess of 1,500 kilowatts. Thus, there is no problem in picking up the individual circuits. In fact, in most cases, there is little difficulty in picking up whole distribution substations. Accordingly, this paper will be largely confined to the design of the metropolitan system to restrict shutdowns to limited areas and to the emergency operation of such a system.

The Bulk-Power System

Fundamental Design

To guard against the possibility of total shutdowns, the design of the bulk power system of The Detroit Edison Company is based on two principles.

The first principle is that the system shall be divided into several load areas (at present five) so interconnected that, should an uncontrollable fault persist in any load area, this area with its prime power source will disconnect itself from the system, thereby permitting the remainder of the system to go on its way. Also, each load area must, within reasonable limits, be self-sustaining since it may have to start up by itself and perhaps run that way for a time. This principle has been dubbed "loose linking."

The second principle is almost corollary to the first and is, that with any load area disconnected the remainder of the system shall be self-sustaining, that is, shall be able to carry its load at normal frequency and voltage.

Figure 1 is a diagram of the bulk power system of The Detroit Edison Company. You will note that there are five power-plant load areas held together at what are termed "interlinking points." Each "interlinking point" is indicated by a small rectangle with a diagonal line drawn through it. The solid black rectangles represent the ones in use at the time and the open rectangles the alternates.

While the design principles outlined above appear simple, there are several implications which in practice call for care in design as well as in operation. The first implication is that, since an uncontrollable fault is one which is not cleared by the normal protective equipment, the operation of the system sectionalizing circuit breakers at "interlinking points" must be delayed until the internal protective gear has had opportunity to clear the fault if it can do so, that is, until the fault has really become uncontrollable. (Needless to say, several successive steps of protective gear must have simultaneously failed before a fault becomes uncontrollable.) This necessary time delay at the interlinking points may amount to several seconds which might be troublesome to the other load areas from the point of view of stability of generators. Therefore, it is necessary to have sufficient impedance in the interlinking ties so that, under the worst fault conditions, the voltage in neighboring

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areas will not fall low enough to cause generator instability in those areas. Based on calculations and such experience as we have had to date, the amount of the line impedance necessary to control short-circuit duty appears to be enough to accomplish this. So far, there has occurred one case of bus trouble which operated the interlinking points of its load area. No generator stability troubles occurred either in the faulted load area or any of its neighbors.

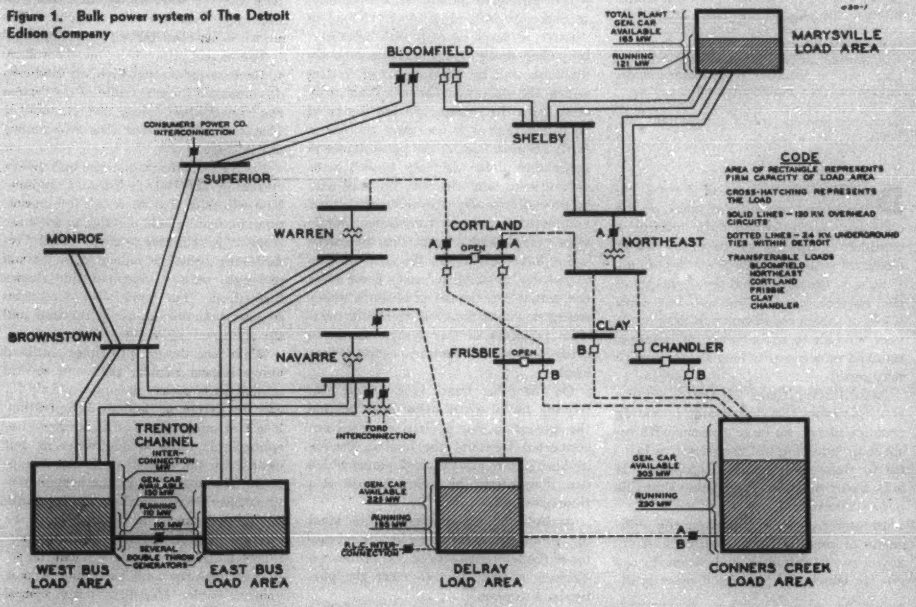
Another implication of these design principles is that the load included in any load area must be adjustable to the amount of generating capacity available to the load area. Not only must these conditions be met at peak load, but the system must sometimes be altered to meet the various generating requirement throughout the day. This may be particularly true during light load periods when the more economical plants are favored as to loading.

Further reference to figure 1 shows the bulk power system as it has been worked out to meet these conditions.

As an illustration of how load areas are adjusted, refer to the Conners Creek load area. At present, that plant is well built up with high-economy generating equipment. You will note from the position of the interlinking points in use, the black squares labeled *A*, that the load area is now widely extended. During the rebuilding period at Conners Creek, it was at one time contracted to the condition shown by the interlinking points labeled *B*.

An interesting variant of this principle exists at the Trenton Channel plant. There the plant operates in two parts, each of which serves a separate load area. It was relatively easy to make several of the generators "double throw," so to speak, and thus make them available to either load area. Thereby, we can, within limits, adjust the generating capacity to the area load, and we therefore do not need so great a proportion of transferable load to maintain the load areas on a self-sustaining basis.

Summarizing briefly, while we contemplate the complete shutdown of only one power-plant load area at a time, nevertheless each of the areas could be started simultaneously from standstill if necessary. Accordingly the problem of starting up becomes, for this system, one of the internal design of the load area.



BLOOMFIELD
MARYSVILLE LOAD AREA
TOTAL PLANT GEN. CAP. AVAILABLE 165 MW
RUNNING 121 MW
SHELBY
CONSUMERS POWER CO. INTERCONNECTION
SUPERIOR
CODE
AREA OF RECTANGLE REPRESENTS FIRM CAPACITY OF LOAD AREA
CROSS-HATCHING REPRESENTS THE LOAD
SOLID LINES – 120 KV. OVERHEAD CIRCUITS
DOTTED LINES – 24 KV. UNDERGROUND TIES WITHIN DETROIT
TRANSFERABLE LOADS
BLOOMFIELD
NORTHEAST
CORTLAND
FRISBIE
CLAY
CHANDLER
MONROE
WARREN
CORTLAND
A
OPEN
A
A
NORTHEAST
CLAY
B
CHANDLER
B
FRISBIE
OPEN
B
BROWNSTOWN
NAVARRE
FORD INTERCONNECTION
TRENTON CHANNEL
INTER-CONNECTION MW
GEN. CAP. AVAILABLE 130 MW
RUNNING 110 MW
110 MW
SEVERAL DOUBLE THROW GENERATORS
WEST BUS LOAD AREA
EAST BUS LOAD AREA
GEN. CAP. AVAILABLE 221 MW
RUNNING 195 MW
R.L.C. INTER-CONNECTION
DELRAY LOAD AREA
GEN. CAP. AVAILABLE 305 MW
RUNNING 230 MW
A
B
CONNERS CREEK LOAD AREA

Figure 1. Bulk power system of The Detroit Edison Company

Load Area Internal Design

Figure 2 shows the internal arrangement of a typical city power-plant load area, that is, where 24-kv underground transmission is used.

By a series of heavy dotted lines there is shown the physical segregation of the equipment and lines into zones extending from the generating station to the customer. Within these zones the radial distribution lines may be picked up one at a time if necessary to aid in restoring service.

Beginning with the generating plant, you will note that the generators and the tie cables to the adjacent plant areas are held together by a star-connected synchronizing bus and its reactors *S*. The synchronizing bus and reactors are carefully segregated from all other bus and switch rooms for the purpose of protecting them from the troubles arising elsewhere in the switch house. Next you will note that each set of generator switches and its "feeder gangs," as well as each set of tie line breakers *G*, is separated from the others by careful sectionalizing of the switch house. Also, the busses and all connections are completely

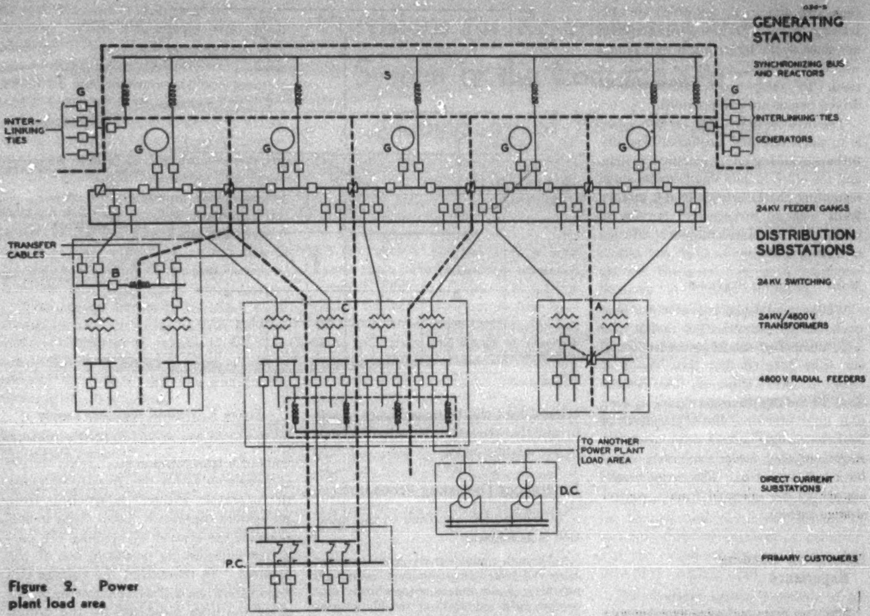


Figure 2. Power plant load area

metal clad and each phase segregated in its own metal enclosure.

In the load area are three kinds of substations. One kind (*B*) has 24-kv switching and usually serves not only as a "satellite center" for the collecting of large industrial and small substation loads, but also as a transferable load to aid in adjusting the size of the load area. Each of its bus sections is separated in a manner similar to the generating-station switch house. Substations of this kind are costly to build and to man but fortunately not many of them are required.

A second kind (*C*) is that commonly used to supply heavy industrial and commercial areas. Such a substation has no 24-kv switching. Its transformers are connected together on their 4.8-kv side by a star-connected synchronizing bus with its reactors which is carefully segregated from the other busses and equipment. The switchgear for each transformer and its "feeder gangs" is also segregated from its neighbors.

A third kind (*A*) is that having no 24-kv switching and no 4.8-kv synchronizing bus or reactors. Each transformer serves a "gang" of 4.8-kv feeders and is so arranged that, should a transformer or the 24-kv cable feeding it fail, its "gang" of feeders will be automatically thrown onto the spare transformer capacity with an outage of some two seconds.

A fourth type (D.C.) is the d-c substation, each of which is fed from at least two separate power-plant load areas and often from three, so that the loss of any power-plant load area will not cripple any d-c substation. Should there be a complete shutdown of all the power areas feeding the d-c system, the d-c load will be picked up by motor generator sets which are equipped with load-limiting apparatus for that purpose.

A fifth type is the a-c network in the downtown area. These are not shown on figure 2. We do not contemplate that any of these networks as laid out is likely to exceed 10,000-kw load. Each is normally fed at 4.8 kv from a substation in one power-plant load area and arranged to be transferred to a substation fed from another power-plant area should the first one fail. The quantities of load involved are not sufficient to present any problem in picking them up.

A sixth type is the primary customer installation (P.C.). Care is taken to see that each feed to such a customer is served from a separate section of supply substation or from separate substations.

In figure 3 there is shown the methods of serving a load area by overhead 120-kv transmission lines. Here the segregation of trouble is not quite so complete but each 120-kv bus section is separately relayed to localize trouble. Also, each 24-kv bus section is carefully segregated by steel barrier walls and double section switches and the 24-kv lines to distribution substations are spread over the bus sections in the same manner as described for the typical power-plant load area. While we have not shown them, the distribution substations are of the same type as shown in figure 2. Again, the dotted lines show the zones of segregation.

From the above description, it is evident that there is no fundamental obstacle to the starting of such a system from a complete shutdown because the load can be picked up in small enough pieces to be well within safe limits for generators and boiler plant.

Auxiliary Supply

Generating Plants

▌It is the practice of the company in each of its generating plants to provide completely independent steam-turbine-driven d-c generators to supply most of those essential auxiliaries which are electrically driven. The principal exceptions are some of the larger boiler feed pumps where system-fed a-c motor drives are used. In these cases, steam-turbine-driven pumps are also provided.

The reasons for so doing are two. One is to shield essential auxiliaries from disturbances arising on the electrical system, and the other to gain the desirable speed regulating characteristics of d-c motors. With such an arrangement, service to power-plant auxiliaries will not be affected by system shutdowns.

MAIN SWITCHING STATIONS

While no particular provision has been made for the simultaneous closing of a large number of the 24-kv feeder breakers, it is believed that three could be closed at once. However, there is little need for the simultaneous closing of even that many since the size of the pieces of load can readily be kept down to what a single outgoing feeder can safely carry for a half hour or so. The circuit breakers are all d-c operated from a control storage battery.

Emergency Loading Experience

While we have had some experience in the matter of suddenly applied overload on generators, most of that experience occurred some years ago before particular interest in that aspect of the problem had been aroused and the records and our memories are not too specific. However, there is a partial record of one case in which a group of generators were suddenly loaded from 80 per cent to about 125 per cent of rating without change of field excitation. The voltage dropped to about 80 per cent of normal and the frequency to 58 cycles, but no generator dropped its load. The short-circuit ratio of these generators probably varied from 0.9 to 1.6, the latter being for the older vertical Curtis turbine then in use. The record contains no statement as to the change in character of the load with that change of voltage and frequency. This case is cited for what it may be worth, recognizing its limitations.

No field tests have been made to determine the change in system load with variation of the voltage or frequency.

The proportion of the load of an area which will immediately be picked up on restoration of service will, of course, vary with the character of the area load. For The Detroit Edison Company system, which is heavily industrial, experience has shown that 40 to 60 per cent of the original load will be immediately picked up and that the remainder will come back in five minutes or so.

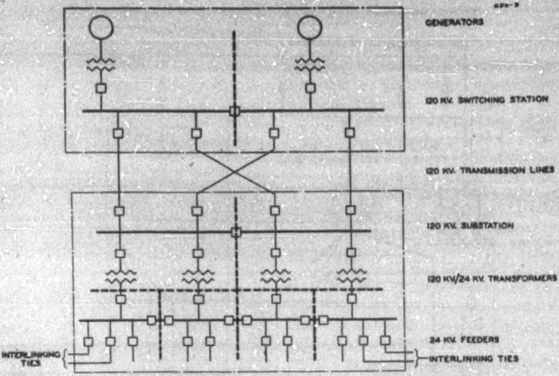

GENERATORS
120 KV. SWITCHING STATION
120 KV. TRANSMISSION LINES
120 KV. SUBSTATION
120 KV/24 KV. TRANSFORMERS
24 KV. FEEDERS
INTERLINKING TIES
INTERLINKING TIES

Figure 3. 120-kv load-area supply

Emergency Operating Procedure

In general, the approach to this question is as follows:

1. Through equipment segregation and by relay and switchgear equipment, means are provided to disconnect automatically and confine most any form of trouble we can foresee.

2. If, for any reason, the several lines of defense provided by the above shall have successively failed and the fault becomes uncontrollable, it is desirable that the operators *immediately* supplement the protective equipment by whatever action is necessary to localize and clear the fault, bearing in mind that the first concern is for the safety of the system as a whole and thereafter for the particular load area or substation, as the case may be. To guide them in so doing, standard emergency switching instructions have been developed. These instructions are designed to be carried out on the initiative of the power plant and substation operators without recourse to the system supervisors wherever that is practicable—and it usually is.

3. If, despite, the automatic protective gear and the action of operators, the system or any of its load areas goes completely dead, the central system supervisors will take control, since the moves necessary to restore service will probably extend outside the ken of any one group of operators.

Because the action necessary to restore service may vary so widely and because of the broad experience and good judgment of the central system supervisors, we have not so far attempted any specific emergency instructions for their guidance.

Perhaps the best way to illustrate the effect of this approach is to outline the procedure followed in emergencies.

When the equivalent of lamp voltage falls to 80 volts at a power plant or 70 volts at a transmission step-down station and remains so for one minute or more, the operator, without instruction from the system supervisor, takes steps to sectionalize the system by opening the particular interlinking points in use at the moment. If, thereafter, the voltage still remains low, he will sectionalize his busses along the lines indicated by the dotted lines in figures 2 or 3, as the case may be, beginning with whichever section he thinks most likely to be in trouble. As soon as the faulted section is cleared, the operator calls the system supervisor and reports the situation.

It is interesting to note that the index upon which the necessity for emergency switching turns is the voltmeter reading. It has been found from experience that, under fault conditions, the behavior of current-indicating devices is often so erratic as to make them undependable and that persisting low voltage is about the only reliable criterion.

Suppose, now, that the trouble has been so serious that an entire plant and its load area is down and that the situation has been referred to the central system supervisors. The first step would probably be to survey the switchgear and busses to determine what bus sections were in condition to go back into service. Following this, all outgoing lines would probably be opened. Thereafter, the running generators would be brought up to speed, one at a time, connected to a considerable section of bus, and loaded by closing the outgoing feeders one at a time. The generators would be synchronized with the synchronizing bus if it were in commission. If not, they would be synchronized to their neighboring generators. When everything was again running, if the synchronizing bus were out of commission, the generator busses might be sectionalized into two parts, if need be, to minimize short-circuit duty.

There is only one precaution to be observed in closing the outgoing feeders and that applies to the feeders running to the industrial substations (*C* in figure 2). These feeders, being tied together through a 4.8-kv synchronizing bus, the system supervisor might find it necessary to open these synchronizing bus ties to prevent overloading of the first feeders to be closed in. This can readily be done.

With respect to the boiler plant, there should be little difficulty because the load will not be picked up in large pieces. An ample supply of make-up water is held in reserve to replace any loss due to the blowing of safety valves.

It is not necessary to alter any relay settings, either at the power plants or at the substations.

To guard against a failure of communication facilities, the company has built up a private telephone system connecting the system supervisors with each generating plant and each principal substation. This is supplemented by Bell System phones. In the case of the Trenton Channel and Marysville power plants, which are more remote from the system supervisors, there is also provided carrier-current communication. There is, in addition, radio telegraph to the Marysville plant. Very rarely have we had to use either the carrier-current or the radiotelegraphic communication. The chance of a simultaneous failure of these communication facilities is very small but, if that contingency did arise and the operators could not communicate with the central system supervisors, they are instructed to proceed to restore service within their own area or substation on their own initiative.

While the power plant and substation operators are required to become very familiar with operating instructions affecting restoration of service, no specific rehearsals have been conducted. It is felt that any of the instructions are so much in line with the steps which would normally be taken by an experienced operator as to preclude the necessity of staged rehearsals.

Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Provisions for Re-energizing the Electric System of the Consolidated Edison Company of New York, Inc.

G. R. MILNE
MEMBER AIEE

H. C. OTTEN
MEMBER AIEE

In the course of the last three or four years there have been a few instances in which generating stations supplying electric service in large metropolitan systems have shut down temporarily because of storms and floods of unusual severity or because of accidents resulting in fires which could not be quickly brought under control. These occurrences, while fortunately very infrequent, have focussed attention upon the problem of starting up a large system in the event of a complete shutdown of one or more generating stations supplying it. It is therefore the purpose of this paper to present:

1. Some of the more important considerations affecting the design of the distribution system and the generating stations in the Consolidated Edison system with respect to the ability to restore service after a complete shutdown.

2. The principal features of the operations involved in re-energizing the electric system, in accordance with a predetermined plan.

3. A review of some operating experiences related to the problem of re-energizing the system.

Description of the System

The Consolidated Edison system furnishes electric service to all of Greater New York City, with the exception of the borough of Richmond and a small portion of the borough of Queens. It also supplies nearly all of Westchester County.

Approximately 2,400,000 consumers are served in an area of about 700 square miles having a total population of nearly 8,000,000.

The electrical supply is derived principally from 11 steam generating stations located within the territory served and having a total installed capacity of 2,556,000 kilowatts. About 70 per cent of the generating capacity operates at 60 cycles and the remainder at 25 cycles.

All of the generating stations are interconnected by direct tie feeders for each of the two frequencies and by five frequency changers having a combined capacity of 190,000 kilowatts. This system normally operates in parallel with the system of the Niagara-Hudson Corporation through a 60-cycle connection of 200,000-kva capacity and with the system of the Brooklyn-Manhattan Transit Corporation through a 25-cycle connection of 40,000-kva capacity.

Figure 1 is an outline map of the territory served showing the location of each generating station.

Figure 2 is a single-line diagram showing the connections between generating stations, their net generating capacity, and the estimated district loads at the time of the peak in 1939.

The 60-cycle system supplies, in all parts of the territory served, residential, general commercial, and industrial loads as well as the entire load of the municipal rapid-transit system. The 60-cycle load at the time of the system peak represents about 70 per cent of the total load.

The 25-cycle system supplies a large d-c traction load which includes the terminal divisions of all of the trunk-line railroads entering New York City; a large d-c load for residential and commercial use, mostly in the borough of Manhattan; a small amount of a-c commercial load, and some single-phase a-c traction load.

The total system load at the time of the peak in 1939 was 1,704,000 kilowatts. The energy distributed during that year was slightly over $7^{1}/_{4}$ billion kilowatthours.

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Fundamental Considerations Affecting the Design of the Distribution System and the Generating Stations

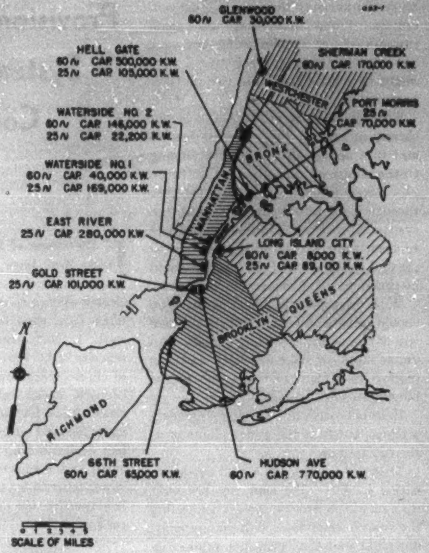


Figure 1. Territory supplied by the Consolidated Edison system and the locations of the generating stations

Electric distribution systems and their sources of supply are designed so as to provide electric service of the highest quality that the density and character of the load will justify. Probably the most important measure of this quality is that the service shall be substantially free from interruption. However, equally important from a system standpoint, is the ability to restore service quickly after an interruption may occur and with little or no damage to equipment during the shutting down or re-energizing periods. In suburban and rural areas the radial type of electric distribution provides service which is rarely interrupted and at a cost sufficiently low so as to be commercially practical. With such a system the load is divided into units which are small in relation to the capacity of the sources of supply and the switching of these units is directly under the control of the local operating personnel. The procedure to be followed in restoring service to a radial system consists of reconnecting to the substations and generating source only such load as those stations and the associated feeders can safely carry. Furthermore, the radial distribution feeders are practically self-protecting in that the feeders and associated load are simultaneously interrupted and therefore do not suffer damage during system shutdown.

To provide the high degree of service continuity required in metropolitan areas, the multiple-feed low-voltage network was developed. This form of distribution has been used continuously since the establishment of d-c service from the Edison system in New York in 1882. The development of the a-c network protector led to the application of the same principle to a-c distribution in Manhattan in 1922. Operating experience including the operating record of cable limiters has demonstrated the high degree of reliability of the low-voltage network and that the over-all service reliability is dependent almost entirely on the reliability of the sources which supply the network. It is not economical or reasonable to provide for the maintenance of continuous service throughout every conceivable contingency condition nor for contingencies having a frequency probability so low that they can be disregarded. Furthermore, not every contingency of nature can be foreseen. It is essential therefore that means be provided for re-energizing individual networks or a system comprising a number of networks.

The system plan adopted by the Consolidated Edison Company contemplates that the generating stations will be interconnected normally by a system of direct tie feeders and frequency changers only and that the total distribution load will be segregated into blocks, which, even at peak-load conditions, are small by comparison with the capacity of the interconnected stations. The existing area segregations are shown in figure 3.

While network areas should be small for facility in restarting after shutdown they should not be made too small as the distribution layout thereby becomes relatively expensive. For a system as extensive as that in New York, we believe that each a-c network should be supplied by from 8 to 12 high-voltage feeders. In order to load these feeders, consisting of three-conductor 800,000-circular-mil cable at 13 kv and three-conductor 500,000-circular-mil cable at 27 kv to reasonable values, it follows that networks supplied by feeders at the lower voltage should range in capacity from 50 to 75 megawatts, and from 75 to 150 megawatts if supplied at the higher distribution voltage. Not all of the areas supplied from a given station need be kept to the minimum values as a station should be able to restart progressively larger units as re-energizing proceeds. If some areas are too large they cannot be selected for initial restarting. In the absence of extensive experience in restarting networks, the above values of area load ranging from five to ten per cent of the system peak load are largely based on opinion but are believed to be conservative. Moreover the limitation of an a-c network to a size appropriate for supply derived from 12 feeders makes it necessary to operate simultaneously only a moderate number of circuit breakers at the instant of restarting.

When the load in a segregated a-c network approaches the above values, the network is subdivided and in some instances regrouped to form other segregated networks whose physical boundaries and primary feeder rearrangements have been previously planned and partially installed in advance of the load growth.

Each primary feeder to a segregated a-c network should supply load in only that one area. If this requirement is not observed, it would be impossible to energize one network without energizing equipment in other networks at the same time. This would either mean starting a number of networks simultaneously; damage to equipment in other networks not being energized; or delaying the starting up until the slow process of blocking open protectors in conflicting areas can be completed. A network to be suitable for independent restarting must be segregated both primary and secondary from other networks.

All of the primary feeders to a given a-c network area should, in our opinion, derive their supply from the electrical galleries of one generating station. During unusual operations such as the restoration of service after shutdown, such an arrangement minimizes the amount of operating co-ordination required and thereby simplifies the operating rules covering the re-energizing procedure. Furthermore, in the event that communication between stations or the direct tie feeders interconnecting them are interrupted such a plan enables a station to operate as an independent unit usually able to supply the load of the area it serves.

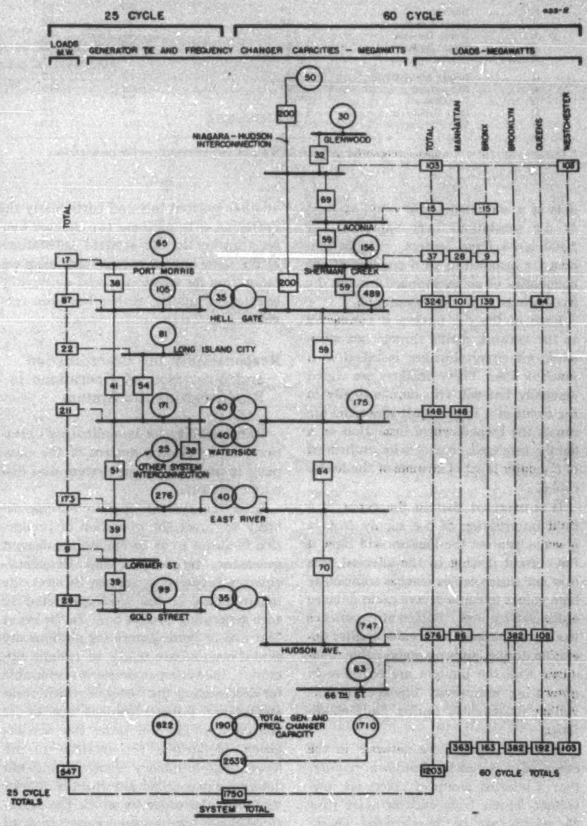
Since the 60-cycle load in an entire area is to be supplied from one station under this plan it follows that electrical and mechanical segregation should be provided to such a degree as to protect the continuity of service during reasonable contingencies. Thus the main bus should be divided into a number of major sections, each separated by gas-tight fire walls so that trouble in one section will not physically communicate to another. Electrical ties between major bus sections which are normally alive should connect through the fire walls by indirect paths such as a synchronizing bus and there should be a minimum of two sets of independent switching and protective gear between a section on which a fault is assumed to occur and other sections. One set of protective gear between a common synchronizing bus and each load bus would seem sufficient on the assumption that not more than one set of protective gear will fail to function properly during a given fault.

The primary feeders to each a-c network should be divided among the major sections and sufficient network transformer and mains cable capacity should be installed so that in emergency the peak load can be carried or re-energized after shutdown with one major bus section out of service.

The number of major segregations provided at the station should be such that the lowest over-all system costs result. The less the number of major sections provided in the station, the greater is the percentage of reserve equipment capacity on the network system required for operation with one section out. Experience indicates that a station can be economically limited to two major sections only when the a-c networks supplied from it are small and in the early stages of development. With networks of moderate or large size at least three and probably four major sections are economical.

Figure 2. Principal system tie connections and sources of supply for district loads

Generator ratings are net


25 CYCLE
60 CYCLE
LOADS M.W.
GENERATOR TIE AND FREQUENCY CHANGER CAPACITIES – MEGAWATTS
LOADS-MEGAWATTS
TOTAL
MANHATTAN
BRONX
BROOKLYN
QUEENS
WESTCHESTER
NIAGARA-HUDSON INTERCONNECTION
GLENWOOD
LACONIA
SHERMAN CREEK
PORT MORRIS
HELL GATE
LONG ISLAND CITY
WATERSIDE
OTHER SYSTEM INTERCONNECTION
EAST RIVER
LORIMER ST.
GOLD STREET
HUDSON AVE.
66 TH ST
TOTAL GEN. AND FREQ. CHANGER CAPACITY
25 CYCLE TOTALS
60 CYCLE TOTALS
SYSTEM TOTAL
50
200
30
32
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39
70
28
99
35
747
576
86
382
108
63
822
190
1710
2532
363
163
382
192
103
547
1203
1750

Each major section may be divided into two or more subsections further to promote reliability in which case differential protection or its equivalent should be provided on each section.

The d-c control supply and the control wiring in the 60-cycle generating stations must be adequate to permit the simultaneous closing of the requisite number of circuit breakers for restarting a network. The relaying must be such that the feeders will not trip out at the moment of re-energizing due to the current surge.

In the foregoing paragraphs the more significant design features of the a-c network distribution system in New York have been mentioned. While many of these features are considered applicable to d-c network distribution, the development of the much older d-c system progressed along different lines.

D-c network distribution is confined to an area of about 19 square miles in the borough of Manhattan. There is also a very small amount of d-c load in Brooklyn. The Manhattan d-c network is not divided into segregated areas but con-

Table I. Network and Radial Loads in Segregated 60-Cycle Load Areas Shown in Figure 3

All Load Values Are Estimated for the Time of the Peak in 1939

Area Reference Letter	Area Name	Radial Load Excluding Railroads (Megawatts)	Radial Railroad Load (Megawatts)	Network Load (Megawatts)	Total Load (Megawatts)
A	Northeast Bronx	11*	1	3	15
B	Washington Heights	6*	8	23	37
C	Southeast Bronx	5	1	7	13
D	Central Bronx	12*	0	29	41
E	West Bronx	16*	10	59	85
F	Harlem	1*	6	32	39
G	Central Park	5*	7	50	62
J	North Queens	15	8	61	84
H	Times Square	2	12	73	87
K	Madison Square	1	6	54	61
L	Lower Manhattan	2	11	73	86
M	Ridgewood	22*	16	102	140
N	Jamaica	16	2	37	55
P	East Brooklyn	48*	2	66	116
R	West Brooklyn	60*	17	102	179

* Denotes radial load of which a substantial part can be restored independently of the network load.

sists of a continuous grid supplied from 41 d-c substations over approximately 2,400 low-voltage feeders. Cable limiters are installed in all d-c mains at the boundaries of zones supplied by the d-c feeders from adjacent substations.

Each of the d-c feeders is connected to the network mains through an automatic air circuit breaker installed in a junction box. These breakers are closed manually but will trip automatically in the event of a feeder fault when the arc causes the breakdown of insulation on a lightly insulated control wire embedded in the outer layer of strands of the feeder cable.

It is expected that in the event of a total interruption of d-c supply from a given substation the limiters will blow if the current flowing to the affected area over the mains cables reaches sufficiently high values to cause severe cable damage unless interrupted. Sufficient experience has been obtained with cable-limiter operation during faults on mains cable to indicate that the limiters are effective in preventing widespread damage to low-voltage mains cable during fault conditions.

Re-energizing the d-c network in the event of a complete shutdown requires that a selected group of about six low-voltage feeders from each of more than 25 substations be re-energized simultaneously and that the remaining d-c feeders from all 41 substations be cut in as rapidly thereafter as practicable. The operations required for starting the large number of converters upon restoration of 25-cycle supply; connection of these converters to an isolated d-c bus in each substation; connection of the dead feeders selected for initial re-energizing to another isolated bus and particularly the operation of tying these two isolated busses together in the selected substations at the same instant require very close co-ordination for which a special emergency telegraph signaling system has been provided.

Responsibility for Co-ordination and Execution of Operations in Re-energizing the System

Responsibility for co-ordinating operations in the electric system of the company is assigned to the system and district operators.

It is the system operator's responsibility to direct the operation of production facilities so as to maintain sufficient generator, tie feeder, and frequency-changer capacity in service to meet the requirements of the load connected to each generating-station bus. In the event that one or more generating stations are shut down and are ready to resume operation, the system operator is responsible for determining the order in which these stations or reserve stations are to be started up. He will order the re-energizing of busses, reconnection of tie feeders and frequency changers, and will determine in conjunction with the district operators the order in which the segregated loads are to be re-energized to utilize capacity to the best advantage as it is made available.

The district operators in each of the three districts established for convenience in administration are responsible for the direction of distribution operations in their respective districts. It is their responsibility to direct the switching operations in the field and in the stations which are required to re-energize each load area as capacity is made available after shutdown.

The generating station operators and substation operators are responsible for disconnecting all feeders, generators, and frequency changers in the event of the complete shutdown of a given station. They also are required to perform, on their own initiative, specified preliminary operations to prepare for re-energizing the system.

While it is expected that in any emergency involving the shutdown of one or more generating stations, the operations performed in restoring service would be co-ordinated by the system operator and district operators in the manner previously described, it is recognized that a concurrent failure of communication facilities might make it imperative for the switchboard operator at the generating station to assume the initiative in restoring part or all of the load as soon as the station is prepared to do so. The distribution system is designed to facilitate re-energizing in this manner. Several proposals are being investigated in an effort to develop a standard procedure which the individual generating station operators could follow in the event that communication is interrupted and it is necessary to restore the direct tie feeders in order to obtain assistance from an adjacent station when re-energizing blocks of network load.

Written instructions covering the procedure to be followed by the generating station and substation operating personnel in the event of a complete shutdown of any station are revised and reissued as often as required by changes in system facilities.

In determining the form and contents of the instructions simplicity and conciseness were emphasized so that they can be used for quick reference when necessary, and to encourage frequent review by the individuals concerned and thereby to promote their familiarity with prescribed procedure. It is recognized that it is impractical to prepare, in advance, complete and specific instructions to apply to a very large number of assumed emergency conditions. The instructions for re-energizing the system are therefore written to cover selected cases including:

1. The shutdown of each 60-cycle generating station separately.
2. The shutdown of all 60-cycle generating stations.
3. The shutdown of the entire d-c network in Manhattan and the associated 25-cycle system.

Procedure for Re-energizing the System

It is assumed, for the purpose of this discussion, that the entire electrical system of the Consolidated Edison Company has been shut down, that the generating stations are in a cold condition, that the electrical ties with other utility systems such as the Niagara Hudson Corporation and the Brooklyn-Manhattan Transit Corporation are open, that all distribution feeder load has been disconnected and that all generators, frequency changers, and tie feeders have been disconnected. This represents the most difficult condition to be met from the standpoint of complexity of the operations required to start up the system.

In the extreme condition where no power is assumed to be available from other utility systems it would be necessary to establish a supply of electrical energy sufficient to meet the initial requirements of electrically driven auxiliaries. If the tie feeders between generating stations are available auxiliary power can be made available to all stations by starting one main 60-cycle or 25-cycle unit in any one of several stations. The Hell Gate and Hudson Avenue stations have house turbine-generator sets which can be started on steam generated by natural draft in the stoker-fired boilers. These generators can provide sufficient auxiliary power for operation of at least two of the main turbogenerators ranging in size from 35,000 kw to 50,000 kw in either station. If a 25-cycle turbogenerator in a station having steam-driven auxiliaries is selected for initial starting, it can be used to energize the 25-cycle busses in all stations and the 35,000-kw frequency changer at Hell Gate station may be started and used to energize the 60-cycle busses in all stations.

With the main 60-cycle busses and 25-cycle busses energized and a supply of auxiliary power available in all stations the next step would be to start and synchronize the remaining frequency changers so as to complete the restoration of all interconnections among the generating stations in the Consolidated Edison system.

In order to provide load for the turbogenerators as they are successively started and synchronized and to build up the fires in the boilers it would be necessary at this point to begin re-energizing 25-cycle and 60-cycle feeders supplying radial load only. This would permit adding load in convenient amounts and would fully restore the large traction load supplied from the 25-cycle system, restore all of the 60-cycle load in Westchester County, and partially restore 60-cycle radial load in the outlying areas in the boroughs of Bronx and Brooklyn. At the time of the system peak in 1939 the total amount of radial load thus restored would be approximately 260,000 kilowatts or about 15 per cent of the total system load.

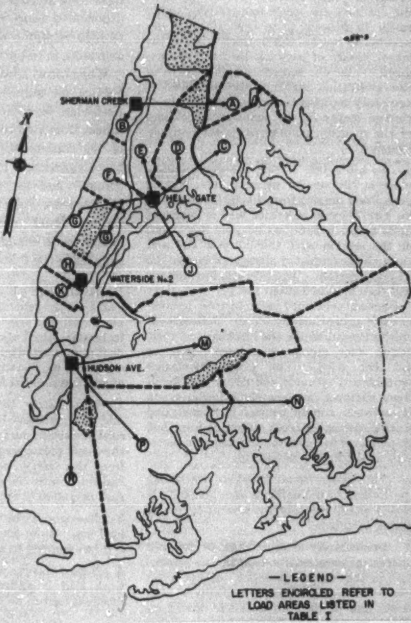

Figure 3. Segregated 60-cycle load areas in New York City

With this amount of load divided among the generating stations it would be possible to restore the 60-cycle networks to service, beginning with the areas having the smallest load and finishing with the largest networks, in each case using the method of restoration to be described later in this paper.

After all of the 60-cycle networks have been restored it would be possible to restore additional 60-cycle radial load amounting to over 200,000 kw which is supplied from feeders which also supply the network load or which is dependent upon the re-energizing of the network load itself. An important 60-cycle load is that of the municipal rapid-transit system in which most of the conversion units are rectifiers and require a supply of auxiliary power from the network before the traction load can be restored. Normal train movements are dependent upon supply from the network for track signals.

The restoration of substantially normal supply to the 60-cycle radial and network loads and to the 25-cycle radial load would build up the total load on the interconnected generating stations to the point where the boiler steaming rates should be high enough to permit the addition of the large block of load represented by the d-c network in Manhattan as the final step in restoring normal service. At the time of the system peak the load on this network is about 375,000 kilowatts and represents about 28 per cent of the total load in the remainder of the system.

The general method of re-energizing each of the 60-cycle networks may be summarized briefly as follows:

1. After the generating station busses have been re-energized, the direct tie feeders between generating stations have been restored, and the frequency changers synchronized, at least one turbogenerator is synchronized to the bus in each station.

2. While additional generators are being started, small blocks of radial 25- and 60-cycle load are connected successively to the busses in each station by re-energizing individual radial feeders as rapidly as consistent with the increase in capacity as the available boiler output is increased. This process is continued until all of the available load has been re-energized except that which would require special switching operations in the field to separate it from network load in the case of feeders supplying both kinds of load.

3. The overcurrent protective relays on all of the 60-cycle tie feeders between generating stations are made inoperative temporarily to prevent them from tripping unnecessarily on the surges which occur as the larger blocks of network load are re-energized. The d-c control supply feeders in the generating stations are paralleled if necessary to insure an adequate supply of control energy for the simultaneous closure of the requisite number of network supply feeders.

4. In a 60-cycle station supplying one or more of the smaller network areas having peak loads ranging from 40,000 kw to 80,000 kw the 60-cycle generator load is increased until as much power as possible is sent out of the station over the 60-cycle ties and through the frequency changer, if any, to the 25-cycle system. The 60-cycle bus voltage will then be raised about five per cent above the normal scheduled value in order to compensate approximately for the increased excitation required for the block of load to be added since automatic voltage regulators are not used. The generating-station switchboard operator will notify the boiler-room engineer immediately before a group of network supply feeders is re-energized so that the boiler room operating force will be prepared to make any adjustments necessary as the load is picked up.

5. The feeder circuit breakers controlling the feeders to the network area selected are closed simultaneously by several operators acting in unison.

6. Immediately after a block of network load is re-energized the load on the generators in the station at which the operation was performed is adjusted manually so that about half of the load added is carried on these generators and the remainder is carried on the generators in other parts of the system through the ties and frequency changers. The bus voltage is adjusted to the normal scheduled value after a block of network load is added unless the same station is to prepare for the immediate addition of another block of load.

This method of re-energizing a typical block of network load is based upon the assumption that the entire Consolidated Edison electric system has been shut down and is to be re-energized with all stations and substantially all parts of the distribution system intact and available for service. In any actual case of shutdown it is unlikely that more than one station will be seriously affected. It is possible, however, that the affected station will have to be restored to service and its load re-energized while one or more of the bus sections are damaged, or while one or more of the main turbogenerators is unavailable for service. All such situations present special problems which require slight changes in the general plan presented.

One of the most important deviations from the general plan would be encountered in the case where a generating station having adequate generating capacity available to carry the normal load might have to be started from a shut-down condition at a time when the 60-cycle ties to other stations were not available for assistance in re-energizing the networks.

While this is a rather remote possibility because the number of separate 60-cycle tie feeders to adjacent stations provided ranges from two to nine and all but one of the stations supplying network load has a connection to the 25-cycle system through one or two large frequency changers we believe that the isolated station would have no particular difficulty in re-energizing the networks it supplies in the following manner:

1. Re-energize all of the available radial load as in the general case and, if necessary, block open the relatively few network protectors at distribution banks connected to dual-purpose feeders which can then be re-energized to restore substantial amounts of additional radial load.

2. Immediately before re-energizing the first and smallest block of network load raise the steam output from the boilers and the steam pressure until steam is discharged from the safety valves. The supply of make-up water is normally adequate for such operation in any of the stations.

3. Re-energize the first block of network load and restore steam pressure to normal if it has dropped to a point below normal.

4. Repeat this process as each block of network load is added, leaving the largest blocks of load to be re-energized last.

Under some conditions it would be feasible to restore service to all or nearly all of the 60-cycle load supplied from a generating station while all of the generators were temporarily unavailable for service because of some condition affecting all of the essential auxiliaries. Such a condition occurred in one instance under circumstances outlined below.

Operating Experiences

The only actual experience obtained in re-energizing segregated 60-cycle load areas in the Consolidated Edison system was on the occasion of the shutdown of the Hell Gate generating station on September 21, 1938, shortly after the work was completed in segregating the load areas supplied by this station. This shutdown was the result of a tidal flood accompanying a hurricane which passed close to New York City on that date. The re-energizing of the areas affected was successfully carried out by following as closely as practicable the methods outlined above. The salient features of this experience were:

1. The shutdown of all 25-cycle and 60-cycle generators in the station was forced by loss of steam pressure following the partial or complete submersion of nearly all of the essential auxiliaries and the complete interruption of the power supply for the auxiliaries which are electrically driven.

2. The 11-kv 25-cycle busses, 13-kv 60-cycle busses, and the associated switchgear were not damaged although all supply to the 60-cycle busses was interrupted because of overloads on the tie feeders and the frequency changer.

3. The shutdown occurred about $1^{3}/_{4}$ hours after the time of the system load peak for the day at which time the system load normally is declining at a rate of approximately ten per cent per hour.

4. All 60-cycle service in the north half of the borough of Manhattan and all service in the borough of Bronx and county of Westchester were interrupted. This load amounted to about 260,000 kilowatts.

5. About one-quarter of the interrupted load was restored within six minutes. Service in the remainder of the affected area was restored in several stages over a period of $5^{3}/_{4}$ hours. Since none of the generators at Hell Gate station was available for operation for several days after the shutdown, 60-cycle power obtained principally from the Niagara-Hudson system was utilized in restoring service.

6. No difficulty was experienced in re-energizing the networks. The largest block of load re-energized in one operation was 90,000 kilowatts which included all of the load in Westchester County and a small section in New York City. This load was re-energized six minutes after it was interrupted and the load picked up was about equal to the load dropped. The next largest block of load was that of one of the networks in Manhattan. This network was re-energized 41 minutes after the interruption occurred and increased the load on the Niagara-Hudson tie by 30,000 kilowatts which was almost exactly the amount of load in the area at the time of the interruption. A few minutes later another of the Manhattan networks was re-energized but the load increment was only 28,000 kilowatts, about 30 per cent less than the load in that area at the time of the interruption. This reduction of load was partly accounted for by the fact that the service voltage in the areas restored was from 8 to 18 per cent below normal immediately after the second block of network load was added. Seven minutes later, after excitation had been adjusted in the Niagara-Hudson system, the service voltage in the re-energized areas was sufficiently improved to permit re-energizing a third block of network load in New York City with the same effect on load and volt-

age as before. The remaining networks were re-energized shortly thereafter with increased 60-cycle power supply available from other generating stations in New York City over tie feeders which could not be restored earlier because of a system ground fault condition requiring several hours to localize and eliminate.

7. It was found entirely practical to close from 6 to 11 feeder breakers at the generating station in such a manner as to re-energize each network without causing network protector fuses to blow because of temporary local overloads. In the case where 11 breakers had to be closed they were closed in two groups of 6 and 5 respectively by several operators working together.

On the occasion of the shutdown of Hell Gate station the circumstances were such that no experience was obtained in re-energizing large blocks of load with power supplied principally by the generators in the station affected.

However, an indication of the probable effect of re-energizing a large block of load from a given generating station, under conditions where the interconnections among all of the generating stations are operating normally, may be obtained from actual experience in several cases where automatic tripping of transmission-line breakers in the Niagara-Hudson system have resulted in instantaneous increases of 100 to 150 megawatts in the load on the Consolidated Edison system.

In all cases the increased load has been picked up by the interconnected stations in New York with slight effect on the system frequency and voltage.

The largest of these instantaneously applied loads on record occurred at a time when the Consolidated Edison system was receiving 150 megawatts from the Niagara-Hudson system and a bus fault in a substation located near the boundary between the two system territories caused the transmission lines to open at that point. At that time the total generator load in the Consolidated Edison system was 789 megawatts of which about 60 per cent was 60-cycle generation. The instantaneous load increase of 150 megawatts was imposed upon the system through connections to the 60-cycle busses in the Hell Gate and Sherman Creek stations but was shared by all of the interconnected stations with a momentary decrease in frequency of one-half cycle and a voltage dip of from $2^1/_2$ to 9 per cent in various parts of the system. The system frequency was restored to normal within three minutes. Unfortunately there is no complete record of the magnitude of the load increase at each of the generating stations. None of the tie feeders or frequency changers tripped out.

In this case the sudden increment of load represented 19 per cent of the previous load on all generators, 32 per cent of the load on the 60-cycle generators alone, 11 per cent of the total running generator capacity, 79 per cent of the running frequency-changer capacity, and approximately 11 per cent of the total net hot boiler capacity.

The boiler equipment in these stations includes a wide variety of types among which are stoker-fired boilers with and without automatic combustion control and pulverized-fuel installations of the unit-mill type and of the bin type.

The operating experiences mentioned indicate that no particular difficulty should be encountered in re-energizing the largest blocks of network load contemplated under the present design objectives provided that substantially normal tie-feeder and frequency-changer capacity is available for service at the time.

Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

The Co-ordination of Electrical Equipment With Basic Insulation Levels

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Basic insulation levels have been defined, and a list of proposed values has been determined by national committees charged with this responsibility. The purpose of this paper is to discuss some of the principles useful in selecting electrical equipment conforming to the requirements of that basic insulation level necessary to assure satisfactory service while operating within the voltage range and under the system conditions expected.

Since the lowest-allowable basic insulation level is closely associated with the protected level provided by the surge-limiting or surge protective equipment, and since the rating of protective equipment selected is closely associated with values of system operating voltage to ground, it is evident that there exist interrelations among:

A. Voltages expected to exist on the electrical system

(*a*). Low frequency or dynamic

(*b*). Surge

B. Voltages permitted by surge protective devices

C. Basic insulation levels

D. Insulation strengths of electrical equipment

The latest proposed values for basic insulation levels are being reported to the Institute by representatives of the national committees having these matters in hand.[1] The performance of lightning-surge protective devices and other related matters also are being presented in associated papers before the Institute.[2,3]

Operating Requirements of Apparatus Insulation

It is of interest to examine the relations existing between required insulation strengths and service conditions on operating systems. Apparatus must be designed to withstand satisfactorily a variety of voltages imposed upon it. Such voltages include:

A. Continuously applied low-frequency voltage

(*a*). Between terminals

(*b*). Between live parts and ground

B. Occasionally applied low-frequency overvoltages

(*a*). Between terminals

(*b*). Between live parts and ground

C. Occasionally applied inpulse overvoltages between live parts and ground

The basic insulation level as now de-

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[1] For all numbered references, see list at end of paper.

fined directly refers only to the last of these varieties. A study of the relationships which should exist between this characteristic and the others is essential to the successful co-ordination of the design characteristics of electrical equipment with basic insulation levels. In the past, it has been common practice to relate insulation strengths to the rated continuous low-frequency voltage *between terminals* for which the apparatus was designed. However, a study of the considerations enumerated above leads to the conclusion that insulation strengths more properly should be related both to (*a*) the surge voltages, and (*b*) the continuous dynamic voltages expected to exist *between live parts and ground*.

A fundamental requirement of apparatus insulation is that its strength between live parts and ground shall be at least equal to the maximum voltage expected to occur between live parts and ground. In the case of impulse surges this maximum value may be considered to be the "protected level" provided by the associated protective apparatus. Therefore, that basic insulation level which is equal to or in excess of the established "protected level" would be the minimum insulation level applicable to the apparatus in question.

As discussed rather completely in a former paper,[4] the protected level must be placed sufficiently above the expected performance of the protective device to provide a reasonable margin which will insure the integrity of the protected level. This margin must take into account many factors, including manufacturing tolerances, layout and arrangement of the station, vagaries of surges and protective equipment, assurance of selective operation of the protective device, etc.

Basic Insulation Levels

The present accepted definition of "basic insulation levels" is as follows:

> "Basic impulse insulation levels are reference levels expressed in impulse crest voltage with a standard wave not longer than a $1^1/_2$x40-microsecond wave. Apparatus insulation as demonstrated by suitable tests shall be equal to or greater than the basic insulation level."

For many years, insulation levels have been defined, perhaps inadequately, in terms of test voltages impressed for a period of time at low frequencies. The basic insulation levels under discussion, however, are defined in terms of impulse voltages with no mention of low-frequency or sustained-voltage strengths. The definition contemplates that apparatus shall withstand successfully the imposition of surges having values as great as the basic insulation level for which the apparatus was designed.

Table I. Typical 4,160-Volt Wye Substation

Insulation Design Basis: Basic Insulation Level 60 on Bus and Important Apparatus; 45 Elsewhere

Item	Apparatus Insulation Classification* (Kv)	Voltage Rating* (Kv)	Withstand Voltage	
			Low-Frequency Test (Kv RMS)	Impulse Full-Wave Test (Kv Crest)
Surge protective equipment:				
Lightning arrester		3		
Supply transformers:				
(Over 500 kva)	5	2.4/4.16 wye	19	75
Station power transformers:				
(Less than 500 kva)	5	2.4/4.16 wye	19	60
Regulators:				
(Under 750 kva, three phase)	5	4.16	19	60
Instrument transformers:				
Current, indoor on bus	8.7	8.66	19	60
Potential, indoor on bus (ratio 20 to 1)	8.7	2.4	19	60
Current, on feeder	5	5	15	50
Potential, on feeder	5	5	15	50
Oil circuit breakers:				
Indoor (below 500,000 kva)	7.5	7.5	19	60
Bus and switch insulators		7.5†		85**
Bushings:				
Outdoor	5		24 wet	75
Indoor	7.5		25 dry	60
Synchronous condenser	7.5	4.16	16	

* This designation is that at present used by manufacturers.

† Lowest standard rating.

** Flashover rating. Impulse withstand test not yet determined.

The basic-impulse-insulation-level values most recently proposed, as given in the joint committee report, "Basic Insulation Levels"[1] by Messrs. Powel and Sporn, are as follows:

Proposed Basic Insulation Levels, Impulse Crest Kilovolts

30	150	650
45	200	750
60	250	900
75	350	1,050
95	450	1,300
110	550	1,550

This list of values constitutes a schedule of insulation impulse strengths to ground which may be made available in apparatus of voltage ratings as required by the trade. It is of interest to examine the relationships existing between service conditions on operating systems and the corresponding insulation levels required for apparatus.

Procedure for Selection of a Particular Basic Insulation Level

Since the selection of basic insulation levels is closely associated with the performance of the protective devices which in turn are selected on the basis of maximum dynamic voltages expected to exist at the point of application, the first consideration in selecting a basic insulation level is to determine the maximum operating voltages between conductors. Next, the system connections and the neutral stability must be considered in order to determine the maximum dynamic voltages between conductors and ground under fault conditions. These values lead immediately to the selection of the voltage rating of the protective device, since the low-frequency characteristics of this device must be co-ordinated with the voltages expected to exist between lines and ground. During line-to-ground faults, the voltage on the faulted phase decreases and the voltage between the unfaulted phases and ground generally increases. The magnitude of the voltage between the unfaulted phases and ground will depend upon the system electrical characteristics and the type of fault and will usually exceed the normal line-to-neutral voltage. Under some circumstances, it may exceed the normal line-to-line voltage. The procedure to be followed in calculating these voltages is adequately covered in other papers which have been presented before the Institute and elsewhere.[5,6,7]

Due to the inherent nature of surge protective devices, they have a maximum voltage rating (given in manufacturers' publications and references)[8–12] which is a measure of the maximum dynamic voltage which may be applied to them during fault conditions. There is always a possibility that the protective devices may be surged simultaneously with the fault. Therefore, the maximum voltage between unfaulted phases and ground which can occur at the point of installation is of prime importance in the application of the protective device, and the voltage rating must be determined in conjunction with the type of grounding as well as with the line-to-line system operating voltage.

A study of the characteristics of the protective device under assumed surge conditions, together with provision for a suitable margin, leads immediately to the protected level which will be provided.

Messrs. Sporn and Gross, in their paper, "Rationalization of System Insulation III—Margins Between Basic Insulation Levels and Protected Levels",[3] suggest that a value given by the formula: (0.10× arrester maximum performance)+25 kv, provides a margin which is generally suitable for the conditions usually to be expected. Margins as calculated by this formula are used in the illustrative examples given later in this paper. Engineering consideration of the specific conditions involved may lead to the requirement for different margins in other cases.

For apparatus closely and continuously associated with a protective device, the basic insulation level equal to or just above the protected level thus determined would probably be the minimum to which the apparatus must conform. In certain situations, the importance of service as related to the economics of the application may indicate the wisdom of selecting a higher or lower basic insulation level.

In stations accommodating many connecting lines and in stations of considerable extent, it may be that different types of protective devices are used at different locations; also, the operation of switching equipment may isolate certain apparatus from some of the protective devices. Therefore, it may be that some of the apparatus in the station should be designed to operate with a different protected level and, consequently, a different basic insulation level than other apparatus in the same station.

The impulse characteristics of a particular piece of apparatus and the corresponding characteristics of the protective device must be studied to assure that an adequate margin is being provided between the protected level and the insulation strength of the apparatus at all points on its volt-time curve.

Co-ordination based on insulation levels as determined by full-wave "withstand" voltage tests is satisfactory only when the protected level is relatively flat down to very short time on the volt-time curve.[4] This is practically true for modern lightning arresters where the short-time protected level is determined by the gap breakdown plus suitable tolerance and margin, and the longer-time protected level comparable to the full-wave test on apparatus is determined by the *IR* drop at some specified value of discharge current plus suitable tolerance and margin. If a gap type of protective device such as a protector tube or a plain rod gap is to be used, the protection afforded is based entirely on the time to break down and should not be considered as a level, but rather a volt-time curve. In this case, a comparison of the apparatus chopped-wave impulse-voltage test with the gap breakdown at a comparable time plus suitable tolerance and margin would more accurately represent co-ordination for apparatus on which complete volt-time curves are not available.

In selecting the insulation level for a specific situation, consideration should be given to the requirements as imposed both by the low-frequency voltages and the impulse voltages which may exist. This discussion deals principally with the impulse voltages. However, the low-frequency voltages which may appear on the system must also be given adequate consideration and the low-frequency insulation requirements, both dry and wet, determined with evaluation of specific conditions, such as dirt, condensation, fog, etc. *In the final analysis, the insulation suitable for a given situation will be that which fulfills the low-frequency requirements, the surge-voltage requirements, and the economics involved.*

Illustrative Examples

In order to make more clear and specific the application of this procedure to the selection of impulse levels, three examples are outlined below. It is thought that the conditions selected are those typically existing on many large transmission and distribution systems in the United States. For convenient reference, table IV summarizes the present impulse insulation levels of various types of apparatus listed according to the impulse full-wave voltage-test levels. These data are based on information available at the present time.

EXAMPLE I—TYPICAL LOW-VOLTAGE DISTRIBUTION-SUBSTATION APPARATUS

Assume a 4,160-volt distribution substation with bus and switching equipment indoors, transformers and regulators outdoors, and with overhead lines connected. The supply transformers are all to be connected wye on the low-voltage side with the neutral solidly grounded and delta connected on the high-voltage side. This makes the ratio X_0/X_1 less than one at all times and, therefore, under fault conditions the voltage to ground at the substation will not exceed normal maximum line-to-ground operating voltage[5,6,7] which might be as high as 2,800 volts on the line side of the voltage regulators. This allows the use of 3-kv protective devices for the supply transformers, the bus, or the regulators. Allowing a margin of ten per cent above the arrester maximum performance at 5,000 amperes discharge

Table II. Typical 12,000-Volt Substation

Designed for Basic Insulation Level 95

Item	Apparatus Insulation Classification* (Kv)	Voltage Rating* (Kv)	Withstand Voltage: Low-Frequency Test (Kv RMS)	Withstand Voltage: Impulse Full-Wave Test (Kv Crest)
Surge protective equipment:				
Lightning arrester		12		
Supply transformers:				
(Over 500 kva)	15	12	34	112
Station power transformers:				
(Less than 500 kva)	15	12	34	95
Regulators:				
(Less than 750 kva, three phase)	15	12	34	95
Instrument transformers:				
Current, outdoor	15	15	34	95
Potential, outdoor (ratio 100 to 1)	15	12	34	95
Current, indoor	25	25	34	95
Potential, indoor (ratio 100 to 1)	Special	12	34	95
Oil circuit breakers:				
Outdoor	15	15	54	110
Indoor—Below 500,000 kva	15†	15†	36†	80†
Bus and switch insulators		25	63* wet	145**
Bushings: Outdoor	15		45 wet	110

* This designation is that at present used by manufacturers.

** Flashover rating. Impulse withstand test not yet determined.

Available indoor apparatus does not meet basic-insulation-level requirements. Next higher (23 kv) rated breakers are at present available only in ratings 500,000 kva and above.

plus 25 kv and using the "industry characteristics" for a modern 3-kv line-type arrester,[19] the protected level would be 44 kv (17.5+1.7+25=44 kv) and the minimum allowable basic insulation level would thus be 45 kv for apparatus closely connected to the protective devices. However, if the station is large or important, a higher basic insulation level probably is justified for apparatus in the most important locations, and a 60-kv basic insulation level would then be indicated for bus and switch insulators, power transformers, circuit breakers, and other apparatus whose failure would mean a complete station interruption.

The selection of the specific apparatus is next a matter of determining the rating or type designation of each kind of apparatus which will meet the impulse insulation level and the low-frequency requirements as decided upon. Table I illustrates a typical selection of equipment for the 4,160-volt distribution substation of example I as based on present available insulation classes necessary to meet a 60-kv, or 45-kv impulse level where such would be permissible. It will be noted that there is little consistency among the various designations of apparatus insulation classes as related to the impulse full-wave test values for the different types of apparatus. The potential transformers connected to the bus would probably be special, since the present practice is to make 20/1 indoor potential transformers to the 50-kv impulse level. If the potential transformers were connected through an adequately insulated fuse and used only for such purposes that a failure of a potential transformer would not cause an interruption, the 50-kv impulse level probably would be considered satisfactory.

It will be noted that low-frequency test values are also included in table I. As mentioned above, consideration must be given to low-frequency requirements in selecting apparatus. However, further analysis of these requirements is not given here since it is outside the scope of this paper.

EXAMPLE II—TYPICAL HIGH-VOLTAGE DISTRIBUTION-SUBSTATION APPARATUS

Assume a major switching substation including indoor and outdoor apparatus operating at 12,000 volts. The station contains disconnecting switches, oil circuit breakers, voltage regulators, potential and current transformers, and stepdown transformers connected delta. The power supply comes in over several overhead lines, the oil circuit breakers to some of which at times may be operated open and to which potential transformers are connected on the line side in connection with the automatic switching scheme contemplated.

Following the outline of example I, it is determined that the low-frequency line-to-line voltage may reach 13,000. The neutral is not effectively grounded, since the neutral ground is located at the generating station some distance away and the maximum low-frequency line-to-ground voltage is accordingly determined to be 11,000 volts. This will require the use of 12-kv-rated protective devices. With line-type arresters for installation on incoming lines and power transformers, a protected level of 94 kv (62.5+6.2+25=94 kv) would be provided and a basic insulation level of 95 kv would be suitable for the impulse requirement of all apparatus in this station. The low-frequency wet voltage requirements would also have to be investigated in the final selection of porcelain insulation, since this wet requirement frequently is the determining factor in this class of substation and may provide a higher impulse characteristic than indicated by the basic-insulation-level requirement.

It will be seen from the values in table II that for bus insulators and indoor instrument transformers, it is necessary at present to select some equipment having a voltage rating of 25 kv in order to meet the insulation requirements and co-ordinate with the other apparatus. In the case of circuit breakers, the existing indoor types having interrupting ratings below 500,000 kva have impulse full-wave test values considerably below that of the other apparatus. Breakers of this interrupting rating are not generally available in the next higher voltage (23 kv) class.

Table III. Typical 138-Kv Substation

Insulation Design Basis: Basic Insulation Level 650 on Incoming Lines, 550 Elsewhere

Item	Apparatus Insulation Classification* (Kv)	Voltage Rating* (Kv)	Withstand Voltages: Low-Frequency Test (Kv RMS)		Withstand Voltages: Impulse Full-Wave Test (Kv Crest)
Main bus and transformers					
Surge protective device:					
Lightning arrester		121 (109)			
Power transformers	115	138	231		540
Instrument transformers:					
Potential (ratio 1,200 to 1)	115	138	231		540
Current	115	115	231		540
Oil circuit breakers	138†	138†	313		680
Bus and switch insulators		115	255	wet**	550**
Bushings		115	230	wet	550
Line connections					
Surge protective devices:					
Lightning arrester		145 (133)			
Line entrance gap		35 in.			
Regulating equipment	138	138	277		650
Instrument transformers:					
Potential (ratio 1,200 to 1)	138	138	277		650
Current	138	138	277		650
Oil circuit breakers	138	138	313		680
Bus and switch insulators		138	340	wet**	705**
Bushings		138	275	wet	650

* This designation is that at present used by manufacturers.

() The greater margins between the protected level and the basic insulation level provided by these lower ratings may be utilized where applicable.

† Line-to-line voltage requirements determine the rating of this apparatus.

** Flashover rating. Impulse withstand tests not yet determined.

EXAMPLE III—TYPICAL HIGH-VOLTAGE SUBSTATION APPARATUS

Assume a 138-kv transmission substation in connection with a steam generating plant. The step-up transformers, of which there are two banks, are delta-connected on the generator side and wye-connected on the high-voltage side with the neutral solidly grounded. One of the transmission lines is an interconnection with another system and therefore has metering and regulating equipment on the line side of the breaker.

Following the procedure outlined, the possible dynamic overvoltages are analyzed at:

(*a*). The transformer connections—where it is desired to install a lightning arrester as close to the transformers as practicable

(*b*). The bus—to which it is considered that one transformer bank will always be connected

(*c*). The line sides of all breakers and metering equipment

Table IV. Present Impulse Insulation Levels of Apparatus

Apparatus Full-Wave Test—1½ x 40-Microsecond Crest Kilovolts

Manufacturer's voltage classification	2.5	5	7.5/8.7	15	25	34.5	46	69	92	115	138	161	196	230	287	345
Transformers over 500 kva		76	95	112	153	195	245	345	445	540	645	750	900	1,050	1,290	1,540
Transformers 500 kva or less	50	60	76	95	153	195	245	345	445	540	645	750	900	1,050	1,290	1,540
Step regulators:																
Over 250 kva, single phase Over 750 kva, three phase		76	95	112	153	195	245	345	445	540	645	750	900	1,050	1,290	1,540
Step regulators:																
250 kva or less, single phase 750 kva or less, three phase	50	60	76	95	153	195	245	345	445	540	645	750	900	1,050	1,290	1,540
Transformers—instrument—outdoor	50	60	76	95	153	195	245	345	445	540	645	750	900	1,050	1,290	1,540
Transformers—instrument—indoor**	30	50	60	76												
Induction regulators (proposed)	Same as step regulators															
Outdoor bushings (proposed for all apparatus rated above 15 kv, and major apparatus 15 kv and below)	60	75	95	110	150	200	250	350	450	550	650	750	900	1,050	1,300	1,550
Indoor bushings	30	45	60	75	130	180										
Insulators—switch and bus*			85	110	145	190	230	315	350	550	705	705	880	1,045		
Oil circuit breakers:‡																
Outdoor—50,000 kva and up			75	110	150	190	250	360	470	570	680	790	950	1,100	1,360	1,620
Indoor—50,000 kva—500,000 kva			60	80												
Indoor—500,000 kva and up				95	110	150										
Synchronous machines†	7.0	13	21	41												

* Mininum average impulse flashover ratings 1½x40 wave. Withstand (full wave) voltage values now being established.

†Not impulse test value. Required protected level = 1.4 (2×rating+1 kv).

** In some ratings higher insulation levels are available.

‡ Negative five per cent tolerance allowable for "withstand" basis.

With the transformers solidly grounded at all times, the condition for maximum dynamic line-to-ground voltage is found to be with maximum system generation and with one transformer bank in service. This voltage is calculated to be 100 kv to ground, which allows the use of lightning arresters rated 121 kv or even 109 kv on the transformers or the bus. The basic insulation level selected for the transformers and bus is, therefore, 550 kv[3,4,8] based on a protected level of $463+46+25=534$ kv.

It is found by calculation that the maximum low-frequency voltage on the line side of the various breakers may be greater than that considered satisfactory for 121-kv protective devices and, therefore, 145-kv or at least 133-kv protective devices are required with a corresponding basic insulation level of 650 kv. All this is shown in table III.

Conclusions

1. The maximum dynamic *line-to-ground* voltage of the operating system existing at the apparatus under consideration is largely determinate in the selection of the basic insulation level required of that apparatus.

2. In selecting apparatus from the standpoint of its insulation strength, it is necessary to consider the insulation test levels to which the apparatus is designed, taking into account both the low-frequency and the impulse levels rather than merely the manufacturer's voltage rating.

3. The insulation strength of all types of apparatus should be clearly designated on the name plate in terms of the "low-frequency withstand" and "impulse withstand" voltage levels.

4. Analysis indicates that the schedule of basic insulation levels provided is adequate to fit the variety of requirements usually to be expected.

5. The basic insulation levels required of certain apparatus closely coupled and continuously associated with the protective device is not necessarily the same as that required of other apparatus in the same station which does not have the same degree of protection; for instance, power transformers as compared with line entrance switches or line potential transformers.

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Proposed Standards for Outdoor Bushings

AIEE JOINT COMMITTEE ON BUSHINGS

THE electrical characteristics of bushings for different types of apparatus vary considerably for corresponding voltage ratings. This is partly due to the fact that different committees have handled the preparation of standards for different types of apparatus. Consequently the AIEE joint committee on bushings was formed in an effort to develop more consistent standards covering the electrical characteristics of bushings for all types of apparatus.

The joint committee was formed in February 1938 under the sponsorship of the AIEE standards committee. Its membership includes representatives from the three AIEE technical committees which deal with bushings, namely, the electrical machinery committee, power transmission and distribution committee, and the protective devices committee. The membership was carefully selected to provide tie-ins with the Edison Electric Institute-National Electrical Manufacturers Association Joint Committee on Co-ordination of Insulation and other committees interested in this problem.

Two major questions are involved in this problem, one, what electrical characteristics are required to meet conditions encountered in service and, two, how nearly do present-day bushings meet these requirements? Two subcommittees were formed to deal with these two questions separately. It was apparent that it would be necessary to prepare definitions and a test code and two other subcommittees were formed to prepare these.

In an effort to determine the requirements on an engineering basis a preliminary tabulation was prepared. The low-frequency characteristics were chosen to provide a satisfactory margin of insulation strength for overvoltages due to switching surges, overspeed, fault conditions, etc. The impulse characteristics were chosen to provide a satisfactory margin of insulation strength above the level of lightning protection which can be provided with modern lightning-protection equipment. The impulse characteristics selected are in accord with the values being considered by the Joint Committee on Co-ordination of Insulation. It was agreed that all of these characteristics should be expressed in terms of "withstand voltages" rather than "flashover voltages" in order to eliminate the variations and uncertainties involved in flashover tests.

Other tabulations were prepared independently showing the characteristics of present-day bushings and these values were compared with the requirements as mentioned above.

Table I. Proposed Standards for Outdoor Bushings

Recommended Minimum Withstand Test Voltages

Voltage Classification (Kv)	Low-Frequency Test, RMS Kv		Impulse Test, $1^1/_2$ x 40-μ Sec Full Wave (Positive or Negative) (Whichever Is Lower) (Crest Kv)
	1 Min. Dry	10 Sec Wet	
2.5	21	20	60
5.0	27	24	75
8.7	35	30	95
15.0	50	45	110
23.0	70	60	150
34.5	95	80	200
46	120	100	250
69	175	145	350
92	225	190	450
115	280	230	550
138	335	275	650
161	385	315	750
196	465	385	900
230	545	445	1,050
287	680	555	1,300
345	810	665	1,550

Explanatory notes:

1. These test values apply to outdoor bushings for all types of apparatus in the voltage classifications 23 kv and up.

2. For voltage classifications 15 kv and below, the test values apply to bushings for major apparatus such as large oil circuit breakers, power transformers, etc.

3. Lower test values may be assigned for bushings used for small oil circuit breakers, distribution transformers, etc. Further study is being given this phase of the subject by the committee.

After considerable study of these two tabulations the following recommendations for outdoor bushings have been agreed upon by the committee:

FUNDAMENTAL PRINCIPLES

(*a*). Bushings are generally integral parts of apparatus, but for the purpose of this study they are considered as separate items of equipment.

(*b*). Performance requirements of bushings shall be such as to permit standard tests on the apparatus with which they are used.

(*c*). Performance requirements are given on the basis of bushings without horn gaps, and under standard atmospheric conditions.

(*d*). Bushings are not intended as protective devices, although they may incidentally furnish partial protection to the apparatus upon which they are installed.

(*e*). The electrical strength characteristics are defined in terms of "withstand" test voltages.

(*f*). In all designs of bushings, the puncture voltage shall be greater than the flashover voltages for both 60-cycle and impulse waves under all standard test conditions.

PERFORMANCE REQUIREMENTS

Bushings shall be suitable for operating at their standard ratings:

(*a*). When and where the temperature of the cooling medium does not exceed 40 degrees centigrade.

(*b*). Where the altitude does not exceed 3,300 feet (1,000 meters).

The bushing requirements as specified in table I apply to outdoor bushings when mounted for testing in accordance with a specified arrangement yet to be determined in the test code.

Withstand Test Voltage

Withstand test voltage is the specified test voltage to be applied under specified conditions without bushing flashover.

For low-frequency voltage the values are expressed as root mean square for a specified time.

For impulse voltages the values are expressed as the crest of a specified wave of either positive or negative polarity, whichever gives the lower value. The impulse voltage shall be applied three times, and if any one (not more than one) of these three applications causes flashover, then the series of tests can be repeated. If in the second series of tests no flashover occurs, then the test causing flashover shall be considered a random shot and disregarded.

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Personnel of AIEE joint committee on bushings: R. T. Henry, chairman; F. S. Brown, J. E. Clem, I. W. Gross, L. H. Hill, J. T. Lusignan, A. C. Monteith, J. R. North, E. Piepho, M. S. Oldacre, A. J. A. Peterson, R. E. Pierce, C. B. Springer, P. C. Turk, F. J. Vogel, and L. Wetherill.

Rationalization of Transmission-System Insulation Strength—III

Margins Between Basic Insulation Levels and Protectable Levels

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SINCE the subject of "Rationalization of Transmission-System Insulation Strength"[1] was first presented in a comprehensive manner in 1928, a great deal of constructive study and analysis have been devoted to the problem, and outstanding progress has been made in placing the insulation of an electric system on a more rational basis from the lightning or impulse viewpoint. Three groups have been most active in this work; the equipment manufacturers, the laboratories, and the users of equipment. Particular attention has been given by the manufacturers to increasing and protecting the impulse strength of various types of commercial apparatus. The laboratories have worked on obtaining fundamental impulse data on equipment impulse strength, and have set up approved methods of testing. Both the above groups as well as the users of equipment have co-operated in field investigations to determine the characteristics of natural lightning. Finally, the users, in co-operation with both the other groups, have worked untiringly to coordinate the entire work into a rationalized insulation system. This principle of insulation rationalization should not be lost sight of in the individual or group efforts on insulation co-ordination to develop particular equipment to meet definite impulse strengths. That is, the fundamental problem is one of rationalizing system insulation strength by co-ordinating the insulation strength of the various items of equipment constituting or forming a part of the transmission or distribution system.

Principles of Insulation Rationalization and Co-ordination

The general principle of insulation rationalization requires three steps.

1. The setting up of a group of insulation levels which can be assigned to various items of equipment.

2. Establishing steps of insulation strength, as seem reasonable, between various types of equipment to insure electrical breakdown, if it does occur, taking place at a predetermined location.

3. Establishing a reasonable margin between the voltage level held by the protective device (the protectable level) and the various basic levels themselves to insure that adequate protection is provided. It is with this latter consideration that the present paper is primarily concerned.

Co-ordination has too frequently been interpreted to mean bringing into the same order, or equality; so strictly interpreted it might well refer to the process of getting equipment to meet an agreed-upon level. Thus, a common level might be sought for all classes of equipment operating on a grounded 69-kv system, for example. But this attempt at oversimplification of the problem will not help its solution. If a distinction is to be made, then, the problem of deciding what types of equipment for given operating conditions shall have equal insulation, is one of co-ordination. On the other hand the setting up of basic insulation levels and applying the margins between levels and between different equipment in a given voltage class is rationalization.

The establishment of insulation steps between different classes of equipment on a given system is predicated on the assumption that the lower-insulated equipment will fail first, and without resulting in flashover or damage of the higher-insulated member. Likewise, the protective-device level should have a sufficient margin between the basic level (which controls the insulation strength of equipment in that level) to insure that all impulse or surge overvoltages which may reasonably be expected will be definitely limited by the protective device to a value below the basic level or to the values allowed for equipment in that level.

This idea was first proposed in 1928[1] and is as sound today as it seemed and was then. It is recognized, however, that the practical working out of it may be difficult if one of the criteria to be observed is to be the highly desirable one of imposing no undue sharp financial burden on the electrical industry. Hence, it will be necessary to be patient. But it would be a grave error to overlook the importance of getting the principle established as soon

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1. For all numbered references, see list at end of paper.

Summary

The AIEE joint committee on bushings has attempted to determine the electrical characteristics of bushings on an engineering basis. The recommendations in this report are based on the theory that an ample margin of insulation strength should be provided above the voltages to which the bushings may be subjected in service, including dynamic overvoltages, switching surges, and lightning surges up to the level of protection which can be provided with modern lightning protective devices. This should make it possible to use bushings having the same electrical characteristics for transformers, circuit breakers, and all types of apparatus of corresponding voltage ratings because, if bushings have an ample margin of insulation strength above the level of protection which can be provided, there should be no need for any higher insulation strength for bushings in circuit breakers than in transformers or other apparatus. Lower values of insulation strength may be assigned for bushings for small circuit breakers, distribution transformers, etc., where space or other limitations may require smaller bushings and where a smaller margin of protection may be satisfactory.

The joint committee is continuing the study of bushings and will make additional recommendations covering indoor bushings as soon as agreement can be reached. They may also make recommendations covering bushings for lower-voltage ratings and possibly other characteristics of outdoor bushings.

Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Table I. Determination of Margins of Protection Between Basic Impulse Insulation Levels (BIL) and Protective Devices

All Voltage Values Kilovolts Unless Marked by Asterisk (*) in Column 1

REF.	Column — 1		2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19	20
(a)	INSULATION REFERENCE CLASS		0.6	1.2	2.5	5.0	8.7	12	15	23	34.5	46	69	92	115	138	161	196	230	288	345
(b)	SYSTEM VOLTAGE-HIGHEST NORMAL (Phase To Phase)	KV*	0.6	1.2	2.4	4.8	8.3	12.5	14.4	24	36	46	69	92	115	138	161	196	230	288	345
(c)	OPERATING VOLTAGE-HIGHEST EXPECTED (Phase To Phase)	KV*	0.72	1.44	2.9	5.8	10	13.8	16.5	25.8	38.1	48.7	73.1	97.8	122	146	171	208	244	305	366
	ISOLATED NEUTRAL SYSTEM — L.As																				
(d)	L.A. MAX. VOLTAGE RATING (For Isolated Neut. System)	KV*	1	1.5	3	6	12	15	20	25	40	50	73	97	121	146	169	207	242	302	363
(e)	L.A.: IR FOR ISOL. NEUT. SYSTEM	KV (1)	6	9	18/11	33/23	63/46	78/57	94/77	118/95	190/154	232/192	342/282	371	463	560	651	780	927	1160	1400
(f)	MARGIN OF BIL ABOVE ISOL. NEUT. L.A. (q-e)	KV (1)	19	21	27/34	27/37	12/29	17/38	16/33	32/55	10/46	18/58	8/68	79	87	90	99	120	123	140	150
(g)	MARGIN OF NEXT HIGHER BIL ABOVE ISOL. NEUT. L.A. [(q+1)-e]	KV (1)	24	36	42/49	42/52	32/39	32/53	56/73	82/105	60/96	118/158	108/168	179	187	190	249	270	373	390	—
	GROUNDED NEUTRAL SYSTEM — L.As																				
(h)	SYSTEM FAULT VOLTAGE-LINE TO GROUND (Under Fault Conditions-Effect. Gr. Neut. System)	KV*	0.54	1.06	2.18	4.35	7.5	10.4	12.4	19.3	28.6	36.5	55	73	91.5	110	128	156	183	229	274
(i)	L.A. MAX. VOLTAGE RATING (For Effect. Gr. Neut. System)	KV	1	1	3	6	9	12	12	20	30	37	60	73	97	109	133	157	183	230	277
(j)	L.A.: IR FOR EFFECT. GR. NEUT. SYSTEM	KV (1)	6	6	18/11	33/23	48/34	63/46	63/46	94/77	148/115	176/143	286/230	342/282	371	420	515	605	705	885	1070
(k)	MARGIN OF BIL ABOVE EFFECT. GR. NEUT. L.A. (q-j)	KV (1)	19	24	27/34	27/37	27/41	32/49	47/64	56/73	52/85	74/107	64/120	108/168	179	230	235	295	345	415	480
(l)	MARGIN OF BIL ABOVE EFFECT. GR. NEUT. L.A. IN NEXT HIGHER INSUL. CLASS [q-(j+1)]	KV (1)	19	12/19	12/22	12/26	12/29	32/49	16/33	2/35	24/57	-36/20	8/68	79	130	138	145	195	165	230	—
	ISOLATED OR GROUNDED NEUTRAL SYSTEMS — ROD GAPS																				
(m)	3⁄4 LINE TO NEUT. VOLTAGE (Based on c)	KV*	1.35	2.7	5.4	10.9	18.7	25.8	31.0	43.8	71.5	91.3	137	183	229	274	320	390	457	572	685
(n)	ROD GAP SPACING (Having 60~ F.O. Given in (m) ½" x ½" Sq. Cut Rod Gap)	IN.	0.07	0.14	0.28	0.55	1.0	1.5	1.9	3.4	6.0	8.5	13.5	18.2	23	27.5	32	40	47.5	60	73
(o)	ROD GAP F.O. (1x5 Neg. Impulse-Avg. Values-50/50 F.O.)	KV	4	7	13	25	37	50	58	92	145	195	300	400	500	600	690	870	1030	1300	1550
(p)	MARGIN OF BIL ABOVE GAP F.O. (q-t)	KV	20	22	30	32	32	37	43	44	33	26	6	-10	-25	-40	-50	-100	-130	-200	-230
	COMPARISON OF BILs WITH L.A. AND ROD GAP CHARACTERISTICS PLUS MARGINS																				
(q)	PROPOSED BASIC IMPULSE INSUL. LEVELS (Crest of 1½x40 Full Wave-Pos. or Neg.)	KV	25	30	45	60	75	95	110	150	200	250	350	450	550	650	750	900	1050	1300	1550
(r)	L.A.: 1.10 IR+25 (For Isolated Neut. System) From (e)	KV (1)	32	35	45/37	61/50	95/76	111/88	128/110	155/130	235/195	281/236	401/335	433	534	641	741	883	1045	1301	1565
(s)	L.A.: 1.10 IR+25 (For Effect. Gr. Neut. System) From (j)	KV (1)	32	32	45/37	61/50	78/62	95/76	95/76	128/110	188/152	219/182	340/278	401/335	433	487	592	691	801	999	1201
(t)	ROD GAP F.O. (1x5 Neg. Impulse-Ref. (o) Plus 15%)	KV	5	8	15	28	43	58	67	106	167	224	344	460	575	690	790	1000	1180	1500	1780

* 60 CYCLE R.M.S. (1) WHERE TWO FIGURES ARE GIVEN THE FIRST IS FOR DISTR. OR LINE TYPE L.As - THE SECOND FOR STATION TYPE L.As

as technical and industry conditions permit.

Considerations Involved in Insulation Margins

There are a number of important considerations involved in establishing margins between protectable levels and basic levels, or what may be called the safe or withstand strength of the equipment built to that level. Among these are the following which are discussed below:

1. Protective-Device Characteristics

Two factors must be considered in the protective device itself: first, its 60-cycle rating; and second, its protective characteristics. A protective device such as a lightning arrester or, in fact, a protective gap, may be considered to have a 60-cycle rating above which it is generally not considered safe or desirable to operate the device. It is well known that a lightning arrester operated continuously or even for short periods of time with a voltage across its terminals above its rating is subject to failure from dynamic overvoltage. Likewise, a protective gap has a lower limit of permissible setting below which the system to which it is applied, when subject to overvoltages such as switching surges and other transients which are not necessarily dangerous to the system insulation, may experience excessive interruptions, thus preventing the rendering of adequate, not to say high grade, service. The normal-frequency voltage of the system, therefore, determines to a large extent the minimum lightning protection which it is possible to apply.

In considering the protective characteristics of the arrester, a number of factors must be considered, on some of which we have little or no information at the present time. These factors are:

(*a*). The maximum lightning current which the arrester may be called upon to discharge. Considerable progress has been made in obtaining information on the magnitude and frequency of such currents in actual field installations.[2,3]

(*b*). The protective characteristic of the arrester (IR drop) varies with the magnitude and rate of current rise through the arrester. The rate of rise of currents through arresters in service is unknown.

(*c*). Again, the lightning-arrester characteristic is affected by the rate of voltage rise of the incoming surge and the arrester-gap breakdown under this condition may be important.

(*d*). A manufacturing tolerance has to be allowed for, as no two arresters of identical characteristics, even made by one manufacturer at the same time, can be relied upon to have exactly the same electrical characteristics.

(*e*). While the arrester may hold the lightning voltage at its own terminals to a predetermined value, under given conditions, just how far distant (circuit feet) it is possible to supply this protection is one of theoretical calculation only at the present time, and again depends upon the rate of voltage rise of the incoming surge and upon voltage reflections.

2. Frequency of Overstress Voltages

The normal or excess margin of insulation over its rated or tested value may have to be taken advantage of in setting up margins between basic and protectable levels. For example, if currents of 15,000 to 30,000 amperes are discharged by the arrester only once in 10 or 20 years, it may be more economical to overstress some equipment insulation once during this period as a result of such a surge, rather than attempt, by expensive additional insulation to maintain the basic insulation level above this value under such conditions.

3. Effect of Aging or Other Deterioration of Insulation

It is well known that some classes of equipment will deteriorate with age even though given what may be considered ordinary or even high-grade care and maintenance. The presence of carbon and moisture in the insulation, and natural aging tend to weaken the insulation of many types of equipment, and this must be considered in arriving at a margin between the protectable level and the equipment strength.

4. Degree of Protection Justifiable and Sought

Finally and perhaps least definite of all, is the degree of protection which it is desired to obtain. Probably no system of insulation steps, even with protective devices, can be practically and economically applied which will preclude an occasional failure of some piece of equipment. In other words, 100 per cent protection is practically, and certainly economically, impossible. But there are differing degrees of near perfection, and different classes of service will each justify its separate different degree of near perfection. Therefore, it is quite apparent that in arriving at the protection margins to protect the basic levels, consideration must be given to what is a reasonable degree of protection to be expected and economically justified under a given set of conditions.

Determination of Protectable Levels by Use of Lightning Arresters

The determination of the margin between protectable levels and basic insulation levels obviously calls for a determination of the protectable level as well as the basic insulation level. If the protectable level is to be obtained by the use of a lightning arrester it is however, necessary to have a clear understanding of the basis of applying the arrester and of determining its performance. This has been done in the analysis which follows:

1. The protective characteristic of the lightning arrester is based on the arrester discharge at 5,000 amperes with the accepted 10–20-microsecond time characteristic. This value of 5,000 amperes is one which, as indicated by recent extended field tests,[3] may be encountered with sufficient frequency to justify its use.

2. The protective voltage levels of the arrester are taken as the maximum values (as distinguished from minimum or average values) for all arresters, whether distribution, line, or station type.

3. In applying arresters on a power system, it is desirable that the arrester maximum permissible rating should be not lower than the maximum power-frequency voltage to ground expected on the system under the most severe operating conditions.

Following this line of reasoning, the maximum rating of lightning arresters applied on an effectively grounded-neutral system has, for purposes of this analysis, been so chosen as to be not less than 1.3 times[*] the highest operating voltage (line to ground) to allow for the higher voltage which may exist under fault conditions. Where more severe conditions exist, it has been assumed that they will be considered as special cases.

4. In considering the entire group of normal system voltages, the numerical values have been increased on a fixed percentage basis to take care of the probable higher operating voltages to be expected on some systems throughout the country; and on account of system voltage regulation, the percentages of increase used are as follows:

For systems up to 8.3 kv rated voltage between phases, 20 per cent increase. For system voltages above 8.3 to 15 kv inclusive, 10 per cent increase; and above 15 kv, 6 per cent.

System voltages where hydro generation predominates are considered as requiring special consideration, although later discussion of obtainable insulation margins touches on this point.

5. In applying rod gaps as protection to the basic levels, a gap spacing equivalent to $3^1/_4$ times[6] the line-to-neutral voltage has been used. It is realized that this results in physical spacings so small in the lower voltage class of equipment that they may be considered impractical in this range.

In applying arresters to any power system, the one important criterion, that the 60-cycle voltage across the lightning arrester should not exceed its maximum rating, has been carefully observed. For arresters on an isolated-neutral system, it is assumed that so long as the arrester maximum rating is not below the power-frequency line-to-line voltage across the arrester at any time, the arrester itself is self-protected against power voltages. This reasoning is based on the large amount of experience throughout the industry in applying arresters on this basis. In applying arresters on an effectively grounded neutral system, on the other hand, recognition is given to the fact that the system line-to-ground voltage may, during fault conditions, considerably exceed normal. To allow for this the system line-to-ground voltage is increased 30 per cent, and this, as previously discussed, it is believed is sufficient to give the arrester dynamic-voltage protection in the preponderating percentage of cases, where system neutrals are effectively grounded. While it may be desirable in actual practice to add a small margin above this figure, the above basis has been used in this analysis.

Protection of Basic Insulation Levels—Margin Between Basic Insulation Levels and Protectable Levels

Table I has been prepared in an attempt to show what margins of protection to the basic insulation levels are possible by the use of protective devices on a sound engineering basis, in a range of system voltages normally encountered. It should be clearly pointed out, before discussing this table, that the analysis given herewith deals with the protection of the basic insulation levels themselves. The consideration of whether or not equipment is protected will, of course, depend on whether or not it conforms to the definite basic insulation level to which it has been assigned and the margin of protection existing above the protectable level. As will be brought out in the succeeding discussion, equipment conforming to any one of the basic insulation levels can in many cases be used in closely associated levels, and reasonable protection supplied unless the equipment itself cannot by virtue of its dynamic-voltage functional characteristics be so applied. (For example, a 2,300-volt transformer could not be used on a 15-kv system even if its basic insulation level could be adequately protected.)

Referring now to table I, in reference *a* is given the "insulation reference class" which conforms quite closely to the "preferred system voltage" now in common use. The second line (reference *b*) gives the highest normal system voltages which may be expected to be encountered on the distribution and transmission systems throughout the country. These have been selected on the basis of the operating voltages now in use by a great many of the larger utility companies.

To arrive at the 60-cycle basis for applying arresters to an ungrounded system, reference *c* has been developed by increasing the "highest normal system voltages" (reference *b*) by one of the three percentages of 20, 10, and 6 as mentioned above. By converting these to line-to-neutral values the voltages for the effectively grounded-neutral system have been obtained (reference *h*). In references *d* and *i*, the lightning arresters have been selected for both isolated and grounded-neutral systems using the maximum permissible rating for commercial arresters as now being made. The protective characteristics, that is, the maximum voltage permitted by the arrester at 5,000 amperes discharge of these two groups of arresters (references *d* and *i*) are given in references *e* and *j*. Where double figures occur in any column, the first figure is for distribution or line-type arresters, and the second figure for station-type arresters. All these numerical values are the maximum for arresters of each class indicated, and conform to published data developed by and agreed upon by arrester manufacturers as representative of their product.*

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[*] This is the figure that applies except in a very few special cases on our system. For fuller discussion of overvoltages see references 4 and 5.

Following the line of thought that a "safety factor" should be provided to take into account the large number of variables as enumerated above, the arrester *IR*-drop characteristic has been increased by a margin of ten per cent plus a flat value of 25 kv. These might be considered as the highest voltage levels that need to be considered in any study of basic insulation levels. Figures so arrived at for both isolated-neutral and grounded systems are given in references *r* and *s*.

In reference *q*, the proposed basic impulse insulation levels now being considered have been listed for the various insulation classes noted in columns 1 to 20 of the table. A comparison of the basic insulation levels in reference *q* with the highest lightning-arrester levels already referred to in references *r* and *s* shows that there is a reasonably close agreement between the basic insulation levels and the lightning-arrester characteristics plus margins (reference *r*) even for the isolated-neutral-system arrester, although it must be admitted that the line-type arrester for isolated-neutral service shows an almost consistent deficiency of margin between the basic insulation level and the highest voltage level to be expected from arrester performance figured on that basis. This is admittedly, however, a method of check that goes to some extremes in assumption of unfavorable protective conditions.

To determine the actual margin between the proposed basic insulation levels and the arrester *IR* drop at 5,000 amperes, references *f* and *k* have been developed for isolated and grounded-neutral systems respectively. Reference *f* shows the margins ranging from 19 kv in the low insulation class, with a general upward trend to 150 kv in the 345 class for station arresters. Line-type arresters do not show up so well however, although they do show a positive margin in their entire range of applicability. One point of distinct interest here is that shown in reference *k* which gives the margins of basic insulation levels over lightning arresters for grounded-neutral service. Here, the margin starts at 19 kv and increases to 480 kv in the 345 class. From the 2.5 class up, the margin between the basic insulation level and the lightning arrester ranges from equality to as high as 3 times as great for station arresters, and as high as 30 times as great in line arresters, for grounded-neutral systems when compared with the isolated-neutral system.

If higher margins than given in reference *f* are desired between basic insulation levels and lightning arresters for isolated-neutral service, this can be accomplished by using basic insulation levels in the next higher level. Margins obtainable in this way are shown in reference *g*. It will be noted that they are quite ample even with line-type arresters.

Again, where the system operates with an effectively grounded neutral, but abnormally high voltages may occur because of hydro generation, system extent, or other conditions, it is possible to use the proposed basic insulation level in the assigned insulation class and arrester in the next higher class, thus supplying the required margin of 60-cycle strength for the arrester and still offering a high degree of protection of the basic insulation level as shown in reference *l*. Many of these margins for the various insulation classes are of the same order of magnitude as is possible by applying standard arresters to isolated-neutral systems (comparison of references *f* and *l*). There is, of course, one notable exception in the 46 class; but for the 69 class and above, the protection thus afforded is equal to or better than that supplied on the isolated neutral system.

This same reference *l* shows the margins which are possible by using the appropriate arrester for grounded-neutral systems, and basic insulation levels in the next lower insulation class. For example, in the 138 class, it is shown that by using 115-class insulation on a 138-kv system with grounded-neutral arrester rated at 109 kv, there is a margin of 130 kv. This, of course, is less than the 230-kv margin obtained when using standard 138-kv insulation with the grounded-neutral arrester. However, it is more than the 90-kv margin obtained in using the standard 145-kv arrester on the isolated-neutral system.

The correlation of the proposed basic insulation levels with protective gap characteristics is given in references *m*, *n*, *o*, and *t*. It needs to be pointed out, however, that the gap data at low spacings in the range of insulation reference classes from 0.6 to 8.7, involving as they do gaps of one-inch spacing or less, are not presented as being particularly reliable or representative of gap spacings which any operating company would be willing to consider as practical in these voltage ranges.

The protective characteristics of a rod gap have been set up on a basis of using flashover data with a 1x5 negative impulse, and the average values of 50-50 flashover of such gaps have been increased 15 per cent to allow for tolerance of performance under practical conditions. This gives a figure which may be comparable to the *IR* drop characteristic of arresters at 5,000 amperes. This particular procedure in arriving at gap characteristics may be questioned, but it is presented here merely for comparative purposes to show the relative degree of protection which can be afforded by rod gaps using assumptions believed to be reasonable, and available data. It will be noted from reference *p* that margins of protection over the proposed basic insulation levels are positive up to the 69 reference class but become negative beyond this point. It should be remembered however, that these margins are based on a gap spacing corresponding to $3^{1}/_{4}$ times the maximum expected line-to-ground system voltage. It is entirely possible where experience with difficulties from line tripouts due to switching surges are favorable, or where the requirements of service continuity are not too stringent, to reduce the gap spacing and thus give positive margins, in some of the higher-voltage classes.

Practical Experience in Applying Lightning Protection

As previously reported[10] there was initiated some five years ago on the system of the American Gas and Electric Company a practice, applied to large power transformers, of locating directly on the transformer tank a lightning arrester correlated with the transformer strength. It is interesting to review the performance record since that time of transformers protected in this way, and compare the margins with those arrived at from the above analysis.

During the past four years there have been installed on the American Gas and Electric Company's 132-kv interconnected system nearly 700,000 kva of transformers which have been protected by lightning arresters mounted either directly on the transformer or as close as possible thereto. A review of the margins between the full-wave transformer test and the lightning-arrester *IR* drop at 5,000 amperes for the 132-kv windings of these transformers indicates values from approximately 40 kv to 205 kv. All of these 132-kv windings conform to the 115-kv insulation class so far as voltage tests were concerned, some were delta connected and others built with graded insulation for grounded neutral service, with an appreciable saving in first cost.

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* By and large these values have been checked by laboratory test.

With the use of standard transformer insulation on the 66-kv systems a margin of 30 kv was obtained between the strength of various transformers applied on them during this period and lightning arrester characteristic; in the 44-kv class, the margin was 33 to 96 kv; in the 34.5-kv class 37 to 80 kv; and in the 22-kv class 39 to 55 kv. It will be noted from the above figures that it has been possible to obtain with standard insulation on transformers as offered today, a substantial margin between the transformer full-wave test strength and *IR*-drop characteristics of lightning arresters of recent design. Further, in the higher-voltage class of 138 kv, it has been possible to utilize impulse insulation one class below the standard 138-kv class and still obtain margins which appear to be satisfactory. It should be pointed out in this connection, however, that in nearly all these cases of graded insulation using insulation in the next lower class, the lightning arrester has been mounted directly on or very close to the transformer.

On the 132-kv system, margins which we have been able to obtain in the past have not been quite as high as those indicated in table I or as obtained during the past four years. There are several reasons for this. One is that present-day arresters undoubtedly have better characteristics than those made several years ago thus permitting of larger margins, and another is that we have permitted ourselves to work on closer margins because we have generally impulse tested the very arrester which was used in service, while mounted directly on the transformer. Under such actually checked margins lower values are obviously still safe. It is further interesting to note that of some 750,000 kva of transformer capacity installed during the past four years involving circuits of 22, 33, 44, 66, 88, and 132 kv, not a single case of transformer failure has occurred where the transformer strength has been built up to the present insulation standards and protection supplied by modern lightning arresters with margins of the above indicated order.

Summary and Conclusions

1. The solution of the problem of rationalization of system insulation has advanced considerably since it was first proposed some 12 years ago, and the currently discussed series of basic insulation levels is a significant contribution toward its solution.

2. The proposed basic impulse insulation levels (reference *q* of table I) appear to provide a sufficient number of steps and their level values are suitably chosen so that modern arresters can be applied to protection of equipment built to these basic levels, and adequate margins realized under practically all conditions likely to be encountered in power-system operation.

3. The spacing of the basic-insulation-level steps has been so arranged as to make possible the securing of considerable flexibility in the choice in the margins of protection of equipment built to these levels without the assumption of burdensome economic handicaps.

4. While the margins between arrester characteristics or protectable levels and basic insulation levels appear adequate, considering the uncertain and unknown factors in lightning voltage surges which may be imposed on apparatus and the performance of a protective device under these conditions, caution is necessary. Particularly is this true if the arrester is expected to provide adequate protection to equipment located at some distance from the arrester as is required in some cases.

5. As a reasonable margin between the basic insulation level and lightning-arrester *IR*-drop characteristic, for general application, it is suggested that the arrester *IR* drop at 5,000 amperes increased by ten per cent and 25 kv should not be greater than the basic insulation level. This will influence in some cases the type of arrester chosen. Where the arrester is located at or close to the equipment to be protected, practical considerations may be permitted to allow acceptable margins below the value so arrived at.

6. With the acceptance of the proposed basic insulation levels (reference *q*, table I) the problem of assigning insulation strength to all classes of equipment in terms of these levels on an engineering and economic basis should not be difficult if a broad, industry-wide point of view is taken of the insulation rationalization problem.

7. Eventually it will be found necessary and desirable, in order to complete the solution of the problem of rationalization of system insulation, to provide definite margins in the levels of different equipment forming part of any given transmission system. It may be that this can best be done as part of the natural development which started with the work of investigation and determination of impulse characteristics of equipment, continued with the investigation of natural and system overvoltage phenomena, to be followed by the establishment of proper basic insulation levels and by the development of equipment to protect these levels and, finally, completed by the assignment of insulation strength to various equipment groups in terms of these levels. Whether such margins in levels for different equipment on one system should be established at the time the problem of assignment of insulation strength to various equipment groups in terms of basic insulation levels is carried through, or whether it will be best to await the experience gained by the assignment of practically co-ordinated levels for all the items of various equipment in a voltage class, is something that need not be determined at the present. The problem, in any case, is well enough in hand to permit patience with a view of not only allowing for integrating further experience, but also of making certain that no heavy burden is thrown on any portion of the industry concerned in this problem.

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Discussion

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Basic Impulse Insulation Levels

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Insulation Co-ordination

ONE of the early steps taken by the electrical industry, in an attempt to help solve the insulation-co-ordination problem was the formation of the National Electric Light Association (now Edison Electric Institute)-National Electrical Manufacturers Association Joint Committee on System Insulation Co-ordination in 1931. Before relating the work done by this committee, to acquaint the industry with what has been done to date and the present status of the work, it may be well to comment briefly, on what is meant by "insulation co-ordination."

It was generally recognized some ten years or so ago, as brought out by many papers in the technical press then and later, that the insulation strength of various classes of equipment was assigned not on the basis of the electric system as a whole, but rather on the requirements, as then understood, of each class by itself. This situation became strikingly evident in regard to the lightning, or impulse, strength of equipment which, until some 13 years ago, received comparatively slight consideration, largely because until then very little was known about the magnitude of lightning voltages on electric systems and the ability of insulation to withstand such voltages.

As information was obtained on both of these factors, it soon became apparent to some that it should be possible to arrange the impulse or lightning strength of equipment on any electrical system in a rational manner related to that particular system's requirements and so proportioned that electrical breakdown from lightning, if it did occur, could be confined to a predetermined location where the least damage would result to apparatus and service.

Closely associated with this idea, which basically required insulation steps or margins between the different classes of equipment, was the idea of setting up agreed-upon insulation levels. These levels, it has always been believed, quite logically should be based on the protection level afforded in the various voltage classes by available protective devices such as lightning arresters. This line of reasoning postulates then the setting up of levels which can receive a reliable degree of protection from the protective device, thus tending to minimize system service outages. But it goes further, and permits assignment to the different pieces of equipment of such strengths as should localize the failure, if such occurs, at that point in the electrical system where the least damage will result. In short, the term "insulation co-ordination" may be defined as the process of setting standards of insulation strength and assigning them to all the items of equipment in the various insulation classes in such a manner as to produce from an economic and operating standpoint both a maximum of protection and, if failure results, a minimum damage to the system.

Progress in Solving the "Insulation Co-ordination" Problem

The first step of the joint committee was the adoption in 1931 of a statement of principles which said:

> "The co-ordination of insulation involves three steps:
>
> "1. Establishment of insulation levels.
>
> "2. Specification of insulation strengths of all classes of equipment in established insulation levels.
>
> "3. Allocation of the insulation levels to the nominal system voltages, taking into account all operating and environmental conditions."

The establishment of levels on an impulse basis with definite kilovolt values given, was covered by a committee report[1] in 1937. The second step was undertaken in 1937 by the formation of the "Insulation Strength of Equipment Subcommittee of the EEI-NEMA Joint Committee on Co-ordination of Insulation" which has since been actively working on the problem. As a result of its work, it appears that the basic impulse insulation levels agreed upon in 1937 should now be reviewed in the light of our present knowledge, and the recent developments in the art. This is the subject of this paper.

Basis for Impulse Insulation Levels

In the 1937 report, basic impulse insulation levels were recommended which were the outcome of a study by a subcommittee of the insulation levels of existing substations and the performance obtained with those levels. The subcommittee did not select for the basic levels the best-insulated systems, but rather the average of a number of systems that were known to have a good operating record over the years. The values selected, therefore, were not unreasonably high but represented good general practice at the time of the survey.

In addition the then proposed values were checked against the performance characteristics of lightning arresters then available, and the flashover characteristics of rod gaps in use for commercial testing of transformers. This seemed to be a practical and sound basis on which to set up the levels.

Up to the time of the 1937 report, it had been customary to express impulse insulation strength in equivalent "inches of rod gap." The reason for this was that the testing technique of the various laboratories had not yet developed to the point where they could agree on the flashover value in volts of a rod gap. By 1937, however, this difficulty had been overcome and it was possible, therefore, to express the basic impulse insulation levels in volts. Considering the evolution of this step from rod gap and insulator testing, it was rather natural that the conversion from gap spacings to volts should be on a 50-50 flashover basis, that is, volts corresponding to gap spacings giving 50 per cent flashover and 50 per cent full wave when subjected to a standard impulse wave.

Since that time experience has been gathered in applying the values to actual practice. This has shown that the basis originally chosen requires clarification and adjustment.

It is not practicable to subject most types of apparatus to a series of flashover tests to demonstrate their insulation levels. Rather, a piece of apparatus is designed to withstand a certain impulse voltage when tested in parallel with a gap set for that particular voltage. It developed, then, that the impulse strength of apparatus was determined on one basis (withstand basis) and the required level was given on another basis (flashover basis). In order to reconcile these differences, it

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[1] For numbered reference, see end of paper.

was necessary, therefore, to include a negative tolerance in the definition of basic insulation levels for demonstration of acceptability of equipment.

Since the basic impulse levels were proposed, manufacturers' testing laboratories have been working on refining impulse testing technique and the determination of the impulse strength of various apparatus. On the basis of this improved testing technique, it has been found quite recently that some of the porcelain insulators in bus and switch assemblies do not meet the basic impulse level proposed for the system voltage on which they are frequently used. As these comprise an important part of the system insulation, and as some insulators with seemingly inadequate insulation (as indicated by the suggested impulse levels) have given good performance in service, it is felt that further consideration should be given to that problem.

Definition of Basic Impulse Insulation Levels

The committee, therefore, set about redefining the 1937 basic insulation levels and put the values on a "withstand" rather than a "flashover" basis to eliminate the need for a tolerance factor.

To indicate how this redefining was accomplished, the old and new definitions are given below:

Old Definition

"Basic insulation levels are reference levels expressed in impulse crest voltage with a standard wave not longer than a 1.5x40-microsecond wave.

"Note: Apparatus insulation as demonstrated by suitable tests shall be equal to or greater than the 'basic insulation levels'. Due to the present limitation in testing, a tolerance of minus five per cent may be allowed when making full-wave tests on specified types of apparatus."

New Definition

"Basic impulse insulation levels are reference levels expressed in impulse crest voltage with a standard wave not longer than 1.5x40-microsecond wave. Apparatus insulation as demonstrated by suitable tests shall be equal to or greater than the basic insulation level."

To clarify what is meant by "withstand basis," the definitions prepared by the laboratory subcommittee and amplified in a separate report are repeated.

Withstand Voltage (for an Impulse)

"The withstand voltage of a test specimen under an impulse of any given wave shape, polarity, and amplitude, which does not cause disruptive discharge on the test specimen, is the crest value attained by that impulse."

Table I. Basic Impulse Insulation Levels

Reference Class (Kv)	Basic Level Based on Old Definition (Kv)	Basic Level Old Definition Reduced 5 Per Cent (Kv)	Proposed New Basic Level* (Kv)
1.2	32	30	30
2.5	53	50	45
5.0	63	60	60
8.66	80	76	75
			95
15	100	95	110
23	150	143	150
34.5	190	180	200
46	250	238	250
69	360	342	350
92	470	447	450
115	570	542	550
138	680	646	650
161	790	750	750
196	950	900	900
230	1,100	1,050	1,050
287	1,360	1,300	1,300
345	1,620	1,550	1,550

* These levels represent definite steps between which no intermediate values will be recognized. It should be noted that the levels are proposed and still under consideration of the joint committee.

Note: Attention is called to the fact that equipment carrying a power-frequency voltage rating which is numerically the same as the "Reference Class" in table I is not necessarily required to have the impulse levels given in the table.

Critical Withstand Voltage (for an Impulse)

"The critical withstand voltage of a test specimen under an impulse of any given wave shape and polarity is the crest value of that impulse when its amplitude is adjusted to be just below disruptive discharge on the test specimen."

Rated Withstand Voltage* (for an Impulse)

"The rated withstand voltage of a test specimen is the crest value of an impulse of given wave shape and polarity, to be applied under specified conditions without disruptive discharge on the test specimen."

New Proposed Basic Impulse Insulation Levels

As a result of the foregoing considerations it is proposed to revise the 1937 levels as indicated in table I. The second column gives the 1937 levels which were on a 50-50 flashover basis. The third column gives the 1937 values reduced five per cent to put them on a "withstand" rather than a flashover basis. And the fourth column, which represents the new proposed levels, is practically the same as column three except that the figures have been rounded off to the nearest 5 kv, and further slightly altered in several cases to plot on a smooth curve against the "Reference Class." Also one new basic level of 110 kv has been added. In the first column the heading "Reference Class" has been used and values given to facilitate a comparison of the proposed levels with previously published[1] data.

Considerable discussion has taken place as to the desirability of associating the impulse levels directly with operating voltages; and two radically different opinions exist on this point. One is, that since lightning voltages encountered in service are not dependent on the operating voltage, the impulse levels should not carry what purports to be an operating voltage rating; rather, arbitrary references such as *A*, *B*, *C*, etc., or 1, 2, 3, etc., might well be used for identification. The other point of view is that to omit the operating voltage designation will result in considerable confusion in the industry. It is outside the scope of this paper to discuss this phase of the problem here. Certain it is, however, that a co-ordinated system of insulation levels is desirable, and the values as given in the 1937 committee report have been extensively used in producing better insulation co-ordination on many electric transmission systems which exist today.

There are, however, still individual items of equipment that do not meet the 1937 basic levels, and in some cases, notably apparatus insulators and assemblies, some are low. The modification of designs covering these devices to meet the proposed levels cannot be undertaken without considering the economic burden on the electric industry. However, it may not be detrimental to the co-ordination program if they do not meet the proposed levels until further experience and discussion on this phase shows the desirability of making them conform to such levels.

Committee Representation

The formation of the joint committee on co-ordination of system insulation, started in 1931 through joint action of NELA (now EEI) and NEMA brought together for a study of the problem two vitally interested groups, one the users and system designers, and the other the manufacturers. Within the last few months the AIEE has agreed to join the above two groups in carrying on the work. As soon as the AIEE representatives are appointed the committee will function as a triple joint committee under the sponsorship of AIEE, EEI, and NEMA.

Summary and Conclusions

1. The adoption of basic impulse insulation levels by committee action in 1937 has actively stimulated the work of designing and

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* Note: When rated withstand test voltages are specified, it is customary to specify also a definite test procedure for demonstration purposes.

Recommendations for High-Voltage Testing

EEI-NEMA SUBCOMMITTEE REPORT

I—Introduction

(a). Scope of Committee Work

THIS subcommittee was established to study laboratory differences in results and methods in high-voltage testing and to propose laboratory technique to remedy these differences. Line-insulator, gap, and apparatus-insulator testing have been included in the scope of subcommittee activities. Some two years ago the subcommittee reported on flashover characteristics of rod gaps and insulators.[1] Since then continued work on high-voltage problems has resulted in a markedly improved understanding of the situation and some further recommendations have been agreed upon. Section II of this paper gives general recommendations for impulse testing and Section III outlines the scope of the Committee's recent activities and presents additional recommendations, particularly as to humidity corrections. All these activities are definitely related to the major problem of insulation co-ordination.

(b). Insulation Co-ordination

Insulation co-ordination is the correlation of the insulation strength of all the electrical equipment in a station or substation to meet a specified level or levels. In general, present practice is to have all the equipment meet a certain level of strength selected so that a protective device will control the path of discharge or flashover. Fundamentally, the purpose of impulse testing is to determine the relative performance of apparatus when subjected to surges such as those caused by natural lightning. In practice the co-ordination of apparatus insulation for impulse voltages is determined by tests which

(*a*). Demonstrate the insulation strength of the apparatus

(*b*). Demonstrate the protected voltage level established by the protective equipment

Uniform testing-equipment characteristics and testing methods and technique are, therefore, very desirable.

II—General Recommendations for Impulse Testing

The following practices related to impulse-voltage-testing equipment and methods have been found satisfactory and are recommended for immediate and general use, where standards for particular types of apparatus do not apply.

(a). Impulse Generator.

A composite schematic diagram of the discharge circuit of a surge generator is shown in figure 1. In many cases, however, some of the elements shown are omitted or are negligible.

A capacitance C_1 is charged by a rectifier to a voltage which produces discharge through a gap into a group of resistors. The impulse test voltage appears across resistance R_4, across which the test specimen is connected.

The capacitance C_1 may consist of several groups of capacitors which are charged in parallel and discharged in series by means of the Marx circuit. The inductance L_1, which is made as small as possible depends upon the physical design of the generator and the total length of the discharge circuit. The resistance R_1 consists of the inherent series resistance of the capacitors and connections and often additional lumped resistances. Additional inductance L_2 or resistance R_2 may be added at the generator terminal for wave-form control. R_3 is a resistor, for controlling the length of the wave, which may be used in addition to R_4 to obtain better regulation of the test voltage especially for short waves. Resistance R_4 may be used alone to control the length of the wave and it simultaneously serves as a voltage divider when a cathode-ray oscillograph is used for recording the wave shape. Capacitances C_2 and C_4 represent the electrostatic capacitances to ground of the high-voltage parts and leads. Included in C_4 are the capacitance of the test specimen and any additional load capacitance that may be required for correct wave shape. L_4 represents the inductance of the test piece. For some test specimens L_4 greatly influences the wave shape.

Because test pieces vary widely as to impulse characteristics and successful testing requires that certain recommended waves be obtained, adjustments of circuit constants are frequently required. Resistances R_2, R_3, and R_4, capacitance C_4, and inductance L_2 should be capable of adjustment.

For practical reasons one terminal of the impulse generator is solidly grounded during the discharge. Impulse voltage of either polarity is obtained by changing

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Paper 40-38, prepared by C. M. Foust and P. H. McAuley as a report by the subcommittee on correlation of laboratory data of the Edison Electric Institute-National Electrical Manufacturers Association joint committee on insulation co-ordination, and presented at the AIEE winter convention, New York, N. Y., January 22-26, 1940. Manuscript submitted November 16, 1939; made available for preprinting January 2, 1940; released for final publication March 27, 1940.

Personnel of EEI-NEMA subcommittee on correlation of laboratory data: J. E. Clem, chairman; A. C. Monteith, secretary; C. M. Foust, H. A. Frey, J. T. Lusignan, P. H. McAuley, K. B. McEachron, P. L. Bellaschi, and L. H. Hill.

[1] Electrical Engineering (AIEE Transactions), June 1937.

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redesigning equipment to meet these levels.

2. Redefining the basic levels to require equipment to withstand the specified test (instead of permitting flashover at the specified values) requires a revision of the values adopted in 1937.

3. The proposed new basic levels appear to be based on sound fundamentals of the insulation co-ordination problem, and their early acceptance should result in a distinct forward step in carrying on the work to a conclusion.

4. It is generally understood that equipment carrying a power-frequency voltage rating which is numerically the same as the "Reference Class" in table I is not necessarily required to have the impulse levels given in the table. Setting up levels of a given value is one problem; assigning equipment to these levels is a separate and distinct one.

5. Discussion is invited by all those interested in this subject, particularly on those aspects of the problem dealing with the proposed new levels, designation of levels, and experience with the use of insulation conforming to or radically different from the levels adopted in the 1937 report.

Reference

1. Basic Impulse Insulation Levels (EEI-NEMA report). AIEE Transactions, 1937, page 711.

Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

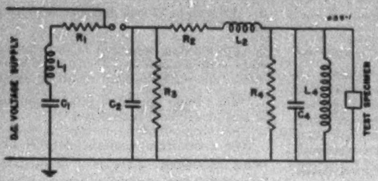

Figure 1. A composite diagram of the impulse generator discharge circuit

In practice R_3, R_4, L_2, C_4, and L_4 may be omitted or may be negligible compared to the other constants in a particular generator

the polarity of the charging circuit of the impulse generator.

(b). IMPULSE WAVES

The most commonly used impulse wave is the 1.5x40-microsecond wave which is being considered for certain standards. The 1x5- and 1x10-microsecond waves have been used occasionally in the past. In the interests of simplification and uniformity, it is recommended that the 1.5x40 wave be universally adopted with proper tolerances for individual apparatus testing codes.

The International Electrotechnical Commission recognizes two waves respectively 1x5 and 1x50 microseconds. Also particular tests sometimes require specified wave fronts such as 50, 100, or 1,000 kv per microsecond.

In figure 2 is illustrated a typical impulse wave. For practical reasons a virtual zero-time point is established at O_1 and determined by a line drawn through the $0.1E$ and $0.9E$ points on the wave front. For the 1.5x40 wave O_1X_1 is 1.5 microseconds and O_1X_4 is 40 microseconds. Thus the standard 1.5x40 microsecond wave rises to crest voltage in 1.5 microseconds and decays to half crest value in 40 microseconds. In this way an impulse wave is specified as a T_1xT_2 wave where T_1 and T_2 are O_1X_1 and O_1X_4 respectively.

If the front is difficult to determine the virtual time-to-crest may be given as a constant such as 1.5 times the interval between the $0.1E$ and $0.9E$ points on the wave front. The proposed standard for transformers calls for 1.5 and, in this case, therefore, the virtual time-to-crest is $1.5X_3X_2$.

The actual recorded wave should conform to the specification. Spurious oscillations on the wave front should be damped out by a sufficient amount of resistance R_1 or R_2 to permit accurate measurement and analysis. The amplitude of such oscillations should not exceed five per cent of the fundamental wave.

Variations from the standard 1.5x40-microsecond wave shape should not exceed ±0.5 microsecond on the wave front and (+10, −0) microseconds on the wave tail. These figures refer to the test wave when no disruptive discharge occurs and where a particular apparatus standard involved does not specify a different tolerance. When the test piece characteristic is such that a wave within these limits cannot be obtained, the actual wave shape should be specified.

(c). IMPULSE FLASHOVER

In testing an insulation specimen a range of voltage is usually found in which disruptive discharge may or may not take place, the percentage of voltage applications which cause disruptive discharge rising from zero at the lower edge of the range to 100 at the upper edge. When no disruptive discharge occurs a *full wave* is obtained. When a disruptive discharge occurs a *chopped wave* is obtained and this chopping may occur on the tail, at the crest, or on the front of the wave depending upon the voltage.

The *critical flashover voltage* of a test specimen under an impulse of a given wave shape and polarity is the crest value of that impulse when its amplitude is adjusted to cause flashover on 50 per cent of the applications.

(d). VOLT-TIME CURVE

The volt-time curve is a graph of the crest values of flashover voltage against the time to flashover for a series of impulse applications between the full-wave test and front-of-wave flashover. The points should be chosen and distributed to fit the volt-time curve throughout this range. Ordinarily 20 to 40 points are desirable where air insulation is involved, but fewer points will give a good approximation of the curve and will suffice for the breakdown of expensive samples. The points for the volt-time curve are obtained from cathode-ray oscillograms of impulse discharges.

(e). FRONT OF WAVE TESTS

Apparatus may be tested with flashovers occurring on the front of the wave. The rate of rise or steepness of the front may be varied by changing the circuit constants or the generator voltage or both.

The rate of rise is specified by the slope of the line through the $0.1E$ and $0.9E$ points on the front, figure 2, in kilovolts per microsecond and is called the effective rate of rise. The average rate of rise is often considered a more significant figure in front-of-wave tests. It has been defined as the slope obtained by dividing the crest voltage of the wave by the time to flashover measured from virtual time zero.

In some cases the lower part of the wave rises gradually to 30 or 40 per cent of the crest voltage and rises more rapidly thereafter. The $0.1E$ point does not give a true indication of the wave when used in determining rate of rise. The tangent for the more rapidly rising portion of the wave should be used in determining the rate of rise in this case.

It is recommended that, at least until it is possible to standardize more definitely on front-of-wave testing technique, both time-to-crest and time-to-flashover together with the method of specifying the rate of rise be recorded and reported in presenting data.

(f). WITHSTAND TESTS

To provide a suitable terminology and facilitate testing procedure for withstand tests, the following definitions are suggested.

Withstand Voltage (for an impulse). The withstand voltage of a test specimen under an impulse of any given wave shape, polarity, and amplitude, which does not cause disruptive discharge on the test specimen, is the crest value attained by that impulse.

Critical Withstand Voltage (for an impulse). The critical withstand voltage of a test specimen under an impulse of any given wave shape and polarity is the crest value of that impulse when its amplitude is adjusted to be just below disruptive discharge on the test specimen.

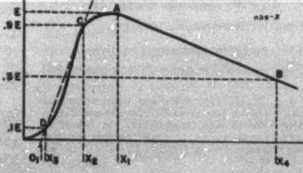

Figure 2. An impulse testing wave illustrating methods of designating significant characteristics of the wave

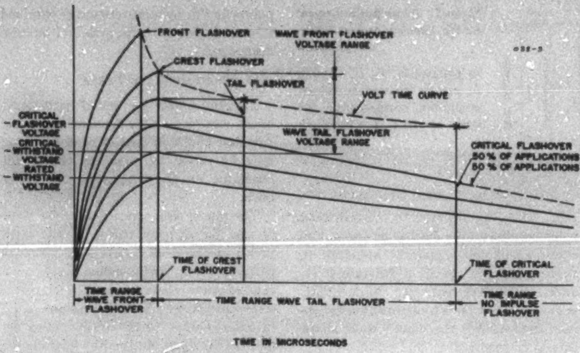
FRONT FLASHOVER
WAVE FRONT FLASHOVER VOLTAGE RANGE
CREST FLASHOVER
TAIL FLASHOVER
VOLT TIME CURVE
CRITICAL FLASHOVER VOLTAGE
CRITICAL WITHSTAND VOLTAGE
RATED WITHSTAND VOLTAGE
WAVE TAIL FLASHOVER VOLTAGE RANGE
CRITICAL FLASHOVER 50% OF APPLICATIONS
50% OF APPLICATIONS
TIME OF CREST FLASHOVER
TIME OF CRITICAL FLASHOVER
TIME RANGE WAVE FRONT FLASHOVER
TIME RANGE WAVE TAIL FLASHOVER
TIME RANGE NO IMPULSE FLASHOVER
TIME IN MICROSECONDS

Figure 3. The series of impulse waves illustrates the terminology and definitions associated with impulse voltage testing

Rated Withstand Voltage[*] *(for an impulse)*. The rated withstand test voltage of a test specimen is the crest value of an impulse of given wave shape and polarity, to be applied under specified conditions without disruptive discharge on the test specimen.

These definitions are illustrated in figure 3.

(g). Deviations in Test Data

For positive-polarity critical withstand and critical flashover voltage measurements, the value obtained in an individual test in any laboratory should be within eight per cent of available standardized values, which are average values or levels obtained on similar apparatus from tests made in a number of laboratories. Negative-wave flashovers are inherently more erratic than positive-wave flashovers, with more chance of this deviation being exceeded. In front-of-wave and volt-time curve testing somewhat greater discrepancies may be expected. This eight per cent deviation was derived from intercomparison of laboratory results and is not to be considered as a tolerance in acceptance tests.

III—Progress Report and Additional Recommendations

(a). Wet Flashover Tests for Insulators and Bushings

This problem is concerned principally with present differences in recommended precipitations and water resistances, wherein the Insulator Standards No. 41 calls for 0.2 inch per minute and a water resistance of from 15,200 to 20,300 ohms per centimeter cube, and Oil Circuit Breaker Standards No. 19 calls for 0.1 inch per minute with a water resistivity of 12,000 ohms per centimeter cube.

The committee examined submitted data, sponsored tests in several laboratories, and studied the question in considerable detail deciding finally that a single standard of precipitation rate should be used and proposed 0.2 inch per minute. A resistance of 15,200 to 20,300 ohms per cm cube (6,000 to 8,000 ohms per inch cube) is also proposed. It was found easier to control the precipitation when the higher rate was used. As far as natural rain conditions are concerned, there appears to be no preference between 0.1 and 0.2 inch per minute. The range of 6,000 to 8,000 ohms per inch cube resistivity involves a correction on one type of apparatus from 0.98 to 1.02. The correction may be disregarded when tests are made within this range of resistivity. Tests may be made outside this range of resistivity provided recognized resistivity correction factors are available. In such cases the resistivity correction factors should be specified in the standards applying to each particular type of apparatus.

Attention was also given to details of wet test arrangements and the following recommended.

1. A 45-degree directional angle for the spray
2. A one-minute wetting before voltage application
3. A pressure range for the water of 35 to 60 pounds
4. All precipitation measurements taken with the test specimen in place
5. A 6-inch to 12-inch diameter measuring vessel
6. Precipitation measured in three places and a variation of from 0.24 to 0.16 inch per minute allowed throughout the string length
7. The voltage applied at a rate requiring at least ten seconds to flashover

The committee recommendations are being considered for a revision of the insulator test standard which is now in process of preparation by the AIEE.

(b). Flashover of Switch and Bus Insulators

Data submitted by several laboratories on outdoor switch and bus insulators have been assembled and are being prepared for submission to the joint committee.

(c). Critical (Minimum) Flashover Voltages for an Impulse

In response to requests from several sources, this committee took action on the use of the word "minimum" in the definition of this quantity as it now appears in ASA Definition Standards 35.90.125. The committee considered a great number of substitutes and finally reached agreement on the use of "critical flashover voltage for an impulse". This term is therefore recommended for immediate use and for substitution in the ASA definitions. The following definition is also recommended:

"The critical flashover voltage of a test specimen under an impulse of given wave shape and polarity is the crest value of that impulse when its amplitude is adjusted to cause flashover on 50 per cent of the applications."

(d). Impulse Volt-Time Curves

In an effort to extend the co-ordination between laboratories on impulse tests obtained on rod gaps and insulators for the critical flashover voltage, volt-time curves from several laboratories on rod gaps, with the 1.5x40 wave, back to two microseconds have been compared. A wide disagreement which increased as the times became shorter was discovered. This disagreement reached ±20 per cent from the average of the laboratory curves for a single test at two microseconds. The disagreement has been shown to be partly caused by wave-shape differences particularly on the front. It has, therefore, been necessary to consider the basic problem of accurate control of impulse wave shapes. Additional tests and exchange of data are on schedule.

(e). Front of Wave Testing

This subject has been given attention by the committee and again differences between laboratories immediately involved consideration of means of production and accurate measurement of wave fronts. The problems here overlap those for item *d* above, "Impulse Volt-Time Curves," involving still more difficult problems of short-time testing.

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[*] Note: When rated withstand test voltages are specified definite test procedure for demonstration purposes should also be specified. In behalf of uniformity it is urged that where possible the procedure recommended in sections II and III of this paper be adopted.

(f). IMPULSE WITHSTAND TESTS

The committee was requested to consider methods of making withstand tests and accordingly has exchanged information pertaining to definitions, test methods, and values concerned. Active exchange of data is continuing and a test procedure is suggested for cases where other standards do not apply. The specific test should be covered in each apparatus standard but it is recommended that this suggested procedure be adopted where possible.

Specifications for Rated Withstand Impulse Tests

1. *Purpose of Rated Withstand Tests.* Withstand tests are made to determine that dielectrics will be capable of withstanding the rated withstand test voltage without a disruptive discharge on the test specimen.

(*a*). For insulator types on which disruptive discharge takes the form of a flashover in air, the rated withstand test voltage is given for standard atmospheric conditions and the test voltage to be applied shall be adjusted as outlined in paragraph 5.

(*b*). For insulator types in which disruptive discharge occurs only as a breakdown of the solid or liquid dielectric, atmospheric corrections are not applied.

2. *Specimen Mounting.* The specimen shall be mounted for test in accordance with the requirements of the apparatus code applying to the specimen (for example, for bus insulators NEMA Publication 35-28 for 60-cycle flashover tests or new AIEE Standards No. 41 when accepted).

3. *Impulse Wave Shape and Polarity.* The wave shape, tolerances, and polarity shall be as specified by the apparatus code applying to the specimen. In general, the polarity shall be that which produces the lower critical withstand voltage on the specimen.

4. *Voltage Measurement.* The amplitude of the applied impulse waves shall be measured by the methods approved in the latest revision of AIEE Standards No. 4.

5. *Correction for Atmospheric Conditions.* For the conditions indicated in 1*a*, the rated withstand voltage shall be corrected with air density and humidity correction factors applicable to the test specimen such that:

$$V = RWV \times \frac{D}{H}$$

where

V = amplitude in kilovolts of the test wave
RWV = rated withstand voltage
D = relative air density
H = humidity correction factor applying to the test specimen

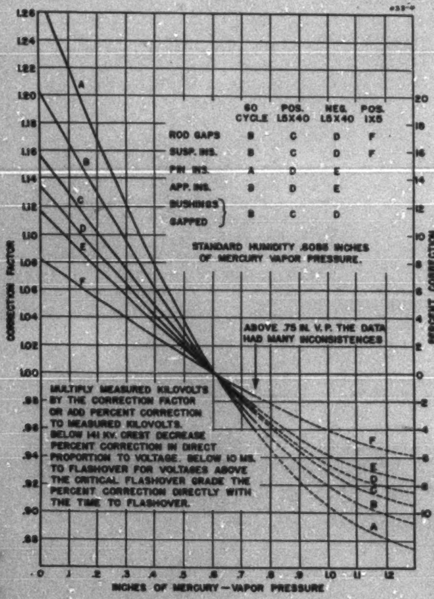

Figure 4. Humidity correction factors for flashover voltages of gaps, insulators, and bushings, based on average data from several laboratories

6. *Method of Test.* Three consecutive impulses shall be applied to the test specimen. The crest voltage of each shall not be less than the rated withstand voltage properly corrected (sections 1 and 5). If a disruptive discharge does not occur, the specimen shall be considered as having met the test.

If two or three of the applied impulse waves cause disruptive discharge, the test specimen shall be considered as having failed the test. If one of the applied impulses cause disruptive discharge,* three additional impulses shall be applied. If disruptive discharge does not occur, the specimen shall be considered as having met the test.

(g). SPHERE GAP AND CATHODE-RAY OSCILLOGRAPH MEASUREMENT COMPARISONS

Several laboratories have submitted comparative data on sphere-gap and cathode-ray-oscillograph measurements of crest voltages based on the criterion for measurement accuracy recommended in AIEE Standards No. 4 (revision of December 1937). Agreement is well within the five per cent stipulated in all cases and there appears to be no tendency for either method to give consistently higher values than the other.

(h). INTERNATIONAL ELECTROTECHNICAL COMMISSION PUBLICATION No. 60; GENERAL SPECIFICATIONS FOR IMPULSE VOLTAGE TESTING

Committee consideration has been given to this standard with the point in view of judging the value of such a general standard for equipment not now covered in particular apparatus or materials recommendations or codes. In the United States the trend to date has been to include all impulse testing recommendations in three places as follows:

1. All definitions of terms in the American Standards Association definitions.
2. All recommendations on measurements in AIEE Standards No. 4, Measurement of Test Voltage in Dielectric Tests.
3. All recommendations as to waves to be applied and circumstances peculiar to their application to be included in the particular apparatus or material codes.

This trend has been a healthy one and, rather than develop a new general standard, it is suggested that section II of this paper be used as an appendix to existing standards.

(i). CORRECTION FACTORS

Attention has been given wherever flashover results were being considered, to necessary corrections for relative air density and humidity. In the previous

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* For solid dielectrics disruptive discharge results in permanent damage and the ruptured dielectric must be replaced.

Table I

Time to Flashover	Measured Kilovolts	Relative Air Density, Divide by	Humidity, Multiply by	Corrected Kilovolts
5	108	0.95	1.077	122
4	108	0.95	1.077	122
3	113	0.95	1.023	122
2	126	0.95	1.015	135
1	158	0.95	1.007	168
0.5	208	0.95	1.004	220

report of the committee humidity correction factors for 60-cycle and positive-wave impulse flashover of rod gap and suspension line insulators were submittted. Since then this work has been extended.

The dielectric strength of air is affected by its temperature and pressure. Also flashover voltages for nonhomogeneous electric fields vary with the absolute humidity of the air. Consequently, the following standard air conditions have been adopted:

Temperature	25 deg C (77 deg F)
Barometric pressure	760 mm (29.92 in.)
Humidity (vapor pressure)	15.45 mm (0.6085 in.)

Correction factors have been established and flashover voltages should be referred to standard air conditions. Similarly, in withstand tests, where air insulation is involved, corrections must be made for the air conditions at the time of the test. Where tests are made at high altitudes the barometric pressure correction automatically compensates for altitude.

1. *Relative Air Density.* The temperature and pressure are combined into one unit called relative air density, which is unity at 25 degrees centigrade and 760 millimeters pressure. The relative air density for any other condition is given by the formula

$$\text{r.a.d.} = \frac{0.392B}{273+T}$$

where

B is barometric reading in millimeters
T is temperature in degrees centigrade

The flashover voltage of disruptive discharges through air is corrected by using the same correction irrespective of frequency of the voltage causing flashover.

2. *Humidity.* The humidity correction varies for different types of apparatus and for different forms and polarities of test voltage. Corrections for a number of different conditions have been evaluated by actual tests over a wide range of absolute humidity and are given in figure 4. The corrections are given both as a correction factor (multiplier) and as a per cent correction (to be added). The latter is used in revising the amount of correction applied at low voltages and at short times. If, for a particular test, none of the standard correction curves applies, the one which most nearly approximates the conditions of wave form, polarity, and type of apparatus should be used.

CORRECTION PROCEDURE

(*a*). *Full Wave Test and Critical Flashover Voltages.* The measured test voltage is divided by the relative air density and multiplied by the humidity correction factor to give the equivalent voltage for standard air conditions. If the measured voltage is less than 141 kilovolts crest, the humidity per cent correction should be decreased in proportion for insulators and rod gaps.

$$H^1 = H\frac{\text{kv}}{141}$$

where H is the per cent correction from figure 4. The humidity correction factor is then $\frac{100+H^1}{100}$.

(*b*). *Front-of-Wave Tests and Volt-Time Curve Voltages.* In correcting volt-time curves to standard air conditions the following standard procedure should be followed:

1. Use the full relative air-density correction at all points on the volt-time curve. In practice, divide voltage values by the relative air density, which corrects to standard temperature and barometric pressure.

2. Use the humidity correction factors for different waves and apparatus, as given in figure 4.

(*a*). When the critical flashover voltage is less than 141 kv crest, grade the per cent correction directly with the voltage.

(*b*). For times to flashover less than ten microseconds, when the corresponding voltage value exceeds the critical flashover voltage, grade the per cent correction directly with the time to flashover.

In practice, multiply the critical flashover voltage values and all voltage values for times greater than ten microseconds by the humidity correction factor. For voltages exceeding the critical flashover voltage at times less than ten microseconds multiply the humidity per cent correction by a factor equal to the corresponding time to flashover divided by ten. The humidity correction factor used as a multiplier for short times then becomes 100, plus this reduced per cent correction divided by 100. When the transition point is considerably less than ten microseconds, judgment is necessary to maintain a smooth curve. When the critical flashover voltage is less than 141 kv crest, decrease the per cent correction in direct proportion to the voltage.

EXAMPLE

Assume the following volt-time curve for a 1.5x40 positive wave obtained on a rod gap at relative air density 0.95 and at a vapor pressure of 0.2 inch.

Time to Flashover (Microseconds)	Kilovolts
5	108
4	108
3	113
2	126
1	158
0.5	208

For a rod gap at 0.2 inch humidity, the correction factor is 1.101 or the per cent correction is 10.1 from figure 4.

(*a*). Correct for a voltage below 141 kv. For a critical flashover voltage of 108 the per cent correction becomes $108 \times 10.1/141 = 7.7$

(*b*). Correct for the time to flashover. At two microseconds the per cent correction is $2 \times 7.7/10 = 1.54$ or the correction factor is 1.0154. Similarly for times less than four microseconds in this case. The corrections for the curve are then as shown in table I.

The corrected kilovolts give the volt-time curve for standard air conditions.

All corrections given are subject to revision from time to time, as more exact test data become available.

Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Transient Starting Torques in Induction Motors

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Synopsis: A transient fundamental-frequency torque is shown to occur in the starting of all induction motors. For full-voltage starting on a general-purpose squirrel-cage motor, this torque may be as high as two or three times the pull-out torque. A consideration of such torques is particularly advisable for cases where induction motors are started and stopped frequently or continuously, since under such conditions mechanical failure of the motor or associated parts, gearing, couplings, etc., may occur if the stresses exceed the endurance limit of the material.

A method of analysis is given for calculating the transient electrical torques acting in the case of a locked rotor; the results obtained by means of this analysis are compared with test data obtained on a special locked-rotor test setup. A method is also given for calculating shaft torque from the electrical torque in the usual application. Test results also indicate that this method is satisfactory for practical use.

FOR many years engineers have used the average starting torque and pull-out torques of induction motors as the basis for mechanical design of shafts and couplings, not always realizing that high alternating torques existed at the moment of starting. So far as the authors are aware no one has published any practical analysis, or even a discussion of this problem.

The analysis and tests given in this paper show that when a motor is thrown on the line by the sudden closing of a switch, the contacts of which close simultaneously, a fundamental-frequency electrical torque which may be several times the pull-out torque, will be developed for the first few cycles. If the closing of the switch contacts of the several phases is not simultaneous, this alternating torque may be higher, and in the worst case of 90 electrical degrees lag, about 40 per cent higher alternating torques will exist.

The actual shaft and coupling torque will depend on the flexibility of the shaft and coupling and the inertia of the load and the motor. It is apparent that if the natural frequency of the mechanical system consisting of rotor, coupling, and load happens to be quite near the current supply frequency, the peak shaft torques may even exceed the electrical torque due to the resonance effect, particularly for high load inertias. Also, if the coupling has backlash or a nonlinear torque-angle characteristic, an increase in torque may be had due to impact effects. Where the driven-load inertia is of the same order of magnitude or smaller than the motor inertia, a reduction in shaft torque is to be expected.

The fact that trouble from this source has been encountered only in exceptional instances may probably be explained as follows: (1) Usually the torsional natural frequency of the system is well below the current supply frequency so that most of the alternating torque is absorbed by the inertia of the rotor and is not transmitted to the shaft; (2) in most applications, starts and stops are relatively few so that a fatigue condition due to repeated loadings does not exist. Consequently, for such conditions, transient torques several times the endurance limit for indefinitely repeated loading may be absorbed without harm. Another way of explaining this is that the endurance curve for the shaft material (with stress concentration present) rises steeply as the number of cycles of repeated stress is reduced; (3) ample safety factors in shaft design based on experience are normally used; and (4) in most applications, the driven-load inertia is of the same order of magnitude or smaller than the motor inertia.

The mathematical analysis is carried out for the case of the locked rotor and yields the maximum torque which will be obtained at full voltage. For the case of reduced-voltage starting with an autotransformer or star-delta connection some flux may remain in the motor at the instant full voltage is applied with the result that even higher torques may occur. These latter conditions are, however, not considered in the present paper.

Electrical Torques

Before attempting a mathematical analysis of the problem, it is desirable to develop a physical picture of what is going on inside the machine under these transient conditions. It is simpler to think in terms of a two-phase machine, since the two windings are in space quadrature and have no mutual inductance. It will be shown in the appendix that the three-phase currents of a three-phase machine can be resolved into components acting on two quadrature axes (α and β).

In dealing with the transient torques in a machine, probably the most useful form of analysis is based on the difference in the products of fundamental current on each axis and fundamental air-gap flux on the axis in quadrature. In per unit notation[1]

$$T = i_{1\alpha} B_\beta - i_{1\beta} B_\alpha$$

or if the magnetizing current i_m on each axis is expressed in per unit notation, *the unit value being the current required to produce a flux sufficient to induce rated voltage at no load*, then:

$$T = i_{1\alpha} i_{m\beta} - i_{1\beta} i_{m\alpha} \qquad (1)$$

The sudden application of an alternating voltage to a reactive circuit will produce a transient asymmetrical flux and current depending on the point on the voltage wave at which the switch is closed.

The fundamental-frequency transient torque results from the reaction of the asymmetrical flux on one axis with the

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[1] For all numbered references, see list at end of paper.

alternating current on the other, and also to the reaction of the asymmetrical current on one axis acting on the alternating flux in the other. The asymmetrical flux and the associated fundamental torque decay slowly, since these depend on the magnetizing inductance. The asymmetrical current and its associated fundamental torque decay rapidly since these depend on the leakage inductances. The torque components due to asymmetrical current and to asymmetrical flux are initially equal and of opposite sign.

The mathematical analysis is given in appendix *B* for the case of a locked rotor. The actual case of the machine coming up to speed becomes much more involved. However, the peak of this alternating shaft torque (except where exact resonance is approached) comes within the first few cycles and in this time the rotor will not have attained more than a few per cent speed and the effects of rotation on the transient currents and flux are negligible.

The results of this analysis of transient locked rotor torques are given in equation 11 in terms of the starting torque and locked power factor, for the case of simultaneous application of voltage to all phases.

For small machines, or larger high-torque machines, where the locked power factor ($\cos\phi$) is not less than 0.3, the component of alternating torque due to the asymmetrical current dies out so rapidly as to be negligible and the peak electrical torque may be obtained by neglecting the terms involving λ_2, which gives:

$$T_{1m}=T_s\left(1+\frac{1}{\cos\phi}\right) \qquad (2)$$

For a large motor with low locked power factor the component due to the asymmetrical current decays more slowly. The resultant alternating torque starts at zero and builds up to a maximum in a few cycles, and the peak electrical torque will still be approximately that given by equation 2.

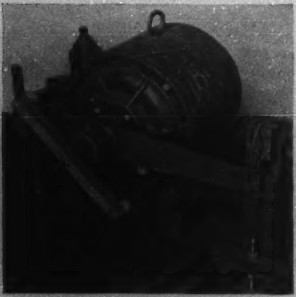

Figure 1. Test setup for measuring electrical torques

These equations apply only when there is simultaneous voltage application on all three phases. Such a condition is often difficult to obtain with actual switches or contactors. For this reason a second analysis was carried out by assuming that the switch was closed on one phase of the equivalent two-phase machine 90 electrical degrees (or 0.004 second at 60 cycles) later than on the other phase. On the three-phase machine this would mean that the third phase was closed 90 electrical degrees after the closure of the second phase. The results of this analysis are given in equation 12. Again neglecting terms involving λ_2 the peak electrical torque becomes:

$$T_{1m}=T_s\left(1+\frac{\sqrt{2}}{\cos\phi}\right) \qquad (3)$$

Values of peak torque found for such conditions are around 20 to 40 per cent higher than those obtained on the assumption of simultaneous voltage application.

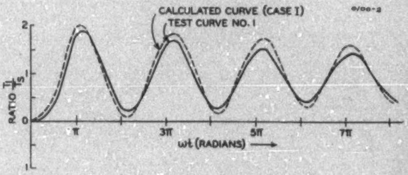

Figure 2. Comparison of test and calculated torque curves for typical high-torque induction motor

A physical explanation of the increased torque due to closing the last phase 90 degrees later may be had, by considering that this may result in maximum asymmetrical flux in each of the two axes which gives a resultant asymmetrical flux $\sqrt{2}$ times the maximum value on one axis.

Tests to Determine Electrical Torques

In order to check up on the theoretical work, a special locked-rotor test arrangement (figure 1) so designed as to record transient torques was used. Essentially, it consists of an arm attached to the motor shaft and held at its end by a thin tool-steel bar. Under the action of pulsating torques, this tool-steel bar is stretched or contracted, these transient movements being recorded on an oscillograph by means of a magnetic strain gauge.[3] By using a tool-steel bar, a fairly high natural frequency, relative to the current supply frequency, was obtained. The relation between rotor torque and oscillograph-beam deflection is established by a static calibration.

In figure 2 a typical curve of transient starting torque obtained on a high-resistance induction motor is shown by the full lines. It may be seen that this consists essentially of a pulsating component at the current supply frequency which dies out after a time. For comparison, a calculated curve obtained from equation 11 is also shown.

A summary of ratios of maximum torque T_m to nominal starting torque T_s obtained on various rotors at different frequencies (so that $\cos\phi$ varied from about 0.6 to 0.95) is shown in figure 3. It may be seen that while there is considerable scatter in the results, most of the test points occur between the limits of the curves defined by the approximate equations 2 and 3, with the latter equation yielding an approximation to the upper limit.

Transient Shaft Torques

GENERAL METHOD—LINEAR COUPLINGS

The preceding discussion has been primarily concerned with the transient electrical torques acting on the rotor at the instant of starting. In most cases, however, the designer is mainly concerned with the transient shaft torques, since on these depends the design of the mechanical parts. As mentioned before, these shaft torques depend not only on the electrical torque, but also on the other characteristics of the drive including rotor and load inertias, flexibility of shaft-coupling system, and in certain cases, the shape of the torque-angle characteristic of the coupling.

In many cases of practical interest, an induction motor is coupled to a heavy inertia load by means of a flexible coupling. An example of such construction is the roll-table drive used in continuous strip mills. In such cases the system may usually be represented for purposes of analysis by a single degree-of-freedom system consisting of a mass, representing the motor, connected by a spring, representing the coupling and shaft flexibility, to a rigid body. This analysis will hold approximately where the equivalent driven-load inertia is large compared to that of the motor. As will be shown later, the results for this case may also be used when the load inertia is finite.

If we neglect damping, the known differential equation of motion for such a system, assuming a coupling with a linear torque-angle characteristic is

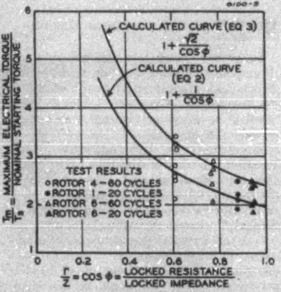

Figure 3. Comparison of test and calculated values of ratios between peak electrical torque and starting torque

$$I_m \frac{d^2\theta}{dt^2}+k\theta=F(t) \qquad (4)$$

The shaft torque may be determined by introducing $F(t)$ as given by equations 11 or 12 into equation 4 and integrating, the integration constants being determined from the initial conditions which require that when $t=0$, $\theta=0$, and $d\theta/dt=0$. Because of the rather complicated expression for $F(t)$, such an integration becomes very laborious and for this reason the following alternative method was used.

By using the principle of superposition, it may be shown that if damping is neglected, the angular displacement for the single degree of freedom system under consideration is given by:[3]

$$\theta=\frac{1}{\omega_n}\int^{t_1}\frac{F(t)}{I_m}\sin\ \omega_n(t_1-t)dt \qquad (5)$$

Application to High-Resistance Rotors

Unless the damping is unusually high, it is justifiable where high-resistance rotors are involved to neglect damping in practical systems where the natural frequency of the system is much less than the current supply frequency since in such cases the peak torque will occur within the first cycle of oscillation at the natural frequency.*

Assuming as before that the magnetizing reactance is very large compared to the rotor or stator resistance, the decrement factor λ_1, will be small compared to ω. Hence, the term $\epsilon^{-\lambda_1 t}$ may be taken as unity in the expression for $F(t)$, since during the first few cycles of applied voltage during which the maximum shaft torque occurs, this term will differ but little from unity. With this simplification, by integrating equation 5 the expression for shaft torque takes the form:

$$T=T_s\{1+\beta_1\epsilon^{-\lambda_2 t}+\beta_2\epsilon^{-\lambda_2 t}\sin\ (\omega t+\gamma_2)+\beta_3\times \sin\ (\omega_n t+\gamma_3)+\beta_4\sin\ (\omega t+\gamma_4)\} \qquad (6)$$

where β_1, β_2, β_3, β_4, γ_2, γ_3, γ_4, depend on ω/ω_n and ϕ.

For typical high-resistance rotors ($\cos\phi>0.3$), it may be assumed that the term $\epsilon^{-\lambda_2 t}$ dies out very rapidly during the first cycle of applied voltage, so that it may be neglected as far as any contribution to the peak torque is concerned. (The results, however, should not be applied when low-resistance rotors are being considered.) In addition, in practical cases where ω_n is not over say $\omega/2$, it may be assumed as a first approximation, that the phase angles γ_3 and γ_4 are such that the torque components β_3 and β_4 add directly. (This will yield results somewhat on the safe side, the closeness of the approximation being greater for smaller ratios of ω_n/ω.) With these simplifications, the peak shaft torque (for high-resistance rotors) becomes:

$$T_m=T_s\{1+\beta_3+\beta_4\} \qquad (7)$$

Values of the ratio T_m/T_s obtained by using this method for various values of $\cos\phi$ are plotted in figure 4 for simultaneous voltage application on both axes and in figure 5 for voltage application on the β axis 90 degrees later than on the α axis. It may be seen that the latter assumption yields definitely higher shaft torques. It should also be noted that peak transient shaft torques as high as four to five times the nominal starting torque may occur for cases where $\cos\phi=0.5$ and $\omega_n/\omega=0.5$. In utilizing these curves, it should be borne in mind that because of the assumptions made, the ratios T_m/T_s may be somewhat overestimated for the larger ratios of ω_n/ω.

An alternative method for obtaining an approximate solution is to take $\epsilon^{-\lambda_2 t}=0$ and $\epsilon^{-\lambda_1 t}=1$, as before, in the expressions for torque given by equation 11 or 12. These approximate expressions are then substituted for $F(t)$ in the differential equation (4) and the integration carried out. Assuming as before that the torque components at the natural frequency and at the current supply frequency add directly, peak torques somewhat lower than those given by the curves of figures 4 and 5 are obtained.

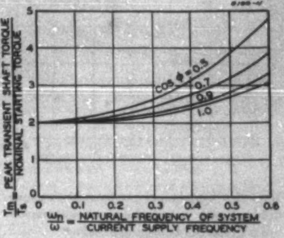

Figure 4. Variation of maximum shaft torque with torsional natural frequency—simultaneous voltage application

Infinite load inertia and simultaneous voltage application in all phases assumed

The previous discussion deals only with the case where the equivalent load inertia is infinite compared to that of the motor rotor. Where this is not the case, that is, if the load inertia is of the same order of magnitude as the motor inertia (a condition which frequently occurs in practice), a comparison of the differential equations for the two cases shows that the curves of figures 4 and 5 may still be used provided that the natural frequency is determined by using an equivalent moment of inertia

$$I_e=\frac{I_m I_r}{I_m+I_r}$$

instead of I_m, and provided that the peak torque, thus found, is reduced in the ratio $I_r/(I_m+I_r)$.

Where the torque-angle characteristic of the coupling is nonlinear (as it is in many practical couplings) the differential equation (equation 4) will still apply if the constant k is replaced by a function $f(\theta)$ which represents the torque-angle characteristic of the coupling. Usually an analytical solution of this

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* When operating with a natural frequency not far from the supply frequency, damping should be considered for an accurate solution.

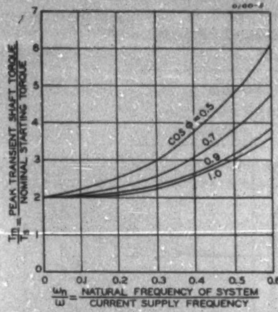

Figure 5. Variation of maximum shaft torque with torsional natural frequency—nonsimultaneous voltage application

Infinite load inertia and voltage application β axis 90 electrical degrees later than on α axis

modified equation is impractical and in such cases a numerical solution* has given good results when compared with actual test values.

The peak shaft torques obtained by using the methods of calculation outlined herein have been compared with the results of actual torque measurements on a typical high-resistance induction motor coupled to a heavy inertia load by means of a flexible coupling. The torques were measured by means of a magnetic strain gauge so mounted as to measure shaft twist, the results being recorded on an oscillograph. The tests were carried out by using a number of different makes of flexible couplings. Although a complete discussion of these results is beyond the scope of the present paper, it may be mentioned that the results indicate that the methods of calculation developed herein for high-resistance rotors are satisfactory for practical application.

LOW-RESISTANCE ROTORS

For low-resistance rotors the $\epsilon^{-\lambda_2 t}$ term in equations 11 or 12 of appendix *B* cannot be neglected. In the first instant, $\epsilon^{-\lambda_2 t}$ is unity and the two components of alternating torque are found to cancel out. After a few cycles $\epsilon^{-\lambda_2 t}$ has decreased considerably and the alternating electrical torque approaches the value obtained from equations 11 and 12 by putting $\lambda_2=0$. Because the alternating torque builds up slowly an approximation to the peak torque should be had by adding directly (1) the shaft torque component at the natural frequency (produced by the impact effect of the average electrical torque applied in the first instant after starting) and (2) the torque component at fundamental frequency due to the alternating electrical torque. Where the load inertia is large, the first of these components will be about twice the average electrical torque in the first instant after starting. Where the ratio ω_n/ω is not low, it would appear that a rough estimate of this average torque could be obtained from equations 11 and 12 by taking $\epsilon^{-\lambda_2 t}$ as unity and neglecting the trigonometric terms. This gives an average electrical torque of $2T_s$ and $(1+\pi/2)T_s$ for the simultaneous and nonsimultaneous cases respectively, with resulting peak shaft torques of $4T_s$ and $(2+\pi)T_s$ due to the impact effect for the two cases. It should be noted that these values may be somewhat too high for the lower values of ω_n/ω and for very low values (say 0.1 or less) the peak impact torque may be taken as $2T_s$. To this must be added the fundamental-frequency shaft torque component due to the alternating electrical torque which is $\pm 1/(\omega^2/\omega_n^2-1)$ cos ϕ or $\pm\sqrt{2}/(\omega^2/\div\omega_n^2-1)\cos\phi$ for the simultaneous and nonsimultaneous cases, respectively, the negative sign being used if $\omega<\omega_n$. For a more accurate solution in any particular case equations 11 or 12 may be taken as the expressions for $F(t)$ in equation 5 and the integration carried out. The fact that mechanical damping is neglected when figuring the torques by these methods means that the calculated values of shaft torque may be somewhat too high particularly where the natural frequency is not far from the current supply frequency. Where the load inertia is not large compared to the motor inertia, these values should also be reduced in the ratio $I_r/(I_m+I_r)$ as discussed previously.

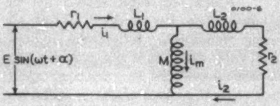

Figure 6. Equivalent circuit used in analysis

Conclusions

1. All induction motors started by sudden application of voltage will have transient fundamental-frequency electrical torques. If started at full voltage the peak electrical torque may be two times the pull-out torque or more.

2. The transient electrical torques can be calculated by equations 11 and 12. For a high-torque motor the peak electrical torque is given approximately by equations 3 and 4.

3. The test results (figure 3) show that the method of analysis is sufficiently accurate.

4. Mechanical torques for the common case where the torsional natural frequency of the mechanical system is well below the supply frequency are given by curves of figures 4 and 5 for high-torque motors. A method is also outlined for motors with low locked power factor.

Appendix A

The Equivalent Two-Phase Machine

Park[4] has shown how the currents of a three-phase machine may be resolved into direct- and quadrature-axis components on the two axes of the rotor. More recently Stanley[5] has given a somewhat more general analysis by resolving the three-phase currents along any two quadrature axes. Examination of the resulting equations of this method shows that they are exactly the same as for a simple two-phase machine, except for zero-sequence currents (that is, current flowing in the neutral).

One can arrive at this same conclusion by simple physical reasoning. The current in one phase may be thought of as dividing between the other two phases and this will be taken as one of the two-phase components. The second two-phase component will be the difference in the currents of the other two phases. So long as there are no neutral currents, the resultants of these two-phase components will be equal to the real three-phase current in any phase.

In order to establish the relation between the equivalent two-phase components, take *I* and *E* as the line currents and phase-to-neutral voltage of the three-phase machine. Then *I* and $3/2E$ are the current and voltage of the first phase of the equivalent two-phase machine, and $\sqrt{3}/2$ and $\sqrt{3}E$ are the current and voltage of the second phase. Either of these two phases could be used as the reference, but it is more convenient to use the first. For this case the equivalent two-phase current is the same as the three-phase current, but the two-phase voltage and the inductance and resistance values will be taken as 3/2 times the three-phase line-to-neutral values.

The locked two-phase machine consists essentially of two independent transformers and the usual transformer equivalent circuit (figure 6) can be used accurately to represent current and voltage relations on each axis. The inductance coefficients L_1 and L_2 are the leakage inductances of the primary and secondary, and are numerically equal to the ohms reactance divided by $2\pi f$. *M* is the magnetizing inductance.

The secondary constants L_2 and r_2 are converted to the primary as is customary in all induction-motor calculations. Actually the secondary reactance and resistance are in most cases affected by eddy currents. The locked values of these constants will be used because they are correct for the fundamental-frequency currents, and the error is only in the decrement.

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* See Timoshenko, reference 3, page 126, for an example of such a solution applied to a similar case.

Appendix B

Calculation of Transient Electrical Torques

In line with the discussion of appendix A the induction-motor winding is assumed divided into α and β components with axes at 90 degrees. Each component is represented by the equivalent circuit of figure 6. The voltage applied to the circuit is given in the general form $E \sin(\omega t+\alpha)$ and the parameter α may be so chosen for each axis that the voltages acting on the α and β axes will be 90 degrees apart. Referring to figure 6, assuming constant values of L_1, L_2, and M, the following differential equations hold:

$$(r_1+pL_1)i_1+pMi_m=E\sin(\omega t+\alpha)$$

$$(r_1+pL_1)i_1+(r_2+pL_2)i_2=E\sin(\omega t+\alpha) \qquad (8)$$

$$i_1=i_2+i_m$$

The solution of these equations may be obtained either by using operational methods or by using the ordinary methods of solving differential equations, but the resulting expressions become rather cumbersome if all the terms are included. However, a simplification is possible since in practical motors the stator and rotor leakage inductances L_1 and L_2 are very small in comparison with the mutual inductance M, and hence may be neglected in certain expressions.*

The following boundary conditions hold:

at time $t=0$, $i_1=0$, $i_m=0$

and since no current is present when $t=0$

$$L_1pi_1+Mpi_m=E\sin\alpha$$

$$L_2pi_2-Mpi_m=0$$

In this manner we find:

$$i_1=I'\{(\sin\phi\cos\alpha-\cos\phi\sin\alpha)\epsilon^{-\lambda_2 t}+\sin(\omega t+\alpha-\phi)\} \qquad (9)$$

$$i_m=I_m'[\epsilon^{-\lambda_1 t}\cos\alpha-\cos(\omega t+\alpha)] \qquad (10)$$

The instantaneous electrical torque is obtained from equation 1. Two cases are assumed as follows:

Case I. Simultaneous application of voltage to both axes.

Case II. Application of voltage to β axis 90 degrees later than α axis to simulate effect of bouncing of switch contacts or irregularity in voltage application.

In practice it will be found almost impossible to apply voltage to all three phases at exactly the same instant; on the other hand the assumption of a 90-degree phase difference between voltage application to the two axes probably represents a fairly severe condition.

Case I—Simultaneous Application of Voltage to Both Axes

In this case we use equations 9 and 10 taking $\alpha=0$ for the α axis and $\alpha=90$ degrees for β axis. Thus $i_{1\alpha}$ and $i_{m\alpha}$ are found from these equations by taking $\alpha=0$; $i_{1\beta}$ and $i_{m\beta}$ by taking $\alpha=90$ degrees. Using the expressions for $i_{1\alpha}$, $i_{m\alpha}$, $i_{1\beta}$, $i_{m\beta}$ thus found in (1) we obtain for the torque:

$$T_1=T_s\left\{1+\epsilon^{-\lambda_2 t}-\frac{\epsilon^{-\lambda_2 t}\cos(\omega t+\phi)+\epsilon^{-\lambda_1 t}\cos(\omega t-\phi)}{\cos\phi}\right\} \qquad (11)$$

Case II—Application of Voltage to β Axis 90° Later Than on α Axis to Simulate Effect of Bouncing of Contacts or Irregularity in Voltage Application

In this case we again use equations 9 and 10 and assume that the switch is thrown on the β axis 90 degrees later than on the α axis. Let t = time measured from instant switch is thrown on the α axis and t_1 = time measured from instant switch is thrown on the β axis; then

$$t=t_1+\frac{\pi}{2\omega}$$

On the α axis we take $\alpha=0$ and $t=t_1+\pi/2\omega$ in equations 9 and 10 and thus obtain expressions for $i_{1\alpha}$ and $i_{m\alpha}$ in terms of t_1. The currents $i_1\beta$ and $i_{m\beta}$ on the β axis will be obtained from the same equations in this case by taking $\alpha=180$ degrees and $t=t_1$ to take into account the fact that there is a phase difference of 90 degrees between the voltages and a lag of 90 degrees between the time the switch is thrown on the two axes. Using these values of $i_{1\alpha}$, $i_{m\alpha}$, $i_{1\beta}$, and $i_{m\beta}$ in equation 1 the following expression for the electrical torque T_2 is obtained in terms of the time t_1:

$$T_2=T_s\left\{1+\tan\phi\left[(1-c)\epsilon^{-\lambda_2 t_1}+(\sqrt{1+c^2})\epsilon^{-\lambda_2 t_1}\sin(\omega t_1+\alpha_1)\right]+\frac{\sqrt{2}}{\cos\phi}\epsilon^{-\lambda_1 t_1}\sin\left(\omega t_1-\phi-\frac{\pi}{4}\right)\right\} \qquad (12)$$

where

$c=\epsilon^{-\frac{\lambda_2\pi}{2\omega}}$ and $\alpha_1=\tan^{-1}c$.

Nomenclature

$\cos\phi=\dfrac{r}{\sqrt{x^2+r^2}}$ = locked power factor

E = maximum value of applied voltage per phase

$f(\theta)$ = function representing torque-angle characteristic of shaft-coupling system

i_1, i_2 = stator and rotor phase currents

i_m = magnetizing current per phase

$i_{1\alpha}$, $i_{m\alpha}$ = stator and magnetizing currents on α axis

$i_{1\beta}$, $i_{m\beta}$ = stator and magnetizing currents on β axis

I' = peak value of steady-state locked stator current

I_m' = peak value of steady-state magnetizing current with rotor locked

I_m, I_r = moments of inertia of motor and driven load respectively

$I_e=I_mI_r/(I_m+I_r)$ = equivalent moment of inertia of rotor-coupling-load system

k = spring constant of shaft-coupling system

L_1, L_2 = stator and rotor leakage inductances per phase

M = mutual or magnetizing inductance

$p=d/dt$ = derivative operator

r_1, r_2 = stator and rotor resistances per phase

$r=r_1+r_2$

T_1 = electrical torque assuming simultaneous voltage application on α and β axes

T_{1m} = maximum electrical torque assuming simultaneous voltage application

T_2 = electrical torque assuming voltage application on β axis 90 degrees later than on α axis

T_{2m} = maximum electrical torque assuming voltage application on β axis 90 degrees later than on α axis

T_m = peak shaft torque or electrical torque

T_s = nominal starting torque

T = shaft torque

t = time

$\omega=2\pi$ times frequency of applied voltage

$\omega_n=2\pi$ times natural frequency of system consisting of motor rotor, coupling, and load

$x=x_1+x_2$

x_1, x_2 = stator and rotor leakage reactances per phase

x_m = magnetizing reactance

λ_1, λ_2 = decrement factors. $\lambda_1=\dfrac{\omega r_1r_2}{x_mr}$; $\lambda_2=\dfrac{\omega r}{x}$

θ = angular twist of shaft-coupling system between motor and load

β_1, β_2, β_3, β_4 = transient shaft-torque components

γ_2, γ_3, γ_4 = phase angles of corresponding transient shaft-torque components

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

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* An integration of these equations in which no terms were neglected has been carried out by E. H. Moss using numerical values for the constants M, L_1, and L_2 corresponding to an actual motor. This showed close agreement with the approximate method described.

A High-Speed Differential Relay for Generator Protection

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RECENT papers have shown that high-speed differential relaying is considerably complicated by the presence of the d-c transient in asymmetrical through fault currents. This is particularly true of generator differential relaying, since an asymmetrical fault near the terminals of a generator provides a more severe d-c transient than at any other point on the system. The effect of the d-c component is to produce a false differential current during a through fault because of dissimilar performance of the current transformers, even though they may nominally be duplicate. When this false differential current reaches tripping proportions with respect to the restraining current, even for as short a time as one cycle with a high-speed relay, the relay problem becomes one of properly discriminating between a false and a true differential current of tripping magnitude.

Means have been provided in the relay described in this paper to make the above discrimination. The solution has been worked out in accordance with two general principles. First, the principle of variable-ratio characteristics has been used. This principle was first described in a paper[1] presented at the 1939 winter convention, wherein it is shown that when the restraining current is large, corresponding to a heavy external fault, the sensitivity of the relay should be reduced to allow for greater shortcomings in current-transformer performance. Second, both theory and tests have shown that when current-transformer saturation occurs, the false differential current tends to be out of phase with the restraining current. Consequently, the new relay has been made less sensitive at the higher restraint values when the differential current is out of phase with the restraining current. The new relay is shown in figure 1, together with the necessary external transformer. The relay and transformer together form a complete three-phase unit.

Principle of Operation

In order to analyze the principle of operation of the relay, a simplified diagram, showing the essential parts for one phase only, is desirable. This is shown in figure 2. The main current transformers on each side of the generator are shown connected to terminals 1, 2, and 3 of the transformer element which feeds the relay. The principal currents from the secondaries of the current transformers in the leads of the generator flow only in the external transformer, as will be noted in the diagram. The transformer element develops auxiliary current and voltage quantities for actuation of the relay element shown in the bottom part of the diagram. The relay element has been shown very schematically. It is composed of a balanced-beam element wherein the contacts are restrained from closing by split-phase voltage windings acting on the rear end of the beam, while the operating-coil magnetic circuit acting on the front end of the beam tends to close the contacts. Inspection of the transformer element of this diagram will show that it consists of two transformer elements, both on the same magnetic circuit. Coils *A* and *B* on the center leg of the punchings form one of these transformers, which will be referred to as the sum coil transformer, wherein a voltage is developed in winding *B* in response to the restraining currents, I_1 and I_2. Since the voltage developed by winding *B* energizes split-phase restraining coils as shown, it is evident at once that the restraining force on the back end of the beam will never pass through zero.

A possible difference current, I_3, is shown flowing in the differential connection of the main current transformers in the diagram. In the transformer element, this current must flow through the two primary coils, C_1 and C_2, of the second transformer unit on the magnetic core, this unit being referred to as the difference coil transformer. The coils, D_1 and D_2, form the secondary windings of this second unit. They are connected in series to energize the operating coil of the relay element.

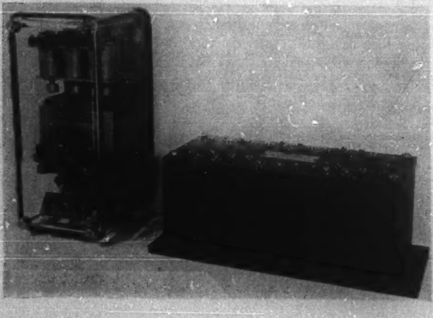

Figure 1 (left). Type HA ratio differential relay for generator protection

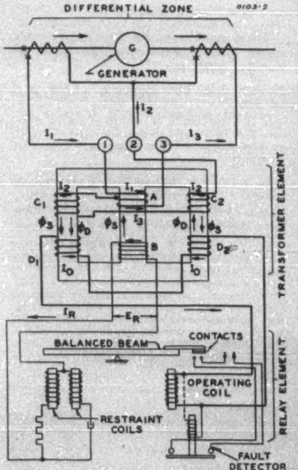

Figure 2. Schematic diagram of one relay element and auxiliary current transformer

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Paper 40-103, recommended by the AIEE committee on protective devices, and presented at the AIEE summer convention, Swampscott, Mass., June 24-28, 1940. Manuscript submitted November 8, 1939; made available for preprinting April 23, 1940.

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1. For numbered reference, see end of paper.

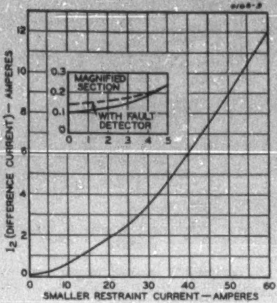


Figure 3. Typical operating characteristics of relay. Difference current, I_2, in phase with restraint current

Variable-Ratio Characteristics

Assuming in-phase currents, it will be seen from figure 2 that if the difference-coil current transformer is made to saturate to a greater extent at the higher currents than the sum-coil current transformer, and assuming linear relay characteristics, then it is obvious that a higher percentage of difference-coil current will be required to trip the relay contacts as the saturation of the difference-coil transformer is increased with respect to the sum-coil transformer. That is, as the restraint current is increased, the difference current to trip the relay will increase at a higher rate, thus resulting in the variable-ratio characteristic of figure 3. It will be noted from the curve that the minimum tripping current of the relay is 0.1 ampere in terms of the secondary, thus providing high sensitivity at no load on the machine. At 5 amperes restraint current, corresponding to full load, the difference current required to trip the relay is 0.25 ampere. This corresponds to a relay sensitivity of 5 per cent. From this point on, the variable-ratio characteristic of the relay is illustrated by the curve. For example, at 60 amperes restraint, a difference current to trip of 12 amperes represents 20 per cent unbalance. This indicates a decrease in sensitivity of the relay by a four-to-one range between normal and 12 times normal full-load current.

Phase-Angle Characteristics

Flux arrows have been drawn in the transformer element, figure 2, corresponding to the relative instantaneous direction of the currents, I_1, I_3, and I_2, as shown. The polarity of the connections of coils C_1, C_2, is such that ϕ_D, established by the difference current, I_2, flowing through coils, C_1 and C_2, circulates around the outer legs of the laminations. With symmetrical coil designs, there is no tendency for a portion of the flux, ϕ_D, to circulate through the middle leg of the transformer, since the two ends of this leg will be at the same magnetic potential with respect to ϕ_D. Thus, the flux, ϕ_D, established by the difference current, I_2, does not tend to establish directly a restraining-coil voltage, E_R, in coil B. The flux, ϕ_S, set up by the currents, I_1 and I_3, in coil A returning from top to bottom in the outer legs of the transformer as shown, has no tendency to induce operating-coil current in coils, D_1 and D_2, since, because of the polarity of connections of coils, D_1 and D_2, opposite voltages will be generated in these two coils by ϕ_S. From the above discussion, it is evident that if the magnetic material were of infinite permeability and never saturated regardless of the required flux density, the two transformer units, A/B and C_1C_2/D_1D_2 would have no effect upon each other and there would be no point to placing them both on the same magnetic structure. The fact that the magnetic material is not perfect, however, does introduce a mutual reaction between the two transformers, and this will be discussed.

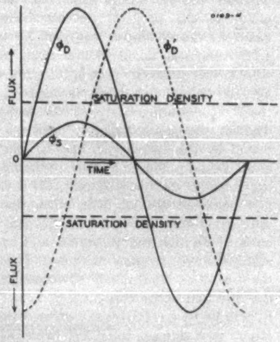


Figure 4. Flux waves in auxiliary current transformer

Assume that the currents I_1, I_3, and I_2, of figure 2 are associated with the flux waves of figure 4, these flux waves being required to generate sufficient voltage in the respective coils to circulate the currents, I_O and I_R, through the relay element. With I_2 in phase with I_1 and I_3, the power factor of the relay burden, both restraining and operating coils, may be so chosen that the fluxes, ϕ_S and ϕ_D, of figure 4 will be in phase as shown by the solid-line curves. If the quantities are so chosen, however, that the peak of the flux, ϕ_D, for the difference-current transformer, C_1C_2/D_1D_2, extends considerably into the zone of saturation, as shown in figure 4, then it will be noted that the peak flux, ϕ_S, for transformer A/B will be hard to obtain magnetically. That is, the return circuit for ϕ_S is the outer legs of the magnetic core in parallel, and these two outer legs are at that particular instant badly saturated by the difference transformer flux, ϕ_D. What this means is that a larger percentage of the sum-coil currents, I_1 and I_3, will go to magnetizing current, leaving a very small percentage to be transformed into restraining coil current, I_R, through the winding, B. (Casual inspection of figure 2 might indicate that the right-hand leg of the transformer punching might serve as a good return circuit for ϕ_S, since ϕ_S and ϕ_D are shown in opposite directions in this leg, thus reducing saturation and voiding the above argument. However, the argument is still valid, because at high saturation densities, there is not much difference between the amount of ampere turns required to reduce the flux density by a small increment and the amount of ampere turns to increase the flux density by the same small increment.) Thus, the solid-line flux, ϕ_S, of figure 4 is associated with a phase-angle condition in which it would be expected that the required difference current to trip the relay would be reduced because

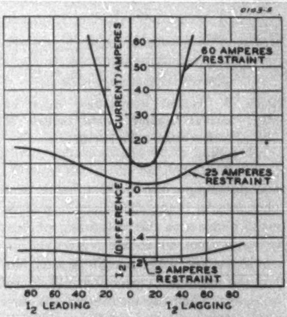


Figure 5. Typical phase-angle characteristics of relay

Phase angle in degrees of difference current with respect to smaller restraint current of a reduced efficiency of the sum-coil transformer. If now the phase angle of the difference-coil current, I_2, is changed by 90 degrees with respect to the restraining current, I_1 and I_3, so that the difference-coil flux, ϕ_D, is moved by 90 degrees lagging, as shown by the broken-line curve, ϕ_D', of figure 4, it will be noted that the peak of sum-coil flux, ϕ_S, occurs at the instant that the difference-coil flux is passing through zero. It would be expected that under this condition the sum-coil transformer would operate more efficiently than in the previously described case, since the peak of its flux, ϕ_S, is more easily obtained when its return circuit is not saturated. The above discussion indicates that since the difference-coil transformer, through its flux requirements, is caused to saturate the return circuit for the sum-coil transformer, and since this saturation effect varies throughout the cycle, then the mutual reaction between the two transformers is variable, depending upon the phase-angle relationship between the sum-coil currents and the difference-coil current. By suitably proportioning the power factor and magnitude of the restraining-coil and operating-coil burdens of the relay element in conjunction with the transformer design, it was possible to arrive at a design in which the combination is less sensitive to difference-coil currents when these difference-coil currents are substantially out of phase with the restraining-coil currents.

The phase-angle characteristics of the relay are illustrated in figure 5. The curve shows that at 60 amperes restraint, it is practically impossible to cause the relay to trip if the difference current is out of phase with the restraining current by more than 40 to 50 degrees. This provides a very large margin of safety in that the difference currents are likely to be highest when the restraining-coil current is the greatest, and they will also tend to be out of phase with the restraining-coil currents. At five amperes restraint, however, corresponding to full-load current, it should be noted that the phase-angle curve of the relay is substantially flat. This is desirable at load current, because there is no way of predicting just what the phase-angle relationship will be between a small internal fault current in a generator and the load current on the machine at the time.

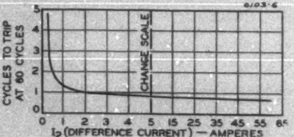


Figure 6. Typical current-time curve of relay. Smaller restraint current constant at five amperes (full load)

When considering the phase-angle curves, it should be remembered that those curves where the restraint current exceeds 5 amperes represent through short-circuit conditions. For this reason, there is no cause for alarm in the indication that, for example, a 40-ampere differential current at 60 degrees lagging would not trip the relay at 60 amperes restraint. It would be impossible to obtain an internal short-circuit of 40 amperes magnitude and maintain a through current of 60 amperes for an external fault. In other words, these curves, except for the 5-ampere restraint curve, apply only to through fault conditions, and are not applicable to internal fault conditions without proper interpretation of the effect of the sum-coil currents. For example, the 60-ampere restraint curve referred to was taken with a setup in which the sum-coil current directions agreed with those shown in figure 2, these representing an external fault. That is, the sum-coil ampere turns produced by the currents, I_1 and I_3, were additive and produced a positive restraining force on the relay element. If it is now assumed that a fault within the differential zone draws 60 amperes (secondary) through the generator, and an additional 60 amperes back-feed from the bus, it is immediately obvious that the direction of the two currents, I_1 and I_3, will be reversed with respect to each other, their ampere turns will cancel in the sum-coil transformer, and there will be no restraint. Hence, the curve does not apply. For another example, let it be assumed that for the same internal fault, the generator current, I_1, is 60 amperes, but that the back-feed current I_3 reversed, is increased to 180 amperes. Then 60 amperes in one-half of the sum coil winding will cancel the effect of 60 amperes of the 180 amperes in the other half, leaving a net effect of 120 amperes in half of the winding, equivalent in effect to 60 amperes flowing in one coil and out of the other, as shown in figure 2. In this case, then, the 60-ampere curve of figure 5 would apply, although it should be remembered that the difference-coil current would be definitely fixed as the sum of the 60- and 180-ampere currents. This sum could conceivably be somewhat out of phase with the net restraining-coil current, but not enough to affect positive relay operation. Referring back to the curve for 5 amperes restraint, figure 5, it is possible that a light internal fault might occur which would not disturb the normal through load current of 5 amperes to any great extent, hence, this curve is applicable for internal faults as stated.

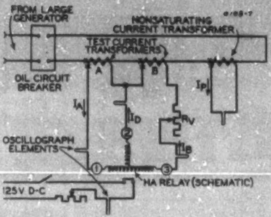


Figure 7. Schematic diagram of test connections

R_v represents a variable resistor used to produce more severe saturation in current transformer *B*

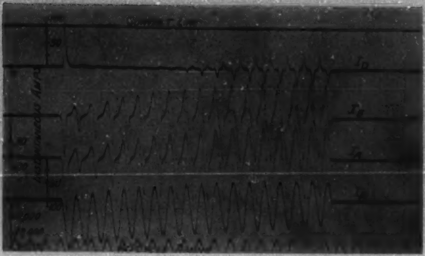
The time of operation of the relay is shown by the current-time curve of figure 6, wherein it is shown that the relay is a one-cycle relay.

Fault Detectors

The relay is provided with three small solenoid-type fault detectors, one for each of the elements, which may be used for those locations where the switchboard panel is subject to heavy jars or vibrations. At very light loads, the restraint occasioned by the load current may be negligible, and the extreme sensitivity of the differential element renders it subject to momentary closing of the contacts because of jars to the panel. In order to eliminate false tripping from this cause, the fault detectors are provided. Their coils are connected in parallel with the operating coils, and their contacts in series with the differential element contacts. Use of the fault detectors increases the minimum tripping current to 0.14 ampere, but has a negligible effect on the rest of the operating characteristics, figure 3.

Confirming Tests

Suitable tests have been made on the relay described. The essentials of the test connections are shown in figure 7. A large generator was used, so that d-c time constants up to $T_a=0.14$ were obtained. The resistor, R_v, of figure 7 was used to increase the burden on test transformer *B* so that the two transformers under test might be deliberately



Primary current, 10,800 amperes rms a-c component, $T_a = 0.11$ second

Test transformer *A*: 1,000/5, 2.7 square inches iron; secondary circuit resistance —1.36 ohms

Test transformer *B*: 1,000/5, 2.53 square inches iron; secondary circuit resistance—4.04 ohms

Figure 8. Test oscillogram for external fault

unbalanced until failure of the relay occurred.

Figures 8 to 11 are typical oscillograms from a large number of tests. The essential data for each of these oscillograms are given in the captions.

In figure 8, representing an external fault, it is to be noted that there is an initial high peak of differential current which scales 87 amperes maximum instantaneous value. The normal output of transformers *A* and *B* would have been 54 amperes rms a-c component, if they held their ratio. Inspection of the traces representing the output of current transformers *A* and *B* reveals, however, that they do not hold their ratio, and that their output is severely limited by d-c saturation for the first few cycles. On this basis, it is obvious that the high-speed relay sustained a very high percentage unbalance for one peak without tripping, as the relay contact line indicates. A simple high-speed ratio-differential relay with the usual current coils would have tripped under this condition.

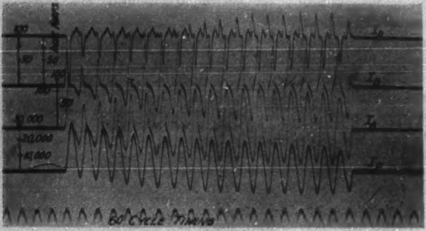


Figure 9. Test oscillogram for external fault

Conditions the same as for figure 8, except that the relay was short-circuited, terminal 1 to 2 to 3 (figure 7)

The peculiar wave shape of the differential current, shown in figure 8, warrants some discussion. The initial high peak is caused by the fact that current transformer *B* became thoroughly saturated by the d-c component a short interval of time before current transformer *A* became saturated. During the interval between the complete saturation of transformer *B* and transformer *A*, when transformer *B* alone was breaking down in ratio, transformer *A* was able to force the peak of differential current through the relay. When saturation was reached in both transformers, however, both of them performed very much as if they were air-core transformers, neither of them having sufficient capacity to hold their ratio with the burdens involved. During the time when both transformers were saturated, both the reduced capacity of the better current transformer as well as the impedance of the differential circuit effectively reduced the magnitude of the differential current. Thus, it will be noted that the difference current is inconsequential for approximately eight cycles after the initial high peak. After approximately eight cycles however, transformer *A*, having a better ratio of iron area to resistance burden, began the process of recovery from d-c saturation so that it was again able to perform somewhat better than transformer *B*. For this reason, a current began to reappear in the differential circuit.

As has been stated, the nominal output of both transformers would have been 54 amperes rms secondary, a-c component, if neither transformer had saturated, this value being derived from the 10,800-ampere symmetrical a-c component of the primary current and the ratio of the current transformers. In order to force this secondary current through the resistance burdens indicated, transformer *B* would have had to develop a flux density of 160,000 lines per square inch, and transformer *A* would have had to develop 51,000 lines of flux per square inch, calculated from the equation:

$$B_m = \frac{IR\,10^8}{4.44nfA}$$

where

B_m = maximum flux density in lines per square inch
n = number of secondary turns
f = frequency in cycles per second
A = cross-sectional area of the iron core in square inches
I = rms secondary amperes
R = total secondary circuit resistance

(This equation has been derived from the familiar transformer equation, $E = 4.44nf\phi_m 10^{-8}$.) The flux density values indicated form a measure of the unbalance between the two current transformers, the flux densities for perfect a-c performance having a ratio of 3.14/1.

The wave form of the differential current in figure 8 was of sufficient interest to arouse speculation concerning what this differential current would look like if the impedance of the relay circuit were zero. In order to obtain data on this point, all three terminals of the relay were short-circuited together and the oscillogram of figure 9 was taken under the same conditions. (No contact line was desired in this case, since the relay element was short-circuited.) Figure 9 shows a similar high initial peak of differential current, which scales 101 amperes. Since there was no differential circuit impedance in this test to form a balancing action between the two transformers, the differential current did not subside to almost zero for several cycles, as in figure 8. The calibration of the oscillograph was the same for both tests.

Figure 10 is an oscillogram for an internal fault with values as given in the caption. The internal fault was simulated by reversing the secondary connections of transformer *B* so that the total current of both transformers passed through the differential circuit of the relay. The time of operation of the relay is scaled from the contact line of the film to be approximately 0.6 cycle. Again, there was considerable saturation of the current transformers, particularly because of the d-c transient, yet prompt

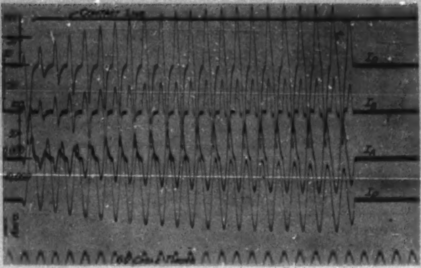


Figure 10. Test oscillogram for internal fault

Primary current, 10,900 amperes rms a-c component, $T_a = 0.123$ second

Test transformers as in figure 8 except secondary circuit resistance for current transformer *B* reduced to 2.44 ohms

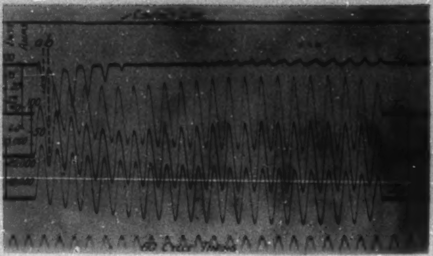


Figure 11. Test oscillogram for external fault

Primary current, 11,000 amperes rms a-c component, $T_a = 0.094$ second

Test transformer *A*: 4 primary, 800 secondary turns, 1,000/5, 4.8 square inches iron, secondary resistance (total) 1.31 ohms

Test transformer *B*: 1 primary, 200 secondary turns, 1,000/5, 1.7 square inches iron; secondary resistance (total) 1.18 ohms

operation of the relay was not affected.

Figure 11 shows another test for an external fault except that a different pair of current transformers was used with a different degree of unbalance, particularly with regard to the cross-sectional area of the iron in the test transformers. In this case, the calculated flux requirements for the a-c symmetrical component alone are as follows:

Transformer *A*—7,100 lines per square inch
Transformer *B*—71,600 lines per square inch

$$\text{Ratio } B/A = \frac{71{,}600}{7{,}100} = 10.2/1$$

In figure 11, the time of the first peak of differential current has been indicated by the dotted line, *a*, and it will be noted that this peak occurs earlier than the peaks of the two restraint, or sum coil, currents as indicated at *b*. This illustrates the meaning of the statement previously made that the differential current tends to be out of phase with the restraint current when the current transformers saturate during through fault conditions.

Summary of Test Results

The tests were made over a wide range of variation between current-transformer designs and loading, as has been indicated in part in figures 8 and 11. The purpose of this was to explore the range of possibilities of the new relay by exceeding the range normally to be expected. For example, it is felt that the conditions of figure 8 are more severe than would normally be expected for a reasonable application of generator differential protection. That is, it appears that the extreme importance of a large generator would justify the application of transformers whose a-c flux requirements for the maximum symmetrical external fault would not exceed the nominal saturation density of approximately 70,000 lines per square inch. Other tests, not described here, were made to cover the conditions imposed when a generator is connected to a double bus through two circuit breakers. In such an application, a heavy current may flow during an external fault from one bus to the other through the two circuit breakers connected to the generator. Such a current, flowing through the two current transformers per phase on the bus side of the generator, would materially increase the duty on them as compared to the duty imposed upon the current transformers in the neutral connection of the machine being protected. The relay performed satisfactorily in these tests.

Since it is obvious that there is a myriad of possibilities between different applications with regard to slight unbalances in iron area and secondary burden in current transformer circuits, the problem is how to best express the permissible variation in general terms. It has been found that the method of analysis used for the test of figures 8 and 11 is the most suitable; that is, where the flux density of the transformer is calculated showing the requirements for the maximum symmetrical current which will be experienced for an external fault. This method gives at once a figure which takes into consideration the factors which tend to unbalance the current-transformer performance for a given application.

A wide variation in permissible loading has been shown by the ratios of calculated flux densities of 3.14/1 for figure 8, and 10.2/1 for figure 11. In general, it was found that a higher ratio was permissible when the maximum calculated flux density for either transformer did not greatly exceed the nominal saturation density of approximately 70,000 lines per square inch.

Conclusions

1. In the past, the application of high-speed (one-cycle) differential protection to generators has required the closest scrutiny of the current-transformer characteristics with respect to the duty imposed. This study involved particular attention to the effect of the d-c component of asymmetrical through faults. In all such cases, careful matching of current-transformer characteristics was required.

2. With the new relay, it suffices to look carefully only at the a-c characteristics of the transformers. The application is thus reduced to the simplicity of the older, slower-speed, induction-type relay, where a consideration of the a-c characteristics of the current transformers was generally sufficient.

3. A means of comparing the duty on the current transformers has been given through the method of calculating the theoretical maximum flux density required for the a-c component. When the resulting figure does not materially exceed the nominal saturation value of 70,000 lines per square inch, an allowable ratio between the values for the two current transformers of 3/1 provides ample safety factor to guarantee satisfactory relay operation.

Reference

1. CONSIDERATIONS IN APPLYING RATIO DIFFERENTIAL RELAYS FOR BUS PROTECTION, Smith, Sonnemann, and Dodds. AIEE TRANSACTIONS, volume 58, June 1939.

Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Frequency Modulation

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FELLOW AIEE

RADIO communication makes use of a medium common to the whole world for the transmission of many signals simultaneously. In order to accomplish this a high-frequency electromagnetic wave has one of its characteristics varied in accordance with the instantaneous variations of the signal to be transmitted. The control of these variations is called modulation. The various simultaneous messages can then be separated by:

(*a*). Differences in the frequency band used.

(*b*). Differences in signal strength.

(*c*). Differences in direction of the source.

The allocation of frequencies and geographic location to stations engaged in different services is now a matter of legislation and international agreement. This regulation is necessary in order to reduce interference to a minimum.

The range of a radio station is limited solely by the point at which undesired interference reduces the quality of the received signal below a certain minimum. The amount of interference which may be tolerated differs with different classes of service, for instance, it would be less on a broadcast program designed to produce pleasure, than on a communication service designed to convey intelligence.

There are five principal sources of interference to radio reception. They are:

(*a*). Interference from other radio stations.

(*b*). Interference from natural electrical disturbances such as thunderstorms (static).

(*c*). Interference from electrical equipment not intended for radio purposes.

(*d*). Interference between identical signals traveling from the station originating the desired signal, but over two different paths. Since radio waves are alternating phenomena resolvable into a band of frequencies, the addition of two similar signals traveling over different paths must take account of both magnitude and phase. Distortion in the resultant signal may result due to the varying phase relations between the components with the same frequency in the two signals. One of the paths is usually caused by reflection from some medium such as the ionosphere. If this path varies in length with time, fading will result. When this produces distortion the phenomenon is called selective fading.

(*e*). Interference from random noise produced in the receiver by fluctuations in the motion of the electrons in the early stages of the amplifiers.

The major problems of the radio engineer are:

I. To obtain the maximum range at the minimum cost.

II. To secure the desired quality in the reproduction of signals.

Because the range is determined by the interference, and the quality is greatly affected by it, the reduction of interference becomes of paramount importance.

The reduction of interference must be accomplished by making use of some characteristic which differentiates the desired signal to a greater or less extent from the undesired interference. Four methods of differentiation have been extensively used. Each method in turn has its limitations. These methods and their limitations are:

1. Use of high power in the transmitter so that the strength of the desired signal will dominate the undesired.

This method is limited by the cost of high powered transmitters and by the interference it introduces to other services. Furthermore it does not affect selective fading since both signals (coming over two paths) are increased by the same amount.

2. Increasing the modulation of the radio wave to the greatest possible value.

This method is limited in amplitude modulation because it is not possible to vary the magnitude of a radio wave by more than 100 per cent and interfering signals, including static, will in general be modulated by similar amounts.

3. Use of selective circuits in the receiver so that only energy in the narrow band of frequencies which includes the desired signal will be received.

This method is limited because a definite band width is necessary for any given quality of reproduction and within this band there may be some portion of the energy in the spectrum of the interference.

4. Use of directive antennas at the receiver so that it is most sensitive to electromagnetic waves coming from the direction of the transmitter creating the desired signal and is insensitive to radiations originating in other directions.

This method is limited by the expense of directional antennas and by the fact that some interference may be originating in the same direction as the desired signal.

It is apparent that the methods just mentioned, taken individually or in combination, do not offer a complete solution of the problem. In fact no complete solution would appear possible, as the ultimate range of any transmission must be determined by the tolerable interference. However, any method which offers increased possibilities in the differentiation between desired signal and interference may be used to improve transmission. *Frequency modulation* offers such an additional method by which interference may be separated from the desired signal and it is the purpose of this paper to outline the principles and practices by which this may be accomplished.

The use and study of frequency modulation is not new. The Poulsen arc, developed before 1914, transmitted continuous-wave telegraph signals in which the frequency was shifted from one value to another when the key was depressed. Carson[1] and Roder[2] made theoretical investigations of the effects of frequency modulation on the spectrum of the modulated wave. Carson's investigation was

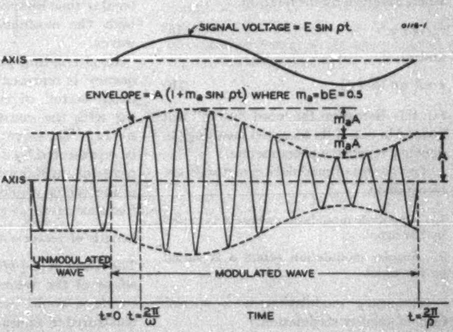

Figure 1. An amplitude - modulated wave

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[1] For all numbered references, see list at end of paper.

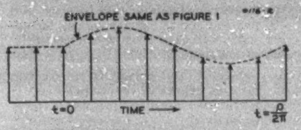


Figure 2. Vector diagrams of the amplitude-modulated wave of figure 1 for successive instants

made to analyze the proposal that frequency modulation could be used to reduce the band width required for a given signal. He proved that, on the contrary, frequency modulation never reduced the band width and might greatly increase it. Mathematics provides a correct answer to questions which are asked by its means, but it cannot be expected to provide answers to questions which are not asked. What was overlooked in the early mathematical analyses was the fact, later demonstrated by Armstrong,[3] and subsequently by Carson[4] and Fry, that frequency modulation provides an important method of distinguishing between desired and undesired signals which occupy the same portion of the frequency spectrum.

It will be necessary to go into some details of the principles of frequency modulation in order to show the reasons for this effect.

Modulation

Modulation of a radio wave is the process by which some characteristic of the radio wave is varied in accordance with the time variation of a signal, such as the instantaneous variations associated with speech, music, or the manipulation of a telegraph key. A general alternating wave may be represented by the equation

$$e=A\sin\theta \qquad (1)$$

where θ is given by the relation

$$\theta=\omega t+\phi \qquad (1a)$$

and so

$$e=A\sin(\omega t+\phi) \qquad (1b)$$

(In this discussion the word "wave" will be used in one of its accepted meanings to denote a repetitive phenomenon.)

Two groups of modulation methods are recognized.

1. **Amplitude modulation where A is varied by the signal.**
2. **Angular modulation where ϕ is varied by the signal.**

(Frequency modulation is a special form of angular modulation.)

Amplitude Modulation

In an amplitude-modulated wave the amplitude is varied about its mean value in proportion to the signal. Let the original signal (such as the sound pressure on the microphone) be represented by the function $f(t)$. Then the amplitude factor A of equation 1b is modified by $f(t)$ to give the amplitude-modulated wave

$$e=A[1+bf(t)]\sin(\omega t+\phi) \qquad (2)$$

where b is a factor determined by the design and operation of the modulating system and has dimensions such that $[bf(t)]$ is a pure numeric. b is usually a constant, but in some cases it is made a function of audio frequency. For example, if it is made to change with frequency in the proper manner compensation may be secured for defects in the frequency characteristic of some other part of the system.

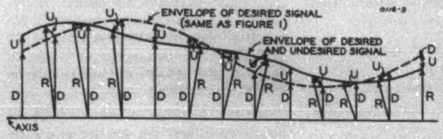

ENVELOPE OF DESIRED SIGNAL (SAME AS FIGURE 1)
ENVELOPE OF DESIRED AND UNDESIRED SIGNAL
U
R
D
AXIS

Figure 3. Interference with an amplitude-modulated wave of a carrier of slightly different frequency

D—Vector of desired signal
U—Vector of undesired or interfering signal
R—Resultant vector (*D*+*U*) of sum of desired and undesired signals

The amplitude variation cannot carry the amplitude below zero. Therefore the factor b should be so chosen by the operator that $[1+bf(t)]$ never becomes negative. Therefore $[bf(t)]$ should not exceed an absolute value of unity. This absolute value of the maximum of $[bf(t)]$ is called the amplitude *modulation factor* and is given the notation m_a.

If the signal $f(t)$ is sinusoidal with a frequency $p/2\pi$, equation 2 becomes

$$e=A(1+m_a\sin pt)\sin\omega t \qquad (3)$$

The curve of equation 3 is illustrated in figure 1 for $m=0.5$ and $\omega/p=10$. It will be noted that the wave crosses the axis at regular time intervals of $2\pi/\omega$ seconds for both the modulated and unmodulated waves.

In alternating phenomena a single frequency is represented by the projection of a vector of constant length rotating with the constant angular velocity $\omega=2\pi f$. The wave of equation 2 could also be represented by a vector rotating with a constant angular velocity ω, but the length of the vector would be changing at a low frequency rate as given by the equation

$$\text{Length of vector}=A[1+bf(t)] \qquad (4)$$

The term $A[1+bf(t)]$ is called the *envelope* of the wave. In equation 3 the envelope would be $A[1+m_a\sin pt]$ as is illustrated in figure 1.

In drawing vectors which represent alternating phenomena it is common practice to consider that the observer is traveling on a platform which is also rotating about the same center with a velocity ω. The original vector would then appear to be stationary and could be represented by a single drawing. However, if either the magnitude or the phase of the vector is changing with time, a series of successive drawings is necessary to illustrate what is happening.

These successive drawings of stationary vectors for the wave of figure 1 are shown in figure 2 for time intervals of one-eighth the period of the low-frequency wave producing the modulation.

At the receiver the detector produces a response which is proportional to the envelope of the modulated wave (except for the constant component).

Interference of Two Amplitude-Modulated Waves

If a second amplitude-modulated wave of the same carrier frequency and phase is added to the wave of figure 1 the resultant wave will have an envelope which is the sum of the envelopes of the two waves, for the vectors will be adding in phase. The interfering effect will be noticeable if the undesired signal is as much as one per cent of the desired signal. Hence it is desirable to make the value of m_a as large as possible, since the operator of a given communication system cannot control the modulation of the interfering wave with the undesired signal.

If the frequency of the interfering wave is slightly different from the desired wave (the difference being too small to eliminate it by selective circuits) then the interfering wave will produce a variation in the envelope which variation has an amplitude equal to the magnitude of the interfering wave (even if it is unmodulated). This additional variation will occur at a frequency which is equal to the difference between the carrier frequencies of the desired and undesired signals, and will produce a squeal which is further superimposed on the resultant envelope. This is illustrated by the vector diagrams in figure 3, where the undesired signal has a frequency which exceeds the frequency of the desired signal by $1.5\rho/2\pi$. It is seen that the resultant envelope is modified by an additional component equal to the magnitude of the undesired wave, and so introduces interference proportional to the magnitude of the interfering wave.

Again it is apparent that the amplitude of the envelope of the desired signal should be kept as large as possible in order that the interference may be minimized. If equation 2 represents current or voltage, the amplitude of the envelope may be increased by increasing either the power or the amount of modulation (m_a).

Angular Modulation

In angular modulation (of which frequency modulation is a subdivision) the angle ϕ of equation 1*b* is given by a function of time which is related, but not in all cases, directly proportional, to the signal function $f(t)$. The two principal subdivisions of angular modulation which have been extensively studied are phase modulation and frequency modulation.

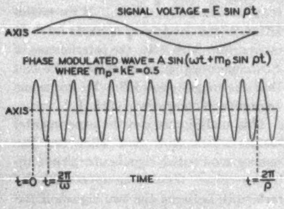


Figure 4. An angular-modulated wave

The 1st, 4th, 7th, 10th, and 13th cycles are shown in detail in figure 5

(*a*). PHASE MODULATION

In this type of modulation the phase angle ϕ is made to vary in accordance with the signal. That is

$$\phi=b_1f(t) \tag{5}$$

where b_1 is a *constant* determined by the design and operation of the modulating system. When equation 5 is inserted in equation 1*b* the wave becomes

$$e=A\sin\left[\omega t+b_1f(t)\right] \tag{6}$$

The maximum value of $b_1f(t)$ is called the *phase modulation factor* m_p. It is the maximum number of radians by which the phase of the carrier is altered during modulation. If the signal is sinusoidal with a frequency $\rho/2\pi$, equation 6 becomes

$$e=A\sin\left[\omega t+m_p\sin\rho t\right] \tag{7}$$

(*b*). FREQUENCY MODULATION

In this type of modulation the *instantaneous frequency* is varied about the average value $\omega/2\pi$ in proportion to the instantaneous value of the signal. By definition, the use of the word "frequency" is extended to the general equations 1 and 1*b* by the relation

$$2\pi f_{\text{inst}}=\frac{d\theta}{dt}=\omega+\frac{d\phi}{dt} \tag{8}$$

Since ω is a constant (2π times the carrier frequency) the signal must modify $d\phi/dt$ so that the instantaneous frequency is given by the relation

$$f_{\text{inst}}=\frac{\omega}{2\pi}+b_2f(t) \tag{9}$$

where b_2 is a design and operating constant. The maximum value of $b_2f(t)$ is the maximum deviation in instantaneous frequency of the modulated wave from the unmodulated one and is called the *frequency modulation factor*, or frequency deviation, m_f. If $f(t)$ is a sine wave with a frequency $\rho/2\pi$ then

$$b_2f(t)=m_f\sin\rho t \tag{10}$$

If equation 10 is combined with equations 8 and 9

$$2\pi f_{\text{inst}}=\omega+2\pi m_f\sin\rho t=\omega+\frac{d\phi}{dt}$$

which gives

$$\phi=\int 2\pi m_f\sin\rho t dt=-\frac{m_f}{f_\rho}\cos\rho t$$

where f is the frequency of the modulating signal. If this phase angle is inserted in equation 1*b* the result will be

$$e=A\sin\left[\omega t-\frac{m_f}{f_\rho}\cos\rho t\right] \tag{11}$$

Equations 11 and 7, which apply to a signal with a single frequency, do not differ appreciably (except for a 90-degree shift in the modulation phase). In equation 11 the maximum shift in phase (corresponding to the phase modulation factor m_p) will be

$$m_p=\frac{m_f}{f_\rho} \tag{12}$$

where m_f is the frequency deviation and f_ρ the modulating audio frequency. The value of m_f when f_ρ is the maximum audio or signal frequency to be transmitted is called the *deviation ratio*.

m_p in phase modulation and m_f in frequency modulation are arbitrary design factors. Unlike amplitude modulation they are not restricted to a maximum value of unity, for m_p may be hundreds of radians or m_f thousands of cycles per second if desired. The limitations on m_p and m_f will be determined by the allowable frequency spectrum and will be discussed later.

The distinction between phase and frequency modulation is as follows: if the frequency, but not the intensity of the modulating signal changes

m_p is constant in phase modulation.

m_f is constant in frequency modulation.

It follows from equation 12 that in frequency modulation the phase deviation m_p is inversely proportional to the modulating frequency. On the other hand in phase modulation the frequency deviation is directly proportional to the modulating frequency.

Figure 4 is an illustration of the angular modulation as represented by equation 7 for the case where $m_p=0.5$ and $\omega/\rho=12$. On a casual examination this would appear to be a single frequency wave. However, the intervals at which it crosses the axis vary throughout the audio cycle. In order to show this the 1st, 4th, 7th, 10th, and 13th cycles are expanded and shown in figure 5. It is seen that the varying shift in phase also produces a change in frequency which varies throughout the low-frequency cycle.

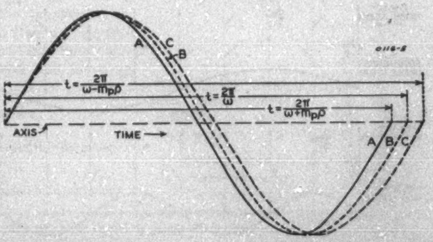


Figure 5. Expansion of individual cycles in figure 4

A—1st and 13th cycles of figure 4

B—4th and 10th cycles of figure 4

C—7th cycle of figure 4

The successive vector diagrams for the angular-modulated wave of figure 4 (corresponding to the diagrams of figure 2 for an amplitude-modulated wave) are shown in figure 6. The signal wave is included for identification of the various instants.

The difference between phase and frequency modulation may be illustrated by the way the motion of the resultant vector would appear to an observer riding with the carrier vector. In phase modulation, two audio signals of equal amplitude, but of different frequencies, would produce equal angular *amplitudes* in the apparent swing of the resultant vector. In frequency modulation two audio signals of equal amplitude would produce equal maximum angular *velocities* in the apparent swing of the resultant vector. In this latter case (frequency modulation) the maximum angle of swing would be inversely proportional to the audio frequency (as is indicated by equation 12). This is illustrated by figure 7 where the vectors for both frequency and phase modulation are drawn for two signals with an audio-frequency ratio of two to one. Note that in phase modulation the maximum angle ϕ_m is the same for both signals while for frequency modulation the maximum angle ϕ_m for signal A (the lower frequency) is twice that for signal B. Since the angular *velocity* is proportional to the instantaneous value of the signal in frequency modulation the vector reaches its maximum angle of deviation when the signal is zero while in phase modulation it reaches its maximum angle of deviation when the signal is a maximum.

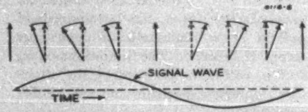


Figure 6. Vector diagrams of the angular-modulated wave of figure 4 for successive instants

Solid-line vectors are vectors of modulated wave

Dashed-line vectors are vectors of unmodulated wave

Other Types of Angular Modulation

Phase and frequency modulation are not the only possible types of angular modulation, but are only two members of an infinite group. Other possible types are:

(*c*). Angular Acceleration Modulation

In this type of modulation the second time derivative of ϕ is directly proportional to the signal function

$$\frac{d^2\phi}{dt^2}=b_3f(t)$$

In this type m_p would be inversely proportional to the square of the audio frequency.

(*d*). nth Order Modulation

In this general type of angular modulation the nth derivative of ϕ is directly proportional to the signal function.

$$\frac{d^n\phi}{dt^n}=b_{n+1}f(t) \tag{13}$$

In this type m_p would be inversely proportional to the nth power of the frequency for modulating signals of equal intensity.

In radio transmission by angular modulation means are provided at the receiver so that the detected signal is proportional to the angular modulation (of the particular subdivision selected) and at the same time this detected signal is made unresponsive to amplitude variations. These means will be discussed in more detail later.

Note: ϕ_m is same for both signals for phase modulation. ϕ_m is inversely proportional to signal frequency for frequency modulation. Maximum velocities of vectors are same for both signals for frequency modulation

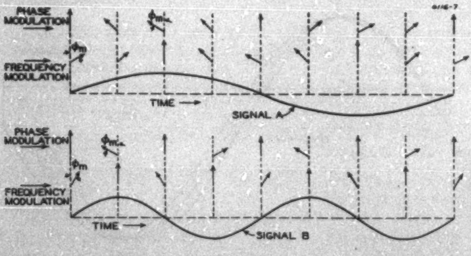


Figure 7. Comparison of phase and frequency modulation by means of vector diagrams

Interference of Two Angular-Modulated Waves

When two angular-modulated waves of the same carrier frequency are added together, the total angular modulation is not the sum of the two individual modulations. This is in distinct contrast to amplitude modulation where the resultant envelope is the sum of the individual envelopes.

This can be illustrated by figure 8 where an angular-modulated wave B is represented by a vector whose angle is changing with time. This is added to a larger vector A which for the moment will be assumed to be unmodulated. The resultant vector R will be the sum of the two vectors.

It is apparent that if B is less than A, then no matter what the total angular variation of B may be (even if it is hundreds of radians) the total angular variation between R and A cannot exceed $\tan^{-1}(B/A)$. For instance if $B/A=0.5$ the maximum value of m_p for the vector R when A is unmodulated is $m_p=0.46$. If $B/A=0.5$ and A in turn has its angle modulated, then the difference between the angle of A and that of R cannot exceed 0.46 radian at any instant. If the modulation factor (m_p) of A is made large in comparison with 0.46, the interference of B becomes negligible, in spite of the fact that the magnitude of B is by no means negligible in comparison with A.

This analysis justifies the experimental results which show that when two frequency-modulated signals are picked up by a receiver, there is no appreciable interference between the two signals if the stronger exceeds the weaker by a ratio of two to one or more.

It will be seen that the greater the value of m_p used for the desired signal the greater is the discrimination against the undesired signal, but this discrimination is not affected by the value of m_p used in the undesired signal.

The discrimination against interference obtained by angular modulation applies to all five types of interference enumerated in the early part of the paper. In particular static may be represented as a vector of varying phase and magnitude. The selective circuits of the receiver admit only those components within the band to which it is receptive. If the amplitude of the admitted noise does not exceed half the amplitude of the desired wave, a very small amount of noise will be introduced into the output. The greater

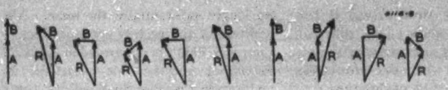


Figure 8. Vector diagrams showing interference in angular modulation

A is vector of desired signal—unmodulated
B is vector of interfering signal (same carrier)
R is vector of total wave $(A+B)$

the average phase deviation in comparison with the angle 0.46 (approximately 0.5) the greater will be the discrimination against the noise. It should also be observed that components of the noise vector which differ in frequency from the carrier by superaudible frequencies, will produce superimposed angular velocities above audibility and so do not contribute to the noise, as long as the noise is small compared with the signal.

In radio operation it will be found that if a portable receiver is driven in an automobile away from a frequency-modulated transmitting station, no appreciable noise will be experienced until the desired field strength drops to twice the noise field strength (taking into account only those components of noise accepted by the selective circuits of the receiver). The noise then rises rapidly, so that a sharp threshold is experienced.

Within the distance limited by the threshold, the signal-to-noise ratio can be improved by either increasing the power or increasing the modulation factor (either phase or frequency). Since power is proportional to the square of voltage or current in a given system, doubling the frequency deviation in frequency modulation has the same effect on the signal-to-noise ratio as increasing the transmitted power four times. In general an increase in the maximum frequency deviation by a ratio n would be equivalent in its effect on the signal-to-noise ratio to an increase in power by the ratio n^2.

The actual voltage produced by noise in the amplifier of a radio receiver increases with the width of the band accepted, the rate of increase depending on the type of noise. This introduces some disadvantage to the use of a wide band, because a stronger desired wave is necessary to insure that the desired voltage shall exceed the noise voltage.

R. F. Guy of the National Broadcasting Company reported before the Federal Communications Commission that in experiments with a one-kw transmitter and an antenna 1,000 feet high, the threshold for a value of m_f equal to 75 kilocycles was at 86 miles, while with an m_f of 15 kilocycles the threshold was at 100 miles due to the smaller noise voltage accepted by a more selective receiver. Within the threshold distances, however, there is a greater discrimination against noise with the greater frequency deviation.

The second major objection to the use of a large frequency deviation is that it would limit the number of stations which can serve a given area if a fixed total band width is allowed for the service.

A compromise must be reached and standard set before the use of frequency modulation is generally adopted so that the receivers may work with the transmitters. In order to study the possible or required allotment, a spectrum analysis must be made of the different classes of modulation.

Spectrum Analysis of Amplitude Modulation

The wave of equation 2 may be expanded by the use of simple trigonometric identities. This equation becomes

$$e=A\sin\omega t+\frac{mA}{2}\cos(\omega-p)t-\frac{mA}{2}\cos(\omega+p)t \qquad (14)$$

Equation 14 shows that the wave which is amplitude modulated by a single frequency may be analyzed into three component frequencies with the following designations:

$A\sin\omega t$	The carrier
$\frac{mA}{2}\cos(\omega-p)t$	The lower side frequency
$\frac{mA}{2}\cos(\omega+p)t$	The upper side frequency

The three components may be represented by three vectors rotating at different angular velocities. Again if the observer were rotating with the carrier vector, this vector would appear to be stationary. The upper side-frequency vector would appear to be rotating *counterclockwise* at a velocity p and the lower side frequency would appear to be rotating *clockwise* at the same velocity p.

The three vectors corresponding to the wave of figure 1 are shown in figure 9. It will be observed that the upper and lower side-band vectors add together to form a vector M called the modulation vector, which is always in phase with the carrier vector, but which varies in magnitude.

The three component frequencies of equation 6 are represented graphically in figure 10. This is shown primarily so that it may be compared later with the frequency spectrum used in frequency modulation.

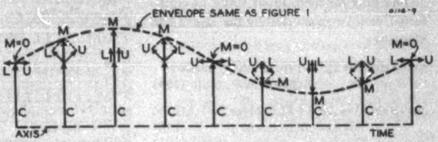


Figure 9. Vector diagram of carrier and side frequencies in an amplitude-modulated wave

C—Carrier vector (constant length)
U—Upper-side-frequency vector
L—Lower-side-frequency vector
M—Modulation vector (sum of *U* and *L*)
Vector of complete wave is sum of *C* and *M*

If the original signal were a complicated sound wave instead of a single frequency, a spectrum analysis would show it to be represented by a band of frequencies. The lower and upper side frequencies would expand into two bands of frequencies each as wide as the band of the original audio signal. For instance if the signal were restricted to a band of 0–5,000 cycles the two side bands would extend from 5,000 cycles below to 5,000 cycles above the carrier frequency. Since the quality of a signal depends upon the width of the band which may be transmitted, an improvement in the quality of transmission would require an extension of the frequency spectrum occupied by the radio

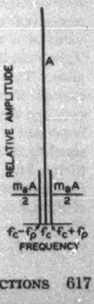


Figure 10. Spectrum analysis of an amplitude-modulated wave

f_c—Carrier frequency
f_p—Audio frequency
A—Carrier amplitude
m_a—Modulation factor=0.5 in this case

wave. However, the narrower the frequency band which is used the greater will be the number of stations which can be accommodated. In practice a compromise must be made. Standard broadcasting stations in North America are assigned carrier frequencies in the range of 550 to 1,600 kilocycles, these assignments being separated at intervals of 10 kilocycles. In order to prevent interference, selective circuits are required in the receiver which are so sharp in most commercial models that side-band components more than 3,000 cycles away from the carrier are greatly attenuated. Hence the quality which is permissible in practical operation is limited by the major problem of interference.

Spectrum Analysis of Angular Modulation

The angular-modulated wave of equation 7 may be expanded by the use of the identities

$$\sin(p \sin x) = 2[J_1(p) \sin x + J_3(p) \sin 3x + J_5(p) \sin 5x + \ldots.] \quad (15a)$$

$$\cos(p \sin x) = J_0(p) + 2[J_2(p) \cos 2x + J_4(p) \times \cos 4x + J_6(p) \cos 6x + \ldots] \quad (15b)$$

where $J_n(p)$ is the nth order Bessel function of the first kind. Equation 7 may be written

$$e = A[\sin \omega t \cos(m_p \sin \rho t) + \cos \omega t \times \sin(m_p \sin \rho t)] \quad (16)$$

If equations 15*a* and 15*b* are inserted in equation 16 the following result will be obtained

$$\begin{aligned} e = A\{&J_0(m_p) \sin \omega t + \\ &J_1(m_p)[\sin(\omega+\rho)t - \sin(\omega-\rho)t] + \\ &J_2(m_p)[\sin(\omega+2\rho)t + \sin(\omega-2\rho)t] + \\ &J_3(m_p)[\sin(\omega+3\rho)t - \sin(\omega-3\rho)t] + \\ &J_4(m_p)[\sin(\omega+4\rho)t + \sin(\omega-4\rho)t] + \\ &\ldots\ldots\ldots\ldots\ldots\ldots + \\ &J_n(m_p)[\sin(\omega+n\rho)t + \\ &(-1)^n \sin(\omega-n\rho)t]\} + \\ &\ldots\ldots\ldots\ldots\ldots\ldots \end{aligned} \quad (17)$$

This indicates that there are an infinite number of side frequencies for a single-frequency signal. However this is not as bad as might at first appear because for any given value of m_p, there will be a value of n above which the coefficients $J_n(m_p)$ fall off rapidly and become negligible. This is shown in figure 11. For example if m_p is one-half radian or less, only the first pair of side frequencies is important. On the other hand if m_p is equal to 20 radians, side frequencies out to the 24th pair would be appreciable. For large values of n this rapid falling off of $J_n(m_p)$ occurs just beyond $n = m_p$. Observe also that the value of the carrier component is always reduced when modulation occurs since $J_0(m_p)$ is less than one for all values of m_p different from zero. This is in contrast with amplitude modulation where the value of the carrier is not affected by modulation.

Figure 4 was drawn for a phase-modulation factor of 0.5 and so the first pair of side bands are the only ones of importance. If all other side bands are neglected the vector diagrams including the side bands for different instants of figure 4 can be shown as in figure 12. The signal wave is shown for identification. The carrier and resultant vectors are the same as those shown in figure 6. The modulation vector, which is the sum of the two side-band vectors, is always 90 degrees out of phase with the carrier and varies in magnitude in the same way that the modulation vector varies in amplitude modulation. The neglect of higher-order side frequencies is the same as an assumption that there is a negligible difference between the arc and a tangent line of the same length when the angle is small.

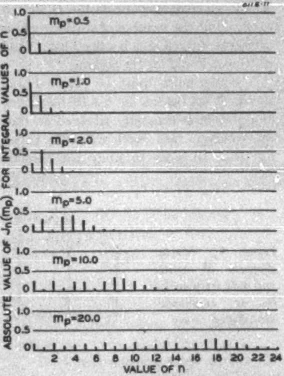


Figure 11. Values of the Bessel function of the first kind for integral orders

When the modulation vector is added to the carrier vector it causes the resultant vector alternately to advance beyond and retard behind the carrier vector. The maximum advance and retardation is approximately one-half radian. The length of the resultant vector is substantially constant. If the additional side bands were included the length of R would be exactly constant.

If the phase modulation exceeds one-half a radian additional side bands must be included because the arc and chord are no longer substantially the same. The addition of the vectors corresponding to these side bands is illustrated in figure 13 for $m_p = 1$ and for one-quarter of an audio cycle, the other three-quarters being similar. It will be noticed that each pair of side bands has associated with it a modulation vector which maintains a constant phase with respect to carrier (assuming that phase reversals are taken care of by negative signs).

If the modulated wave represents a quantity whose square is proportional to power in a given system the *average* power in an angular-modulated wave is not changed by the modulation, as the rms value of the wave is not modified if the amplitude remains constant. Therefore the square root of the sum of the squares of the carrier and all the side-band components remains constant for all values of m_p. The side-band power is obtained by a reduction in carrier power. This is also proved by the well-known relation

$$J_0^2(m_p) + 2\sum_{n=1}^{n=\infty} J_n^2(m_p) = 1$$

for all values of m_p. The number of terms which are of importance in the infinite series can be evaluated by setting

$$J_0^2(x) + 2\sum_{n=1}^{n=s} J_n^2(m_p) \geq \lambda$$

Then if λ is taken as some value less than unity, the sum can terminate with a finite value of n equal to s. If λ is equal to 0.999 then 99.9 per cent of the energy in the wave would be due to side-band components corresponding to values of n equal to or less than s.

For example, if $m_p = 4$ and six components are taken in each side band

$$\begin{aligned} J_0^2(m_p) + 2\sum_{n=1}^{n=6} J_n^2(m_p) &= 0.157688 + \\ &2(0.004356 + 0.132569 + 0.185072 + \\ &0.079017 + 0.017450 + 0.002411) \\ &= 0.999438 \end{aligned}$$

and all the components corresponding to $n > 6$ would contain only 0.0562 per cent of the energy.

The constancy of power output is in marked contrast to amplitude modulation where the carrier power remains constant and the side-band power is added. For that reason certain problems in design are simplified in a phase- or frequency-modulated transmitter. This will be discussed in the section on transmitters.

Comparison of the Spectra of Phase and Frequency Modulation

In phase modulation the value of m_p is made directly proportional to the maxi-

Table I

Audio Frequency	m_p for 60,000-Cycle Deviation	Approximate Number of Side-Band Components Required	Approximate Band Width in Kilocycles
30	2,000	4,030	120.06
60	1,000	2,020	120.20
600	100	208	124.8
2,500	24	46	140
3,000	20	24	144
5,000	12	30	150
10,000	6	16	160
15,000	4	12	180

mum value of the signal. If two different audio frequencies have equal amplitudes, and modulate the signal in succession, the same number of side-band components would be necessary for each case and these components would have the same relative magnitude. It has been shown that the advantage of angular modulation in the reduction of interference requires the use of large values of m_p for the desired signal. If the value of m_p for a special case is taken equal to 20, then by figure 11 it is apparent that approximately 24 side-band components would be desirable for the upper side band and a similar number for the lower side band. Therefore if an audio signal of high quality containing components up to 15,000 cycles were to be transmitted, a band width of approximately 2×24×15,000 or 720,000 cycles would be required. This is obviously impracticable. For this reason phase modulation (as distinguished from frequency modulation) has not been used for radio transmission.

In frequency modulation the value of m_f is made directly proportional to the maximum value of the signal. If two different audio frequencies have equal amplitudes and modulate the signal in succession with equal values of m_f, by equation 12 the values of m_p for the two cases will be inversely proportional to the audio frequency. Thus if m_p is equal to 4 for 15,000 cycles it would be equal to 40 for 1,500 cycles and equal to 400 for 150 cycles. A study of figure 11 shows that the number of components of appreciable magnitude in each side band is slightly in excess of m_p. Therefore as the modulating frequency is reduced, the number of components necessary increases, and the modulated wave occupies almost a constant band width in the spectrum. As an example consider a case where the maximum frequency deviation is assumed to be 60,000 cycles. Then if a high quality signal is to be transmitted, frequency components in this signal up to 15,000 cycles might be desired. If the wave were frequency modulated with a 60,000 cycle deviation ($m_f=60{,}000$) at 15,000 cycles m_p would equal 60,000/15,000=4 radians. For this case figure 11 shows that approximately six components in each side band separated at intervals of 15,000 cycles are desirable and the corresponding band width would be 2×6×15,000 or 180,000 cycles. On the other hand if the wave were to be frequency modulated with a 60,000 cycle deviation by a 3,000 cycle wave $m_p=60{,}000/3{,}000=20$ and approximately 24 components in each side band separated at intervals of 3,000 cycles would be desirable. The band width for this signal would be 2×24×3,000=144,000 cycles which is somewhat less than that needed for $m_p=4$ at 15,000 cycles. Table I is constructed for a maximum deviation of 60,000 cycles.

The spectrum analysis for a deviation of 60 kilocycles and modulating frequencies of 2,500, 5,000, 10,000, and 15,000 cycles is shown in figure 14 and it is apparent that the signal is contained within a band width of approximately 200 kilocycles in all cases. In practice, no audio signal would contain only the higher audio frequencies and so it is found practicable to use a deviation of m_f of 75,000 cycles for a band width of 200 kilocycles.

The spectrum analyses for a modulating frequency of 15,000 cycles and deviation frequencies of 30, 15, 7.5, and 3.0 kilocycles are shown in figure 15.

It is apparent from figures 14 and 15 that when the deviation frequency is large compared with the audio frequency, the band width required is approximately twice the deviation frequency while when the audio frequency is large compared with the deviation frequency the band width is twice the audio frequency. The latter case coincides with the situation in amplitude modulation. In other words the band width required is approximately twice the larger of the two frequencies (audio or deviation). If the audio and deviation frequencies are approximately equal the band width required is approximately four times that of the larger frequency. (See figure 15 for $m_p=0.5$, 1, and 2.)

The spectra of figures 14 and 15 may be used for any other combinations of audio and deviation frequencies which have the same deviation ratio m_p by modifying the scale of abscissa so that the interval between adjacent components is equal to the audio frequency.

The reader must be cautioned that if a signal contains two or more audio frequencies, the resultant spectra cannot be obtained by adding the spectra resulting from each audio frequency alone (as can be done in amplitude modulation). However, the total spectra will remain approximately within the limits set by the maximum frequency deviation when the latter is large.

Although the discrimination against noise is proportional to m_p, it is impracticable to use large values of m_p at all audio frequencies because of the band width involved. However, noise and interference is the composite result of a larger number of noise components. If frequency modulation is employed, the maximum value of m_p is obtained for each audio component in the signal which will at the same time keep the side-band components within the limits in the spectrum assigned to the transmission. Therefore frequency modulation is the type of angular modulation which reduces the composite noise effect to the greatest practicable extent.

If the maximum modulation factor m_p (deviation ratio) is low, m_p less than 0.5, frequency modulation does not appear to have any advantages over amplitude modulation. The early proposals for the use of frequency modulation envisioned this method of operation and so were dis-

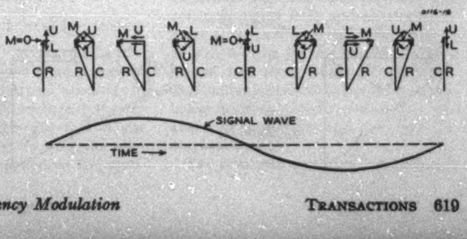

C—Carrier vector
U—Upper-side-frequency vector
L—Lower-side-frequency vector
M—Modulation vector (U+L)
First pair only
R—Resultant vector (C+M)
M=0
U
L
C
R
M
SIGNAL WAVE
TIME

Figure 12. Vector diagram of carrier and side frequencies in an angular-modulated wave for low values of modulation

carded after the analyses of Carson[1] and Roder.[2]

Practical Considerations in Frequency Modulation

It has been shown that the reduction of interference makes frequency-modulated transmitters most desirable for the transmission of high-quality signals. However these transmitters require relatively large frequency bands. Therefore frequency modulation, or F-M, does not appear to be feasible in the present broadcast band. For high-quality broadcasting they should be allocated to high frequencies where band widths of 200 kilocycles are available. The Federal Communications Commission has assigned the frequencies from 42 to 50 megacycles for this service or a total of 40 channels. This is in the range of the so-called ultrahigh frequencies.

Radio waves with frequencies of the order of 40 megacycles and above are not reflected from the ionosphere, and so their range is limited by the curvature of the earth. For the same reason static is greatly reduced at these frequencies because the energy which lies in the ultrahigh-frequency spectrum and which is originated by electrical disturbances at distant points on the earth's surface cannot travel to the receiver over long distances. Other factors also reduce static at high frequencies. As an average, the static voltage producing interference in a receiver with a given band width and tuned to 40 megacycles is about $^1/_{40}$ of the static voltage which would be picked up by a receiver of the same band width tuned to 1,000 kilocycles. It is also possible to transmit a wide-band audio signal which requires corresponding wide side bands at ultrahigh frequencies because for practical reasons it is not desirable to assign carrier frequencies as close together as in the standard broadcast band and so sufficient band width is available for high-quality transmission. Therefore ultrahigh frequencies inherently offer an improvement in quality whether amplitude or frequency modulation is used.

However, there are three important difficulties with ultrahigh-frequency transmission for broadcasting purposes. The first difficulty is the effect of the curvature of the earth on ultrahigh frequencies. Because there is no reflection from the ionosphere, it is frequently stated that the limit of transmission for these frequencies is the distance from the transmitting antenna to the horizon. The equation for this distance is

$$d = 1.22\sqrt{H} \qquad (18)$$

where

d is the distance to the horizon in miles
H is the height of the transmitting antenna in feet

For a height of 400 feet the distance to the horizon would be only 24.4 miles.

Within this distance the field strength of the signal is given approximately by the equation[6]

$$E = \frac{0.0105\sqrt{WGHAF}}{D^2} \qquad (19)$$

where

E = field strength in microvolts per meter
W = power in watts
G = gain of the antenna over a half-wave dipole
A = receiver antenna height in feet
F = frequency in megacycles
D = distance in miles

The formula and the statement that the transmission is limited to the horizon are not strictly true because it is found that diffraction and refraction of the waves produce a signal[7] beyond the horizon, but this signal falls off more rapidly than the inverse-distance-squared term of equation 19.

The second difficulty with ultrahigh-frequency transmission is the noise produced locally by electrical apparatus. The principal sources of this noise are automobile ignition and fever therapy machines. These sources might be eliminated in time by legislation requiring adequate shielding.

A third difficulty with ultrahigh-frequency transmission is the sharp shadows thrown by buildings, hills, etc. Sharp shadows are produced in the field of any wave motion when the interfering bodies have dimensions large in comparison with the wave length, and so are particularly apparent at ultrahigh frequencies (short wave lengths).

Because of these difficulties the transmission of amplitude-modulated waves at ultrahigh frequencies has not made very much progress for broadcast transmission. However, these difficulties are only aspects of the fundamental problem pointed out at the beginning of the paper, that radio transmission is limited by the ratio of interference to signal. It has been shown that frequency modulation offers an important improvement in the solution of this problem. By its means the signal-to-noise ratio at any fixed distance may be increased. It seems probable an increase in transmission range may be secured by frequency modulation due to the greater signal-to-noise ratio and this may make economically feasible the use of ultrahigh frequencies for broadcasting purposes.

In field tests, adequate signals for the operation of an F-M receiver at two to three times the horizon distance are regularly reported when the transmitter is a high-powered one.

The wide band width which is required for frequency modulation is compensated

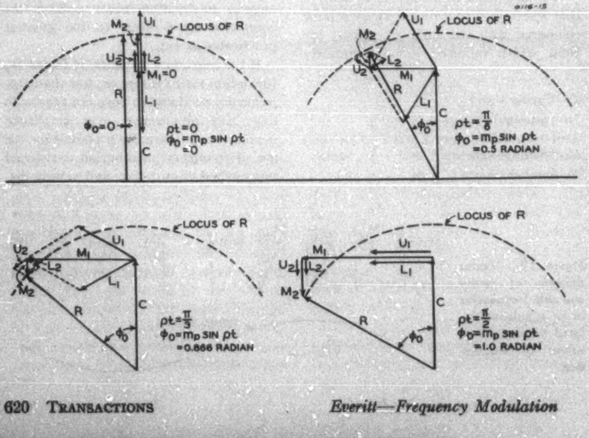
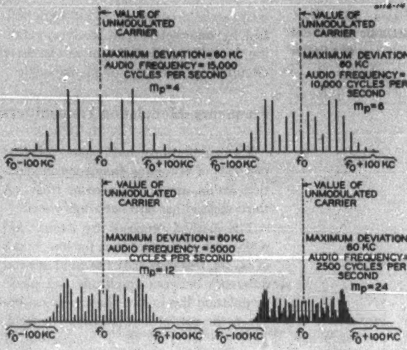


Figure 13. Vector diagram of carrier and two pairs of side frequencies in an angular-modulated wave where $m_p = 1$

U_1—First upper side band vector
L_1—First lower-side-band vector
M_1—First modulation vector ($U_1 + L_1$)
U_2—Second upper-side-band vector
L_2—Second lower-side-band vector
M_2—Second modulation vector ($U_2 + L_2$)
C—Carrier vector
R—Resultant vector



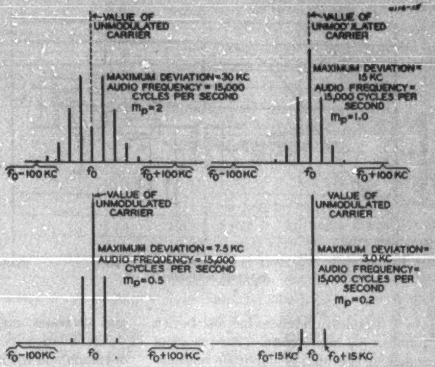


Figure 14 (left). Spectrum analysis of frequency modulation for a constant deviation and variable modulating frequencies

Figure 15. Spectrum analysis of frequency modulation for a constant modulation frequency and variable deviation frequency. (Signal is for phase modulation)

for by the reduction in interference between two stations on the same channel. In amplitude modulation, interference is caused if an undesired signal is one per cent of the desired signal. It has been shown that in F-M a ratio of two to one between desired and undesired signals is sufficient. If two stations of equal power and located in neighboring cities should operate on the same channel, there would be only a small territory about half way between the two stations where there would be any interference. Even in this territory, relatively simple directional antennas would be sufficient to pick out one station or the other. Therefore numerous stations could be located throughout the nation on the same carrier frequencies.

I. R. Weir, of the General Electric Company reported before the Federal Communications Commission that in their experiments they used a 150-watt transmitter at Albany and a 50-watt transmitter at Schenectady, a distance of 14.5 miles. They operated both transmitters on the same frequency with both frequency and amplitude modulation. In driving a car equipped with a receiver along a line between the two stations the following results were obtained:

Type of Modulation	Interference-Free Range of Albany Station	Transitional Distance With Interference	Interference-Free Range of Schenectady Station
Frequency	10.5	1.0	3.0
Amplitude	3.3	11.7	0.5

A further advantage of frequency modulation in the operation of the transmitter and receiver is that nonlinear distortion in frequency modulation is not affected by the nonlinearity of the tubes, as their nonlinearity is a function of amplitude. In F-M nonlinear distortion depends only on circuit design, that is, on nonlinear relations which are a function of frequency. For that reason it is claimed that high-quality reproduction is more practicable.

Frequency-Modulation Receivers

In the discussion of F-M receivers and transmitters, a certain familiarity with communication theory must be assumed in order to conserve space. The discussion will deal with present commercial practice.

Frequency-modulated receivers differ from amplitude-modulation receivers in three important respects. They are:

(*a*). The inclusion of a *limiter* to remove any amplitude modulation resulting from an interfering signal.

(*b*). The use of a special detector circuit called the discriminator to change the frequency modulation into a variable-amplitude signal.

(*c*). The use of a wide-band intermediate amplifier.

In addition it is much more important in a frequency-modulation receiver to have a high-gain intermediate-frequency amplifier, because the operation of the limiter is dependent upon a certain minimum signal being applied to its input. Also, if advantage is to be taken of the high-quality transmission which is possible with F-M, a better-than-average audio system and loud speaker should be included.

Except for these differences, the F-M receiver will follow the practice of amplitude-modulated receivers. The use of superheterodyne receivers is universal.

If a tuned-radio-frequency amplifier has impressed upon its grid a high-frequency voltage exceeding a certain minimum amplitude, the radio-frequency current in the tuned plate circuit will be practically independent of the magnitude of the input. This is because after the radio-frequency component of voltage in the plate circuit has reached an amplitude equal to the d-c component of the plate voltage no further increase in the radio-frequency component can be obtained for the instantaneous value of plate voltage cannot be driven negative. By the use of a high-gain tube and low values of plate voltage, saturation at relatively low values of grid excitation may be obtained. Figure 16 shows the circuit and the limiting action for a typical commercial F-M receiver. The operation of the limiter is the same as that of a class *C* amplifier[8] and the use of a bias obtained by a resistance in the grid circuit assists in securing a flat curve.

The use of a limiter is necessary because any amplitude modulation which reaches the detector will also produce amplitude variations in the reproduced signal. *The use of the limiter or its equivalent at the receiver is the most important component in an F-M system, for no reduction in interference will occur without its operation.* The limiter is possible in frequency modulation because the saturation does not affect the instantaneous frequency of the output.

The elimination of hum due to the use

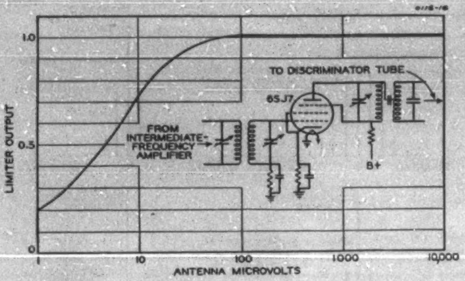


Figure 16. Characteristic and circuit of a limiter

of a-c supplies in receivers has also been a problem for many years. The principal source of this hum is amplitude modulation produced in the receiver by the hum component of the rectified d-c plate supply and by the heating of the cathodes by alternating current. If this occurs in the early stages of the receiver it is amplified along with the signal. While this hum level is reasonably well controlled in modern receivers, the limiter in a frequency-modulated receiver provides an additional improvement. This is particularly important because the elimination of other sources of interference makes the reduction of hum to an extremely low level much more desirable.

Because the audio amplifier and loudspeaker must reproduce the amplitude variations of the original signal, the discriminator or detector circuit must change the frequency variations into amplitude variations. The most common discriminator circuit in use is that of figure 17. The tuned transformer L_1-L_2 has a split secondary feeding two diode plates with the resistance loads R_1 and R_2. The voltage E_3 will be 90 degrees out of phase with the voltage across L_2 at the resonant frequency of L_2-C_2. The radio frequency across diode 1 in series with resistance R_1 is represented by the vector $E_a=E_1+E_3$ while that across diode 2 in series with resistance R_2 is $E_b=E_2+E_3$. When the output of the discriminator is at the intermediate frequency the phase relations will be those shown in diagram *A*. However, when the frequency shifts, the voltage across L_2, (E_1-E_2), shifts in phase. It can be shown that this voltage will follow the locus indicated by the circles. Hence E_a and E_b change in magnitude, one decreasing and the other increasing. By detector theory[5] the instantaneous value of the voltage across R_1 is proportional to E_a while that across R_2 is proportional to E_b. Therefore the instantaneous voltage E_4 supplied to the audio amplifier changes. Increases in frequency make E_4 positive and decreases make it negative.

The relation between the frequency deviation and the instantaneous value of E_4 is also shown in figure 17 for a typical receiver. The circuit constants must be selected so that the straight-line portion of the characteristic will accommodate the frequency shift which is used at the transmitter. The linearity of this characteristic affects the nonlinear distortion in the reproduced signal. It should be observed that its linearity is not a function of the tube characteristic. This makes it apparent that it is necessary to adopt standards at both receiver and transmitter which will be co-ordinated.

After a hearing before the Federal Communications Commission where all the problems were thoroughly discussed, the band width of 200 kilocycles per channel was adopted as standard. The allocation of channels adjacent to each other in the range of 42 to 50 megacycles simplifies the problem of receiver design, since no band-switching equipment will be required.

Because the intermediate amplifier must amplify a wider band of frequencies than is necessary in amplitude modulation a higher intermediate frequency is used. The present standard adopted is 4.3 megacycles for the intermediate carrier frequency.

A system which uses negative-frequency feedback in the receiver[8] has been proposed. It accomplishes similar results to those obtained by the use of the limiter, but as it is not at present available commercially, it will not be discussed here, although additional advantages are claimed for it.

Receivers are being manufactured which receive both amplitude and frequency modulation. In these combined receivers separate intermediate frequency transformers and detectors must be used. This makes the combination receiver somewhat more complicated, but complicated circuits have never been a bar to commercial production. This is exemplified by superheterodyne receivers retailing for $10 which have more extensive circuits than $200 sets of 15 years ago.

Frequency-Modulation Transmitters

Two important and quite different methods of obtaining frequency modulation are in use at the present time. A third method has also been announced.

The method proposed by Armstrong[9] makes use of the fact that for low values of m_p (m_p less than 0.5) the fundamental difference between amplitude and phase modulation lies in the phase relation between the carrier and side-band vectors. This is illustrated by a comparison of figures 9 and 12. Balanced modulators have been extensively used[5] in carrier-current systems to obtain the two side bands of amplitude modulation and eliminate the carrier. In the Armstrong system the carrier output of the oscillator is shifted 90 degrees and then used in a balanced modulator to obtain side-band components corresponding to amplitude modulation with this carrier phase. The output is then added back to a carrier component with the phase of the oscillator to form a phase modulation system. The maximum allowable modulation factor m_p produced with these components is 0.5 since only one pair of side frequencies is available.

A block diagram showing the Armstrong system is shown in figure 18. The

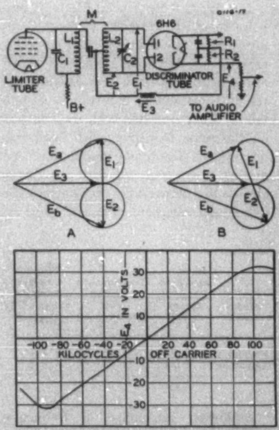


Figure 17. Characteristics and circuit of a frequency discriminator

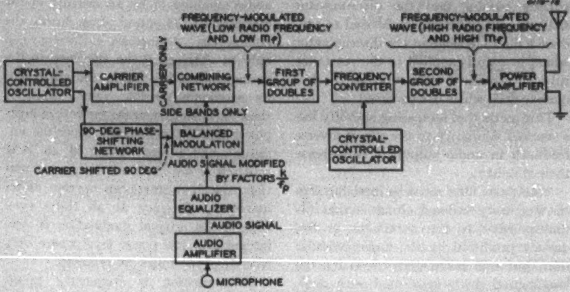

FREQUENCY-MODULATED WAVE (LOW RADIO FREQUENCY AND LOW m_f)
FREQUENCY-MODULATED WAVE (HIGH RADIO FREQUENCY AND HIGH m_f)
CRYSTAL-CONTROLLED OSCILLATOR
CARRIER AMPLIFIER
CARRIER ONLY
COMBINING NETWORK
FIRST GROUP OF DOUBLES
FREQUENCY CONVERTER
SECOND GROUP OF DOUBLES
POWER AMPLIFIER
SIDE BANDS ONLY
90-DEG PHASE-SHIFTING NETWORK
BALANCED MODULATION
CRYSTAL-CONTROLLED OSCILLATOR
CARRIER SHIFTED 90 DEG
AUDIO SIGNAL MODIFIED BY FACTORS $\frac{k}{f_p}$
AUDIO EQUALIZER
AUDIO SIGNAL
AUDIO AMPLIFIER
MICROPHONE

Figure 18. Block diagram of Armstrong frequency-modulated transmitter

several components required will be discussed in turn.

This method is fundamentally a phase-modulation system, but it may be converted into a frequency-modulation system if an audio equalizer is used before the modulation. The required characteristic of this equalizer is that the output voltage must be inversely proportional to the audio frequency of the input voltage. With this equalizer in the audio system the phase modulation will be inversely proportional to the audio frequency of the original signal. It has been shown that this is the requirement for frequency modulation and so the combination produces true frequency modulation.

It has been explained that with this system m_p is limited to a maximum value of 0.5, in the original modulation. On account of the characteristic of F-M which has been discussed, this maximum value of m_p can only be secured at the lowest audio frequency to be transmitted. The value of m_p at higher frequencies will be inversely proportional to the audio frequency. Then if a range from 40 to 15,000 cycles is to be transmitted the maximum value of m_p at 15,000 cycles will be $40/15{,}000 \times 0.5 = 0.0013$.

Such a method of obtaining frequency modulation, if used alone, would be entirely impractical since operation would be restricted to such low values of m_p and the advantages of frequency modulation lie in the use of large values.

If a radio-frequency amplifier has a large grid bias and relatively large radio-frequency voltage applied to its grid, the plate current will flow in pulses[5] containing harmonics of the grid exciting voltage. If the plate circuit is tuned to a harmonic of the grid voltage, the voltage across the tuned circuit will have a high value at this harmonic. For efficient operation this harmonic should be a low one, say the second or third. When tuned to a second harmonic the combination is called a frequency doubler.

It has been found that a frequency doubler also doubles the phase or frequency shift if an angular modulated wave is applied to the grid, and therefore doubles the value of m_p and also m_f for such a wave. A series of n doublers would multiply m_p by 2^n.

If the method of obtaining frequency modulation now being discussed is operated with a relatively low carrier frequency and the output is passed through a sequence of doublers or triplers, the value of m_p can be correspondingly increased. It has been shown that if a deviation of 75,000 cycles is desired, the value of m_p at an audio frequency of 15,000 cycles would be $75{,}000/15{,}000 = 5$. It has also been shown that for the wide audio range of 40–15,000 cycles the n_p which can be initially produced by the Armstrong method at an audio frequency of 15,000 cycles is 0.0013. Therefore the amount of multiplication of m_p required is approximately $5/.0013 = 3{,}750$. The number of doublers required is then obtained by the solution of the equation

$$2^n = 3{,}750$$

or

$$n = 12 \text{ nearly}$$

If a final carrier frequency of 41 megacycles is to be used and straight multiplication were to be employed, the carrier frequency at which the initial modulation should take place would be determined by the frequency multiplication required. In this case

$$\text{Initial carrier frequency} = \frac{\text{Final carrier frequency}}{\text{Multiplication of doublers}} = \frac{41{,}000{,}000}{2^{12}} = 10{,}000 \text{ cycles per second}$$

It is not possible to modulate a 10,000-cycle carrier by an audio frequency of 15,000 cycles, since the audio frequency must be less than the carrier frequency. Therefore some modification of the system must be made. The modification adopted is to perform the modulation at an initial carrier frequency of the order of 200 kilocycles and pass it through a first group of say six doublers. The carrier frequency has then been multiplied 2^6 times and has reached a value of 12.8 megacycles. If now a final carrier frequency of 41 megacycles is desired, the 12.8 megacycle signal is combined with the output of a second crystal oscillator whose frequency is selected so that a beat note of $41/2^6$ or 0.6406 megacycle is obtained. This does not affect m_p. The 640.6-kilocycle wave is then passed through a second group of six doublers to obtain the final 41-megacycle output. Since the initial frequency-modulated wave has now passed through 12 doublers, the multiplication of 2^{12} or 4,096 has been obtained.

The output of the second group of doublers is then amplified up to the final power required.

While this circuit seems complicated, it should be remembered that these operations can be performed at low power and with receiving-type tubes. The cost is not prohibitive for a transmitter station, since the investment in other equipment would be much greater.

The second method of frequency modulation operates in a distinctly different manner. If the capacity of a capacitor could be varied at an audio rate, and if this capacitor were included in the tuned circuit of an oscillator, it is apparent that the output would be frequency modulated.

The fundamental characteristic of a re-

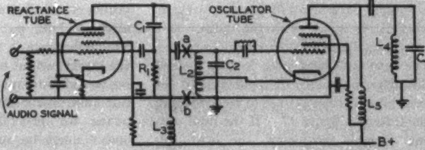

REACTANCE TUBE
OSCILLATOR TUBE
C_1
R_1
a
b
L_2
C_2
L_3
L_4
C_4
L_5
AUDIO SIGNAL
B+

Figure 19. Circuit of an oscillator which is frequency-modulated by a reactance tube

actance is that the current flowing into the two terminals is 90 degrees out of phase with the voltage applied across these terminals. This same effect can be secured with a tube circuit. The method is illustrated in figure 19. The resistance R_1 is made small in comparison with the reactance of C_1. Then the alternating voltage between the control grid and cathode is substantially 90 degrees out of phase with the voltage impressed across the terminals *a-b*. But the plate current which flows in the tube is determined largely by the grid voltage. The choke L_4 provides a large impedance to alternating current so that the a-c component of the plate current will flow in the terminals *a-b*. The current which flows into the terminals *a-b* will be 90 degrees out of phase with the voltage across it because of the grid control and the circuit appears like a reactance at these terminals.

The magnitude of the reactance is a function of the amplification constant of the tube. In a variable-mu tube the amplification constant may be controlled by the voltage on the grid. Therefore if an audio voltage is also impressed on the control grid of the tube the effective reactance may be varied at an audio rate.

The terminals *a-b* are connected in parallel with the tuned circuit L_3-C_3 of a conventional oscillator whose output frequency is determined by the resonance of that circuit. With this combination a variation in the audio voltage on the grid of the reactance tube will produce a frequency-modulated wave in the output circuit of the oscillator L_4-C_4.

The circuit of figure 19 does not have the inherent stability of the system of figure 18 because the carrier frequency or frequency for zero modulation is not crystal controlled. Stability equivalent to crystal control is necessary in modern radio operation. In order to secure crystal control a more elaborate system is necessary. This is illustrated by the block diagram of figure 20.

The oscillator and reactance circuit are similar to those of figure 19. The oscillator usually operates at some submultiple of the desired frequency, say one-fourth or one-sixth. A frequency multiplier is used before the operation of the final power amplifier, as is standard practice at ultrahigh frequencies. A sample of the output is brought back into a frequency converter where it is mixed with the output of a crystal oscillator for comparison purposes. The resultant beat note is then passed through a frequency discriminator of the same general type as used in receivers and illustrated in figure 17. If the final output changes frequency, the beat frequency passing into the discriminator will change. This change is used to produce a direct voltage in the discriminator which in turn is applied to the reactance tube to provide a correction on the frequency drift which has occurred.

This method of increasing stability has a marked similarity to the use of inverse feedback in audio amplifiers to increase their stability.

A delay or filter must be introduced in the frequency feedback circuit so that it is unresponsive to the variations in frequency produced by the audio modulation, but will make corrections for the long period drifts associated with oscillators which are not crystal controlled.

The two methods of frequency modulation transmitters each have their proponents. The Armstrong method is claimed to be more stable because the carrier frequency is directly controlled by the two crystals. The reactance tube method is simpler and would seem particularly applicable to low powers and portable equipment. It is too soon in terms of practical operation to be sure which will find the most general application.

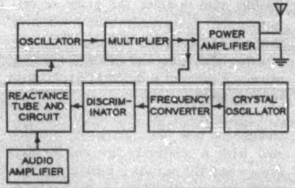


Figure 20. Block diagram of a reactance-tube-controlled frequency-modulated transmitter with crystal stabilization

The operation of the final amplifiers in both systems is essentially simpler than is the case in amplitude modulation, since attention need not be paid to the linearity of input-output amplitude curves.

In amplitude modulation the final high-power stage presents certain difficulties in operation. A transmitter normally requires[5] either a high-power audio amplifier with an output about 75 per cent of the rated carrier power to perform the modulation, or else the final stage must be operated at half its maximum efficiency. This is because in amplitude modulation the power output must be increased during modulation by an amount equal to the side-band power (50 per cent of the carrier power for $m=1.0$). The increase in power must be supplied by either an audio amplifier or by an increase in the efficiency of the output stage during the audio cycle. An increase in efficiency can only be obtained if the efficiency in the absence of modulation is limited to half its maximum possible value. Both of these expedients increase the cost of high-power stations materially. Since the amplitude and power output of an F-M transmitter are constant during modulation, the final stage can operate at its maximum efficiency at all times even though the original modulation is performed at a low power level where only receiving-type tubes are required.

The question is frequently raised whether the wide band which must be transmitted for F-M does not introduce difficulties in the tuned circuits. However, it should be remembered that the selectivity of any tuned circuits is given in terms of frequency ratio rather than absolute band width. A 200-kilocycle band width at 40 megacycles is only one-half of one per cent of the carrier frequency, while the 10-kilocycle band width used in standard broadcasting at 1,000 kilocycles is one per cent of the carrier frequency. Therefore no difficulties are introduced in the transmitter by the required absolute width of the band when the carrier frequency is high.

Application of Frequency Modulation to Services Other Than Broadcasting

Frequency modulation seems to have inherent advantages for other services than broadcasting. Important among these are its application to police and airway communication. The sharp limitation of the range obtained with F-M is an advantage and it would appear that many more police transmitters covering specific areas could be used without interference. In airway service some of the most important contacts are needed during severe electrical storms and F-M could make an important contribution in this field.

F-M would also appear to have advantages for military purposes where limited ranges are desired and interference is a particularly severe problem.

In police, airway, and military communication, high-quality reproduction is not necessary and so a more limited band would serve the purpose.

F-M has also been proposed for longer-distance communication by the use of relay or repeater stations spaced at intervals determined by the range of the transmitters. By the use of directional antennas this range can be increased. Previous proposals to use relay stations for broadcast-station interconnection have not met with favor because of the limited range and additional interference which they would cause, but it appears that a large part of this objection is eliminated when F-M is used.

It is also possible to multiplex other services on F-M transmission, such as facsimile, transmitting this service along with a sound signal, but with a reduction in the signal-to-noise ratio. Another suggestion has been to transmit two sound signals to obtain binaural reproduction. The possibilities of these uses have been only partially explored.

Conclusion

It would appear that frequency modulation is capable of producing a marked change in broadcasting within the next few years. It is very doubtful if it will eliminate the use of the standard broadcast frequencies, as they are still capable of covering larger distances when (and only when) a transmitter is given exclusive use of a channel. It seems probable that many local stations designed to cover a limited area will be transferred to F-M operation, and the number of cleared channels increased so as to take better advantage of the limited band of standard broadcast frequencies available. The development in these lines must eventually respond to the laws of economics and engineering.

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Low-Voltage D-C Measurements on Electrical Insulating Oils

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THE development work and experimental results presented herein have been a part of a co-operative project of the Utilities Co-ordinated Research, Inc. (Association of Edison Illuminating Companies) and the Massachusetts Institute of Technology. The purpose of this joint research project is the investigation of the mechanism of the deterioration of electrical insulating oils and the development of methods and apparatus for the study of this deterioration, with special reference to its influence on the electrical characteristics. It was for this reason that it was desirable to investigate the conductance of electrical insulating oils and methods of determining the conductance of such oils.

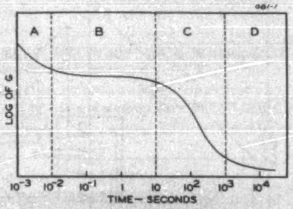

Figure 1. Variation of conductance with time at high gradients

General Considerations

In the usual conductance test for electrical insulating oils[1] the conductance is measured by means of a galvanometer used as an ammeter. A cell with a guarded electrode is used and from 500 to 1,500 volts potential difference is applied across the cell giving a potential gradient of from 200 to 1,200 volts per millimeter. The conductance (or resistance) is computed from the current reading one minute after the voltage is applied. As has been shown, principally by Whitehead,[2] this value is entirely empirical and often does not represent either the initial or the final conductivity of the oil. Using an amplifier-oscillograph and potential gradients of 500, 1,000, and 1,500 volts per millimeter Whitehead was able to measure the change of conductance with time from about 0.001 second after the application of voltage. The general type of the curve thus obtained for oils of low conductance is shown in figure 1.

Region *A* in figure 1 represents a brief initial decaying conductance indicating a phenomenon similar to dielectric absorption in solids. It has been suggested that it is due to the orientation of large polarized particles. Region *B* represents the true d-c conductance which is supposed to be due entirely to ions. It is this region which is of one to ten seconds duration in which we are most interested. Region *C* represents the time during which the ions are swept out of the space between the electrodes and space charges built up in the spaces near the electrodes. The empirical nature of the one-minute conductance value is easily understood since it falls in this region. Region *D* represents the long-time conductance after the "clean up" of the oil by the current which may take a period of hours to reach a constant value.

It was desired to find some more practicable and easier method than the amplifier-oscillograph arrangement by which to measure the true d-c conductance of region *B*. It was thought that the velocities of the ions could be reduced by reducing the potential gradient between the electrodes so that the true d-c conductance of region *B* would be extended from ten seconds duration to perhaps ten

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The authors wish to acknowledge their appreciation to the General Radio Company of Cambridge, Mass., for their hearty co-operation, and to R. F. Field of that company for his helpful advice in the development of the bridge herein described. The authors also wish to acknowledge the co-operation of the committee on insulating oils and cable saturants, Utilities Co-ordinated Research, Inc., Herman Halperin, chairman, and of the other committees representing the oil-refining and electrical-manufacturing companies.

[1] For all numbered references, see list at end of paper.

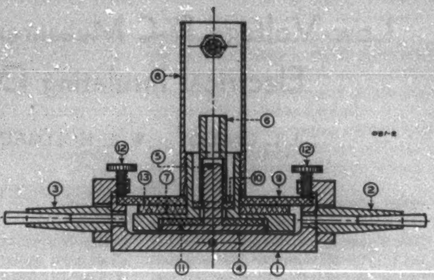
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Figure 2. Oil-measuring cell

1—Low electrode
2, 3—Filling arms
4—High electrode
5—Electrode stem
6—Tapped stem
7—Guard electrode
8—Shielding tube
9, 10, 11—Insulators
12—Clamps
13—Pyrex spacer

minutes duration. If, for example, the potential gradient were reduced from the customary 1,000 volts per millimeter to 10 volts per millimeter the duration of the true d-c conductance would be extended from ten seconds to more than ten minutes.

Measuring Cell

It was further desirable to be able to make measurements of the d-c conductance using the same low-capacitance cell (approximately ten micromicrofarads capacitance) used in this laboratory for power-factor measurements. By doing this, both a-c and d-c measurements could be made on the same oil sample under identical conditions of cleaning and filling. This cell is shown in figure 2. It is a three-terminal cell which is so constructed that it may be completely taken apart for purposes of cleaning. The cell used in the work herein reported was made of stainless steel with nickel arms for filling. The cell was cleaned by three washings in technical benzene and then dried for one hour at 100 degrees centigrade with a continuous stream of nitrogen passing through, and finally cooled in a desiccator.

Bridge

Using a General Radio type 544 megohm bridge upon which various alterations had been made so as to increase its range and to allow it to operate at low bridge voltages, it was shown that this type of an arrangement was practicable for use with bridge voltages of from one to ten volts. With various desirable improvements in mind a new bridge was designed and built specifically for the purpose of making conductance measurements. This bridge has the same essential circuit as the General Radio megohm bridge and is shown diagrammatically in figure 3. It consists of a high-resistance Wheatstone bridge with a vacuum-tube amplifier and sensitive galvanometer as a detector. *B* and *N* are high-ratio arms, *B* with a maximum resistance of a megohm and *N* with a maximum of 1,000 megohms. The arm *A* is a decade resistance unit ranging from 0 to 10,000 ohms in 0.1-ohm steps. The measuring cell is placed in arm *P*, the measuring electrode being connected to the high terminal by a shielded lead. The shield of this lead together with the guard ring is connected to the guard terminal which is grounded. The entire assembly is mounted on an aluminum panel and placed in a shielded case, which is also grounded. All switches in the actual bridge circuit are made of low-loss yellow bakelite and connections where perfect insulation is essential are air-insulated bus wires.

The switching arrangement of the bridge is controlled by the switch indicated as *S*. This is a four-pole double-throw switch with the points bent so as to make contacts in the off position as

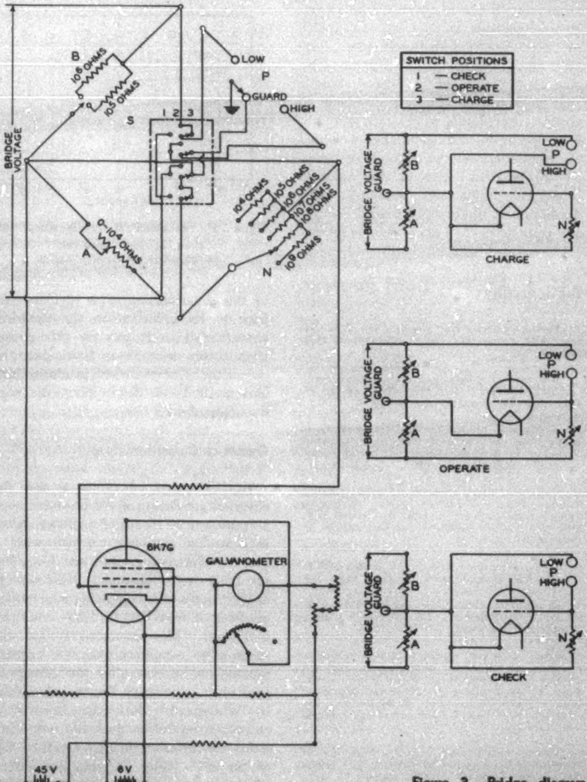
B
10^6 OHMS
10^5 OHMS
S
1 2 3
LOW
P
GUARD
HIGH
BRIDGE VOLTAGE
0-10^4 OHMS
A
10^4 OHMS
10^5 OHMS
10^6 OHMS
10^7 OHMS
10^8 OHMS
10^9 OHMS
N
6K7G
GALVANOMETER
45 V
6 V
SWITCH POSITIONS
1 — CHECK
2 — OPERATE
3 — CHARGE
BRIDGE VOLTAGE
GUARD
B
A
LOW
P
HIGH
N
CHARGE
BRIDGE VOLTAGE
GUARD
B
A
LOW
P
HIGH
N
OPERATE
BRIDGE VOLTAGE
GUARD
B
A
LOW
P
HIGH
N
CHECK

Figure 3. Bridge diagram

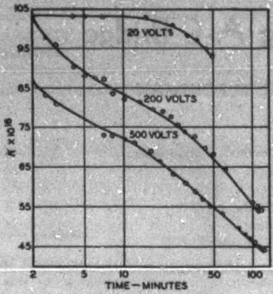

Figure 4. Specific conductance versus time

Table I. Specific Conductances of a Dewaxed Midcontinent Oil at Various Stages of Oxidation

Frequency	0	69	143	187	217	256	328
	Cubic Centimeters O_2 Absorbed						
Direct current	15	26	53	85	133	195	278
40	15	28	47	89	133	218	322
80	16	27	46	83	122	198	296
200	16	28	51	82	117	188	287
400	15	27	49	89	122	204	312
1,000	11	25	43	91	119	210	308

Values—$\kappa \times 10^{14}$; $T = 65$ degrees centigrade.

indicated by connections between the switch terminals. The usual off position then becomes the "operate" position of the bridge. The other two positions are indicated as "check" and "charge" positions. The "check" circuit allows the zero of the detector to be checked and adjusted, at the same time removing the bridge voltage from the measuring arm *P*. The "charge" circuit also allows the zero of the detector to be checked while leaving the bridge voltage across the measuring arm *P* so that measurements may be made from time to time with a continuous application of voltage to the measuring cell. This arrangement allows the time-decay of conductance to be measured from one to two minutes after the application of voltage, that time being necessary for the detector circuit and galvanometer to attain a steady state.

The 0.01, 0.1, and 1 megohm standards are accurate to 0.1 per cent and by connecting the proper open switch terminals of arm *N* to the low terminal of arm *P* it was possible to calibrate the 1,000, 100, and 10-megohm resistors to better than 0.3 per cent. The sensitivity of the detector is such that in measuring a resistance of the order of 10^{14} ohms the error is less than five per cent with about five volts applied. The detector sensitivity limits the value to which the bridge voltage may be reduced; this value also depending upon the resistance value of the oil-filled cell being measured. By the use of a more sensitive detector arrangement the accuracy of the bridge can be increased and the bridge voltage further reduced. It is obvious that the uses of this bridge are not limited to conductance measurements, but that it may also be used to measure almost any type of a high resistance up to 10^{15} ohms.

Guard Circuit

In this bridge the necessity for a conventional shield circuit is circumvented by the use of arms *A* and *B* as a shield circuit. The error in this method is negligible when measuring oils of low conductance and high resistance. However, as the value of the resistance between the guard ring and the low electrode of the cell decreases to approach the value of the resistance in arm *B*, the effective value of *B* will be accordingly affected. Since the resistance between the guard ring and the low electrode acts as a shunt around arm *B*, it is necessary in order to keep the error in arm *B* to less than 0.1 per cent that the value of this shunting resistance should be 1,000 times *B* or about 1,000 megohms. The resistance between the guard ring and the measuring electrode, which shunts around the detector, does not affect the accuracy of the bridge but may affect the sensitivity of the detector. Since the cell used in this work (figure 2) is so designed that the respective resistances between the guard ring and the measuring electrode and the guard ring and the low electrode are approximately the same as the resistance between the measuring and low electrodes, this guard circuit is quite satisfactory, and has the advantage of allowing measurements to be made rapidly. If it were desired it would be a comparatively simple matter to install a conventional shield circuit in the bridge.

Experimental Results

Two methods were chosen to show the value of low-voltage d-c measurements on electrical insulating oils. First, the change of d-c conductance with time was measured at various voltages and second, d-c measurements were compared with a-c measurements on the same oil sample. Through an inspection of these data, a few examples of which are presented here, a definite idea was obtained of the value of this type of a measurement.

Figure 4 shows the specific d-c conductances plotted against time for a heavy, dewaxed, solvent-refined, midcontinent oil[3] with bridge voltages of 20, 200, and 500 volts. It will be noted that with 20 volts per millimeter gradient (the cell spacing being 1.04 millimeters), a constant conductance was observed for a period of 15 minutes, while in the case of the higher potentials of 200 and 500 volts the conductance decreased rapidly with time from the initial two-minute reading. It is believed that these curves are thus an excellent confirmation of the theory expressed above, namely, that by reducing the potential gradient in the oil the velocity of the ions would be so reduced that the constant

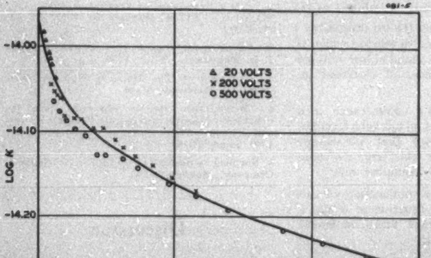

Figure 5. Logarithm of specific conductance versus (voltage × time)

conductance of region B in figure 1 would exist for a much longer period of time. This interpretation of these curves would lead us to conclude that low-voltage measurements of this type do measure the true d-c conductance of electrical insulating oils.

Figure 5 shows a plot of the logarithm of the specific d-c conductance (κ) of the same oil against the product of voltage (V) and time (t). For all three voltages the points fall along the same curve, showing that the conductance change depends upon the quantity of electricity passed through the oil. If we assume that this decrease in conductivity is due entirely to the discharge of ions at the electrodes, we can, by the use of certain other assumptions calculate the mobilities of the ions involved from the slope ($d \log_e \kappa/d(Vt)$) of a curve such as shown in figure 5. For this oil mobilities varying from 8×10^{-10} to 3×10^{-8} cm^2 sec^{-1} volt^{-1} were thus obtained. These mobilities are of a reasonable order of magnitude, and these investigations are being continued.

Using the power-factor bridge[4] which has been developed in this laboratory, power-factor measurements were made on the same oil samples on which d-c measurements were made. By the use of the formula

$$\kappa=0.556\times10^{-12}\nu \tan \delta C/C_0$$

where δ is the loss angle, C and C_0 are the capacitances of the cell with oil and air respectively as dielectrics, and ν is the frequency in cycles per second at which the measurement is being made, specific a-c conductances were computed for purposes of comparison with d-c values. Table I gives the specific conductances of a low-viscosity distillation cut of a midcontinent crude[5] at various stages of oxidation at 65 (±1) degrees centigrade. On inspection of these data it will be seen that there is, in general, good agreement between the d-c and a-c values, the average deviation from the mean being about five per cent. The deviations were thought to be due largely to fluctuations in temperature, and these fluctuations will be reduced in subsequent work. However, it is considered that these data do definitely show that the a-c losses are, in the case of this oil, wholly due to the same type of a loss which gives a d-c conductance and which is generally considered to be the result of ionic conductance. This type of a comparison is now being made on other oils that are being investigated in this laboratory. Since by this type of a comparison, low-voltage d-c measurements do account for all of the a-c losses it may again be concluded that the low-voltage measurements herein described do measure the true d-c conductances of electrical insulating oils.

Summary

1. Apparatus has been developed which is suitable for the measurement at low voltages of the d-c conductances of electrical insulating oils.

2. The change of d-c conductance with time of a highly refined oil has been found to substantiate the theory that low-voltage methods do measure the true d-c conductance of electrical insulating oils.

3. The change in the conductance of this oil is shown to be a function of $V\times t$, V being the applied voltage, and t the length of time of application.

4. D-c measurements have been compared with a-c measurements on a low-viscosity oil and low-voltage d-c measurements have been shown to account for all of the a-c loss in the oil thereby again leading to the conclusion that the low-voltage methods developed in this paper do measure the true d-c conductance of electrical insulating oils.

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Rapid-Recording A-C Bridge

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THIS paper describes a rapid automatic recording a-c bridge for continuously recording the power factor and capacitance during life runs or short-time tests on cables, capacitors, and insulating materials at a frequency of 60 cycles per second.

During the manufacture of electrical insulating structures and also during such extreme variations in conditions as occur on accelerated life tests, changes occur in the dielectric power factor and in the capacitance which are not fully reversible. A full record of such changes may be expected to contribute toward an understanding of the factors leading to failure under operating conditions. Data of this type in the past, generally were obtained manually, plotting point-by-point measurements made on a manual bridge. It is often desirable to obtain records of rapid changes which occur faster than can be recorded by this method. The automatic bridge described here rapidly records any change in the test specimen and is capable of operation 24 hours a day without attention. A photograph of the complete equipment is shown in figure 1.

A block diagram of the equipment is shown in figure 2. Referring to this diagram, a Schering bridge is used to measure capacitance and power factor. The voltage across the detector diagonal of the bridge, indicating bridge unbalance, is amplified and impressed upon circuits sensitive to components of the unbalance voltage produced by capacitance change and power-factor change respectively in the test specimen. These phase-sensitive circuits, called "phase selectors," control two thyratron circuits which supply power to two motors. The motors are coupled to adjustable circuit elements in the arms of the Schering bridge so that the unbalance voltage is reduced to zero and the motors are de-energized. Thus, the bridge is automatically balanced for the new values of capacitance and power factor which the test specimen has assumed. The time required for this operation is about one second. To prevent motor travel beyond the balance point because of the inertia of the motor arma-

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tures and the associated moving parts, an antihunting feature is built into the equipment.

The new positions assumed by the circuit elements are indicated on the capacitance and the power-factor recorders, the pens of which are electrically coupled to, and move in unison with, the shafts of the bridge-circuit elements. The scales are marked directly in capacitance increase and in power factor. The recording Schering bridge consists of the following units:

Figure 1. Rapid-recording a-c bridge

1. Schering bridge
2. Voltage amplifier
3. Phase selectors
4. Thyratron circuits and motors
5. Antihunting circuits
6. Recorders

The following is a brief discussion of the circuits and the principle of operation of each unit.

Schering Bridge

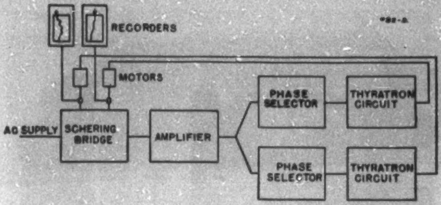

Figure 2. Block diagram of complete automatic bridge

The Schering bridge is the conventional one shown in figure 3. A fixed resistor R_4 is shunted by an adjustable capacitor, and connected in series with a high-voltage standard capacitor, C_1. R_3 is a manually adjustable resistor in series with the specimen capacitor. Power-factor balance is obtained by setting the adjustable capacitor for balance. Capacitance change in the specimen is measured by adjusting a tap on a slide-wire resistor shunting the R_3 resistor until bridge balance is obtained. Guard circuit potential is adjusted by setting the resistor *F* to the point which allows push button *B* to be operated with no change in the "capacitance change" balance.

A choice of several fixed resistance values at R_4 and a manually-operated step capacitor in parallel with the adjustable capacitor provide a means for selecting a power-factor range suited to the specimen tested. Range widths on the present bridge are 0.01, 0.02, and 0.05 tangent loss angle, which is approximately equal to the power factor or the sine of the loss angle. By use of the step capacitor, tangent loss angle as large as 0.3 may be measured, in which case the range covered by the recorder chart will be 0.25 to 0.30 tangent loss angle. Capacitance changes for full chart width deflection are $2^{1}/_{2}$, 10, 25, and 100 per cent. When used with a 100-micromicrofarad standard capacitor, capacitances from 50 to 10,000 micromicrofarads can be measured. Larger capacitances may be measured with the aid of external shunts. A photograph of the motor-operated Schering bridge unit is shown in figure 4.

It is possible to test several specimens in succession by providing an adjustable resistor in series with each and a time switch for connecting the slide wire at R_3 successively to each.

Voltage Amplifier

The function of the amplifier is to increase the feeble unbalance signal appearing at the output of the bridge when unbalance occurs, so that sufficient voltage is available to operate the phase-selector circuits. With a few exceptions, it is a conventional three-stage amplifier with resistance and capacitance interstage coupling and transformer input, as shown in figure 5. A photograph of the amplifier and phase-selector unit is shown in figure 6.

For proper operation of the phase-selective circuits which follow the amplifier, the phase shift in the amplifier must be either zero or an integral multiple of 90 degrees and the output voltage must be relatively free from harmonics. Control of both is obtained by the insertion of resistance-capacitance attenuators *R,C* between stages. The need for these effects is explained later.

Series resistances are inserted in the leads to the grids of the second and third stages to minimize parasitic phase shifts. To minimize changes in amplifier performance produced by changes in tube characteristics if the tubes overload, all plate load resistances are chosen low. Amplifier sensitivity is controlled manually by potentiometer resistors in the grids of the first two stages.

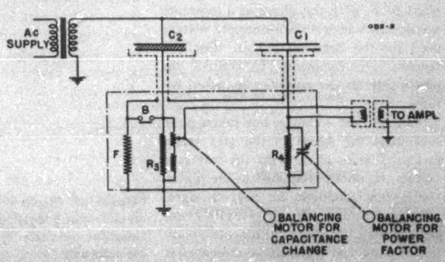
AC SUPPLY
C2
C1
B
F
R3
R4
TO AMPL
BALANCING MOTOR FOR CAPACITANCE CHANGE
BALANCING MOTOR FOR POWER FACTOR

Figure 3. Circuit diagram of Schering bridge unit

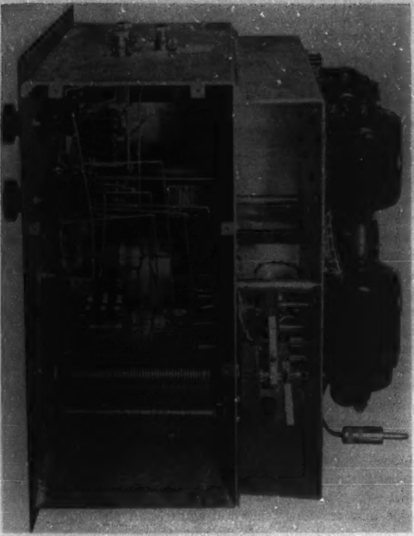

Figure 4 (left). Rapid-recording a-c bridge; Schering bridge unit

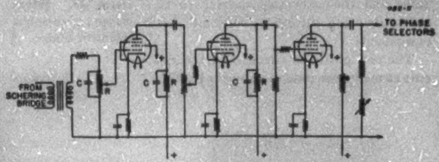

Figure 5. Circuit diagram of amplifier unit

Phase Selectors

The function of the phase selectors is to separate the voltage components of bridge unbalance produced by capacitance and power-factor changes in the test specimen. The amplified bridge unbalance voltage is simultaneously applied to both phase selectors, but a capacitance unbalance causes one phase selector to function while a power-factor unbalance actuates the other. If both quantities change in the test specimen, both respond. The amplifier and both phase selectors are mounted on one panel as shown in figure 6. For purposes of clarity, only one has been shown in the circuit diagram in figure 7.

The phase selector operates by comparing the phase and magnitude of the amplified bridge unbalance voltage applied to *T*, with the phase of a reference voltage E_R which is the a-c supply voltage used for the Schering bridge. For this reason, it is important to control the phase shift in the amplifier as noted before.

Assume that a change has taken place such that the signal to the grid transformer *T* is in phase with the reference voltage E_R which is applied to the plates. Under this condition, for a given signal voltage the phase selector gives its greatest output. For the instantaneous polarities of plate and grid voltage indicated in the diagram, tube 1 passes current during the half cycle that this condition is maintained. During the next half cycle, tube 2 passes current. Thus, a pulsating direct current passes through the common load resistor R_1, producing an average direct voltage across the output terminals leading to the thyratron circuit, the magnitude of which is proportional to the magnitude of the input signal to *T*. If the signal voltage is 180 degrees reversed, tubes 3 and 4 pass current through R_2, producing a voltage of opposite average d-c polarity across the output terminals. However, if the signal is in quadrature with the reference voltage, all tubes pass current during a portion of the cycle and the average direct voltage across the output terminals is zero. Under this condition, the other phase-selector circuit is actuated. The phase selector not described is identical except a 90-degree phase-shifting network is inserted ahead of the signal input transformer *T*.

The phase selector is not affected immediately by harmonics of the applied voltage, but it is necessary partially to filter out the harmonics present in the bridge unbalance voltage to avoid overloading the amplifier with these harmonic voltages, thereby decreasing the response to the fundamental bridge unbalance voltage.

Thyratron Circuits and Motors

The function of the thyratron circuits is to supply power to the balancing motors at a rate dependent upon the magnitude of the d-c output of the phase selectors. Both thyratron circuits are contained on one panel shown in the photograph in figure 8. The motors are mounted on the Schering bridge panel shown in figure 4.

A diagram of one of the thyratron circuits is shown in figure 9. The motor is series wound with a divided field winding, each field winding being connected in the plate circuit of a thyratron tube. The grid bias and plate-supply voltages,

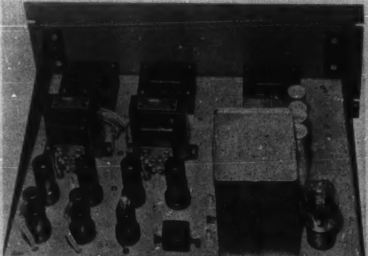

Figure 6. Rapid-recording a-c bridge; amplifier and phase-selector unit

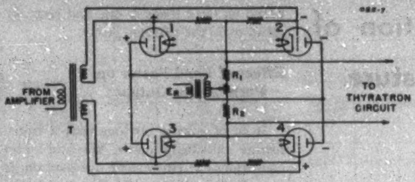

Figure 7. Circuit diagram of phase-selector unit

e_g and e_p respectively, are obtained from the 60-cycle a-c power supply. With d-c excitation on the grids, the tube with a positive grid fires or starts to pass current during each positive half cycle of the a-c supply voltage on its plate. This current persists until the end of the half cycle. If the d-c grid excitation changes polarity, the other tube fires, sending current through the other field winding and causing the motor to reverse direction. Speed of rotation is controlled as follows. For a high value of d-c grid excitation, the tube passes current during the full half cycle. For low excitation, the current impulse is shorter because the tube fires later in the half cycle. Hence, the average current through the motor becomes less and the motor runs more slowly.

The operation of the thyratron tube in this circuit can be explained as follows. The capacitor C_4 in the grid-bias transformer T_3 produces a constant phase shift in the alternating bias voltage e_g which lags the plate voltage e_p by about 90 degrees as shown in figure 10. Under this condition, the grid is at no time sufficiently positive to cause the tube to fire. With a slight positive d-c grid excitation superimposed upon the a-c bias, e_g is raised high enough to cause the tube to fire during the latter part of the positive half cycle of e_p. With increased d-c excitation, e_g is raised still higher so that the lowest point in this curve becomes sufficiently positive to cause the tube to fire, and the tube passes current for the full half cycle. Thus, regulated control of motor speed, depending upon the magnitude of the grid excitation voltage, is obtained.

Figure 8. Rapid-recording a-c bridge; thyratron unit

Antihunting Circuit

The function of this circuit is to prevent hunting produced when the inertia of the motor and associated rotating parts cause the motor to follow through or overshoot the correct position when the bridge balance is reached, thereby unbalancing the bridge in the reverse direction and setting up an oscillating condition.

A resistor R_8 and capacitor C_8 shown in figure 9 are inserted in the signal input lead to the thyratron circuit. These build up a temporary d-c potential in series with the signal voltage and opposite in polarity. This reduces the effective voltage at the thyratron grids and causes it to drop to zero slightly before the bridge has actually reached balance. The motor, by virtue of its inertia, then tends to coast to the proper position. Should the motor not stop at the correct position, the antihunting voltage quickly drops to zero, the signal voltage again takes effect, and the motor receives an additional impulse which finishes the balancing operation.

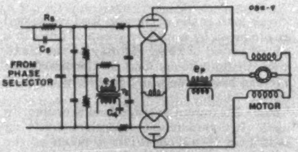

Figure 9. Circuit diagram of thyratron unit

Recorders

The function of the two recorders is to follow the position of the capacitance and power-factor balancing elements in the Schering bridge, thereby recording the capacitance and power factor of the test specimen as a function of time.

Each recorder has a separately excited ratio-type instrument movement excited from the a-c supply line through a resistance potentiometer mechanically coupled to the shaft of the balancing element in the bridge. The line voltage can vary as much as 20 per cent without affecting the indication. Chart speeds of one inch per minute and one inch per hour are provided.

Switches for changing bridge range, giving an alarm, or discontinuing the test may be located on the balancing elements to handle specimen changes larger than anticipated.

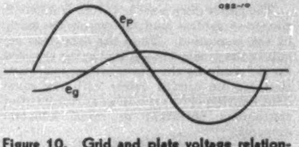

Figure 10. Grid and plate voltage relationship; thyratron unit

Summary

A power-factor and capacitance bridge capable of unattended recording has been described. Changes in the specimen may be fully recorded within one second of their occurrence.

The bridge may be used in connection with life tests, during treating processes, and for recording tests on large numbers of nominally identical structures, in the process of production.

The automatic control circuits described in this paper are not limited only to application to the Schering bridge, but may be used for automatic balancing of practically any other type of a-c bridge as well.

References

1. A New Phase-Angle Meter, W. Mikelson. *General Electric Review*, volume 41, December 1938, pages 557–8.

2. An Automatic A-C Potentiometer and Its Application to the Nondestructive Testing of Insulating Equipment, George Keinath. AIEE Transactions, volume 58, 1939, pages 887–90.

Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 "Transactions Supplement" to Electrical Engineering.

Effect of Load Factor on Operation of Power Transformers by Temperature

V. M. MONTSINGER
FELLOW AIEE

Synopsis: The practice of loading transformers by temperature is growing among operators who desire to take advantage of the additional capacity offered by this method of loading. The standards now recognize continuous overloads in cool ambients and short-time overloads under recurrent and emergency conditions.

The paper discusses the effects of different shapes of system load factors on the aging of the insulation. Then the ones that result in the greatest amount of aging are used for determining the permissible peak loads that can be carried safely with the same deterioration of its insulation as results from continuous operation at rated kilovolt-amperes.

A rational method of calculating the hot-spot temperature is shown by means of examples using a general average "time constant". The hot-spot temperature curve is then integrated to compare the aging with a given constant temperature.

From these results it is concluded that a normal power transformer can be overloaded 3 per cent for each 10 per cent that the system load factor is below 100 per cent. That is, for a 50 per cent load factor the permissible peak load is 15 per cent over the transformer rating in a standard ambient. For low ambients where 1 per cent overload for each degree that the ambient is below 30 degrees is now permitted, the permissible peak loads will vary depending on ambient and on load factor. By combining the 3 per cent rule for load factor and 1 per cent rule for ambient, proposed loading curves are shown for different load factors and different ambients.

SEVERAL papers have been written[1,3,5] on the operation of transformers by temperature during the past several years. As a result of these activities, the American Standards Association sectional committee on transformers, *C*-57 has published "Guides for Operation of Transformers" as an appendix to the proposed American standards for transformers, regulators, and reactors.

These guides give permissible overloads under the following conditions: (1) continuous overloads in cool ambients, (2) short-time overloads under recurrent conditions, and (3) short-time overloads under emergency conditions. These short-time overloads apply to transformers irrespective of the system load factors, and are limited to a few hours duration.

There are conditions under which the maximum load may last for times longer than those permitted for the overloads given in the guides, and where it is safe to carry some overload. It is primarily for this condition that this paper is presented.

Load Factor—Definition

The ASA definition for load factor (35.10.125) is as follows:

"Load factor is the ratio of the average load over a designated period to the peak load occurring in that period".

The ASA definition of peak load (35.10.135) is:

"Peak load is the maximum load consumed or produced by a unit or group of units in a stated period of time. It may be the maximum instantaneous load or the maximum average load over a designated interval of time".

"*Note:* Maximum average load is ordinarily used. In commercial transactions involving peak load (peak power) it is taken as the average load (power) during a time interval of specified duration occurring within a given period of time, that time interval being selected during which the average power is greatest".

Load factor as used in this paper refers to the load on the transformer bank under consideration. It is to be noted that load factor as so defined is to be distinguished from the ratio of the average load to the transformer rating, as this latter ratio is, by ASA definition, "capacity factor".

Very few power transformers operate at 100 per cent load factor, the majority of load factors being in the order of 50 to 70 per cent.

There are in general three types of loads, industrial, residential, and combined industrial and residential, all of which generally have load factors well below 100 per cent. Figures 1, 2, and 3 are good illustrations of such loads. These load factors are 51.5 per cent, 55 per cent, and 60.5 per cent respectively, based on peak loads of one-half hour, instead of on the extreme peaks.

Effect of Load Factor on Permissible Output

It is, of course, obvious that a transformer operating under low-load-factor conditions can carry some overload under peak load conditions with the same degree of aging of its insulation as one carrying rated load continuously. The problem is to determine the amount of overload that will give the same aging, for different load factors. To do this requires: (1) determination of the hottest-spot temperature during one complete 24-hour period, and (2) integration of the hottest-spot temperature curve during the 24-hour period, to obtain the amount of aging of the insulation. When an overload is found (by trial) to cause the same amount of aging as occurs when the transformer is carrying rated load for 24 hours, this represents the permissible overload for that particular kind of load curve.

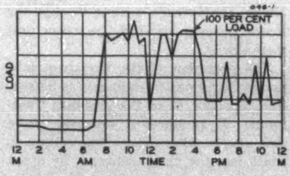

Figure 1. Industrial load, 51.5 per cent load factor

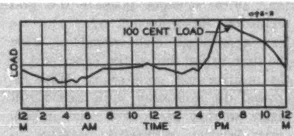

Figure 2. Residential load, 55 per cent load factor

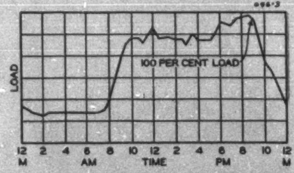

Figure 3. Industrial and residential load, 60.5 per cent load factor

Figures 1–3. Typical load curves on American Gas and Electric Company system

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Paper 40-96, recommended by the AIEE committee on electrical machinery and standards co-ordinating committee number 4, and presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940. Manuscript submitted April 4, 1940; made available for preprinting April 29, 1940.

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1. For all numbered references, see list at end of paper.

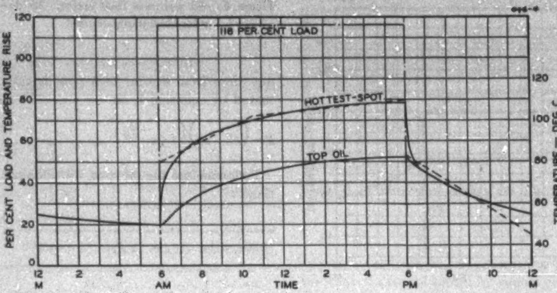

Figure 4. 50 per cent load factor composed of industrial load for 12 hours during 24-hour period

24-hour average load = 58 per cent; 30 degrees centigrade ambient

The permissible overload for a given load factor varies over a wide range, depending on the shape of the load curve. For example: a 50 per cent load factor in which 100 per cent load is carried 50 per cent of the time, with no load on the remainder of the time (figure 4) will permit a minimum overload rating, whereas a 50 per cent load factor in which partial load is carried most of the time and a peak load is carried the rest of the time (figure 7) will permit a greater overload rating. The first case might correspond to an industrial 50 per cent load factor condition requiring power during the day, only, and the second case, might correspond to a condition where, during the late evening hours, a heavy residential peak load is superimposed on a light 24-hour load.

Table I

Kva	High-Voltage Kv	B Time Constant
1,000	13.8	2.86
1,000	13.8	2.78
1,000	69.0	4.55
		3.39
2,500	13.8	2.83
2,500	69.0	4.0
2,500	138.0	4.46
		3.76
5,000	13.8	2.32
5,000	69.0	2.64
5,000	110.0	3.70
		2.89
8,333	34.5	3.03
8,333	69.0	3.23
8,333	138.0	4.55
		3.60
20,000	34.5	2.5
20,000	69.0	3.41
20,000	138.0	4.2
		3.37
33,333	69.0	3.03
33,333	115.0	3.20
33,333	138.0	3.65
		3.29

Calculation of Hottest-Spot Temperature

The hottest-spot temperatures are calculated in two stages. First, the top-oil temperature rise is obtained, and then the hottest-spot rise over the oil is determined and added to the top-oil rise. The most difficult part is the calculation of the oil temperature under varying load conditions. When this is done, the rest is easy, since the winding rise over the oil becomes constant in 20 to 30 minutes and can be taken directly from a curve of ultimate rise versus load, and added to the oil rise.

For the particular case under consideration the following assumptions are made, in calculating the hottest-spot temperatures.

1. The ratio of losses at rated load is 2:1 (2 copper, 1 iron).
2. The ultimate top-oil rise at rated load is 45 degrees centigrade.
3. The hottest-spot temperature rise over the top oil is 20 degrees centigrade (10 degrees average rise +10 degrees hot-spot rise over average rise) at rated load, and varies as the load raised to the 1.6 power.[1]
4. The no-load ultimate oil rise is 19 degrees centigrade. (The exact value based on 2:1 ratio of losses at rated load and temperature rise varying as 0.8 power is 18.7 degrees centigrade.)
5. The ultimate oil rise varies as the 0.8 power[1] of the losses.
6. The time constant corresponds to that for the average single-phase power transformer ranging in size from 1,000 to 33,333 kva. Table I.
7. One hundred per cent excitation voltage is on at all times. The time constant is, therefore, based on the copper losses only.
8. The load cycle selected is repeated from day to day such that the oil temperature is the same at the beginning and end of each 24-hour period.
9. A constant ambient temperature of 30 degrees centigrade is maintained.

Tests have shown[5,11] that the oil rise under transient conditions can be estimated quite closely by the exponential formula:

$$\theta_r = \theta_u\left(1-\epsilon^{-\frac{t}{B}}\right) \tag{1}$$

where

θ_r = temperature rise of oil at any time t
θ_u = ultimate oil rise
ϵ = 2.718
t = time in hours
B = time constant*
$= \dfrac{C \times \theta_u}{\text{loss at initial temperature}}$
C = thermal capacity of iron core, windings (copper) tank and oil
$= \dfrac{3.5 \times \text{pounds (core+copper+2/3 tank)} + 90G}{60}$
G = gallons oil (United States)

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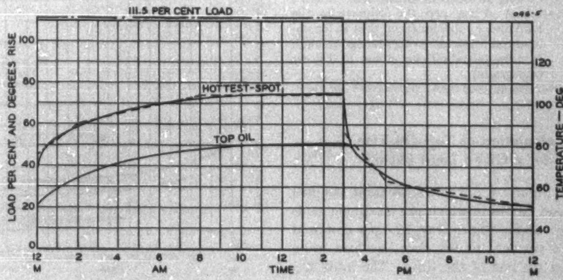
* The time constant is the time required to reach ultimate temperature rise, if all heat were stored in the transformer.

Figure 5. 62.5 per cent load factor; 30 degrees centigrade ambient



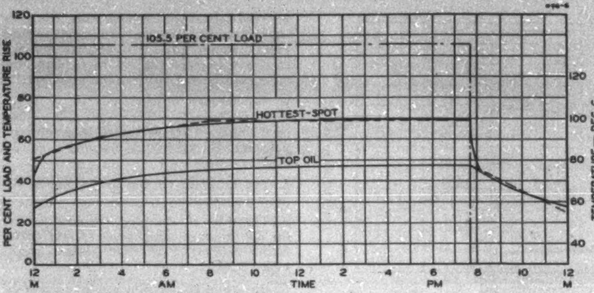


Figure 6. 82 per cent load factor, 30 degrees centigrade ambient

Only 2/3 of the tank is used because the lower third is comparatively cool, and 85.7 per cent (of 105*G* the correct value to use when the oil is at a uniform temperature) of the oil is used for the same reason.

In table I are some representative values of *B* for load (copper) losses only, taken at 110 per cent load. One hundred and ten per cent load was chosen for the reason that the overload periods exert the greatest effect on the life of the insulation, and 110 per cent load is a general average value used for the various load factors.

The average of the values of *B* in the table is 3.38. A general average value of 3.33 appears to be a reasonable one to use. It is proper to point out, however, that *B* varies to some extent for different oil rises for the reason that the oil rise is not proportional to the losses but varies as the 0.8 power of the losses. Tests show,[8] however, that results close enough for practical purposes are obtained by using an average value of *B* for the oil rises under consideration.

Equation 1 applies to oil-temperature rises. When the oil temperature is cooling down, the following equation applies:

$$\theta_c=\theta_i\left(1-\epsilon^{-\frac{t}{B}}\right) \qquad (2)$$

Where

θ_c = cooling in degrees during time period t
θ_i = initial oil rise over the ultimate for the load under consideration (that is, for no-load condition following some given load, θ_i is the initial oil rise minus 19 degrees centigrade)
t = time in hours
$B = 3.33$

Calculation of Top-Oil Rise for a Given Load Factor

Figures 4, 5, and 6 show three assumed load curves in which the maximum loads are on for 50 per cent, 62.5 per cent, and 82 per cent of the time—no load being on the rest of the time. The loads are, therefore, 50, 62.5, and 82 per cent respectively. Figure 7 shows load curve with a 50 per cent load factor, made up of a 51.5 per cent load for 19 hours, and 140 per cent load for 5 hours representing a peak residential overload from 5 to 10 p.m.

As an illustrative case, the top-oil rise will be calculated for the 50 per cent load factor, figure 4.

It was found by trial that the oil rise is 25 degrees at the beginning and end of the 24-hour period. This gives a starting point of 25 degrees centigrade rise at 12 midnight.

From 12 to 6 a.m., the oil is cooling down. Using equation 2 we have (when $t=6$ and the ultimate no-load oil temperature rise is 19 degrees centigrade)

$$\theta_c=\left(25-19\right)\left(1-\epsilon^{-\frac{6}{3.33}}\right)=5.0 \text{ degrees centigrade}$$

Therefore, at 6 a.m. the oil rise has dropped from 25 to 20 degrees centigrade.

At 6 a.m. 116 per cent load comes on and the oil temperature starts to rise. The ultimate oil rise for 116 per cent load is 53 degrees centigrade. The oil rise at any time from 6 a.m. to 6 p.m. is estimated by equation 1, or

$$\theta_r=\left(53-20\right)\left(1-\epsilon^{-\frac{t}{3.33}}\right)$$

Following are the oil rises, for different times t

Time	7 a.m.	10 a.m.	2 p.m.	6 p.m.
t=	1	4	8	12
θ_r=	8.6	23	30	32.0 over 20 degrees rise (starting at 6 a.m.)

From 6 p.m. to 12 midnight, the oil temperature again decreases:

$$\theta_u=(52.0-19)\left(1-\epsilon^{-\frac{6}{3.33}}\right)=27.0 \text{ degrees}$$

or $52.0-27=25.0$ degrees centigrade rise at 12 midnight

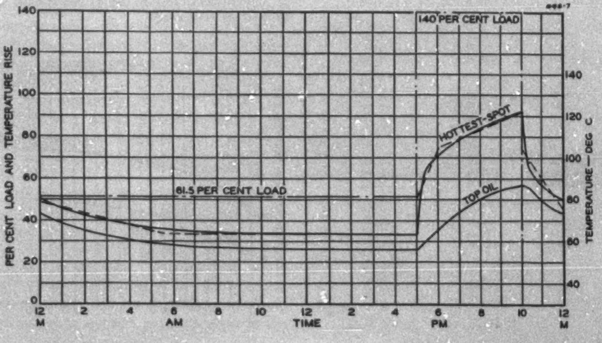
Adding 30 degrees centigrade ambient to the above values, we obtain the top-oil temperature as shown in figure 4.

The next step is to add the winding hottest-spot to the top-oil temperature curve shown in figure 4. The ultimate winding hottest-spot rises over the oil versus load are shown in figure 8, assuming 20 degrees rise at rated load.

The hottest-spot curve figure 4 is divided into trapezoids (dashed lines), for the purpose of integrating the area to determine the aging during a 24-hour period.

Figure 7. 50 per cent load factor, composed of light industrial and peak residential load

24-hour average load = 70 per cent; 30 degrees centigrade ambient



As shown in a previous paper,[10] the aging may be computed by the following equations:

For a constant temperature

$$A = t\epsilon^{kT} \qquad (3)$$

where

A = aging units
ϵ = 2.718
T = temperature degrees centigrade
k = 0.0865—which doubles the aging for each 8-degree-centigrade increase in temperature

For a trapezoid in which the two unequal sides represent temperature

$$A = t\left[\frac{\epsilon^{kT_2} - \epsilon^{kT_1}}{k(T_2 - T_1)}\right] \qquad (4)$$

where

T_2 = the maximum temperature degrees centigrade
T_1 = the minimum temperature degrees centigrade

As previously stated, the load-cycle conditions have been selected such that the aging will be the same as that under continuous rated load conditions. For a 24-hour period, the aging at 95 degrees centigrade (55 rise by resistance+10 degrees hottest-spot+30 degrees ambient) is by equation 3

$$A = 24\epsilon^{0.0865\times 95}\ 87{,}500 \text{ units}$$

Integrating the hottest-spot (dashed) curve in figure 4, the aging by equation 4 is

$$A_1 = 6\left[\frac{\epsilon^{0.0865\times 55.0} - \epsilon^{0.0865\times 50}}{0.0865(55.0-50)}\right] = 570$$

$$A_2 = 4.5\left[\frac{\epsilon^{0.0865\times 102} - \epsilon^{0.0865\times 80}}{0.0865(102-80)}\right] = 13{,}650$$

$$A_3 = 7.5\left[\frac{\epsilon^{0.0865\times 110} - \epsilon^{0.0865\times 102}}{0.0865(110-102)}\right] = 70{,}000$$

$$A_4 = 6\left[\frac{\epsilon^{0.0865\times 83} - \epsilon^{0.0865\times 45}}{0.0865(83-45)}\right] = \underline{2{,}320}$$

$$\text{Total} \quad 86{,}540 \text{ units}$$

The figure 86,540 is close enough to 87,500 units since a change of one degree centigrade affects the aging approximately 12.5 per cent—based on the rate of aging doubling for each eight-degree-centigrade increase in temperature.

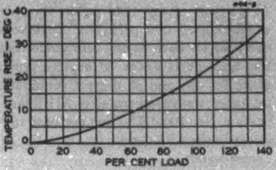


Figure 8. Hottest-spot winding rise over top oil

It is well to point out that "integration by parts" of the hot-spot temperature curve gives approximately correct results. That is, if in figure 4 the "time" is divided into one hour and one-half hour intervals (depending on the rate of change of temperature) and the average of these time interval hot-spot values is used for T in equation 3, the total aging units obtained are 86,980 which are within 0.5 per cent of those obtained by equation 4. In fact, when the hot-spot curve is quite irregular, the method of "integration by parts" is simpler and probably more accurate.

The aging of figure 7 is by equations 3 and 4:

$$A_1 = 5.5\left[\frac{\epsilon^{0.0865\times 79} - \epsilon^{0.0865\times 64}}{0.0865(79-64)}\right] = 2{,}800$$

$$A_2 = 11.5\times\epsilon^{0.0865\times 64} = 2{,}890$$

$$A_3 = 1\left[\frac{\epsilon^{0.0865\times 105} - \epsilon^{0.0865\times 80}}{0.0865(105-80)}\right] = 3{,}570$$

$$A_4 = 4\left[\frac{\epsilon^{0.0865\times 121.5} - \epsilon^{0.0865\times 105}}{0.0865(121.5-105)}\right] = 75{,}000$$

$$A_5 = 2\left[\frac{\epsilon^{0.0865\times 105} - \epsilon^{0.0865\times 75}}{0.0865(105-75)}\right] = \underline{6{,}400}$$

$$\text{Total} \quad 90{,}660 \text{ units}$$

Again, 90,660 units is close enough to 87,500 units—the aging at 95 degrees centigrade for 24 hours.

From the above results, it can be seen that the permissible overload for a 50 per cent load factor ranges from 16 per cent (figure 4) to 40 per cent (figure 7) depending on the character of the overload.* With shorter than five-hour peak loads imposed on partial load conditions, the permissible overload would be greater than 40 per cent. For a one or two-hour peak load, the permissible overload, considered only from the standpoint of aging, might be as high as 80 to 100 per cent. These heavy overloads might not be safe. In the first place, there are often limits other than aging of the insulation, such as overheating of lead joints (when soldered) and ratio-adjuster contacts, regulation, etc. In the second place, it has been shown[11] that when cellulose is maintained at temperatures above approximately 120 degrees centigrade chemical deterioration becomes an important factor.

Table II. Permissible Loads for Self-Cooled Transformers (Per Cent of Rated Capacity)

Per Cent Load Factor	Ambient Temperature (Deg C) 0	5	10	15	20	25	30
50	145	140	135	130	125	120	115
60	142	137	132	127	122	117	112
70	139	134	129	124	119	114	109
80	136	131	126	121	116	111	106
90	133	128	123	118	113	108	103
100	130	125	120	115	110	105	100

Under a condition of this kind the short-time heavy overload should be limited to the time specified in the ASA guides. For example if, when following 50 per cent load, the overload was 1.4 times the rated load current the time should be limited to approximately 25 minutes for recurrent conditions and to approximately 100 minutes for emergency conditions.

General rules for overloading transformers should be safe for the worst condition, as one is not sure of the conditions under which these rules will be used.

An integration of the hottest-spot temperature curves shown in figures 5 and 6 gives maximum peak loads of 111.5 per cent with 62.5 per cent load factor and 105.5 per cent with 82 per cent load factor.

It is recognized that with water-cooled transformers the aging under low load factor might be more than for self-cooled transformers, because the oil temperature will respond more quickly to load changes. The difference, however, will not be very great since the faster oil cooling will partially compensate for the faster oil heating up.

The ASA recommended overloads as covered in the guides for operation of transformers, limits two-hour recurrent overloads to 10 per cent and 20 per cent, following full-load and no-load conditions respectively; also, emergency two-hour overloads are limited to 25 per cent and 40 per cent respectively.

The object of presenting the data in this paper is not to replace or supersede the ASA's recommendations which are

Table III. Permissible Loads for Water-Cooled Transformers (Per Cent Rated Capacity)

Per Cent Load Factor	Ambient Temperature (Deg C) 0	5	10	15	20	25
50	140	135	130	125	120	115
60	137	132	127	122	117	112
70	134	129	124	119	114	109
80	131	126	121	116	111	106
90	128	123	118	113	108	103
100	125	120	115	110	105	100

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* For the load curves shown in figures 1, 2, and 3, the permissible overloads are in the order of 25 per cent to give the same aging as for continuous operation at rated loads. In other words, the rating of the transformers could be 80 per cent of the peak loads.

not directly coupled with load factor, but rather to show what is possible under different load-factor conditions, and (most important) to point out the limitations to the use of overloads for various load factor conditions. Several utility engineers are calculating[7,8,9] the permissible overloads on distribution transformers under various conditions of service, and it is the hope that this paper will be of some benefit in these efforts.

We have seen that under the most severe conditions 16 per cent overload can be held on a transformer operating at its maximum load 50 per cent of the time, that is, 50 per cent load factor, figure 4. For 62.5 per cent and 82 per cent load factors shown in figures 5 and 6, 11.5 per cent and 5.5 per cent overloads respectively can be held.

Expressed in terms of load factor, we have:

Figure	Per Cent Load Factor	Per Cent Overload	Per Cent Overload for Each 10 Per Cent Reduction in Load Factor Below 100 Per Cent
4	50	16	3.2
5	62.5	11.5	3.07
6	82.0	5.5	3.05

It appears, therefore, that a safe rule would be to permit 3.0 per cent overload for each 10 per cent that the load factor is under 100 per cent. Approximately the same overload values were obtained in my 1930 AIEE paper,[1] although a different method of calculation was used.

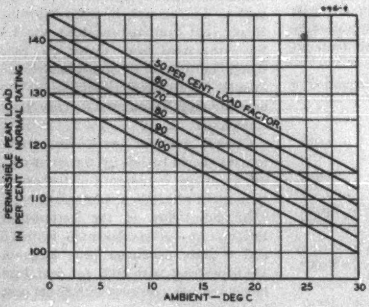

Figure 9 (left). Loading curves for self-cooled transformers

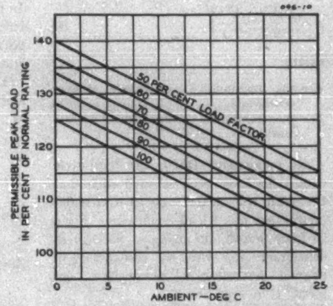

Figure 10 (right). Loading curves for water-cooled transformers

As a result of my 1930 paper, the rule which allows one per cent continuous overload for each degree centigrade that the ambient is under 30 degrees centigrade for air, and 25 degrees centigrade for water, was incorporated in AIEE publication No. 100. The one per cent rule is now in the ASA Guides for Operation of Transformers.

This information will permit taking advantage of both low load factor conditions and low ambient conditions, in cases where this seems advisable.

The proposal is to combine the three-per cent rule and the one-per cent rule which will give the permissible peak loads in per cent of normal rating for low load factors and low ambients as shown in tables 2 and 3 for self-cooled and water-cooled transformers respectively.

The values shown in tables II and III are plotted in curve form in figures 9 and 10. It is not intended that these overloads relating to load factors, be added to the short-time overloads already permitted by the ASA Guides for Operation of Transformers.

Future Work

To complete the work on loading power transformers by temperature, it is planned to present, at some future date, a paper showing how to convert any load cycle consisting of short-time high peak loads to the equivalent rectangular load cycle, and then limit its duration to the time given by the ASA guides.

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 "Transactions Supplement" to ELECTRICAL ENGINEERING.

Dead Points in Squirrel-Cage Motors

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Synopsis: Certain squirrel-cage motors are known to have a wide variation in starting torque depending on the angular position of the rotor. In extreme cases the torque may be actually negative at certain positions making the motor entirely useless. Various names such as "dead points," "cogging," and "locking" torque have been used to describe this condition but no detailed study of its mechanism appears to be available.

An analysis of the causes of such behavior is presented here as one of several phenomena not covered by classical theory since they arise from the nonsinusoidal nature of the air-gap field. It is shown (1) that only with certain definite relations of slot numbers can dead points exist at all, and (2) that, given one of these vulnerable slot combinations, the amplitude of the torque variation moves over an extremely wide range as the coil span is changed. The effect of coil span is not merely that due to the numerical change of harmonic chord factor but is shown to be the result of changes of direction of the various component forces. Test results confirming the analysis are included. It now becomes possible to correlate a great many apparently divergent results and to avoid troublesome combinations in future designs.

THE simple elementary theory of squirrel-cage induction motors treats the air gap field as a pure sine wave. Based on this simplification the classical laws of motor performance have been developed. It is evident, however, that in most actual motors the flux field differs sufficiently from a simple sine wave to warrant a closer study of its shape if the performance is to be predicted accurately. For example, the following phenomena depend largely on the nonsinusoidal nature of the flux field.

1. Vibration and noise.
2. Stray load loss.
3. Parasitic torques resulting in
   - (*a*). **Asynchronous cusps in the torque curve.**
   - (*b*). **Synchronous locking at low speeds.**
   - (*c*). **Dead points or locking at zero speed.**

While all of these subjects have been investigated to some extent, the dead-point phenomenon has probably received the least attention and no detailed study of its mechanism is available, at least in the English language. The present paper attempts to fill this gap, offering an explanation of the cause of dead points and some test results that confirm the analysis. It is shown that only with certain definite relations of slot numbers is it possible for dead points or locking torque to exist at all, and, further that the magnitude of the locking torque varies over an extremely wide range as the coil span is changed. While it is admitted that exact quantitative results are difficult to predict, a basis for judging the probability of trouble is established.

Test Observations

A motor which exhibits dead points, or cogging, as it has sometimes been called, will be found to have the following characteristics. The locked-rotor torque varies with the angular position of the rotor, passing through a cycle for each rotor slot pitch. If the running torque is measured slightly above and slightly below zero speed, the torque is found to agree quite closely with the *average* locked value. The locked torque, therefore, is a constant amount plus a variable component which has positive and negative values dependent on the rotor position. The net locked torque may be entirely positive or it may be zero or even negative at certain positions of the rotor. In the latter case the motor is truly "dead" and will not start even without load. However, when the torque is all positive it is only the minimum value that is useful for starting a load. Figure 1 shows tested values of locked torque for a motor of this kind.

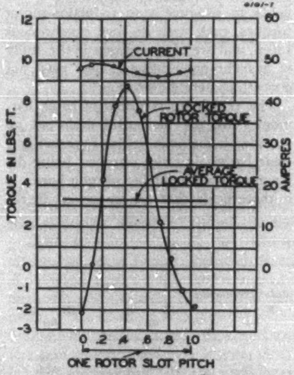

Figure 1. Typical test curve showing variation of locked rotor torque with rotor position

Motor had 24 stator slots, 18 rotor slots, 2 poles, 3 phases, 7/12-coil span, rotor-skewed one rotor-slot pitch

The variable component of torque is similar to the torque of a synchronous machine being dependent on the angular position of the rotor and passing through positive and negative half cycles. One is led therefore to search for a similar cause: two independent fields which are in synchronism at zero rotor speed and have distributions in space such as to give the required torque-angle characteristics. The following discussion shows that the necessary fields are present to fulfil just these requirements.

It has been observed further that motors having the same combination of stator and rotor slots may have widely different amounts of the variable torque component. Since most explanations of this phenomenon have been based heretofore on considerations of permeance variations, the observed results have appeared inconsistent. The analysis given here shows that these results can be explained.

Analysis

1. STATOR HARMONICS

As with many problems of this kind, it is sufficiently accurate to examine the harmonic fields of stator and rotor which result from the following assumptions:

(*a*). The slot currents are concentrated at points.

(*b*). The air gap permeance is uniform.

For integral-slot stator windings it is well known that the possible harmonics are:

$$n = 1 + MK \qquad (1)$$

where M is the number of phase belts per pair of poles, K is any positive or negative integer including zero. The algebraic sign of n shows the direction of travel with respect to the fundamental field. The velocity of each harmonic field is $1/n$th of the speed of the fundamental.

2. HARMONIC FIELDS OF SQUIRREL-CAGE ROTORS

A squirrel-cage winding carrying currents induced by the fundamental stator

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1. For all numbered references, see list at end of paper.

field sets up harmonic fields of the following order in addition to its fundamental field.

$$m = 1 + KS_2 \qquad (2)$$

where S_2 is the number of rotor bars per pair of poles, K is any integer. Usually the only fields having sufficient magnitude to be of importance are those obtained by letting K equal plus and minus one.

This, however, does not complete the list of rotor fields. In addition to currents in the rotor bars induced by the fundamental stator field, there are currents resulting from each of the other stator fields. For each separate current component flowing in the bars there are the following fields:

1. The nth, corresponding to the nth stator field which induced the current under consideration
2. The mth, where

$$m = n + KS_2 \qquad (3)$$

In this case it is usually necessary to consider only the one or two lowest values of m resulting from any given value of n.

3. COMPARISON OF ROTOR AND STATOR HARMONIC FIELDS

It is now possible to set down the harmonic fields of both members and compare them. For purposes of explanation it will be simpler to use an actual case and the one selected is a motor for which test data are available. The specifications are as follows:

Number of poles = 2
Number of stator slots = 24
Number of rotor slots = 18
Number of phase belts (total) = 6
Slots semiclosed for both members.

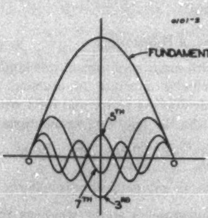

Figure 2. Relation of third, fifth, and seventh harmonics to the fundamental for a single coil

From these facts and the expressions for stator and rotor harmonics above, the orders of the various fields can be written as shown in table I.

The stator harmonics are simply those that occur in any three-phase motor having an integral-slot winding and the list could be carried out indefinitely. The first column under rotor harmonics is the series induced by the corresponding stator fields. These combine with the stator fields to produce true induction-motor torques, each with its own synchronous speed, and are of importance in the study of asynchronous cusps. They need not concern us here, however.

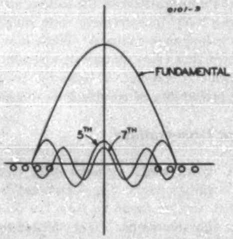

Figure 3. Relation of the fundamental, 5th and 7th harmonics for a three-phase winding with four slots per phase belt and full-pitch coils

The remaining columns of rotor harmonics are the incidental fields set up along with the main fields of column one, their harmonic order numbers being determined by the number of rotor bars. It is these fields that are important in the present study. It will be noticed that in setting up a secondary field with fundamental distribution we incidentally produce 17th and 19th harmonics which have the same signs as the 17th and 19th stator fields. Thus there is a pair of 17th harmonic fields moving backward at $^1/_{17}$th of synchronous speed but maintaining synchronism with each other while the rotor is at rest, and there is a pair of 19th harmonic fields going forward at $^1/_{19}$th speed under the same conditions. There are, then, steady torques produced by each of these pairs of fields but the direction and magnitude of these torques and their dependence on rotor position will have to be investigated further.

Table I

Stator Harmonics	Rotor Harmonics				
		(K = −1)	(K = +1)	(K = −2)	(K = +2)
n	n	m_1	m_1	m_2	m_2
+ 1	+ 1	−17	+19	−35	+37
− 5	− 5	−23	+13	−41	+31
+ 7	+ 7	−11	+25	−29	+43
−11	−11	−29	+ 7		+25
+13	+13	− 5	+31	−23	
−17	−17	−35	+ 1		+19
+19	+19	+ 1	+37	−17	
−23	−23	−41	− 5		+13
+25	+25	+ 7	+43	−11	

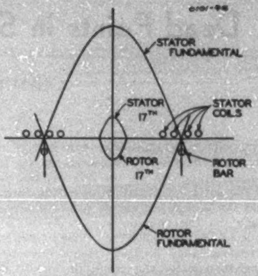

Figure 4a. Relation of 17th-harmonic waves of stator and rotor, for a particular coil span, with rotor in original position

Note: The 17th harmonic is drawn to an enlarged horizontal scale for clearness

Before going into these questions, however, we can take note of the fact that there are other such pairs of fields present. For each harmonic in the stator column, there is a corresponding harmonic in one or more of the rotor m columns.

4. THE PHASE RELATIONS BETWEEN THE HARMONICS

In order to proceed with an analysis of the torque components it is first necessary to establish phase relations between the various harmonic fields. Figure 2 shows three of the component fields of a single full-pitch coil in relation to the fundamental. If there are several symmetrical phases of one coil each, this picture also represents the relation between the combined harmonics of all phases at the particular instant when the current in the coil shown is a maximum. It will be noted that those harmonics in the series 1, 5, 9, 13, 17, etc., have positive maximums coinciding. Those in the series 3, 7, 11, 15, 19, etc., have their negative maximums coinciding with the positive

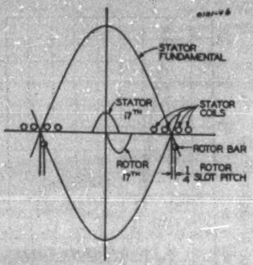

Figure 4b. Relation of stator and rotor 17th-harmonic waves after rotor has been moved one-fourth of a rotor-slot pitch

Table II. Magnitude of Torque Components for Different Coil Spans

n	m	Coil Span as a Fraction of Full Pitch 5/12	6/12	7/12	8/12	9/12	10/12	11/12	12/12
1	17	−11.70	+7.27	+ 1.15	−7.27	+7.27	−1.93	−4.52	+7.27
1	19	− 1.71	+8.42	−10.55	+8.42	−3.42	−2.24	+6.57	−8.42
5	13	− 0.27	+1.37	+ 1.28	−1.37	−0.19	+0.36	−1.13	+1.37
5	23	− 1.20	+5.70	− 7.12	+5.70	−2.26	−1.50	+4.52	−5.70
7	11	− 2.07	−1.00	− 0.17	−1.00	+0.41	+0.24	+0.58	+1.00
7	25	− 5.26	+3.38	+ 0.56	−3.38	+3.20	−0.85	−1.97	+3.38
11	7	− 1.47	−0.58	− 0.05	−0.58	+0.26	+0.15	+0.38	+0.58
13	5	− 0.15	+0.53	+ 0.49	−0.53	−0.07	+0.13	−0.41	+0.53
17	1	− 0.68	+0.41	+ 0.05	−0.41	+0.41	−0.11	−0.26	+0.41
19	1	− 0.09	+0.45	− 0.56	+0.45	−0.19	−0.11	+0.36	−0.45
23	5	− 0.52	+1.24	− 1.58	+1.24	−0.53	−0.32	+0.98	−1.24
25	7	− 1.58	+0.96	+ 0.11	−0.96	+0.90	−0.24	−0.55	+0.96
Totals		26.70	28.15	16.30	0.31	5.79	6.42	4.55	0.31

For each coil span the number of turns of the winding is changed to keep a constant fundamental flux density.

Table applies to case of 24 stator slots. 18 rotor slots, 2 poles, 3 phase.

maximum of the fundamental. The significance of this point will be seen later.

In general there will be more than one coil per phase belt in the stator and the coils will not be full pitch, both factors having an effect on the relations found for a single coil. In the case of the example previously used having four coils per phase belt it is found that the vector addition of the harmonic components of all four coils gives phase relations as shown in figure 3. Some harmonics are now found to be reversed and others remain as they were for a single coil. The process of finding which ones reverse is discussed in more detail in appendix *B*.

Proceeding to the case of chorded windings, it is found for the sample motor that the various harmonics again either reverse or hold their positions depending on the coil throw as discussed in appendix *B*. For any given stator winding, therefore, it is possible to draw the relation between the fundamental and any or all harmonics for a particular instant.

Considering the rotor for a moment we see that it is equivalent to a winding with one coil per phase belt and so can be represented by figure 2 for any one component of current.

5. THE TORQUE CYCLE FOR ANY HARMONIC

The next step in the analysis is to place the rotor and stator fundamental waves together with about the same phase relation that they might be expected to have normally with the rotor locked. This would be nearly 180 degrees apart, the actual angle depending upon the power factor of the secondary circuit and the ratio between magnetizing current and total current. For our present purpose the exact angle is not very important and so for simplicity the two fundamental waves will be drawn 180 degrees apart (figure 4*a*). The zero points of the stator wave pass through the center of a phase belt and the zero points of the rotor wave pass through the center of two bars, a pole pitch apart. Under these conditions the 17th harmonic of the stator will bear a fixed relation to the 17th rotor harmonic as shown. An instant later the fundamental waves will have moved slightly in a positive direction while the 17th harmonics will have moved $^1/_{17}$th as far in a negative direction. Thus the picture represents only a particular instant. Since the two 17th waves do not change their positions relative to each other, however, the torque between them, which is zero in this case, is constant.

Suppose now that the rotor be turned through one-quarter of a rotor bar pitch and a new picture be drawn for the same instant in time as before. Figure 4*b* shows this and it will be seen that the two fundamental waves remain in the same relation to one another. This must occur since the secondary current is induced by the primary and the time relation of the current in the bars simply changes to accommodate the new bar position. The 17th stator wave remains as it was but the rotor 17th is shifted relative to the rotor fundamental. The amount of the change can be visualized by going back to figure 4*a* and considering the position of the two rotor waves after the elapse of sufficient time for the fundamental to have moved one-quarter of a rotor bar pitch. This distance is 5 degrees of the fundamental wave or 85 degrees if referred to the harmonic wave length. During this interval the 17th wave will have moved 5 degrees of its own wave length in the opposite direction. The relative movement is thus 90 degrees. A change in rotor position of one-fourth of a bar pitch has therefore resulted in a change in phase relation of the stator and rotor 17th harmonic waves of 90 degrees. In this new position there is a torque exerted between the two fields tending to bring them in line with one another. By continuing the process of shifting the rotor slightly it can be seen that when a shift of one complete bar pitch has been made the relative position of the harmonic fields will have gone through a full cycle and the torque between them will have passed through a set of positive and then negative values.

If the 19th harmonic wave is examined it will be found to have the same properties so that it also produces torque depending on rotor position and goes through a complete cycle as the rotor is moved one rotor tooth pitch. This was one of the requirements of a theory that would agree with the known facts.

We have yet to investigate the effect of other pairs of harmonic fields and it is now proposed to examine the 5th harmonic. The rotor 5th harmonic wave has been shown to be a result of current induced in the rotor bars by the 23d stator harmonic. The relation between the rotor 23d and rotor 5th is shown in figure 5*a*. For the stator it is necessary to select a particular coil throw from which the relation of 23d and 5th can be worked out as shown in appendix *B*. This has been done and is shown in figure 5*b*. Now if the same assumption is made as before and we place the stator and rotor 23d harmonic fields 180 degrees apart, the two 5th harmonic waves fall in the positions shown in figure 5*c*.

The next step is to move the rotor to a new position and see what happens to the two 5th harmonic waves. If we select one-fourth of a rotor slot pitch for the first point as before we reason as follows. The 23d rotor field must bear the same relation to the 23d stator field in all positions of the rotor since it is induced by it. The rotor must therefore be pictured as it would be after the lapse of sufficient time for the 23d harmonic to move one-fourth of a rotor bar pitch which is 5 fundamental electrical degrees or 115 degrees based on the 23d harmonic wave length. But during this period the rotor 5th harmonic would have moved 115 degrees of its own wave length since all waves move equal parts of their wave lengths in a given time. Converting both distances to degrees of the 5th harmonic wave and noting that both waves move in a negative direction the relative movement of the 5th with respect to the 23d and hence with respect to the stator 5th is,

$$115-115\times 5/23=90 \text{ degrees} \tag{4}$$

Here again we find that the 5th harmonic waves behave in the same manner as the 17th and 19th in that displacement of the rotor causes them to move apart and thus produce torque and the torque cycle requires a displacement of one full rotor bar pitch for its completion. In fact all harmonic pairs that exist follow this same rule.

This establishes a series of torque cycles, each arising from a particular harmonic pair and all existing simultaneously and having the same dependence on rotor position over a slot pitch. Two questions then remain to be answered. First, what are the magnitudes of the various torques? Second, what about the phase relations between the several torque cycles?

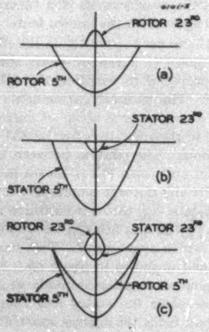

Figure 5

(*a*)—Relation of rotor 5th and rotor 23d

(*b*)—Relation of stator 5th and stator 23d for a particular winding

(*c*)—Relation of stator and rotor 5th-harmonic waves when the rotor 23d is induced by the stator 23d

Note: Waves are not drawn to scale

In answer to the first question, table II has been prepared from calculations on a particular motor by using the torque formula developed in appendix *C*.

The second question, that of phase relation of the torque cycles, is important and is responsible for some of the anomalous results that have been observed. It will be considered in detail in the following section.

6. Relation Between the Various Torque Components

It is evident that there are two possible relations between the rotor and stator waves of a given harmonic when the rotor is in the position shown in figure 4*a*. The two waves are either in phase or 180 degrees out of phase. If they are in phase any movement of the rotor shifts their relative positions in such a way that the resulting torque tends to return the rotor to its original position. This, then, is a stable zero position. On the other hand if the rotor and stator fields are in direct opposition, movement of the rotor shifts the rotor field so as to produce torque assisting the rotor movement. In this case the original position is an unstable zero point.

Since all torque cycles repeat in the same space angle, they either add or subtract and all that remains to be done is to note whether the rotor and stator waves are in phase or out of phase for each harmonic in the original position. As noted above certain harmonics will be found to be one way and others the opposite way for any given winding. Table II has been arranged to show these relations for a specific case. The important fact brought out by this table is the way in which the coil span affects the final results. Thus for a given number of rotor and stator slots and phases there is a wide variation in the locking torque depending on the coil span used. This variation is partly due to the change in magnitude of certain harmonics but the more important cause is the cancellation of forces as a consequence of their phase positions.

Test Results

In order to confirm the theory developed here a two-pole motor having 24 stator slots and 18 rotor slots was wound successively with four sets of coils, each having a different coil span. The number of turns and the operating voltage were adjusted so that the fundamental flux density was the same in all cases. A test of the locked torque variation was made for each winding with two different rotors.

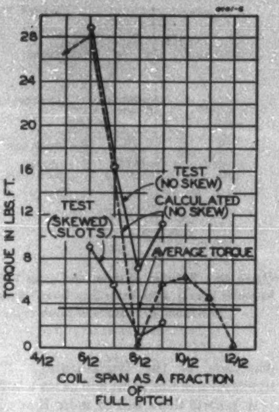

Figure 6. Amplitude of locked-rotor torque variation as affected by coil span. (The total spread between maximum and minimum torque is twice the amplitude shown here)

Curves apply to three-phase motors with 24 stator and 18 rotor slots per pair of poles

One rotor had no skew while the other was skewed a full rotor-slot pitch. The readings were taken at half rated voltage to avoid abnormal heating and consequent changes of resistance.

A sample set of test readings is plotted in figure 1 showing the torque variation as the rotor is moved through a rotor-slot pitch. A minor variation in current occurs also and a curve showing this is included. In figure 6 the amplitude of the torque variation for each of the tests has been replotted to show the effect of a change of coil span. A line showing the average value of the locked torque is included to emphasize the enormous variation that occurs with certain coil spans.

The calculated curve which is shown was worked out in accordance with appendix *C* and applies to the case of the straight-slot rotor. The agreement between test and calculated values, while not as close at all points as might be possible had more rigorous methods been used, seems to indicate that the analysis is essentially correct.

A number of such calculated curves have been worked out for other slot ratios and checked against available data from test files. In most cases fairly satisfactory agreement has been found. The effect of skew has not been calculated in detail but approximate methods of taking it into account can be devised.

In the interest of accuracy it should be recorded that some of the tests, particularly those with no skew, showed an additional variation having a cycle of one-fourth of a rotor-slot pitch. No attempt has been made to account for this component of the torque variation but it is probable that accurate plots of the air gap flux would reveal the cause. Figure 8 shows this type of variation.

The calculated curve of figure 6 is applicable to any motor having the same number of slots per pair of poles as those used for this example provided the torque scale is adjusted to suit the particular values of current as set forth in appendix *C*. For all other three-phase slot combinations which have been investigated so far the general shape of the curve is similar, having minimum points at full pitch and two-thirds pitch.

It may be argued that once the causes of dead points are understood there should be little need to calculate their effects accurately since most designers will simply avoid slot numbers that produce them. The fact remains that other considerations may sometimes determine the slot numbers and thus a means of finding the best coil span is useful.

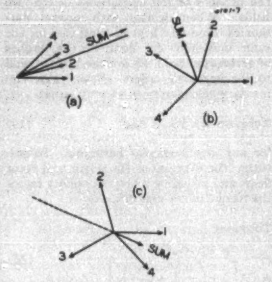

Figure 7

(*a*)—Summation of four fundamental vectors

(*b*)—Summation of four fifth-harmonic vectors

(*c*)—Summation of four seventh-harmonic vectors

Rules to Avoid Dead Points

Since there must be stator and rotor harmonics of the same order traveling in the same direction to produce dead points, trouble can be avoided in the case of integral-slot windings by choosing a number of rotor slots which is not a multiple of the number of stator phase belts. For fractional-slot windings of the balanced type the rule is as follows: Avoid a number of rotor slots which is a multiple of the number of phase belts divided by *Y*, where *Y* is the denominator of the fraction, reduced to its lowest terms, expressing the number of stator slots per pole per phase.

If, for any reason, it is desirable to use one of these vulnerable slot ratios a coil span should be used that will minimize the dead-point effect.

A skewed rotor will always decrease the dead-point effect.

Previous Work

After the present study had been completed it was found that the idea of positive and negative signs for the torque components was not original but had been developed independently by Dreyfus[1] in Sweden. It was used later by Richter[2] in Germany and included in his "Elektrische Maschinen." Unfortunately there appears to be no English translation of either Dreyfus' paper or Richter's books and so their results have not had the appreciation in this country that they deserve.

A German investigator, Möller,[3] has taken a series of tests with various slot combinations. His tests showed strong locking with most of the combinations having a slot difference which is a multiple of the phase belts but he also got a weak locking torque in cases where the difference was a multiple of half the phase belts. The latter he attributes to a possible second harmonic of the slot pulsation. The coil span of the stator coils is not stated and no indication is given that its effect was considered important.

Gray[4] discusses the question of dead points and attributes the cause to permeance variations entirely. He shows a test on one motor in which most of the torque variation has a space distribution corresponding to a fifth harmonic of the rotor slot pitch. His motor has five stator slots for each four rotor slots but the numbers of poles, phases, and total slots are not given.

A discussion by Cotton[5] points to permeance variations as the principal cause and states that a common factor for slot numbers is to be avoided. Say and Pink[6] state that locking at standstill occurs when the stator and rotor slots are the same in number or if one is a multiple of the other.

The most recent reference to the subject before the AIEE occurred during the discussion of E.E. Dreese's[7] 1930 paper on synchronous running torques.

Appendix A. Magnitude of the Harmonic Fields

1. Stator

The magnitude of the stator harmonic fields which arise from the distribution of the winding can be calculated with fair accuracy by assuming that the slot current is concentrated at the center line of each slot and that the permeance is uniform. With these assumptions,

$$B_{Sn} = \frac{k_n}{nk_1} \tag{5}$$

where

B_{Sn} = maximum value of *n*th harmonic field in terms of the fundamental field.

k_1 and k_n = product of pitch and distribution factors for fundamental and *n*th harmonic.

2. Rotor Fields

The magnitudes of the *m*th harmonics, or incidental fields of the rotor bear a fixed relation to the *n*th harmonic fields. The well-known relationship between the harmonic components of a rectangular wave is that the magnitude of each component is proportional to the reciprocal of its harmonic order. For two fields of orders *n* and *m* resulting from one set of currents the relation between them is,

$$B_{Rm} = \frac{nB_{Rn}}{m} \tag{6}$$

where B_{Rm} and B_{Rn} are the magnitudes of the rotor *m*th and *n*th fields respectively. The problem then is first to find the value of B_{Rn}. One simple, approximate solution that suggests itself is to assume the rotor

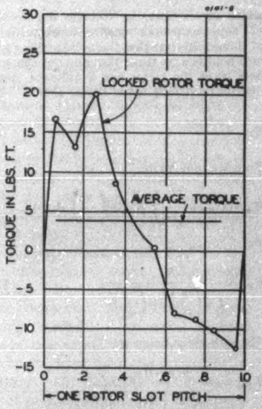

Figure 8. Typical test curve showing variation of locked-rotor torque with rotor position for motor without skewed slots

Motor had 24 stator slots, 18 rotor slots, 2 poles, 3 phases, 7/12 coil span ampere turns equal the stator ampere turns. For the fundamental field or for some of the lower order harmonics this might be done without serious error, but when applied to cases where n is high and m is low it is soon apparent that this simple relationship is far from the truth. The reason for this is that the incidental mth harmonic fields produce flux linkages with the rotor bars which, in the case of a high ratio of n to m, become the determining factors in limiting the rotor current. A more nearly accurate method of finding the value of the rotor fields, though still an approximation, is to equate the rotor bar voltage set up by the stator nth field to the sum of the voltages due to the rotor nth and mth fields.

Thus,

$$E_{Sn}=E_{Rn}+E_{Rm1}+E_{Rm2}+\ldots \tag{7}$$

where,

E_{Sn} = rotor bar voltage induced by stator nth harmonic.

E_{Rn} = rotor bar voltage induced by rotor nth harmonic.

E_{Rm1} = rotor bar voltage induced by rotor m_1th harmonic.

E_{Rm2} = rotor bar voltage induced by rotor m_2th harmonic.

If we use B with its proper subscript to represent the maximum flux density of the various fields then, using per-unit notation,

$$E_{Sn}=B_{Sn} \tag{8}$$

$$E_{Rn}=B_{Rn} \tag{9}$$

$$E_{Rm1}=\frac{nB_{Rm1}}{m} \tag{10}$$

$$E_{Rm2}=\frac{nB_{Rm2}}{m} \tag{11}$$

which states that the voltages due to the nth fields are proportional to the magnitudes of those fields but that the voltage due to an mth field is n/m times the value of the mth field since the latter has a different space distribution. We noted before that:

$$B_{Rm}=\frac{nB_{Rn}}{m} \tag{6}$$

Thus we may write,

$$B_{Sn}=B_{Rn}+\left(\frac{n}{m_1}\right)^2B_{Rn}+\left(\frac{n}{m_2}\right)^2B_{Rn}+\ldots \tag{12}$$

and,

$$B_{Rn}=\frac{B_{Sn}}{1+\left(\frac{n}{m_1}\right)^2+\left(\frac{n}{m_2}\right)^2} \tag{13}$$

The mth rotor fields are:

$$B_{Rm1}=\frac{n/m_1\ B_{Sn}}{1+\left(\frac{n}{m_1}\right)^2+\left(\frac{n}{m_2}\right)^2} \tag{14}$$

and,

$$B_{Rm2}=\frac{n/m_2B_{Sn}}{1+\left(\frac{n}{m_1}\right)^2+\left(\frac{n}{m_2}\right)^2} \tag{15}$$

Expressed in terms of the fundamental stator field density, and the pitch and distribution factors, the rotor mth fields are,

$$B_{Rm1}=\frac{Kn}{m_1\ {}_1\left[1+\left(\frac{n}{m_1}\right)^2+\left(\frac{n}{m_2}\right)^2\right]} \tag{16}$$

$$B_{Rm2}=\frac{Kn}{m_2k_1\left[1+\left(\frac{n}{m_1}\right)^2+\left(\frac{n}{m_2}\right)^2\right]} \tag{17}$$

Appendix B. Phase Relation Between the Harmonic Fields

If the fundamental fields due to each of the coils of a phase belt are added together the vector relations are shown in figure 7a. The vector representing the resultant field for the group lies at the center of the group of individual vectors.

A similar vector addition for the fifth harmonic fields of the phase group is shown in figure 7b. The angle between vectors is now five times the angle between fundamental vectors. Here again the resultant is located midway between the second and third vectors.

Figure 7c shows the addition of vectors for the seventh harmonic fields but this time the resultant vector is negative and therefore lies 180 degrees from the dotted line representing the center of the group.

Continuing with the other harmonic fields it is found that some have a positive resultant located along the center of the group while with others the resultant is negative. Thus with respect to the resultant fundamental field certain of the harmonic fields maintain the same phase relation that existed for a single coil while for others the effect of grouping a number of coils together in a phase belt is to reverse the phase relation of fundamental and harmonic. The term phase relation is used here to indicate whether at the point of positive maximum of the fundamental the given harmonic has a positive or a negative value. To find whether a given harmonic will reverse its position for a group of coils as compared to a single full-pitch coil it is only necessary to solve the expression for the sum of a group of vectors separated by a uniform angle and note the algebraic sign. If Σ is the resultant vector,

$$\Sigma=\frac{\sin Nn\frac{\alpha}{2}}{\sin n\frac{\alpha}{2}} \tag{18}$$

where N is the number of vectors and $n\alpha$ is the angle between them. When Σ is positive the harmonic under consideration keeps the same relation to the fundamental that it has when a single coil is examined. If the sign is negative the harmonic reverses.

By a similar process the effect of chording the winding may be investigated. In this case the top and bottom layers of the winding are considered as two groups of coils displaced in phase by the angle by which the coil span differs from full pitch. The expression for Σ is solved for the case of two vectors differing by n times this angle and the algebraic signs determine whether or not the nth harmonic is reversed due to chording.

These relationships can be kept clear by using positive and negative signs for the distribution or breadth factor and for the chord or pitch factor. Thus the factor Kn in appendix A which is the product of these two factors will be either positive or negative. The torque formula in appendix C will also give either positive or negative results depending upon the algebraic signs of Kn and Km. Table II shows the torque components with their algebraic signs for a typical motor with various coil spans.

Appendix C. The Torque Formula

The torque developed between stator and rotor fields of the same harmonic order varies with the phase relation between them, reaching a maximum at 90 degrees. The maximum torque is proportional to the product of the magnitudes of the two fields. When dealing with several harmonics it must be kept in mind that for the same maximum flux densities the torques of various pairs of fields are also proportional to the harmonic orders. From these elementary considerations we may write:

$$\text{Torque}\propto m\times B_{Rm}\times B_{Sm} \tag{19}$$

for any one particular harmonic. Substituting the expressions for stator and rotor fields and including a proportionality factor the torque in per unit terms is,

$$\text{Torque}=\frac{K_nK_m\ I_L^2}{mK_1^2\left[1+\left(\frac{n}{m_1}\right)^2+\left(\frac{n}{m_2}\right)^2\right]I_1I_m\sin\theta} \tag{20}$$

where,

I_L = locked current

I_1 = rated current

I_m = magnetizing current

θ = phase angle between I_1 and I_m

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A Decade of Progress in the Use of Electronic Tubes

Part I—In the Field of Communication*

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THE dependency of the art of communication on the science of electronics is so great as to make a review of progress in electronics almost of necessity a review of the field of communications itself. While it is true that the early forms of telephone and radio communication advanced to a degree without the use of electronic devices as we know them today, the recognition of the vacuum tube as an amplifier and generator of high-frequency alternating currents in the years just preceding the first World War marked a turning point in the development of the communication art. From that day to this the progress of electronics and communications has gone hand in hand. The need of the communications engineer for new electronic tools has kept him continually urging the electronics engineer to improve old devices and to originate new ones, and each time the efforts of the latter have been rewarded with success, the fruits of his work have been immediately applied to produce new and more startling miracles of long-distance communication.

Because of the close relationship of electronics and communications it is necessary in reviewing the progress of the last decade to keep in mind that it is progress in electronics and not in communications which is our theme. It will be necessary to survey the trends in communications during the period under review, but then it will be necessary to ask to what extent the progress which has been made is due to advances in the electronic field and what advances in the electronic devices themselves have laid the foundation of this progress. There has been no attempt made to make this review comprehensive in the sense that it include all items of progress which are of individual interest. To do so would make it merely a catalog of these many advances and an index to the periodical literature of the subject. Rather the object has been to trace the most significant trends of development in the various fields and to emphasize those lines of advance which appear to be most closely related to the general direction of progress in the several fields of electrical communication.

The Trend in Radio—to Ultrashort Waves

The history of radio has seen a continual demand for expansion of the usable region of the frequency spectrum. Because the number of available communication channels is limited it has been necessary to ration the frequency spectrum by governmental regulation and international agreement among the various users and classes of service. Existing services, most of which are experiencing a continual expansion, have completely absorbed the allocations in the intermediate and short-wave band and leave nowhere to place new services but in the ultrashort-wave region above 30 megacycles. Not only are the new services forced to the ultrahigh frequencies by congestion at the longer waves, but in some cases they are themselves of such a nature that an adequate number of channels with the necessary band width can be obtained only at the high frequencies. Television and wide-band frequency modulation are cases in point. A band width of over four megacycles is required to transmit a modern high definition television image. Frequency-modulation systems at present proposed require a 150-kilocycle band. Radio relay systems for these same services will require band widths at least as great and can obviously be accommodated only in the ultrashort-wave region. The development of these and other wide-band services is therefore dependent on satisfactory means for working at ultrahigh frequencies.

Another reason for the current interest in ultrashort waves is that new services are developing making use of their unique transmission properties. Their very shortness makes practical the construction of directive antenna systems and this can be more easily accomplished the shorter the waves. Directivity, which of course can be applied only in the case of point-to-point services, greatly reduces the power requirements on the transmitter. For broadcast services the problem of interference between stations is simplified because the transmission of ultrashort waves is approximately limited to line-of-sight paths. To relate their propagation exactly to line of sight as has been frequently done in recent years is an oversimplification of the problem but it is probably true that the ultrashort waves will find few applications for ranges beyond 100 miles and that interference need not be feared beyond a few hundred miles at most.

In the very front of this field, in the range of centimeter waves, that is above 300 megacycles, the directive possibilities of ultrashort-wave systems become particularly marked. Commercial applications in this region are as yet few, but may be expected to increase rapidly in the future. As examples of the unique type of problem capable of solution only by the use of centimeter-wave technique may be cited the development of the absolute altimeter to measure the height of an airplane over the terrain by the reflection of radio waves[6] and a 40-centimeter airplane blind-landing system.[7]

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1. For all numbered references, see list at end of paper.

The Trend in Television—to Electronic Television

Television is an art which has developed almost entirely within the last 15 years. In no field is the close relationship between communications and electronics better illustrated than here. All modern systems of television depend on the modulation of a carrier frequency wave by a video signal. This video signal is derived by scanning the picture in a series of horizontal lines, the amplitude of the video signal varying with the brightness of the individual picture element traversed by the scanning beam. The transmission of the modulated carrier is a radio problem in which the principal difficulties to be overcome are those related to the wide band required for a high-definition picture and to the high frequencies assigned for television transmission. At the receiver, demodulation and a second scanning process synchronized with the first are required to reconstruct the picture. The earlier systems of television used mechanical scanning methods at both transmitter and receiver. These mechanical scanning methods had numerous disadvantages. They called for rapidly rotating mechanical elements, perforated disks, lens disks, drums bearing mirrors, and numerous other devices of a mechano-optical nature. It is inherently more difficult to synchronize two rapidly rotating mechanical systems than two electrical circuits on account of the inertia of the mechanical parts. It was hard to get sufficient optical efficiency either for satisfactory studio technique at the transmitter or a sufficiently bright image at the receiver. All these difficulties were magnified when the progress of the art called for the production of a high-definition picture, the mechanical problems because a higher scanning speed was required, the optical difficulties because the light available was proportional to the area of the scanning spot and high definition meant a smaller spot. A solution was sought in electronic television. It was found first at the receiver, for the cathode-ray tube which had been used for some time as a laboratory tool had all the elements of an electronic scanning device for a television receiver and needed only to be adapted and improved for the purpose. For the transmitter, however, completely new devices had to be invented and they came in the form of the Iconoscope, the image dissector, and later modifications of these. Today electronic methods of scanning have replaced mechanical methods practically universally at the transmitter although it is as yet too early to say that some place may not be retained for mechanical systems, for example in the scanning of film. At the receiver electronic methods are also most generally used, the Scophony system[61] being the outstanding example of a mechanical scanning method still successfully holding a place for itself.

The Trend in Telephony—to Broad-Band Carrier Systems

Electronic devices have played a major part in the field of long-distance telephone transmission for many years. In local circuits the attenuation is small enough so that no amplification between the transmitter and receiver is necessary to counteract it, but in all long-distance circuits attenuations are so great that without the vacuum-tube amplifier, or telephone repeater as it is called, communication would be impossible. The earlier systems were voice-frequency systems in which one pair of conductors was used to carry each conversation. The advantages of carrier systems in which one pair of conductors could carry several frequency channels each modulated with its own voice current were early recognized. Such systems generally carrying three speech channels have been in use for 20 years or more. The total frequency band transmitted in these earlier systems was however limited to about 24 kilocycles. Within the last decade tremendous impetus has been given to the development of broad-band carrier systems where the frequency band transmitted may be as high as several thousand kilocycles.

There are two general types of broad band carrier systems.[8–10] There are those in which, by the addition of repeaters and terminal equipment, the carrying capacity of existing conductors both cable and open wire can be greatly increased.[11–14] These systems in general will accommodate from 12 to 16 channels per pair and utilize carrier frequencies up to 145 kilocycles. They are extremely important from an economic standpoint since they permit great economy in the amount of plant required to accommodate an increase in traffic when existing facilities are fully utilized. Then there are the very broad-band coaxial-conductor systems.[15,16] The circuit in this case consists of two concentric conductors separated by small dielectric spacers. Such systems are capable of transmitting exceedingly high frequencies with a maximum degree of shielding from outside interference. Their importance lies not only in their ability to transmit as many as several hundred telephone conversations simultaneously, but also in the fact that they present the only practical means at present in prospect for setting up wire transmission systems capable of transmitting high definition images in television broadcasting networks.[17] The detailed discussion of broad-band carrier systems is outside the scope of this paper. What does concern us here is the problem of amplification which must be accomplished by electronic means. In the cable carrier system attenuations run as high as 4 decibels per mile, in the coaxial system 8 decibels at two megacycles. If we envision 1,000 miles of coaxial circuit the attenuation which has to be compensated by amplification amounts to 8,000 decibels. This represents a total power amplification between terminals of 10^{800}. The requirements on the amplifiers of such a system are obviously very stringent. They must have a high degree of stability so that the power level at the terminals shall be constant, they must be linear so that the intermodulation products which would produce interference with one voice channel by another shall be negligible, and they must be capable of transmitting a wide band of frequencies. Such requirements can be met only by the stabilized-feed-back amplifier[74] which is an electronic circuit development of the highest order of importance. Without this solution of the amplifier problem broad-band carrier systems as we know them today would be impractical.

While considering carrier telephone systems we should pause to note one of the losses of electronics as a contender for supremacy in the telephone transmission field. Copper-oxide modulators and demodulators have in general proved superior from the standpoint of compactness, economy, and ease of maintenance to the vacuum-tube circuits formerly used for this purpose.[18]

Progress in Electronic Devices

So far consideration has been given to the trend of progress in the several major divisions of the communications field and it has been shown how electronics has played an important role in contributing to this progress. The remainder of the paper will be devoted to a review of progress more specifically in the electronic devices themselves. In considering these it will be convenient to divide the devices into several broad classes: those depending primarily on thermionic emission for their operation, those depending on secondary and photoelectric emission and the new science of electron optics, and those based on the phenomena of gas discharges. Finally a few of the most fundamental electronic circuit developments will be described.

Thermionic Devices

The extension of radio into the ultrahigh-frequency portion of the spectrum has been paralleled by a corresponding growth of interest in the development of vacuum-tube devices for use at these frequencies. Those which have found most general use up to the present time are modifications of the familiar three-element or negative-grid vacuum tube designed particularly to fill the requirements of ultrahigh-frequency operation.[19-21] Major development efforts have proceeded in two directions; toward the generation of relatively small amounts of radio-frequency power at higher and higher frequencies, attempting to extend the useful portion of the radio spectrum into the region of the centimeter waves, and toward an increase in the usable range of power at more moderate frequencies in the lower parts of the ultrashort-wave region where television and frequency modulation have brought a commercial demand for radio transmitters. In the former class the frequency limit has been pushed up to 1,800 megacycles; in the latter water-cooled power amplifiers capable of power outputs of approximately 10 kw at 100 megacycles are now commercially available.

The tendency of negative-grid tubes to produce unwanted oscillations due to the feedback of power from the output to the input circuits makes the problem of producing amplifiers at the ultrahigh frequencies more difficult than that of producing satisfactory oscillators. Methods of neutralization which can be used at lower frequencies to prevent these oscillations from occurring become inadequate at the higher frequencies because of the inherent difficulties of circuit technique. The problem has been solved by the construction of special push-pull pentodes containing two tube structures within one envelope.[22,23] With such tubes, stable amplifiers with power outputs of 10 watts at 250 megacycles and some measurable amplification as high as 500 megacycles have been constructed.

Negative-grid tubes are used both as oscillators and amplifiers. Two other devices, both by their nature incapable of amplification but interesting as oscillators because their power-generating capacity exceeds that of negative-grid tubes in the centimeter-wave region, have received considerable attention.[24-26] These are the positive-grid or Barkhausen oscillator and the magnetron. Of these the latter has received the greater attention. Magnetrons depend for their operation on the curvature of the path of the electrons as they move under the influence of the electrostatic field between a filament and a surrounding cylindrical anode placed in a homogeneous magnetic field along the axis. Two general modes of oscillation exist. In the first the static negative-resistance characteristic of the tube is utilized. Operating in this mode magnetrons have been used to produce power outputs of as high as 450 watts at 46 centimeters.[27,28] Efficiencies in the range from 30 to 60 per cent have been reported. While this is considerably better than has been obtained with the negative-grid triode, there are difficulties of frequency stability and modulation with magnetrons, and these together with the additional complication of the equipment required to supply the magnetic field make them rather poor competitors of negative-grid tubes at these frequencies. The second type of magnetron oscillation is one in which the transit time of the electrons is approximately equal to the period of the radio-frequency output.[29] Operating in this manner the magnetron has established the record for ultrahigh-frequency wave generation at 61,000 megacycles corresponding to a wave length of only 4.9 millimeters.[30]

For the conventional negative-grid triode the maximum power-handling capacity has been shown to vary inversely as the square of the maximum frequency at which the tube is designed to operate[19] so that some new principle must be called into play if significantly greater power outputs are to be realized in the centimeter-wave region. Very recently tubes operating on such fundamentally new principles have occupied a great deal of attention by research workers and show promise of giving performance considerably better than that yielded by negative-grid tubes both with respect to power output in the centimeter-wave region and to their maximum frequency of operation. They differ in their principles from conventional tubes in that the radio-frequency output is taken from the electron stream by induction in an anode element which is not the same as that which collects the current and which therefore does not need to dissipate the d-c power. The variations in electron density capable of inducing these radio-frequency currents are produced in two different ways. In the inductive output tube[31] the conduction current is modulated by a cathode grid structure exactly as in the ordinary triode. In the "velocity modulation" tube[32] or in the so-called "klystron"[33] the principle of velocity variation is used. A stream of electrons accelerated from a cathode passes through two grids between which a radio-frequency signal is applied. This signal retards or accelerates the electrons according to the phase of the cycle at which they happen to pass the grid giving rise to velocity variation of the beam which then passes through a field-free "drift space" in which the faster electrons catch up to the slower ones. A bunching or conduction current variation of the beam is thus produced and renders it capable of generating radio-frequency currents in the output electrodes by induction. Tubes of this design overcome two limitations inherent in the negative-grid type. Higher efficiencies may be attained because the output electrode is not called upon to dissipate the tube losses and in the case of velocity-variation tubes transit time through the radio-frequency input section of the device is much less than in triodes since the electrons are moving with high velocity when they enter this space instead of having to accelerate from zero velocity. This greatly reduces "active grid loss", that is, the increase of grid driving power required as the operating frequency is increased which is directly due to the transit time becoming comparable in magnitude with the radio-frequency period and which is the fundamental limiting factor in the high-frequency operation of negative grid tubes. In contrast to the magnetron, velocity-variation tubes are well suited to operation as radio-frequency amplifiers. Experimental data on performance reported in the literature are meager as yet but operation at frequencies as high as 6,000 megacycles (five centimeters) and oscillator power outputs of 500 watts at 750 megacycles have been reported.[1]

Space will not permit the mention of many detailed improvements in the general field of thermionic vacuum tubes, but some reference should be made to the application of electron optics to thermionic devices in the beam power tube.[38] In this tube the electron stream, which is confined to move in beams by accurately lined up grids and by electrostatic deflecting plates, produces a space-charge potential minimum in the neighborhood of the plate which suppresses secondary emission just as the retarding field of the suppressor grid does in the ordinary pentode. Such tubes have received wide acceptance as power amplifiers on account of the high efficiency obtainable with them.

Wide-band amplifiers of the type required in television receivers and in broad-band carrier systems require tubes with a high ratio of transconductance to interelectrode capacitance. Recently developed tubes for these systems have shown considerable advances in this respect[39,40] and in view of the increasing importance of broad band systems, further advances can be anticipated along this line.

Devices Based on Secondary Emission, Photoelectric Emission, and Electron Optics

The devices which we will consider in this section are largely a group whose development has been brought about by the needs of the new art of electronic television. They have been made possible by the great advances made in the underlying science of electron optics, and by putting to work the phenomenon of secondary emission. The knowledge and application of the laws of electron optics have progressed to the point where beams of electrons can be focused and directed in accord with well-defined and well-understood laws. Electron images can be projected in space by electron lenses which consist of electric fields set up by suitably disposed electrodes at the proper electrical potentials or by magnetic fields from the appropriate current-carrying coils.

The basic phenomena of secondary emission have been known for many years. Their effect in modifying the characteristics of vacuum tubes was also well known, the third grid in the ordinary pentode being introduced specifically for the purpose of suppressing secondary electron emission from the plate. It was not however until recently that it was demonstrated that since one electron impinging on a suitable surface with appropriate energy has the ability to release more than one secondary electron, it is possible to build electronic amplifying devices based on the phenomenon of secondary emission. These devices are called secondary emission multipliers.[41,42,57] In general they consist of a number of successive electrodes whose surfaces have been suitably treated to give them a high secondary emission ratio and which are maintained at sufficient potential difference to give an optimum over-all multiplication to the device. If under these conditions a secondary emission ratio of m prevails, the multiplication in k stages is m^k. Values of secondary emission ratio in present-day multipliers are typically in the neighborhood of four and the number of stages varies up to 16. Total over-all amplifications of several billion have been reported.[43] The limiting output is determined by power dissipation in the last stage and defocusing of the electron beam due to space charge. Multipliers can be associated with various types of input. When applied to a tube with a photoelectric input, multipliers can readily be produced with sensitivities as great as 30 milliamperes per lumen or greater.[44]

One advantage of such multipliers over the combination of a photoelectric cell and a thermionic amplifier is that no coupling resistor is required between the photoelectric-cell output and the first stage of amplification. Thermal noise in this resistor is therefore eliminated, and for this reason the photoelectric multiplier is capable of operating at much lower levels of illumination than the photoelectric cell with thermionic amplification.[41,45] As described below, multipliers can be associated directly with the output of television pickup devices. Finally, secondary-emission multiplication applied to the output of an ordinary thermionic vacuum tube offers attractive possibilities in the field of wide-band amplification since circuit problems could be greatly simplified if an amplifying device were available capable of giving a greatly increased gain per stage. The multiplier can do this and has the further important property that its amplification is essentially independent of frequency up to many megacycles where transit time becomes a limiting factor. One interesting commercial tube in which one stage of secondary electron multiplication is added to an ordinary tetrode, has a transconductance of 14,000 micromhos.[46]

The electron optical problems of multiplier design are worthy of special attention because of the ingenious methods which have been devised for their solution. Earlier multipliers used magnetic focusing to assure that all the electrons originating on one stage were suitably collected on the succeeding one. Present-day multipliers are generally of the electrostatic type, the electrodes being so shaped that the equivalent focusing action is obtained. The problem of determining electron trajectories in ordinary multiplier structures is too complex to admit of ready mathematical solution. Electrolytic tank models for determining electric fields in complicated structures and mechanical models for determining electron trajectories have proved invaluable design tools.[43,44] In the latter method metal spheres rolling on a stretched rubber membrane whose slope simulates the electric fields of the tube elements can be shown to trace out the trajectories with a highly practical degree of accuracy.

In electronic television systems four types of pickup tubes are today in common use. The first is the image dissector.[55–8] In this tube the visual image is focused on a photoelectric surface. The photoelectrons emitted are focused by means of an axial magnetic field in the plane of the scanning aperture. This whole image is then moved back and forth across this stationary scanning aperture, the image thus being "dissected." Since the aperture must be minute for a high-definition television picture, the current is very small and passes immediately into an electrostatic electron multiplier for amplification. The output from the last stage of this multiplier is the video signal.

The second pickup tube is the Iconoscope.[55,59–64] The essential element of this tube is the mosaic plate consisting of minute islands of photoemissive substance backed by a thin insulating layer of mica the reverse side of which is covered with a conducting coating. This layer forms the output electrode of the device and to it the elements of the mosaic are capacitively coupled. The optical image is focused on the mosaic. Photoelectrons are emitted from the elements causing them to assume a potential which is dependent on the intensity of the light falling upon them. The mosaic is scanned by an electron beam from an electron gun. This electron beam impinging on the surface under conditions in which the secondary emission ratio is greater than one recharges the element to a definite equilibrium potential and the variations in the recharging current required constitute the video signal which is transmitted to the output circuit through the capacity of the mosaic.

The Iconoscope has the important advantage over the image dissector that its elements act to store charge during at least a portion of the scanning time and this stored charge is then swept away quickly during the traverse of the scanning beam. This gives it a considerably greater sensitivity than the image dissector and makes it suitable for taking scenes under many conditions of illumination where the image dissector is inadequate. Balanced against this is the presence in the Iconoscope output of a spurious signal or "dark spot" arising from the redistribution of photoelectrons and of secondary electrons from the scanning spot over the whole mosaic surface. This spurious signal must be balanced out by the electrical insertion of a compensating signal from a special electrical network. The image dissector also has an advantage in being a directly coupled device the output of which contains information as to the general level of illumination of the picture. The Iconoscope being capacitively coupled gives information only as to variations in illumination, and its output must therefore be continuously monitored to adjust for changes in background illumination as well as for the dark-spot signal just mentioned. At present the Iconoscope type of camera appears to have most general applicability on account of its greater sensitivity but the simplicity of operation of the image dissector gives it a field of use in special cases, for example in pickup from motion-picture film where adequate levels of illumination are easily obtained.

Sensitive as the Iconoscope is on account of its charge-storing property, still more sensitive pickup devices are urgently required by television techniques. Tests show that the charge storage in the mosaic is not greater than five or ten per cent what would be expected under ideal charge-storage conditions, being low because the field which attracts the photoelectrons from the mosaic is small and because only a fraction of the secondary emission current reaches the anode. The super-emitron[64] or its American counterpart the image Iconoscope[65] increase this low efficiency. In these tubes the photoelectric surface is separated from the mosaic, an electron image of the electrons emanating from the former being focused on the latter by means of an electron lens. This mosaic is then scanned as in the Iconoscope. This arrangement enables two gains to be made. A more efficient photoemissive surface can be produced and the photoelectrons may be drawn away from it under conditions of voltage saturation. An over-all gain in sensitivity of about ten times over the Iconoscope has been achieved in practical tubes of this type. They are used to some extent in commercial television pickups particularly under low outdoor conditions of illumination.

Another interesting pickup tube, the "orthicon," has recently been described.[66] It differs from the Iconoscope in that the mosaic is scanned by a beam of low velocity electrons. The photoelectrons leave the surface under voltage saturation conditions giving the tube an over-all high efficiency and making the light response linear. Since the energy of the scanning electrons is low, secondary emission plays little or no part in the operation of the device and the level of the spurious signal is low. Such devices appear to offer promise but problems of focusing a low-velocity beam are formidable and the definition of pictures produced with such tubes do not at present compare favorably with those obtained with the Iconoscope.

For the picture tube in television receivers the conventional cathode-ray oscilloscope tube needed only to be adapted, although many detailed improvements have been required. Electron guns for producing high-current electron beams of accurate focus have been developed.[68] Great progress has been made in the production and use of fluorescent materials.[69] Much of the current development effort is directed at the production of tubes with a sufficiently bright image to enable optical projection onto a large screen.[70] Tubes for this purpose with electron-beam energies as high as 80 kv are under laboratory investigation.[49]

Gas-Discharge Devices

In communication systems gas-filled tubes have found most general use as rectifiers to provide d-c power from a-c supplies or as relay devices in control and signaling systems. Use in circuits carrying the communication signals themselves has proved impractical because of the noise inherent in the gas discharge itself and various proposals for gas-filled amplifying devices have so far proved impractical on this account.

In radiobroadcast transmitters, mercury-vapor rectifiers have largely replaced rotating machines for providing d-c supplies. Mercury-vapor rectifiers have been found to give efficient service with a minimum of maintenance for all outputs up to 20 kv and 100 amperes.

Thyratrons or gas-filled rectifiers which contain a grid to control the starting of the discharge are also becoming widely used in rectifiers for battery charging in telephone central offices. By utilizing grid control in such tubes, regulated rectifiers can be built which automatically adjust the charging rate of the battery in such a way as to hold its terminal voltage constant as load and line voltages vary.[71,72] This condition makes for long battery life and also eliminates the maintenance required by manually adjusted chargers.

In control and signaling systems one type of gas-filled device, the cold-cathode tube,[73] appears to offer prospect of extensive use. In its simplest form it consists of a gas discharge tube containing a cathode, an anode, and an auxiliary electrode. The passage of a small amount of current of the order of one microampere to this auxiliary electrode will control the starting of the anode current which can then be as large as 10 to 100 milliamperes in existing types. The tube thus functions as an extremely sensitive relay. Dissimilarity of cathode and anode can be used to provide a rectification property. In recent years cold-cathode tubes have been extensively applied in four-party selective-ringing circuits for the selective signaling of telephone subscribers on party lines.

Electronic-Circuit Developments

The importance of the stabilized-feed-back amplifier in wide-band carrier telephone systems has been brought out. The principle of such amplifiers is basically very simple.[74] A portion of the output is fed back to the amplifier input usually through an attenuating network in such a manner as exactly to oppose the input signal. This reduces the over-all gain of the amplifier and also reduces noise and distortion usually in the same proportion. With modern high-amplification-constant vacuum tubes the decrease in gain can be readily restored and the net result is an amplifier which is superlatively better than the simple amplifier without feedback. The first major advantage of such amplifiers is the linearity of their characteristics. In carrier systems this is important since intermodulation which causes interference between the various speech channels can be practically eliminated. To obtain this same result without the use of stabilized feedback would require the use of vacuum tubes of much larger power ratings operated over a small portion of their characteristics with a consequently greatly reduced efficiency. The second advantage of the negative-feed-back amplifier is its stability. When the amount of feedback is large it can be shown that the over-all amplification depends only on the characteristics of the feed-back circuit and is practically independent of the amplifying circuit. Since the feed-back circuit contains only fixed circuit elements and no vacuum tubes the over-all amplification becomes independent of variations in tube characteristics and supply voltages. This stability is essential in a carrier system which has many repeaters in tandem if the total amplification between terminals is to remain constant. The third advantage is the ease with which the over-all gain and the gain frequency characteristic of the amplifier can be controlled. These are functions only of the feedback circuit characteristic which can be given any desired shape by the introduction of appropriately designed equalizing networks. In carrier systems the attenuation-frequency characteristic of the line and the variations in attenuation-frequency characteristic with temperature can thus be almost exactly compensated by adjusting the gain-frequency characteristic of the repeaters.

A great amount of technical interest and commercial activity is currently being devoted to a new form of radiobroadcasting known as wide-band frequency modulation.[75,76] It is as yet too early to say what the ultimate development of this system will be, but its most enthusiastic advocates foresee the obsolescence of the present system operating within the broadcast band in the neighborhood of one megacycle and the development of a new ultrahigh-frequency system operating on the principles of frequency modulation. In conventional radio-telephone systems the amplitude of the carrier wave is modulated by the audio frequency. In the proposed system the frequency of the carrier is caused to vary back and forth over a frequency band approximately 150 kilocycles in width, the instantaneous deviation of the frequency from the mean being proportional to the instantaneous amplitude of the modulating audio signal. This system has several interesting features as compared with the existing low-frequency amplitude-modulation broadcasting. Natural atmospheric noise is inherently low in the ultrahigh-frequency region of the radio spectrum. Interference from automobile ignition systems, on the other hand, is more severe than at broadcast frequencies. The characteristics of the proposed frequency-modulation system are such that the radio-frequency interference-to-signal ratio may be higher without noticeable interference with the program than is the case with amplitude modulation. The same characteristics of the system which make this true are effective in reducing interference between stations on the same frequency. It is possible with frequency modulation to transmit programs of high fidelity without unduly high expenditures of power at the transmitter but this is done at the expense of using a wider frequency channel.

The extension of high-frequency practice to the centimeter wave region has called for new circuit techniques. As the frequency is increased the lumped inductances and capacities of the resonant circuits decrease and the distributed constants of the connecting leads play a continually greater part in the circuit performance. In the ultrahigh-frequency region and particularly at wave lengths less than one meter, it becomes convenient to do away with the lumped circuit constants altogether and to use instead the resonant properties of transmission lines of either the parallel wire or coaxial types.[77] Such circuits are readily adjustable, convenient to work with, and the mathematical theory of their properties is well understood. At still higher frequencies even transmission-line circuits become impractical but at this point the resonant properties of conducting cavities can be exploited to advantage. It was demonstrated about four years ago[79] that a cavity whose boundary is a good conductor is capable of sustained electrical oscillations at certain characteristic frequencies determined by its geometry and that such cavities may be used advantageously as circuit elements associated either with sources or sinks of electromagnetic wave power. In some recent electronic devices, for example the velocity-modulation tubes and klystrons mentioned above, this principle has been invoked. The tube elements themselves have in effect been set directly into the walls of such resonant cavities.[78]

Closely related to the phenomena of resonant cavities is the propagation of electromagnetic waves through hollow metal tubes or wave guides.[79–84] For simple geometrical shapes the theory of this form of electromagnetic wave propagation is well established.[80] Propagation can take place only when the wave length is less than some critical value which is of the same order of magnitude as the diameter of the conductor. The method is therefore limited to the ultrahigh frequencies. Attenuations even at these extremely high frequencies can be made comparable with those of ordinary coaxial cable at the lower frequencies so that at the moment wave guides appear to offer considerable promise in future wide-band ultrahigh-frequency communication systems. The end of a wave guide may be flared out into a highly directive electromagnetic horn which may be used either as a radiator or receiver of ultrahigh-frequency waves. The resulting power gains are comparable with or even greater than those of the more conventional directive antenna arrays now in general use.[85–8] The rapid development of the understanding of the transmission properties of ultrashort waves in wave-guide systems has provided a further impetus for the development of electronic devices capable of generating and amplifying these ultrahigh frequencies.

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A Decade of Progress in the Use of Electronic Tubes

Part II—In Other Than the Field of Communication*

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Synopsis: The past ten years include nearly the whole period during which electronic tubes have been used outside the communication field. The types of tubes used in this field may be classified into groups, each having properties that make it valuable in certain phases of industry or in specific applications. The references are grouped according to nature of application and represent a selected group chosen by careful elimination from several thousand.

IN comparison with the communication field, the use of electronic devices in other industry has played an entirely different role. In communication in general and in radio in particular, tubes and tube developments have been basic to most improvements. They have been truly called the "heart" of nearly all radio apparatus. In other industry, however, they have in general had to supplant other methods and to compete in highly developed fields of electrical engineering.

The use of tubes in the noncommunication field has passed through several broad phases. At first they were looked upon with a certain amount of mistrust as being rather fragile playthings entirely unsuited for the severe conditions encountered in the industrial field. Next, as a result of the increased popular interest in science and the optimism of the late 20s, they became "wonder" devices and their wide use in nearly every electrical field was freely predicted.

Within the past few years, a more realistic viewpoint has prevailed and much solid progress has been made. It is realized that there is no magic in an electron tube, and the tube must either accomplish something that cannot be done by any other means or make its own way against competition solely on the basis of what it can do for less cost than existing methods.

Scope of This Review

The last decade includes the greater part of tube activity in this field, but reference is necessarily made occasionally to earlier work to clarify or to complete the picture.

Electrons are now known to play such an active part in so many devices that, in the interest of brevity, certain devices are not included in this review. These omissions are neon lights as well as other gaseous light sources, ultraviolet tubes, X-ray tubes, pumped mercury-arc tanks, iron-wire ballast tubes, mercury and vacuum switches, and uses in phonographs and talking moving pictures. This review is also limited to American references in the interest of brevity.

Due to the activity in this field and the relatively long period covered, only a small fraction of the available references are included. Those included were chosen on the basis of availability, general nature of treatment of subject, and their use of further references.

The emphasis in this review has been placed mostly on tube applications rather than the tubes themselves. This has been done because of the wider interest in applications and because the review must be kept to a reasonable length. The references to tube design have been included only when they were important from the viewpoint of new applications or unless similar tubes were not used in communication.

Arrangement of References

In general the references are classified under headings similar to those employed by the AIEE in indexing its TRANSACTIONS in past years.

Factors Favoring the Use of Tubes in Industry

In competing with rotating equipment, the use of tubes eliminates noise, vibration, and the need of special foundations.

The ability to amplify from very low input levels both in high-vacuum and gas-content tubes makes them unique electrical devices. Their amplification can also be made continuously variable rather than step-by-step.

Tubes provide cycle-by-cycle and fractional-cycle control.

Tubes in combination with such devices as thermocouples, microphones, photoelectric cells, and movable capacitance plates permit electrical response to temperature, sound, light, and motion. Therefore, they extend control operations to variations in sensory response.

Tubes also find ready application where they extend certain factors to extreme limits. Timing to very short intervals, response to extremely small electrical quantities, and control from very small movements are examples of this.

They operate relatively independent of frequency in comparison with most other electrical devices.

Factors Retarding the Use of Tubes in Industry

The relatively high cost of tubes in combination with the fact that they are renewable items has retarded their application.

Their mode of operation and application has not been so commonly understood as other electrical devices.

Considerable engineering knowledge and effort are often required to work out a new application successfully and in some cases the expense of this apparently cannot be justified from the viewpoint of available business.

A General Summary of Types of Tubes

The following comments are based on tube types and applications outside the communication field.

HIGH-VACUUM DIODES

These tubes are inherently rectifiers only and are suitable for use where high frequencies are involved. Their use is also limited in general to currents of about an ampere. They are suitable, however, for voltages up to the hundreds of thousands.

The early applications of this type of tube were for the purpose of obtaining high voltage (25 to 150 kv) for smoke and dust precipitation as well as high-voltage cable testing by power companies. This

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use has continued and grown to some extent. They also find a wide variety of application, but in limited quantities, in laboratories for high-voltage, low-current, d-c power supplies.

HIGH-VACUUM TUBES WITH CONTROL GRID OR GRIDS

This class of tubes involves one or more control electrodes; their chief functions are as amplifiers and oscillators. They range in ratings from less than a watt to over 100 kw.

There are probably hundreds of thousands of these tubes in daily operation in a bewildering variety of circuits. By far the big majority of them are radio-receiving-tube types used in oscillating and amplifying circuits. In some cases, requirements of power output or a special operating condition has dictated special types. Automatic train control, high-frequency therapeutic oscillators, and low-grid-current amplifier tubes are examples of such special designs. There are many others.

The literature contains hundreds of articles on special applications of this class of tube and thousands of references are made to their use in the literature.

HOT-CATHODE GAS-FILLED DIODES

This class of tube is utilized for rectification of heavier currents at lower frequencies and voltages than the high-vacuum type. Gas-filled tubes have a relatively low and constant voltage drop in comparison with high-vacuum tubes.

There is one field where the great majority of tubes of this type are used and that is for low-voltage battery charging. Most of these are tungsten filament, argon-filled tubes for currents under ten amperes, and for direct voltages under 125.

Another group of rectifier tubes of this class utilizes mercury vapor and is suitable for voltages up to about 20,000. Although the great majority of such tubes are used for d-c power supply of radio transmitters, some are used for high-voltage testing and to supply direct current for high-frequency tube oscillators.

Similar tubes are also employed in rectifiers to supply power at the lower standard voltages—125, 250, 500, and 650—to replace power formerly obtained from a d-c Edison system or private generating plant.

HOT-CATHODE GAS-FILLED GRID-CONTROLLED TUBES—THYRATRONS

The control action of this type differs from the high-vacuum type in that only the start of the discharge is controllable. They are particularly suitable for controlling currents of a few amperes from a-c circuits. Their ability to control by cycles of the a-c supply leads to a number of valuable applications.

One of the earliest commercial applications for this type of tube was for the control of the illumination from incandescent lamps used in theaters and decorative lighting. In this application they functioned as controlled rectifiers to supply a variable direct current to saturation reactors. These latter functioned as variable reactors in series with the groups of lamps.

Within the past few years, applications have fallen largely into certain classes:

(*a*). Rectifiers to supply variable voltage or current from an a-c source of supply. This combination is employed to control d-c motors, field excitation of generators, and various electromagnetic devices.

(*b*). Inverters to supply either constant or variable-frequency a-c power (up to a limit of a few hundred cycles) from a d-c supply. As a variation of this the same general principle may be used for frequency changing.

(*c*). The use of a pair of tubes connected "back-to-back" so that in combination they pass alternating current. This gives a circuit element that not only acts as a contactor but by phase control of the grid voltage permits variation of the current passed. These tubes are so used in some welding control circuits where too large currents are not involved.

During recent years there has been a trend toward the use of inert gases, such as argon, neon, or xenon, to replace mercury vapor. This gives this class of tubes a freedom from ambient temperature effects that are sometimes troublesome with the use of mercury-vapor tubes. They are, however, not usable for peak inverse voltages up in the thousands as in the case of mercury vapor tubes. For small tubes to handle fractions of an ampere, the inert gas content also allows operation as an inverter up to thousands of cycles.

COLD-CATHODE GLOW-DISCHARGE TUBES

These are particularly applicable where filament operating power is not available and where ample control voltage can be employed. These tubes are often equipped with control electrodes. They are characterized by a higher voltage drop than the hot-cathode or pool type of gas-content tube. Also the current-carrying capacities are limited to a fraction of an ampere average current except that currents up to a hundred amperes or more can be carried for very short periods of time.

The two-electrode form has a number of applications that may be classified as follows:

(*a*). As a discharge tube to break down at some abnormally high voltage and thus act as a circuit protective device.

(*b*). As a voltage regulating device to place across a circuit. This use results from the characteristic of such a tube, with proper design, to pass a wide range of currents with a nearly constant voltage.

(*c*). As a negative-resistance element to generate oscillations by the relaxation circuit principle.

(*d*). As a rectifier based on the dissimilarity of the two electrodes.

(*e*). To produce light in accordance with the pulses of current through the tube. The light produced may be extremely intense for a very short time as for stroboscopic work or modulated in accordance with a variable current intensity or frequency.

When one or more control electrodes are added, the characteristics of a relay are gained. The applications listed above are retained and in some cases their scope extended. To date the three-electrode form has largely found its usefulness in the communication field but has wide relay possibilities in industry as it becomes better known and further developed.

PHOTOELECTRIC TUBE

In industry the photoelectric tube is performing many functions not possible by other means. In addition it is becoming an important agent in eliminating routine human effort. In combination with an amplifying tube, it is finding its place in applications where the eye and the hand are needed but not the brain.

During the past ten years, photoelectric-tube applications in great numbers have been made and, due to their apparent news value, widely publicized. Development of the photoelectric tubes themselves has been along the lines of smaller size, greater sensitivity, selective response in the spectrum, and novel mechanical designs for special applications. Lowered cost has also been helpful in extending their use.

Their applications may be grouped into three general classes:

(*a*). Applications in which a light beam, when interrupted or established, actuates a photoelectric relay. Examples of this are sorting, counting, door opening, hole detection, alarm systems, safety devices, and miscellaneous control jobs in industry.

(*b*). Applications where the tube and its accessories are actuated by variation in the amount of light falling upon it. Examples of this are smoke detection, check on turbidity of solutions, photometry and radiation measurement.

(*c*). Applications utilizing the difference in response of photoelectric tubes to light from different parts of the spectrum either directly or by the addition of filters.

Photoelectric tubes may be grouped according to the combination of features they employ. These classifications are:

(*a*). High-vacuum or gas-filled to increase their sensitivity.

(*b*). Nature of photoemissive surface which controls sensitivity and response to different parts of spectrum including the ultraviolet.

(*c*). Design and composition of bulb to transmit or restrict light flux from different parts of the spectrum to the photoemissive surface.

(*d*). Variations in mechanical design to adapt the photoelectric tube to special uses involving size, nature of base and direction, and degree of solid angle from which radiation reaches the photoemissive surface.

Existing photoelectric tubes have various combinations of these features.

Photovoltaic cells and selenium cells have not been considered within the scope of this review.

POOL TUBES—IGNITRONS

The outstanding characteristic of the pool cathode is its ability to pass very heavy currents—thousands of amperes. The addition of the ignitor gives the pool tube control characteristics. By phase control, voltage and current regulation are obtained and, by cycle control, short-time switching functions.

For a considerable number of years, preceding about 1933, the sealed-off pool type of tube had been slowly experiencing a decline in usefulness. Its chief applications were battery charging and high-voltage rectification for series arc lamps for street lighting. These were not growing applications. Then two new developments appeared which changed this picture completely. One was the ignitron and the other was the sealed steel envelope. The first solved the starting problem and gave the pool type the equivalent of grid control. The latter made large tubes and water-cooling practical and the tubes more acceptable to industry.

Since 1933 the use of pool tubes has grown steadily. Most of this growth has been in the welding control field where the tube's ability to carry very heavy peak currents has been an all important factor. The use of the sealed-off steel envelope ignitron for power conversion rectifiers for outputs between 75 and 350 kw has begun and appears to be a promising field.

CATHODE-RAY TUBES

In industry the use of a fine beam of electrons to produce and control a light spot on a fluorescent screen for oscillographic purposes is most common. These tubes can also be used for various forms of indicators.

For many years cathode-ray-tube oscillographs were considered as laboratory devices only but now they are commonly employed by electrical engineers and are necessities for many measurement problems. A better understanding of them, together with better-designed equipment and lowered cost, have been the chief contributions.

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Measurements at Radio Frequencies

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IT IS interesting to recall that this art is less than a century old[1] and that the early experimenters[2] actually worked in the frequency range now called "hyper frequencies."

The same basic principles are used at power frequencies and radio frequencies in the measurement art but the different techniques and nomenclatures often obscure this fact. These differences have arisen because the magnitudes of the fundamental quantities usually are widely separated. As the frequency is increased, the circuit usually changes markedly[3] since elements which are negligible at one frequency often become important in the new range. Size, design, and required accuracy also affect measurement problems.

It is the purpose of this paper to discuss the more common measurement problems at radio frequencies and to give a résumé of the status of this rapidly growing art.

Measurements of Voltage

The primary standards of voltage at radio frequencies are actually the usual d-c standards, and measurements are made by means of transfer instruments. The most popular instrument is the vacuum-tube voltmeter[4] which is available in forms ranging from the simple diode rectifier to a multitube inverse-feedback amplifier. All forms have the following characteristics in common: high input resistance, 0.25 to 10 megohms; low input capacitance, 1.5 to 30 micromicrofarads; response proportional to peak or average value, depending upon design; flat frequency response over the range for which they are designed, and they require a power supply. Because some forms of this instrument are so reliable and versatile, it is really no problem to measure radio-frequency voltages for a wide variety of conditions. The problem of obtaining a voltage measurement without affecting the voltage being measured is more common at radio frequencies than it is at power frequencies. The acorn-type vacuum tube is generally useful when this problem is encountered because of its small size and good electrical characteristics. Even under conditions which require that the small capacitance of this tube be compensated for, good measurements have been obtained. In fact, with proper technique, the acorn diode has given satisfactory operation at 300 megacycles per second. Conditions sometimes require that the diode voltmeter be built into the circuit[5] to avoid the errors which would result from moving even such a small device from one part of the circuit to another.

The vacuum thermocouple[6] is also a useful transfer device at radio frequencies because of the following characteristics: small frequency corrections, one calibrated on direct current has a correction less than one per cent at 100 megacycles per second; response to effective value regardless of wave shape; small size; and it is a passive network, primarily resistive. In addition, there is the cathode-ray tube which is rapidly gaining favor because it gives information as to wave form and frequency as well as magnitude. At present, it is much more expensive and more difficult to use than the other devices.

The radio engineer is often concerned with the microvolt, but none of these instruments is sufficiently sensitive to measure such small voltages so indirect means are employed to effect these measurements. A common way is by comparison with a voltage obtained by attenuating a voltage of the order of one volt by known amounts.[7] Another way to obtain microvolts is to generate them with a microvolt generator or second harmonic generator.[8] Figure 1 shows one form of the microvolt generator, and figure 2 is a schematic diagram of it. It is a full-wave rectifier so connected that the usually unwanted ripple current is used to generate a known voltage across the known impedance, *Z*. It depends upon the fact that if the tube characteristics are square law, the only output will be at twice the frequency of the input voltage and the peak value of this output

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1. For all numbered references, see list at end of paper.

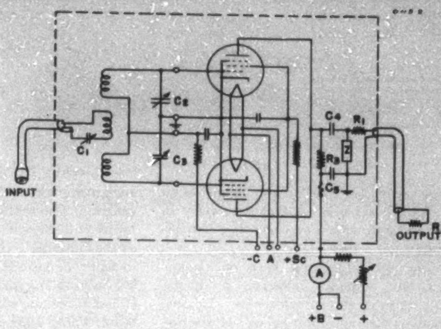

Figure 2. Schematic diagram of microvolt generator

current will be equal to the change in direct current in the plate circuit. When the tube characteristics depart from the square law, higher-order even harmonic currents are generated. It has been found practical to use the sixth harmonic. The essential fact is that no fundamental or odd harmonics are present and the radio-frequency current is indicated by a d-c microammeter.

When special problems arise, such as measuring 0.1 microvolt, which cannot be readily solved by either of the methods described, it is usually wise to refer to the work of Faraday, Maxwell, Hertz, and other pioneers. A mutual-inductance attenuator based on Maxwell's formula[9] and tested at high level was found easy to construct, test, and use to obtain voltages of the order of 0.1 microvolt at 40 megacycles per second.

The problem of measuring high voltages at radio frequencies is usually solved by using some form of multiplier with a moderate-voltage instrument as it is at power frequencies. The problem of designing the multiplier has no satisfactory general solution, but when capacitance dividers cannot be used, a tuned circuit or a quarter-wave section of transmission line may provide the solution.

The problem of inventing a practical standard of voltage at radio frequencies is fascinating and tantalizing. The phenomenon of resonance potential[10] in gases appears to have promise. A tube in which this phenomenon was observed is shown in figure 3. It consists of an anode and cathode sealed into a tube along with a drop of mercury and a coating of zinc sulphide. The anode is like the grid of an ordinary vacuum tube so that many of the electrons will go beyond it to strike mercury atoms. Those electrons having an acceleration of 4.87 volts cause radiation at a wave length of 2,537 angstrom units and this radiation is made visible by the fluorescence of the zinc sulphide. The problem remaining is to get precise correlation between the impressed voltage and the resonance potential. There must be a way to harness the electron.

Figure 1. Microvolt generator with indicator

Measurement of Current

The primary standard of current at radio frequencies is the oscillating-ring type of electrodynamic ammeter,[11] figure 4. Although unconventional in its method of indication, the oscillating-ring type of electrodynamic ammeter is simple. It is essentially a metal ring, free to rotate about a diameter, and immersed in an electromagnetic field produced by the current being measured. The field tends to orient the plane of the ring parallel to the flux lines with a torque proportional to the mean square current and to the small angular displacements of the ring's plane from this torque node. Therefore, if the ring is mechanically displaced and then released, it oscillates about the torque node, until damping brings it to rest, with a frequency of torsional oscillation proportional to the rms current. To obtain the value of a current, then, instead of noting the deflection of a pointer on a scale, the observer determines the mechanical frequency, F_m, of oscillation of the ring.

The actual measurement required for each determination is that of time, a fundamental measurement which can be made with great accuracy. Mathematically, the high-frequency current is given by the equation,

$$I=\left(\frac{2\pi\sqrt{JL}}{m}\right)F_m$$

where J, L, and m, are the ring's moment of inertia, inductance, and coefficient of mutual inductance with the exciter and are calculable from measurements of lengths and mass which need be made but once for the instrument. Since the fundamental measurements of length, mass, and time suffice for the determination of current in this instrument, it is a primary standard. That the required measurements of length, mass, and time can be made, and the necessary computation carried out, with accuracy, has been demonstrated by checking several different designs of vibrating-ring electrodynamic instruments against thermocouple ammeters at frequencies below the thermal error range of the latter.

As to structural details of this type of instrument, the exciter or current-carrying circuit, in whose field the ring is placed, is not restricted to any one form. For example, a single circular loop has been found useful for low currents and a single toroidal turn (figure 4) for large currents. In any case, its form does not require high inductance, nor does its function require an energy-consuming heating effect. Neither is large shunt capacitance, inherent in the temperature equalizing terminals of thermocouples, at all necessary.

The support for the ring may be a simple fiber suspension, or jeweled bearing for greater sturdiness.

The determination of the mechanical frequency of the ring's oscillation may be done quite closely by an observer with a

Figure 3. Critical-voltage tube

stop watch, or with greater precision by a light beam and photoelectric relay in conjunction with a two-pen tape chronograph and a time standard.

The oscillating-ring type of electrodynamic ammeter possesses a unique combination of characteristics. In contrast to the deflection types[12] of electrodynamic instruments, its impedance is independent of current, and it is not subject to loss of precision nor to limitation on maximum current by a cramped scale. As in other electrodynamic ammeters for high frequency, the effect upon its indication, produced by raising the frequency of the current, is calculable and small. In contrast to the effect in thermal ammeters, it decreases with increasing frequency. This follows from the fact that whereas thermal ammeters depend upon effective resistance which continues to increase with frequency, electrodynamic instruments depend upon flux linkages or inductance which approaches a fixed limit as frequency is raised. For example, in a conventional five-ampere thermocouple, the change in indication was calculated to be 1.6 per cent between 0.5 and 5.0 megacycles, and over 40 per cent between 5 and 50 megacycles; whereas in one electrodynamic instrument used, the corresponding figures were 0.77 per cent between 0.5 and 5 megacycles, and only 0.25 per cent between 5 and 50 megacycles.

A secondary standard of current at high frequencies, like one at power frequencies, should be capable of insertion in a circuit without seriously affecting the operation of either the circuit or the instrument. Furthermore, essentially all the radio-frequency current led to the "high" terminal should traverse the actuating element, and no indication should be had from strong surrounding fields. It is also desirable that high-frequency error in the actuating element be minimized.

The design of a low-impedance thermocouple-type ammeter indicates the methods applied to meet these requirements. In this low-impedance design, the actuating element or heater is a thin flat ribbon doubled back on itself with a thin mica separator between its inner surfaces. In order to preserve the advantages of minimum inductance and negligible stray fields inherent in this type of heater, its supporting studs are made short and concentric. The center stud, having its external end flattened and drilled to serve as the ammeter's "high" terminal, visible in figure 5, intrudes through an insulating bushing in a small shallow cup so that it makes contact with one flat end of the heater whose other end, opposite the mica separator, is mounted on the center of the cup's bottom. The cup, shown removed from the ammeter in figure 6, is recessed in the ammeter's back plate which itself serves as the external "low" terminal, seen in figure 5. The shielding effect of this concentric "low" terminal is continued about the miniature d-c instrument by the small contiguous metallic case and scale plate.

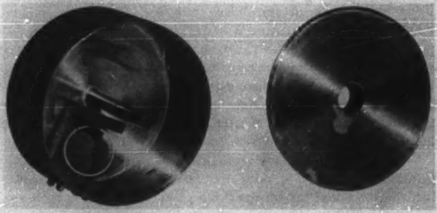

Figure 4. High-frequency electrodynamic ammeter

The effect on performance at high frequencies, which the above features of design have produced in the low-impedance thermocouple-type ammeters, is indicated by the results of comparative tests such as those described below.

For example, two conventional "high-frequency design" thermocouple ammeters when placed in series, read alike at 32 megacycles, one per cent different at 42 megacycles, and three per cent different at 52 megacycles; but these same instruments when individually placed in series with the low-impedance type instrument, with the latter in the "low" side of the circuit, each read the same as the low-impedance ammeter.

A further indication of the low impedance of this type of instrument is given by the fact that when applied as a series resonance indicator in a conventional wavemeter circuit, it allowed extension of the top frequency from about 150 to 360 megacycles at the same time permitting the use of a larger inductance loop. The wavemeter[13] smoothly covered a band from 160 to 360 megacycles, with a capacitor of 25 micromicrofarads maximum capacitance and an inductance comprising slightly more than one turn one inch in diameter in addition to the capacitor leads. The inductance of the instrument is 0.005 microhenry.

The effectiveness of its shielding has been demonstrated in a test in which a number of thermocouple-type ammeters, including both the low-impedance and conventional types, were placed directly in radio-frequency fields. Between 15 and 90 megacycles, the ammeters were placed within one-quarter inch of the tank coil of a 50-watt oscillator, and between 260 and 380 megacycles they were placed in the maximum field of the tank lines of a line-controlled 15-watt oscillator. Although conventional-design ammeters during these exposures, with no current actually applied, gave readings ranging from zero at lower frequencies to more than full scale at higher frequencies, the low-impedance type instruments under the same conditions uniformly maintained a reading of zero.

Thus, when the low-impedance thermocouple-type ammeter is inserted in a circuit, it introduces a minimum of reaction on the circuit, remains unaffected by surrounding fields, and indicates no more nor less than the current entering its "high" terminal. In addition to the demonstration of these characteristics prerequisite in a secondary standard of high-frequency current, further tests have shown that skin-effect error at high frequencies in the doubled-ribbon heater has been greatly reduced below that found in the solid-wire type of heater.

The concepts of Faraday are particularly helpful in problems of measuring current at high frequencies, because when high-frequency currents of any size flow, the fact that the energy is in the fields becomes apparent. For example, men standing six to eight feet away from a ten-kilowatt oscillator operating at 75 megacycles have felt their ankles get warm. This approach to a current-measuring problem leads to correct application of available instruments because the way high-frequency currents divide in a particular circuit becomes reasonable when the fields are considered. When a spacer is thought of as a dielectric medium rather than as an insulator, the "mysterious" loss of current in a circuit becomes an expected occurrence.

Measurements of Frequency

A primary standard of frequency is an oscillator whose rate is substantially constant when determined by counting the number of oscillations and dividing it by the time taken to produce them. The unit of frequency is, therefore, based on the standard second which is 1/86,400 of the mean solar day or 1/86,164.100 of the sidereal day. The National Bureau of Standards maintains several primary standards of frequency and makes some of them available for general use by means of standard frequency transmissions over station WWV, Beltsville, Md. Many laboratories maintain primary standards of frequency by using the WWV standard frequency transmissions and the United States Naval Observatory time signal[14] transmissions to check them. The counting of radio-frequency oscillations may be accomplished by means of a combination of electronic counter circuits and a mechanical counter. The multivibrator[15] or relaxation oscillator is commonly used for the electronic counter and a synchronous-motor clock for the mechanical counter. Rates constant to one part in ten million are commonly obtained by means of quartz-crystal-controlled oscillators operating at 100,000 cycles per second.

In many cases, a signal from the oscillator or transmitter whose frequency is to be determined can be brought to the primary standard and the measurement made by direct comparison. When this is not practical and an accuracy of 0.1 per cent is acceptable, the common absorption-type wavemeter consisting of an inductor and a capacitor connected in series can be used. The operation of this device depends upon the following relation between frequency, inductance, and capacitance:

$$f = \frac{1}{2\pi\sqrt{LC}}$$

When greater accuracy is required, some form of the heterodyne wavemeter may be used. The heterodyne wavemeter is a calibrated oscillator which can be tuned to the frequency of the unknown signal with great accuracy by one of the beat note methods. When a quartz-crystal-controlled oscillator is used to check the calibration of the heterodyne oscillator, an accuracy of 0.001 per cent can be obtained.

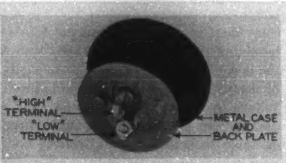

Figure 5. Low-impedance thermocouple ammeter (rear)

The problem of measuring higher and higher frequencies stays with us as the art progresses. In most cases, the first need is a suitable receiver for the unknown signal and the problem remaining is to set a high-frequency oscillator accurately on a harmonic of the standard. For example, adjust a 1,000-kc crystal to the tenth harmonic of the 100,000-cycles-per-second standard, then set a 10,000-kc crystal oscillator on the tenth harmonic of the 1,000-kc oscillator. This process can be carried as far as necessary to provide guide posts in the upper frequency spectra.

Since frequency can be measured so accurately, it is highly desirable to be able to use it as the indicator when measuring other quantities. For example, an excellent way to measure small changes of capacitance[16] is to put the capacitor in an oscillating circuit so that these changes cause changes in frequency which can be monitored continuously and accurately.

Measurements of Power Factor and Dielectric Constant of Materials

It is as important to know the power factor and dielectric constant of an insulating material used in a radio transmitter as it is to know the power factor of a load on a power line. The power factor of a material is the cosine of the angle whose cotangent is the ratio of conductance to susceptance in the sample when it is used as the main dielectric in a two-electrode capacitor. This is based on representing the sample by a pure resistance in parallel with a pure capacitance. It may also be represented by a resistance in series with a capacitance and then the power factor is the cosine of the angle whose cotangent is the ratio of resistance to reactance.

Power-factor standards can be made of disks of pure, clear fused quartz because the power factor is 0.0002 from 1 to 3,000 megacycles per second. The bases of this statement are a determination at 3,000 megacycles by W. C. Hahn, engineering general department, the work of Von Lothar Rohde[17] at 200 and 500 megacycles, and the work of this laboratory.

There are many ways[18] of measuring the power factor of a material at frequencies less than one megacycle per second. Some of the circuits available are: parallel bridge, series bridge, resistance variation, substitution, and reactance variation.

At frequencies above one megacycle, the reactance-variation[19] type of circuit has been popular. This popularity probably resulted from the fact that power factor is determined by measuring capacitances, changes of capacitance, and ratios of voltages. Variable capacitance of known value is easier to obtain than variable resistance at frequencies above one megacycle. The procedure is to measure the width of the resonance curve for a certain ratio of voltage at resonance to voltage off resonance with the sample capacitor tuning the resonant circuit and repeat this with an air capacitor in place of the sample. These data are sufficient for two separate circuit-resistance determinations and, therefore, for the effect of the sample on the circuit. Since the measurement of power factor is a resistance measurement, the following factors in addition to those in the measuring equipment are important: the size and shape of the sample; the size, shape, and material of the electrodes; and the method of applying the electrodes to the sample. The size and shape of the sample are major determining factors of the capacitance. If this capacitance is made small compared to the inherent capacitance of the measuring circuit, it will obviously become difficult to determine the effect of inserting the sample. On the other hand, if it is made large, its reactance will approach that of the residual inductance in the measuring equipment and will cause resonance effects and consequent inconsistent operation. The size and shape of

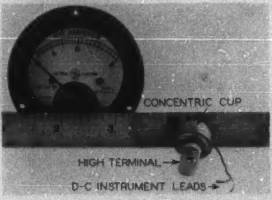

Figure 6. Low-impedance thermocouple ammeter (front)

the electrodes also affect the capacitance, but the effective resistance of the electrodes is often the more important factor.

Because there are so many possible sources of error, it is good to have a clear fused-quartz standard sample to test the over-all operation of any power-factor measuring equipment.

The dielectric constant of a material is the ratio of the capacitance of a pair of electrodes with the dielectric between them to their capacitance for the same spacing in a vacuum. It is necessary to choose the electrode geometry properly[20] to allow accurate determination of the dielectric constant when the sample size is fixed. The capacitance of the sample is usually a by-product of the power-factor measurement procedure.

Measurements of Impedance

One way to determine impedance is to measure the current through and the voltage across the unknown impedance and calculate the ratio of E to I. Even at power frequencies, it is not convenient to use this method and at radio frequencies it is seldom convenient to use. One reason for this is that one-inch leads are the same part of a wave length at 63.36 megacycles per second as one-mile leads are at 1,000 cycles per second. It is hard to overemphasize the need for short leads from the measuring equipment to the unknown impedance.

In addition to requiring short leads, the problems which have arisen frequently since the advent of television have required that the measuring equipment be portable where portable means that the user can carry it up an antenna tower and use it at the top. An instrument for this service in the 40 to 70 megacycles per second frequency range is shown in figure 7 and a schematic diagram is shown in figure 8. It will be noticed that provision has been made to obtain short leads. At the same time, the measuring circuit was shielded from the oscillator and from external fields by placing it in a separate metal compartment, by providing Faraday screens between the oscillator coil and one coupling coil and between the other coupling coil and the measuring circuit coil, and by using a common ground.

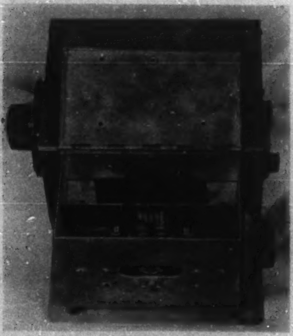

Figure 7. Portable impedance measuring instrument

The measuring circuit is a coil and a capacitor so arranged that either balanced or single-ended impedances can be measured by the reactance-variation method. The parallel tuned circuit has been so arranged that the unknown can be connected in series with the coil and consequently low values of impedance can be measured. The usual parallel connection of the unknown is also provided for high values of the unknown. The procedure is to determine the circuit constants with and without the unknown connected in terms of capacitance, change of capacitance, and voltage ratios. The differences between the two resulting sets of susceptance and conductance values are the constants of the unknown impedance.

This instrument was used in the installation of the transmission lines and antenna of General Electric television station W2XB in band 3 (66 to 72 megacycles).

It is often necessary to measure the inherent inductance in low-value resistors. One way to measure these small inductances is to use the microvolt generator.

The procedure is to insert a resistor, R_1, as Z and measure the current, i_1, which causes a certain output at frequency, f_1; replace R_1 by a different resistor, R_2, and find the current which causes the same output; repeat this procedure at another frequency, f_2, and solve the resulting simultaneous equations:

$$i_1\sqrt{R_1^2+(2\pi f_1L_1)^2}=i_2\sqrt{R_2^2+(2\pi f_1L_2)^2}$$

$$i_1^1\sqrt{R_1^2+(2\pi f_2L_1)^2}=i_2^1\sqrt{R_2^2+(2\pi f_2L_2)^2}$$

In a particular case, a two-ohm and a ten-ohm resistor were used at 25 and at 40 megacycles. The inductance of the two-ohm resistor was 0.016×10^{-6} henry. The impedance of the two-ohm resistor is, therefore, 3.20 ohms at 25 megacycles and the necessity for determining inherent inductance in low-value resistors is evident.

Measurements of Transmission Line Characteristics

In television transmitting systems as in other radio transmitting systems, it is usually impractical to locate the transmitter unit adjacent to the antenna. A transmission line is employed to connect these parts, and for the wide-frequency-band operation needed in television communication, the transmission line must have quite definite characteristics.

Physically such a transmission line usually consists of straight sections of concentric tubes with spaced low-loss beads for insulation. Sections are joined at corners by means of junction boxes and the inner conductor is supported in long vertical runs by means of an anchor joint. To secure proper performance of the transmission line, it is necessary to compensate for the electrical disturbances produced by these junction boxes and anchor joints. It is also necessary to match the antenna circuit to the transmission line much more closely than was commonly practiced in communication or broadcast transmitter systems. For these reasons, it is desirable to measure the characteristic impedance of the line and to determine its frequency characteristic with compensating elements added at the required positions.

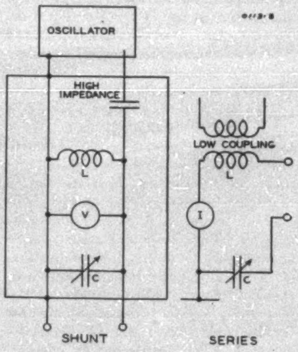

Figure 8. Schematic diagram of measuring instrument

The reactance-variation type impedance-measuring instrument already described can be used to measure the characteristic impedance of the line, but it is not convenient to use this instrument for determining the frequency characteristic of the line. This may be done more easily by measuring the ratio of output to input voltage over the operating range of frequency. In practice, it has been found desirable to hold the input voltage constant which makes the output voltmeter indication proportional to the ratio wanted.

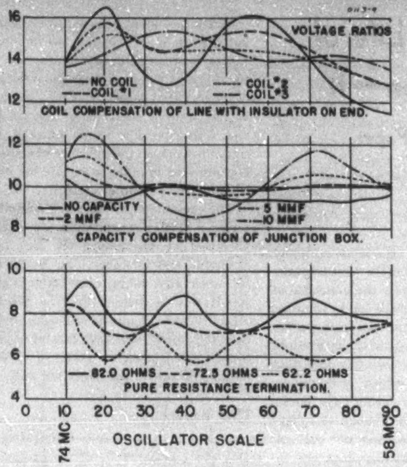

Figure 9. Experimental voltage-ratio curves

It is necessary that the ratio of output to input voltage remain practically constant over the operating-frequency range and desirable that it remain so over a wider range to give a safety factor. The information given in plots of voltage ratio versus frequency is shown in figure 9. The lowest set of curves shows that resistance terminations can be readily identified as lower or higher than characteristic impedance of the line. The middle set shows how changes of capacity at a junction box caused marked and easily recognized changes in the ratio curves also. The top set demonstrates the case of coil compensation.

When this method of measuring transmission-line characteristics is used, it is desirable to start with a short section and to add junction boxes, other sections, and anchor joints one at a time. If several things are added at once, the voltage-ratio curves become difficult to interpret; but if one thing is added at a time, the curves are as easy to interpret as those shown. A mathematical treatment of these relations is in the literature.[21]

That complete transmission characteristics over wide frequency ranges can be secured by properly using a pair of diode voltmeters is encouraging.

Conclusions

How basic principles are used in making measurements at radio frequencies has been shown in a number of cases.

Something of the range of the subject has been suggested by the diode voltmeter and the "critical voltage" tube.

A primary standard of current at high frequencies has been described.

A new low-impedance thermocouple ammeter has been presented.

Clear fused quartz has been proposed as a standard of power factor in the 1 to 3,000 megacycle-per-second frequency range.

A typical technique of making measurements at radio frequencies has been disclosed by the description of the "reactance-variation" method of measuring impedance.

It is hoped that the material presented will prove informative to engineers not directly connected with radio-frequency measurement work as well as to those in it.

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The Dielectric Strength and Life of Impregnated-Paper Insulation—II

The Influence of the Thickness of the Paper

J. B. WHITEHEAD
FELLOW AIEE

THE peculiarly severe conditions imposed by manufacture, transportation, installation, and operation of high-voltage cables have emphasized the importance that full attention be paid to all physical properties of the insulation, especially those relating to permanence of mechanical structure and of high insulating properties. The requirement of wide flexibility is at the root of the present method of applying the paper insulation in spiral tape form in successive layers.

Mechanical flexibility and high dielectric strength do not always go hand in hand. They do so to an astonishing degree in the case of impregnated paper. However, with the available materials it is not always possible to retain at the same time the maximum of flexibility and the maximum of dielectric strength and stability. Consequently in cable design, in order to meet the special requirements of particular cable installations, it is necessary to make such selection of the paper to be used as will ensure the optimum combination of flexibility and dielectric strength. Carefully prepared wood-pulp paper with low content of electrolytic impurity is now used almost exclusively for the type of insulation in question. It is available over fairly wide ranges of density and thickness, as determined largely by experience in manufacture and usage over the past 20 years or more. It is within these ranges of paper structure that the cable engineer finds the possibility of selecting papers for a very wide range of type of cable and conditions of service. The fact that impregnated-paper cables are giving highly satisfactory service for large blocks of power in the highest range of values of voltage is an indication not only of the remarkable insulating properties of this type of insulation, but also of the resourcefulness and persistence of cable engineers in experimental study and manufacturing skill.

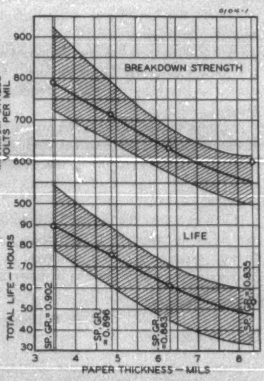

Figure 1. Breakdown strength and life as function of paper thickness

In a related paper,[1] the present author has reported the results of a study of the influence of a variation of the density of the paper on dielectric strength as determined in a fixed program of accelerated voltage-time tests. The results of the study were at some variance with commonly accepted ideas[2] as to the influence of paper density on dielectric strength. They confirm the suggestion that has been made from time to time[3] that proper consideration must be given to the laminated form of construction, to the open spaces between successive tapes, and to the different values of dielectric constant of impregnated paper and of oil. In view of the positive character of the results and of their possible bearing on cable design, the work on the influence of density is continuing in order to explore the possible influence of other variables.

The present paper describes a similar series of studies on the influence of the thickness of the paper on dielectric strength and stability, as indicated by accelerated voltage-time tests. Here too it will be found that the results are of definite character and again indicate the importance that careful attention be given to the variations in the internal structure of the insulation wall.

The Papers

High-grade cellulose paper for cable manufacture is available in the range of thickness 0.0035 to 0.008 inch (3.5 to 8 mils, 0.0089 to 0.02025 centimeter). For the present studies four thicknesses of approximately 0.0035, 0.0048, 0.0063, and 0.00835 inch were used. The papers were supplied by a well-known manufacturer in the form of tapes one inch wide. It was intended that the density of the paper structure be the same for each thickness. As measured, however, the density of the 8-mil paper fell slightly below the others (see table I). We refer to the papers briefly as having nominal thicknesses 3, 5, 6, and 8 mils respectively.

The Test Specimens

The test specimens were made by wrapping the paper tapes on smooth brass tubes with $33^1/_3$ per cent and $66^2/_3$ per cent overlay with a butt spacing of the tapes of approximately $^1/_{64}$ inch (0.00397 centimeter). The number of layers of each thickness of paper was so chosen as to give approximately the same over-all thickness of all test specimens. (See table I.)

Nine identical specimens were constructed from each thickness of paper. They were dried and impregnated in groups of three. All specimens were impregnated with a light oil as used for oil-filled cables by American manufacturers.

Further details as to the measuring and guard electrodes, reinforced ends, etc., of the test specimens, of the physical characteristics of the oil, and of the methods of drying and impregnation will be found in earlier papers.[4]

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This research, carried out in the laboratories of the School of Engineering of The Johns Hopkins University is a project of the Engineering Foundation with the sponsorship of the AIEE. Grateful acknowledgment is made of the support and advice frequently furnished by both these bodies, and particularly by members of the advisory committee appointed by the AIEE to follow the course of the work. Cordial thanks are also extended to the Union Mills Paper Company, The Sun Oil Company, the Locke Insulator Corporation, and to the Insulated Power Cable Engineers Association for material and other forms of assistance furnished. Special thanks are due Research Assistant C. O. Newman, to whose skill and devotion much of the success of the work is due.

[1] **For all numbered references, see list at end of paper.**

Experimental Method

All tests were made on single specimens immersed in the impregnating oil in a bath enclosed in a thermally insulated box with automatic temperature control.[5] The air pressure in the test box was that of the atmosphere. The temperature for all tests was 40 degrees centigrade.

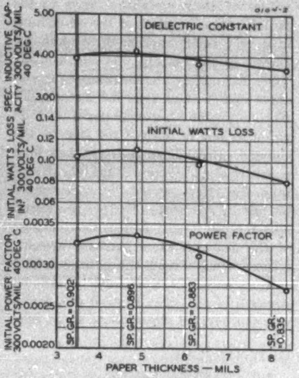

Figure 2. Dielectric constant, dielectric loss, and power factor as function of paper thickness

Life and Breakdown Tests. The accelerated voltage-time tests started at a voltage to give an average stress of 400 volts per mil on the specimen and the test voltage was increased by 3.12 per cent every four hours, thus giving a geometric increase such as to bring the stress to 700 volts per mil within three days. Under this program the range of life of all specimens tested was from two to four days.

Power Factor and Capacitance. Power factor and capacitance were measured over the range 100 to 400 volts per mil on a high-voltage Schering bridge[6] at the beginning of the voltage-time test and at 12-hour intervals.

Conductance. The long-time d-c conductance of the dry paper specimen was measured at 1,500 volts and 105 degrees centigrade before impregnation, and of the impregnated specimen at 60 degrees centigrade immediately following impregnation.

Experimental Results

The results of the accelerated voltage-time and breakdown tests on the three-mil paper are given in table I. They are typical of the results on the other thicknesses of paper, for which the average values only are given in the table. Using the average values of breakdown stress, as given in table I, the results on breakdown strength and accelerated voltage-time and other electrical properties are plotted in figures 1 and 2. The shaded areas represent the extreme limits of the spread of the test values. The average spread is substantially lower as seen in table I.

The results indicate that with increasing thickness of the paper, there are marked variations in all the electrical quantities, as well as the values of breakdown stress and life.

Breakdown Strength and Life. Both these quantities decrease with increasing thickness of paper, over the range studied. (See figure 1.) This similarity of behavior is a necessary consequence of the type of accelerated voltage-time test. Since the life run begins with a stress of 400 volts per mil, it happens that the percentage change in over-all duration of time (life to breakdown) is greater than that of voltage. The decrease in the average values of breakdown stress is from 788 to 602 volts per mil. This indicates a decrease in breakdown strength of something more than 25 per cent as between the three-mil and the eight-mil papers. This decrease would have been about 28 per cent if the specific gravity of the eight-mil paper had had the same value as that of the other papers. Discussion of these results and a probable explanation will be found in a later paragraph.

Dielectric Constant. Over the range of thickness studied there is a small decrease in the value of dielectric constant (see figure 2). This is apparently due to the slight decrease in density with increasing paper thickness. Corrected[1] for this variation, the value of the dielectric constant is very closely the same for all thicknesses.

Dielectric Loss and Power Factor. Both these quantities show a noticeable decrease in value with increasing paper thickness. Of the total change in power factor approximately 40 per cent may be attributed to the change in paper density.[1] The fraction of the total change

Table I. Accelerated Voltage-Time Tests—Influence of Paper Thickness

Specimen	Number Layers	Paper Thickness	Density J.H.U.	Oil:Paper Ratio by Weight	Breakdown (Volts Per Mil)	Hours at Maximum	Total Hours	Power Factor at 300 Volts Per Mil	Dielectric Loss (W/in.³ at 300 Volts Per Mil)	Capacitance (μμf, at 300 Volts Per Mil)
34-*A*	23	0.00353			770	3.8	87.8	0.00318	0.1010	608
34-*B*	23	0.00351			770	2.6	86.6	0.00312	0.979	602
34-*C*	23	0.00344			820	3.0	95.0	0.00318	0.1015	624
35-*A*	23	0.00342			722	2.2	78.2	0.00332	0.1030	611
35-*B*	23	0.00345			745	3.1	83.1	0.00338	0.1069	616
35-*C*	23	0.00344			745	2.7	82.7	0.00334	0.1072	627
36-*A*	23	0.00350			795	1.5	89.5	0.00331	0.1068	620
36-*B*	23	0.00337			928	1.2	109.2	0.00327	0.1057	644
36-*C*	23	0.00338			795	2.6	90.6	0.00322	0.1031	636
Average		0.00345	0.902	38.8	788	2.5	89.2	0.00326	0.1038	621
Maximum deviation					17.8%					*K*=3.94
Average deviation					5.28%					
Specimen 22-24										
Average	(16)	0.00487	0.896	40.2	727	1.48	78.0	0.00336	0.1102	651
Maximum deviation	(16)				17.6%					*K*=4.11
Average deviation	(16)				5.64%					
Specimen 37–39										
Average	(12)	0.00631	0.883	42.1	633	2.3	61.3	0.00312	0.0969	634
Maximum deviation	(12)				7.26%					*K*=3.82
Average deviation	(12)				3.95%					
Specimen 40–42										
Average	(10)	0.00835	0.835	49.1	602	1.6	53.6	0.00272	0.0802	550
Maximum deviation	(10)				20.0%					*K*=3.69
Average deviation	(10)				7.35%					

due to this cause in the case of the loss is approximately 30 per cent. Apparently, therefore, some factor other than density is playing a part in these decreases.

Oil Content. The oil content of the impregnated paper tape alone expressed as the ratio by weight of oil to paper is given in table I. It will be seen that the volume of oil absorbed increased approximately uniformly with increasing thickness of paper from the value 38.8 per cent for the three-mil, to 49.1 per cent for the eight-mil paper. This variation also is accounted for by the variation in paper density.

Type of Breakdown. All specimens were carefully examined after failure as to evidence of its beginning and cause. In the latter half of the work these studies were greatly facilitated by the use of a thyratron tube, relay, and rapid circuit breaker[6] for interruption of the primary circuit of the transformer. The operation of this device is so sensitive and rapid that not only is the usual burning attendant upon breakdown very greatly reduced, but in many cases the circuit is opened before the failure has extended further than two or three layers of paper. Further details of the operation of this equipment are given in the paper already referred to on the influence of the density of the paper on breakdown and life.[1]

Failures took place almost invariably under the central electrode of the test specimen and all failures have been of definitely radial type.

A clear indication of the foregoing studies on density, and of the present studies, is that most of the failures begin in the oil channels between successive turns of paper, most often at the central high-voltage electrode, sometimes in the center of the insulation wall, and in a few cases at the outer electrode. In a number of cases in which the specimen was dissected after the first two or three shots, the incipient failure was found to be confined to two or three layers. At one point the oil in a channel would be found to have become black, the blackening extending a short distance either way along the channel and along the edge of the paper. The next stage would be the puncture of one or perhaps two paper layers, but generally reaching the next oil channel on the same radius. Occasionally longitudinal tracking and tree patterns to adjacent channels were noticed, but these extended only a very short distance, probably because of the relatively thin total wall thickness. Evidence of initial blackening in oil spaces was also invariably noticed at the edges of the small holes in the central tubular conductor and outer lead-foil electrode drilled to facilitate drying and impregnation. However, the failure showed no preference for the edges of these holes. Invariably the central tubular conductor would be blackened under the full spiral length of the oil channel between the tape edges of the first layer, the remainder of the conductor remaining clean and bright.

Altogether the failures seem to be closely similar to those reported by Robinson[7] and Race,[8] both of whom find that the initial carbonization occurs next an electrode. However, several of our specimens have shown evidence that the initial seat of the trouble may be in the center of the insulation wall.

The interrupted failures and behavior described above are best observed in the thinner range of papers (see particularly results of the study of the influence of paper density[1]). In the present tests as the thickness increases, it is more and more difficult to limit the failure to a portion of the wall. Most of the failures above those in the three-mil paper have been through-failures with more pronounced burning, with less tendency to tree patterns and tracking and consequently with greater obscurity of the conditions surrounding initial failure.

Discussion of Results

Breakdown Strength and Life

The most probable explanation of the decrease of breakdown stress and life with increasing thickness of paper is the well-known fact that the breakdown strength of thin oil films decreases with increasing thickness. Our results indicate that the failures always start in the oil films between successive turns of paper tape. J. Sorge[9] reports a decrease of dielectric strength with increasing gap thickness, between approximately parallel planes, for xylol, benzene, and hexane, over a range of thickness 0.2 to 0.7 millimeter (8 to 28 mils). This is just outside our range of oil-channel thicknesses (3.5 to 8.0 mils). However, Sorge's curves are all pointing sharply upward at the lower values of gap. Other authors[10] report similar behavior.

Approximate computations[1] of the values of stress in the oil channels in our results in the increasing order of paper thickness are 1,506, 1,416, 1,170, 1,075 volts effective per mil. Naeher's[11] values for pure liquids and thicker layers mentioned above are from 1,700 to 1,200 volts per mil. His results on transformer oil and for gaps from 0.5 to 3 millimeters show a decrease of from 560 to 400 volts per mil. It will be seen, therefore, that our values are intermediate between those for the pure liquids of Sorge and those of Naeher for transformer oil, which was probably not so highly purified as the oil of our tests. In explanation of the above behavior, I suggest the presence of mobile ions in the oil which are greater in number with increasing thickness of oil channel. These ions, owing to the high values of their mobilities, pass to the radially opposite faces of the oil channel in a very small fraction of one-half the period of alternating 60-cycle voltage. They accumulate at the opposite faces as space charges and thus increase the potential gradient between paper and oil on either one or both of the faces of the channel. Breakdown begins at these high points of electric stress in the oil. Naturally the effect would be accentuated by minute filaments of paper projecting into the oil. With increasing thickness of the channel, more ions are present, greater values of space charge accumulate, and consequently a critical breakdown strength of the oil is reached at a lower value of over-all stress.

Dielectric Loss and Power Factor

These two quantities apparently decrease with increasing thickness of the paper, the former by about 20 per cent and the latter by about 15 per cent. The explanation is not entirely obvious. Only a portion of these decreases is accounted for by the variation in paper density. With increasing paper thickness, there are obviously fewer oil layers between paper tapes. The oil channels, however, apparently do not enter into the matter of loss in any considerable extent; first because the oil channels have the same total thickness for all thicknesses of paper; moreover, the volume of oil is a relatively small fraction of the total volume, and the loss per unit volume in oil is generally substantially less than that in the impregnated paper.

The losses in impregnated paper, qualitatively speaking, should vary in accordance with the laws for a Maxwell layer dielectric. This affords a very much better explanation of the results found. There are fewer oil layers with the thicker tapes and the barrier action to current flow is obviously greater in the thicker tapes. Thus the number of surface layers of charge at the surfaces of separation, the amount of dielectric absorption, and consequently the dielectric loss under alternating stress, are all lowered with increasing thickness of paper.

Summary and Conclusions

1. Accelerated voltage-time tests have been carried out on samples of impregnated-paper insulation with the thickness of the paper tape as a variable. Four thicknesses of nominal values, 3, 5, 6.5, and 8 mils, and all of approximately the same density, were studied. All specimens were impregnated with a very fluid insulating oil.

2. In accelerated voltage-time tests beginning at 400 volts per mil and with geometric increase of voltage at the rate of 3.12 per cent every four hours, the 60-cycle breakdown voltage was found to decrease with increasing paper thickness. Over the range of thickness mentioned, the decrease in breakdown voltage with increase of thickness was about 28 per cent. The decrease in life was approximately 45 per cent.

3 The failures almost invariably start in the oil channels between successive convolutions of the paper tape.

4. The maximum stress in the oil channels at breakdown, as computed from the dielectric constant of the oil, and the measured dielectric constant of the impregnated paper, decreased with increasing thickness over the range studied from 1,506 to 1,075 volts effective per mil. An explanation of this behavior is offered.

5. The dielectric constant of the impregnated paper is approximately constant over the range of thickness studied.

6. Power factor and dielectric loss decrease with increasing paper thickness, the former by about 30 per cent and the latter by about 20 per cent over the range of thickness studied. This behavior is explained qualitatively by the variation in interfacial polarization in accordance with Maxwell's theory.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 Supplement to Electrical Engineering—Transactions Section.

Computation of Accuracy of Current Transformers

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WHILE the general principles of the operation of current transformers have been known for some time, and tests have provided much experimental data, the introduction of high-speed relaying, especially differential protection, required a greater knowledge of current-transformer performance than was necessary previously. It is the purpose of this paper to present a method of calculating the accuracy of current transformers.

Formulas for calculating leakage flux and leakage reactance in power transformers have been available for several years. Some good experimental work showing the effect of leakage on the flux densities of the core of current transformers was done by H. W. Price and C. K. Duff of the University of Toronto, and much study has been devoted to the magnetic properties of iron and other material, but little seems to have been done in applying the formulas of power transformers to current transformers, or in establishing the rules which govern the effect of leakage flux on the accuracy. Most of the information concerning current transformers has been derived by the experimental method of making ratio correction factor and phase-angle tests of models.

However, within recent years, a considerable amount of work and testing has been done at the West Lynn Works of the General Electric Company to obtain reliable information on the exciting currents of various magnetic materials over a wide range of flux density. This information made it possible to check methods of calculating the accuracy. The method of calculation here presented was developed largely on a theoretical basis from the fundamental principles involved making use in so far as possible of the well-established power-transformer formulas and was found to give results in very good agreement with the tested accuracy.

By using the method presented in this paper it becomes possible to calculate current-transformer performance from available experimental data without making and testing models. This makes it possible to design current transformers for specific application, or determine the suitability of an existing design without the cost of overcurrent ratio and phase-angle test. It is also very useful as a tool to the designer, because it enables him to compare several proposed designs, and select the best at less cost than by using experimental models and making tests.

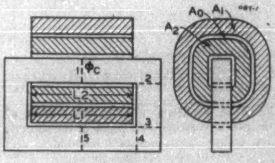

Figure 1

A_1—Area of end of primary winding as shown*

A_0—Area of space between windings as shown*

A_2—Area of end of secondary winding as shown*

L_1—Primary winding layer length*

L_2—Secondary winding layer length*

N_2—Secondary turns

$\mathbf{Z}_B$—Impedance of external burden $= R_B + jX_B$

R_B—Resistance of external burden in ohms

X_B—Reactance of external burden in ohms

R_2—Resistance of secondary winding

$\mathbf{V}_2$—Secondary terminal voltage

$\mathbf{E}_2$—Secondary induced voltage (that is, voltage induced by mutual flux and leakage flux)

$\mathbf{E}_{L2}$—Voltage induced in secondary by leakage fluxes

$\mathbf{I}_2$—Secondary current in amperes

$\mathbf{\Phi}_M$—"Mutual flux" $\mathbf{\Phi}_M$ will be considered to be entirely within the core (that is, it completes its circuit without entering or leaving the core)

$\mathbf{\Phi}_{L2}$—Secondary leakage flux (that is, flux passing through secondary copper)

$\mathbf{\Phi}_{L0}$—Leakage flux between windings (that is, flux passing between primary and secondary)

$\mathbf{\Phi}_{L1}$—Primary leakage flux (that is, flux passing through primary copper)

$\mathbf{\Phi}_C$—The flux in the core at the center of the secondary coil (section 1 shown in figure 1)

$j = \sqrt{-1}$

$K = 2\pi f N_2 (10^{-8})$ where f = frequency

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* All dimensions in inches.

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In order to compute the accuracy of a current transformer it is first necessary to define the term "accuracy". This can best be done by defining the inverse of accuracy, that is, the error. Fundamentally, the error of a current transformer is the vector difference between the primary and secondary ampere turns. Now this difference is, by virtue of the relative position of the primary and secondary coils, the number of ampere turns acting on the magnetic circuit of the transformer and is, therefore, the exciting current of the transformer in ampere turns. Hence, the rather intangible expression "accuracy" upon analysis becomes simply the inverse of exciting current.

It is, therefore, the first purpose of the following analysis to determine the exciting current. Only a wound-type transformer with two coils, one inside the other, will be considered in detail, but the general principles for the most part apply to any type.

In the development of the formulas the following assumptions will be made:

1. The secondary coil is inside the primary, which is usually the case.

2. The leakage flux flows parallel to the axis of the coil.

3. Flux density is zero on the outside of the primary copper and inside of the secondary copper, and increases linearly to the edges of the space between the copper of the two coils, the density on the primary side of the space being $3.2N_2I_2/L_1$ lines, secondary side $3.2N_2I_2/L_2$ making a mean density in the space of $3.2(2N_2I_2)/(L_1+L_2)$ all values being expressed in English units* (see figure 1 for symbols). Although it is obvious that the assumption regarding the conformation of the magnetic field is not exactly true, these assumptions are conventionally made in calculating reactance of transformers and have been shown to give values in good agreement with test.

4. In the computation of exciting current the ampere turns required for the core joints will not be considered separately. This procedure is usually permissible, if, as in the computations here presented, the core data used are derived from tests on a core having approximately the same number of joints per inch as the core of the transformer under consideration and if the joints are symmetrically disposed on the core. In some cases where the joints all happen to be in regions of flux density much higher than that in the rest of the core it may be necessary to consider the joints separately if the densities are high. This can be done by determining the density at the joints from the equations hereafter presented and adding the necessary ampere turns for the joints to the exciting ampere turns required for the rest of the core. Except on cores having short magnetic circuits or cores made of very high permeability material good lapped joints will not take a very large per cent of the total exciting current required for the core at low flux densities but usually will at high densities.

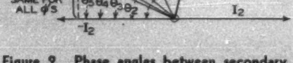

Figure 2. Phase angles between secondary current and fluxes in different sections of core

5. Much the same statement holds with respect to grain direction for cores in which the flux path is in different directions relative to the grain of the iron in different sections of the core, as the exciting current is usually higher when the flux flows across the grain than when it flows parallel to the grain.

6. The exciting currents will be treated as vectors in adding the exciting current for the various sections of the core, although it is not strictly correct to represent them as such since they are not in general sinusoidal. Experience, however, indicates that the errors introduced by assuming them so are negligible except possibly at quite high flux densities where the wave form of the exciting current is badly distorted.

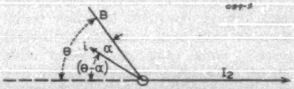

Figure 3. Vector diagram showing relation between flux density, exciting ampere turns, and secondary current for any particular core section

7. The magnitude of the leakage flux and its conformation are assumed to be dependent only upon the geometry of the apparatus, and the ampere turns in the coils, and are independent of the permeability of the magnetic core material. This assumption, of course, is valid only if the permeability of the core material is high relative to that of the air or other nonmagnetic media. Should the permeability of the core become quite low as the result of either a high overcurrent or a heavy burden which badly saturates the core, the configuration of the leakage flux may be altered, and its magnitude will no longer be strictly proportional to the ampere turns in the coils. This assumption limits the application of this method of analysis to conditions where the maximum magnetizing force in the core is about 300 ampere turns per inch or less.

8. It is also assumed in the analysis that with a sinusoidal primary current the secondary current will be a sine wave. For this assumption to be reasonable, it is necessary that the exciting current either be nearly sinusoidal or be a small per cent of the primary current. Actually the exciting current is almost never sinusoidal although it may be approximately so for the lower values of flux density; but it is usually, in good transformers, a small per cent of the load current. Therefore, the harmonics introduced by the exciting current are seldom of much consequence in the normal range of operation. However, here again, if due to either high overcurrent or a heavy burden, the core becomes saturated, the exciting-current wave form has large harmonics and the transformer error usually gets relatively large in which case the per cent of harmonics in the secondary current is likely to be appreciable.

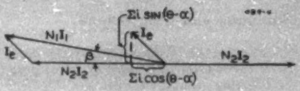
$\Sigma i \sin(\theta-\alpha)$
N_1I_1
I_e
I_e
β
N_2I_2
N_2I_2
$\Sigma i \cos(\theta-\alpha)$

Figure 4. Vector diagram of relation between primary and secondary ampere turns and total exciting ampere turns for core of a current transformer

If the harmonics thus introduced into the secondary current are large enough to be of concern, then a more precise and elaborate definition of accuracy is necessary as the ratio and phase-angle errors alone will no longer suffice to define completely the difference between the correct and the actual values of secondary current.

In figure 1 is shown a wound-type current transformer of a type which will be considered, and below it is a list of the principal symbols used. **Bold-face** type indicates a vector quantity, and all vectors will be referred to the secondary current as a base.

On the basis of the assumption relative to the distribution of leakage flux, the values of leakage flux are:

$$\Phi_{L2}=3.2\frac{N_2I_2}{2L_2}A_2 \qquad (1)$$

$$\Phi_{L0}=3.2\frac{N_2I_2}{L_1+L_2}2A_0 \qquad (2)$$

$$\Phi_{L1}=3.2\frac{N_2I_2}{2L_1}A_1 \qquad (3)$$

In order to determine the exciting current it is first necessary to establish the

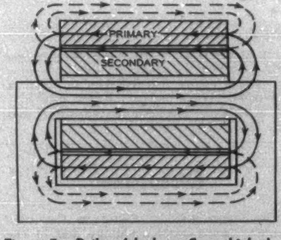

Figure 5. Paths of leakage flux which does not pass through secondary copper

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* The factor 3.2 comes about as follows: For air using metric units flux density $B=H=0.4\pi NI$/cm lines per square centimeter. Therefore, in English units $B=0.4\pi NI$/in.×2.54 lines per square inch.

flux density or densities in the core. This can most readily be done by beginning with Φ_C. To find Φ_C it will be assumed that the fraction of Φ_{L2} which returns inside the secondary is Y and that the fraction of $(\Phi_{L1}+\Phi_{L0})$ returning inside the secondary is U. The fractions Y and U are involved in the calculation of Φ_C in two ways. In the first place, it is apparent that

$$\Phi_C=\Phi_M+Y\Phi_{L2}+U(\Phi_{L1}+\Phi_{L0})$$

In the second place Φ_M is related to E_{L2} and E_2 by the relation

$$E_2=-jK\Phi_M+E_{L2}$$

whence

$$\Phi_M=\frac{j}{K}E_2-\frac{j}{K}E_{L2}$$

and it is obvious that E_{L2} will be dependent on the values of Y and U. In the appendix it is shown that

$$E_{L2}=-jKU(\Phi_{L1}+\Phi_{L0})-jK\Phi_{L2}\left(Y-\frac{1}{3}\right) \quad (4)$$

$$\therefore \Phi_M=\frac{j}{K}E_2-\frac{j}{K}\left[-jKU(\Phi_{L1}+\Phi_{L0})-iK\Phi_{L2}\left(Y-\frac{1}{3}\right)\right]$$

or

$$\Phi_M=\frac{j}{K}E_2-U(\Phi_{L1}+\Phi_{L0})-\Phi_{L2}\left(Y-\frac{1}{3}\right)$$

Substituting this value of Φ_M in the equation of Φ_C above gives

$$\Phi_C=\frac{j}{K}E_2-U(\Phi_{L1}+\Phi_{L0})-\Phi_{L2}\left(Y-\frac{1}{3}\right)+Y\Phi_{L2}+U(\Phi_{L1}+\Phi_{L0})$$

$$\therefore \Phi_C=\frac{j}{K}E_2+\frac{1}{3}\Phi_{L2} \quad (5)$$

It is, therefore, evident that it makes no difference in the value of Φ_C what assumptions are made regarding the return of the leakage flux since neither Y nor U occurs in the expression for Φ_C.

Referring to the last equation since Φ_{L2} has already been expressed in terms of I_2 (equation 1), it is now necessary to determine E_2 in terms of I_2. This is readily accomplished by adding the internal secondary IR drop to the secondary terminal voltage since E_2 by definition is the net voltage induced by all fluxes including leakage.

$$\therefore E_2=V_2+I_2R_2$$

but

$$V_2=I_2R_B+jI_2X_B$$
$$\therefore E_2=I_2(R_2+R_B)+jI_2X_B$$

Substituting in equation 5 this value of E_2 and the value of Φ_{L2} given in equation 1 in terms of I_2 gives

$$\Phi_C=I_2\left[\frac{j}{K}(R_B+R_2)\right]+\frac{j}{K}(jI_2X_B)+\frac{1}{3}\left(3.2\,\frac{N_2I_2}{2L_2}A_2\right)$$

or

$$\Phi_C=I_2\left[\frac{j}{K}(R_B+R_2)-\frac{X_B}{K}+.533\,\frac{N_2A_2}{L_2}\right] \quad (6)$$

To determine the fluxes at other points in the core it is necessary to know how much of the leakage enters the core between various points in its length. In general, it can be assumed that all the leakage flux which passes through and between the copper inside the core window enters the core. Of the remainder about 50 per cent will enter the core between sections 1 and 5.

If Φ_{LT} = flux entering the core between sections 1 and 5,

$$\Phi_{LT}=(\Phi_{L1}+\Phi_{L0}+\Phi_{L2})\times\left[\frac{h}{MLT_0}+\frac{1}{2}\left(1-\frac{h}{MLT_0}\right)\right]$$

Where h = height of core in the case of a "core" type core or twice the height in the case of a shell-type core and MLT_0 = mean length in the direction of the windings around the space between the windings

$$\therefore \Phi_{LT}=\left(\frac{\Phi_{L1}+\Phi_{L0}+\Phi_{L2}}{2}\right)\left(1+\frac{h}{MLT_0}\right)$$

Of Φ_{LT} a certain fraction, K_1 will enter the core between sections 1 and 2, another fraction, K_2, will enter between 1 and 3, and another fraction, K_3, between 1 and 4. The values of K_1, K_2, K_3, will be usually in the neighborhood of 0.57, 0.87, and 0.93 respectively. With these values of leakage flux entering the core the values of flux at the sections 2, 3, 4, and 5 will be given as follows, the subscripts after the Φ denoting the section:

$$\Phi_2=\Phi_C-K_1\Phi_{LT}=I_2\left[\frac{j}{K}(R_B+R_2)-\frac{X_B}{K}+\frac{0.533N_2A_2}{L_2}\right]-K_1\Phi_{LT} \quad (7)$$

$$\Phi_3=\Phi_C-K_2\Phi_{LT}=I_2\left[\frac{j}{K}(R_B+R_2)-\frac{X_B}{K}+\frac{0.533N_2A_2}{L_2}\right]-K_2\Phi_{LT} \quad (8)$$

$$\Phi_4=\Phi_C-K_3\Phi_{LT}=I_2\left[\frac{j}{K}(R_B+R_2)-\frac{X_B}{K}+\frac{0.533N_2A_2}{L_2}\right]-K_3\Phi_{LT} \quad (9)$$

$$\Phi_5=\Phi_C-\Phi_{LT}=I_2\left[\frac{j}{K}(R_B+R_2)-\frac{X_B}{K}+\frac{0.533N_2A_2}{L_2}\right]-\Phi_{LT} \quad (10)$$

The flux densities, β_C, β_2, β_3, β_4, and β_5 corresponding to the fluxes Φ_C, Φ_2, Φ_3, Φ_4, and Φ_5 are, of course, the absolute values of these fluxes divided by the cross-sectional areas of the core at the respective points, and the sines of the angles θ_C, θ_2, θ_3, θ_4, and θ_5 between β_C, β_2, β_3, β_4, and β_5 respectively and the secondary current reversed are the imaginary components of the fluxes divided by the absolute values (see figure 2). It should be noted that θ_C may be greater than 90 degrees.

If α_C, α_2, α_3, α_4, and α_5 (figure 3) are the angles between the flux densities and their respective unit values of exciting ampere turns* i_C, i_2, i_3, i_4, and i_5, as determined from magnetization data on the iron, then by averaging the values for each section of length the components of exciting ampere turns in phase with the secondary current reversed will be:

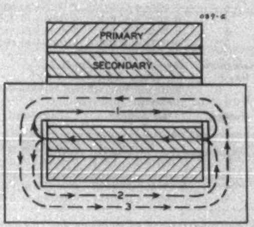

Figure 6. Paths of leakage flux which does pass through secondary copper

$$\frac{L_{12}}{2}[i_C\cos(\theta_C-\alpha_C)+i_2\cos(\theta_2-\alpha_2)]$$
for section 1–2

$$\frac{L_{23}}{2}[i_2\cos(\theta_2-\alpha_2)+i_3\cos(\theta_3-\alpha_3)]$$
for section 2–3

$$\frac{L_{34}}{2}[i_3\cos(\theta_3-\alpha_3)+i_4\cos(\theta_4-\alpha_4)]$$
for section 3–4

$$\frac{L_{45}}{2}[i_4\cos(\theta_4-\alpha_4)+i_5\cos(\theta_5-\alpha_5)]$$
for section 4–5

Where the L's represent the lengths between the sections denoted by the subscripts. Since there are two of each of the above sections in the whole core the total component of exciting ampere turns in phase with the secondary current reversed will be given by

$$\Sigma i\cos(\theta-\alpha)=L_{12}i_C\cos(\theta_C-\alpha_C)+(L_{12}+L_{23})i_2\cos(\theta_2-\alpha_2)+(L_{23}+L_{34})i_3\cos(\theta_3-\alpha_3)+(L_{34}+L_{45})i_4\cos(\theta_4-\alpha_4)+L_{45}i_5\cos(\theta_5-\alpha_5)$$

Similarly the component of exciting am-

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* These quantities here represent actual ampere turns per inch.

Table I. Core Flux

Transformer No. 1 Secondary amperes = 4.89
Burden = 0.533 ohm R and 0.878 ohm X
Volts per turn referred to secondary current

Section	Measured	Calculated
1	0.0158 + j0.0052	0.0152 + j0.0067
2		0.0152 + j0.038
3		0.0152 + j0.055
4	0.0165 + j0.0538	0.0152 + j0.059
5	0.0164 + j0.0583	0.0152 + j0.063

Transformer No. 2 Secondary short-circuited
Secondary amperes = 4.89
Volts per turn referred to secondary current

Section	Measured	Calculated
1		0.0065 − j0.0076
2	0.0064 + j0.0315	0.0065 + j0.035
3	0.0063 + j0.057	0.0065 + j0.058
4	0.0066 + j0.061	0.0065 + j0.062
5	0.0075 + j0.064	0.0065 + j0.067

Transformer No. 3 Secondary short-circuited
Secondary amperes = 4.85
Volts per turn referred to secondary current

Section	Measured*	Calculated
1		0.0058 − j0.0033
2	0.00616 + j0.0064	0.0058 + j0.0064
3	0.00630 + j0.0113	0.0058 + j0.0115
4	0.00640 + j0.0125	0.0058 + j0.0127
5	0.00656 + j0.0144	0.0058 + j0.0138

*Values are average of values obtained on two transformers. Samples differed by almost 20 per cent.

Transformer number 1—General Electric type *KF*-85.

Transformer number 2—General Electric type *KF*-58.

Transformer number 3—General Electric type *WF*-12.

pere turns 90 degrees out of phase with the secondary current is given by

$$\Sigma i \sin(\theta-\alpha)=L_{12}i_C \sin(\theta_C-\alpha_C)+(L_{12}+L_{23})i_2 \sin(\theta_2-\alpha_2)+(L_{23}+L_{34})i_3 \sin(\theta_3-\alpha_3)+(L_{34}+L_{45})i_4 \sin(\theta_4-\alpha_4)+L_{45}i_5 \sin(\theta_5-\alpha_5)$$

Since these components of exciting ampere turns must be added to the secondary ampere turns reversed to obtain the primary ampere turns the latter are given by

$$N_1I_1=-N_2I_2-\Sigma i\cos(\theta-\alpha)+j\Sigma i\sin(\theta-\alpha)$$

as shown in figure 4.

Therefore, ratio correction factor is†

$$RCF=\sqrt{\left[1+\frac{\Sigma i\cos(\theta-\alpha)}{N_2I_2}\right]^2+\left[\frac{\Sigma i\sin(\theta-\alpha)}{N_2I_2}\right]^2}$$

and the phase angle is

$$\beta=\sin^{-1}\frac{\Sigma i\sin(\theta-\alpha)}{N_1I_1}$$

If the common method of compensating by changing secondary turns is employed this percentage, of course, must be subtracted from the value computed above. Other special methods of compensation require special calculations of their own to compute the final accuracy, although it is usually necessary to compute the uncompensated accuracy first in any event.

Discussion and Review

An inspection of the results of the analysis reveals some interesting facts. In the first place equations 6 to 10 inclusive show that the component of flux 90 degrees out of phase with the secondary current is the same in all sections of the core.

Actually this is not strictly true as in transformers having large primary conductors the eddy currents tend to make the leakage flux lag the primary current so that a component of the leakage shows up 90 degrees out of phase with the primary and secondary turns. The magnitude of this component of the leakage flux however, is almost always quite small compared to the average value of total flux in the core as shown by table I.

Equations 6 and 10 show that if the external burden and internal resistance are small Φ_C and Φ_s may be almost 180 degrees apart. This may seem unreasonable but actual flux measurements on the core show it to be true.

It will be noticed that no mention has been made of secondary reactance. The term was not used because it necessitates defining a rather elusive quantity which is not of any particular value in solving the problem. The term is elusive and confusing because reactance, being volts divided by amperes, depends on the voltage induced by the secondary leakage flux which, as has been seen, depends on the direction of return of this leakage. Moreover, the secondary leakage flux, Φ_{Ls} must return outside the secondary coil if the in-phase component of Φ_C is negative, as is generally the case (see equation 6), because if one is to adhere strictly to physical realities there cannot be flux flowing in opposite directions in the same section of the core at the same instant. If Φ_{Ls} does all return outside the secondary, the voltage generated is positive and hence the reactance must be negative. If, on the other hand, the in-phase component of Φ_C is $+{}^1/_3\,\Phi_{Ls}$, the maximum value without leading power factor burden, then ${}^1/_3\,\Phi_{Ls}$ returns inside the secondary. Referring to equation 4 this means $Y={}^1/_3$ and hence the voltage induced by the secondary leakage flux equals zero. Therefore, if reactance is to be defined in the conventional manner and the flux paths are to be taken exactly as they are, the only conclusion is that the secondary reactance is a variable depending on the external burden and varying between zero and $-K\Phi_{Ls}/3I_s$. It is consequently evident that the use of the term "secondary reactance" is of no advantage in this analysis, as a rigorous definition of such a term makes it necessary to solve most of the problem to determine the reactance.

Tables I, II, and III give comparisons of calculated and measured results. Transformers numbers 1 and 2 were different in size but of similar design while the proportions of transformer number 3 were considerably different from the first two. An inspection of the tabulated values shows that there is still some room for improvement in the accuracy of the calculations which further study and added refinements might produce. On the other hand, the discrepancies between the calculated and the measured values of flux are no greater than the errors frequently incurred in computing the physical dimensions of the coils and the space between them. In fact, to go even further, tests show that in some cases the variation between individual samples of the same design is greater than the error of the calculations. Moreover, both tests and simple computation show that what appears to be a small degree of dissymmetry in the coils will frequently cause a large difference between the values of flux in the opposite ends of the core. From a practical point of view, therefore, further refinement in the method of calculating the flux will not produce the improvement in over-all results which would be expected at first glance.

Moreover, any such refinements in the calculations would be largely a matter of field theory in determining the exact conformation of the leakage flux outside the core rather than any alteration of the principle involved and in the writer's

Table II

Transformer Number 1—Leakage Fluxes, Secondary Short-Circuited, Secondary Amperes = 5.0

	Crest Values of Flux	
	Measured	Calculated
Φ_{Ls}	9,450	8,750
$\Phi_{Le}+\Phi_{Li}$	25,650	28,000
$\Phi_{Li}+\Phi_{Le}+\Phi_{Ls}$	35,100	36,800
Negative flux at center of secondary	3,490	2,920
Flux in item above divided by Φ_{Ls}	0.37	0.33
Φ_{LT}	20,270	21,500
$\left[\dfrac{\Phi_{LT}}{\Phi_{Le}+\Phi_{Li}+\Phi_{Ls}}\right]$	0.578	0.585

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† This equation for ratio correction factor, of course, applies only to uncompensated transformers.

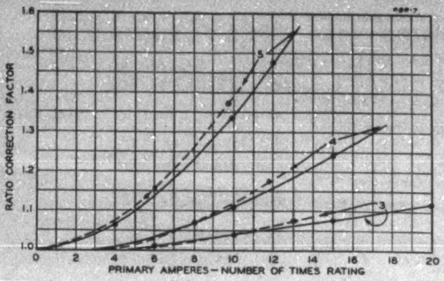

Figure 7. Overcurrent accuracy, transformer number 2 (see table I)

Burdens (volt-amperes based on five amperes):
3—50 volt-amperes, 50 per cent power factor
4—100 volt-amperes, 50 per cent power factor
5—200 volt-amperes, 50 per cent power factor
Solid curves—Test values
Dashed curves—Calculated values

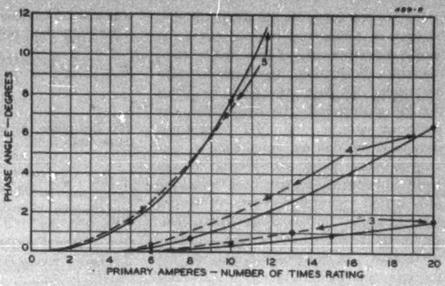

Figure 8. Overcurrent accuracy, transformer number 2 (see table I)

Burdens (volt-amperes based on five amperes):
3— 50 volt-amperes, 50 per cent power factor
4—100 volt-amperes, 50 per cent power factor
5—200 volt-amperes, 50 per cent power factor
Solid curves—Test values
Dashed curves—Calculated values

opinion the chief value in the foregoing analysis is the establishment of the general theorem by which the flux densities in the core can be determined from a given conformation of leakage flux, ampere turns, secondary burden, etc. A concrete illustration of this statement is afforded by the bar or through-type transformer having a single primary turn. In this type the magnetic field is altogether different from the wound type, and a totally different method of evaluating the leakage flux must be used, but having established the values of leakage flux entering the core the method of determining the flux densities in the core is exactly the same as with the wound type. In passing it might be mentioned that in the case of high-current bar or through-type transformers, particularly those with only two secondary coils, the leakage flux is generally of much more importance than in other designs.

A detailed discussion of a precise definition of accuracy including the effects of wave-form errors is beyond the scope of this paper, but since the exciting current is responsible for the added complications of harmonics the subject of wave form should not be passed over without mentioning the importance of the method of making exciting-current tests. At high flux densities in magnetic cores harmonics become a large per cent of the effective values of voltage or current or both depending on the test setup, and results may vary over a wide range depending on the wave form of voltage applied and the methods of measurement. On the basis of both experience and theoretical considerations, in the writer's opinion, the most satisfactory method of making exciting-current tests for current-transformer accuracy calculations is to apply a sine-wave voltage to a coil of low resistance wound on the core, and measure both the current and the voltage with an instrument such as a vibration galvanometer or separately excited dynamometer instrument which will give the fundamental components of the voltage and current waves. For tests at very high density the actual transformer coils and core should be used, the exciting current being applied to the primary coil and the voltage measured on the secondary.

While the method of calculation here presented is subject to the limitations implied in the assumptions and discussed previously, it can be used with reasonably good results for the calculation of overcurrent accuracy up to a current where the maximum magnetizing force in the core is about 300 ampere turns per inch. For many cases this will be high enough. Curves, figures 7 and 8, show results that may be expected up to limit specified. On some transformer designs which by virtue of their geometry have no leakage entering the core, the flux density in the magnetic circuit is uniform throughout its length. In such cases, the overcurrent calculations can be carried out successfully above the limit specified above.

With regard to the practical value of the method of calculation here presented, the method is quite useful in designing transformers to meet special requirements or specifications. It is also valuable in comparing the worth of proposed designs, and its value in this respect is enhanced by the fact that to test such proposed designs it is necessary to make several samples on account of the uncertainty with most core materials in obtaining a representative core in any particular sample. Another value in the method of calculating the accuracy is to be able to compare the relative advantages of different core materials in a given design, where again, if actual tests are made a number of cores will usually have to be tried before a representative sample is obtained. However, in this case where

Table III. Accuracy (Without Wilson Compensation)

Transformer Number	Burden (Ohms)	Secondary Amperes	Measured Values: Ratio Correction Factor	Measured Values: Phase Angle (Minutes)	Calculated Values: Ratio Correction Factor	Calculated Values: Phase Angle (Minutes)
1	$R=0.24$, $X=0$	0.5	0.9994	5	1.0002	4
		5	0.9961	0.5	0.9964	0
	$R=0.98$, $X=1.53$	0.5	1.0043	7	1.0050	6.5
		5	0.9979	−0.5	0.9983	−1
3	$R=0.24$, $X=0$	0.5	1.0054	17	1.0059	17
		5	1.0030	6	1.0029	6
	$R=0.98$, $X=1.53$	0.5	1.0158	17	1.0165	17
		5	1.0064	1	1.0070	1

transformers are probably available it is almost as quick and more accurate to make measurements of flux in the core, thereby eliminating that much computation. If data are then available on the materials to be considered, the accuracy can be computed from these data and the flux measurements.

Moreover, since considerable equipment and expense are involved in making overcurrent accuracy tests, it is, of course, quite valuable to be able to calculate the performance at high currents particularly in designing apparatus for special requirements when there is usually not time to build and test samples.

Appendix

Voltage Induced by Leakage Flux in Secondary; Proof of Equation 4

In verifying equation 4, first consider the leakage $(\Phi_{L0}+\Phi_{L1})$ figure 5. Since all this flux in traversing the space occupied by the coils passes outside the secondary copper, all that returns along the paths represented by the solid lines links all the secondary turns and all that returns along the dotted paths links no secondary turns. Hence, the voltage induced by the amount that returns outside the secondary will be zero and that which returns inside the secondary coil will generate a voltage equal to $2\pi f N_2 \cdot 10^{-8} U \cdot (\Phi_{L0}+\Phi_{L1})$ since U is by hypothesis the fraction returning inside the secondary. Also, since $U(\Phi_{L0}+\Phi_{L1})$ is in phase with the secondary magnetomotive force the voltage generated is $-jKU(\Phi_{L0}+\Phi_{L1})$ referred to the secondary current.

To determine the voltage generated by Φ_{L2} consider any one particular line of flux as shown in figure 6. Let the fraction of the secondary turns between this line of flux and the inside of the secondary coil equal S. If the line of flux returns along the path number 1, the voltage generated is $-jKS$. If, on the other hand, the line of flux returns along path number 2, the voltage is $+jK(1-S)$. The algebraic difference, $jK(1-S)-(-jKS)$ is readily seen to be jK. Hence, the difference in generated voltage obtained by changing the return of the flux line from path number 1 to path number 2 is jK and is the same as the voltage generated by a line following path number 3. But the flux line considered is any line passing through the secondary copper, and, therefore, the result must be true for all such lines. Consequently, the voltage difference due to changing the paths of N such lines from inside to outside is jKN. Therefore, if the voltage generated by assuming all Φ_{L2} returns along path number 1 is E_0, the voltage E_A, generated when $(1-Y)\Phi_{L2}$ lines return along path number 2 is $E_A=E_0+jK(1-Y)\Phi_{L2}$ where Y is, of course, the fraction of Φ_{L2} returning inside the secondary as assumed in equation 4.

It can be shown* that if d_4 = the mean radius of the inside of the secondary coil (that is, $^1/_{2\pi}$ times the length at the inside turn) and d_2 = the thickness of winding in inches, the voltage E_0 is given by the equation

$$E_0=-j2\pi f\left\{\frac{0.8\pi^2N_2^2I^2}{L_2}\left[\frac{d_4d_2}{3}+\frac{d_2^2}{4}\right]\right\}\times (2.54)10^{-8}$$

the 2.54 being the factor to change the formula from centimeters to inches.

$$\therefore E_0=-jK\left(\frac{6.4N_2I_2}{L_2}\right)\left[\frac{d_4d_2}{3}+\frac{d_2^2}{4}\right](\pi)$$

and

$$\Phi_{L2}=\frac{6.4N_2I_2}{L_2}\left[\frac{d_4d_2}{2}+\frac{d_2^2}{3}\right](\pi) \qquad (11)$$

$$\therefore \frac{E_0}{\Phi_{L2}}=-jK\left(\frac{\dfrac{d_4}{3}+\dfrac{d_2}{4}}{\dfrac{d_4}{2}+\dfrac{d_2}{3}}\right)$$

or

$$E_0=-jK\left(\frac{\dfrac{d_4}{3}+\dfrac{d_2}{4}}{\dfrac{d_4}{2}+\dfrac{d_2}{3}}\right)\Phi_{L2}$$

$$E_A=-jK\Phi_{L2}\left[\left(\frac{\dfrac{d_4}{3}+\dfrac{d_2}{4}}{\dfrac{d_4}{2}+\dfrac{d_2}{3}}\right)+Y-1\right]$$

since

$$E_A=E_0+jK(1-Y)\Phi_{L2}$$

or

$$E_A=-jK\Phi_{L2}\left[Y-\left\{1-\left(\frac{\dfrac{d_4}{3}+\dfrac{d_2}{4}}{\dfrac{d_4}{2}+\dfrac{d_2}{3}}\right)\right\}\right]$$

if

$$\left(\frac{\dfrac{d_4}{3}+\dfrac{d_2}{4}}{\dfrac{d_4}{2}+\dfrac{d_2}{3}}\right) \qquad (12)$$

is set equal to q and m equal to $(1-q)$, then

$$E_A=-jK\Phi_{L2}[Y-m] \qquad (13)$$

It is evident that q will be between $^2/_3$ and $^3/_4$. As a matter of fact, it will be generally very nearly $^2/_3$ making m very nearly $^1/_3$. It can, moreover, be shown that the value of $m\Phi_{L2}$ is exactly the same if Φ_{L2} is calculated as shown in equation 1 and m is taken equal to $^1/_3$ as it is if m and Φ_{L2} are computed as shown in formulas 11 and 12.

Using the latter combination it is seen that

$$m\Phi_{L2}=(1-q)\Phi_{L2}=\left[1-\left(\frac{\dfrac{d_4}{3}+\dfrac{d_2}{4}}{\dfrac{d_4}{2}+\dfrac{d_2}{3}}\right)\right]\times \left[6.4\pi\frac{N_2I_2}{L_2}d_2\left(\frac{d_4}{2}+\frac{d_2}{3}\right)\right]$$

$$m\Phi_{L2}=6.4\pi\frac{N_2I_2}{L_2}d_2\left(\frac{d_4}{2}+\frac{d_2}{3}-\frac{d_4}{3}-\frac{d_2}{4}\right)$$

$$m\Phi_{L2}=6.4\pi\frac{N_2I_2}{L_2}d_2\left(\frac{d_4}{6}+\frac{d_2}{12}\right)$$

whereas if Φ_{L2} is computed as in equation 1

$$\Phi_{L2}=\frac{3.2N_2I_2}{2L_2}\left[2\pi\left(d_4+\frac{d_2}{2}\right)d_2\right]$$

$$\Phi_{L2}=\frac{3.2N_2I_2}{L_2}\pi d_2\left(d_4+\frac{d_2}{2}\right)$$

$$=\frac{6.4\pi N_2I_2}{L_2}d_2\left(\frac{d_4}{2}+\frac{d_2}{4}\right)$$

$$\therefore \frac{1}{3}\Phi_{L2}=6.4\pi\frac{N_2I_2}{L_2}d_2\left(\frac{d_4}{6}+\frac{d_2}{12}\right)$$

which is exactly the same as the value of $m\Phi_{L2}$ above.

While the method of calculating Φ_{L2} as given in equation 1 gives a slightly lower value than that of equation 11 the value of Φ_{L1} is higher so that the total leakage $(\Phi_{L2}+\Phi_{L0}+\Phi_{L1})$ is practically the same either way; and equations 1, 2, and 3 are somewhat simpler to use.

Therefore, using the latter formulas for the leakage fluxes and taking $m={}^1/_3$ it is evident from quotation 13 that

$$E_A=-jK\Phi_{L2}(Y-{}^1/_3)$$

Now the voltage generated by the leakage fluxes, E_{L2} has two parts, one, that generated by Φ_{L0} and Φ_{L1} shown in the first part of the appendix to be $-jK(\Phi_{L0}+\Phi_{L1})U$ and second, the part generated by Φ_{L2} which is E_A

$$\therefore E_{L2}=-jKU(\Phi_{L0}+\Phi_{L1})-jK\Phi_{L2}(Y-{}^1/_3)$$

This relation is readily seen to be equation 4 given previously.

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 SUPPLEMENT to ELECTRICAL ENGINEERING—TRANSACTIONS SECTION.

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* See "Principles of Alternating-Current Machinery" by Ralph R. Lawrence, second edition, pages 188 and 189.

Reference Values for Temperature, Pressure, and Humidity

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IN most engineering measurements the readings obtained at a given time depend upon a number of conditions, such as temperature and barometric pressure, existing at the time of the test. In some cases the effects are very small, compared to the accuracy desired in the result, and can be neglected. In other cases prevailing conditions have a decided effect on the numerical values of the test results. For instance, in a determination of the boiling point of water, it is very necessary to consider the barometric pressure. In measuring the flashover voltage of an insulator, the temperature, pressure, and humidity each has a measurable effect on the observed value. Therefore, it has become necessary to select standard conditions to which certain measurements may be referred. This permits measurements made in different laboratories or at different times in the same laboratory to be compared directly. Certain values have been chosen, such as 760 millimeters barometric pressure, to which measurements for any physical or electrical quantity usually are referred, regardless of where in the world a particular measurement is made. This is a very convenient practice and greatly simplifies comparisons and use of engineering data.

It is therefore proposed to consider reference values for the temperature, the atmospheric pressure, and the humidity of the air. Because of their influence on measurements and on the rating of apparatus, the effects of these conditions have been analyzed. Reference values have been selected and correction factors for nonstandard conditions determined by interested standardizing groups. Certain values have been chosen as reference values because of average conditions prevailing at the time original tests were made or because of ease in maintaining a certain condition. A review of current practices indicates that 20 degrees centigrade is a favored reference value for physical and chemical quantities in both United States and international use. On the other hand 25 degrees centigrade is deeply rooted in American electrical practice. The agreement on a barometric pressure of 760 millimeters is universal. With respect to humidity, there is not much consistency in the reference values used or in the units in which they are specified. Humidity is important in fewer cases and its effects have been appreciated and evaluated more recently.

At times there is little advantage in the use of one reference value over another and, lacking organized co-ordination, slightly different values have been introduced in various standards. Although it may be impractical to remedy this undesirable condition in all cases, it is hoped that the information collected here will help to avoid further complication by minimizing the reference values adopted in future standards.

From an engineering point of view there are two types of reference values; in one case the value is of the nature of an average to which values obtained throughout a range of conditions may be referred or corrected. In the second case the problem is one of maximum values rather than average. This condition occurs notably in connection with the thermal limits and therefore the rating of electrical machinery. For rating purposes, the engineer is interested principally in ambient temperatures exceeding certain values and the time during which they prevail. The ambient-tem-

Table I. Reference Temperatures for Physical Quantities*

Quantity	United States Preferred Reference Temperature (Deg)	International Preferred Reference Temperature (Deg)
Resistivity (Cu)	20C	20C
Conductivity (Cu)	20C	20C
Temperature coefficient of resistance (Cu)	20C	20C
Specific gravity (Cu)	20C	20C
Resistivity (metals)	20C	20C
Conductivity (metals)	20C	20C
Temperature coefficient of resistance (metals)	20C	20C
Specific gravity (metals)	20C	20C
Cross section (metals)	20C	20C
Surface resistivity (dielectrics)	18–25C	18–25C
Volume resistivity (dielectrics)	20C	20C
Conductivity (dielectrics)	20C	20C
Coefficient of linear expansion	20C	20C
Coefficient of cubical expansion	20C	20C
Meter length	0C	0C
Gas volume	0C	0C
Gas density	0C	0C
Air density	0, 15, 15.5C	15, 15.5C
15-degree calorie	15C	15C
British thermal unit	60F, 39F	60F, 39F
Thermal conductivity of air	0C	0C
Coefficient of thermal expansion	20C (Ranges)	20C (Ranges)
Density (Hg)	0C	0C
Density (H_2O)	4C	4C
Density (calcite)	20C	20C
Specific gravity (oils)	15/4C	20/4C
Weights (mass)	0C	0C
Refractive index (liquids)	20C	25C – 18C
Refractive index (air)	15C	15C
Cold junction of thermocouples	0C	0C
Standard cell (Weston)	20C	20C
Standard cell (Clark)	15C	15C
Barometers (reference temperature)	0C	0C
Translational energy of molecule	0C	0C
Angstrom unit (wave length)	15C	15C
Range and velocity of alpha rays	0C	0C
Mobility of ions in gases	0C	0C
Resistance of international ohm	0C	0C
Contact potentials	15–25C	
Gauges for metal fits (ASA)	20C	
Ball and roller bearings (ASA)	20C	

*Smithsonian Physical Tables, Smithsonian Meteorological Tables, Chemical Handbook, Marks Mechanical Engineers Handbook, Penders Handbook, International Critical Tables, Physical and Chemical Tables (German).

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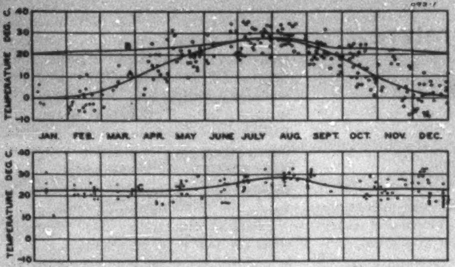

Figure 1

A—The temperatures at which high-voltage impulse tests were made outdoors at Sharon, 1936–38

B—The monthly average temperature curve indoors at Sharon, 1936–37

C—The temperatures at which high-voltage tests were made indoors at Trafford, 1936–39

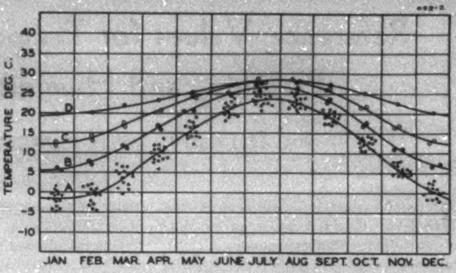

Figure 2. The average temperature curves for various regions in the United States (table IV)

A—Locations 1–15
B—Locations 16–17
C—Locations 18–19
D—Location 20

perature considerations which determine apparatus rating are covered elsewhere,[5,13] notably in AIEE Standard No. 1.

In choosing and correcting to standard reference conditions, simplicity of the method is very desirable. Therefore, the reference value should be selected to represent approximately average actual conditions and to make the correction process as simple as possible. In this connection, the background and development of present standard reference conditions are discussed. Furthermore, the importance of dependable correction factors to convert performance data to the reference conditions is considered.

Temperature

(A). Reference Values

Certain specific values in connection with temperature are easily determined from well-known physical characteristics. The freezing and boiling points of water are examples. Another value is the point of maximum density of water at four degrees centigrade. In the beginning, the temperatures, at which measured quantities are given, depended upon the ease of obtaining certain temperature conditions. As progress was made the desirability of using a single or fewer reference temperatures became apparent. Table I shows some of the reference temperatures for physical and chemical quantities in United States and international use. These values were taken more or less at random from a number of standard handbooks and engineering tables. It is apparent from this table that 0 and 20 degrees centigrade are the reference temperatures now in most common use for such quantities.

Another common reference value which has been adopted, particularly for electrical-engineering use in this country, is 25 degrees centigrade. This value was established as the reference for testing apparatus. In the report of the standards committee and in companion papers presented at the 1913 AIEE midwinter symposium on standardization and apparatus rating,[1] the following parts sum up essentially the basis and reasons recognized at that time for selecting the 25-degree-centigrade value:

> "The reference temperature in the guarantees should, therefore, be such as can easily be secured; that is, it should be the average temperature of the places at which the apparatus may be operated. This is from 20 degrees centigrade to 25 degrees centigrade and, as it is easier to raise than to lower the room temperature, the upper figure is advisable as a reference value. This reference temperature, therefore, should be chosen as 25 degrees centigrade, which is in accordance with the previous AIEE standard."
>
> "It is, therefore, recommended . . . that all measurements and tests of electrical apparatus be based on a room temperature of 25 degrees centigrade."

At this same time, Chubb and Fortescue presented calibration data for the 25-, 37.5-, and 50-centimeter sphere gaps.[2] Although temperature and barometric pressure are not mentioned in their article, it is apparent from the discussions that the effect of these conditions was appreciated. The correction formula in use today for temperature and pressure was given by Peek in his discussion. The foregoing explains the use of 25 degrees centigrade in sphere-gap calibration tables in AIEE standards.[4] The use of this temperature has of course grown and for the past ten years, all high-voltage measurements have been referred to this temperature.

The present status of reference values in the electrical field is summed up in table III. In general, 25 degrees centigrade is the reference temperature specified for tests and test data on electrical apparatus in AIEE and ASA standards. On the other hand, AIEE standard on conductors refers to the 20-degree-centigrade temperature. International Electrotechnical Commission Standards[3] on high-voltage practice, insulators, and conductors are based on 20 degrees centigrade.

The American Society for Testing Materials has standardized a wide variety of electrical-insulating material tests for many of which temperature and humidity are important. Table II gives the reference values and specified testing ranges for a number of ASTM standards. This group has recognized the situation for some time and are aiming at a reference value of 25 degrees centigrade where possible.

(B). Temperature Conditions

For proper selection of reference values, the conditions of location of test as well as location of use are of interest. Therefore it should be useful, at this time, to consider the merits of different reference temperatures in the light of actual conditions recorded over a period of years at typical industrial establishments and of the values reported in the climatic summary of the United States Weather Bureau.[6]

(*a*). *Outdoor Use and Laboratories.* Figure 1*A* shows temperature readings over a period of several years at the Sharon, Pa., high-voltage laboratory of the Westinghouse Electric and Manufacturing Company, recorded in the course of commercial and

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[5] For all numbered references, see list at end of paper.

development testing of station apparatus. At this laboratory, tests on apparatus were made outdoors and the values shown, therefore, represent outdoor, daytime temperatures for Sharon. The range in temperature is from −9 degrees centigrade to 36 degrees centigrade, a total spread of 45 degrees centigrade. The annual average is about 15 degrees centigrade. The April–September average is about 24 degrees centigrade and during the winter months close to 0 degrees centigrade.

A detailed survey of these data plotted in figure 1*A* reveals that temperature variations during the day may affect the air density three to four per cent and even more from one day to the next. These variations are clearly apparent from figure 3, in which the daily maximum and minimum temperatures for Sharon covering the 24 hours during 1936 are given. Daily maximum variations frequently are 10 degrees centigrade, occasionally 15 degrees centigrade, and even more during marked changes in the atmospheric conditions. From similar charts, the annual average temperature during 1936–38 is 9.6 degrees centigrade, a value substantially lower than the 15 degrees centigrade annual average for figure 1*A*, since the former covers the full 24 hours of the day and thus includes the lower temperatures of the night. The chart for 1936 (figure 3) is of further particular interest as it shows a maximum peak temperature close to 41 degrees centigrade (July) and a minimum peak of −26 degrees centigrade (January), both of which, however, were sustained for a relatively short time. The spread between the more sustained maximum and minimum peak values is 60 degrees centigrade, to which there corresponds then a total air-density change of 22 per cent.

The climatic summary of the United States Weather Bureau gives data on temperatures that have been recorded at numerous stations located throughout the entire country. Average temperatures for 24 typical locations are listed in table IV. Locations 1 to 15 are distributed in the hot-summer and cold-winter region. This is the northeastern region between 38 degrees north and 45 degrees north latitude, which extends from approximately 105 degrees west longitude to the Atlantic Seaboard. In this region are located some three-quarters of the population and the major activities of the country. Curve *A* of figure 2 gives the temperature curve for this region as determined from these representative locations. Curve *B* is for two locations at approximately 34 degrees north latitude and curve *C* for two other locations on the 30 degrees north latitude. These curves clearly bring out the change in temperature cycle from north to south, that is, from the hot-summer and cold-winter region into the hot-summer and mild-winter region. Curve *D* is for the extreme southern fringe of the country. Due to the moderating meteorological influences on the Pacific Coast, the temperature for locations 21 to 24 (table IV) are more uniform than at corresponding latitudes in the east. For the northeastern region, the annual average (curve *A* of figure 2) is 11 degrees centigrade; the May-to-August average is 20.8 degrees centigrade. For the southeastern region, the annual average (curves *B* and *C*) is 18.7 degrees centigrade and the May-to-August average, 25.9 degrees centigrade. The annual average for the entire United States over a period of 50 years is 12.7 degrees centigrade.

In addition, the climatic summary gives the average maximum and minimum temperatures. These two limits vary from the average an amount depending on the location. For Pittsburgh which is an "average" location in the northeastern region, the spread of the average maximum and minimum temperatures from the annual average curve is about five degrees centigrade for practically the entire year. Much larger daily variations can occur as it is clearly shown in figure 3. The average temperatures for Sharon, Trafford, and East Pittsburgh come within one to two degrees centigrade of the Pittsburgh values (location 8 in table IV). It is apparent then that the temperature data recorded indoors and outdoors in testing apparatus, which are presented and discussed in this paper, are quite typical of conditions[12] that are expected elsewhere in the northeastern section of the country.

(*b*). *Typical of Large Factory Buildings, Machine Test Floors, and Large Laboratories.* Figure 1*C* shows temperature conditions for a period of years at the Trafford high-voltage laboratory. Here the testing is done indoors although the building is not well insulated and there are many places around doors which are not tight. It is seen that the average temperature throughout the year is around 23 degrees centigrade. This is very close to the standard temperature to which corrections are made. The variations in temperature measurements in this heated building are due to the high temperature gradient from floor to ceiling and represent conditions that generally are encountered in high-voltage laboratories. The distance from the floor to the roof is 65 feet and at times the gradient may reach 0.5 degree centigrade per foot. The temperature values shown in the figure were taken for all kinds of tests, but usually at the level at the apparatus being tested. For a rod gap on the floor, the temperature was measured 3 to 4 feet

Figure 3. The daily temperature variations at Sharon, 1936

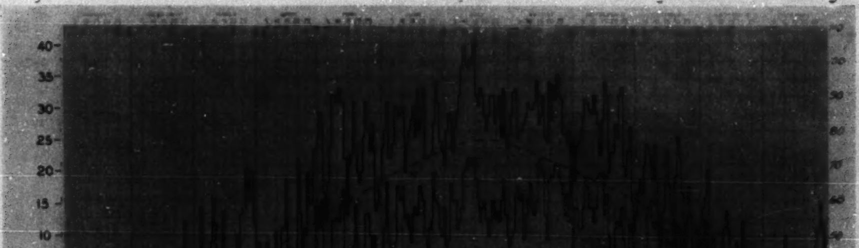

Table II. Reference Values and Testing Temperatures, in the Room-Temperature Range, Specified in October 1939 ASTM Standards on Electrical Insulating Materials

Material	Test or Treatment	Temperature or Temperature Range (Deg C)	Relative Humidity (Per Cent)
Sheet and plate	Physical	25 ±8	
	Conditioning	25	40
Laminated tubes	Physical	25 ±8	
Laminated rods	Physical	25 ±8	
Varnished cloth	Elongation	20 to 30	
Varnishes	Density	25 ±1	
	Viscosity	25 ±0.2	
	Time of drying	25	
	Conditioning	25 water	
Varnished cloth	Conditioning	20 to 30	60 to 65
	Power factor	25 to 30	
Flexible varnished tubing	Conditioning	20 to 30	50 to 55
	Aging	20 approx.	
Shellac	Iodine number	21.5 to 22.5	
Shellac varnish	General	21 to 32	
	Iodine number and drying time	20 to 30	
	Special	20	65
	Specific gravity	20	
Molded materials	Tensile strength	20	
	Dielectric	25	
	Water absorption	25 ±2	
Phenolic laminated sheet	Power factor	20 to 30	
Laminated tubes	Water absorption	25 ±2	
Laminated round rods	Water absorption	25 ±2	
Sheet and plate	Tensile strength	20	
	Rate of absorption	25 ±2	
	Total absorption	20 to 30	
	Bonding strength	20 approx.	
	Hardness	25 ±2	
Insulating oils	Dielectric	20 to 30	
Electrical porcelain	Tensile strength	20 approx.	
Filling and treating compounds	Power factor	20 to 25	
Untreated paper	Conditioning	20 to 30	65
Friction tape	Adhesion	20 to 22.2	less than 80
Rubber tape	Physical	20 to 28	
Rubber gloves	Electrical	15 to 32	
Asbestos yarns	Conditioning	21	65

above the floor and for a suspension-insulator string, the temperature measurement might be made as high as 30 feet above the floor. The average temperatures experienced indoors at Sharon are shown in figure 1*B*. These temperatures were recorded at one of the test floors where apparatus is subjected to the usual standard commercial tests as well as to other tests that may be required. The form of this average curve is practically similar to that of figure 1*C*.

(*c*). *Typical of Living Quarters Such as Homes, Offices, and Laboratories in Smaller Rooms.* In a well-heated building, consisting of smaller quarters, during the winter months, the temperature is 24 ±1 degrees centigrade. This conclusion has been reached from the temperature measurements over a period of time in the engineering laboratory building at East Pittsburgh.

From the foregoing, therefore, the problem of reference temperature and correction factors is twofold. In the first place, the desirability of keeping corrections small should be kept in mind chiefly because of inaccuracies and variations in the original correction-factor data. The undesirable effect of discrepancies due to very large correction factors can be somewhat minimized by using reference values representing average conditions. Thus, for outdoor testing as in case (*a*), in correcting data to a standard temperature of 25 degrees centigrade the corrections are, in most cases, larger than if a lower temperature were used as a reference value. The 25-degree-centigrade reference value has merit for case (*c*) and also for case (*b*), though it is apparent that there is not much to choose between 25 degrees centigrade and 20 degrees centigrade in these two cases. However, as a general rule, it is desirable to have the reference temperature higher rather than lower than the average, for it is easier to heat than to refrigerate to obtain standard conditions for testing. The second part of the problem pertains to dependable correction factors which are desirable in view of the inherent variations in atmospheric conditions.

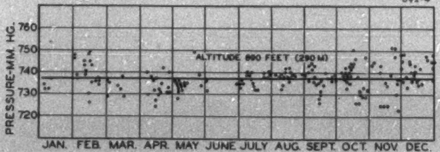

Figure 4. The barometric pressures for tests at Sharon, 1936–38

Barometric Pressure (Altitude)

The barometric pressure of 29.92 inches (760 millimeters) of mercury has been adopted universally as a standard reference value. This represents average conditions at sea level. The pressure of the atmosphere affects the thermal limits and the voltage rating of apparatus. Much apparatus is tested and used at elevations above sea level and consequently these effects cannot be ignored.

For a given location, however, the variations encountered in barometric pressure throughout the year are relatively small. Figure 4 shows readings at the Sharon laboratory during 1936–38. Each reading was taken at the time of a voltage flashover test. A ±1.5 per-cent band from the average includes practically all variations and a detailed examination of the records shows that diurnal variations seldom exceed 0.5 per cent. In a few instances only did the barometer vary from one day to the next as much as 2 per cent. To the meteorologist, these variations are an important measure of the trend in the weather or of prevailing conditions, but for the engineering purpose of this survey, the data hardly permit other than an average curve drawn through 735 millimeters mercury. This average value of mercury column corresponds quite well with the altitude of the Sharon laboratory which is 890 feet (290 meters). The effect of altitude on barometric pressure[7] is shown in figure 5.

The AIEE TRANSACTIONS for 1913 indicate that the standards in use at the time prescribed a correction of one per cent of the observed rise in the temperature of apparatus for each 10-millimeter deviation from the standard pressure of 760 millimeters. The present standards state that specified ratings hold for elevations up to 1,000 meters. This is a gratifying example of the simplification of practice in using a reference value, which is so desirable. It is noted in passing that AIEE Standard No. 11 for Railway Motors states: "Machines rated in accordance with these standards may be tested at any altitude not exceeding 1,200 meters and no correction shall be applied to the observed temperature rise". AIEE Standard No. 16 for Railway Control Apparatus gives the usual figure of

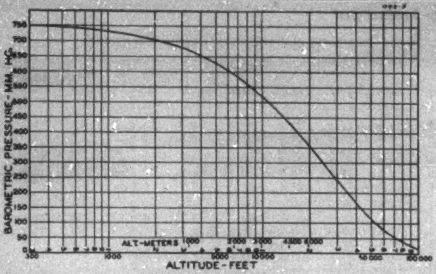

Figure 5. The effect of altitude on barometric pressure

1,000 meters. AIEE Standards for Transformers in addition to the 1,000-meter range give a correction for temperature rise tests made at altitudes greater than 1,000 meters.

At 1,000 meters the normal barometric pressure is 675 millimeters. Based on the old AIEE rule, an observed temperature rise at this altitude would be reduced eight per cent by the correction. As indicated above, present practice is to ignore altitude corrections up to 1,000 meters and to classify equipment for use at higher altitudes as "high-altitude apparatus". This applies to voltage as well as to thermal ratings. This simplification of the problem recognizes the impropriety of permitting a few applications to complicate the majority of ratings and measurements. For voltage flashover measurements, however, it is standard practice to combine barometric and temperature corrections under relative air density.

A recent occurrence is a backward step in progress toward uniformity of engineering practice. The United States Weather Bureau in July 1, 1939, adopted the practice of using the millibar as the unit of pressure. A bar is one dyne per square centimeter and 1,013.3 millibars corresponds to 760 millimeters, the standard atmospheric pressure. Several years ago the millibar was adopted for aerological observations and enjoys international use in this limited field. From an engineering point of view this action is regrettable. Barometric pressure invariably is expressed in terms of the height of a mercury column, both in standards and in laboratory practices. The use of Weather Bureau data will therefore require the use of a conversion factor.

The problem of barometric pressure in engineering measurements is a case where a single standard exists and where agreement on the unit of expression is reasonably consistent. Corrections have been minimized by standardizing on a range where possible. This fortunate situation is partly because the range of pressure variations in practice is small and the effects thereof on measurements and ratings correspondingly small. The growing use of electrical apparatus for aeroplanes eventually may lead to some modifications of present practices concerning ratings.

Humidity

Nearly 30 years ago the effects of humidity of the air on temperature rises of machinery were investigated and found to be negligible. It was also found that sphere-gap spark-over values were not influenced by humidity. Changes in

Table III. Reference Values in Apparatus Standards

		Reference Values**			
Standard*	Apparatus	Temperature (Deg C)	Barometric Pressure (Mm)	Humidity (Mm Hg)	Correction Factors Specified
AIEE No. 4—1940	Measurement of test voltage in dielectric test	25	760	15.45	Relative air density
AIEE No. 41—1940	Insulator tests (insulators)	25	760	15.45	
AIEE—Bushings	Electrical Engineering, December 1934; AIEE Paper 40-37†	25	760	15.45	Relative air density, humidity, water resistance
ASA—1940—Proposed	Transformers, regulators, and reactors	25	760	15.45	Temperature rise above 1,000 meters
ASA—*C*50—1936	Rotating electrical machinery	25	760		Temperature cooling air and windage losses at high altitude
AIEE No. 19—1938	Oil circuit breakers	25	760	15.45	Voltage rating and temp. rise at high altitudes. Water resistivity
AIEE No. 22—1925	Disconnecting and horn-gap switches	Reference values and correction factors for numbers 22, 20, 28, and 26 substantially as in number 19			
AIEE No. 20—1930	Air circuit breakers				
AIEE No. 28—1936	Lightning arresters				
AIEE No. 26—1936	Automatic stations				
AIEE No. 18—1935	Capacitors	Leakage tests at 20 deg C			
AIEE Paper 40-38‡	"Recommendations for High-Voltage Testing"	25	760	15.45	Humidity corrections for rod gaps, insulators and bushings, relative air density
AIEE standards on conductors	Aluminum, copper, etc.	Resistance, temperature coefficient, and density of metal specified at 20 deg C			
IEC standards	High-voltage practice, insulators, and conductors	20	760	11	

* Some of the AIEE are also ASA standards.

** Apparatus standards almost invariably specify an altitude not exceeding 1,000 meters as usual service condition. AIEE Standard No. 11 ("Railway Motors and Other Electrical Machinery on Rail Cars and Locomotives") is the exception as it gives 1,200 meters.

† AIEE Transactions, volume 59, 1940 (October section), pages 590–1.

‡ Committee report, AIEE Transactions, volume 59, 1940 (October section), pages 598–602; also Electrical Engineering (AIEE Transactions), June 1937.

Table IV. Average Temperatures*

Climatic Summary of United States—Weather Bureau, United States Department of Agriculture (Degrees Centigrade)

	Jan.	Feb.	Mar.	Apr.	May	June	July	Aug.	Sept.	Oct.	Nov.	Dec.	Annual Average
1. Boston, Mass.	− 2.1	− 2.0	2.4	8.0	14.1	19.2	22.2	21.0	17.6	12.0	5.8	0.1	9.8
2. New York, N. Y.	− 0.6	− 0.6	3.6	9.3	15.5	20.4	23.3	22.4	19.2	13.3	6.8	1.2	11.2
3. Philadelphia, Pa.	0.5	0.9	5.2	11.1	17.1	22.0	24.7	23.6	20.2	14.1	7.6	2.2	12.5
4. Baltimore, Md.	1.5	2.1	6.3	12.2	17.8	22.9	25.4	24.3	20.4	13.9	8.2	2.9	13.2
5. Washington, D. C.	1.0	1.8	6.3	12.0	17.8	22.5	18.8	23.7	20.2	13.9	7.5	2.5	12.8
6. Albany, N. Y.	− 4.7	− 4.6	0.8	8.0	14.8	19.9	22.6	21.4	17.5	10.9	4.3	− 2.1	9.2
7. Buffalo, N. Y.	− 3.8	− 4.3	− 0.2	5.9	12.2	18.0	21.0	20.3	16.8	10.4	3.9	− 1.6	8.3
8. Pittsburgh, Pa.	− 0.6	− 0.2	4.4	10.5	16.7	21.3	23.6	22.4	19.2	12.7	6.0	1.0	11.5
9. Columbus, Ohio	− 1.6	− 0.9	4.4	10.6	16.6	21.4	23.9	22.7	19.8	12.7	5.6	0.2	11.3
10. Detroit, Mich.	− 4.2	− 3.3	1.0	7.8	14.3	19.6	22.4	21.3	17.6	11.2	3.9	− 1.7	9.2
11. Indianapolis, Ind.	− 2.0	− 0.7	4.7	11.3	17.1	22.1	24.4	23.2	19.5	13.0	5.6	0	11.6
12. Chicago, Ill.	− 4.5	− 3.1	2.2	8.4	14.0	19.5	22.6	21.9	18.4	12.1	4.4	− 1.7	9.6
13. St. Louis, Mo.	− 0.2	1.6	7.0	13.6	18.9	23.8	26.3	25.2	21.2	14.3	7.0	1.5	13.4
14. Denver, Colo.	− 1.0	0.3	4.0	8.5	13.6	19.2	22.2	21.6	20.8	10.6	4.4	− 0.1	10.1
15. Salt Lake City, Utah	− 1.6	1.0	5.3	9.9	14.4	19.7	24.5	23.7	18.0	11.4	4.9	− 0.2	11.0
16. Atlanta, Ga.	6.1	7.5	11.5	16.1	20.7	24.5	25.7	25.2	22.8	17.0	11.0	7.0	16.3
17. Little Rock, Ark.	5.4	7.1	11.9	17.0	21.1	25.4	27.2	26.7	23.6	17.6	11.2	6.7	16.8
18. New Orleans, La.	12.6	14.0	17.3	20.6	24.1	27.2	28.0	28.0	26.4	21.7	16.7	13.1	20.8
19. Houston, Texas	11.8	13.3	17.1	20.8	23.9	27.3	28.5	28.4	26.1	21.5	16.3	12.5	20.8
20. Miami, Fla.	19.7	20.0	21.8	23.3	25.3	26.8	27.6	27.8	27.2	25.3	22.6	20.3	24.2
21. Seattle, Wash.	4.3	5.5	7.2	9.8	12.7	15.3	17.7	17.6	14.9	11.3	7.8	5.4	10.8
22. Portland, Ore.	3.9	5.7	8.4	11.1	14.1	16.8	19.5	19.4	16.5	12.5	7.9	5.1	11.8
23. San Francisco, Calif.	9.9	11.5	12.2	13.1	13.8	14.8	14.8	15.1	16.3	15.8	13.7	10.7	13.5
24. Los Angeles, Calif.	12.8	13.3	14.2	15.5	16.8	19.2	21.3	21.8	20.8	18.5	16.6	13.8	17.2

* Average for the various locations in from 40 to 100 years.

flashover values of insulators and point gaps with humidity were observed, but it was less than 10 years ago that more complete quantitative tests[8] were started. AIEE Standards No. 41 for insulators gave a formula for determining the humidity from the wet- and dry-bulb temperature readings in terms of the vapor pressure in inches of mercury and specified 0.6085 inch as the standard humidity. It was stated that flashover voltage shall be determined at or corrected to this standard humidity, but no correction factors were given and the control of humidity for much high-voltage testing was impractical. The standard humidity value was arrived at by choosing 65 per cent relative humidity at 25 degrees centigrade and 760 millimeters pressure as the standard. This represents average conditions in the summer months when many insulation troubles appear due to lowered leakage resistances and to the lightning season.

The annual humidity variations at the Sharon and Trafford laboratories as measured in the course of high-voltage testing are shown in figures 6 and 7 and may be regarded as typical of the more populated sections of the country. Diurnal variations in the absolute humidity of 0.10 inch mercury are common, but seldom exceed 0.20. From one day to the next, the humidity may vary from 0.20 to 0.30 inch mercury. The total spread for the year may reach 0.80 inch mercury, to which amount there corresponds a humidity correction factor of 25 per cent for some of the station and line apparatus. The annual average for the recorded data in the figures is close to 0.40 inch mercury. In the summer months, the average is above 0.60 inch mercury. It is at once seen that from a laboratory viewpoint the 0.6085 inch standard is too high. About 80 per cent of the testing is carried on at substandard conditions. This means a higher percentage correction factor and test results are correspondingly more at the mercy of the accuracy of the correction factors. These, of course, were based on a limited amount of data. However, the difficulties of changing the standard on which so much work has been based in determining insulation values and in setting insulation levels have made acceptance of the above undesirable feature much more attractive than the confusion of changing.

The laboratory subcommittee[9,10] of the EEI–NEMA Joint Committee on Coordination of Insulation have been working on the problem of the influence of humidity in connection with insulation problems for many years. Similarly, the International Electrotechnical Commission has carried on work of this nature abroad. The IEC has adopted a standard humidity of 11 grams per cubic meter, which corresponds to 0.45 inch vapor pressure. This is much closer to average testing conditions throughout the year. It would be convenient to have the United States and International standard agree to facilitate comparing data. This is a matter for the interested committees to consider. Fundamentally, the problem is one of the inconvenience of changing established values rather than of the intrinsic merits of a particular standard.

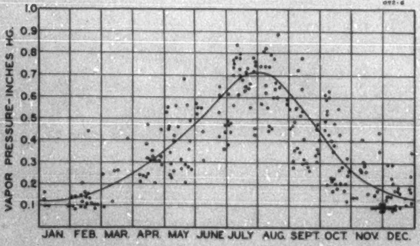

Figure 6. Humidity conditions at the Sharon high-voltage laboratory for outdoor tests, 1936–38

An undesirable situation prevails in connection with wet flashover testing. AIEE standards for circuit breakers and for transformers call for wet testing of bushings with a precipitation of 0.1 inch

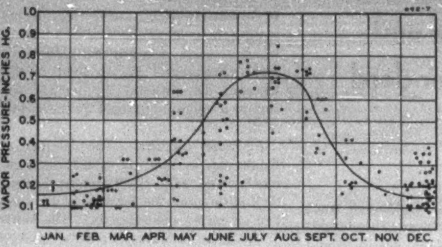

Figure 7. Humidity at the Trafford laboratory for indoor tests, 1934–38

per minute and a resistivity of 12,000 ohms per centimeter cube. AIEE standards for insulators specify wet flashover testing at a precipitation of 0.2 inch per minute and a water resistivity of 7,000 ohms per inch cube. AIEE standards for lightning arresters specify at least 0.1 inch of water per minute with the resistivity of 5,000 ohms per inch cube. The IEC groups have discussed standardizing on 3 millimeters per minute precipitation and 9,000–10,000 ohms per centimeter cube resistivity. A committee has been appointed to study and report on this situation and it is hoped that a simplification of testing procedures will result.

Summary

Reference values in present use are partly a product of history and were partly selected as a result of analysis. In some cases they represent average conditions and sometimes they do not. For certain applications different standardizing groups use different values and co-operative activities are thereby hindered.

An attempt has been made here to show the relation of reference values to variations found in practice. In most cases correction factors are available to correct measured values to standard conditions. The importance of correction factors is shown by figure 8 which gives the correction factor for humidity for the 60-cycle flashover voltage of suspension insulators. This indicates a correction of 16.5 per cent for tests made at 0.1 inch vapor pressure, a common condition in the winter months. Hence, from this point of view the selection of reference levels to minimize correction factors is to be recommended.

Attention is called to existing conditions, particularly where they are not conducive to easy comparisons of testing data and to simple testing procedures. It is hoped to stimulate the study of the problems involved and to assist present and future standardizing bodies in avoiding the confusion of the past and in organizing further work on a rational basis. At best all reference values and specified testing ranges must be a compromise for broader variations encountered by equipment under service conditions. Therefore, it seems reasonable to minimize the number and the ranges of the reference conditions.

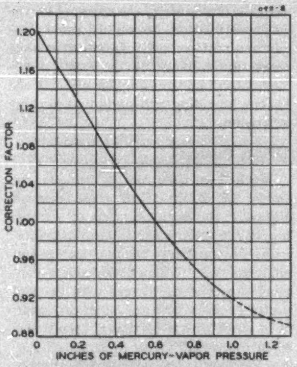

Figure 8. The importance of correction factors is indicated by the 20-per-cent spread in flashover voltages for the changes in absolute humidity indicated in figures 6 and 7

Humidity correction factors for 60-cycle flashover of rod gaps and suspension insulators; multiply measured kilovolts at humidity of abscissa by the correction factor

Standard humidity 0.6085 inch (15.45 millimeters)

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 Supplement to Electrical Engineering—Transactions Section.

Rating of Potential Devices and Suggested Material for a Standard

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THERE has been an evident need for an exposition of the factors relating to the rating of capacitance potential devices and to their application. It is our intention to review this situation indicating the several limitations which govern ratings and application, and to offer material which may be used in the preparation of standards for potential devices. Vacuum-tube equipment is not covered. The authors were requested by the AIEE relay subcommittee to write a paper outlining the basis of rating for the purpose of formulating standards.

Description of Device Circuits

The potential device may be defined as a voltage-transforming equipment used in conjunction with a high-voltage bushing or capacitor as a source of energy to provide a low voltage suitable for operating instruments and relays. The output voltage may be corrected by one of several means to obtain the desired instrument voltage and phase angle.

LINES OF DEVICES

Practice has resulted in two varieties of capacitance devices, one for operation with bushings having a capacitance tap, and one for operation with coupling capacitors such as used for carrier-current work. The former is commonly referred to as a "bushing-potential device" and the latter as a "coupling-capacitor potential device".

TYPES OF DEVICES

Practice has also resulted in two general types of devices as used with coupling capacitors, and they may be designated as "quadrature" and "in-phase", respectively. With the "quadrature device" the maximum output is obtained when the phase angle between the output voltage and the impressed line-to-ground voltage of the phase to which the device is connected through the coupling capacitor is approximately 90 degrees. With the "in-phase device" the rated output is obtained with these voltages substantially in phase. It is not essential, however, that the quadrature device always be adjusted for 90 degrees, and hence it may be more aptly termed "out-of-phase" device. It is essential that the in-phase device always be adjusted for the in-phase relation, to obtain rated output and performance. However in some cases, an in-phase device may be adjusted to obtain an angular shift at the expense of rating and performance characteristics.

DEVICE CIRCUITS

Much has been written about the theory of operation of these devices. Basically, the device consists of a transformer for voltage reduction and a reactance, or auxiliary burden, for phase-angle control. When used with a coupling capacitor the device may be connected in series with the main capacitor, or in shunt with a portion of it, whereas when connected to an apparatus bushing, the shunt arrangement only is used.

The most usual circuit arrangements are as shown in figure 1, (*a*)—(*f*).

Quadrature or Out-of-Phase Circuits. The circuit of figure 1*a* is the simplest and least useful and may be used when the phase angle of the secondary voltage is a matter of indifference and the load burden is exactly right to obtain the required voltage. If the load burden becomes disconnected the voltage rise will be limited only by the insulation strength of the transformer, or by the flashover of a protective gap. Its use should be discouraged.

Figures 1*b* and 1*c* are the circuits commonly used with the out-of-phase devices, and it should be noted that in figure 1*c* the auxiliary burden must include an inductive reactance to compensate for the capacitive reactance of C_2, if true quadrature performance is desired. The magnitude and phase angle of the output voltage is controlled by adjusting the value of the auxiliary burden, which consists of resistance, inductance, and capacitance. If the load burden becomes disconnected the voltage will assume a value determined by the setting of the auxiliary burden.

In-Phase Circuits. The circuits of figures 1*d*, 1*e*, and 1*f*, are those commonly used with the in-phase devices, depending upon the location of the control, or series, reactance Z_s. The value of the series reactance is closely equal to the reactance of the auxiliary, or shunt, capacitor C_2 and is adjustable, mainly for phase-angle control. The capacitor C_2 is also adjustable in value to correct the power factor of the burden to unity. If the device secondary becomes short-circuited the voltage on the device primary will rise and be limited only by the losses or by the flashover of the protective gap.

Procurement of voltage at zero phase angle error primarily involves the use of a tapped bushing, tapped capacitor, or the equivalent, which in reality becomes a capacitance potentiometer. The tap voltage when supplying a totally capacitive burden will be in phase with the line-to-ground voltage of the coupling capacitor or bushing. A burden, other than zero power factor, will cause a phase displacement of the output voltage depending on its location in the circuit and the angular relation of the capacitor charging current and the burden current. This inherent phase displacement is corrected by the use of the inductive reactance Z_s.

Factors Affecting Rating

The rating of a potential device will depend upon economic and performance limitations that may be imposed. Factors involved in determining rating are:

1. Line voltage and frequency
2. Capacitance (electrostatic capacity) of the bushing or coupling capacitor
3. Permissible operating tap voltage on primary of device transformer
4. Insulation level of the device, and shunt or auxiliary capacitor
5. Characteristics and requirements of burden to be energized
6. Over-all performance requirements

AVAILABLE VOLT-AMPERES

The maximum volt-amperes available for potential-device operation depends on the line-to-ground charging current through the bushing or coupling capacitor and the operating tap voltage at which the potential device diverts this current for instrument use. Roughly the prod-

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1. For all numbered references, see list at end of paper.

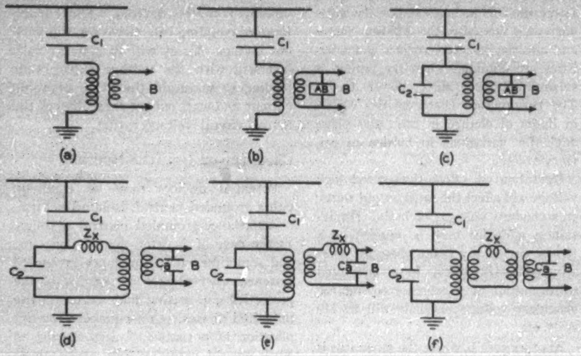

Figure 1. Typical circuits used for potential devices

C_1—Line-to-tap capacitance, sometimes coupling capacitance

C_2—Tap-to-ground capacitance, sometimes auxiliary capacitance, sometimes shunt capacitance

B—Useful burden

AB—Auxiliary burden for control

C_a—Power-factor-correction capacitance

Z_x—Series reactance for control

uct of these two quantities represents the maximum volt-amperes available from the transmission line. Practical considerations place a limit on the charging current and operating tap voltage. The voltage rating of the bushing, or the actual carrier coupling, fixes the capacitance. The operating line voltage and frequency determine the charging current that passes through this capacitance.

Watts Output

The output in watts of a potential device may be more accurately expressed:[1]

$$W=2\pi f C_1 E E_2 \sin\alpha - K$$

where f is the system frequency, C_1 is the capacitance line to tap, E and E_2 are respectively the line and tap voltage to ground with the angle α between them. K represents the loss in the device network. The actual rating of a device is somewhat less than the maximum theoretically available to insure satisfactory operating performance. The term $2\pi f C_1 E$ is the charging current and term E_2 is the operating voltage at which the potential network is normally energized. This voltage usually dictates the insulation level of the transformer and the shunt or auxiliary capacitor which will be used in the circuit.

Effect of Series Reactance

The use of series reactance for phase-angle control (in-phase device) will affect the transformer operating voltage and the resultant voltage on the auxiliary, or shunt, capacitor. The circuit may be analyzed by several methods. However, without going into detail, the network resolves itself into an essentially parallel resonant circuit wherein the bushing or coupling capacitor capacitive reactances are fixed and the equivalent inductive reactance may be varied. The operating tap voltage will increase as the inductance in the circuit is increased, up to the point of resonance, for any fixed load impedance. The rating of a device must not be such that will result in a voltage across the tap and transformer in excess of their individual insulation ratings.

Protective Gap

A protective gap is connected across the tap circuit or transformer primary to provide overvoltage protection in service, and when so protected the device should be able to withstand the overvoltages to which the coupling capacitor or bushing may be subjected. The setting of this gap must be co-ordinated with the normal-frequency voltage that may be obtained during the fault period and with the voltage rating of the component parts of the circuit to be protected. The gap should not operate at less than twice normal voltage at normal frequency, otherwise incorrect voltage may be imposed on the relays. Thus the setting of an insulation level for the component parts of the device will depend on the maximum normal operating voltage of the tap circuit or potential transformer, which in turn will govern the output of the over-all equipment for a given voltage class of bushing or coupling capacitor. Naturally economic factors will also affect the setting of the insulation level and ultimate size of the equipment.

Burden Requirements

The requirements of the burden to be energized, such as phase angle displacement of the voltage and permissible ratio error will affect the required characteristics and rating of the device. For instance, if voltage indication only is required at normal operating voltages, device regulation and phase-angle displacement are of no concern and the simplest form of device would be satisfactory. On the other hand, if certain types of high-speed relays are to be energized a much more accurate device will be required. This latter type of burden requires an accurate reproduction of the primary voltage both as to phase angle and ratio over a large range of line voltage and secondary burden. Nonlinear burdens represent a special case and their use leads to instability of the combined device and burden circuit for certain conditions of operation.

Performance Features

The over-all performance of a device includes:

(*a*). *Performance Characteristics*

1. Regulation, or change in ratio and phase angle, with variation in burden, at constant applied voltage.

2. Regulation with variation in applied voltage, at constant burden impedance.

Note: The regulation is usually given on the basis that the device is initially adjusted for normal voltage and burden impedance.

(*b*). *Transient Characteristics*

The transient characteristics consist of the rapidity and fidelity with which the device follows changes in the system voltage, and the freedom from instability under normal and fault conditions.

Regulation

The ratio and phase-angle performance of a device may be represented by:[2]

$$E_b=E_{bo}-I_bZ \qquad E_{bo}=\frac{A}{n}E_h$$

where E_b and I_b are the burden voltage and current respectively; E_{bo} the no-load voltage; Z the total equivalent potential device impedance; E_h the line-to-ground voltage, A the relation between certain impedances, and n the ratio of the potential-device transformer. These relations show that increased burdens on a given device generally result in increased ratio and phase-angle errors. This is due to the increased range of voltage on the potential device or tap circuit and to increased losses in the potential network.

Transients

Transient performance depends on the constants of the potential network used, the power factor, and type of the burden energized. All of these are interrelated with the device rating and the excitation requirements of the potential network.

Effect of Temperature Changes

The effect of temperature changes on the performance of potential devices may be considered in two steps: the effect inherent in the device itself; and the effect resulting from the changes in burden characteristics due to temperature changes. In the case of the in-phase device the temperature effect is practically negligible from either consideration for the usual conditions of operation. For the case of the out-of-phase device the inherent effect is also negligible except in regard to the coupling capacitance (auxiliary or shunt capacitance not used) for which a change of $2^1/_2$ per cent may take place for a temperature change from −20 degrees to 130 degrees Fahrenheit. Changes in burden, including the auxiliary burden, due to changes in temperature affect the performance in accordance with the characteristic curve.

Table I. Maximum Ratio and Phase-Angle Deviation With Variation in Applied Voltage

Per Cent Primary Voltage	Class A		Class B		Class C	
	Ratio (Per Cent)	Angle (Deg)	Ratio (Per Cent)	Angle (Deg)	Ratio (Per Cent)	Angle (Deg)
110	±1	±1	± 2	± 2	±2	± 5
100*	±1	±1	± 2	± 2	±2	± 5
85					±5	±10
25	±3	±3	± 6	± 6		
5	±5	±5	±12	±12		

* Device to be initially adjusted within these limits.

Basis of Rating

Potential-device rating is expressed on definite primary and secondary voltages, the watts output, and the performance characteristics. The primary voltage ratings are the standard high voltages such as 115 kv, 138 kv, etc. The corresponding secondary voltages are 115 and/or 66.4.

The output rating of potential devices is given in watts, instead of volt-amperes as is customary for potential transformers. This is done because the watts output is actually the limiting factor and also the best performance characteristics are obtained when the burden is substantially at unity power factor. The performance characteristics consist in limits of change in ratio and phase angle for variations in burden or primary voltage.

Operation at other than rated high voltage will affect the rated output watts, or secondary voltage, or both. For instance, a device may be applied on a 138-kv circuit at a point where the operating voltage is 132 kv. In this case rated watts output may be obtained but the secondary voltage normally will be 110 volts, etc.

Another case is where the apparatus is operated at a circuit voltage corresponding to the next voltage class below normal insulation rating. For such a condition a special device must be used and the device output is derated in proportion to the reduction in primary voltage, and normal secondary voltages and performance characteristics obtained.

Effect of Bushing Capacitance

The low capacitance of apparatus bushings limits the charging current available for potential-device operation. Impulse characteristics of these bushings, and the apparatus in which they are installed, place a definite limit on the operating tap voltage and thus the capacitance of the tap for potential device operation. The higher-voltage bushings will have a larger charging current than the lower-voltage bushings and thus the bushing potential device ratings will vary with the voltage class of the apparatus bushing. These output ratings will vary for each of the accepted standards of primary voltage.

Effect of Coupling Capacitance

Coupling-capacitor potential devices will have the same rating for any voltage class and in general it is higher than for bushing potential devices. The capacitance of coupling capacitors for the various voltage classes will in general vary inversely with the voltage rating thus tending to maintain the same charging current and watt output regardless of the normal circuit voltage rating.

Grounded or Isolated Neutral

Potential devices may be used on either grounded-neutral, isolated-neutral, or impedance-grounded neutral systems. In the case of isolated-neutral systems, and sometimes for impedance-grounded systems, overvoltages in excess of the protective gap setting may occur on the unfaulted phases. Consequently the application of potential devices should be given special consideration under these conditions.

Choice of a Potential Device for Relaying

The in-phase device should be used where the following forms of relaying are involved: ground directional, phase directional, carrier pilot, pilot wire, distance.

The out-of-phase device may be used for any one of the following forms of relaying: ground directional, balanced current with voltage restraint, undervoltage or overvoltage. It should be noted that most directional ground relays are arranged for operation from in-phase devices. If an out-of-phase device is used, a change in the voltage circuit of the relay is generally necessary.

Ratio and Phase-Angle Characteristics

The potential device should be chosen having ratio and phase-angle accuracy commensurate with the degree of accuracy required of the relaying. Generally, the accuracy of any in-phase device is suitable for all except possibly some forms of distance relaying.

In the case of distance relaying using reactance-type elements, errors in current and voltage sources introduce the following error in distance measurement:

$$\text{Per cent error} = 100\left[1 - \frac{K_c}{K_p}\,\frac{\sin(\phi+\theta_p-\theta_c)}{\sin\phi}\right]$$

where

K_c = ratio correction factor of current transformer
K_p = ratio correction factor of potential device
ϕ = power factor angle of line
θ_p = phase-angle error of potential device (positive if leading)

Table II. Maximum Ratio and Phase-Angle Deviation With Variation in Burden

Per Cent Burden	Class A Ratio (Per Cent)	Class A Angle (Deg)	Class B Ratio (Per Cent)	Class B Angle (Deg)	Class C Ratio (Per Cent)	Class C Angle (Deg)
100*	± 1	±1	± 2	± 2	±2	±5
50	± 6	±4	±10	± 5	**	**
0	±12	±8	±20	±10	**	**

* Device to be initially adjusted within these limits.

** Limits cannot be set on account of the varying relationship between values of useful and auxiliary burdens.

θ_c = phase-angle error of current transformer (positive if leading)

If the per cent error is a positive quantity, the distance measured will be less than the actual distance.

If impedance elements, which are not sensitive to phase angle, are used the error, usually small, can be expressed as:

$$\text{Per cent error} = 100\left(1 - \frac{K_c}{K_p}\right)$$

Appendix

Suggested Material for a Standard for Potential Devices*

There is a need for co-ordinated practice in regard to potential devices and in this appendix there is presented material suitable for standards. It is hoped this will serve as the basis for the formulation of potential-device standards by the Institute.

These suggestions have been prepared by the authors, reviewed by the AIEE relay subcommittee, and are presented here for the purpose of bringing them to the attention of the industry for comment.*

Scope

These standards apply to potential devices for use with coupling capacitors and/or bushings.

Terminology

POTENTIAL DEVICES

The potential device may be defined as a voltage-transforming equipment used in conjunction with a high-voltage bushing or capacitor as an energy source, to provide a low voltage suitable for operating instruments and relays.

Note: The term "coupling-capacitor potential device" may be used to indicate use with coupling capacitors, and "bushing potential device" indicates use with bushings.

RATIO

The ratio of a potential device is the overall ratio of the line-to-ground voltage of the phase to which the device is connected to the secondary voltage of the device. (It is not the turn ratio of transformers used in the network.)

REGULATION

The term "regulation" designates the change in ratio and phase angle when the burden is varied over a specified range (burden regulation) with constant primary voltage; or when the line-to-ground voltage is varied over a specified range (voltage regulation) with constant burden impedance.

Standard Ratio

The standard ratio shall be such as to give standard rated secondary voltages at rated burden when coupling capacitors are used at their rated voltage.

Classification

Potential devices are divided into two types and three accuracy classifications.

In-phase type—zero phase-angle adjustment—two accuracy classes

Out-of-phase type—30 to 150 degrees phase-angle adjustment—one accuracy class

TYPES

In-Phase. The secondary voltage shall be adjustable to be closely in phase and have correct ratio with respect to the line-to-ground voltage of the phase to which the device is connected, and to remain so for all values of applied primary voltage and secondary burden within the rating of the device, subject to the allowable deviations as herein stated.

Out-of-Phase. The secondary voltage shall be adjustable to the desired phase angle and of correct ratio with respect to the line-to-ground voltage of the phase to which the device is connected, subject to the allowable deviation as herein stated.

CLASSES

The accuracy classifications are as follows:

Class *A*—High accuracy, in-phase type

Class *B*—Lower accuracy, in-phase type

Class *C*—Out-of-phase type—for use with coupling capacitors only

Rating

The rating of potential devices shall include

1. Circuit voltage
2. Line-to-neutral voltage
3. Maximum watts output
4. Secondary volts
5. Frequency

Performance Characteristics

The standard characteristics of potential devices shall be based on

(*a*). Rated line-to-ground voltage of the circuit

(*b*). A frequency of 60 cycles

(*c*). Standard secondary voltages

(*d*). Specified burden

For the in-phase devices the performance characteristics shall be based on a unity-power-factor burden.

Rated Watts Output and Standard Secondary Voltages

For coupling-capacitor potential devices the rated watts output and standard secondary voltages shall be:

Class	Main Winding Watts	Main Winding Secondary Volts	Auxiliary Winding Watts	Auxiliary Winding Secondary Volts
A	150	115/66.4	75	115
B	100	115/66.4	50	115
C	75	115		

For bushing potential devices the standard secondary voltages shall be as given in the following table. The rated watts output is dependent upon the standard bushing and is being studied further. It is hoped to reach a conclusion in time to incorporate the results in the adopted standards.

Rated Circuit Voltage (Maximum Kv)	Line-to-Neutral Voltage (Maximum Kv)	Secondary Voltage (Volts)
115	66.4	66.4 or 115
138	79.7	66.4 or 115
161	93.0	66.4 or 115
230	132.0	66.4 or 115
287.5	166.0	66.4 or 115

Performance Specifications

VARIATION IN APPLIED VOLTAGE

The regulation with constant burden impedance for variation in applied voltage shall not be greater than given in table I.

VARIATION IN BURDEN

The regulation with constant primary voltage for variation in burden shall not be greater than given in table II.

Output at 50 and 25 Cycles

The output shall be reduced to 80 per cent for use on 50-cycle circuits. For use on 25-cycle circuits a special design is required.

Deviation of Circuit Voltage

Standard performance characteristics at proportionately reduced output ratings shall be maintained for a reduction in voltage from 5 per cent up to 20 per cent (that is, use of coupling capacitor of the next higher voltage class, in which case the watts output rating shall be reduced 20 per cent and a special design is required).

Power-Factor Correction

Coupling-capacitor potential devices shall be provided with a tapped capacitor in the secondary circuit as follows: class *A*, 115 volt-amperes in 3.75-volt-ampere steps; class *B*, 75 volt-amperes in 5-volt-ampere steps.

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* Publication of this material with this paper authorized by the AIEE committee on protective devices at the Niagara Falls meeting, May 17, 1940.

In class *C* devices this feature is incorporated in the auxiliary burden.

Bushing potential devices shall be provided with tapped capacitor in the secondary circuit to correct the actual burden, assumed to be at 0.60 power factor lagging, to the rated burden at unity power factor, in 5-volt-ampere steps.

Adjustment Range and Steps

The maximum range over which adjustment shall be required and the maximum steps of adjustment shall be:

	Maximum Range of Adjustment		Maximum Tap Steps	
Class of Device	Ratio (Per Cent)	Phase Angle (Deg)	Ratio (Per Cent)	Phase Angle (Deg)
A	±5	±5	1	1
B	±5	±5	2	2
C	*	30–150	2	5

* Not specified.

Protective Gap and Grounding Switch

A protective gap set at not less than twice the maximum steady-state operating tap voltage shall be provided. A switch shall be provided for grounding the device primary.

Overvoltage Operation

Devices shall be capable of momentary operation with rated burden at circuit overvoltages not exceeding two times normal line-to-neutral voltage without becoming unstable.

Insulation Level

The insulation level of potential devices, and shunt and/or auxiliary capacitors, etc., in the primary circuit, shall be the same as used for instrument transformers of corresponding voltage class. The secondary winding, capacitors, etc., in the secondary circuit shall receive a test of not less than 2,500 volts, in accordance with instrument-transformer practice, proposed ASA Standards for Transformers, C57, page 34, table 7.

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Discussion

Discussion will be found in the 1940 annual TRANSACTIONS volume and in the 1940 SUPPLEMENT to ELECTRICAL ENGINEERING—TRANSACTIONS SECTION.

A Method for Detecting the Ionization Point on Electrical Apparatus

G. E. QUINN
MEMBER AIEE

WHEN any part of the air path included in the insulating circuit of a piece of electrical equipment is overstressed, ionization occurs. The critical value of voltage is a function of the nature of the remainder of the dielectric circuit, the manner of use, surface conditions, the proximity of grounded conducting bodies, the temperature, and in some cases, the relative humidity. Ionization in air causes the formation of small quantities of ozone and oxides of nitrogen. The presence of ozone leads to accelerated aging of insulating materials, especially organic materials such as varnished cambric and paper. The oxides of nitrogen may combine with water to form nitrous and nitric acids which attack cellulose materials and metals. While increasing temperature tends to decrease the voltage at which ionization occurs or to increase its severity at a given voltage, this effect is somewhat offset by the fact that the acids formed are decomposed at temperatures above 80 degrees centigrade. Under certain conditions increased humidity on the other hand not only causes more severe ionization at a given voltage but increases the severity of the action on the cellulose insulation or metal parts which may be present. Nonleaded cables have been known to fail in a few years when subjected to apparently mild ionization under high-humidity conditions. Ionization produces radio interference either by direct radiation or by conduction along the supply circuit or some paralleling circuit.

The minimum voltage at which ionization occurs on a piece of electrical equipment is of interest to both manufacturer and user. The necessity to arrest this condition and not wait until ionization is visible or audible has long been obvious with the result that a number of methods have been proposed for the determination of the voltage at which ionization occurs.[1–8] The method most commonly used at present is probably the one given in the "Report of the Joint Coordination Committee on Radio Interference of the EEI, NEMA, and RMA". Using this method the "radio noise influence voltage" of the equipment under test is measured and it is therefore essentially a radio-interference test. It is, however, satisfactory as a means of detecting the voltage at which ionization occurs on apparatus which can be tested at locations not subject to stray interference. It is often necessary for this work to be conducted under severe stray field conditions and it is the purpose of this paper to describe a simpler and less expensive method which may be used under such circumstances.

In the proposed method a cathode-ray oscilloscope is used to detect the pulses, surges, or damped oscillations introduced into the charging current by ionization

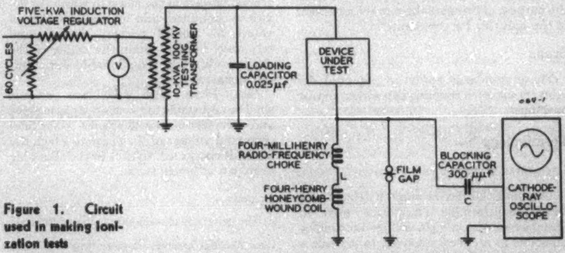
FIVE-KVA INDUCTION VOLTAGE REGULATOR
60 CYCLES
V
10-KVA 100-KV TESTING TRANSFORMER
LOADING CAPACITOR 0.025 μf
DEVICE UNDER TEST
FOUR-MILLIHENRY RADIO-FREQUENCY CHOKE
L
FOUR-HENRY HONEYCOMB-WOUND COIL
FILM GAP
BLOCKING CAPACITOR 300 μμf
C
CATHODE-RAY OSCILLOSCOPE

Figure 1. Circuit used in making ionization tests

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1. For all numbered references, see list at end of paper.

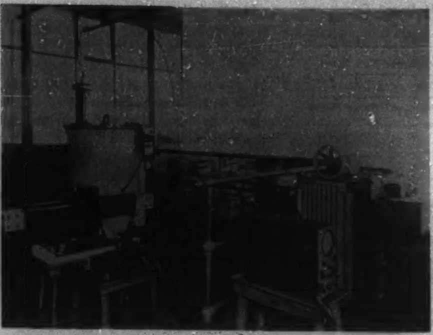

Figure 2. Test setup for determining ionization point on 5,000- to 5-ampere 15,000-volt current transformer

A circuit used with considerable success is shown in figure 1. A reactor L is inserted in the ground lead of the device under test and the voltage drop across this reactor is applied to the oscilloscope. Some time ago Lloyd and Starr[4] applied the drop across a resistor to an oscilloscope in a study of corona loss. While this method served its purpose it is not adequate for ionization-point determination. If this resistor is replaced by a suitable reactive shunt the usefulness of the method is greatly increased. Referring to figure 1 it will be noted that the reactive shunt is made up of a four-millihenry radio-frequency choke in series with a four-henry honeycomb-wound coil. This combination of impedance is used in order to obtain maximum deflection of the ionization components of current on the oscilloscope. The radio-frequency choke has a distributed capacitance of about one micromicrofarad and produces maximum drop for the high-frequency components of the ionization current while the four-henry coil is effective on the lower-frequency components. A blocking capacitor C is used in series with the oscilloscope in order practically to eliminate the test-frequency current appearing on the screen thus making it more readily possible to detect the first evidences of ionization. In addition, the oscilloscope may be operated with constant amplification as the voltage on the apparatus under test is increased.

A loading capacitor is connected in parallel with the device under test to provide a low-impedance path for ionization currents produced. By disconnecting this capacitor a simple means is provided for checking whether the oscilloscope is indicating ionization produced by the device under test or ionization inherent in the test setup. If the indication on the oscilloscope is due to ionization currents produced by the device under test, the indications will decrease. If it is due to a faulty test circuit the indications will increase because the short-circuiting effect of the capacitors will no longer be present.

The oscilloscope is a standard model with self-contained amplifiers and the sensitivity is usually set for 0.005 inch per millivolt deflection of the beam. It has been found that further increase in sensitivity increases the magnitude of the indication only, without appreciably decreasing the voltage at which oscillatory discharges are first detected.

In setting up this equipment, care should be taken to have all high-voltage connections free from sharp corners and edges in order to prevent ionization from these sources. Ground connections should be solidly made and capable of carrying the full short-circuit current of the testing set. In addition, a film gap of low capacitance should be connected as shown in figure 1 for further protection of the operator. Figure 2 is a photograph of a typical test setup, and shows a 15,000-volt 5,000-ampere current transformer under test. A shielded lead is shown connecting the oscilloscope to the reactive shunt. The two coils which make up the reactive shunt L will be noted on the table in front of the current transformer.

In using this setup the voltage applied to the device under test should be increased in about 200-volt increments and the oscilloscope observed at each value for indications of ionization. The sweep circuit of the oscilloscope should be adjusted for the test-circuit frequency or some submultiple thereof in order that the trace of the screen be stationary. The oscillatory discharges which accompany ionization appear superimposed on the charging current.

The definiteness with which the ionization point can be determined using this circuit can be seen in figure 3. The device under test in this case was a 3,000-volt lightning arrester. The marked difference between the oscilloscope traces obtained at 200-volt increments leaves no doubt as to the point of inception of ionization. These traces were obtained using a sensitivity of 0.005 inch per millivolt. Though the ionization point is not always as sharply defined in all pieces of equipment, it has been found that if the sensitivity is increased sufficiently oscilloscope traces similar to those shown on this figure may be obtained on any piece of equipment. The voltage at which ionization is detected at greatly increased sensitivity has been found to be not more than 200 volts below the value noted by a trained observer when the oscilloscope is set for normal sensitivity.

Figure 4 is an oscilloscope trace of ionization encountered at 6,000 volts on a 5,000-to-5-ampere 15,000-volt current transformer. The test circuit and sensitivity were the same as those used in obtaining the traces shown in figure 3.

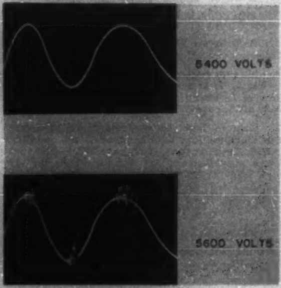

Figure 3. Oscilloscope traces showing the point of inception of ionization in a lightning arrester

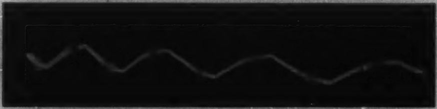

Figure 4. Oscilloscope trace showing ionization at 6,000 volts in a new 5,000- to 5-ampere 15,000-volt station-type current transformer

Table I. Results of Ionization Tests Using "Radio Noise Influence Voltage" Circuit and Cathode-Ray-Oscilloscope Method

Device Number	Type of Equipment		Minimum Voltage at Which Disturbance Was Noted			
			Increasing Voltage		Decreasing Voltage	
			Oscilloscope	Noise Meter	Oscilloscope	Noise Meter
1	Lightning arrester (repaired)		600	600	550	550
			500	500	500	500
			500	500	500	500
		Avg.	550	550	500	500
2	Lightning arrester (repaired)		2,000	2,000	1,750	1,750
			1,750	1,750	1,750	2,000
			2,200	2,200	1,750	1,750
		Avg.	2,000	2,000	1,750	1,850
3	Lightning arrester (repaired)		500	500	Less than 500	2,500
			600	3,000	800	3,000
			1,000	3,000	600	2,600
		Avg.	700	2,200	Less than 700	2,700
4	Current transformer (repaired)		1,300	1,300	800	950
			1,500	1,500	1,700	1,700
			1,800	1,800	2,650	2,650
		Avg.	1,550	1,550	1,700	1,750
5	Current transformer (new)		9,500	9,500	6,100	6,100
			9,000	9,000	4,500	4,500
			9,000	9,000	5,000	4,500
		Avg.	9,200	9,200	5,200	5,050
6	Current transformer (new)		4,700	4,700	4,800	4,800
			5,300	5,300	5,100	5,100
			4,900	4,900	4,700	4,700
		Avg.	5,000	5,000	4,900	4,900

Visual corona on this transformer was not detected until the voltage had been increased to 16,500 volts. Although this transformer is of recent manufacture, it can be seen from the figure that there is unmistakable evidence of ionization at a voltage considerably below the intended operating voltage to ground. This may be taken as evidence of a defect in this transformer since transformers of identical design and similar manufacturing processes, which were tested at the same time, showed no evidence of ionization below operating voltage to ground.

A series of tests comparing the oscilloscope method and the "radio noise influence voltage" method were made recently under normal laboratory conditions. Throughout these tests, intermittent stray disturbances were picked up by the radio-noise meter. At times these became so severe that it was found necessary to delay the tests until the stray interference decreased sufficiently to permit a satisfactory determination of the amount of interference generated by the device under test. In the case of the cathode-ray oscilloscope the external disturbances were identified by the nature of their appearance on the screen and therefore did not materially interfere with the progress of the test. External disturbances appeared on the oscilloscope as random oscillations at no definite point, whereas the high-frequency discharges from the device under test were always repeated at definite points on the screen since the frequency of the sweep circuit was set at a submultiple of the test frequency.

The results of the tests given in table I are reported as the minimum voltage at which the frequency discharges and radio interference were detected for both ascending and descending voltage on each device tested. The results of the test show that with the exception of one measurement obtained on current transformer designated device number 5 the oscilloscope indicated high-frequency discharges at or below the minimum voltage at which interference was noted on the radio-noise meter.

It will be noted that on the lightning arrester designated device number 3 high-frequency discharges were observed on the cathode-ray oscilloscope at voltages considerably below those at which interference was noted by the radio-noise meter. In order to study this further arbitrary values of the amplitude of the discharges were obtained. These extended from 0.05 inch at 200 volts to 3.70 inches at 4,250 volts. The radio-noise meter first detected interference at approximately 3,000 volts. The insensitivity of the radio-noise meter at the low voltages may have been due to a lack of interference at the frequency to which it was tuned.

In all of these tests the radio set was tuned to a frequency of approximately 1,000 kilocycles. It was found on an additional test that the noise measured by the radio-noise meter increased appreciably as the tuning frequency of the receiver was increased from 1,000 to 1,400 kilocycles with constant voltage applied to the device under test. This is contradictory to claims made for this method and indicates that further work should be done along this line.

The cathode-ray-oscilloscope method of detecting the ionization point of equipment has been used on many other types of electrical equipment including sections of bus, ground and test switches, disconnect switches, cables, porcelain pin-type insulators, and distribution transformers. All this work has served to illustrate the importance of a standard method for determining the ionization point of equipment and the desirability of treating this as the basic problem rather than using the secondary approach of radio noise.

While it is admitted that further work needs to be done in connection with the determination of ionization voltages, it would appear that the following conclusions may be drawn:

1. A cathode-ray oscilloscope used in connection with an appropriate air-core reactor will detect high-frequency discharges at or below the minimum voltage at which radio interference may be noted by the "radio noise influence voltage" method.

2. Since ionization when continued over a long period of time might have a deteriorating effect on the apparatus under test, it is important to know the minimum voltage at which it occurs.

3. The oscilloscope method is simple and satisfactory and is not affected by stray fields.

4. The oscilloscope method is dependable as a means of detecting disturbances over a very wide band of frequencies.

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Discussion

Discussion will be found in the 1940 annual Transactions volume and in the 1940 Supplement to Electrical Engineering—Transactions Section.

A New Measuring Instrument for Direct Current

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MEMBER AIEE

A. J. CORSON
ASSOCIATE AIEE

Synopsis: This paper describes the design of a new d-c measuring instrument of the permanent-magnet moving-coil type. Alnico is utilized as a magnet alloy and the design factors involved in the application of this material to instrument design are discussed. A moving system for use with the Alnico magnet system is described and the resulting instrument performance summarized.

RECENT advances in the development of alloys for permanent magnets have been of particular interest to the instrument designer, who utilizes these materials both in d-c instruments and in the damping systems of a-c instruments. While it is not proposed to describe in any detail the new aluminum-nickel-cobalt alloys, it may be stated that their distinguishing characteristics are high values of coercive force and available energy, as shown in figure 1. In instrument application, these qualities result in a magnet of short length and relatively high gap flux density. The use of one such material, an alloy of approximately 10 per cent Al, 17 per cent Ni, 13 per cent Co, and 6 per cent Cu, and remainder Fe, termed Alnico II, in instrument design is herein described. The instrument under consideration is of the permanent-magnet moving-coil type utilized primarily for direct current and voltage measurement, and the design is treated in some detail as very little material of this character has appeared in Institute publications.

The Magnetic Circuit

In permanent-magnet moving-coil instruments of conventional type, the magnet is either of horseshoe shape or U-shaped with soft-iron pole pieces. This construction is probably the most efficient that can be made for magnets of materials requiring a large ratio of length to diameter. When Alnico is used in a construction of this kind, the horseshoe becomes short and thick with the result that flux paths are of widely different lengths. Referring to figure 2, it is obvious that if *b* represents the correct length of the magnet for the given air gap, paths *c* and *a* are respectively too short and too long. As a result, the performance will not be equivalent to that of a magnet of equal weight and uniform section in which the entire magnetic flux passes through a path of length *b*.

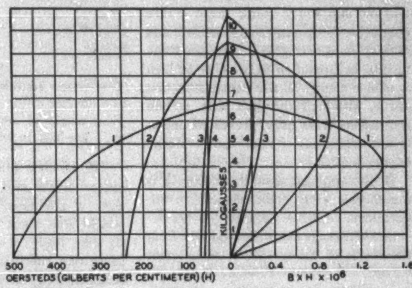

Figure 1. Approximate demagnetization and available-energy curves on several permanent-magnet materials

1—Alnico II alloy

2—Thirty-six per cent cobalt magnet steel

3—Tungsten magnet steel

4—Chromium magnet steel

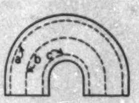

Figure 2. U-shaped magnet of approximate length-to-breadth ratio required for Alnico II

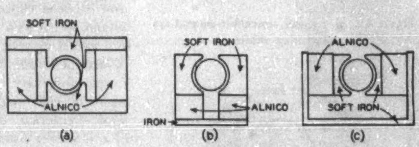

Figure 3. Alnico-magnet systems using soft-iron pole pieces

Perhaps the most obvious method of obtaining this result is by the use of short bars of Alnico in conjunction with bars and pole pieces of soft iron. A number of possible constructions of this type are shown in figure 3. These designs have a disadvantage, however, in that the leakage flux tends to be large, due to the large area of the soft-iron pole pieces. This leakage flux, aside from diminishing the air-gap flux, makes it necessary to shield instruments which are to be mounted close together, or near any magnetic material. The construction shown in *c* is least objectionable in this respect.

A condition of minimum leakage is obtained when the maximum magnetic potential is restricted to as small an area as possible immediately adjacent to the magnet pole face. This condition makes it necessary to have a soft-iron pole piece of minimum leakage area. Since the function of the soft-iron pole piece is to distribute the flux uniformly along the air gap, a condition essential to a uniform instrument scale, it is evident that the thickness may be reduced according to the amount of flux to be distributed. Assuming a uniform magnetic material, if the magnet is designed to give a uniform magnetic potential along the pole face no soft-iron pole piece will be necessary.

Referring to figure 4, it is seen that this condition of uniform magnetic potential is obtained by using a large number of bar magnets of equal lengths radially disposed about the core. This, however, is not a practicable method of construction and a similar result may be obtained by the use of sector-shaped magnets as shown in *b*. Reasonably uniform flux distribution may be obtained without using soft-iron pole pieces on magnets of

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1. For all numbered references, see list at end of paper.

this shape, but it is advisable to use thin pole pieces attached to the face of the magnet since there may be slight fissures or inclusions in the permanent-magnet material. It is also difficult to magnetize these magnets in a perfectly radial manner. The flux tends to concentrate at the pole tips when they are magnetized as shown in figure 5, which method has been found to give the most nearly radial direction of magnetization.

Returning now to figure 4*b*, it will be noted that the area of cross section of the magnet varies in the radial direction, and that for any given cross section the area is roughly proportional to the sum of the useful flux in the gap and the leakage flux between the given section and the gap. The sector shape is thus conducive to operation at a uniform flux density in the magnet. This condition results in operation at a point at high available energy on the demagnetization curve, although the fact that certain dimensions were fixed, prevented operation at the point of maximum available energy.

In the application of this design it is found that true sector shapes are unnecessary. This is because (*a*) the increase in leakage flux with departure from the center of the system is not as great as the increase in magnet section thus obtained, (*b*) the outer corners of the sector (shaded area in figure 6) are partially ineffective due to difficulties in magnetization. The magnet may then be cut back as shown in figure 6 and the shaded area omitted, without appreciable loss in useful flux.

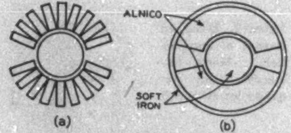


Figure 4. Elementary concentric-magnet system using Alnico

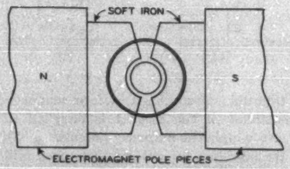


Figure 5. Apparatus for magnetization of concentric-magnet system

The Moving System

The design of a moving system (illustrated in figure 7) for use with concentric Alnico magnets does not differ in principle from conventional constructions, the theory of which is well known.[1] Several details of interest will, however, be described.

The general objective in the design of this part was a maximum mechanical factor of merit and speed of response at a definite current value. It has been shown[2,3] that the mechanical factor of merit (an index of the freedom from frictional errors) of an indicating system with vertical shaft and *V*-type bearing is

$$F = \frac{T}{W^{1.5}}$$

where

T = system restoring torque per unit deflection

W = weight of moving system including one-half the weight of the springs

The obvious function of the moving system is the movement of the pointer over a graduated scale. The pointer weight was, then, considered analogous to the "load" of other devices in the sense that the design originated at this member. To these values were added the weight of the balancing device, pivots, pivot supports, etc., all of which values depend upon the pointer weight, and the summation of which is the total fixed "load." It is obvious that this value must be kept at an absolute minimum consistent with mechanical durability, and overload capacity. The proper relationship between the fixed "load" and coil weight may be derived on the basis of current sensitivity in which case the torque will vary directly as the coil turns or coil weight. To satisfy the $T/W^{1.5}$ criterion, this leads to a value of coil weight equal to twice the fixed weight for a maximum factor of merit at a definite value of current.

It is of interest to note that the microwatt input for a given design (fixed winding space, constant space factor, flux density and torque) is theoretically independent of the current rating. Hence the coil resistance will vary approximately as the inverse square of the coil current and, without readjustment of other design constants, this resistance would achieve very high values for current values of a few microamperes and would involve impossibly small wire sizes. It is, therefore, advisable to lay out the basic design with a very liberal factor of merit to avoid impairment of accuracy when this factor is necessarily reduced as in microammeters. This procedure is facilitated by designing for as high a gap flux density as practicable, in which connection the high gap density obtainable with Alnico is of particular value.

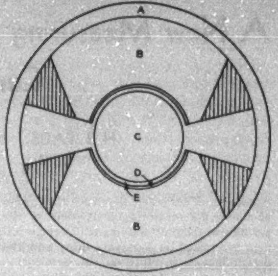


Figure 6. Concentric-magnet system (final form)

A—Soft-steel ring
B—Alnico magnets
C—Soft-steel core
D—Thin steel pole piece
E—Air gap for moving coil

Considering next the design of the moving system in relation to the speed

Table I. Constants of a Concentric-Magnet Millivoltmeter

Accuracy Rating, 0.5 Per Cent of Full-Scale Reading

I.	The magnetic circuit	
1.	Total magnet length (two sections) (centimeters)	3.02
2.	Maximum magnet area (at ring) (figure 6) (square centimeters)	9.1
3.	Minimum effective magnet area (at gap) (figure 6) (square centimeters)	4.4
4.	Total magnet flux (saturated) (maxwells)	23,600
5.	Leakage flux (saturated) (maxwells)	12,000
6.	Air-gap flux (saturated) (maxwells)	11,600
7.	(*a*) Air-gap flux density (saturated) (gausses)	2,900
	(*b*) Air-gap flux density (after stabilization) (gausses)	2,600
8.	Torque per ampere turn (millimeter-grams)	8
II.	The moving system	
1.	Angular deflection (degrees)	90
2.	Pointer length (centimeters)	7.0
3.	Scale length (centimeters)	10.4
4.	Fixed weight (pointer, pivot supports, etc.) (grams)	0.25
5.	Variable weight (winding and insulation) (grams)	0.50
6.	Total weight (grams)	0.75
7.	Torque at full scale (millimeter-grams)	1.00
8.	Mechanical factor of merit ($T/W^{1.5}$)	1.54
9.	Moment of inertia (gram-centimeters^2)	0.68
10.	Free period (seconds)	0.65
11.	Damping constant (dyne-centimeters per radian per second)	9.7
12.	Damping factor	32*
13.	Response time (seconds)	0.95
14.	Input (microwatts)	38.0

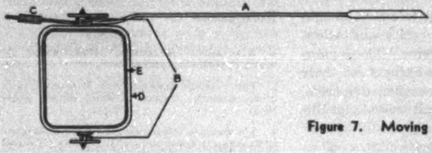
* Corresponding to a decrement of 0.03.

of response, three factors are involved: (*a*) the moment of inertia, (*b*) the actuating torque, and (*c*) the damping torque. The ratio of moment of inertia to actuating torque determines the free period (although the relationship is not direct) hence, a high ratio of torque to moment of inertia is necessary for a low free period, and quick response. The required damping torque then follows from values already determined (that is, actuating torque and moment of inertia) although considerations must be given to the fact that the damping arrangement also adds to the weight and moment of inertia of the moving system.

It was necessary to give special consideration to the means of providing damping torque in this instrument. First, various values of damping torque were required for different applications depending on the actuating torque, and resistance of the circuit in which the instrument was to be used. This necessitated a flexible means of obtaining various damping constants. Secondly, the conventional aluminum frame (in effect a short-circuited turn on the moving system) was found to be inapplicable in a majority of ratings because in combination with the high gap flux density, overdamping resulted, even when the thickness of the frame was reduced to a minimum. The desired result was obtained by the use of damper windings of turns and resistance determined by the characteristics of the instrument involved.

Table II. Performance of Concentric-Magnet Instrument

1. Frictional error (difference between ascending and descending scale readings) (per cent of full scale)	<0.1
2. Ambient temperature coefficient in per cent per degree centigrade	
(*a*) Of magnet	−0.013
(*b*) Of control springs	+0.036
(*c*) Of circuit	−0.046
(*d*) Of instrument	−0.023
3. Instantaneous overload Load which may be applied for one second without causing mechanical damage	60 times normal load
4. Error due to stray fields	
(*a*) 20-gauss field, temporary error (per cent)	2.1
(*b*) Permanent error (per cent)	0.1
5. Magnet stability—per cent change due to normal aging, conditions similar to instrument application, period of 18 months, five magnets:	
Maximum change	−0.15
Minimum change	−0.0
Average change	−0.07



A—Pointer
B—Pivot supports
C—Counterweights
D—Coil
E—Damper winding or shell

Figure 7. Moving system for use with concentric-magnet system

Mechanical Design

The concentric magnet element lends itself to simplicity of mechanical design. The individual magnets *A*, figure 8, are sintered, precision ground, then placed in a soft-steel ring and permanently locked together by the die casting *B*. This die casting includes suitable mounting bosses, scale-plate supports, and grooves for receiving the element subassembly *C*. The complete element with a section cut away to show the core construction is shown in figure 9 and a complete instrument is shown in figure 10.

Numerical Values and Performance Data

Attention is directed to table I, listing significant constants of a typical concentric-magnet instrument, designed in accordance with the principles outlined in the preceding sections. A comparison of the total magnet flux and air-gap flux with maximum and minimum magnet sections will show that the magnet is operated at reasonably uniform density. A comparison of these constants with those of U-shaped magnet designs would be of value but the wide variation in design practice makes such a comparison impracticable. While a leakage of about one-half the total flux seems high, it should be remembered that the leakage flux is taken care of by taper in the magnet section, hence it does not subtract from the effectiveness of the design. So far as the authors' observation has extended, a useful air-gap flux density of 2,600 gausses in indicating instruments has been equalled only by using cobalt-steel magnets of very liberal proportions.

The particular moving system described is intended for use in a millivoltmeter having an accuracy rating of one-half per cent of the full-scale reading. The allowable energy input and hence the mechanical factor of merit of this instrument is relatively large and the response time is short. Where the allowable energy input is more limited, as in microammeters, the mechanical factor of merit will necessarily be less, although it must be consistent with the accuracy rating of the instrument involved.

Considering next the performance data in table II, it is noted that the frictional error is not observable, a statement which can be applied to any satisfactory design with vertical shaft and good bearing construction. In tabulating the temperature coefficients, influences causing an increase in deflection with a temperature rise are called positive, while those values causing a decrease in reading are negative. Various observers[4] have reported temperature coefficients for Alnico of the same order of magnitude as those obtained for chrome magnet steel. The temperature effect is apparently not exactly linear, hence the tabulated value is an approximation over the working range of the instrument. The control spring has, in general, a positive coefficient of greater magnitude than the magnet, resulting in a net positive coefficient for the element. In many applications, this is an advantage in that it partially cancels the negative coefficient of the electrical circuit.

The temporary error caused by stray fields is extremely small due to the self-

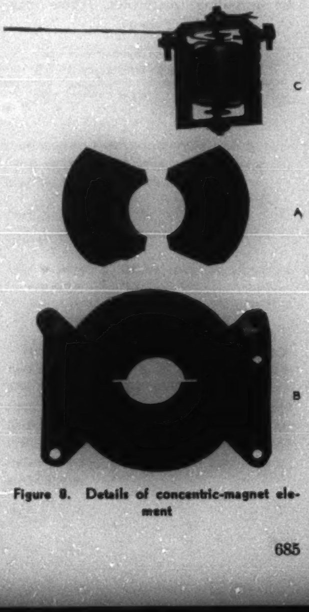


Figure 8. Details of concentric-magnet element

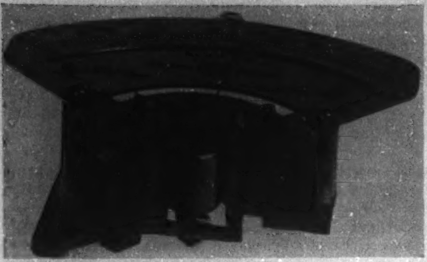


Figure 9 (left). Concentric-magnet-element assembly. Section cut away to show moving coil and core

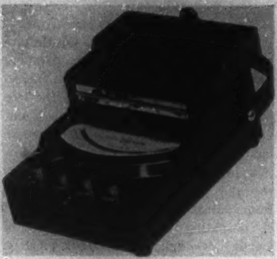


Figure 10 (right). Concentric - magnet voltmeter, completely assembled

shielding effect of the steel ring in the magnet system. In addition, the permanent error due to the application of the field is small since Alnico is relatively insensitive to external demagnetizing influences.

Data relating to the aging of permanent magnets are of limited value unless test conditions simulate service conditions very closely, and both the metallurgical and magnetic history of the material are known. A general review of the data accumulated to date in connection with this development indicates a normal aging under laboratory conditions of 0 to 2 parts in 1,000 during the first year, after which the aging curve flattens out and no noticeable change occurs. This applies to material which has been stabilized by a temperature cycle and a-c demagnetization. The aging curve is not improved, apparently, by annealing treatments. These magnets are very resistant to effects of subsequent temperature cycles and vibration incidental to service. These properties, and the insensitiveness of these magnets to external demagnetizing fields, make Alnico very well suited to this application.

Conclusion

The development of a new d-c instrument element has been described. As a result of careful consideration of the properties of Alnico as a permanent-magnet material, a compact design has resulted, characterized by inherent self-shielding and high gap flux density. These characteristics make possible instruments of small size, having sustained accuracy and high sensitivity. In addition, the element itself is particularly suited to incorporation in apparatus where space is at a premium.

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Discussion

For discussion, see page 1076.

The Rectifier Calculus

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Synopsis: The more involved rectifier-circuit problems, now becoming important because of the increasing magnitude of rectifier loads, can be most readily solved by the rectifying mathematical operator presented in this paper. Even with the more elementary problems, the final numerical computations are more simple in form than is usually obtained when conventional methods are used, since a lesser number of integrals and trigonometric functions are involved. In this paper, application is made to various rectifiers supplied with unbalanced power-line electromotive forces; also some elementary applications of the more simple formulas are included.

INITIAL efforts in rectifier engineering have quite naturally been directed toward mechanical design. But as the proportion of industrial power load carried by rectifiers is becoming large, due to electrolytic plants (electrochemical industries) and other new applications, it is important to be able to solve the more difficult circuit problems without excessive labor.

The rectifier becomes an important unit in a large system. As is well known, large systems may become unbalanced, short-circuited, fall out of synchronism, develop a local fault etc. Although under balanced conditions and sinusoidal electromotive forces, rectifier operation may be computed by conventional methods, under unbalanced conditions, or nonsinusoidal electromotive forces, the conventional methods become extremely intricate and laborious. Particularly in a large system, containing rectifier units, the system electromotive forces and degree of unbalance may be unknown, and since the rectifier units cannot be treated as ordinary three-phase impedances, solution is almost impossible by conventional methods. Only by gradual evolution of a more promising tool of analysis, such as the rectifier calculus presented in this paper, can the solution of such problems be hoped for.

In a restricted sense, rectifiers may be considered nonlinear. But the new theorem will reduce a nonlinear operator to what is for practical purposes a *linear expansion*. The result permits of easy calculation.

Because of the wide scope of the subject, a single paper can cover thoroughly only a single field of application, presenting the *theorems most suited to that field*. In this paper, the calculation of the output of unbalanced rectifiers has been chosen as the field of application.

In order to illustrate, a simple problem of unbalance in the polyphase a-c supply voltage will be considered. For example, in figure 1*a* is shown the no-load voltage output wave of a three-phase rectifier. Sections *A*, *B*, and *C* of this wave, which correspond to the three anode electromotive forces, e_a, e_b, e_c, are entirely dissimilar and unsymmetrical.

Even the intersection points 1, 3, 5, 1 are located at unequal heights and their timing is unsymmetrical. Furthermore the location of these intersection points is unknown.

To determine even the average value of such a wave by ordinary methods is complicated and laborious. It would be necessary to integrate separately each of the sections, and before the integration could be performed, the time of each intersection point, 1, 3, 5, 1 must be determined, as, for example, by finding the phase angle of the difference of the two sine waves concerned.

This new operator mathematics performs these integrations without recourse to the usual integration formulas involving integration limits. Hence, it is termed rectifier calculus.

Product Waves; The Operator Definition

Consider the two waves shown in figure 1*b*. *e* is *any* electromotive-force wave, which can be nonsinusoidal if desired, and $S_{\alpha\beta}$ is a unit rectangular wave extending between the points α and β on the axis. In figure 1*c* the curve $S_{\alpha\beta}e$ is the result of multiplying these two waves together, point by point. All portions of the original wave *e* are chopped off, except those between the limits α and β. Such a wave, denoted $S_{\alpha\beta}e$, will be referred to as a product wave. Each of the three sections *A*, *B*, *C*, in figure 1*a* is such a product wave.

This unbalanced wave, figure 1*a*, may now be expressed as the sum of three product waves, each similar to the wave shown in figure 1*c*:

$$S_{13}e_a+S_{35}e_b+S_{51}e_c \tag{1}$$

Such an expression may be transformed mathematically into new expressions, more convenient to integrate, and to perform other operations. Expression (1) may be abbreviated as follows:

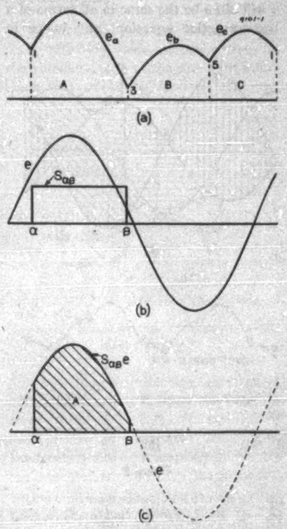

Figure 1

(*a*)—Unbalanced voltage wave
(*b*)—Components of product wave
(*c*)—Product wave

$$S_{135}e \tag{2}$$

where

1,3,5 = chopping limits such as abscissas 1,3,5
e = general letter for the electromotive forces involved, e_a, e_b, e_c

It is sometimes desirable to avoid mentioning the limits 1, 3, 5. These limits are determined by physical conditions such as valve action of the anode, grid delay, etc. If such anode physical properties are denoted as a group by a single index letter *y*, abbreviation may be carried further,

$$S_y^3e \tag{3}$$

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where

S = an operator
3 = number of phases (anodes)
e = letter indicating all phase electromotive forces involved
y = index letter indicating properties of the operator (anode and grid properties)

The advantage of this notation is that y will often be the same in all terms of a transformation equation, and hence in practice can be taken outside of the entire equation. The application to grid-controlled rectifiers and inverters, as well as to mechanical commutators, depends on the y principle, and will be given after the transformation theorem has been developed. In general, any number of phases, p, may be used with the operator S instead of 3, thus $S_y^p e$.

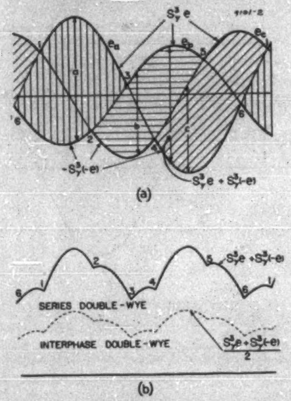

Figure 2

(*a*)—Graph of transformation theorem. $a = S_y^1 \times (e_a - e_b)$, $b = S_y^1(e_b - e_c)$, and $c = S_y^1(e_c - e_a)$

(*b*)—Resulting output voltage waves

Transformation Theorem

It may be proved by algebraic expansion (given in appendix) that,

$$S_y^3 e + S_y^3(-e) = S_y^1(e_a - e_b) + S_y^1(e_b - e_c) + S_y^1(e_c - e_a) \quad (4)$$

where

$S_y^3 e$ is the previous operator expression (3)

e_a, e_b, e_c are the anode electromotive forces of $S_y^3 e$

(Note: The form of this equation has been carefully selected to make impossible writing it for the general case of an odd total number of anodes, except by slight restrictions taken up in detail later in the paper.)

In figure 2*a* the waves involved in this equation are illustrated. The three-anode heavy wave $S_y^3 e$ is the same wave as shown in figure 1*a* and is the first term on the left-hand side of equation 4. The other three-anode heavy wave, 6-2-4-6, etc., is similarly constructed from the bottoms of the three electromotive-force waves e_a, e_b, e_c. It is denoted $-S_y^3(-e)$, since the electromotive forces are reversed, and it is plotted downward. If plotted upward, it would be the second term in equation 4, $S_y^3(-e)$.

The two terms $S_y^3 e$ and $S_y^3(-e)$ are characteristic of the well-known double-wye circuits in rectifier engineering, which involve two three-anode groups, *F* and *G*, figure 3*a*, phase displaced 180 degrees from each other. $S_y^3 e$ represents the output electromotive force of group *F*, and the other group *G*, with electromotive forces reversed, is naturally represented by $S_y^3(-e)$.

In an interphase double-wye (figure 3*a*) the output electromotive forces $S_y^3 e$ and $S_y^3(-e)$ of the two three-anode groups are paralleled through the interphase reactor, which averages them, giving one-half their sum, that is, one-half the quantity $S_y^3 e + S_y^3(-e)$. Both the sum and one-half the sum are shown as waves in figure 2*b*. Note there are six impulses per cycle.

Consider now the first right-hand term $S_y^1(e_a - e_b)$ in equation 4. e_a and e_b are indicated on figure 2*a*, and $e_a - e_b$ is the vertical distance *a*, between them. The exponent 1 on the operator S indicates a single impulse only, due to the single electromotive force involved, $(e_a - e_b)$. The index y indicates that the impulse is formed and controlled by an anode having the same physical properties as the anodes used on the left-hand side of the equation 4. As drawn in figure 2*a* for simplicity, the anodes of S_y^3, etc., have merely valve action, no delay being introduced by grid control or difficulty of anode pickup. $S_y^1(e_a - e_b)$ or *a* in figure 2*a*, therefore, will have in this case half-wave rectification (described below), since there is only one anode. From the left-hand point 6 to point 3, $e_a - e_b$ is positive, and $S_y^1(e_a - e_b)$ has the value $e_a - e_b$, indicated by *a* in the vertically shaded area. Elsewhere the term is zero, that is beyond point 3, because the single anode of S_y^1 cuts off. If sine waves are used, $S_y^1(e_a - e_b)$ is the positive half of the sine wave $e_a - e_b$.

Such half-waves as $S_y^1(e_a - e_b)$ are extremely easy to calculate even with unbalanced electromotive forces, and nonsinusoidal electromotive forces. For example, a half-sine wave has an average value equal to the peak value divided by π. With half-nonsinusoidal waves, the ratio of average to rms or peak value may be the same for all terms, and can be measured by single-phase instruments applied to the input alternating current.

The three half-wave terms on the right-hand side of equation 4 are indicated *a*, *b*, *c* in figure 2*a*. The boundaries of *a*, *b*, *c* are indicated by shading; there is no overlapping.

a extends from 6 to 3 only
b extends from 2 to 5 only
c extends from 4 to 1 only

It may be seen by inspection that the sum of the heights *a*, *b*, *c* is always equal to the height between the two major waves $S_y^3 e$ and $-S_y^3(-e)$. Hence the transformation theorem is made visible in distinct waves.

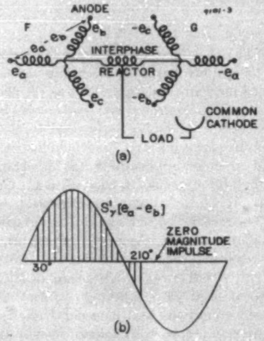

Figure 3

(*a*)—Ordinary double-wye circuit

(*b*)—S_y^1 term with 30-degree grid control

Other S_y^1 terms are similar, shifted to another phase of circuit

Numerical Application

Application will be best explained by an example of an unbalanced rectifier, with commutation overlap.

Example 1a. A double-wye rectifier is supplied with sinusoidal but unequal a-c voltages of such value that $e_a - e_b = 1{,}000$ rms volts, $e_b - e_c = 1{,}500$, $e_c - e_a = 900$. $e_a - e_b$ is the between-anode voltage, figure 3*a*, etc. Determine first the no-load voltage, assuming 20 volts arc drop, and assuming the interphase reactor can be properly excited.

Solution

$$\tfrac{1}{2}[S_y^3e+S_y^3(-e)]=\frac{\sqrt{2}}{2\pi}\times(1{,}000+1{,}500+900)-20 = 745 \text{ volts (average or d-c value)} \quad (5)$$

Explanation. The factor $^1/_2$ is inserted because with an interphase transformer, the two wyes supply the load in parallel. The $\sqrt{2}$ gives the peak value and the $1/\pi$ the average value. The three terms are added as required by equation 4. The arc drop, 20, is subtracted from every electromotive force, hence is conveniently left until last.

Note particularly that the intersection points of the waves (1, 2, 3, 4, 5, 6 in figure 2*a*) have not been determined, and neither is it necessary to integrate each section of wave, as it would be by the old methods, which are quite laborious for a problem of this type.

Regulation

Example 1b. Now determine the voltage regulation due to reactances in the anode and associated circuits in the rectifier of example 1*a*. The commutation reactances are $X_{ab}=0.1$ ohm, $X_{bc}=0.2$ ohm, and $X_{bc}=0.15$ ohm. The load current $2I=200$ amperes, and is well smoothed.

Solution

$$\tfrac{1}{2}[S_y^3e+S_y^3(-e)]=\frac{1}{4\pi}[0.1+0.2+0.15]100 =\frac{45}{4\pi} \text{ average or direct volts} \quad (6)$$

Explanation. The rectifier calculus may be used with nonsinusoidal electromotive forces and hence with reactance drops due to the nonsinusoidal currents. The average reactance voltage for one term on the right-hand side of equation 4 is

$$S_y^1(e_a-e_b)=\text{inductance}\times\text{average}\frac{di}{dt} =\frac{X_{ab}}{2\pi f}\times fI=\frac{IX_{ab}}{2\pi}=\frac{0.1}{2\pi}100 \text{ direct volts} \quad (6a)$$

where

I = peak current = 100
f = frequency
X_{ab} is measured between anodes

In this particular example, the two wyes, Sy^3e and $Sy^3(-e)$ deliver equal average voltages, and hence in spite of even harmonics present the interphase transformer operates, and the equation holds.

Note in example 1*b* that equation 4 can be successfully applied to regulation; with balanced rectifiers, it yields the conventional regulation formulas (see general theorem).

Grid Delay

In example 1*a* and figure 2*a* there is no grid delay. But the *y* in equation 4 permits the use of any physical system. To illustrate, assume a grid delay of 30 degrees on each anode in example 1*a*. That is the anode will not fire until 30 degrees after normal time. The grid delay applies also to the right-hand side of equation 4, so that from figure 3*b*,

$$S_y^1(e_a-e_b)=\frac{\sqrt{2}}{\pi}1{,}000\cos 30° \text{ average or direct volts} \quad (7)$$

$(\sqrt{2}/\pi)1{,}000$ is the value without grid delay. The limits must be moved to 30 and 210 degrees, the latter since the second half-cycle (zero anode volts) must be considered delayed also. An apparatus to produce this wave would consist of two anodes with anode voltages e_a-e_b and zero; it is still a half-wave, extending over a 180 degree interval. The average value involves the integral of a sine wave introducing cos 30 degrees. Hence the answer differs from the answer of example 1*a* in that everything except the 20 volts arc drop is multiplied by cos 30 degrees. This result is not surprising since the current lags 30 degrees. It is important to note, however, that the mathematics is still valid. The method can, of course, be applied to inverter circuits, which are special cases of grid delay.

General Theorem

Equation 4 may be extended to include any number of phases. Thus for *p* phases:

$$S_y^pe+S_y^p(-e)=\sum_1^p S_y^1(e_k-e_{k+1}) \quad (8)$$

where

e_k = the electromotive force of the *k* phase
e_{k+1} = the electromotive force of the $k+1$ phase
S_y^1 indicates single impulse (or half-wave) rectification
Σ indicates the sum of *p* terms
p = number of phases

This relation is very interesting since for practical purposes, equation 8 is a linear expansion because it involves Σ and e_k-e_{k+1}. The single-phase operator S_y^1 causes no difficulty because S_y^1 is independent of the number of anodes and has fixed numerical value for a given grid angle, supply voltage wave form, etc. Assuming unbalanced sine waves, grid delay α, unbalanced commutation reactances:

$$\text{average } S_y^pe+S_y^p(-e)= \beta\frac{1}{\pi}\left[(e_k-e_{k+1})_m\cos\alpha-\frac{IX_{ab}}{2}\right] \quad (9)$$

e_k-e_{k+1} = electromotive force between *k*th and $(k+1)$th anodes
m indicates peak value
X_{ab} = *k*th commutation reactance, measured between anodes
I = direct current to be commutated
$\beta=p$ for a balanced rectifier or $\sum_1^p$ for an unbalanced rectifier

Star Rectifiers

The theorem, equation 8, can be applied, with very moderate assumptions to star rectifiers, such as the commercial six-phase fork connection, figure 4. The reason for having any assumptions at all, is that the theorem is in double-wye or full-wave form $S_y^pe+S_y^p(-e)$ instead of merely star or half-wave form S_y^pe. Evidently there is something inherently fundamental in full-wave mathematics, not available in half-wave mathematics. Pure half-wave functions, such as S_y^pe alone, are already in simplest mathematical form and cannot be expanded, but merely denoted by the abbreviation S_y^pe. On the right-hand side of equation 8, each term, $S_y^1(e_k-e_{k+1})$ is already of the simplest form, S_y^pe, where $p=1$ (and of course $e=e_k-e_{k+1}$ etc.). Hence, it seems improbable that S_y^pe itself can be conveniently expanded without moderate assumptions, suitable to unbalanced rectifiers, as well as to normal rectifiers.

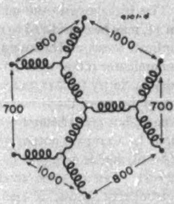

Figure 4. Six-phase fork connection, unbalanced supply

If the star rectifier has an even number of anodes, and the transformer has either interlaced secondary windings or is operating at light load, the rectifier may be considered full-wave, that is opposite anodes have equal and opposite electromotive forces, assuming symmetrical winding. In either case the leakage reactance does not interfere with the assumption.

Under these conditions,

$$S_y^pe=S_y^p(-e) \quad (10)$$

and equation 8 may therefore be applied. To illustrate:

Example 2. A six-phase fork-connection has between-anode electromotive forces (see figure 4) of 800, 1,000, and 700 rms volts, the supply being unbalanced. Determine the no-load output voltage, assuming sine-waves and 20 volts arc drop.

Solution

$$S^6e = {}^1/_2[S^6e + S^6(-e)] = \frac{\sqrt{2}}{\pi}[800+1{,}000+700]-20 \qquad (11)$$

Note π instead of 2π of example 1*a*.

Explanation. The first step is based on equation 10, $p=6$. Next, for the double-wye calculation there are six terms ($p=6$), and the average values of three of them are duplicated, due to full-wave conditions. The $^1/_2$ above in the middle of equation 11, may be considered to remove these duplicate terms, and hence π appears in the denominator rather than 2π.

In order to clarify further and to show the relation between these examples, a special example is given below:

Example 3. Compare output voltages of fork ($p=6$), double wye ($p=3$), single-phase ($p=2$). Assume same anode-neutral electromotive forces, E rms volts, balanced.

Solution

Fork: $$^1/_2[S^6e+S^6(-e)] = \frac{\sqrt{2}}{\pi}3(E) \qquad (12)$$

Double wye: $$^1/_2[S^3e+S^3(-e)] = \frac{\sqrt{2}}{\pi}\,\frac{3}{2}(\sqrt{3}\,E) \qquad (13)$$

Single phase: $$^1/_2[S^2e+S^2(-e)] = \frac{\sqrt{2}}{\pi}(2E) \qquad (14)$$

Explanation

Fork: between-anode electromotive force $=E$
six terms, with $^1/_2$ gives 3

Double-wye: between-anode electromotive force $=\sqrt{3}E$
three terms, with $^1/_2$ gives 3/2

Single phase: between-anode electromotive force $=2E$
two terms, with $^1/_2$ gives unity

Note that the double-wye gives $\sqrt{3}/2$ or cos 30 degrees times the average output voltage of the fork. The fork gives 3/2 the average output voltage of the single-phase rectifier. The peak inverse voltage of the double-wye is also cos 30 degrees times that of the fork. The values obtained by the new method are in agreement with values obtained by conventional methods. In this example the freedom of the rectifier calculus from trigonometric functions is apparent, together with extraordinary simplicity.

Odd Number of Anodes

In the previous section the star rectifiers considered had two or six anodes or in general an even number. But with an odd number of anodes the rectifier is half-wave, since opposite anodes do not exist. It is usually practicable though to compute the corresponding double-wye rectifier, illustrated below.

Assume the d-c output is independent of polarity of the a-c input (absence of input even harmonics).

This is incidentally one of the conditions for proper interphase operation. With this moderate assumption the average voltage is determined on a simple mathematical basis:

$$\text{average } S_y^pe = \text{average } S_y^p(-e) \qquad (15)$$

rather than by equation 10, for which p is even. To illustrate graphically, in figure 2*a*, the upper heavy wave S_y^pe has, by inspection, an equal and opposite average value compared with the lower heavy wave $-S_y^3(-e)$. In other words S_y^3e and $S_y^3(-e)$ have the same average value.

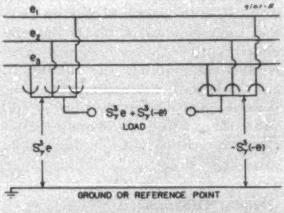

Figure 5. Full-wave three-phase "bridge" circuit

Physically the two wyes of figure 3*a* will have the same d-c output under normal conditions.

The method of application is similar to example 1*a*, since

$$\text{average } S_y^3e = \text{average } {}^1/_2\,[S_y^3e+S_y^3(-e)] \qquad (15a)$$

and the answer to example 1*a* is therefore also the output of a three-anode rectifier with the specified unbalanced electromotive forces.

Therefore, for commercial purposes, the new mathematics is applicable to rectifiers irrespective of the type of circuit used. Some very special circuits may, however, require the use of the equivalent star rectifier, being solved as a star.

Nonsinusoidal Waves

It has been stated that this method may be applied quantitatively to nonsinusoidal electromotive-force waves. Labor is saved, however, only if the nonsinusoidal waves are balanced. This permits various circuits to be compared rapidly without graphical or other wave-form labor, which is certainly not the case if the voltage waves must be integrated separately for each circuit, as with conventional methods. The development will, therefore, be for balanced rectifiers.

IN TERMS OF FORM FACTOR

Replace $\sqrt{2}/\pi$ for sine waves by $^1/_2K$ where K is the form factor (rms/average value of half-cycle loop) and the $^1/_2$ factor is for half-wave rectification (single-anode).

From equation 15

$$\text{average } S_y^pe = \text{average } {}^1/_2[S_y^pe+S_y^p(-e)] \qquad (15b)$$

Note: form factor cannot be used in presence of even harmonics, hence (15*b*) applies, having the same limitation.

Then from equation 9, assuming $\alpha=0$

$$\text{average } S_y^pe = p\left[\frac{(e_k-e_{k+1})rms}{4K}-\frac{IX_{ab}}{2(2\pi)}\right] \qquad (16)$$

Following equation 9, the reactance regulation has been included, to show the method.

Example 4. Compare the output voltages of double-wye and fork connections, for the same rms anode-neutral voltage, E, form factor $K=1.2$.

Solution. (Refer to equation 16)

Fork ($p=6$,

$$e_k-e_{k+1}=E): \frac{6E}{4K} \text{ direct volts, } K=1.2 \qquad (17)$$

Double-wye ($p=3$,

$$e_k-e_{k+1}=\sqrt{3}E): \frac{3\sqrt{3}E}{4K} \text{ direct volts} \qquad (18)$$

Answer

$$\text{ratio} = \frac{2}{\sqrt{3}} \text{ independent of form factor} \qquad (19)$$

IN TERMS OF WAVE FORM

The average value of e_k-e_{k+1}, half-wave rectification, grid delay α (and $180+\alpha$, see figure 3) may be denoted by H.

$$H \text{ replaces } \frac{(e_k-e_{k+1})_m \cos\alpha}{\pi} \qquad (20)$$

Hence equation 9 becomes,

$$\text{average } S_y^pe = p\left[\frac{H}{2}-\frac{IX_{ab}}{4\pi}\right] \qquad (21)$$

If example 4 is repeated, the ratio $2/\sqrt{3}$ will also be found, independent of grid delay α, since H is fixed for a given α, independent of p.

Bridge Rectifiers

A typical bridge circuit is the full-wave three-phase bridge, figure 5. By inspection, this consists of two wye rectifiers, and is in fact a series-double-wye circuit, the series connection replacing the interphase transformer.

Equation 4 or 8 is directly in bridge form, giving the voltage of any series-double-wye circuit. Hence, application does not differ materially from the examples given for the interphase double wye except that the output voltage is twice as great.

There are many involved subjects in the field of rectifiers, inverters, and grid control to which the rectifier calculus has been applied. In this paper, no attempt is made to discuss them, or even to introduce derivatives, integrals, and the corresponding operator treatment which would make the subject far too involved for an introductory paper.

The operator itself and its expansion theorem have been described without calculus, and a sufficient number of simple applications have been given to show that a new field of analysis has been opened to the engineer and mathematician. The ease with which the peculiar waves encountered in rectifiers and allied fields can be represented should recommend it to teachers and beginners as well as to experts. The ability to write a large number of circuit equations in one without introducing a laborious method or notation should be recognized.

A partial list of application fields, in which the subject has been developed, is as follows:

Inductive and capacitive loads on rectifiers
Rectifiers with d-c back electromotive force in load
Regulation of rectifier circuits
Alternating currents of polyphase rectifiers
Grid-controlled rectifiers
Inverters
Polyphase rectifiers, more than six anodes
Commutators, other than d-c machinery
Self-regulated rectifiers
Compounded rectifiers
Differential and transcendental equations

From the definitions, analyses, and applications which have been given in this paper, the following conclusions may be drawn:

1. The method saves much labor in the calculation of unbalanced systems.
2. Calculation of ordinary balanced systems is rapid. The method is, therefore, of general application.
3. Commutation overlap, grid delay, arc drop, and choice of circuit do not invalidate the method or cause inconvenience.
4. The method demonstrates the general linearity of rectifier mathematics, when expressed in terms of between-anode electromotive forces, which are more significant than anode-neutral electromotive forces.
5. By means of (4), all trigonometric functions are eliminated from fundamental formulas.
6. The operator does not assume a particular physical application such as grid-controlled rectifiers and may conceivably be applied to new systems.
7. A wave-form assumption is generally necessary only for a rectifier whose total number of anodes is odd.
8. It is easy to interpret circuits into equations and vice versa.
9. Methods of solution are rational, as distinguished from ingenious devices.
10. The method is inherently suited to multiple discontinuities. Some of the contemporary methods are primarily intended for single discontinuities only or for very simple combinations of discontinuities.

Appendix

Proof of Transformation Theorem

In the text, rectangular waves are used as factors in defining the operator. Properties of rectangular waves will be utilized in the proof.

By inspection of figure 6, the rectangular wave S_{14} is equal to the sum of sections S_{12}, S_{23}, S_{34} thus

$$S_{14}=S_{12}+S_{23}+S_{34} \qquad (22)$$

Now the rectified voltage term $S_y{}^1(e_a-e_b)$ may be expanded by means of the operator definition (equation 1) into a single product wave such as, $S_{14}(e_a-e_b)$. Then on substituting the value of S_{14} further expansion takes place.

$$S_y{}^1(e_a-e_b)=S_{14}(e_a-e_b)=(S_{12}+S_{23}+S_{34})(e_a-e_b) \qquad (23)$$

Similarly:

$$S_y{}^1(e_b-e_c)=(S_{34}+S_{45}+S_{56})(e_b-e_c) \qquad 24)$$

$$S_y{}^1(e_c-e_a)=(S_{56}+S_{61}+S_{12})(e_c-e_a) \qquad (25)$$

On adding the left-hand sides in order to build up equation 4, some terms on the right-hand side cancel, and the remainder may be grouped:

$$\begin{aligned}S_y{}^1(e_a-e_b)+S_y{}^1(e_b-e_c)+S_y{}^1(e_c-e_a) = {}&(S_{23}+S_{34})e_a+(S_{12}+S_{23})(-e_b)+\\&(S_{45}+S_{56})e_b+(S_{34}+S_{45})(-e_c)+\\&(S_{61}+S_{12})e_c+(S_{56}+S_{61})(-e_a) \qquad (26)\end{aligned}$$

Combining according to the principle of figure 6, $(S_{23}+S_{34})=S_{24}$, etc., the right-hand side may be abbreviated:

$$\begin{aligned}&S_{24}e_a+S_{51}(-e_a)+\\&S_{46}e_b+S_{13}(-e_b)+\\&S_{62}e_c+S_{35}(-e_c) \qquad (27)\end{aligned}$$

The vertical columns have the form, $S_{246}e$, etc., of one of the stages in the operator definition (see expressions 1 and 2). Therefore

$$S_y{}^1(e_a-e_b)+S_y{}^1(e_b-e_c)+S_y{}^1(e_c-e_a)=S_{246}e+S_{513}(-e)=S_y{}^2e+S_y{}^2(-e) \qquad (28)$$

The use of y on the right-hand side of the equation depends on the fact that the anode properties (value action, etc.) are referred always to the between-anode voltages, which are the same, e_a-e_b, etc., on both sides of the equation.

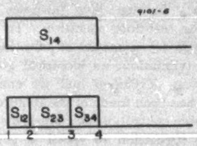

Figure 6. Rectangular wave expansion

This proof assumes that the electromotive forces are lettered in actual firing order. Should electromotive forces be such that firing order is upset, the lettering should be changed accordingly.

Discussion

For discussion, see page 1073.

Recent Developments in Speed Regulation

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Synopsis: This paper deals with recently developed electrical speed governors in which a system stabilizing influence is incorporated by means of a governor response to time rates of change of speed. It is shown that the response to acceleration constitutes a powerful antihunting means which is anticipatory in action and offsets the effect of time delays in the regulated system. New designs utilizing electrical contacts are described, and a mathematical analysis is given for the determination of the conditions for stability of several typical regulated systems comprising assemblies of rotating electrical machinery.

THE PROBLEM of automatically maintaining constancy of speed in rotating machinery is frequently encountered, aside from prime-mover governing, in such applications as d-c drives for a-c generators whose frequency must be kept unvarying to a high degree of accuracy for signalling purposes or for exacting test-floor operation. To satisfy the need for a means of effecting such speed regulation, an electrical governor has been developed and an analytical study has been made of the requirements for stable control of the governed system.

The regulation of speed is generally effected by the application of corrective torques whose magnitude and direction, as well as duration of application, are controlled by a mechanism responsive to physical quantities which are affected by changes in speed. The following are the most important of the quantities which may be utilized for control of speed:

1. Position with respect to a member moving at the desired speed.
2. Viscosity forces due to a velocity gradient in fluid between moving and stationary parts.
3. Electromagnetic forces between moving and stationary parts.
4. The generated voltage of a generator.
5. The frequency of a voltage generator.
6. Centrifugal forces, whether acting on a solid mass or on a fluid mass in a pump system.
7. "Coriolis" forces, as in a precessing gyroscope, for controlling slow speeds too low for a directly connected governor of any other type.

Systems utilizing numbers 1 and 5 are not strictly speed-regulated systems, but rather position regulated because they maintain perfect synchronism with respect to a standard of reference. The types of regulator to be considered in this article are those in which the speed approaches perfect constancy, but still small percentage deviations are tolerated; in other words, perfect phase accuracy is not expected.

Whenever the corrective torques are subject to time delays, that is, whenever time lags occur between the initiation of a corrective stimulus and the final application of corrective torques, then, to secure good accuracy with stability of regulation, the governor must respond not only to a change in speed, but also to the time derivatives of this change, and notably to the first rate of change of speed. The mathematical analysis described here shows that in a speed-regulating system, a governor response to acceleration constitutes a powerful antihunting means which greatly extends the range of regulator amplification giving stable operation. The physical quantities available for sensing acceleration are:

(*a*). The force of acceleration of a mass.

(*b*). The velocity of change in length of a spring that is acted upon by forces proportional to the regulated speed.

(*c*). The voltage drop across an inductance when the current through it is proportional to the regulated speed.

(*d*). The current through a capacitor when the voltage across it is proportional to the regulated speed.

Principle of Contact Governing

The newly developed speed regulator functions by controlling an appropriate electric circuit, such as the field of a motor, by means of a movable contact acted upon by both centrifugal and angular acceleration forces.

The use of contacts for speed control had been limited heretofore to the regulation of fractional-horsepower series or shunt motors with moderate load-variation and speed-accuracy requirements. A typical regulator for small-motor application comprises a movable spring-biased contact which is subject to the centrifugal force of its own mass and which makes and breaks a slip-ring-connected field circuit respectively above and below the regulated speed. Such a simple governing element is severely limited, however, in power-handling capabilities and is totally inadequate for applications involving the accurate regulation, over a wide speed range, of machines having large time delays or in systems of cascaded machines controlling large power machinery. The presence of time delays in the corrective cycle will make the regulated system oscillatory, or even self-oscillatory under certain conditions, appearing as hunting if proper measures are not taken to stabilize the system.

To meet the severe regulation requirements imposed by such time delays, the new governor designs incorporate an acceleration response which, since it precedes a response to change in speed, is anticipatory in action and offsets the action of time delays within the regulated system. This is illustrated schematically in figure 1, which shows the principle applied to a governor consisting of one pair of contacts. In figure 1*a*, if the spring-biased contact is confined to motion in a radial direction only, the acceleration component of the force on the movable contact will not change the contact pressure and the response will be only that due to a change in centrifugal force on the contact as a result of change in speed.

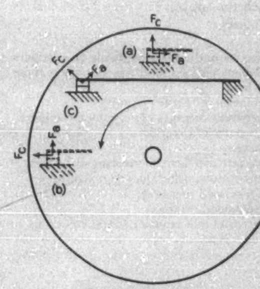

Figure 1. Schematic of simple contact governor

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The writers wish to acknowledge the valuable assistance given by S. Sentipal in working out many details of design in connection with this paper.

Conversely, in figure 1*b*, when the movable contact is confined only to motion in a tangential direction, the contact-pressure variation due to angular acceleration is a maximum and there is no contact response to a change in speed. With the contacts disposed in an intermediate position between these two extremes, as in figure 1*c*, both the centrifugal and the angular acceleration forces on the contact have components along the line of travel of the movable contact, and the resultant change in contact pressure is the algebraic sum of the changes produced by a change in speed and by its time rate of change.

At the regulated speed, the centrifugal force on the movable contact is equal to the biasing-spring force and a hovering condition at the contacts is established whereby a value of regulator resistance is developed sufficient to maintain the desired speed. The regulating action of such contacts when shunting a control resistance may be developed either through intermittent opening and closing or by changes in pressure between the contacts which can occur with practically no contact motion. In the range where no separation of the contacts occurs, but only changes in pressure, the contacts exercise a regulating function by virtue of the resistance-pressure characteristic of contacts subjected to contact pressure. An example of such a characteristic for a carbon-silver pair is given in figure 2, which shows the contact resistance as a function of the force with which the contacts are pressed together. It is apparent from this curve that the full range of resistance variation from an open circuit to a low resistance can be obtained with extremely small changes in contact pressure in the hovering region beginning with the inception of contact. Oscillograms of the contact current actually show that regulation is effected at frequencies of several hundred cycles per second in the hovering region where only contact-pressure changes occur, but no actual separation. This extreme sensitivity of hovering contacts may be used advantageously to secure great accuracy in regulating applications, but it may also be a source of difficulty by making the contacts susceptible to extraneous influences other than those produced by the regulated quantity, if adequate provision is not made for suppressing such influences. For example, if the simplified governor of figure 1*a* were mounted on a horizontally revolving shaft the contacts would be subject to a force of gravity that would reverse its direction along their line of movement once per revolution, producing a maximum change in contact pressure corresponding to twice the weight of the movable contact. The effect of such pressure pulsations, or of contact vibration in general, is to reduce the contact sensitivity. What might be termed the "contact amplification," or the ratio of change in regulating current to change in contact pressure, is reduced because larger changes in steady pressure are then required to produce a given change in regulating current.

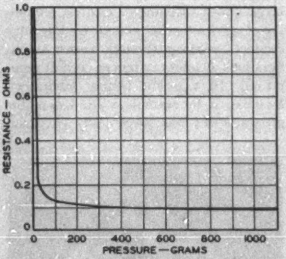


Figure 2. Resistance-pressure curve for silver-carbon contacts

The reduction in contact amplification due to vibration may be calculated quite simply for the case in which actual separation of the contacts occurs in the idealized example shown in figure 3. The "static" current-pressure characteristic of the contact pair of figure 2 when shunting a control resistance is shown in figure 3, with i_1 corresponding to an open contact condition and with i_2 for a closed contact, while the vertical line joining i_1 and i_2 at zero pressure corresponds to the hovering condition represented by the substantially vertical portion of the pressure-resistance characteristic of figure 2.

To simplify the calculation, a massless contact is assumed to be capable of moving through a negligibly small displacement, which is sufficient to make or break the circuit, while being subjected to a steady force P_s and an alternating force $P_a \sin (2\pi t/T)$. The average current as a function of P_s will then depend on the ratio of the time the contacts are open to the time they are closed. Therefore,

$$i_{avg} = \frac{i_1t_1 + i_2t_2}{t_1 + t_2}$$

where $t_1 + t_2$ = period of alternating force = T. To determine t_1 and t_2, we note that the contacts are closed when the resultant force on the movable contact is positive and open when it is negative.

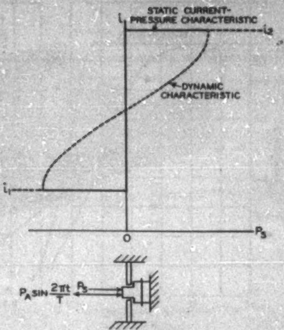


Figure 3. Effect of contact vibration in idealized case

The resultant contact force is

$$P = P_s - P_a \sin \frac{2\pi t}{T}$$

When $P = 0$,

$$\frac{2\pi\,{}_1t_0}{T} = \sin^{-1} \frac{P_s}{P_a}$$

and

$$\pi - \frac{2\pi\,{}_2t_0}{T} = \sin^{-1} \frac{P_s}{P_a}$$

Whence

$$t_1 = {}_2t_0 - {}_1t_0 = \frac{T}{2} - \frac{T}{\pi} \sin^{-1} \frac{P_s}{P_a}$$

$$t_2 = T - t_1 = \frac{T}{2} + \frac{T}{\pi} \sin^{-1} \frac{P_s}{P_a}$$

Therefore

$$i_{avg} = \frac{i_1 + i_2}{2} + \frac{i_2 - i_1}{\pi} \times \sin^{-1} \frac{P_s}{P_a} \quad (\text{for } P_s < P_a)$$

This relation is represented by the dotted line of figure 3 and shows a practically linear variation between current and steady contact pressure at the hovering condition of zero steady pressure. In this idealized case, the contact amplification was thus reduced from the infinite value for no vibration to that represented by the slope of the dotted curve with contact vibration. In an actual case, the conditions under which contact vibration occurs are complicated by inertia and impact effects and it is difficult to derive any quantitative relations for the effect of contact vibration. In the main, however, one may expect that reduced amplification will result from such a condition. This is illustrated in figure 4, which gives the results of measurements in a vibrat-

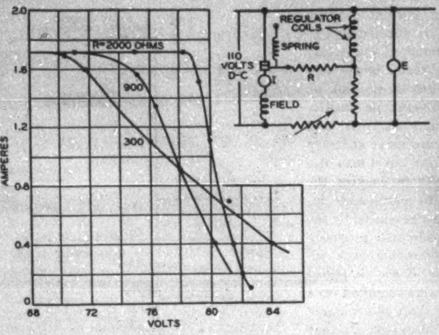


Figure 4. Amplification of vibrating regulator

Figure 5. Two-member governor on submarine signaling motor generator set

ing-contact type of voltage regulator. The curves of field current versus generated voltage are plotted for different values of the resistance *R* which determines the amount of vibration at the contacts. It is seen that as *R* is decreased and the vibration increased the amplification of the contacts, as measured by response in field amperes per volt of voltage deviation, is decreased.

The reduction in contact amplification as a result of vibration must be avoided when high regulation accuracy is desired. In certain cases, however, a lowered contact amplification is a definite advantage that may be purposely introduced. Such is the case, for example, in the governing of prime movers which must operate in parallel on the same load. To insure proper division of load at a given speed between the machines, their respective governors must produce a drooping speed-load characteristic in proper relation to each other, as determined by the required load division. With the contact type of governor, the speed-load characteristic produced may be readily varied to suit any requirement by means of vibration of the contacts.

New Governor Designs

The principal objectives which determined the design of the new governor were the preservation of contact amplification to insure high accuracy and the provision of adequate system stabilizing influences by means of anticipatory response. The design which was worked out to meet these requirements over a wide range of applications is shown in figure 5 as attached to a motor shaft. It consists of a gravity-balanced speed-responsive member which is pivoted on ball bearings about an axis through its center of gravity and perpendicular to the axis of the motor, and an acceleration-responsive member in the form of a flywheel mounted on ball bearings on the motor axis. The speed-sensing member is a cage-like structure supporting a pair of weights which give rise to a centrifugal couple tending to rotate the cage about its pivot axis. This rotation is restrained by a Z-shaped flat spring attached at its ends to the cage and at its center to a stub shaft which is rigidly coupled to the motor shaft. Two pairs of silver contacts are attached to the cage and flywheel in such relation that relative movement between the cage and the flywheel can make and break a slip-ring-connected external circuit. For a given direction of rotation only one pair of contacts acts in a control capacity, while the other pair provides the necessary reaction for the control-contact pressure. It is apparent that, in the absence of angular acceleration, the contact forces are equalized by the freedom of the cage and the flywheel to rotate about their pivot axes. During angular acceleration, this equalization is disturbed by the amount of torque required to accelerate the flywheel, and this torque is produced by a decrease in force between one pair of contacts and a corresponding increase between the other pair. This dependence of contact pressure on the cage position and on the flywheel acceleration prevails also at the regulated speed when a hovering condition is established by equality between the centrifugal couple on the cage and the spring torque.

When the direction of machine rotation is reversed, the effect of flywheel acceleration on contact pressure must also be reversed in order to maintain the proper anticipatory action. In the two-member inertia governor discussed above, a change in rotation is made possible simply by interchanging the control and reaction contact functions by means of a switch. The regulated speed may be changed by adjusting the amount of initial spring torque on the cage by means of a set-screw.

Practically unlimited contact life may be secured by keeping the contact voltage below the arcing limit of 10 to 12 volts and the current below one ampere. Long life may be secured for voltages above the arcing limit if the current is of the order of 0.1 to 0.2 amperes or less. Where a certain amount of maintenance may be tolerated, contacts may be worked to 25 or more volt-amperes, but naturally with a reduced life. In the control of large power machinery, greatest reliability of service can be secured by low-duty contacts working through power amplifiers such as pilot exciters or current regulators. In a recent testing laboratory installation, for example, a 37.5-kva motor generator set was successfully governed

Figure 6. Recent commercial application of anticipatory governor to five-horsepower motor generator set

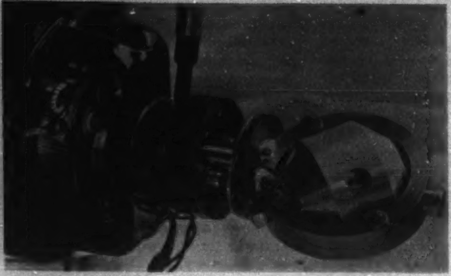


Figure 7. Single-member governor with anticipation

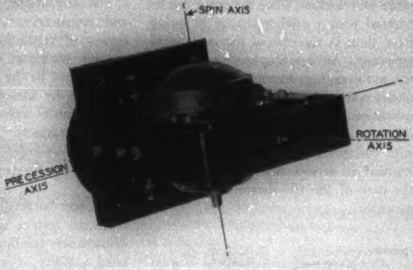


Figure 8. Gyroscopic governor for low-speed control

with a contact-controlled pilot exciter whose output supplied the field power to the motor.

Figure 6 shows a recent application of the two-member governor to a motor generator set for 60-cycle auxiliary power supply. The governor controls the speed of a five horsepower motor through a pilot exciter with an accuracy of two per cent. Smaller machines having little vibration have been governed with accuracies of one per cent or better.

The two-member governor is quite flexible in its adaptability to different operating conditions because both the acceleration and the speed response can be varied independently of each other over a wide range without the necessity of changing such design details as bearing mountings or sizes. It is possible, however, to obtain both speed and acceleration sensing with a single inertia member, although at some sacrifice of application flexibility. A single-member design is shown in figure 7. Its inertia member is a rotor which is pivoted inside a ring frame around an axis inclined at 45 degrees to the axis of rotation of the regulated machine. The rotor is mounted on ball bearings, but is restrained against rotation by a flat torsion spring running through the hollow pivot axis of the rotor. The torsion spring is keyed to the rotor at one end and extends through the ring frame at the other end, where it is fastened to a lever that permits adjustment of the regulated speed by changing the amount of spring setup. The shape of the rotor was calculated to be such that it was in complete dynamic balance to begin with. The requisite amount of unbalance for producing a speed-sensing centrifugal couple could then be introduced either by adding unbalance weights to the rotor or, as is shown in the figure, by removing material by drilling a hole through it.

This design requires but one pair of contacts, but can be used only for one direction of rotation. (A design symmetrically inverted about the motor axis is required for the other direction of rotation.) One contact is stationary on the ring frame, while the other is attached to the movable rotor in such a way that rotation of the rotor about its pivot axis can make and break the control circuit. Angular acceleration of the ring frame will give rise to a rotor inertia torque, which has a component about the pivot axis. Similarly, the centrifugal couple caused by the presence of the hole out of the plane of the ring frame, in an otherwise dynamically balanced rotor, also has a component along the pivot axis. The resultant contact pressure is given by the algebraic sum of the components due to speed and to acceleration.

Another example among the recently developed speed governors is the gyroscopic type, which is shown in figure 8. In the governor types described above the centrifugal force is proportional to the square of the speed. At low speeds large weights are therefore needed to obtain the force required for proper contact operation. Another limitation of the centrifugal governor at low speeds is the magnified influence of gravity disturbances. For example, any mass revolving at a radius of one inch will have a gravity component equal to its centrifugal force at 185 rpm. At low speeds of say 100 rpm or less, it would therefore be practically impossible to obtain any regulation with directly-connected centrifugal governors because of the large weights required and a gravity effect that would be difficult to balance out due to the presence of friction torques. The gyroscopic governor was developed to permit speed regulation in this low speed range with direct coupling to the regulated machine. This is also a contact type of regulator but develops its speed-responsive contact force by means of a gyroscopic torque. The gyroscope is a small single-phase motor of suitable moment of inertia which is mounted with its axis of spin at right angles to the axis of rotation and is supported by ball bearings on its precession axis which is at right angles to both the axis of spin and the axis of machine rotation as shown in figure 8. The precession of the gyroscope is restrained by a loaded spring which holds the contacts open or closed, depending on the control circuit, in the same manner as do the springs used in the centrifugal types. The torque available to overcome this spring is given by the product of the spin inertia of the gyroscope rotor times the spin velocity of the gyroscope times the rotational speed of the regulated machine. From this it is apparent that, although the regulated speed may be low, it is possible to obtain a large precessional torque by making the spin momentum of the gyroscope sufficiently large. Different regulated velocities may be obtained either by changing the precession-spring force or by using a variable-speed motor to control the gyroscope spin velocity.

In comparing the operation of the gyroscopic governor with that of the centrifugal type, it will be noted that the force at the contacts of the former is directly proportional to the regulated speed, while in the latter it is proportional to the square of the speed. The centrifugal governor without anticipation will, therefore, function for either direction of rotation, while it is necessary to provide an additional contact or to reverse the direction of spin if the gyroscopic governor is to regulate for both directions of rotation. The linear speed response of the gyroscopic governor is particularly desirable in applications covering a wide range of velocities, since the range can be covered with a smaller change in spring force than in the case of the centrifugal governor. Antihunting may be added to the gyroscopic governor by making one of the control contacts responsive to acceleration. This may be done by mounting the contact on a mass which revolves with the governor, but which is free to respond to acceleration, as described in the case of the two-member centrifugal governor.

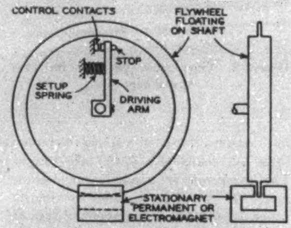

CONTROL CONTACTS
FLYWHEEL FLOATING ON SHAFT
STOP
SETUP SPRING
DRIVING ARM
STATIONARY PERMANENT OR ELECTROMAGNET

Figure 9. Energy-loss type of governor

A fourth type of governor, shown schematically in figure 9, functions by means of speed-responsive forces other than inertia forces. In this, the energy-loss type, the speed-sensitive element consists of a rotating member, such as a flywheel, whose rotation is opposed by forces varying suitably with speed. These forces may originate in eddy-current drag, viscous friction between the rotating member and a fluid, or as a result of imparting kinetic energy to a fluid, as in a centrifugal or axial flow pump. Electromagnetic forces opposing the flywheel rotation may be developed by means of a permanent-magnet field, for example, traversing the rim of the flywheel as shown in figure 9. The flywheel floats on the driving shaft and is driven through an arm rigidly attached to the shaft. The arm carries a contact and is movable through the clearance between a stop and another contact mounted on the flywheel. The speed about which the regulation takes place is determined by the setup in the spring between the arm and the flywheel and by the magnitude of the field traversing the flywheel rim. The latter fact makes it possible to adjust the regulated speed while the machine is rotating simply by changing the position of the magnet relative to the flywheel. The torque for accelerating the flywheel is supplied through the same contact that takes the driving reaction of the forces opposing the flywheel rotation, thus providing a stabilizing acceleration response.

The advantages of design simplicity and compactness together with the accuracy of regulation possible with the contact governors described above can be extended to the field of prime-mover governing by means of electrically operated throttle valves controlled by the governor contacts.

Criteria for System Stability

In order to predict the behavior of a regulated system which is subjected to disturbances tending to change the regulated quantity, it is sufficient in the case of a linear system to determine the transient following a disturbance. The response of a linear system to any disturbance is then completely defined by whether the transient is aperiodic, oscillatory, or self-oscillatory with effectively negative damping. Since the permissible deviations in the regulated quantity are generally small, any system may be considered to be characterized by a linear differential equation in the regulated range and analyzed in this way for stability.

In the case of speed regulators, the system frequency and the amount of positive or negative damping present may thus be obtained from the equilibrium equation between the corrective torques and the opposing torques, due regard being given to their phase relations as affected by the presence of time delays. In the subsequent analysis the effect of load is neglected since it usually does not affect the dynamic characteristics of the system. Even in cases where the load is a function of the speed, the allowable speed deviations are too small to change the load torque appreciably. During correction, therefore, the corrective torques work principally against the inertia of the system in re-establishing the required speed value.

A corrective torque proportional to deviations in angular velocity and to angular acceleration may be expressed as:

$$\begin{aligned}\text{Torque} &= r\Omega + Ip\Omega \\ &= r\Omega\left(1 + \frac{I}{r}p\right) \\ &= r\Omega(1 + T_a p) \qquad (1)\end{aligned}$$

where

Ω = deviation from average velocity in radians per second
r = torque per unit deviation in velocity
I = torque per unit acceleration
T_a = time of anticipation, in seconds
p = usual time derivative operator

The quantity r has the dimensions of a mechanical resistance since it opposes changes in velocity. Moreover, numerically it is a measure of what might be termed the "amplification" of the regulator which initiates the corrective action. All other things being equal, the larger the change in force available at the contacts for a given deviation in speed, the larger will be the amplification r.

The factor of proportionality I between the corrective torque and the angular acceleration has the dimensions of an inertia and has been indicated as such. The symbol I, as used in this sense, does not, however, represent any specific mass inertia in the system, but merely a dimensionally equivalent quantity which may be produced purely electrically, just as may r.

The ratio of I to r has the dimension of time and has been denoted as the time of anticipation T_a. Since both $r\Omega$ and $Ip\Omega$ are caused to appear by virtue of the action of the governor contacts, which contacts function because of contact forces, the time of anticipation may be expressed in terms of governor design constants as the ratio of the change in contact force per unit acceleration to the change in contact force per unit change in regulated velocity, without reference to the circuit producing $r\Omega$ and $Ip\Omega$. That is

F_{Ω_0} = centrifugal force on contact at the regulated speed Ω_0,
F_α = contact force due to an angular acceleration α,

then

$$T_a = \frac{\dfrac{dF_\alpha}{d\alpha}}{\dfrac{dF_{\Omega_0}}{d\Omega_0}} \qquad (1a)$$

For example, in the centrifugal governors described previously,

$$F_\alpha = \frac{I_g\alpha}{h_1}$$

in which

I_g = effective governor flywheel inertia
h_1 = moment arm of contact on which F_α acts
α = angular acceleration

Also,

$$F_{\Omega_0} = mh_2\Omega_0^2$$

where m and h_2 are the effective governor mass and radial distance producing the contact force due to speed and therefore,

$$\frac{dF_\alpha}{d\alpha} = \frac{I_g}{h_1}; \quad \frac{dF_{\Omega_0}}{d\Omega_0} = 2mh_2\Omega_0$$

and

$$T_a = \frac{I_g}{2mh_1h_2\Omega_0} \qquad (1b)$$

A physical insight into the meaning of the anticipation time thus calculated may be gotten by noting that, for the case in which the regulated machine and attached governor are subjected to a suddenly applied constant angular acceleration α, the change in contact force due to the resulting change in speed will become equal to the acceleration contact force only T_a seconds *after* the instant of appearance of α. In other words, the acceleration response in the governor produces a contact force, calling for a correction, T_a seconds *before* a similar call would be produced if the change in contact pressure were only due to a change in speed.

The corrective torque given by equation 1 is subject to time delays in any actual system. In the case of electrically produced torques, these delays are caused by the inductance of field and armature windings which prevents the instantaneous building up of current upon application of voltage. For example, if we consider a simple circuit consisting of an inductance L in series with a resistance R, it is well known that for the case of a suddenly applied voltage E to such a circuit the current will ultimately reach a value E/R, rising to within $1/\epsilon$ of this value in a time $T = L/R$. The differential equation governing the rise in current is:

$$Ri + Lpi = E$$

where p is again the time derivative operator.

Solving for the current,

$$i = \frac{E}{R + Lp}$$

This may be written as

$$i = \frac{E}{R(1 + Tp)} \qquad (2)$$

Although equation 2 is written for current, it also represents the character of a torque as influenced by a time delay. It will be noted that the delay time T appears in the denominator of equation 2, in contrast to T_a in equation 1, which appears in the numerator and indicates anticipation. A quantity $(1 + Tp)$ in a differential equation term may therefore be thought of as an operator having mathematically the same character as the quantity $a + jb$ in the theory of a-c vectors, indicating a phase advance or anticipation when appearing in the numerator and a phase lag or delay when appearing in the denominator.

It can be shown in this manner that when the corrective torque is subject both to an anticipation time T_a and to time delays T_1, T_2, T_3, etc., the correct representation for it is

$$\text{Torque} = \frac{r\Omega(1 + T_a p)}{(1 + T_1 p)(1 + T_2 p)(1 + T_3 p)} \cdots \qquad (3)$$

The multiplicative grouping of time delays in the denominator of (3) can be simply demonstrated, for example, for the governor-controlled Ward-Leonard system of figure 10*a*. This system comprises two time delays which act in series so to speak, one delay arising from the inductance of the generator field and the other from the combined effective inductance of the generator armature and the controlled-motor armature. The incremental current and voltage relations of the circuit are as follows:

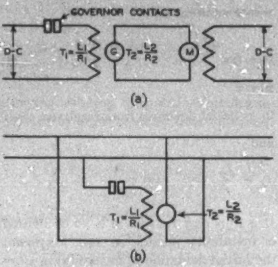


Figure 10. Systems with two time delays

(*a*)—Ward-Leonard system

(*b*)—Shunt motor with field control

$$i_1 = \frac{e_1}{R_1 + pL_1}$$

where

e_1 = variation in generator field voltage E
R_1 = resistance of generator field
L_1 = inductance of generator field

The variation e_2 in the generated voltage due to i_1 is

$$e_2 = Ki_1 = R_2 i_2 + L_2 p i_2$$

where

i_2 = current variation in armature of controlled motor
L_2 = combined inductance of generator and controlled-motor armatures
R_2 = combined resistance of generator and motor armatures

Hence,

$$\frac{i_2(R_2 + pL_2)}{K} = \frac{e_1}{R_1 + pL_1}$$

Or,

$$i_2 = \frac{Ke_1}{(R_1 + pL_1)(R_2 + pL_2)} = \frac{Ce_1}{(1 + T_1 p)(1 + T_2 p)} \qquad (4)$$

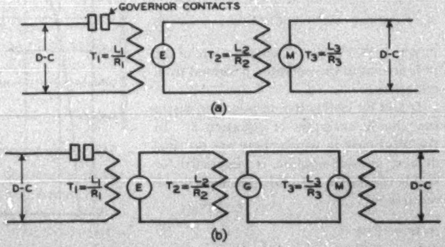
in which

$$T_1 = \frac{L_1}{R_1}; \quad T_2 = \frac{L_2}{R_2}$$

Since the field of the regulated motor is constant, equation 4 also represents the character of the motor torque as influenced by the electrical time delays of the system.

Another system with two delays is shown in figure 10*b*, comprising a shunt motor with field control by means of a contact governor in the motor field circuit. One delay is due to the field inductance while the other arises from armature inductance. The latter is due to the fact that at a given speed a small change in the motor field produces a small change in torque which is at first exactly opposite the required corrective torque, and the corrective torque only attains its required value when the armature IR drop, which is prevented from changing instantaneously by the armature inductance becomes equal to the new difference between the applied voltage and the changed back electromotive force due to the change in field.

When the field of a governed motor is energized by a pilot exciter whose field is regulated by the motor governor, as shown in figure 11*a*, the corrective torque controlling the motor speed is retarded in phase by three time delays. This is



Figures 11a and b. Systems with three time delays

(*a*)—Field control with pilot exciter

(*b*)—Ward-Leonard system with pilot exciter true also for the case of a Ward-Leonard system with a pilot exciter or other current regulator, as shown in figure 11*b*. The circuit of figure 11*c* is also a system with three time delays, but differs from the others in that the pilot exciter is arranged to permit across-the-line starting of the motor. When the circuit is energized, the main motor field is fully excited, and regulation is then effected by the pilot exciter which is mounted on the main motor shaft and produces a counter electromotive force which reduces the motor field to the proper value to establish the desired speed. The currents and voltages in a system comprising any number of such cascaded machines can be shown to be so related, in a manner analogous to the case of the two-delay Ward-Leonard system, as to produce a corrective torque which is mathematically expressed by a multiplicative denominator grouping of the several time delays involved.

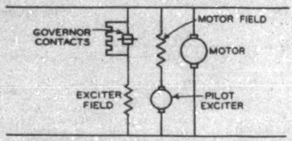


Figure 11c. Governor-controlled motor with pilot exciter for across-line starting

The speed-governing torque of equation 3 will generally be opposed by the inertia torque due to the rotational inertia of the moving system, the effect of load being neglected as explained previously. The differential equation governing the free motion of the regulated system is therefore:

$$I_r p\Omega = -\frac{r(1+T_a p)\Omega}{(1+T_1 p)(1+T_2 p)(1+T_3 p)}\cdots \tag{5}$$

in which I_r = moment of inertia about rotational axis. Its solution is

$$\Omega = C_1\epsilon^{P_1 t} + C_2\epsilon^{P_2 t} + C_3\epsilon^{P_3 t} + \ldots \tag{6}$$

in which $P_1, P_2, P_3 \ldots$ are the roots of the polynomial in the operator p formed from equation 5.

It will be instructive to consider first a few simple examples of equation 5. In the ideal case in which there are no time delays, no anticipation is necessary for stable regulation. In this case equation 5 reduces to

$$I_r p\Omega = -r\Omega \tag{7}$$

whose solution is

$$\Omega = \Omega_1\epsilon^{-\frac{r}{I_r}t} = \quad {}^{-\frac{t}{T_0}} \tag{8}$$

where

Ω_1 = initial deviation from regulated speed

and

$$T_0 = \frac{I_r}{r}$$

It is apparent that there is no oscillation or instability possible in such a system: the initial deviation Ω_1 in speed dies away at a rate determined by the ratio of the rotational inertia to the regulator amplification. As was to be expected, the higher the amplification and the smaller the system inertia, the more quickly will the regulated speed be re-established following a transient disturbance.

When the corrective torque is subject to one time delay T_1, as in a series motor, or a shunt motor with armature control, the equation of regulation becomes:

$$T_0 p\Omega = -\frac{\Omega}{1+T_1 p} \tag{9}$$

Its solution is given, in accordance with equation 6, by the roots of the derived quadratic in p:

$$p^2 + \frac{1}{T_1}p + \frac{1}{T_0 T_1} = 0 \tag{10}$$

This represents a damped free oscillation with a vibration frequency of

$$f = \frac{1}{2\pi}\sqrt{\frac{1}{T_0 T_1} - \frac{1}{4T_1^2}} \text{ cycles per second} \tag{10a}$$

and a fractional decay per cycle of

$$D = 1 - \epsilon^{-\pi\sqrt{\frac{T_0}{T_1}}} \tag{10b}$$

(A fractional decay is defined as shown in figure 12.)

There is, therefore, no instability possible in a linear system characterized by equation 9. With a large delay T_1 or a small T_0, however, the amplitude decay may be so small that in an actual system of this type large free oscillations may be continually excited by transient variations in either the load torque or the driving torque.

By adding an anticipatory response to the governor controlling the corrective torques, the conditions of equation 9 are changed to:

$$T_0 p\Omega = -\frac{\Omega(1+T_a p)}{1+T_1 p} \tag{11}$$

The power of anticipation to neutralize the action of time delays may be well illustrated here by observing that when T_a is made equal to the delay time T_1 the factor $(1 + T_a p)$ just cancels the denominator and equation 11 reduces to equation 7, thus making the system highly damped and nonoscillatory.

In systems with two time delays, like those of figure 10, the equation of motion leads to a third-degree equation in p, with the result that not only is oscillation possible, but the system may also become unstable due to effectively negative damping. The general equation for this case with anticipation is:

$$I_r p\Omega = -\frac{r(1+T_a p)\Omega}{(1+T_1 p)(1+T_2 p)} \tag{12}$$

giving the following polynomial in p:

$$T_0 T_1 T_2 p^3 + T_0(T_1 + T_2)p^2 + (T_0 + T_a)p + 1 = 0 \tag{13}$$

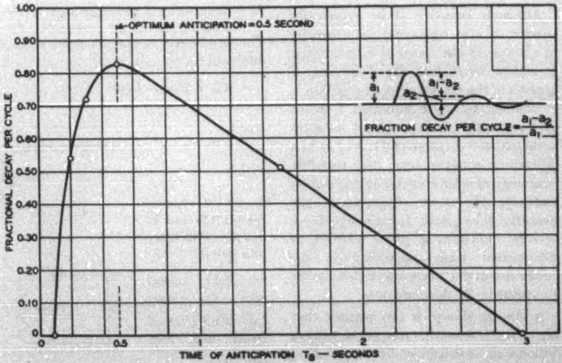


Figure 12. Damping in regulated 37-kva motor generator set

in which $T_0 = I_r/r$ has the dimensions of time.

The roots of (13) will represent a stable solution only if all the coefficients of p are positive and if

$$T_0(T_1 + T_2)(T_0 + T_a) > T_0T_1T_2$$

or

$$T_0 + T_a > \frac{T_1T_2}{T_1 + T_2} \qquad (14)$$

The first condition for stability is obviously satisfied in equation 13. The condition expressed by the inequality (14) forms the basis therefore for the design of the governor and the determination of the system stability limits. T_0 is usually very small when high accuracy is required, hence T_a is the principal stabilizing influence.

It might be supposed by analogy with the case of one time delay discussed previously that the best conditions of regulation would be realized by making the anticipation time cancel one of the two delays, preferably the longer one. That is not the case, however, as can be seen by analyzing (13) for the criteria for optimum damping of the system. When T_0 is small compared to the other time constants and T_a is comparable to T_1 and T_2, then the cubic (13) may be factored approximately into:

$$(T_ap + 1)\left[\frac{T_0T_1T_2}{T_a}p^2 + \frac{T_0}{T_a}\left(T_1 + T_2 - \frac{T_1T_2}{T_a}\right)p + 1\right] = 0 \qquad (15)$$

The quadratic factor of the form $ap^2 + bp + c = 0$, represents the damped oscillation

$$\Omega = \epsilon^{-\Delta t}(A \cos 2\pi ft + B \sin 2\pi ft)$$

in which

$$\Delta = \frac{b}{2a}$$

$$f = \frac{1}{2\pi}\sqrt{\frac{c}{a} - \frac{b^2}{4a^2}} \qquad (16)$$

The fractional decay per cycle is

$$D = 1 - \epsilon^{-\frac{\Delta}{f}}$$

and this is a maximum when Δ/f is a maximum. From (16):

$$\frac{\Delta}{f} = \frac{2\pi}{\sqrt{\frac{4ac}{b^2} - 1}} \qquad (17)$$

The fractional decay is therefore a maximum when $4ac/b^2$ is a minimum. Therefore, the optimum anticipation time for maximum damping is the time that will make $b^2/4ac$ a maximum. This is obtained from

$$\frac{\partial}{\partial T_a}\left(\frac{b^2}{4ac}\right) = 0 \qquad (18)$$

Applying relation (18) to the quadratic (15), the best anticipation time becomes:

$$T_a = \frac{3T_1T_2}{T_1 + T_2} = \frac{3}{\frac{1}{T_1} + \frac{1}{T_2}} \qquad (19)$$

and the best decay factor is

$$\frac{\Delta}{f} = \frac{2\pi}{\sqrt{\frac{9T_aT_1T_2}{T_0(T_1 + T_2)^2} - 1}} = \frac{2\pi}{\sqrt{27\frac{(T_1T_2)^2}{T_0(T_1 + T_2)^3} - 1}} \qquad (20)$$

In order that

$$D \geqq 1 - \frac{1}{\epsilon}, \quad \frac{\Delta}{f} \geqq 1$$

must be true.
Hence,

$$\frac{27(T_1T_2)^2}{T_0(T_1 + T_2)^3} - 1 \leqq 4\pi^2 \leqq 39.4$$

from which

$$T_0 \geqq \frac{0.67(T_1T_2)^2}{(T_1 + T_2)^3} \qquad (20a)$$

For cases where $T_2 \ll T_1$, the best anticipation time is, from (19):

$$T_a = 3T_2 \qquad (20b)$$

and

$$T_0 \geqq 0.67\frac{T_2^2}{T_1} \text{ for } D \geqq 1 - \frac{1}{\epsilon} \qquad (20c)$$

When $T_1 = T_2$, the best anticipation time is

$$T_a = \frac{3}{2}T_2 \qquad (20d)$$

and

$$T_0 \geqq 0.084T_2 \text{ for } D \geqq 1 - \frac{1}{\epsilon} \qquad (20e)$$

By way of comparison, we note that when the anticipation time is made equal to the longer of the two delays, say T_1, the cancellation of $(1 + T_1p)$ in equation 12 reduces it to the form of equation 9. In order then that $D \geqq 1 - 1/\epsilon$, from (10*b*) it is apparent that

$$T_0 \geqq \frac{T_2}{\pi^2} \qquad (20f)$$

The amplification r given by the equality (20*f*) is thus seen to be inferior to the amplification with the optimum T_a, as given by (20*c*) and (20*e*).

Systems with three time delays form an important class since they include the highly useful circuits of figure 11; namely, the field-controlled motor with a pilot exciter and the Ward-Leonard system with the generator field energized by a governor-controlled pilot exciter. Mathematically equivalent conditions exist when the pilot exciter in each case is replaced by any other current regulator having one time delay. The differential equation of free motion for these cases is given by:

$$I_rp\Omega = \frac{-r\Omega(1 + T_ap)}{(1 + T_1p)(1 + T_2p)(1 + T_3p)} \qquad (21)$$

This leads to the following polynomial in p:

$$T_0T_1T_2T_3p^4 + [T_0T_1T_2 + T_0T_3(T_1 + T_2)]p^3 + T_0(T_1 + T_2 + T_3)p^2 + (T_0 + T_a)p + 1 = 0 \qquad (22)$$

A sufficient condition for stability is in this case:

$$T_0(T_0 + T_a)(T_1 + T_2 + T_3)[T_1T_2 + T_3(T_1 + T_2)] > (T_0 + T_a)^2T_1T_2T_3 + T_0[T_1T_2 + T_3(T_1 + T_2)]^2 \qquad (23)$$

The appearance of T_a^2 in the right-hand member of (23) indicates that there is a limit to the anticipation time beyond which the system will be unstable, for by making T_a large enough the inequality (23) will obviously be not satisfied. To determine this limit it is necessary to solve (23) for T_a at the limiting condition of equality. To simplify the expressions, let

$$T_1 = aT_0; \quad T_2 = bT_0; \quad T_3 = cT_0; \quad T_a = dT_0 \qquad (23a)$$

The equality derived from (23) then becomes

$$(1 + d)^2abc - (1 + d)(a + b + c)[ab + c(a + b)] + [ab + c(a + b)]^2 = 0 \qquad (24)$$

This is a quadratic in $(1 + d)$, whose solution is:

$$1 + d = \frac{[ab + c(a + b)]}{2abc}\left[(a + b + c \pm \sqrt{(a + b + c)^2 - 4abc})\right] \qquad (25)$$

A further restriction on the regulating system appears in equation 25. It is apparent that d will be real only when the quantity under the radical is positive. This requires that

$$4abc \leqq (a + b + c)^2 \qquad (26)$$

Or, substituting in (26) the values of a, b, c from (23*a*):

$$T_0 \geqq \frac{4T_1T_2T_3}{(T_1 + T_2 + T_3)^2} \qquad (26a)$$

Hence, the amplification

$$r \leqq \frac{I_r(T_1 + T_2 + T_3)^2}{4T_1T_2T_3} \qquad (26b)$$

Thus, in a three-time-delay system, the time of anticipation and the allowable amplification are both limited. When equations 26 are satisfied, the anticipation time for stability is confined between the limits given by equation 25. That is,

$$1 + d > \frac{[ab + c(a + b)]}{2abc}\left[a + b + c - \sqrt{(a + b + c)^2 - 4abc}\right] \quad (27a)$$

and

$$1 + d < \frac{[ab + c(a + b)]}{2abc}\left[a + b + c + \sqrt{(a + b + c)^2 - 4abc}\right] \quad (27b)$$

The difference between (27*a*) and (27*b*) lies in the sign before the radical.

Since instability occurs at both the limits set by (27*a*) and (27*b*), it is to be expected that a best value of $d\ [=T_a/T_0]$, for which the system damping is maximum, exists between these extremes. It is difficult to find a general expression for this best T_a, however, as that involves factoring the fourth-degree equation 22. In a specific case, when numerical constants are available, the optimum anticipation time can be readily obtained from a point-by-point solution of (22). This was done in a recent application involving the regulation of a 37.5-kva motor generator set through field control by means of a governor-regulated pilot exciter. The pertinent machine constants in this case were:

Exciter field time constant = 0.05 second = T_1
Motor armature time constant = 0.2 second = T_2
Motor field = 0.8 second = T_3
Regulated speed = 1,800 rpm
Full-load motor torque = 146 pounds-feet
Total inertia of rotating parts = 55.4 pounds-inches-seconds² = I_r

Assuming that a regulation accuracy is required which will not permit over two per cent speed change for a variation in load from no load to full load, the necessary amplification becomes:

$$r = \frac{146 \times 12}{0.02 \times 2\pi \times \dfrac{1{,}800}{60}} = 465 \text{ pounds-inches per radian per second}$$

Hence

$$T_0 = \frac{I_r}{r} = \frac{55.4}{465} = 0.12 \text{ second}$$

and

$$a = \frac{T_1}{T_0} = \frac{0.05}{0.12} = 0.417$$

$$b = \frac{T_2}{T_0} = \frac{0.2}{0.12} = 1.67$$

$$c = \frac{T_3}{T_0} = \frac{0.8}{0.12} = 6.67$$

As a first check on the system stability, we note that criterion (26*a*) is satisfied, since

$$0.12 > \frac{4 \times 0.05 \times 0.2 \times 0.8}{(0.05 + 0.2 + 0.8)^2}$$

or

$$0.12 > 0.029$$

Substituting *a*, *b*, and *c* into the relations (27*a*) and (27*b*), we next determine the limits of anticipation time. From (27*a*) we get

$$1 + d < 25.8$$

$$d < 24.8 \left[d = \frac{T_a}{T_0}\right]$$

Hence

$$T_a < 0.12 \times 24.8 = 2.98 \text{ seconds}$$

Similarly, from (27*b*):

$$T_a > 0.094 \text{ second}$$

A point-by-point solution of equation 22 for values of T_a between the limits defined above gives the results shown in figure 12. The system damping, as expressed by the fractional decay per cycle becomes a maximum for an anticipation time of one-half second and is equal to zero for the limiting values of anticipation found above.

Characteristics of Restricted Ionization and Its Relation to Voids in Insulating Materials

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THE phenomena accompanying the formation of corona about cylindrical conductors and the laws governing such corona formation have been under investigation for a number of years, and the relations of such corona loss to voltage gradient, pressure, temperature, and the geometry of the system are now quite completely rationalized. Corona or ionization also occurs in restricted spaces, such as the voids which frequently are formed in dielectrics during their fabrication, and this corona formation has important effects on the behavior of such dielectrics. An example of such restricted corona formation is to be found in high-voltage impregnated-paper cables[1,2,3] within which occluded-gas spaces occur. Although the characteristics presented in this paper are for gaps longer than those ordinarily occurring in cables,[4,5,6] data obtained with cables show that the characteristics differ only quantitatively. The effect of such gaseous films on the deterioration of the insulation is well known. A knowledge of the characteristics of restricted ionization has made it possible to separate the solid-dielectric and the ionization losses in cables.[2,3]

In the investigations described in this paper, a large number of measurements of restricted ionization under various conditions of voltage, pressure, and frequency have been made. From the data so obtained attempt has been made to establish certain rational laws for restricted ionization. The phenomena accompanying restricted ionization are far more complicated than those accompanying corona about a cylindrical conductor and the laws therefore cannot always be reduced to simple forms. Also, this investigation contributes to the knowledge of the phenomena which, in general, accompany the ionization spark-over of gases.

Electric Phenomena in Gases With Uniform Field

When an increasing voltage is applied between two parallel, flat electrodes, with gas as the intervening medium, an arc discharge will occur when the sparking gradient is exceeded. Many complex phenomena occur, however, between the initial application of voltage and the ultimate arc discharge. Although these phenomena have been studied by a number of investigators, there is still considerable doubt as to the exact nature of the phenomena which take place during some parts of the breakdown cycle. This is due to the fact that shortly after cumulative ionization by collision begins, the impedance characteristic of the gas becomes unstable and arc discharge follows almost instantly. The unstable impedance characteristic, combined with the rapidity of the arc discharge, makes it very difficult to determine experimentally the volt-ampere characteristic over a considerable part of the cycle. However, by combining the experimental results of a number of investigators, together with their conclusions, it becomes possible to sketch an approximate volt-ampere characteristic, indicating in a general way the nature of the phenomena, from the initial application of voltage to the ultimate arc discharge.

In any normal gas space there is usually a limited supply of free ions. When a small voltage gradient is applied between parallel, flat electrodes, some of these free ions are withdrawn from the field resulting in a very small current. At some definite gradient, the current becomes practically constant and remains so until the gradient becomes sufficient to produce ionization by collision. When the action becomes cumulative, the current increases to an indefinitely large value with but slight increase in the voltage gradient. This characteristic of the gas has been investigated at low pressures by Townsend, and figure 1 is the same as figure 45, page 266, in his book.[7]

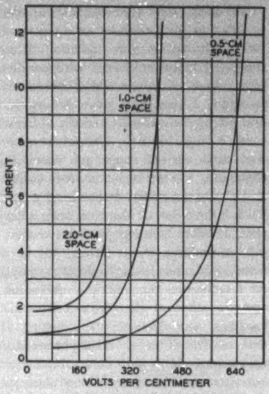

Figure 1. Volt-ampere characteristic at 4.13 millimeters pressure (Townsend)

There is very little in the literature which shows the complete volt-ampere characteristic of a gas. Engel and Steenbeck,[8] however, do present schematically a nearly complete characteristic (figure 86, page 120),[8] in part theoretical, and this is, in slightly modified form, shown in figure 2.

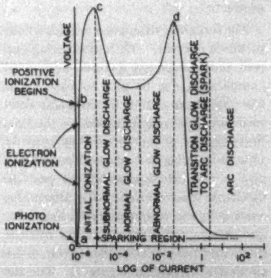

Figure 2. Complete characteristic of self-sustained gas discharge (approximate)

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[1] For all numbered references, see list at end of paper.

Although this characteristic applies to tubes at low pressures, in the general form it should not differ materially from the characteristic obtained with parallel plates and at much higher pressures. In figure 2 current is given as abscissa and voltage as ordinate, and the abscissa scale is logarithmic. The phenomena over each current range are shown in the figure. In the region *oa*, only casual and photoions exist. Ionization by collision is first due to electrons only, since their mobility is much greater than that of the positive ions. Hence, between *a* and *b* ionization is due to electrons only. At *b* ionization by positive ions begins and the slope of the characteristic diminishes, the characteristic reaching a first maximum at *c*. The phenomena which occur from glow to arc discharge are often considered as taking place in the sparking region and this is indicated in figure 2. The authors know of no data showing definitely which of the two maxima, *c* or *d*, is the greater. However, the results obtained by the authors, combined with sparking experiments of Townsend, indicate that *c* is the greater.

It would follow from figure 2 that for an arc to occur between parallel, flat electrodes, the voltage should not only be sufficiently high to initiate ionization by collision, but must exceed the maximum voltage represented by the maximum *c* or that represented by the second maximum *d*, whichever of these is the greater.

As has already been pointed out, the reason that the characteristic of figure 2 has not been completely explored experimentally is the fact that the discharge becomes unstable and cannot be sustained for a sufficient time to permit measurement. There have been attempts to overcome this difficulty by having in series with the discharge a disk made of high-resistance ceramic products.[9]

The foregoing discussion applies wholly to direct current. With alternating current the phenomena become even more complicated due in part to time lag in the ionization and to the irregular wave forms of the current caused by the nonlinear nature of the circuit (see figure 5). Due to time lag in ionization by collision and in the deionization of the gas, the discharge characteristic with alternating current in the region of initial ionization and subnormal glow discharge will undoubtedly have a hysteresis effect similar to that indicated in figure 3. The decreasing values of current require less voltage than the increasing values.

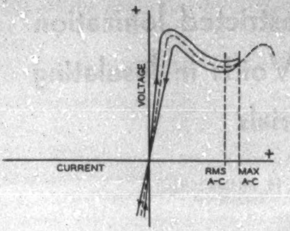


Figure 3. Portion of a-c discharge characteristic

(Also see figure 111, page 165, reference 8).

Slepian,[10] in his studies of arc discharge and reignition, shows a similar effect in the regions of arc discharge. Due to the high initial ionization occurring with arcs under a-c conditions, the discharge does not go through the initial ionization condition and a maximum such as *c*, figure 2, does not occur. With electrodes similar in geometry and of the same material, the positive and negative characteristics are similar in both the arc and the glow discharges. With electrodes of different material or of different geometry, the positive and negative characteristics may be dissymmetrical.

In our research, the left-hand portion of figure 2, to just beyond the maximum *c* has been investigated with alternating current. The discharge has been stabilized by means of a disk of glass, the electrical characteristics of which have been determined. The disk is in series with the corona discharge.

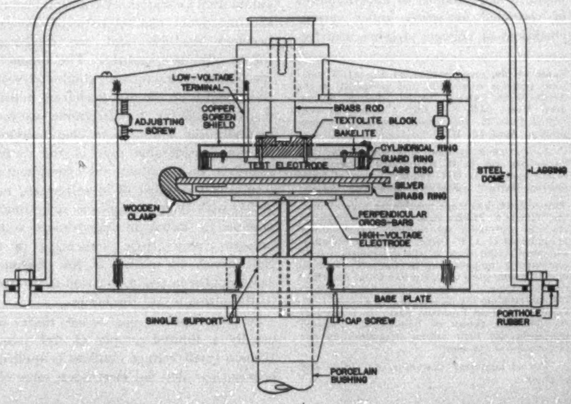
Apparatus and Procedure

The apparatus assembly is shown in figure 4. The test electrode was practically at ground potential. The electrode to which the glass disk is attached is the high-voltage electrode. The original apparatus differed slightly from that shown in figure 4, for, as the work progressed, minor improvements were made. The activating and mechanical action of the discharge is so great that even chromium plating on brass and steel electrodes was chipped in a relatively short time. Ultimately, it was found that only disks of hard chrome steel resisted indefinitely the destructive effects of the corona. The disks were accurately ground, lapped, and polished, although frequent polishing is necessary.

The diameter of this disk is 16.5 centimeters (6.5 inches) making the area 214 square centimeters (33.2 square inches). It is surrounded by an annular guard 1.27 centimeters (0.5 inch) radial width with a 0.238-centimeter ($^{3}/_{32}$-inch) space intervening. The thickness of the disk is 0.476 centimeter ($^{3}/_{16}$ inch). An outside cylindrical ring 0.317 centimeter ($^{1}/_{8}$ inch) thick and 3.17 centimeters (1.25 inches) axial length forms part of the guard.

The disk is mounted on a vertical shaft 2.54 centimeters (1 inch) diameter

Figure 4. Ionization test apparatus



by means of a block of Textolite, and the shaft is free to move vertically in a close-fitting bearing. The length of gap is first determined by a micrometer and then verified by the capacitance which can be accurately measured and calculated. The two values ordinarily check each other within three per cent. The low-voltage electrode is shielded on top by a copper screen. Thermocouples are mounted on the disk for determining the temperature.

The diameter of the glass disk is 24.1 centimeters (9.5 inches) and 0.530 centimeter (208.5 mils) thick. It is silvered to within approximately 1.27 centimeters (0.5 inch) of the edge.

The disks are of a special low-dielectric-loss and high-permittivity glass supplied by the Corning Glass Company. The surfaces of the disks were ground accurately, the disk was silvered on both sides, and the electrical characteristics were measured over a frequency range of from 25 to 75 cycles and a temperature range of from 20 degrees to 100 degrees centigrade. The characteristics are independent of voltage. The silver on one surface was then removed. The silver on the other surface made contact with the high-voltage electrode and the nonsilvered surface was adjacent to the gap. The disk acts as a series ballast, which stabilizes the corona discharge.

The assembly is mounted on a tripod structure, figure 4. The structure is in turn mounted on a base of boiler plate 1.59 centimeters ($^{5}/_{8}$ inch) thick, by a single central support so that deflections of the base plate due to changes in pressure do not change the air-gap. A dome thoroughly heat-lagged and with a small glass window for observation purposes is set over the apparatus and is bolted to the base plate making the apparatus air tight. A continuous stream of air passes through the apparatus during measurements. Unless this air is thoroughly dried, erratic results are obtained, and products resulting from the chemical action of the corona on the moisture are found in the gap. The air is dried by passing it through activated alumina. There is a resistor unit for controlling the temperature and there is also a small electric fan for circulating and agitating the air.

Measurements were made with a mutual-inductance, high-voltage bridge, in the high-voltage laboratories of the Graduate School of Engineering of Harvard University.[11] The voltage wave was sinusoidal, as shown in figure 5.

All necessary shields and guards were used and each was balanced to the potential of its shield. A tuned vibrating galvanometer was used as the detector. Since such a galvanometer responds to the fundamental frequency only, the characteristics measured were in terms of fundamental frequency. Due to the nonlinear characteristics of the gas, the percentage of harmonics in the current wave is large and harmonics of very high frequency occur. In figure 5 is shown a typical oscillogram of voltage and current waves taken at 11.5 kv and with a gap of 0.159 centimeter.

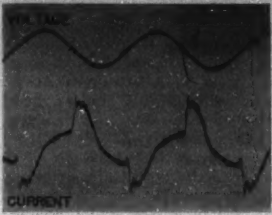


Figure 5. Oscillogram for gap of 0.159 centimeter—11,500 volts—pressure 76 centimeters

The branch of the bridge circuit consisting of the standard air capacitor and resistance (R_1) in series is linear so that the current in this circuit is sinusoidal. Since harmonic currents cannot contribute power with respect to the fundamental voltage, the power measured by the bridge is that corresponding to the fundamental voltage across the bridge and the fundamental current to the specimen.

An amplifier was used in the detector circuit of the bridge. It has been shown[12] that nonlinearity in an amplifier or other circuit element may cause two or more harmonic electromotive forces in the input to yield a fundamental component in the output and thus introduce error into the measurements. To eliminate this error, the amplifier is made essentially linear and the galvanometer circuit is provided with three low-pass filters which cut off at 45, 67, and 100 cycles.

Although the results submitted are in terms of the fundamental, the voltage across the gap itself cannot be exactly sinusoidal. This is due to the fact that this voltage is the difference between the sinusoidal voltage impressed across the glass disk and gap in series and the nonsinusoidal impedance voltage drop in the glass disk. To correct for this factor would involve great complication and no attempt is made to do so. Moreover, the conduct of our experiments corresponds with the conditions usually met in practice. Also, our results agree so closely with those obtained by other investigators, whose methods did not involve restricted ionization, that we feel that omitting the correction did not have any substantial effect in the results. Moreover the harmonics in the current wave for the most part are of very high frequency so that the impedance drop in the glass disk, due to them, is small.

From each bridge measurement, the total impedance of the glass and the gas space or gap in series was determined. By subtracting the known impedance of the glass disk from the total impedance, the impedance of the gap under the particular condition is determined. The several electrical characteristics of the gap, such as voltage gradient, power factor, power loss, can then be computed.

Measurements were made with gap lengths varying from 0.08 to 0.4 centimeters and at pressures of from 20 to 100 centimeters of mercury. Although measurements have been made at different frequencies, the results submitted in this paper are confined to 60 cycles. As the pressure is decreased, the maximum current density which can be obtained also decreases, because of the correspondingly reduced arc-over voltage of the apparatus. A very large number of tests have been made, but owing to space limitations only typical ones are given in this paper.

Voltage Gradient—Current Density

When the gap is ionized, a large space charge exists within it. Positive ions are moving toward the negative boundary of the gap and negative ions are moving toward the positive boundary of the gap. Also some recombinations are taking place. The space charge must have some effect on the distribution of gradient within the gap. This effect is complex and no attempt is made to correct for it. The values of gradient given, therefore, are the average for the gap.

In figure 6 are shown average voltage-gradient–current-density characteristics at different pressures for air-gaps of two different magnitudes, the average thickness of one gap being 0.180 centimeter and of the other 0.382 centimeter. Until ionization begins, the characteristics for all gaps are linear and identical, the gradient being merely that for a centimeter-cube constant-capacitance, parallel-plate air capacitor. Although these characteristics were obtained at widely different times and under different conditions, the initial linear portions of all the characteristics lie in the same straight line and the coincidence of the experimental points with the linear characteristic is surprisingly close. This indicates, we believe, that the precision of the measurements is good.

Ionization begins at the critical gradient G_o, beyond which the rate of voltage-increase diminishes rapidly, the gradient reaches a maximum at G_m and with further increase in current density the gradient diminishes. G_o lies at the intersection of the linear and the curved portion of the characteristics and can be critically determined since it corresponds to the beginning of an energy component of the gradient. The range of the characteristics shown in figure 6 lies in the initial-ionization and subnormal glow-discharge regions of figure 2 and the maxima in figure 6 correspond to the maximum shown at *c* in figure 2.

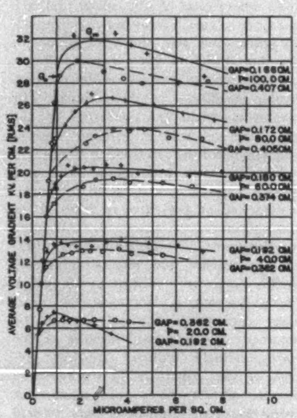


Figure 6. Voltage-gradient, current-density characteristics at several pressures

In figure 7 are shown three characteristics giving the relations, for very short gaps, of average voltage gradient to current density at three different pressures. The average gap length is 0.0871 centimeter. Comparing the initial linear portions of the characteristics of figures 6 and 7 with the initial portion of figure 2, the ionization current is completely masked by the displacement current. After ionization begins, however, the power factor rises rapidly and reaches values of nearly unity (figure 16). Hence, a short time after the ionization voltage is reached, the energy current becomes strongly predominating and apart from some variations due to time lag in the ionization, the characteristics with alternating current should not differ very greatly from those with direct current.

The characteristics in figures 6 and 7 show certain definite properties of ionized gaps. The maximum gradient G_m, and G_o become greater with decreasing length of gap. This is well illustrated in figure 7 in which the characteristic for the shortest gap is shown, and is in accordance with Townsend's equation for gap spark-over (equation 1). Both G_o and G_m diminish with the pressure and with decrease in pressure the difference between G_m and G_o also diminishes.

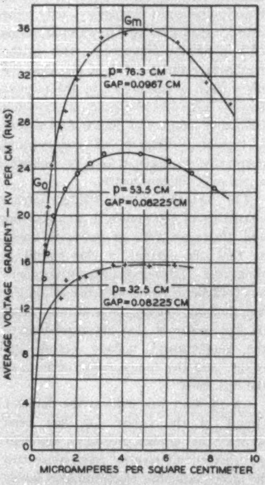


Figure 7. Voltage-gradient, current-density characteristics for short gap

From the foregoing it follows that the sparking voltage-gradient for gases between electrodes is not constant, but at constant pressure and temperature, is a function of the distance *S* between electrodes. The relation of the sparking gradient to *S* has been determined by several investigators but their results are not in close agreement. This is undoubtedly due in large measure to the different conditions under which the investigations were conducted. Townsend[7] (also see 13, page 116), experimenting at atmospheric pressure with gaps of the order of $^1/_{16}$ inch (0.16 centimeter) has found that the sparking gradient *G* may be expressed by the following equation:

$$G = G_o + \frac{1.35}{S} = 30 + \frac{1.35}{S} \text{ kv per centimeter} \qquad (1)$$

where G_o, = 30 kv per centimeter direct current, is the approximate initial breakdown or critical gradient of air at atmospheric pressure, and *S* is the length of the gap in centimeters. Other investigators give equations of the same general character as (1). S. Whitehead[13] (diagram II, page 42) gives, in the form of a graph, the combined results of a large number of investigators. In accordance with Paschen's law, the abscissas are given as the product of pressure *P* and spark length *S*, the pressure being in millimeters of mercury and the spark length in centimeters. The sparking voltage *V*, for the region represented by the gaps investigated by the authors (diagram II, page 42), is

$$V = 5 + 0.0353PS \text{ kv} \qquad (2)$$

Dividing by *S* and taking a pressure of 760 millimeters,

$$\frac{V}{S} = G = 26.8 + \frac{5}{S} \text{ kv per centimeter} \qquad (3)$$

the equation of which is similar in form to (1) but is only in rough numerical agreement with it. Slepian, in figure 58, reference 14, shows an experimental sparking-potential curve which is in quite close agreement with equation 1 for the magnitude of gaps investigated by the authors.

In figure 6 the critical gradient G_o at 80 centimeters pressure for the 0.172-centimeter gap is 22 kv per centimeter root-mean-square or 31 kv per centimeter maximum; the initial ionization for the 0.405-centimeter gap is 20 kv per centimeter root-mean-square or 28.3 kv per centimeter maximum. These values are very close to the generally accepted value of G_o and to the value given by Townsend in (1). In figure 7 the critical gradient for the 0.0967-centimeter gap at 76.3-centimeter pressure is 22.5 kv per centimeter root-mean-square or 31.9 kv per centimeter maximum, which is higher than the value for the two longer gaps. This, however, is rational since the critical gradient increases with decrease in length of gap.

Equations, such as (1) and (3), for the sparking voltage gradient are the result of experiment and are essentially empirical. So far as the authors are aware, no satisfactory physical basis for this form of equation has been given. This is particularly true for the second term which is proportional to $1/S$. The authors believe that the characteristics of figures 6 and 7 offer a rational explanation of this term. Sparking cannot occur when the gradient is less than the value G_m. This is due to the fact that between G_o and G_m the slope of the characteristic is positive and the impedance of the gap during this interval is stable. However, after the gradient reaches the value G_m, represented by the maximum of the characteristic, the slope of the characteristic becomes negative, the impedance of the gap becomes unstable, and cumulative ionization and transition from initial ionization to glow or arc discharge follow depending on circuit constants. Obviously sparking cannot occur at the gradient G_o but only when the maximum gradient G_m is reached. Therefore, the second term in (1) and (3) must represent the difference between the sparking gradient, G_m, and the critical gradient G_o.

Also, the increasing difference between G_m and G_o with decreasing length of gap, as shown in figures 6 and 7, explains why the second term in the equations 1 and 3 for sparking gradient is proportional to $1/S$.

There are several theories to account for the fact that spark-over does not occur at the gradient G_o but rather at the gradient G_m. Until the gradient G_m is reached, ionization by collision, primarily by electrons, has not become cumulative, so that sparking cannot occur until G_m is reached. Cumulative ionization does not occur until additional processes such as collision ionization by positive ions, electron emission by positive-ion bombardment of the cathode, ionization and photoelectric emission by ultraviolet radiation, and also the action of metastable atoms, become appreciable. The effectiveness of these processes varies according to experimental conditions.[14,15]

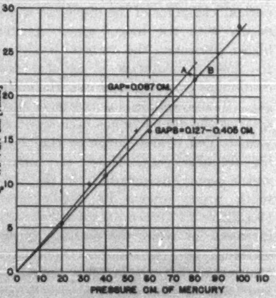


Figure 8. Critical-gradient and pressure

Effect of Pressure

Paschen's law states that the sparking potential is a function of the product of the density of the gas and the spark length. This law holds over a considerable range. With parallel fixed electrodes the sparking potential should be proportional to the pressure.

In figure 8 are two characteristics showing the relation of critical gradient G_o to pressure. The upper characteristic A is plotted from the values of G_o in figure 7. The data for the lower characteristics B, were obtained in part from figure 6 and also from similar characteristics for gaps with varying lengths. The critical gradients for the 0.087-centimeter gap are greater than for the longer gaps. However, it was found that for gaps of 0.17-centimeter and greater, the critical gradient becomes substantially independent of gap length. Both characteristics are linear showing that the critical gradient is proportional to the absolute pressure.

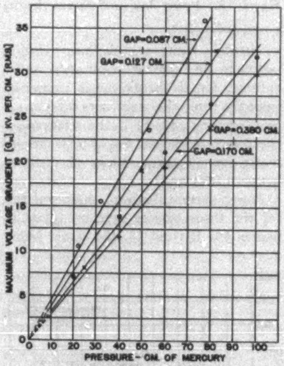


Figure 9. Maximum voltage-gradient and pressure

The equations of characteristics A and B are respectively:

(A) $G_o = 0.297\,p$ kv per centimeter (root-mean-square) (4)

(B) $G_o = 0.27\,p$ kv per centimeter (root-mean-square) (5)

where p is given in centimeters of mercury.

In figure 9 is shown the relation of maximum voltage gradient G_m to pressure for four different gap lengths, 0.087, 0.127, 0.17, and 0.38 centimeter. In each case the characteristic is linear showing that the maximum gradient in kilovolts per centimeter (root-mean-square) also is proportional to the absolute pressure.

$$(S=0.08\text{ cm})\qquad G_m=0.46\,p \qquad (6)$$

$$(S=0.127\text{ cm})\qquad G_m=0.39\,p \qquad (7)$$

$$(S=0.17\text{ cm})\qquad G_m=0.33\,p \qquad (8)$$

$$(S=0.38\text{ cm})\qquad G_m=0.30\,p \qquad (9)$$

In figure 10 the maximum gradients, corresponding to 76 centimeters pressure and the slopes of the characteristics taken from figure 9, are plotted as functions of gap length. Figure 10 shows that the maximum gradient G_m increases with decreasing length of gap. This is in accordance with theory (reference 13, diagram VI, page 44) and also in accordance with equations 1 and 3. Also for gap lengths of 0.3 centimeter and greater, the slopes of the G_m-p characteristic and the value of G_m become essentially constant.

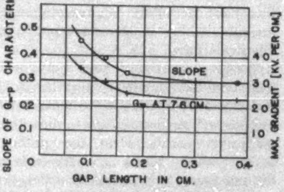


Figure 10. Slope of G_m-p characteristic and maximum gradient versus gap length

Using the characteristics of figures 8 and 9, the equations of the sparking gradient G $(=G_m)$ in terms of maximum gradient (root-mean-square $\sqrt{2}$) are for each gap length in kilovolts per centimeter (maximum):

$$(S=0.08\text{ cm})\qquad G=\left(0.44+\frac{0.0215}{S}\right)p \qquad (10)$$

$$(S=0.127\text{ cm})\qquad G=\left(0.39+\frac{0.0195}{S}\right)p \qquad (11)$$

$$(S=0.17\text{ cm})\qquad G=\left(0.39+\frac{0.0144}{S}\right)p \qquad (12)$$

$$(S=0.38\text{ cm})\qquad G=\left(0.39+\frac{0.0161}{S}\right)p \qquad (13)$$

At 76 centimeters pressure, equations 10, 11, 12, and 13 become respectively:

$$G=33.5+\frac{1.63}{S} \tag{14}$$

$$G=28.8+\frac{1.48}{S} \tag{15}$$

$$G=28.8+\frac{1.1}{S} \tag{16}$$

$$G=28.8+\frac{1.22}{S} \tag{17}$$

These equations are in reasonably close agreement with those of other investigators.

The first terms of equations 11, 12, and 13 are the same. In all four equations, (10–13), the second term depends on the length S of the sparking distance; and because of the linearity of the characteristics of figures 8 and 9, the sparking voltage is proportional to pressure, which is in accordance with Paschen's Law.

Sparking equations 1 and 3 of Townsend and S. Whitehead, together with their close correlation with equations 6 to 17 as well as figures 6 and 7, show that spark-over occurs when the gradient G_m is reached. Since G_m corresponds to the maximum c in figure 2, under the conditions of these tests spark-over must occur when the gradient given by c is reached.

Power–Current-Density Characteristics

When power is plotted as a function of current density, the relation invariably has been found to be essentially linear, particularly at atmospheric pressure. When the complicated phenomena which must be taking place in the ionized gas space, particularly under a-c conditions, are considered, it seems remarkable that the relation should be so simple. However, this linear relation seems to be inherent, even with direct current, for if the power is computed from the three characteristics determined by Townsend, figure 1, the relations of the power to current are also essentially linear for all three values of distance between electrodes.

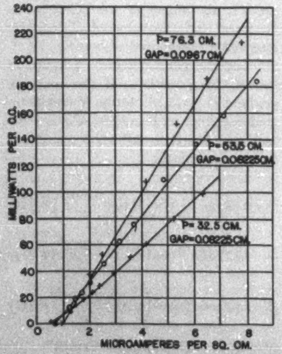


Figure 11. Power loss and current density

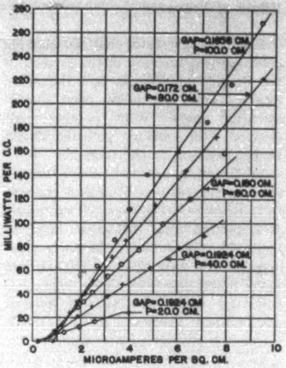


Figure 12. Power loss and current density

Power-current-density characteristics for the three typical gaps are given in figures 11, 12, and 13, the power being given in milliwatts per cubic centimeter. In figure 11 are given the characteristics for the average shortest gap (0.087 centimeter average), in figure 12 the characteristics for the average intermediate gap (0.18 centimeter average) and in figure 13 the characteristics for the average longest gap (0.382 centimeter average). The characteristics at the different pressures are essentially linear. They also show that the power loss per cubic centimeter increases with the pressure. The relation is shown in figure 14 which gives the power loss characteristics at constant current density of six microamperes per square centimeter for the three typical gaps, as functions of pressure. The characteristics are concaved slightly downward and if extrapolated apparently go through the origin. Obviously the power loss at zero pressure should be zero. The characteristics are of the exponential type, the exponents being slightly less than unity. The equation for each characteristic is given in the figure.

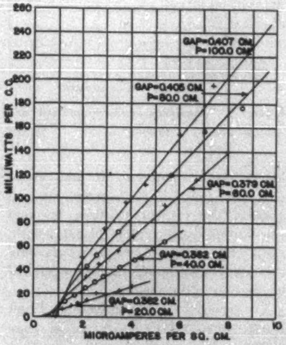


Figure 13. Power loss and current density

In figure 13, which gives the power loss for the longest gap of 0.382 centimeter (average), with the higher pressures and at the lower current densities the characteristics break slightly downward as they approach the axis of abscissas. This is due to the intermittent character of the discharge which occurs with the longer gaps. At the higher pressures, the current density is too low to maintain a uniform and continuous glow discharge over the entire gap. The resulting intermittent character of the discharge has been observed visually and is also shown by the sharp high-frequency pulsations of current which occur intermittently in the sawtooth waves shown by oscillograms. (See figure 5.) When the current density increases, or when the pressure is decreased, the intermittent discharge changes to the steady glow discharge and the power-current-density characteristics become linear throughout as

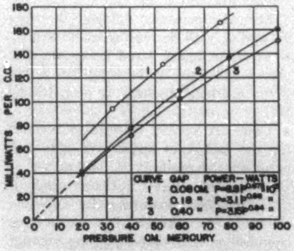


Figure 14. Gap loss and pressure, current density six microamperes per square centimeter

shown at the higher current densities and at the lower pressures of 40 and 20 centimeters, figure 13.

The normal power-current characteristics must intersect one another at the low values of current density and power. This is due to the fact that the current density at which ionization begins, diminishes with the pressure, whereas the slopes of the characteristics increase with the pressure. These intersections are shown clearly in figures 11, 12, and 13.

Since the power–current-density equations are essentially linear, they may be expressed by the general form of equation,

$P = m(I - I_o)$ watts per cubic centimeter **(18)**

where P is the power in watts per cubic centimeter, m the slope in watts per ampere per square centimeter, I the current density in amperes per square centimeter, and I_o the ionizing current density in amperes per square centimeter.

The ionizing current density is proportional to the pressure. No attempt was made to make precise determinations of the critical values.

Equation 18 holds only when $I > I_o$ and for the linear portions of the characteristics for the pressures of 100, 80, and 60 centimeters, figure 13. It is interesting to note that for a given gap the slopes of the characteristics in figures 11, 12, and 13 are linear functions of the pressure. This is illustrated in figure 15 which gives the slopes for these different average gap lengths. These characteristics are linear but do not go through the origin. They show that the rate of power increase is a linear function of the pressure.

Power Factor

Below ionization, the power factor of the gap is zero, there being no conduction except the very small amount due to random ions. After ionization by collision begins, a pronounced power loss occurs, and the power factor increases rapidly from its zero value and at moderate values of current density reaches values as high as from 0.8 to nearly unity. Characteristics at different pressures for the gaps having average length of 0.18 centimeter are shown in figure 16. The power factor increases almost immediately to values ranging from 0.6 to 0.8, after which the rate of increase diminishes. We find that for any given pressure the longer the gap the greater the power factor. Also the lower the pressure the greater the power factor. The power-factor characteristics at the lower pressures rise more rapidly and attain values approaching unity at much lower current densities than do those for the higher pressures.

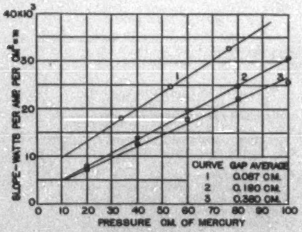


Figure 15. Relation of rate of power increase to pressure

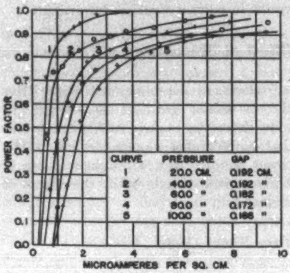


Figure 16. Power-factor characteristics

The general equation for the power factor characteristic is given by the power equation (18) divided by the product of voltage gradient and current density. That is

$$\text{P.F.} = \frac{m(I - I_o)}{EI} \qquad (19)$$

for $I > I_o$.

Also the power factor is a function of both pressure and gap length. Although empirical relations among these parameters can be determined, they would be far from simple and thus would add little to the understanding of the fundamental physical phenomena.

The rapid increase in power factor may be accounted for also by the very rapid increase in the series dielectric constant with current density (see figure 17) and the accompanying increasing power loss (figures 11, 12, 13).

As stated earlier in the paper, the fact that the power factor increases so rapidly to values approaching unity undoubtedly accounts for the fact that the characteristics of the gap with alternating current are of substantially the same character as for those with direct current.

Dielectric Constant

Under given conditions, an imperfect capacitor may be replaced in an electric circuit by either a perfect capacitance in series with a resistance, each of appropriate value, or by a perfect capacitance in parallel with a resistance or a conductance, each of appropriate value.[16] It is often convenient, particularly in circuit computations, to substitute an equivalent circuit for the imperfect capacitor.

Characteristics showing the variations in the equivalent parallel capacitance and resistance with change in current density may indicate the physical changes which are taking place.

The space charge within the ionized gap shortens the dielectric lines and accordingly increases the capacitance. The oscillations of the ions due to the alternating field reduce the dielectric constant.[17] The effect of the oscillation is noticeable only at very high frequencies, and not appreciable at power frequencies. The increase in capacitance may be considered as due to an increase in the dielectric constant of the ionized medium between the capacitor electrodes. The capacitor is changed from a perfect one to an imperfect one by the losses in the gap due to ionization.

In figure 17 are shown the values of series dielectric constant for the 0.18-

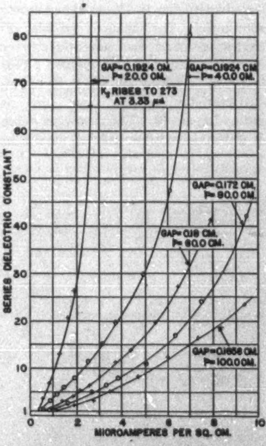


Figure 17. Series dielectric constant and current density

centimeter (average) gap at pressures of 20, 40, 60, 80, and 100 centimeters of mercury. These characteristics are not very different from those of the 0.382-centimeter (average) gap. The values of dielectric constant for a given value of current, however, are much less with the 0.08-centimeter gap. It is to be noted that with diminishing values of pressure there is an extremely rapid rise in the value of dielectric constant. For example, at 20 centimeters pressure the value at 3.33 microamperes per square centimeter is 273. At atmospheric pressure the dielectric constant has a value of about 40 at a current density of 9 microamperes per square centimeter.

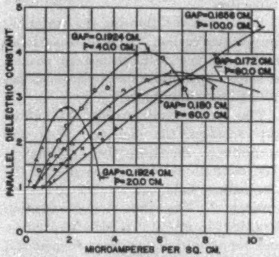


Figure 18. Parallel dielectric constant and current density

In an equivalent parallel circuit the value of capacitance necessary to replace a given imperfect capacitor becomes much smaller than with the series equivalent circuit, after the losses have become substantial. The values of parallel dielectric constant are accordingly very much less than the series values. This is shown in figure 18, which gives the characteristics for the same gaps as those of figure 17. The maximum value of dielectric constant reached occurs at 100 centimeters pressure and is equal to only 4.5. The characteristics of figure 18 are typical of a large number which have been obtained. It is to be noted that the characteristic of the gap at 100 centimeters pressure is nearly linear. As the pressure diminishes the characteristics become concaved downward, reach maximum values and with further increase in current density drop rapidly.

Effect of Frequency

Preliminary experiments indicate that the maximum gradient is affected in some degree by change in frequency and that considerable change occurs in the power factor. In a subsequent paper it is planned to show in detail the effects of frequency.

Very Short Gaps

In cable insulation a large proportion of the gaps or air films are of the order of 2.5 to 5 mils (0.0064 to 0.013 centimeter) thickness, whereas the shortest gap on which these tests were conducted is 0.07 centimeter or five to ten times the thickness existing in cables. The rate of change of characteristic increases rapidly with decrease in gap length, as shown in figure 10. With decrease in length of gap, the irregularities of the electrodes have much greater proportional effect. It is planned, however, to combine a more sensitive bridge with small precise specimens and to use a more precise micrometer adjustment of the gap with the object of obtaining data for these very thin gaps.

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Discussion

For discussion, see page 1071.

Corona Discharge on Rubber-Insulated Cables

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Introduction

Nature of Investigation and General Method

When rubber-insulated cables operate at higher than 1,500 volts potential, conditions may be favorable to the production of corona discharge over all or a portion of the outer surface of the insulation, or its braid covering. The cable may be on an inner metal conduit surface, or lie on a grounded flat metal surface, all of these conditions being favorable to surface corona at sufficiently high voltages. Corona discharge is known to be objectionable because of the chemical effect of the actinic ozone produced, and because of inductive and radiation effects, notably interference with radio reception.

The purpose of this investigation was twofold: First, it was desired to learn how cables of different designs, ratings, insulation characteristics, etc., compared as to corona behavior when an operating condition is imposed which tends to produce corona at nominal voltages. Second, it was desired to learn what effect, if any, corona discharge may have on the power factor of the insulation under the test conditions.

It was decided to test each piece for corona discharge and power factor behavior as it lay on a flat metal plate, with the ends of the test piece upturned to prevent flashover at the ends. Each test piece was 34 to 36 inches long. As potential between the conductor and this contact surface is increased, a value will be reached where corona discharges will begin within a more or less limited or concentrated region, close to the point where the insulation surface is in contact, or near contact with the metallic plate. Several ozone determinations were made when disruptive discharge phenomena were electrically detected, with means described in this paper. Some test pieces possessed very stiff, heavy wire, and braid having a rough, irregular surface, so that it was very difficult to attain firm contact at every point. In fact, the contact for such test pieces was not uniform, as would often be the case in practice. This condition will be termed *erratic contact*, and for such cases corona discharge will start at several points, and at the lowest voltage. When the voltage is further increased a value will be then reached where visible corona discharge more or less suddenly extends from the curving away points of contact to a continuously visible state of corona along the entire length of the cable including regions of firm contact as well. Power-factor measurements were made with voltage applied between the conductor and metal contact plate with and without corona, and with unbraided samples. The measured power factors would apply in practice to lengths of line actually in contact with the metal plate.

The conclusions are itemized below. They show that corona discharge may well occur at or near normal operating voltages under conditions liable to be obtained in actual use, and in addition to the previously assumed effects of ozone production, power factor may be affected.

Tests were made on an insufficient number of cables shielded with semiconducting materials or metallic tapes to justify any conclusions at this time regarding the effect of such shielding on corona behavior.

Conclusions

1. The commercial two-kv cables with ozone-resisting rubber insulation both with braid and without braid and its saturant are in general properly rated as to voltage from the corona starting point of view. The five-kv cables are close to the nominal rating. The 12-kv and 14-kv cables which were tested appear to be overrated, since corona discharge occurs below the normal operating voltage.

2. Corona starting voltages increase with the thickness of the rubber insulation for all samples tested with braid removed and for those without braid. This is not strictly true when braid is present. Corona starting voltage was found to decrease from 17 to 30 per cent for an increase of about 70 per cent in the specific inductive capacity of the insulation rubber.

3. For most cables, the voltage for fully visible corona along the entire length of the cable resting on the test plate is about 30 per cent higher than the voltage at which corona begins.

4. Ozone is produced in the case of a rubber-insulated cable resting on a metal when and only when the potential difference between the conductor and plate is sufficiently great to cause disturbances in the charging current, which may be measured by the methods described.

5. In some cases of five-kv unbraided cables tested at line voltage, the presence of corona discharge increases the power factor, when measured on a flat metal plate, to several times the value of power factor at the same voltage but with corona suppressed by the application of air pressure. When the cable has a braid covering the power factor is markedly higher than the

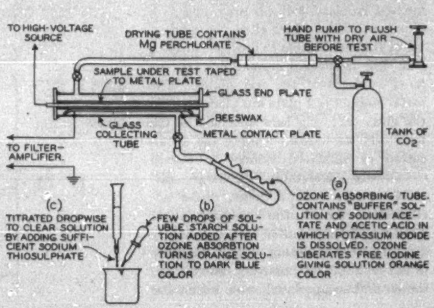
TO HIGH-VOLTAGE SOURCE
DRYING TUBE CONTAINS Mg PERCHLORATE
HAND PUMP TO FLUSH TUBE WITH DRY AIR BEFORE TEST
SAMPLE UNDER TEST TAPED TO METAL PLATE
GLASS END PLATE
BEESWAX
GLASS COLLECTING TUBE
METAL CONTACT PLATE
TANK OF CO2
TO FILTER-AMPLIFIER.
(a)
OZONE ABSORBING TUBE. CONTAINS "BUFFER" SOLUTION OF SODIUM ACETATE AND ACETIC ACID IN WHICH POTASSIUM IODIDE IS DISSOLVED. OZONE LIBERATES FREE IODINE GIVING SOLUTION ORANGE COLOR
(b)
FEW DROPS OF SOLUBLE STARCH SOLUTION ADDED AFTER OZONE ABSORBTION TURNS ORANGE SOLUTION TO DARK BLUE COLOR
(c)
TITRATED DROPWISE TO CLEAR SOLUTION BY ADDING SUFFICIENT SODIUM THIOSULPHATE

Figure 1. Means for quantitative determination of ozone generated on rubber-insulated cable

Grams O_3 = 0.024 times cubic centimeters of sodium thiosulphate added

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1. For all numbered references, see list at end of paper.

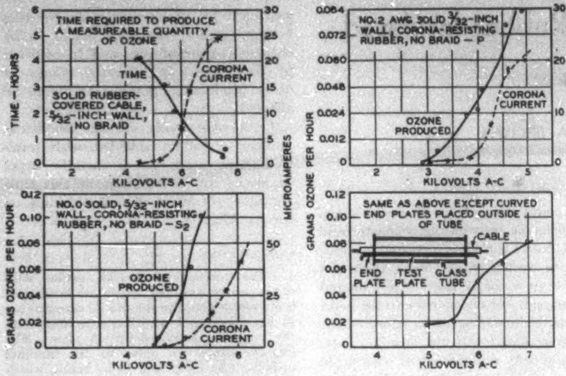


Figure 2. Ozone production with rubber-insulated cables on flat metal test plates

power factor without the braid, and the presence of corona discharge has almost negligible effect on the power factor.

6. The method of detecting the presence of corona discharges by the effect of the disturbances caused by corona on the charging current has been found to be highly sensitive and satisfactory.

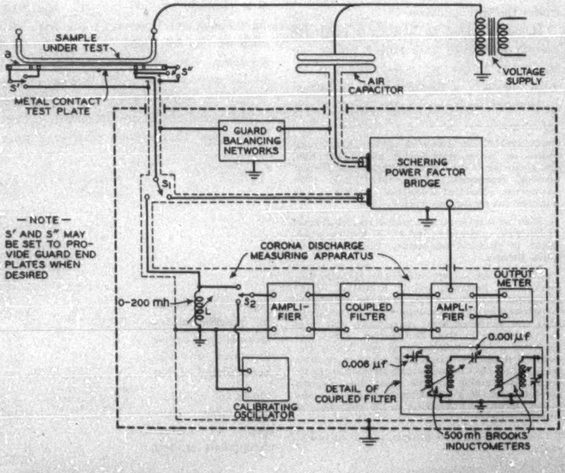
Relation of Ozone Production to Corona Discharge

Quantitative Test for Ozone Production

A quantitative test for the amount of ozone produced was carried out on several representative pieces of cable. It was desired to learn whether or not sufficient ozone was liberated under ordinary operating conditions to be quantitatively measurable. The method of procedure in this determination is indicated by the schematic diagram of figure 1. The test piece is enclosed in a suitable glass tube, having flat glass end plates, cemented in place with beeswax, and provided with holes which allow the conductor of the test sample to protrude at each end, the insulation terminating at the inner surfaces of the end plates. The metal-contact test plate was placed within the glass enclosing tube and tied to the test piece as shown. During the application of sufficient voltage to obtain electrically detectable discharge disturbances for several hours, ozone accumulated within the enclosing tube. Care was taken to allow only thoroughly dry air in the enclosing tube at the start of the test. After removing the voltage, the air and accumulated ozone within the enclosing tube were swept out with dry carbon dioxide and into an absorbing tube containing the chemical absorbing and buffer solutions indicated in figure 1. About 15 minutes is required thoroughly to sweep out the enclosing tube of all traces of ozone. The absorbing solution absorbs the ozone and iodine is liberated, giving the solution a brownish orange color. After this, more sodium acetate solution is added and then a few drops of soluble starch solution, which changes the test solution to a dark blue color. The amount of ozone is then determined by titration, sodium thiosulphate is added drop-wise until the solution becomes clear. The number of grams of ozone liberated is then equal to 0.024 times the cubic centimeters of sodium thiosulphate added.

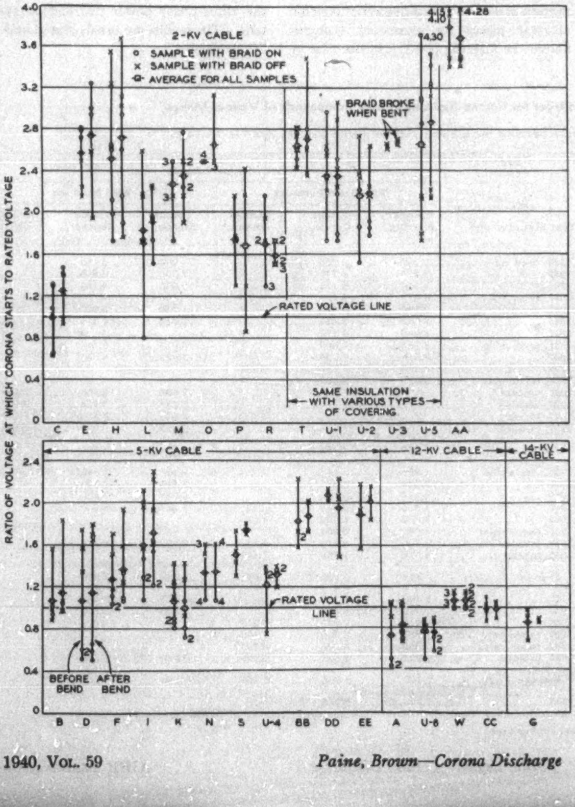
Some typical test data are shown in graph form in figure 2, the amount of ozone produced being compared with the corona discharge intensity and voltage applied. In each case the variation of corona discharge current with voltage is shown. This was determined in a manner described later. It will be noticed that at the voltage for which output-meter indications of corona discharges are obtained, it required more than four hours to obtain a measurable quantity of ozone, and that the ozone produced increased rapidly at voltages above this value. Comparing the graphs on the right-hand side of figure 2, it should be noted that when the special setup of the lower graph is used, in order to remove the curved ends of the test plate to the outside of the glass enclosing tube, measurable ozone was not obtained until the voltage was increased from less than three kilovolts to about five kilovolts. This means that the ozone produced at the critical separation of insulation and test plate where the insulation curves away from the test plate was, of course, not included in the collected ozone, and that ozone could not be collected until the voltage arose to a value which produced corona discharge uniformly along the length of test plate inside the enclosing

Figure 3. Test circuit for rubber-insulated cables



tube and in firm contact with the insulated surface. In this test care was taken to use a test sample which was sufficiently flexible so that it could be held in firm contact with the test plate by being bound thereto with waxed lacing cord. It should be here pointed out that in this latter test setup, detectable discharges were obtained at 2.8 kv, as shown in the upper right-hand graph. The end contact plates shown in this special setup in this figure were connected to ground, but this does not remove the effect of corona discharges at the curving away region from the detecting-recording apparatus. The discharge phenomena in this region disturbs the charging current to the active portion of the test plate connected to the detecting apparatus by capacitance coupling, although to a lesser degree than if the curved end plates, or they might be considered as guard plates, were conductively connected to the active test plate within the enclosing tube.

Figure 4. Relation of corona starting voltage to operating voltage of rubber-insulated cables



Corona Characteristics for Various Types of Rubber-Insulated Cables

Description of Test Equipment and Procedure

The test arrangements for detecting and measuring the corona discharge current are shown in figure 3. This apparatus has been formerly described[1] and has since been slightly modified. The metal-contact test plate is shown with *guard end plates* which can be connected to the active test plate or isolated therefrom and grounded by means of switches S' and S'' as shown. The transfer switch, S_1, connects the test plate to the apparatus for detecting and measuring the discharge phenomena when this switch is closed to the left. This connects the test plate through the series variable air-cored inductance to ground, as shown. Previous investigation work on high-voltage paper-insulated cables[1] showed that the potential drop across this variable inductance at the random damped conglomerate discharge frequencies, which vary from 4,000 to 9,000 cycles, is greatly increased in proportion to the drop across this inductance at the 60-cycle charging-current frequency and harmonics thereof. Therefore, this inductance is the first step in filtering out the potentials due to charging current. Through switch S_2 closed on the upper position potentials across the inductance are carried to a one-stage shielded amplifier employing a type 37 tube and output transformer with a 2,000-ohm secondary, this transformer having a high-fidelity frequency response. A coupled filter system, indicated in the figure, effectively suppresses all remaining 60-cycle charging current and harmonics thereof. This filter system is more fully described in the reference previously made, and is very briefly diagrammed in the detailed insert in figure 3. The Brooks inductometer makes a satisfactory type of astatically wound, variable coupling inductance in this filter system. The filter is followed by a three-stage amplifier, comprising metal tubes of the 6*J*7, 6*C*5, and 6*F*6 types, all resistance coupled, and terminating in an output transformer with a 5,000-ohm secondary. The output meter must be one of flexible range and sensitivity. A multirange milliammeter, reading 0–100 microamperes for its lowest range, is used, and in series with this is connected a type 230 tube with grid tied to plate as a rectifier. The conventional input gain-control potentiometer is incorporated in the main or second amplifier. When discharges occur in the crevices between the insulation surface and the test plate the output meter will give a convenient reading with proper setting of the gain control, then switch S_2 is closed on the lower position and the output voltage of a calibrating oscillator of 5,000 cycles frequency is suitably reduced in amount through a calibrated attenuator until the output meter shows the same reading as was obtained for the corona discharge on the test plate. Knowing the value of the variable inductance and assuming a 5,000-cycle average frequency, the current through this inductance is assumed to be equal to the voltage across the inductance as measured by the calibrating oscillator with its associated output voltmeter and attenuator, divided by the 5,000-cycle reactance of the variable inductance *L*. The use of the coupled filter will suppress any 60-cycle component of corona discharge disturbances that may exist, and the application of the steep wave front and random frequency potentials upon the coupled filter results in a certain amount of regularly damped oscillations in the filter network. In order to eliminate all such effects, it is necessary to provide a resistance-capacitance type of bridge circuit incorporating equal resistance ratio arms and a discharge-free capacitor in order to balance out the 60-cycle charging current almost completely so that discharges may be detected. The authors' previous work with high-voltage cables indicates that the test setup indicated in figure 3 gives comparative corona-discharge current intensities just as accurately as the more cumbersome bridge system, and even in the bridge system a small aperiodic filter must be used to remove the unbalanced 60-cycle residue resulting from in-phase loss component in the charging current in the sample under test. Further investigation indicates no appreciable 60-cycle component of ionization discharge current exists, as it would when the metallic test electrodes are completely separated by air or gas so that ions can drift directly from one electrode to the other, as is the case with the conventional corona tube containing air or other gas. When switch S_1 is closed to the right, a Schering power-factor bridge and its associated guard balancing networks and loss-free air capacitor are put into use to measure power factor.

Each sample of rubber-insulated cable was cut into test pieces three feet long so that when bent upward at the ends, about 30 inches was left in contact with the test plate. The test piece was taped to the test plate so as to insure firm contact, but not sufficiently firm as to cause flattening of the insulation surface. As the voltage was raised, a sensitive telephone-receiver headset was used to detect the point at which discharge disturbances began and the output meter deflection occurred. In the cases of most cables tested, discharge disturbances occur with an abrupt start and increase, so that the audibly detectable corona discharge and a readable output meter deflection coincide. Readings were taken for all test pieces for increasing voltages above the starting point. In the case of each piece tested the point at which the corona discharge quite suddenly becomes visible along the entire length of the insulation surface lying on the test plate was also observed.

Results of Tests for Corona Discharges

The principal object of these tests was to make a study of the starting voltage for corona discharge, and to obtain visible voltage behavior for representative samples of rubber-insulated cables. Figure 4 shows diagrammatically the starting corona voltage for each piece tested, before and after bending the test piece three times around a mandril of five times the cable diameter; with braid on, and braid removed, in the case of braided samples. The point conventions are explained on the graph, and the ordinates are plotted with respect to the rated operating voltage line, assuming that test voltage applying between the conductor and plate is compared with the wye-leg voltage or voltage to neutral in the three-phase power distribution system. The results are briefly stated under "Conclusions."

Table I. Corona Voltages for Corona-Resistant Rubber Compounds of Various Makes

All Values Are Before Bending the Cables; All But Three Samples Are New Cables

Size	Insulation Wall (Inches)	Key	Year Manufactured	Corona Voltages					
				With Braid Removed			With Braid On		
				Corona Starting	Visible Corona	Audible Corona	Corona Starting	Visible Corona	Audible Corona
No. 6 solid	3/32	*L**	1929, used cable	2,500	4,225		1,810	3,565	
		M	1936	2,625	4,770		2,750	4,850	
No. 6 stranded	5/32	*N*	1936	4,440	6,175		3,000	5,140	
No. 2 solid	3/32	*P*	1937, taped only	2,010	4,890				
		R	1937, braided	2,190	4,235		1,750	3,960	
		E	1936	2,665	4,230		3,210	4,335	
		O	1937	3,000	4,950				
		J	1931, used cable	3,185	4,660		3,015	5,115	
		H	1937	3,510	4,850		2,430	4,865	
		AA	1938	4,385	5,325	3,805			
		T	1937, taped only	3,025	3,495	2,700			
		U1	1937, braided				2,605	4,505	2,450
		U2	1937, braided				2,235	3,775	2,150
		U3	1937, braided				3,560	5,050	3,400
		U5	1937, braided				3,885	5,095	2,780
			Average	2,995	4,580		2,835	4,585	
No. 0 solid	5/32	*K*	1930, used cable	3,505	5,150		2,410	5,685	
No. 0 stranded	5/32	*B*	1935	3,325	5,535		2,855	5,135	
		F	1936	4,015	5,525		3,085	5,800	
		D	1936	4,505	5,665		1,665	2,500	
		BB	1938	5,260	7,390	4,675			
		S1	1937, taped only	4,390	6,200				
		S2	1937, braided	5,300	5,810	4,800	3,990	6,520	
		U4	1937, taped only	3,510	4,700	3,480			
			Average	4,330	5,830	4,830	2,900	4,990	
No. 0 stranded	6/32	*I*	1937	5,350	7,400		3,575	6,355	
No. 2 stranded	8/32	*A*	1935	7,015	8,580		3,255	4,855	
		CC	1938	6,925	10,225	6,275			
		U6	1937, taped only	5,885	7,305	5,500			
		V	1937, taped only	7,340	8,535	6,705			
		W	1937, braided	8,025	8,825	6,860	6,990	8,830	
			Average	7,040	8,895	6,335	5,120	6,840	
No. 2 stranded	9/32	*G*	1936	7,325	8,235				

* This is not corona-resistant compound but a 30 per cent rubber type.

Table II. Average Values of Corona Voltages for All Makes of Corona-Resistant Rubber Compounds

All Values Are Before Bending the Cables

Size	Insulation Wall (Inches)	Number of Samples	Corona Voltages: Braid off: Starting	Corona Voltages: Braid off: Visible	Corona Voltages: Braid on: Starting	Corona Voltages: Braid on: Visible
No. 6 solid	3/32	1	2,625	4,770	2,750	4,850
No. 6 stranded	5/32	1	4,440	6,175	3,000	5,140
No. 2 solid	3/32	8(for *a*, *b*) 8(for *e*, *f*)	2,995(*a*)	4,580(*b*)	2,855(*e*)	4,585(*f*)
No. 0 solid	5/32	1(used cable)	3,505	5,150	2,410	5,685
No. 0 stranded	5/32	7(for *c*, *d*) 4(for *g*, *h*)	4,330(*c*)	5,830(*d*)	2,900(*g*)	4,990(*h*)
No. 0 stranded	6/32	1	5,350	7,400	3,575	6,355
No. 2 stranded	8/32	5(for *k*, *l*) 2(for *m*, *n*)	7,040(*k*)	8,895(*l*)	5,120(*m*)	6,840(*n*)
No. 2 stranded	9/32	1	7,325	8,235		

NOTE: Range of values:

(*a*) 2,010 to 4,385	(*k*) 5,885 to 8,025	(*g*) 1,665 to 3,990
(*b*) 3,495 to 5,325	(*l*) 8,535 to 10,225	(*h*) 2,500 to 6,520
(*c*) 3,325 to 5,300	(*e*) 1,750 to 3,885	(*m*) 3,255 and 6,990
(*d*) 3,735 to 7,390	(*f*) 3,775 to 5,115	(*n*) 4,855 and 8,830

Tables I and II show results indicated in figure 4, but before bending, and show which cables are braided and which are not, and also show average values for the samples with braid on, and with braid removed. The left-hand curve of figure 5 shows that the corona starting voltage increases with the insulation wall thickness and nearly in direct proportion. For 2-kv cables ($^{3}/_{32}$-inch wall) the operating voltage to neutral is nearly one-third the corona starting voltage, while for 12-kv cables the corona starting point is almost at the rated operating neutral voltage. The right-hand curve of figure 5 shows the variation of corona starting voltage with the specific inductive capacity (dielectric constant) of the rubber insulating material using data on samples tested before bending, and for no braid or braid removed. Many factors, of course, cannot be accounted for, but in general a decrease of roughly 30 per cent in corona starting voltage results for an increase of roughly 70 per cent in specific inductive capacity of the rubber. For the higher-voltage cables the decrease is probably around 17 per cent.

Effect of Corona Discharge on Power Factor

METHODS AND CONDITIONS FOR POWER-FACTOR MEASUREMENT

A number of power factor measurements were made in order to learn whether or not the existence of corona discharge had any considerable effect on power factor. The test setup of figure 3 was used again, and for the power factor data submitted in this paper no grounded guard end plates were used, switches *S′* and *S″* being thrown so as to connect the end plates to the center test-plate section. This was done in order to include the effect of the starting corona discharge at the point or points where the insulation curved away from the test plate. The effect of corona discharge on the contact-plate power factor is most readily obtained by observing the change of power factor upon removal of corona discharge by suitable means. To do this the test piece with its contact plate, from which the latter curved at the two ends, was placed inside an adequately sealed three-inch-diameter Pyrex glass tube having one-eighth-inch wall thickness, and a length of 21 inches. When air pressure of 10 to 15 pounds per square inch was provided by means of a tire pump, the corona discharge was completely eliminated, even though the voltage was sufficiently high to produce the uniformly

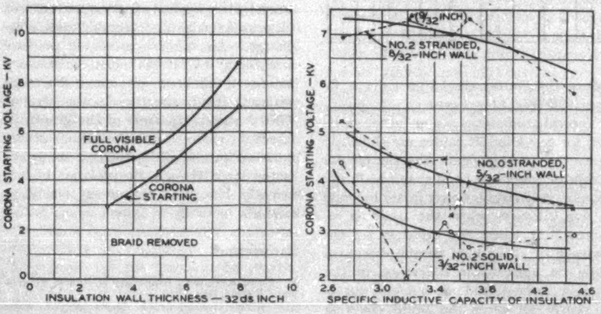
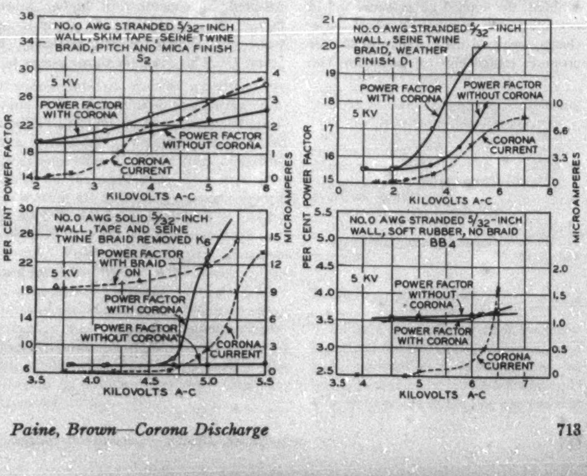


Figure 5. Variation of corona with thickness and specific inductive capacity of insulation rubber

Figure 6. Effect of corona discharge on power factor of rubber-insulated cables tested on metal contact plate



visible corona along the entire length of the test piece lying on the metal plate. In the case of some stranded conductor samples, and some of the samples provided with a tightly woven braid and saturant, the applied air pressure would not completely remove all corona discharge, owing to the inability of the air pressure completely to permeate the space between the braid and the rubber, or between the rubber and the stranded conductor surfaces. In all cases, however, air pressure of 15 pounds per square inch above the atmospheric value would reduce the corona discharge current intensity at least 85 per cent.

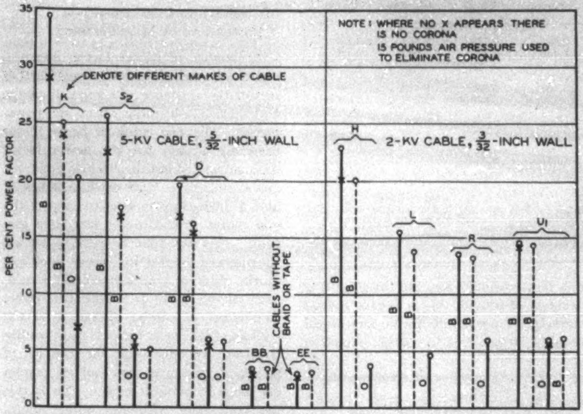


Figure 7. Relation of corona and braid covering to power factor of rubber-insulated cable on flat plate

Values shown are average for three pieces of each make of cable
Solid line—Tested at rated line voltage
Dashed line—Tested at wye leg voltage
O—Power factor at rated voltage
X—Power factor with all or at least 75 per cent of corona discharge removed
B—Tested with braid and other covering on
O—Tested with braid, tape, etc., removed

Typical examples of power-factor variation for five-kilovolt cables are shown in figure 6. In this figure the sample for which the lower left-hand graph is shown, shows a considerable power factor increase at five kilovolts when the braid is removed, but when the braid is on the presence or absence of corona discharge made no appreciable difference in the position of the dotted power-factor curve. In nearly all cases the braid covering resulted in considerably higher power factor. Some results of power-factor tests are shown diagrammatically in figure 7. This diagram shows power factors obtained for both tests at the nominal rated line voltage and for the actual voltage to neutral over which the cables would probably operate in a three-phase system. In all of the samples tested it is quite evident that corona discharge at actual operating voltages has little effect upon the power factor. On some of the vertical lines in the diagram no crosses (indicating power factor with corona discharge removed) appear, these indicating that in these particular instances no corona discharge existed at the testing voltages. One sample shown in this diagram has a power factor of 15 per cent at rated line voltage with braid removed, which was reduced to 7 per cent with applied air pressure. The higher power factor for the test piece with braid covering is due to loss in the braid rather than to any corona discharge, because the power factor remained high when the voltage was sufficiently low so that no corona discharge could be detected, and when existing corona discharge was present, little change in power factor was obtained when applied air pressure removed all or practically all detectable corona discharge. Air or carbon-dioxide pressure was found to be the most satisfactory means of eliminating corona discharge without itself effecting power factor. This is shown by some of the graphs in figure 6 where the application of air pressure produced no change in power factor even though the voltage is sufficiently low that no corona discharge existed.

Reference

1. Bulletin No. 260, University of Illinois Experiment Station, pages 6–8.

Discussion

For discussion, see page 1070.

The Dielectric Strength and Life of Impregnated-Paper Insulation—I

The Influence of the Density of the Paper

J. B. WHITEHEAD
FELLOW AIEE

PAPER as used in the spiral laminated impregnated insulation of high-voltage power cables is available over wide ranges of density and thickness. There result important variations in the ordinary physical properties of the paper such as flexibility, tearing, tensile, and bending strength. Variations in the electrical properties of conductivity, dielectric constant, and breakdown strength also result. The peculiarly severe conditions imposed by the manufacture, transportation, installation, and operation of high-voltage cables have emphasized the importance that full attention be paid to all physical properties of the insulation, especially those relating to permanence of mechanical structure and of high insulating properties. Manufacturers of cables often utilize the several types of variation referred to in designing cable insulation to meet the particular requirements of cables for different types of service.

This paper reports the results of a part of an experimental program of study of the life of impregnated-paper insulation, particularly as related to those changes occasioned by the presence of electric stress, carried out under the auspices of the Engineering Foundation with the sponsorship of the AIEE. The method in view is to make accelerated life and other electrical tests on samples in which the only variable is some one of the physical properties mentioned above. In the present tests this variable has been the density of the paper. Few if any definite data have been presented as to the influence of variations in density on the electrical characteristics of impregnated paper and its performance in service. The results of the present study will be found to be quite definite. From what has been said, however, it will be recognized that the influence of variations in density of the paper must be considered as only one element of the general problem in cable design, and that the influence of a variation in density on other important physical properties must be duly considered in particular cases.

The Papers

Four grades of paper were studied, all by the same manufacturer, from the same basic wood pulp, and all of approximately the same thickness. Table I gives the density of the several grades expressed as specific gravity, and also data as to other properties investigated. The specific gravity was determined by measuring the over-all volume and the weight of 12 pieces of the paper tape piled together under moderate pressure, by similar measurements on three-inch diameter rolls of tape, as delivered, and also as rewrapped under heavy tension. The results of these tests are given in columns 3, 4, 5, and 6 of table I. Those in columns 3, 4, and 6 were made in the research laboratories of the General Cable Corporation through the courtesy of R. W. Atkinson. The figures of column 5 were made in the author's laboratory. The average values in column 7 have been used in plotting the results of the electrical tests. All of the measurements of specific gravity were made under atmospheric conditions. Approximate figures for the amount of moisture contained in the paper under these conditions were obtained from measurements on the same rolls after drying in circulating air at 105 degrees centigrade for approximately 44 hours. The loss by weight in per cent is given in column 8 of table I. Values of the Gurley air resistance furnished by Mr. Atkinson are given in column 9. Columns 10 and 11 give the per cent oil content by volume and by weight in the impregnated paper, as measured after the electrical tests by repeated saturation with ether and repeated distillation in a closed system. (See also figure 4.)

Table I also gives data on grade *E* paper, 0.004 inch thick, as used in an earlier research.[1] The paper was thus about five years old. Certain interesting properties of this paper will be mentioned below.

The Test Specimens

The test specimens were made by lathe, wrapping 16 layers of 0.005 inch (0.0127 centimeter) by 1 inch (2.54 centimeters) paper tapes on smooth brass tubes, with a $33^1/_3$–$66^2/_3$ overlay, and with a butt spacing of the tapes of approximately $^1/_{64}$ inch (0.0397 centimeter). The central or measuring electrode was 42 centimeters long overlapped by and insulated from a foil guard electrode extending up the conical paper reinforcement at each end of the specimen. Further details as to the test specimen will be found in earlier papers.[1,2] Much attention was given to the making of the specimens as nearly alike as possible. They were all given the same drying and impregnation treatment[1] (predrying in moving air at 105 degrees centigrade, evacuation at two millimeters mercury pressure, and 105 degrees centigrade for 24 hours, impregnated with oil degassed at 60 degrees for 24 hours).

Nine identical specimens were constructed from each grade of paper. They were dried and impregnated in groups of three.

The Oil

All specimens were impregnated with a light oil as used for oil-filled cables by American manufacturers. The physical characteristics of the oil are approximately as follows:

Gravity, API/60 degrees	24.8
Flash (degrees Fahrenheit)	320
Fire (degrees Fahrenheit)	360
Saybolt Universal viscosity ÷ 100 degrees Fahrenheit	102
Color N.P.A.	1−
Neutralization number ASTM	Neutral
Pour test (degrees Fahrenheit)	−60
Iodine number	12.4
Sligh oxidation	21.2
Dielectric strength	32.5 kv
Resistivity Race cell/115 degrees centigrade:	
Initial-ohms per centimeter cube	20.0×10^{13}
Aged-ohms per centimeter cube	12.5×10^{13}

Experimental Method

All tests were made on single specimens immersed in the impregnating oil in a bath enclosed in a thermally insulated box with automatic temperature control.[1] The air pressure in the test box was that

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1. For all numbered references, see list at end of paper.

Table I. Properties of Papers Tested

Paper Grade	Thickness (Inches)	Specific Gravity				Average	Per Cent Moisture	Gurley Air Resistance	Per Cent Oil Content	
									By Volume	By Weight
A	0.0048	0.758	0.759	0.703	0.737	0.739	5.64	19	52.5	64.0
B	0.0048	0.932	0.917	0.896	0.929	0.918	6.24	400	41.0	40.2
C	0.0055	1.155	1.154	1.136	1.095	1.135	6.57	1,760	33.1	26.2
D	0.005	1.198	1.187	1.144	1.190	1.18	6.46	2,070	22.1	16.8
E	0.00384	0.875	0.886	0.832	0.866	0.865	6.38	700	39.0	40.6

of the atmosphere. The temperature for all tests was 40 degrees centigrade.

Life and Breakdown Tests. The accelerated step-up life test started at a voltage to give an average stress of 400 volts per mil on the specimen and the test voltage was increased by 3.12 per cent every four hours, thus giving a geometric increase such as to bring the stress to 700 volts per mil within three days. Under this program the range of life of all specimens tested was from two to four days.

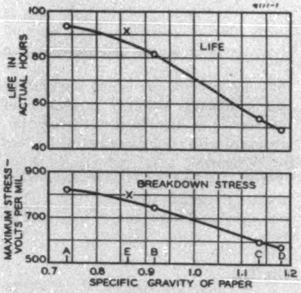

Figure 1. Breakdown strength and life as function of the specific gravity of the paper

Power Factor and Capacitance. Power factor and capacitance were measured on a high-voltage Schering bridge[1] at the beginning of the life test and at 12-hour intervals and over the range 100 to 400 volts per mil.

Conductance. The long-time d-c conductance of the dry paper specimen was measured at 1,500 volts and 105 degrees centigrade before impregnation, and of the impregnated specimen at 60 degrees centigrade immediately following impregnation.

Experimental Results

The results of the accelerated life and breakdown tests on grade *C* paper are given in table II. They are typical of the results on the other grades of paper for which the average values only are given in table II. Using the average values of life and breakdown stress, as taken from table II, and the values of specific gravity and oil content, as given in table I, the results on accelerated life and breakdown and other electrical properties are plotted in figures 1 to 3 inclusive.

The results indicate that with increasing density of the paper, there are marked variations in all electrical quantities as well as in the values of breakdown stress and life.

Breakdown Strength and Life. Both these quantities decrease with increasing paper density over the range studied. (See figure 1.) This similarity of behavior is a necessary consequence of the type of accelerated life test. In the present case it happens that the change in the over-all duration of time (life) is greater than that of voltage. The decrease of dielectric strength and life with increasing density is contrary to the behavior commonly attributed to an increase in density of a homogeneous material and also to the influence often attributed to the use of heavier papers in cable manufacture. Discussion of this result and the probable explanation will be found in a later paragraph.

Power Factor. The values of power factor given in table II are those measured at the beginning of the test at 300 volts per mil. In figure 2 they are plotted as

Table II. Accelerated Life Tests

Specimen	Paper	Stress—Volts Per Mil		Time (Hours)		Power Factor at 300 Volts Per Mil	Dielectric Loss Watts Per Inch Cubed at 300 Volts Per Mil	Capacity (Micromicrofarads) at 300 Volts Per Mil	Conductivity Mhos Per Centimeter $\times 10^{-16}$	
		Initial	Breakdown	At Maximum Stress	Total Life				Dry 105 Deg	Impregnated 60 Deg
25-*A*	*C*	400	582	4.0	52.0	0.00396	0.1446	681	64.3	3.31
25-*B*			546	3.0	43.0	0.00381	0.1388	676	55.1	3.80
25-*C*			618	0.6	56.6	0.00365	0.1315	665	56.4	4.30
26-*A*			546	3.0	43.0	0.00370	0.1322	668	91.6	2.84
26-*B*			658	1.4	65.4	0.00359	0.1296	671	93.8	3.32
26-*C*			582	1.5	49.5	0.00348	0.1230	656		3.81
27-*A*			599	0.4	52.4	0.00359	0.1280	655	32.6	2.88
27-*B*			618	1.7	57.7	0.00353	0.1266	662	75.5	2.87
27-*C*			639	2.3	62.3	0.00340	0.1181	656	46.5	3.32
Average		400	599	2.0	53.5	0.00363	0.1302	665	64.5	3.38
Maximum deviation		0	9.85 per cent					*K* = 4.73		
Average deviation			5.17 per cent							
Average	*A*	400	820	1.1	93.5	0.00268	0.0768	612	14.7	1.65
Maximum deviation		0	6.34 per cent					*K* = 3.78		
Average deviation			3.24 per cent							
Average	*B*	400	745	1.8	81.5	0.00336	0.1102	690	43.6	2.46
Maximum deviation		0	6.31 per cent					*K* = 4.29		
Average deviation			3.56 per cent							
Average	*D*	400	573	2.9	48.9	0.00442	0.1670	778	40.7	4.80
Maximum deviation		0	7.86 per cent					*K* = 4.91		
Average deviation			4.18 per cent							
Average	*E*	400	805	1.6	91.9	0.00307	0.0948	658	8.3	2.81
Maximum deviation		0	7.58 per cent					*K* = 4.08		
Average deviation			3.86 per cent							

function of the density of the paper and it will be seen that there is a uniform rise in power factor with increasing density. Power factor-voltage curves were taken at intervals throughout each life test. They show only slight variations among the various specimens as between the beginning and toward the end of a test. A typical series of power factor-voltage curves is shown in figures 5 and 6, together with typical curves of power factor and temperature variations throughout the course of a test. In a few cases a definite rise in power factor was noted during the last hours of a test. In general, power factor behavior throughout the test indicated constancy of value, little if any internal gaseous ionization, and appreciable rise only in a few cases during the short period in which the specimen was approaching breakdown.

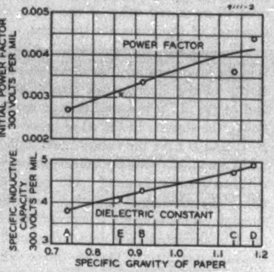

Figure 2. Power factor and dielectric constant as function of the specific gravity of the paper

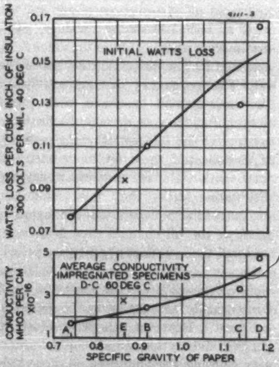

Figure 3. Dielectric loss and long-time d-c conductivity as functions of the specific gravity of the paper

Dielectric Constant. As seen in figure 2 the dielectric constant of the impregnated specimen increases uniformly with increasing density of the paper. On single specimens there was no appreciable change in the value of capacitance either with change of voltage nor over the course of a test.

Dielectric Loss and Conductivity. In figure 3 the dielectric loss at 40 degrees and 300 volts per mil and the long-time d-c conductivity at 60 degrees centigrade, are shown as function of the density of the paper. As indicated, there is a marked increase in both conductivity and loss with increasing paper density.

Oil Content. The oil content of the impregnated paper as per cent by weight and per cent by volume for the different grades of paper is shown in figure 4. It will be seen that the volume of oil absorbed by the several grades of paper follows in linear relation the variation in the density of paper.

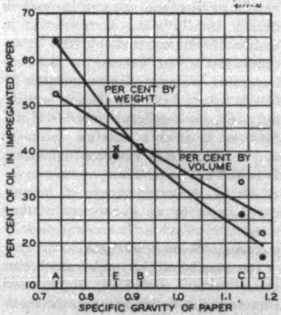

Figure 4. Oil content of impregnated paper by weight and by volume as function of the specific gravity of the paper

Type of Breakdown. All specimens were carefully examined after failure for evidence as to its beginning and cause. In the latter half of the work these studies were greatly facilitated by the use of a thyratron tube, relay, and rapid circuit breaker, for interruption of the primary circuit of the transformer. The combination was actuated by the current to ground from the measuring electrode of the specimen. The operation of this device is so sensitive and rapid that not only is the usual burning attendant upon breakdown almost completely suppressed, but in many cases the circuit is opened before the failure has extended further than two or three layers of paper. By repeated applications or "shots" of voltage, the initial failure can be extended and its path followed. A series of stress values of shots giving repeated interruptions and leading finally to practically complete puncture is as follows: 792, 785, 725, 632 volts per mil; the over-all duration of time for the four shots was about two minutes. In most cases it was found desirable to give the specimen two or three shots to be sure that failure had actually started and to locate definitely its origin.

Failures took place almost invariably under the central electrode, occasionally under the adjacent regions of the guards of equal radial thickness and never, since the early stages, under the conical reinforced ends. All failures have been of definitely radial type.

The clear indication of these studies is that most of the failures begin in the oil channels between successive turns of paper; most often at the central high-voltage electrode, sometimes in the center of the insulation wall, and in a few cases at the outer electrode. In a number of cases in which the specimen was dissected after the first two or three shots, the incipient failure was found to be confined to two or three layers. At one point the oil in a channel would be found to have become black, the blackening extending a short distance either way along the channel and on the edge of the paper. The next stage would be the puncture of one or perhaps two paper layers, but generally reaching the next oil channel on the same radius. Occasionally longitudinal tracking and tree patterns to adjacent channels were noticed, but these extended only a very short distance, probably because of the relatively thin total wall thickness. Evidence of initial blackening in oil spaces was also invariably noticed at the edges of small holes in the central tubular conductor and outer lead foil electrode drilled to facilitate drying and impregnation; however, the failures showed no preference for the edges of these holes.

Altogether the failures seem to be closely similar to those reported by Robinson,[3] with the exception that in our case, the free oil space which is the initial seat of the trouble, may lie in the center of the insulation wall. Robinson finds it to be always next to either the conductor or the sheath.

Open Versus Closed Testing Bath. At the outset the question was raised in the discussion of some of the author's earlier work, as to the reliability of accelerated life tests in a bath open to atmosphere. It was suggested that the absorption of oxygen into the oil of the bath might penetrate to the specimen and thus introduce a cause of instability which would not commonly be present in cable insulation and which it was therefore desirable to exclude in these tests.

In the original choice of the present method of test the adoption of the open bath was based on much independent evidence of the very slow rate at which oxidation of insulating oils takes place at the relatively low temperature of 40 degrees centigrade, and also on the completely enclosed character of the test specimen. These two conditions seemed to indicate that within the period of a few days little or no oxygen could penetrate to the insulation wall of the specimen, and that if it did, its effect on the properties of the insulation would be negligible at the relatively low temperature of the test.

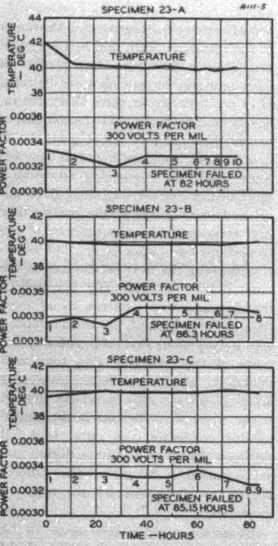

Figure 5. Power factor and temperature variations in the course of a life test

In order to answer this question, and preliminary to the study of paper density, a series of comparative tests was carried out on specimens in open baths and identical specimens completely enclosed in glass. The paper used was a supply of grade *E* (table I) remaining from earlier work. The enclosed specimens were mounted in glass tubes which were open during the process of drying and impregnation, but closed before transfer to the constant temperature test bath.

The results of the tests on eight open and seven closed specimens indicated average values of breakdown voltage closely the same, the difference being well within the experimental error as indicated by the spread of the results on each group. The test in the open bath is much simpler, more convenient, and more rapid, and was therefore continued in the present studies.

Discussion of Results

Breakdown and Life. Under any current theory of the nature of breakdown in insulating materials, an increase in density should be accompanied by an increase in dielectric strength. Most such theories invoke the energy and ionizing power of ions in motion, and since an increase in density usually means a restriction in motion of the ions, it should also be accompanied by an increase in dielectric strength. Widespread experiment is in fact in accord with this theory, particularly for homogeneous materials and for those in which a variation of density may be effected by a variation in pressure. It is well known for example, that an increase in the compactness of impregnated paper by an increase of winding tension or otherwise is accompanied by increase in dielectric strength.

The results of this paper show that under certain conditions, impregnated paper presents an exception to the above mentioned behavior. Variations in density under otherwise uniform conditions may be caused by a variation in the density either of the paper or of the oil, and our present results show that with an increase of the density of the paper by 60 per cent, there results a decrease of dielectric strength of 30 per cent and a life shorter by 47 per cent in our accelerated life tests.

Attention has been called to the evidence that the failures almost invariably begin with the failure of the oil in the interspaces between paper tapes. This suggests at once that an explanation of the results may be found in the increasing dielectric constant of the paper with increasing density, and the consequent transfer of a greater proportion of the over-all stress to the oil. This is, in fact, supported by an approximate computation of the stresses in the oil channels. If we assume that the dielectric constant of the oil is constant at the value 2.15, it is possible, using the over-all measured value of dielectric constant, to compute approximately the dielectric constant of the impregnated paper. Owing to the small volume of free oil space, it is found that the dielectric constant of the impregnated paper itself is about 1.04 times the over-all measured value of the dielectric constant of the complete specimen.

Assuming then that the stress in the oil is in the inverse ratio of the dielectric constants of the impregnated paper itself, and of the oil, it is found that at breakdown, the stresses in the oil of grades *A*, *B*, *C*, and *D* have the values 1,500, 1,550, 1,375, and 1,355 volts per mil respectively, with an average value of 1,445 volts per mil. The maximum deviation from the average value is about 7.3 per cent, which must be considered low under the conditions of experiment. There is thus a definite suggestion over the whole range of papers studied that the specimens fail at approximately the same value of stress in the oil channels.

However, the values of oil stress given above are very high, in fact well above the values at which disruption and breakdown are known to occur in oils. The disruptive strength of carefully purified and dried oils is in the neighborhood of 1,000 volts per mil. Photographic evidence of initial discharges in oil-impregnated paper are reported by Gemant[4] as beginning in the neighborhood of 560 volts per mil. Initial collision ionization causing increased conductivity begins at 300 volts per mil. C. V O. Terwilliger,[5] working in the author's laboratory, has recently reported initial discharges in thin films of carefully dried oil at stresses as low as 60 volts per mil. It may be assumed, therefore, that the computed values of oil stress given above are too high as initial breakdown values. The explanation possibly lies in the value 2.15, assumed for the dielectric constant of the oil. The increase of the conductivity of the impregnated specimen with paper density indicates an increase in the ionic content of the oil (see following section). If, as indicated, these ions move back and forth within the oil channels under the alternating stress, they will accumulate as space-charge layers on the opposite faces of the oil channel each half-cycle, and will thus in effect constitute a polarization in addition to the atomic polarization (corresponding to the value 2.15), and consequently an increase in the dielectric constant. However, an increase of 50 per cent due to this cause would be necessary to bring the computed oil stress values down to the highest disruptive values reviewed above. This is a large increase for the type of cause suggested, but, in view of the very high stresses undoubtedly present, it may be considered as a possible explanation.

Further, it is to be noted also that there is a general trend downward of the values of stress in the oil at breakdown with increasing paper density. This tendency is accentuated if the value for paper *E* (oil stress at breakdown 1,580 volts per mil) is included. Discussion in the following section on conductivity points out that the variation in the conductivity may also be a contributory cause in breakdown, and of such character as to explain the decrease in the critical oil stress with increase of paper density.

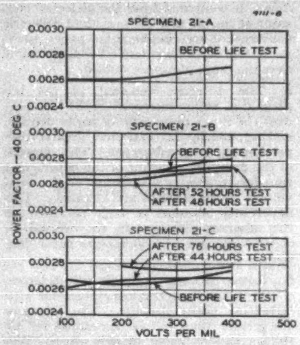

Figure 6. Power factor-voltage relations in the course of a life test

Conductivity. Great interest attaches to the change in value of conductivity with increasing paper density. In figure 3 it is seen that the long-time d-c conductivity at 60 degrees of the impregnated specimen increases about 2.6 times from *A* to *D* paper. Within this same range the mass of paper fiber increases 1.6 times and the mass of oil in the paper decreases to 0.30 times the amount in *A* paper. It is evident, therefore, that the increase in conductivity is greater than can be accounted for by the increase in density.

Two sources of the additional supply of conduction ions suggest themselves. One is soluble material of electrolytic character entering the oil from contact with the paper. The other is residual moisture adsorbed on the paper fibers. The residual conductivity of the dried and evacuated paper measured at 105 degrees centigrade just before impregnation, apparently increases about 2.2 times from paper *A* to paper *D*, that is, over the range of density of the paper. Measurements of the d-c conductivity of different samples of the same dry paper vary widely and cannot be considered as completely reliable indication, but the measurements in question clearly indicated an increase in the average values by the amount mentioned. We may assume that on impregnation some of the adsorbed ions are released by the presence of the oil, entering the latter where they have greater freedom of motion and thereby increase the conductivity of the specimen. With increasing density of the paper, the amounts of both soluble material and adsorbed surface moisture are increased, and thereby the numbers of ions and resulting conductivities as observed.

Some of the original supply of dissolved and adsorbed ions find their way into the oil in the oil channels between the paper tapes. Here they oscillate back and forth under the alternating stress. Their velocity of motion is quite high (of the order 10^{-4} centimeters per second per volt per centimeter)[7] and great enough to cause practically complete passage of all ions present back and forth over the whole thickness of the channel, in one 60-cycle period. It seems possible, therefore, that this large supply of ions in continuous oscillation, either through heating in minute channels, or perhaps better through the formation of space charges and resulting high electric surface intensities, may be the cause of initial breakdown in the oil channels as observed in the tests. Since the conductivities of both dry and impregnated specimens of grade *D* paper are greater than those from grade *A* paper, we should expect under this view a lower oil stress at failure for *D* paper than for *A* paper, as found in the computed values given above.

In this connection attention may be drawn to the properties of the grade *E* paper. As seen in figure 1, both life and breakdown stress of this paper lie above the curves showing the results on papers *A* to *D*. The *E* paper is over five years old, having been stored since some earlier studies. There is also other evidence that the breakdown strength of *E* paper has increased over the storage period. Its particular interest in the present connection is the very low value of the conductivity of the dry paper specimens *E* before impregnation, as compared with the values found for the new papers *A* to *D*. The increased dielectric strength of this paper is thus perhaps accounted for by a slow loss of adsorbed ions over its long storage period.

Dielectric Loss. The dielectric loss increases 2.2 times from paper *A* to paper *D*. This is also greater than the increase in both dry paper density (1.6) and impregnated-paper density (1.38). This result is to be expected in view of the changes in conductivity discussed above. An increase in the number of ions liberated in the oil is attended by increased conductivity, increased dielectric absorption (interfacial polarization), and consequently by increased dielectric loss.

Power Factor. The power factor increases about 1.67 times over the density range *A* to *D*. This increase is less than that of the dielectric loss owing to the increase of the capacitance of the specimen with increasing paper density.

Dielectric Constant. The increase of dielectric constant over the density range *A* to *D* (see figure 2) is about 1.31 times (3.78 to 4.91). This is roughly in the same proportion as the over-all increase in the density of the impregnated paper.

Knowing the relative volumes of oil and of paper and assuming the dielectric constants to be 2.2 for the oil and 5.5 for the paper, the computed values of their combination from papers *A* to *D* are 3.08 to 3.97, if oil and paper are assumed to be in series, and 3.75 to 4.29 if oil and paper are assumed to be in parallel. The proper computed value is probably somewhere in between, giving say the average values 3.41 to 4.13 as between papers *A* and *D*. The measured values are thus substantially above the computed values.

The increase in the dielectric constant due to dielectric absorption or interfacial polarization at 60 cycles in thoroughly dried paper is only about one per cent of the geometric capacitance.[8] Data as to this increase in impregnated paper are wanting, but it is probable that the increase would be of about the same order. It appears, therefore, that in our present case the measured values of dielectric constant represent an increase over the range of density *A* to *D* which is greater than can be accounted for by geometric capacitance and dielectric absorption.

In dry paper a considerable proportion of the value of the dielectric constant is due to restricted adsorbed moisture and possibly to the motion of adsorbed ions on the surface of the paper fiber. On impregnation, these ions probably have greater freedom of motion. Some of them may be converted to conduction ions in the oil, which oscillate as space charges in the alternating field. In either case the contribution to the dielectric constant of the paper is increased, resulting in the higher measured values of the impregnated specimen as found.

Original Cause of Breakdown. As already seen initial failure begins almost invariably in the oil channels. The over-all stress at failure decreases with increasing paper density, but the stress in the oil channels at failure is approximately the same over the whole range of paper density. Increased values of stress are possible within the oil channels, due to the increased number of ions derived from the soluble and adsorbed ions in the dry paper. These ions move back and forth in clouds within the oil channels forming space charges each half cycle on opposite faces of the channel. The presence of these space charges greatly increases the local stresses at the interface between oil and paper. These stresses cause initial electric breakdown, or local thermal paths sufficient to begin the liberation of gas, and consequent spread of breakdown through the well-known processes of gaseous ionization.

Stability and Life. As stated above, initial breakdown at maximum stress is followed very promptly by complete breakdown on repeated application of voltage. Moreover these repeated values of breakdown voltage are progressively lower in value. In other words, when initial failure has once set in, the time element is practically absent in the further progress to failure.

On the other hand, the specimens frequently stand up for several hours at the maximum stress. And further, the computed values, corresponding to breakdown voltage, of oil stress in the channels, where the breakdown invariably starts, are far above the range of values at which collision, ionization, discharge, and breakdown are known to occur in oil. Both these facts strongly suggest the presence of a time element. Instability sets in at an earlier value of stress and its continued progress only results in failure at the higher value of stress.

The accelerated tests here reported, therefore, partake more of the character of dielectric-strength tests than life tests. On the other hand, it is strongly suggested that deterioration has already set in at a lower value of stress than that present at complete failure. A good test of relative stability would probably result if the increase of voltage were stopped at say 75 per cent of the maximum breakdown values as found above, and the test continued at that value.

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Cordial thanks are also extended to the Union Mills Paper Company, The Sun Oil Company, the Locke Insulator Corporation, and to the Insulated Paper Cable Engineers Association for material and other forms of assistance furnished.

The burden of experimental work has fallen on Research Assistants R. B. Kaufman and C. O. Newman to whose skill and devotion much of the success of the work is due.

Summary and Conclusions

1. Accelerated life tests of a particular type as described in the paper have been carried out on samples of impregnated paper insulation. Four grades of paper were studied with a range of specific gravity 0.74 to 1.18, and all of approximately the same thickness, 0.005 inch. All specimens were impregnated with a very fluid insulating oil.

2. In accelerated life tests beginning at 400 volts per mil and with geometric increase of voltage at the rate of 3.12 per cent every four hours, the 60-cycle breakdown voltage was found to decrease with increasing density of the paper. Within the range of specific gravity mentioned, the decrease in breakdown voltage with increase of specific gravity was 30 per cent. The decrease in life was 47.5 per cent.

3. The failures almost invariably start in the oil channels between successive convolutions of the paper tape.

4. The stress in the oil channels at breakdown, as computed from the dielectric constant of the oil, and the measured dielectric constant of the impregnated-paper specimen, had approximately the same value over the range of papers studied.

5. The dielectric constant of the impregnated paper increases by 31 per cent over the range of increase of paper density, and apparently in somewhat greater proportion than as computed. The increase of the dielectric constant of the paper tapes transfers a greater proportion of the total voltage to the oil films and channels. This apparently accounts in large measure for the observed decrease in the over-all dielectric strength with increasing paper density.

6. Power factor, conductivity, dielectric constant, and dielectric loss all increase with increasing paper density. The increases are greater than can be accounted for by the increase in paper density alone. Discussion is presented suggesting that the liberation into the oil of ions adsorbed on, or contained in the fiber structure of the paper may account for increased loss and increased conductivity and may also contribute to the lowering of dielectric strength with increasing density.

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Discussion

For discussion see page 1085.

Tests on Oil-Impregnated Paper—IV

Mechanism of Breakdown

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I. Introduction

IN three previous papers[1,2,3] we have presented some of the results of our studies on miniature cable specimens started in 1934. In the first two papers the relative life histories and the results of post-mortem examinations were given for (*a*) specimens saturated with gas at one atmosphere pressure, (*b*) specimens saturated with gas at 200 pounds per square inch pressure, and (*c*) specimens, gas free but under one atmosphere liquid pressure. In the third paper we presented data on fluid flow through cable paper and showed that even with high-viscosity impregnants and low-permeability papers there was sufficient flow through our specimens to prevent void formation.

It is the purpose of this paper to present observations regarding the mechanism of failure of the gas-free specimens.

II. Methods of Post-Mortem Examination

Most of the specimens were given only a visual examination after removal from their glass cells. A few selected specimens were given post-mortem examination as indicated in the first paper. However, since the first paper was written considerable improvement has been made in the microchemical technique for determining acid number and copper and lead compounds in solution, as indicated below.

A. Microchemical Determination of the Acid Content of Oil

For the determination of the acid content, approximately 0.06 gram of the oil was weighed out and dissolved in a suitable mixture of benzene and absolute ethyl alcohol, containing phenolphthalein as a titration indicator. To get rid of carbon dioxide without oxidizing the alcohol, the mixture was evaporated on the steam bath at reduced pressure to a volume near 0.5 cubic centimeter. With this small volume, the detection of the end-point was facilitated by the intensification of the color by the glass of the cell bottom. After cooling, the mixture was titrated with $N/10$ KOH (one-tenth normal solution of potassium hydroxide), with a rapid stream of dry hydrogen passing through the titration cell to avoid infiltration of CO_2, which would spoil the titration.

It was found that the acidity of these oils was extremely low, so that the actual amount of $N/10$ KOH solution per titration was around 0.0003 cubic centimeter. A microburette of 0.1 cubic centimeter capacity, graduated to 0.001 cubic centimeter and permitting the estimation of 0.0001 cubic centimeter, was employed. A precise titration technique, including optimum illumination for the determination of the titration end-point, was developed. Whenever the burette was set up for acid determinations, a blank was first run (blank = 0.0001 cubic centimeter approximately), followed by a known sample of 0.1 acid number. The titration end-point can be determined by an experienced operator to within 0.0001 cubic centimeter of $N/10$ KOH solution.

The acid content of oils is here expressed in terms of "acid number", which is the number of milligrams of KOH required for the neutralization of the acid in one gram of the oil.

The acid-number data are presented in table I, the values ranging from less than 0.01 to 0.03. Hence the acidity of all of these samples is very low. The accuracy of the acid-number determination is about ±0.005. No determinations have been made in those cases in which no values are recorded.

B. Microchemical Methods for Measuring Cu and Pb in Solution[4]

A preliminary survey of the best available microchemical methods for copper and lead determinations showed that "dithizone" (diphenyl thiocarbazone) was the most promising. Dithizone, a dyestuff, is an exceedingly sensitive reagent for several metals, and analytical methods employing it have been developed especially by Doctor H. Fischer in Germany. The smallest amounts (six gamma[*] of lead; four gamma of copper) of metals that he could determine were many times larger, however, than the amounts present in our cable oil samples. It was consequently necessary to carry through an almost complete revision of his analytical methods. We can now detect the presence of 0.1 gamma of either metal.

To achieve this sensitivity, we have made use of the General Electric recording photoelectric spectrophotometer.[5]

In the use of this instrument, the light transmission of the dithizone solution (obtained after the copper or lead treatment) is measured and recorded continuously from 400 to 700 millimicrons. The amount of copper or lead is measured by the relative displacement of the transmission curve, preferably at the most critical wave lengths, which for copper we have found to be at 508 millimicrons, and for lead at 620 millimicrons. These values are interpreted by reference to curves obtained from known amounts of copper and lead.

The following method was developed for getting the copper and lead out of the oil and into aqueous solution. An "ashless" filter paper of low and tested copper and lead content was creased and placed in a cleaned porcelain crucible. From 0.10 to 0.15 gram of the oil was applied to the top of the innermost creases of the filter and weighed. In this manner, the loss of oil by creeping on the sides of the crucible was prevented. The oil and paper were ashed by careful heating on a covered hot plate, the temperature being gradually increased and gentle suction applied to withdraw the oil vapors. The ash was finally treated with a small amount of perchloric acid and made up to the proper concentration in distilled water.

1. *The Copper Determination.* When an acidified aqueous solution of copper salts is shaken with a solution of dithizone, the copper combines with the dithizone, changing the green dithizone into the pink copper compound. We have found it convenient to determine the amount of this pink copper compound without removing the excess of green

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1. For all numbered references, see list at end of paper.

[*] One gamma = 10^{-6} gram.

Table I. Chemical and Electrical Data on Failed

	Portion	ε′	Power Factor	ε′	Power Factor	ε′	Power Factor	ε′	Power Factor
Specimen		307		304		305		225	
Days 22 kv		152		153		153		150	
Days 30 kv		0.01		1.6		33		123	
Oil		*A-26d*		*A-26a*		*A-26b*		Used	
Paper		*D*		*D*		*D*		*D*	
Treatment									
Test no. 1—ε′ and power factor. Paper plus oil	*A*	2.78↑	0.0545	3.32↑	0.038	3.48↑	0.0219	3.01↑	0.0485
	B	3.64	0.0488	3.28	0.0358	3.39	0.0097	2.91	0.0402
	C	3.43↓	0.0485	3.28	0.0267	3.44	0.0317	3.29	0.0467
	D	3.29	0.0313	3.39	0.0423	3.07	0.0401	3.87↓	0.0474
	E								
	F								
	G	3.36	0.0233	3.37↓	0.0125	3.11↓	0.0186	3.27	0.0478
	H	3.25	0.0281	3.39	0.0176	3.37	0.0081	3.21	0.0456
	I	3.32	0.0255	3.35	0.0267	3.34	0.0145	3.23	0.0443
	J	3.34	0.0256	3.57	0.0437	3.02	0.0198	3.05	0.0444
Test no. 2—ε′ and power factor. Oil alone	1	2.17	0.015	2.15	0.00132	2.23	0.0106	2.18	0.0467
	2	2.19	0.0074	2.16	0.0048	2.24	0.0050	2.20	0.0460
	3	2.19	0.010	2.17	0.00258	2.22	0.0050	2.20	0.0474
	4	2.19	0.0074	2.21	0.00483	2.23	0.0027	2.24	0.0331
Test no. 3—Acid; mg KOH per g oil	1	0.02		0.03				0.02	
	2			0.02				0.02	
	3							0.01	
	4							0.02	
		Cu	Pb	Cu	Pb	Cu	Pb	Cu	Pb
Test no. 7—Cu and Pb in oil; g×10⁻⁸, per g oil	1	3	2	2	2	1	1	13	7
	2	1	2	1	1			13	7
	3	3	1	3	1			12	7
	4	4	2					6	1
Test no. 8—Cu and Pb in paper; g×10⁻⁸, per g paper	1	360	3			3,270	1,710	228	180
	2					53	8	34	4
	3							72	50

The vertical line in test number 1 indicates the number of layers showing numerous punctures resulting from general ionization in oil between layers. Where no vertical line is shown there was no evidence of this general ionization.
In tests numbers 2, 3, and 7, portion 4 is from the oil in the cell surrounding the specimen.

* Layer full of carbonized spots.

dithizone. The colors of these CCl_4 layers varied from green (little or no copper) to pink (3.5 gamma of copper; little or no dithizone). The actual extraction of the copper was carried out in volumes near one cubic centimeter; the CCl_4 layer thus obtained was then diluted to 27 cubic centimeters and the transmission measured by the spectrophotometer. The strength of the dithizone reagent was always adjusted so that its CCl_4 solution transmitted approximately 70 per cent of the incident light at a wave length of 508 millimicrons. Corrections for the copper in the paper and reagents are applied in the final calculation. The determination is accurate to approximately one gamma of copper.

2. *The Lead Determination.* In the copper determination the lead present does not combine with the dithizone, but remains in the aqueous layer. The latter is transferred to a flask (made of nonlead glass) and made alkaline with ammonia, then shaken with the dithizone solution. The lead unites with the dithizone to produce a pink CCl_4 solution. Further shakings with potassium cyanide assure the elimination of any other metals and the removal of the excess dithizone.

The sensitivity of the determination is considerably improved by converting the pink to green by a final acidification. Hence the light transmission of this green solution is measured in the spectrophotometer and the lead concentration calculated. Corrections are applied for the lead in the paper and in the reagents. The determination is accurate to approximately one gamma of lead.

3. *The Determination of Copper and Lead in Paper Samples.* In the cases of nine of the samples, portions of the cable

Table IA. Chemical and Electrical Data on Three Longest-Life Specimens

Specimen		198	400*A*	248	250
Days 22 kv		186	+ 31 = 217	156	156
Days 30 kv		169	+ 334 = 503	645	781+
Oil		*A*	*A*	*A-20a*	*A-20a*
Paper		*D*	*D*	*D*	*D*
Treatment				(Filtered oil)	(Filtered oil, washed paper)
P.F. (power factor) at 60 deg C, 60 cycles per second	first	0.015	0.014	0.009	0.004
	last	0.016	0.057	0.068	0.118

	Portion		ε′	Power Factor	ε′	Power Factor	ε′	Power Factor
Test no. 1—ε′ and power factor. Paper plus oil	*A*		4.45	0.011	2.96	0.0225	2.63	0.014
	B		4.32	0.011	3.18	0.0192	3.13	0.0165
	C		4.25	0.01	3.03	0.0182	3.42	0.012
	D		4.48	0.01	2.97	0.0180	3.29	0.015
	E				3.25	0.0177		
	F				2.88	0.0120		
	G		4.67	0.009	3.18	0.0084	3.36	0.0145
	H		4.28	0.009	2.89	0.0139	3.50	0.010
	I		4.86	0.009	3.06	0.0276	2.89	0.011
	J		4.92	0.01			4.27	0.119
			Cu	Pb	Cu	Pb	Cu	Pb
Test no. 8—Cu and Pb	*A*	Specimen was removed from test before failure, benzene extracted, rebuilt, and returned to test as number 400*A*	5,790	650	9,730	330	7,430	390
	B		360	200	640	<9	1,110	120
	E		400	<5	290	<13	920	130
	F						750	480
	I				360	290		
	J		220	4,970	660	3,110	2,400	29,700

Specimens Arranged in Order of Life at 30 Kv

333		245		313		273		348		243	
149		149		170+		162		4.3		149	
138		160		212		374		435		521	
A-22		*B*		*A*-26*a*		*L*		*A*-30*a*		*A*-22	
D-1		*D*		*D*		*D*		*D*		*D*	
		Tap H_2O wash		H_2O wash		H_2O wash		Hot H_2O wash		Tap H_2O wash	
ε′	Power Factor	ε′	Power Factor	ε′	Power Factor	ε′	Power Factor	ε′	Power Factor	ε′	Power Factor
3.37 ↑	0.0510	2.97	0.0124	2.77	0.0215	2.93	0.0097	3.07	0.0173	2.95	0.0139
3.29	0.0524	2.69	0.0108	3.06	0.0185	2.68	0.0092	3.17	0.0163	3.39	0.0075
2.84	0.0254	3.16	0.0089	3.07	0.0175	2.99	0.0000	3.26	0.0167	3.40	0.0052
2.77	0.0267	2.94	0.0076	3.01	0.0205	3.08	0.0118	3.48	0.0141	3.25	0.0064
3.14	0.0280										
3.23 ↓	0.0305										
3.14	0.0297	3.12	0.0064			3.18	0.0099	3.54	0.0157	3.20	0.0061
3.18	0.0250	3.15	0.0046	2.89	0.020	2.96	0.0089	3.38	0.0131	3.30	0.0065
3.25	0.0302	2.85	0.0085	2.46	0.0535	3.30	0.0081	3.26	0.0310	3.41	0.0184
3.22	0.0806	2.82	0.0094	2.00	0.0875	3.10	0.0086	3.59	0.0527	3.41	0.0185
2.15	0.0298	2.11	0.0008	2.13	0.206	2.11	0.008	2.19	0.0112	2.20	0.0001
2.16	0.0318	2.12	0.0006	2.18	0.0002	2.11	0.0010	2.19	0.0128	2.20	0.0001
2.17	0.0318	2.11	0.0006	2.00	0.97*	2.11	0.0010	2.19	0.0124	2.20	0.0001
2.15	0.0332	2.15	0.0028			2.14	0.0082	2.22	0.0145	2.22	0.0065
0.01		0.01		0.02		0.01					
0.01		0.02		0.02		0.01					
0.01		0.01		0.02		0.01					
0.01		0.02		0.03		0.01					
Cu	Pb	Cu	Pb	Cu	Pb	Cu	Pb	Cu	Pb	Cu	Pb
22	1	4	1	4	6	6	1	5	3	1	1
10	1	11	4	2	3	1	1	7	2	1	1
5	1	5	2	4	3	4	2	2	1	2	1
8	1	1	1	8	18	1	1	3	1	2	1
				502	113			437	428	757	3
				46	3			28	6	34	3
				52	273			85	25	143	7,680

test papers, after centrifuging, were tested for copper and lead in the same manner as were the oils. A strip of paper approximately 0.25 inch wide and one inch long (weighing about 0.03 gram) was cut out laterally in each case, weighed, ashed, and treated with dithizone.

The brownish-black striations observable on the number 1 papers (copper end) persisted as black striations in the ash.

III. Post-Mortem Chemical and Electrical Data on Ten Specimens

Table I gives the results of very careful microexamination of selected specimens having a range of life from 0 to 781 days at 30 kv. All measurements were made as soon as possible after opening the cell. The electrical measurements were made in the manner previously described.[1] In test number 1 electrical measurements were made of a one-inch strip of oil-soaked paper cut from the test section as far as possible from the puncture, extending from layer *A* next to the inner copper electrode to layer *J* next to the outer lead electrode.

The vertical line indicates the number of layers showing numerous punctures resulting from general ionization in the oil between layers. In general, layers showing visual effects of ionization also showed high power factor even though the portion tested contained no carbonized spots. (See discussion of figure 3 and figures 7, 8, and 9.)

Test number 2 shows electrical measurements made on portions of oil centrifuged from (1) the inner two layers next to the copper, (2) the center layers, and (3) the outer two layers next to the lead.

The technique previously used for the acid-number measurements (test number 3) has been considerably refined as already indicated. Tests numbers 4, 5, and 6 used in the 1935 studies[1] were not used because the previous results were of so little value. The new colorimetric microchemical methods described earlier in this paper for quantitative determination of Cu and Pb in test number 7 are about 100 times as sensitive as the spectroscopic methods previously described.[1] Also in addition to determining the metals in solution in the centrifuged portions of oil, test number 8 has been added in which the metal content has been determined for portions of paper from (1) the inner layers next to the copper, (2) the middle layer, and (3) the outer layer next to the lead.

A study of table I leads to the following conclusions concerning chemical changes during life.

1. Acid number of all specimens examined was extremely low, even including specimen number 225 containing used oil. Therefore, either no acids are formed in the absence of oxygen during these extreme life conditions or, if formed, they react with the metals as rapidly as they are produced.

2. Portions of centrifuged oil contain only very low concentrations of either Cu or Pb and in a given specimen the concentration of metal soaps dissolved in the oil is fairly uniform. This may indicate saturation conditions for metal soaps dissolved in each specimen.

3. The paper, on the other hand, contained large amounts of both metals particularly in the inner and outer layers. The presence of copper on the paper in contact with the Cu electrode and the presence of Pb on paper in contact with the electrode might be explained by the adhesion of free metal particles. However, this assumption will not explain the finding of large amounts of copper in the layer next to the lead electrode and of lead in the layer next to the copper electrode in all of the long-life specimens examined, nor will it explain the presence of lower concentrations of both metals in the intermediate layer. New samples of papers *D* and *M* were checked for metal content and no detectable amounts of either were found ($<1\times10^{-6}$ gram per gram paper).

4. The last two observations lead to the

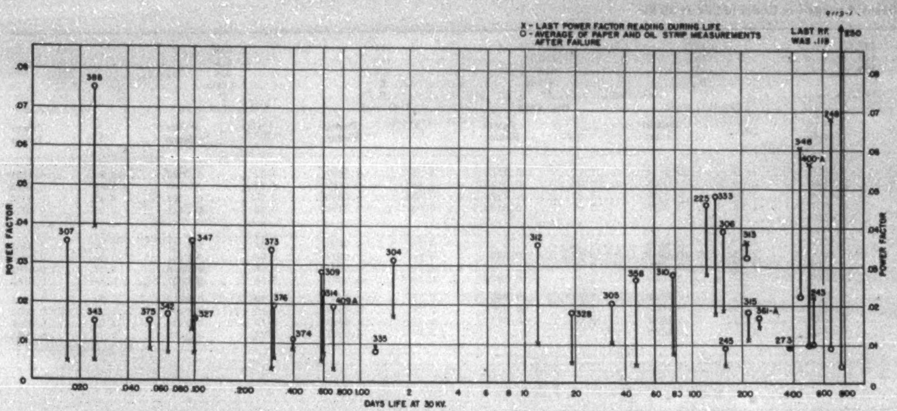


Figure 1. Study of life versus dielectric loss before and after failure

conclusion that although the concentration of metal soaps in oil is normally low, these compounds diffuse through the specimen because of temperature cycles and the electric field and are deposited in the layers of paper next to the electrodes.

In a further attempt to determine whether increasing dielectric losses during life result from deterioration of paper or chemical changes in the oil, strips of oil-soaked paper from several specimens were benzene extracted after the usual post-mortem electrical measurements. These were measured dry and then three of the strips were reimpregnated with new oil *A* and measured again. The results are shown in table II. Too few data have been obtained to warrant definite conclusions. However, the materials responsible for high losses and partly removed by benzene extraction probably resulted from chemical changes in the oil rather than the paper. On the other hand, the power factor of the layers of paper next to the metals remained higher than for new paper both after extraction and after reimpregnation. Thus high-loss materials which are insoluble in benzene must have been formed or electrolytically deposited in these layers of paper.

IV. Post-Mortem Electrical Data on 30 Specimens

In addition to the specimens shown in table I in which separate studies were made of the inner, middle, and outer layers, electrical measurements were made on bulk oil and on oil-soaked paper strips from 20 additional specimens. Figure 1 has been plotted from these electrical data to study any possible correlation between power factor and life. All specimens on which post-mortem electrical measurements were made that had a life at 30 kv greater than 0.01 day have been plotted. To show gradual changes of power factor with time, the life histories of the six longest-life specimens have been plotted in figure 2. A study of these data leads to the following conclusions.

1. There is no correlation between power factor before failure and life.

2. In all but the specimens having longest life (313, 273, 348, 400*A*, 243, 248, and 250) the post-mortem power factor was considerably higher than the last life measurement, indicating that the deterioration leading to failure occurred during a relatively short time.

3. In specimens 313, 348, 400*A*, 243, 248, and 250, which had very long life, the post-mortem strip measurement was lower than the last life measurement. All of them showed very gradually increasing power factor during life. None of the longest-life specimens showed general ionization. Also, the strip for post-mortem examination was always taken as far as possible from the puncture. Therefore it is evident that *in these six specimens the cause of failure was gradual local chemical deterioration in the region which eventually failed but not materially affecting the portion later used for the strip measurements.*

V. Visual Examination of 30 Specimens

The most striking result of visual examination is the evidence of what we shall call "general ionization" occurring in the oil spaces between layers of paper and starting at the inner electrode. Typical examples are shown by the photographs reproduced in figures 7, 8, and 9 of specimens 305, 343, 374, and 421, whose make-up and life are given in table III.

Table II. Power-Factor Measurements on Paper Strips

Specimen No.	304			305		313		
Oil	*A*-26*a*			*A*-26*b*		*A*-26*a*		
Paper	*D*			*D*		*D* H₂O wash		
	X	Y	Z	X	Y	X	Y	Z
A	0.038	0.01	0.009	0.022	0.006	0.021	0.005	0.007
B	0.036	0.003	0.005	0.01	0.002	0.018	0.004	0.005
C	0.027	0.005	0.005	0.032	0.004	0.017	0.003	0.004
D	0.042	0.006	0.004	0.04	0.002	0.020	0.003	0.004
E							0.006	0.008
F								
G	0.012	0.005	0.007	0.019	0.005			
H	0.018	0.005	0.006	0.008	0.003	0.02	0.007	0.006
I	0.027	0.005	0.004	0.014	0.003	0.053	0.006	0.011
J	0.044	0.026	0.017	0.02	0.006	0.087	0.015	0.013

Column *X*—Oil-soaked paper as removed from cell.

Column *Y*—Dry measurement after benzene extraction.

Column *Z*—Wet measurement after impregnation with new oil *A*-26.

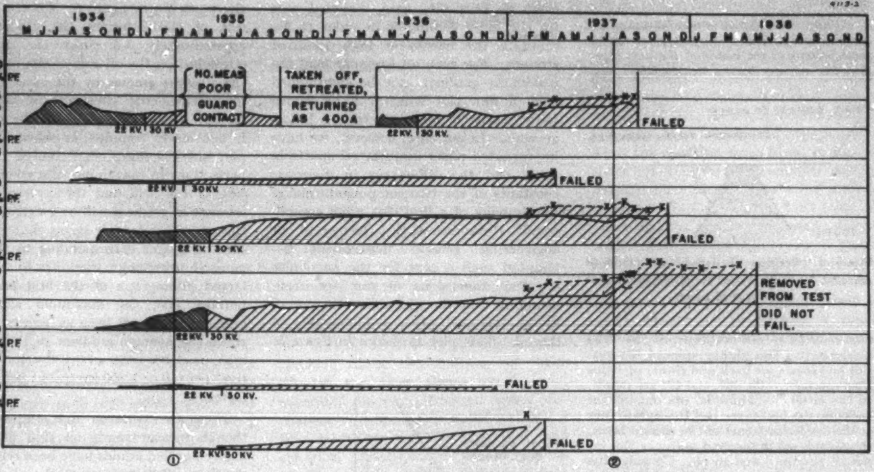


Figure 2. Power-factor life histories of six longest-life specimens

Solid curve—Power factor measured at 6 kv at maximum oven temperature

Crosses—Power factor measured at 30 kv at maximum oven temperature

①—Started daily heating cycles, room temperature to 60 degrees centigrade

②—Raised maximum oven temperature to 100 degrees centigrade

For comparison, the photograph of specimen number 212, which was removed for examination after 146 days at 30 kv, shows no evidence of such ionization. This is also true of 12 out of the 15 specimens having lives over 40 days, as is shown by figure 3, in which are plotted the number of layers, starting from the inner electrode, through which such multiple punctures had occurred.

A careful scrutiny of all specimens leads to the following definite observations:

1. *For every puncture visible in any layer of paper there is a corresponding hole or conducting spot through every intervening layer to the inner electrode*, that is, in no specimen did we find a single carbonized spot which had no conducting path to the inner electrode. It is true that branching paths were numerous, as shown by number 327 and the enlarged photograph of number 374, but these could all be traced back to the inner electrode.

2. *The inside of the first layer, that is, the surface that had been in contact with the inner electrode always shows dark markings corresponding to the lines of contact of the paper and the threads on the inner conductor.* The enlarged photograph of the portion within the circle of number 305 is typical and shows that when the paper was in close contact with the edge of the tooth, the marking occurred as a double line just away from

Table III. Data for Specimens Shown in Figures 7, 8, and 9

Specimen Number	Days Life 22 Kv	Days Life 30 Kv	Impregnant	Paper	Thickness (Inch)	Layers	Density	Gurley Air Flow
212	149+	146+	*A*-22	*K*	0.0065	10	0.85	35
305	153+	33	*A*-26*a*	*D*	0.0065	10	0.75	30
327	153+	0.1	*A*-22	*P*	0.006	10	1.05	150
343	153+	0.07	*A*-22	*Q*	0.0045	15	1.11	330
348	4.3+	435	*A*-20	*D*	0.0065	10	0.75	30
374	170+	0.4	*A*-20	*R*	0.005	13	1.18	670
421	0	31	*A*-26	*M*	0.0065	10	0.75	21

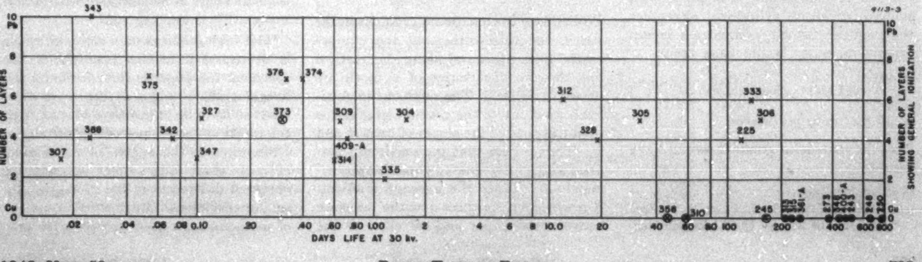


Figure 3. Number of layers through which numerous punctures had been caused by general ionization

each side of the area of contact, as sketched in figure 4.

3. *Where carbonization or wax formation had occurred, the outside of any layer shows the same pattern as the inside of the next layer, these two surfaces having been together, separated only by a hin but finite oil film.* To illustrate this fact photographs of the outside and inside (or back and front) of strips from specimens 305, 327, and 421 are shown. In the latter, for example, the outside (or back) of the first layer has the appearance of the inside (or front) of the second layer, the outside of the second matches the inside of the third and so on. Consider the spot marked (1) on the inside of the third layer. The corresponding spot on the outside of this same layer is marked (3) but corresponds in appearance to the spot marked (2) on the inside of the fourth layer. The corresponding spot on the outside of this layer is marked (4) and corresponds in appearance to the spot on the inside of the fifth layer, marked (5). Here the pattern stops since this particular incipient failure did not get through the fifth layer of paper before the specimen finally punctured in an entirely different spot. The enlarged photographs of the inside and outside surfaces of the area shown in the circle on specimen number 343 illustrate this same observation.

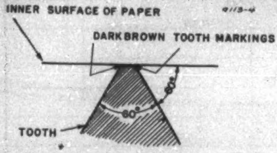


Figure 4. Position of dark-brown markings adjacent to tooth edge on inner surface of first layer of paper

4. *In many specimens carbonization appeared to have occurred over considerably larger areas in layers away from the electrodes than in layers adjacent to the electrodes.* This is illustrated to some extent by all the specimens shown.

5. *The outer five or six layers were usually punctured by but a single hole with little evidence of progressive carbonization.* This also is illustrated by all the specimens shown.

VI. Maximum Electrical Gradient

If our specimens had been made with smooth concentric cylindrical electrodes the average gradient, assuming a homogeneous dielectric, would have been

$$\frac{30{,}000}{65}=461 \text{ volts (effective)/mil}$$

or 257 kv (peak)/centimeter.

Also, the maximum gradient would have been

$$G_1=E/r_1 \log_e (r_2/r_1)$$
$$=30{,}000/187.5\times 0.3=533 \text{ volts (effective)/mil or 297 kv (peak)/centimeter.}$$

In designing the test specimen, however, we purposely used threaded rods for the inner conductor in order to accentuate the effects of high localized stresses. For such an irregular field the maximum gradient can be determined from a flux plot which can be drawn fairly easily if the dielectric is homogeneous. In our case, however, we have impregnated paper and free oil spaces in series and the differences in dielectric constants of the two components makes the drawing of a flux plot more complicated. Nevertheless, S. I. Pierson of our engineering general department attempted such a plot for the particular physical dimensions of our specimen, assuming that the dielectric constant of the impregnated paper is double that of the oil. This plot is shown in figure 5.

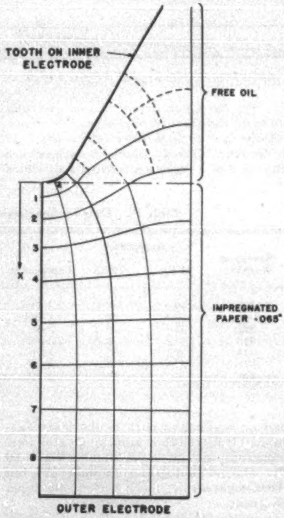


Figure 5. Flux plot for determining gradient

To estimate the maximum gradient in the paper, the intersections of the equipotential lines measured along the vertical line through the center of a tooth are drawn in figure 6. The ratio of the maximum gradient to the average gradient is then the ratio of the slopes of lines *A* and *B*. Thus we see that the maximum gradient in the impregnated paper is approximately 3.5 times the average gradient. Now consider the point *a* at the interface between the paper and oil close to the point of contact between the paper and the tooth tip in figure 5. On the paper side of this interface the gradient is approximately 3.5 times the average stress but on the oil side of the interface the stress is greater by the inverse ratio of the dielectric constants of the two media. Again if the edge of the thread is not nicely rounded as shown in the plot but has sharp edges caused by imperfections in machining, the concentration of the field and the corresponding increase in gradient will be greatly accentuated. Therefore, it seems that for the thin oil spaces corresponding to the regions showing color markings in the enlarged photograph of the first layer in number 305, the maximum electrical gradient may have been as high as seven times the average gradient in the specimen, or of the order of 3,200 volts (effective) per mil or 1,800 kv (peak) per centimeter.

These are extremely high gradients although measurements on thin films of highly purified liquids have been reported showing dielectric strengths between 1,400 and 1,800 kv (peak) per centimeter.[7,8] Also, in some unpublished experiments we have obtained dielectric strengths of the order of 1,200 kv (peak) per centimeter in 0.025-inch gaps.

At some fairly definite high gradients it is evident that even gas-free liquids break down by ionization (so-called), but little is known about the actual mechanism and the controlling factors. From work on crystals, Von Hippel[9] states that "Our picture of the breakdown mechanism is developed for the condensed phases in general. It covers, therefore, the liquid insulators too, but the high mobility of the particles will certainly bring some new features in the situation. If suddenly space charges form by electron emission or impact ionization in the neighborhood of the electrodes, the electrostatic repulsion may overcome the surface tension and bubbles may form in this manner without the presence of occluded gases." If this hypothesis is correct there is little argument about subsequent steps in the breakdown mechanism.

This high gradient on a series oil space is decreased if in the assembly of the specimen the paper is forced around the irregular sharp edges of the inner electrode so as to be in intimate contact with the metal at the regions of highest stress. Differences in the degree of enveloping of such sharp edges may account for observed differences in life of supposedly similar specimens. In practice a number of methods have been proposed for pro-

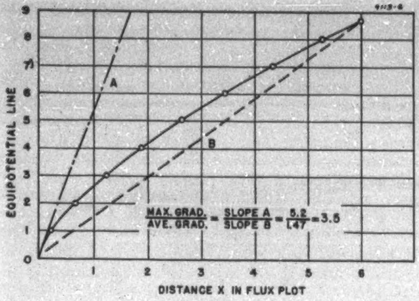


Figure 6. Curves of calculation of maximum gradient from flux plot

viding a smooth cylindrical equipotential surface at the inner conductor, for example, lead sheathing, or covering with copper tape or metalized paper[10] in order to eliminate such high-stress regions and raise the corona starting voltage for a given total thickness of insulation.

VII. Apparent Mechanism of Failure

A number of writers have discussed the mechanism of failure of impregnated paper cables. We shall not attempt a summary of their statements although a few recent references are given.[10–14] Also, an excellent summary of new developments and improvements in high-voltage cables is given in a series of articles in *The Engineer* starting with the August 6, 1937, issue.

From the evidence presented above we believe that the failures of our gas-free oil-filled specimens can be attributed to two distinct and independent causes, the sequence of events being somewhat as follows:

A. Ionization

If the physical conditions are such that the electrical gradient at the surface of the inner electrode is greater than the dielectric strength of the liquid, ionization in the form of localized sparking causes puncture of the first layer of paper at local hot spots. Also, the action of the corona gradually decomposes the impregnating oil, liberating gas and causing condensation to form wax. The effects of electrical bombardment of organic liquids has been given considerable study[15–18] and need not be discussed here. The generation of gas may or may not accelerate the breakdown, depending upon factors such as the rates of evolution and absorption, pressure developed, migration of liquid during heating and cooling cycles, and the like. In any case, progress of the incipient failures in our specimens is dependent upon the burning of holes through the paper sheets by the heat generated in such localized spark discharges. If the paper could withstand the discharges without chemical and electrical deterioration the specimens would not fail. Now a carbonized hole through the first layer of paper constitutes a sharp-pointed extension of the inner electrode and the resulting high electric gradient causes the spark discharges at its terminus to bore progressively through the layers of paper toward the other electrode (electron drilling, as it were). The speed with which this progresses depends upon a number of counterbalancing factors such as the intensity of the discharge, the density of the paper, the quenching action of the impregnant, and the like. The appearance of many of the specimens (see photograph of number 374) indicates that after the spark discharges have punctured several layers of paper the electric field distribution about such a carbonized path is no longer purely radial but has large tangential components. Thus the sparking spreads out in the oil space between two layers of paper causing damage over much wider areas until a weak spot is found in the next layer of paper and the process is repeated. Eventually, however, enough layers of paper will have been broken down to raise the potential gradient across the remaining good layers to the point where a quick failure occurs forming a single small radial puncture through the remaining layers, as shown in the photographs. The mechanism of this final failure is probably thermal since the average gradient is only of the order of 1,000 volts (effective) per mil, which is well below usual values for impulse or disruptive breakdown for oil-impregnated paper.

This entire picture agrees with the conclusions of Doctor Robinson's[10] work as to the mechanism of failure in solid-type cables. However, there are two major differences in conditions. First, we do not assume that ionization started in voids or gas pockets since our specimens were very thoroughly dried and impregnated, being degassed in the process to about one micron pressure. Further-

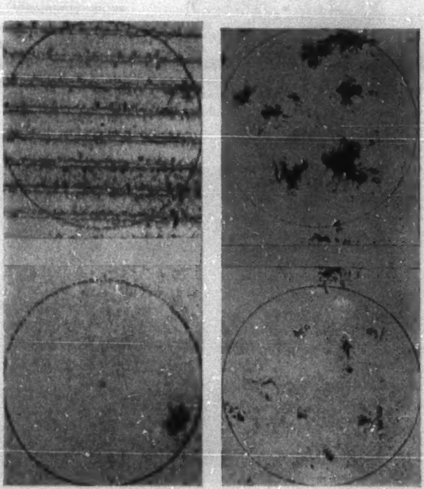


Figure 7

Specimen 305 (left)—Enlargement of inside and outside surface of one-half inch circle in first layer

Specimen 343 (right)—Enlargement of inside and outside surface of one-inch circle in fourth layer

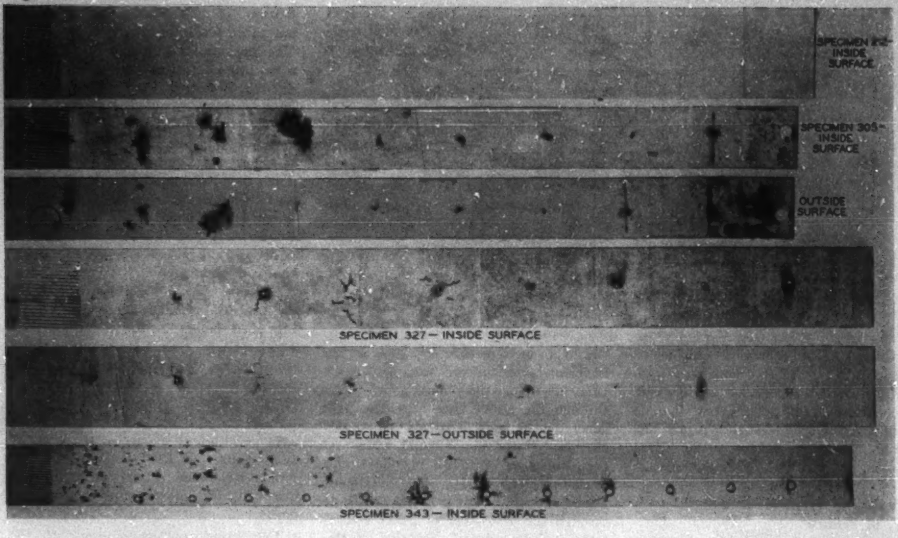
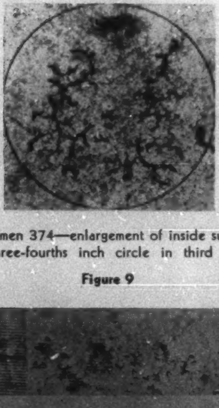

SPECIMEN [illegible]—
INSIDE
SURFACE
SPECIMEN 305—
INSIDE
SURFACE
OUTSIDE
SURFACE
SPECIMEN 327— INSIDE SURFACE
SPECIMEN 327—OUTSIDE SURFACE
SPECIMEN 343— INSIDE SURFACE

Figure 8



Specimen 374—enlargement of inside surface of three-fourths inch circle in third layer

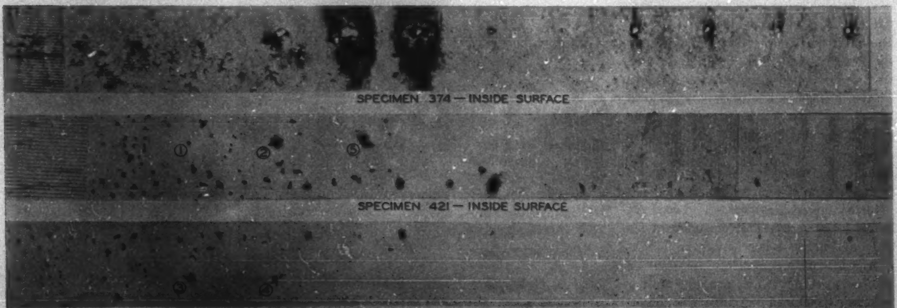
Figure 9

more, they were kept in sealed glass cells during their entire life so that there was no chance for leakage of gas from the outside. Thus, if gaseous ionization occurred, the gas must have been generated within the cell. Second, we used a continuous sheet of paper wrapped around the inner electrode to give the desired total insulation thickness so that there was no chance for creepage around tapes as is commonly found in commercial cable and each layer of paper had to be punctured before the next layer could be attacked by a spark discharge starting at the inner electrode.

With these modifications, the evidence indicates that the great majority of our specimens failed by ionization resulting in spark discharges beginning at the inner electrode and progressively burning through successive layers of paper.

B. Chemical Deterioration

The mechanism just described accounts for the failure of all the specimens reported in this paper except a few of the longest-life specimens (see figure 3). These latter specimens were prepared by special methods so that the inner layers of paper were formed around the sharp edges of the threaded inner electrode, thus putting solid dielectric instead of oil in the regions of highest stress. This difference apparently prevented ionization at the surface of the inner electrode at the operating voltage and al-


SPECIMEN 374—INSIDE SURFACE
①
②
③
SPECIMEN 421—INSIDE SURFACE
SPECIMEN 421—OUTSIDE SURFACE

lowed chemical deterioration to be the ultimate cause of failure. Our picture of this process is as follows:

From part IV, including tables I and II, it is evident that even in the absence of ionization and free oxygen, very slow chemical deterioration occurs under the extreme conditions of temperature and electrical gradient used. This slow deterioration results in gradually increasing dielectric loss in localized regions until such regions become thermally unstable and a typical thermal failure results. That these effects are localized is shown by the fact that the post-mortem strip measurements on specimens 348, 400*A*, 243, 248, and 250 gave lower dielectric loss than that shown by the whole sample before failure (see figure 1). That this chemical deterioration involved metal compounds which migrated through the paper was shown by the copper and lead determinations (see tables I and II).

However, in the absence of ionization and in the absence of free oxygen the rate of chemical deterioration was so slow that the longest-life specimen was subjected to a maximum gradient of at least 1,800 volts (effective) per mil or 1,000 kv (peak) per centimeter for about two years before failure. This is so far above commercial operating conditions that we believe that *in practical service chemical deterioration can only become a contributing factor to failure after extremely long life or when oxygen or oxidized oil is accidentally admitted to the cable.*

VIII. Conclusions

1. Our gas-free oil-impregnated miniature cable specimens were subjected to extremely high electrical gradients. Calculations indicated gradients in the oil at the interfaces between the inner electrode and the paper as high as 3,200 volts (effective) per mil or 1,800 kv (peak) per centimeter.

2. Under these conditions the most important cause of failure was ionization in the oil leading to spark discharges and carbonization of the paper, beginning at the inner electrode.

3. Where special construction was used so as to minimize the maximum gradient in the oil and thus prevent ionization, chemical deterioration gradually caused failure by thermal instability after extremely long life.

4. The fact that carbonized areas were not found which had no direct connection by a conducting path to one of the electrodes indicates either that under the conditions of our tests local ionization does not start within the body of the impregnated paper or that even if it did occur the amount of energy which can be fed to such a spot through the intervening good dielectric is insufficient to cause carbonization. In solid-type cables ionization within the body of the impregnated paper is known to exist because of observed wax formation but under normal voltage such ionization does not lead to failure except as spark discharges start from the electrodes causing progressive carbonization of the paper layers.

This is in agreement with statements made by other students of mechanisms of cable failure (see, for example, pages 26 and 101 of reference 10).

5. These experiments indicate that in commercial gas-free oil-filled cable the usual operating potentials are well below ionization gradients so that ionization failure can only occur if the cable is incompletely degasified when installed or gas is accidentally admitted. Also, chemical deterioration can only become a contributing factor to failure after extremely long life or when oxygen or oxidized oil is accidentally admitted.

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Discussion

For discussion see page 1083.

Dielectric Strength of Insulating Liquids in a Continuously Circulating System

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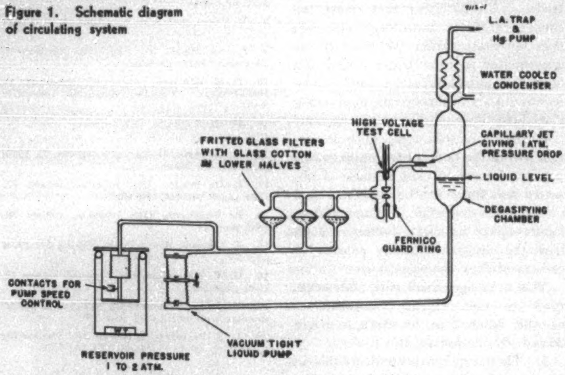
FELLOW AIEE

THE electrical properties of liquid dielectrics have been studied by theoretical and experimental physicists for a long time. Of the various properties one of the most important, and probably the least understood, is the mechanism of electrical breakdown. To become acquainted with the voluminous literature in this field one must read not only monographs[1-4] but also more recent important summaries and experimental contributions.[5-11] One of the important concepts in the past has been that breakdown is initiated by ionization in gas bubbles thrown out of solution or suspension in the liquid. As a new experimental approach to this problem we set up a closed system in which the liquid could be continuously circulated through the dielectric strength cell with filters, dryers, and a degasifier in series in the system so that solid particles, moisture, and dissolved gas could be removed. Arrangements were made so that the partial pressure of gas in solution could be varied from <0.01 micron to one-atmosphere pressure. Another object of setting up a continuously circulating system was to determine whether progressive removal of residual gas, moisture, and floating particles would decrease the mean deviations of successive breakdown measurements. Further, because of the usual large spread in dielectric strength measurements on liquids it was deemed important to obtain a large number of shots so that statistical methods of analysis could be applied to the results. The system was provided with automatic controls, time-delay relays, and a voltage recorder so that periodic breakdown measurements could be made and recorded at predetermined intervals without the tedium of continuous attendance by the operator.

Figure 1. Schematic diagram of circulating system


L.A. TRAP & Hg PUMP
WATER COOLED CONDENSER
HIGH VOLTAGE TEST CELL
CAPILLARY JET GIVING 1 ATM. PRESSURE DROP
FRITTED GLASS FILTERS WITH GLASS COTTON IN LOWER HALVES
LIQUID LEVEL
DEGASIFYING CHAMBER
FERNICO GUARD RING
CONTACTS FOR PUMP SPEED CONTROL
RESERVOIR PRESSURE 1 TO 2 ATM.
VACUUM TIGHT LIQUID PUMP

Apparatus and Experimental Procedure

Circulating System

The system devised to continuously circulate, filter, and degasify the liquid to be tested is shown schematically in figure 1. The pump and speed control reservoir are connected to the glass system by Fernico-glass seals. The vacuum-tight liquid pump was developed by F. S. Cooper of our laboratory and is shown in detail in figure 2. The top and bottom plates are rigidly mounted, while the center oscillates vertically. The valves are made of aluminum and are light enough so that a few centimeters head of oil is sufficient to lift them. With a vertical motion of one centimeter or less the Sylphons should operate indefinitely without mechanical trouble. In order to use a reciprocating pump to give continuous flow in a closed hydraulic system a flexible storage reservoir was provided to maintain a constant pressure on the

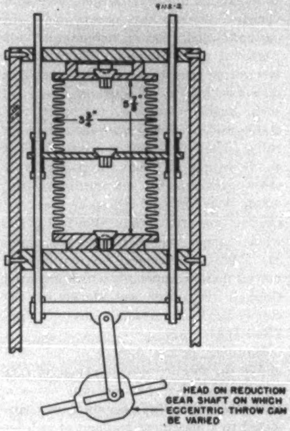

3 3/4"
5 7/8"
HEAD ON REDUCTION GEAR SHAFT ON WHICH ECCENTRIC THROW CAN BE VARIED

Figure 2. Vacuum-tight circulating pump

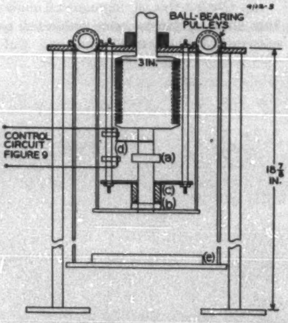

BALL-BEARING PULLEYS
3 IN.
CONTROL CIRCUIT FIGURE 9
(d)
(a)
(c)
(b)
(e)
16 7/8 IN.

Figure 3. Sylphon reservoir for pressure control

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1. For all numbered references, see list at end of paper.

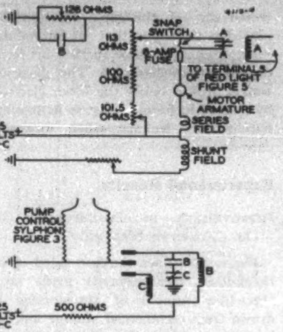

126 OHMS
4113-4
SNAP SWITCH
113 OHMS
A
A
A
6-AMP FUSE
TO TERMINALS OF RED LIGHT FIGURE 5
100 OHMS
MOTOR ARMATURE
101.5 OHMS
SERIES FIELD
125 VOLTS + D-C
SHUNT FIELD
PUMP CONTROL SYLPHON FIGURE 3
B
C
B
C
125 VOLTS + D-C
500 OHMS

Figure 4. Pump speed-control circuit

liquid. The details of this reservoir are shown in figure 3. The vertical rod mounted on the lower end of the Sylphon has collars (*a*) and (*b*) which limit its motion in the fixed guide (*c*). Normally, however, the travel is held within narrow limits by automatically controlling the speed of the pump motor through the contacts (*d*). When the upper contact is made the motor speed is increased. When the lower contact is made the motor speed is decreased again. The wiring diagram of the speed control circuit is shown in figure 4.

Referring to figure 1, the jet into the degasification chamber was designed so that there will be one-atmosphere pressure drop at normal pumping speed. Thus the pump maintains one-atmosphere hydraulic pressure on the liquid passing through the test cell even though a good vacuum is being maintained in the degasification chamber.

For some of the experiments the back pressure of gas in this chamber was increased as high as one atmosphere. To maintain the same rate of flow the hydraulic pressure had to be increased accordingly. This was accomplished by adding lead weights (*e*) on the pan in figure 3, thus adding to the force on the pressure-control reservoir. For back pressures of gas between zero and one atmosphere the weights were adjusted to give approximately the same rate of liquid flow so the hydraulic pressure in the test cell in each case was roughly equal to one atmosphere plus the partial pressure of gas.

ELECTRICAL CIRCUITS

The wiring diagram for the 60-cycle power circuit is shown in figure 5 with the necessary push-button stations and relays for manually or automatically raising and lowering the voltage by means of a motor-driven regulator.

To remove the voltage as quickly as possible after a breakdown occurs, the thyratron trip circuit shown in figure 6 was used.

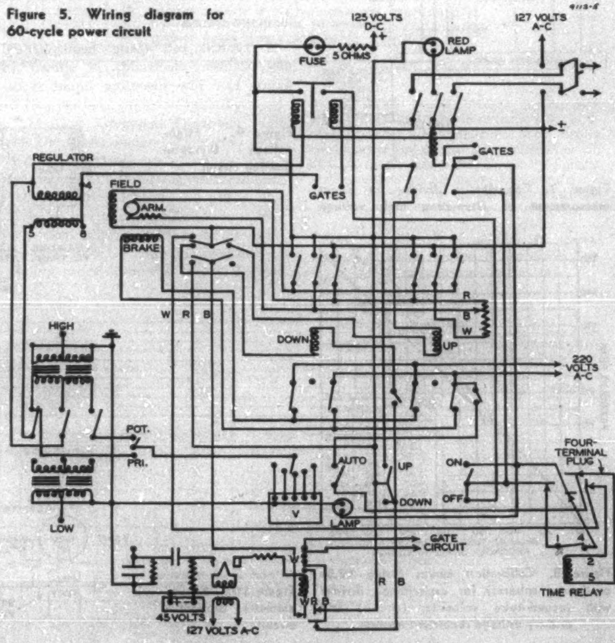
For d-c measurements a 60-kv kenotron was used with a smoothing capacitor consisting of five 0.25-microfarad capacitors in series. A 260-megohm resistor was used to grade the potential along the capacitors to ground and to provide a definite load on the kenotron.

Several items of auxiliary equipment were assembled for use with this set which deserve recording.

Because of the poor regulation of the X-ray transformer used as a power source it seemed imperative to measure the potential applied to the testing cell directly on the high side of the transformer. A capacitance divider was built as shown in figure 7. The spheres are spaced far enough apart so that they will not arc over at the upper limit of voltage E_1 of the set. They are rigidly mounted so that the capacitance to the measuring section is constant. This fixed measuring capacitance C_1 is connected in series with a low-loss capacitor C_2 of such a value that the maximum voltage E_2 across C_2 will be 150 volts by the relation $(E_2/E_1) = (C_1/C_2)$. The voltage E_2 is then measured by a vacuum-tube voltmeter. This scheme is possible because the vacuum-tube voltmeter is essentially an infinite impedance device and can measure E_2 even though the capacitance-charging current is very small. Also the vacuum-tube voltmeter measures peak voltages independent of wave shape or frequency. Calibration curves for the capacitance divider with vacuum-tube voltmeter and the primary voltage recorder are shown in figure 8. This calibration does not apply to data taken previous to October 17, 1937.

When making repeated d-c breakdown measurements the total energy stored in the smoothing capacitors was discharged through the testing cell at each shot. This caused much more gas evolution and carbon formation on direct current than on alternating current and caused successive shots to be increasingly lower unless very long circulating times were allowed between shots. Therefore, the

Figure 5. Wiring diagram for 60-cycle power circuit


125 VOLTS D-C
+
4113-4
127 VOLTS A-C
FUSE
5 OHMS
RED LAMP
±
GATES
REGULATOR
1
4
5
8
FIELD
ARM.
GATES
BRAKE
R
B
W
W
R
B
DOWN
UP
HIGH
220 VOLTS A-C
POT.
PRI.
LOW
AUTO
UP
ON
FOUR-TERMINAL PLUG
V
DOWN
OFF
LAMP
GATE CIRCUIT
1
4
2
5
W
R
B
W R B
45 VOLTS
127 VOLTS A-C
TIME RELAY

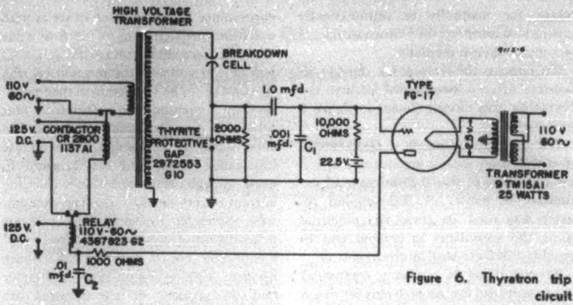

HIGH VOLTAGE TRANSFORMER
BREAKDOWN CELL
110 V 60~
125 V. D.C.
CONTACTOR CR 2800 1137 A1
THYRITE PROTECTIVE GAP 2972553 G10
2000 OHMS
1.0 mfd.
.001 mfd.
C_1
10,000 OHMS
22.5 V.
TYPE FG-17
2.5 V
110 V 60 ~
TRANSFORMER 9 TM 15A1 25 WATTS
125 V. D.C.
RELAY 110 V-60~ 4387823 G2
1000 OHMS
.01 mfd.
C_2

Figure 6. Thyratron trip circuit

high-voltage thyratron shorting circuit shown in figure 9 was assembled to discharge the smoothing capacitors to ground when tripped by the small thyratron tube shown in figure 6. This worked very well except when arc-backs occurred in the high-voltage thyratron.

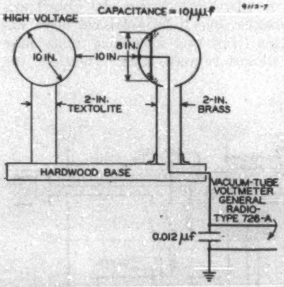
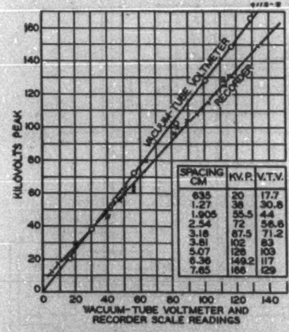

HIGH VOLTAGE
CAPACITANCE = 10μμf
10 IN.
10 IN.
8 IN.
2-IN. TEXTOLITE
2-IN. BRASS
HARDWOOD BASE
VACUUM-TUBE VOLTMETER GENERAL RADIO-TYPE 726-A
0.012 μf

Figure 7. Capacitance divider for direct measurement of alternating high voltage



SPACING CM	KV.P.	V.T.V.
635	20	17.7
1.27	38	30.8
1.905	55.5	44
2.54	72	56.6
3.18	87.5	71.2
3.81	102	83
5.07	128	103
6.36	149.2	117
7.65	166	129

Figure 8. Calibration curves (using 12.5-centimeter spheres) for capacitance divider with vacuum-tube voltmeter (circles) and primary voltage recorder (crosses)

For some of the d-c tests we measured current in the test cell with a sensitive microammeter. Two methods were tried for protecting the instrument if a breakdown occurred while the instrument was connected to the ground side of the test cell. The simplest scheme was to connect a small Thyrite protective gap across the instrument. Another scheme is shown in figure 10. The instrument is protected from steep transients by a filter circuit until the *FG*-17 tube can cause the short-circuiting relay to close, thus shunting the impulse current to ground.

Testing Cells

A typical cell using hemispherical molybdenum electrodes is shown in figure 11. The incoming liquid is directed through the gap. The outlet is placed at the top so that gas formed during a discharge will be carried out to the degasification chamber. The glass cell wall is broken below the lower electrode by a fused-in Fernico ring, so as to eliminate surface leakage when measuring current.

Experimental Results

Improvement in Transformer Oil Caused by Heating

Figure 12 shows the last third of the breakdown measurements made on a three-liter sample of transformer oil drawn from commercial storage and put into the circulating system without any purification other than degasification. In about a week of continuous circulation, 1,380 dielectric strength measurements were made on this sample. One significant observation is that the continuous removal of the products of decomposition by evacuation and filtration leaves the remainder of the liquid as good as ever so that the final measurements were as high as any. My interpretation of the gradual improvement obtained during the first 260 measurements shown in figure 13 is that continued heating gradually removed moisture. This is confirmed by the fact that when the temperature was lowered to 30 degrees centigrade at the end of the run the improvement was retained. A probability plot

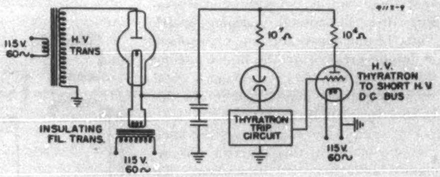

115 V. 60~
H.V TRANS
INSULATING FIL. TRANS.
115 V. 60~
$10^7\Omega$
$10^4\Omega$
THYRATRON TRIP CIRCUIT
H.V. THYRATRON TO SHORT H.V D C BUS
115 V. 60~

Figure 9. High-voltage thyratron shorting circuit

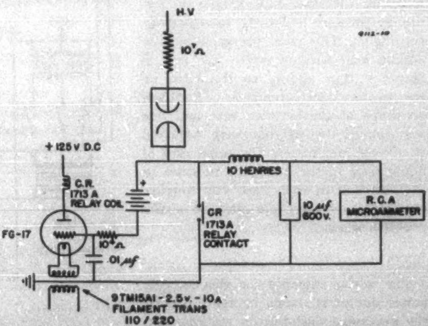

H.V
$10^7\Omega$
+125 V D.C
C.R. 1713 A RELAY COIL
10 HENRIES
10 μf 600 V.
R.C.A MICROAMMETER
CR 1713A RELAY CONTACT
FG-17
$10^4\Omega$
.01 μf
9 TM15A1 - 2.5 V. - 10A FILAMENT TRANS 110 / 220

Figure 10. Microammeter protective circuit

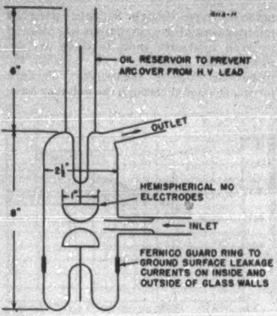

OIL RESERVOIR TO PREVENT ARC OVER FROM H.V. LEAD
OUTLET
HEMISPHERICAL MO ELECTRODES
INLET
FERNICO GUARD RING TO GROUND SURFACE LEAKAGE CURRENTS ON INSIDE AND OUTSIDE OF GLASS WALLS
6"
8"
2½"
1"

Figure 11. Testing cell with hemispherical electrodes

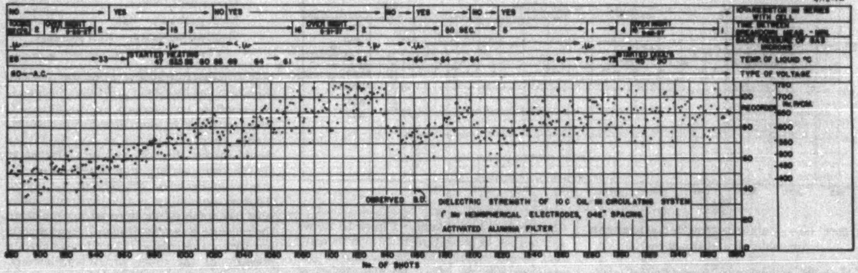


Figure 12. Dielectric-strength measurements on 10-C oil

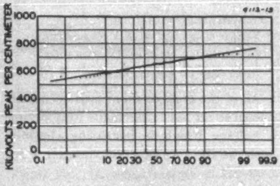


Figure 13. Dielectric-strength data for 10-C oil

Shots 1281 to 1380, 9/19/37 to 9/23/37
Kilovolts peak per centimeter:
Maximum—750
Most probable—650
Standard deviation—7.4 per cent

of the last 99 measurements is shown in figure 13.

Decomposition products resulting from one arc have a marked effect on the next breakdown measurement unless sufficient circulation time is allowed between measurements for the washing of such products out of the test cell.

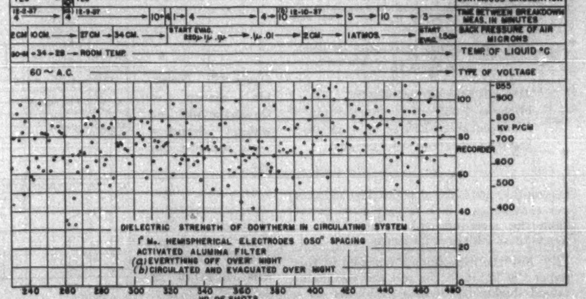
To limit the amount of energy dissipated in the cell for each shot a 10^7-ohm resistor was connected in series with the high-voltage lead to the cell at shot 950. Shots 1020–1030, 1140–1160, and 1200–1220 show conclusively the deleterious effects of such products when the circulating time between shots is short. Therefore the current-limiting resistor was used for subsequent measurements.

Effects of Varying the Partial Pressure of Air in Solution From < 0.01 Micron to One Atmosphere

Figure 14 shows a series of measurements on a synthetic high-boiling liquid composed of 73.5 per cent diphenyl oxide and 26.5 per cent diphenyl manufactured by the Dow Chemical Company, and given the trade name Dowtherm. The liquid, as received, contained an undesired amount of conducting contaminants so that it was necessary to purify the sample by fractional distillation in a five-foot packed column at 20 millimeters nitrogen pressure, the fraction saved having a boiling range from 133 to 134 degrees centigrade at this pressure. The important and entirely unexpected result of this series of measurements is that *changing the partial pressure of air in solution from < 0.01 micron to one atmosphere had no effect on the dielectric strength.* This is contrary to the results of many previous investigators so that it is important to point out several major differences between these and previous experiments.

(*a*). Routine dielectric strength tests made in this country are usually taken in an open cell with sharp-edged one-inch electrodes spaced 0.1 inch apart. Thus the maximum gradient at the edges of the electrodes is unknown. Also, the average of several shots is taken without removing the products of decomposition, and further, the tests are made in open cells with little provision for eliminating moisture or floating contaminants. While such measurements may serve a useful purpose in detecting the presence of gross contamination, they cannot characterize the ultimate dielectric strength of a particular insulating liquid.[16] For example, Clark[8] in his figure 8 reports a dielectric strength for transformer oil at one

Figure 14. Effects of varying the partial pressure of air in solution in Dowtherm from <0.01 micro to one atmosphere



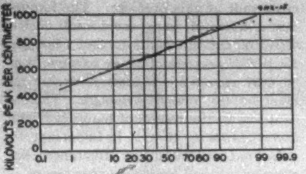


Figure 15. Probability plot of last 99 measurements on Dowtherm from figure 14

Kilovolts peak per centimeter:
Highest—955
Most probable—740
Standard deviation—6.2 per cent

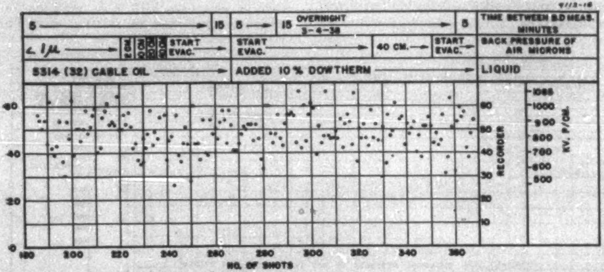


Figure 16. Dielectric-strength measurements on cable oil and Dowtherm

One-inch molybdenum hemispherical electrodes, 0.028-inch spacing; activated alumina filter, continuous circulation; room temperature

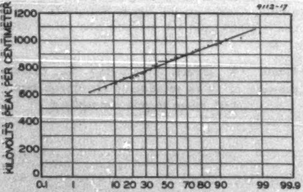


Figure 17. Probability plot, cable oil alone from figure 16

Kilovolts peak per centimeter:
Highest—1,055
Most probable—835
Standard deviation—14.4 per cent

atmosphere pressure of about 220 kv peak per centimeter, which is very much lower than the most probable value shown in figure 18 of this paper for similar oil.

(*b*). For the experiments reported in this paper the liquid in the test cell was maintained during continuous circulation at a hydraulic pressure of one atmosphere plus the partial pressure of gas in solution. Thus the hydraulic pressure was varied from one to two atmospheres while the partial gas pressure was varied from <0.01 micron to one atmosphere. The inhibition of free gas-bubble formation by the superimposed hydraulic pressure probably accounts for the fact that we found no dependence between dielectric strength and partial gas pressure. This is in agreement with experiments[6,9] showing that gas in solution has no effect on breakdown but contrary to much of the data reported in the literature.[5,8,13] Figure 15 shows a probability plot of the last 99 shots from figure 14. While the order of magnitude of dielectric strength is high we have not yet eliminated one or more uncontrolled factors resulting in an average deviation of 14 per cent. Figure 16 shows another set of measurements which indicate that in our system varying the partial gas pressure has no effect on the dielectric strength of either a low viscosity cable oil alone or mixed with ten per cent of Dowtherm. Figures 17 and 18 show probability plots indicating no appreciable difference between the oil alone and with ten per cent Dowtherm.

(*c*). Another major difference between this and previous work is that for most of our measurements the liquid was being forced through the test gap while voltage was being applied. Figures 19 and 20 show that when circulation was continued while voltage was being applied the breakdown strength was 35 per cent greater than when the circulation was stopped during the measurement even though in both cases 36 minutes continuous circulation was allowed between successive shots to clean out decomposition products. Two possible explanations suggest themselves: Perhaps forcing the liquid through the gap may have had a beneficial effect in sweeping away minute solid suspended particles which were too small to be removed by the filter. If such particles having dielectric constants higher than oil remain in the field they would distort the field and be attracted to the electrode surface, and thus increase the maximum electrical gradient in the liquid. Another possibility is that hydraulic pressure has some effect since with circulation the pressure was greater than 76 centimeters but without circulation the oil head gives a pressure of the order of 15 centimeters. As yet we have insufficient data to determine whether either of the above suggestions is the real cause of the observed difference in breakdown strength with and without circulation.

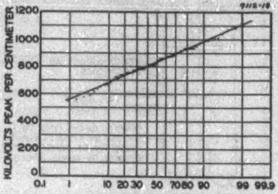


Figure 18. Probability plot, cable oil plus ten per cent Dowtherm from figure 16

Kilovolts peak per centimeter:
Highest—1,085
Most probable—835
Standard deviation—15.2 per cent

Effects of Conditioning at a Fixed Voltage for a Predetermined Time Before Increasing Voltage to Breakdown

All of the breakdown measurements reported so far have been made by applying a voltage which is increasing continuously at the rate of approximately 2 kv

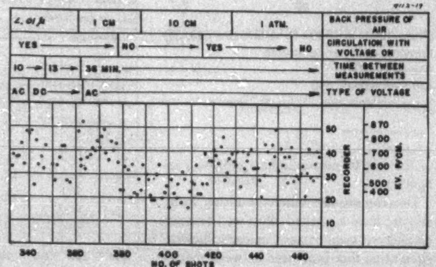


Figure 19. Dielectric strength of Dowtherm with and without circulation while applying voltage

Cell number 1, one-inch molybdenum electrodes, 0.028-inch (0.071 centimeter) spacing; data 4/21/38 to 4/24/38 peak per second. The question arose as to the conditioning effect of holding the voltage at a predetermined value below the expected breakdown point for a predetermined time, after which the voltage would again be increased at the usual rate to breakdown. To accomplish this a time-delay relay was inserted in the regulator motor circuit actuated by a voltmeter contact on the photoelectric recorder[15] which could be set at any predetermined primary voltage. Figure 21 shows data taken with and without such a conditioning period. Cells with different spacing between electrodes were connected in series in the system with results as shown. Shots 38–90 show again the previously noted decrease in breakdown strength when the liquid was not being circulated while voltage was applied. Between shots 90 and 108 the voltage was allowed to rise to 523 kv peak per centimeter and then was held at this value. For all but the 98th shot breakdown occurred within nine minutes. The conditioning voltage was lowered to 476 kv peak per centimeter and breakdown did not occur within nine minutes for about half the shots. The time intervals for both clean-up circulation and partial voltage conditioning for two groups of shots are shown in figure 22. Apparently under the conditions of our tests, breakdown is a probability function of time of voltage application as well as the magnitude of the gradient. From the very limited data available we found that with five-minute circulation clean-up intervals and when conditioning at a gradient approximately 50 per cent of the maximum value obtained on a short-time test, there was a 50 per cent probability that failure would occur within nine minutes. (Shots 109–131.) Later with circulation clean up intervals of about 14 minutes and with a conditioning gradient of approximately 65 per cent of the maximum value obtained on a short-time test, there was a 50 per cent probability that failure would occur within 33 minutes. (Shots 155–173.) It should be very interesting to study these time-gradient relations with different types and conditions of electrodes and with variations in the physical and chemical purity of the insulating liquid.

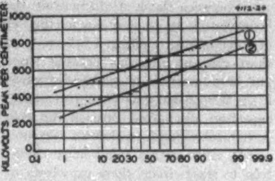


Figure 20. Probability plots, with and without circulation from figure 19

Kilovolts Peak Per Centimeter

Curve	Maximum	Most Probable	Standard Deviation (Per Cent)
1—With circulation	870	658	14.3
2—Without circulation	710	497	22.4

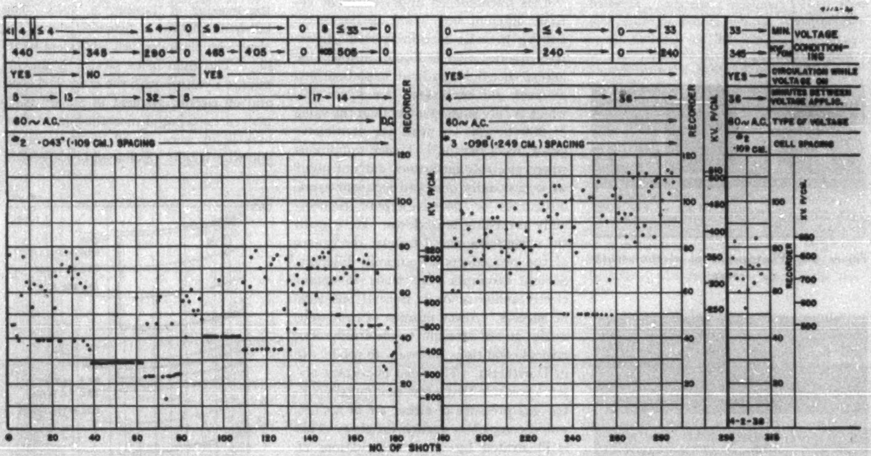


Figure 21. Dielectric strength of Dowtherm with and without conditioning at part voltage

One-inch molybdenum electrodes; fiber glass and fritted glass filter; 10^7-ohm resistor in series with cell; 60 degrees centigrade, <0.01 micron gas pressure

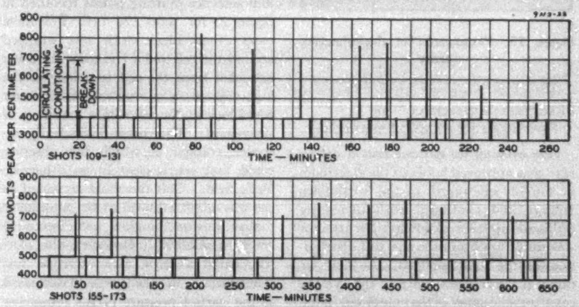


Figure 22. Circulation and conditioning times for portions of figure 21

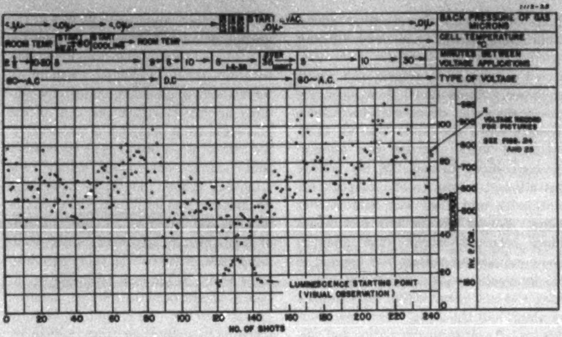


Figure 23. Dielectric strength of Dowtherm showing luminescence starting points

One-inch hemispherical electrodes; 0.050-inch (0.127-centimeter) spacing; activated alumina filter; 10^7-ohm resistor in series with cell

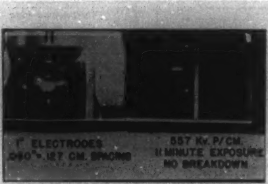


Figure 24. Photographs of electroluminescence

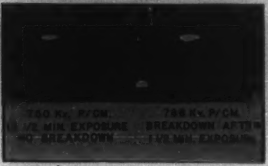


Figure 25. Photographs of electroluminescence

"Electroluminescence" in Dowtherm Preceding Breakdown

One evening we noticed that a *diffuse blue glow* appeared between the electrodes at roughly one-fourth of the breakdown voltage and continued with increasing brilliance up to breakdown. No attempt was made to obtain the spectral distribution of the light emitted. At first we called it fluorescence since to the eye the color was the same as that observed in the same liquid under X-ray or high-pressure mercury-arc excitation. However, since the glow has an electrical origin the name "electroluminescence" is suggested.

This phenomenon appeared to be entirely different from spark discharges occurring in cells containing solid dielectrics in series with the liquid, although the lowest threshold gradient was about 160 kv peak per centimeter (see figure 23), which is only slightly lower than the value of 200 to 250 kv peak per centimeter given by Conradi.[16] Under conditions where electroluminescence did not occur we occasionally observed incipient spark discharges or *vorentladungen*[14] immediately preceding breakdown. Here again the appearance and, I believe, the causes of the two effects are entirely different. Several attempts were made to obtain electroluminescence in mineral oils with no success. Also a number of runs were made in an attempt to determine the critical conditions necessary to produce it in Dowtherm. The most important observation was that a few centimeters partial gas pressure of either air or CCl_2F_2 would completely quench the glow. This is illustrated by visual observations of luminescence starting points recorded in figure 23 for shots 120–144. For these observations black cloth was wrapped around the cell and a Herkolite tube was extended from the cell to a hole in the high-voltage cage so that an observer could look at the cell with a minimum of stray light. The crosses indicate the recorder voltage at which the observer could first see a glow in an otherwise black field. This threshold increased as gas was admitted until at ten-centimeter partial pressure no glow was discernible before breakdown. However, the glow reappeared as soon as evacuation was started again and the threshold decreased as the partial pressure of gas remaining in solution was decreased.

Another interesting observation was that as gas was admitted the glow not only decreased in intensity but also appeared to be concentrated adjacent to both electrode surfaces, leaving a dark space between them. Unfortunately the breakdown voltages were low for this set of observations, resulting, I believe, from short clean-up intervals between shots and large amounts of energy dissipated in the cell per shot since the d-c smoothing capacitors discharged through the cell. A few subsequent breakdown measurements, taken with a high-voltage thyratron to short-circuit these capacitors to ground (see figure 9) as soon as possible after breakdown, indicated no significant difference between 60-cycle peak and d-c breakdown voltages.

Since, as far as we knew, such a glow in an insulating liquid, apparently unrelated to breakdown, had not previously been reported we photographed it during the evening of January 6, 1938. The left-hand picture in figure 24 shows the electrodes photographed with room lighting. The right-hand picture is an 11-minute exposure with the room darkened and an electric gradient of 557 kv peak per centimeter applied to the liquid. The high lights were caused by street lights and were largely eliminated in succeeding

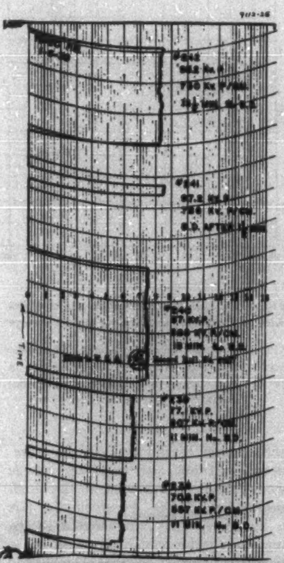


Figure 26. Primary voltage record obtained while photographing electroluminescence

photographs by using a black curtain between the cell and the windows. The right-hand picture in figure 24 shows a breakdown which occurred after about $1^1/_2$ minutes at 766 kv peak per centimeter. After this breakdown the liquid was allowed to circulate about six minutes and then 750 kv peak per centimeter was applied and held without breakdown for $15^1/_2$ minutes while the glow was photographed as shown. The voltage records for these photographs are indicated by "x" marks on the extreme right of figure 23 to show the order of magnitude compared to previous breakdown measurements with the same setup. Also the photoelectric primary voltage record obtained while these pictures were being taken is reproduced in figure 26.

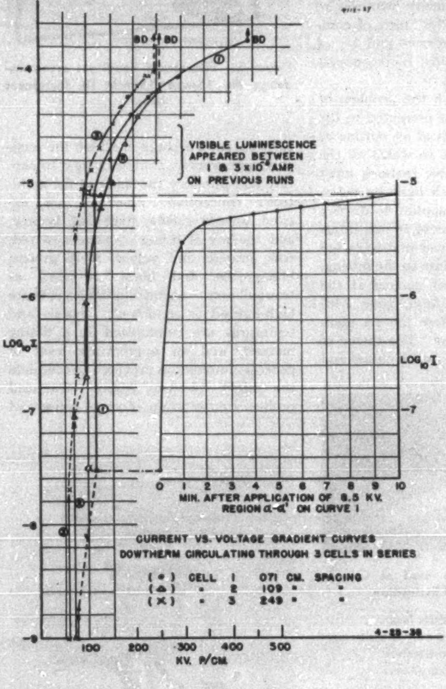


Figure 27. Current versus voltage-gradient curves in Dowtherm

CURRENT VERSUS VOLTAGE-GRADIENT RELATIONS

It seems to the writer that a very important tool in the study of the mechanism of breakdown in liquids is the examination of voltage-current relations.[13] Three typical curves are shown in figure 27 illustrating several interesting observations. Instruments were not available for current measurement below 10^{-9} ampere so that the initial Ohm's law current caused by the residual free ions in the liquid could not be measured. The low end of the current which we did observe was usually very unstable as though the instrument, which has a long time constant, was integrating irregular bursts of current of very short duration. The vertical lines terminated by arrow heads indicate gradual increases in average current observed while the voltage was held sensibly constant (that is, no change in regulator setting). The line $a-a'$ on curve 1 with its corresponding time relation to the right shows a 200/1 change in ten minutes, most of which occurred in the first minute. Data taken with a current instrument having a much shorter time constant would be necessary to resolve and interpret this rather crude observation.

The second interesting point is that under optimum conditions the electroluminescence threshold occurred regularly in a narrow range of current (1 to 3×10^{-5} ampere).

Another observation is the saturation effect at higher gradients with breakdown occurring unpredictably and without observable increase in current preceding breakdown within the time limit of response of our instrument. Although the experimental difficulties appear great, we hope to make some cathode-ray oscillographic studies of these currents.

Conclusions

1. In a filtered, water free, insulating liquid on which the hydraulic pressure is one atmosphere plus the partial pressure of gas in solution, the dielectric strength is unaffected by the partial pressure of air or CCl_2F_2 in solution from 0.01 micron to one atmosphere pressure.

2. The large average deviations observed even after days of continuous circulation, drying, filtering, and degasification support the conclusion of several previous experimenters that the condition of the electrode surface is a major controlling factor.

3. In one of the insulating liquids studied a diffuse glow, which occurred at gradients well below breakdown, was observed and photographed. To the best knowledge of the writer this phenomenon has not been previously reported. A descriptive name of "electroluminescence" is suggested since the cause appears to be electronic. The glow was completely quenched by admitting a few centimeters partial pressure of air or CCl_2F_2. The breakdown gradient was unaffected by the presence or absence of the glow.

4. Commercial insulating liquids treated under laboratory conditions and carefully freed from water and suspended solids have dielectric strengths of the order of 10^6 volts per centimeter, comparable to pure chemicals. This observation is subject to the reservation that the important factor in breakdown may be the condition of the electrode surface and its ability to emit electrons and not a specific property of the liquid.

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Painting the Golden Gate International Exposition With Light

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ON a man-made island of 400 acres which President Roosevelt has referred to as our latest insular possession, architects, artists, and engineers have constructed a magic walled city about an architectural plan that combines the ancient designs of Burmese, Cambodian, Incan, Mayan, and other Pacific cultures into the unique and surprisingly beautiful Golden Gate International Exposition. One and a half million dollars spent on landscaping and another million dollars worth of sculptures, murals, and decorations have made of it a "Garden of Paradise" and a comprehensive artistic architectural unit rather than a disconnected assemblage of exhibit buildings.

To design the lighting for such a composition requires that the engineer approach his problem with tolerance toward the view of others. The lighting must be in harmony with the work of the architects, artists, sculptors, and horticulturists, and strictly engineering considerations subordinated in many instances to those of aesthetics. This spirit of compromise between the science and art of illumination will be noted in the effects described in this paper.

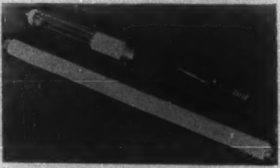

Figure 1. Light sources used at Golden Gate International Exposition

A—15-watt fluorescent Mazda lamp
B—100-watt type *H*4 red-purple-bulb mercury lamp for fluorescence
C—250-watt type *H*2 mercury lamp

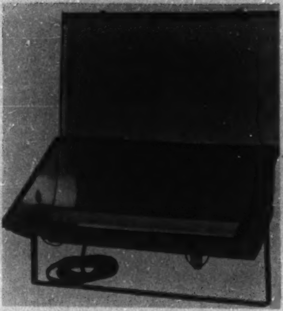

Figure 2. Fluorescent floodlight accommodating the 15-watt 18-inch T8 fluorescent lamp

The manner in which the problem of lighting an exposition is presented to the engineer takes the form of an outline of the architectural theme to which all the structural and decorative features must be related and in which light by color, direction, and scale is applied to display the architectural elements in an interesting and faithful manner and to create an atmosphere appropriate to the setting. The 12 principal exhibit palaces at the Exposition (see cartograph, page 746) serve to form and enclose the six main courts of the Exposition. The theme of each court is suggested by its name, and each becomes a stage in which the engineer endeavors by the use of light to capture and intensify the spirit of the architect's conception. Emotions are excited toward gaiety, liveliness, beauty, and the joy of living by shades of red, rose, orange, and yellow, while greens, blue-greens, and blues are used as tranquilizers. Architectural elements are high-lighted or shadowed; murals and sculptures are emphasized in a fitting manner, and an appropriate level of general illumination provided to complete the night picture. Many structural problems enter in an effort to house and

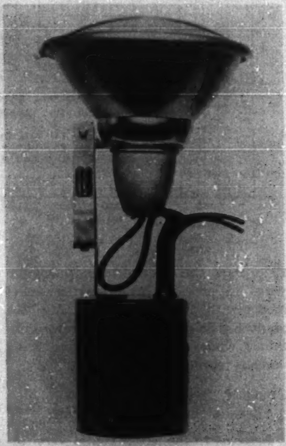

Figure 3. Mercury floodlight and transformer installed throughout the grounds for lighting trees and shrubbery

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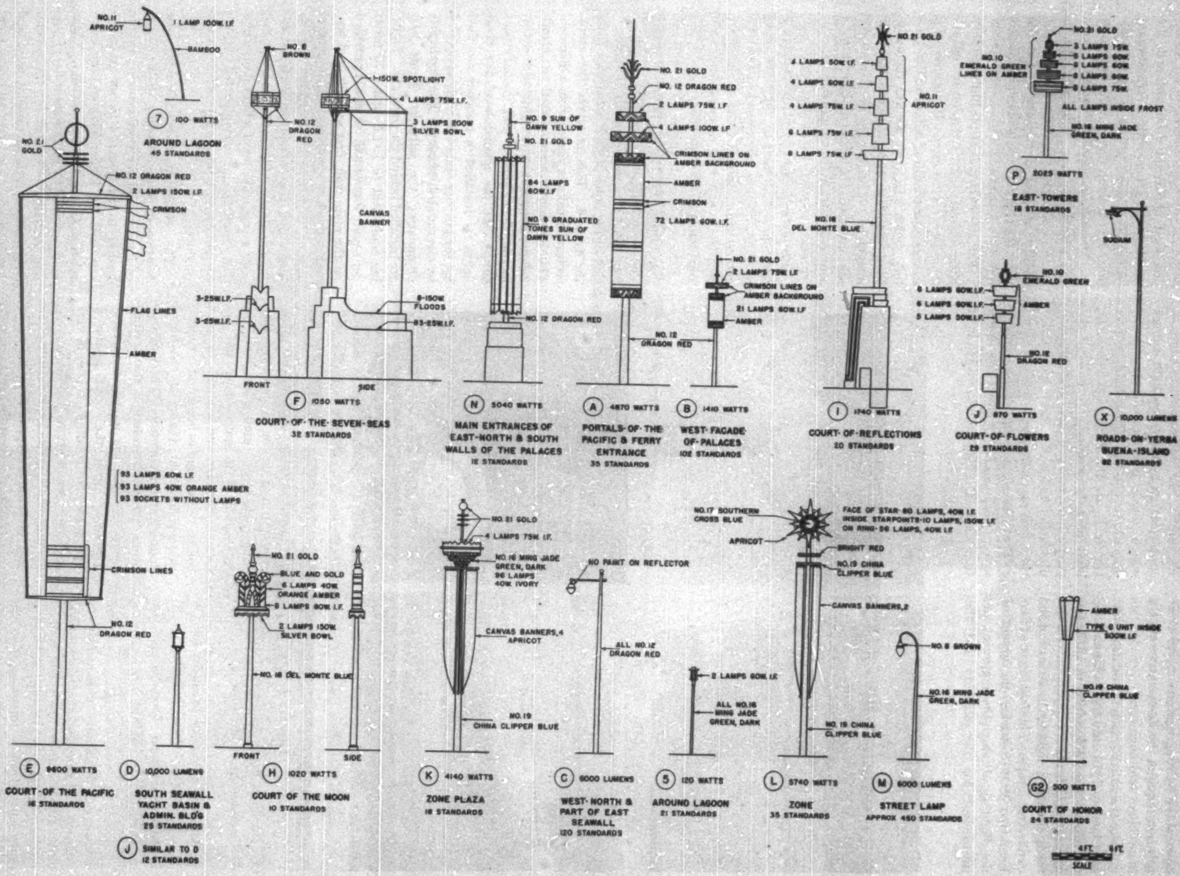

NO. 11 APRICOT
1 LAMP 100W. I.F.
BAMBOO
7 100 WATTS
AROUND LAGOON
45 STANDARDS
NO. 21 GOLD
NO. 12 DRAGON RED
2 LAMPS 150W. I.F.
CRIMSON
FLAG LINES
AMBER
93 LAMPS 60W. I.F.
93 LAMPS 40W. ORANGE AMBER
93 SOCKETS WITHOUT LAMPS
CRIMSON LINES
NO. 12 DRAGON RED
E 9600 WATTS
COURT-OF THE PACIFIC
16 STANDARDS
D 10,000 LUMENS
SOUTH SEAWALL YACHT BASIN & ADMIN. BLDG
25 STANDARDS
J SIMILAR TO D
12 STANDARDS
NO. 8 BROWN
NO. 12 DRAGON RED
3-25W I.F.
3-25W I.F.
1-150W. SPOTLIGHT
4 LAMPS 75W. I.F.
3 LAMPS 200W SILVER BOWL
CANVAS BANNER
8-150W FLOODS
83-25W I.F.
FRONT
SIDE
F 1050 WATTS
COURT-OF-THE-SEVEN-SEAS
32 STANDARDS
NO. 21 GOLD
BLUE AND GOLD
6 LAMPS 40W. ORANGE AMBER
8 LAMPS 60W. I.F.
2 LAMPS 150W. SILVER BOWL
NO. 16 DEL MONTE BLUE
FRONT
SIDE
H 1020 WATTS
COURT OF THE MOON
10 STANDARDS
NO. 9 SUN OF DAWN YELLOW
NO. 21 GOLD
84 LAMPS 60W. I.F.
NO. 9 GRADUATED TONES SUN OF DAWN YELLOW
NO. 12 DRAGON RED
N 5040 WATTS
MAIN ENTRANCES OF EAST-NORTH & SOUTH WALLS OF THE PALACES
12 STANDARDS
NO. 21 GOLD
4 LAMPS 75W. I.F.
NO. 16 MING JADE GREEN, DARK
96 LAMPS 40W. IVORY
CANVAS BANNERS, 4 APRICOT
NO. 19 CHINA CLIPPER BLUE
K 4140 WATTS
ZONE PLAZA
16 STANDARDS
NO. 21 GOLD
NO. 12 DRAGON RED
2 LAMPS 75W. I.F.
4 LAMPS 100W. I.F.
CRIMSON LINES ON AMBER BACKGROUND
AMBER
CRIMSON
72 LAMPS 60W. I.F.
NO. 12 DRAGON RED
A 4870 WATTS
PORTALS-OF-THE PACIFIC & FERRY ENTRANCE
35 STANDARDS
NO. 21 GOLD
2 LAMPS 75W. I.F.
CRIMSON LINES ON AMBER BACKGROUND
21 LAMPS 60W. I.F.
AMBER
B 1410 WATTS
WEST-FACADE-OF-PALACES
102 STANDARDS
NO PAINT ON REFLECTOR
ALL NO. 12 DRAGON RED
C 6000 LUMENS
WEST-NORTH & PART OF EAST SEAWALL
120 STANDARDS
2 LAMPS 60W. I.F.
ALL NO. 16 MING JADE GREEN, DARK
5 120 WATTS
AROUND LAGOON
21 STANDARDS
NO. 21 GOLD
4 LAMPS 60W. I.F.
4 LAMPS 60W. I.F.
4 LAMPS 75W. I.F.
6 LAMPS 75W. I.F.
8 LAMPS 75W. I.F.
NO. 11 APRICOT
NO. 16 DEL MONTE BLUE
I 1740 WATTS
COURT-OF-REFLECTIONS
20 STANDARDS
NO. 17 SOUTHERN CROSS BLUE
APRICOT
FACE OF STAR-80 LAMPS, 40W. I.F.
INSIDE STARPOINTS-10 LAMPS, 150W. I.F.
ON RING-36 LAMPS, 40W. I.F.
BRIGHT RED
NO. 19 CHINA CLIPPER BLUE
CANVAS BANNERS, 2
NO. 19 CHINA CLIPPER BLUE
L 5740 WATTS
ZONE
35 STANDARDS
NO. 10 EMERALD GREEN
6 LAMPS 60W. I.F.
6 LAMPS 60W. I.F.
5 LAMPS 50W. I.F.
AMBER
NO. 12 DRAGON RED
J 870 WATTS
COURT-OF-FLOWERS
29 STANDARDS
NO. 8 BROWN
NO. 16 MING JADE GREEN, DARK
M 6000 LUMENS
STREET LAMP
APPROX 450 STANDARDS
NO. 21 GOLD
NO. 10 EMERALD GREEN LINES ON AMBER
3 LAMPS 75W.
6 LAMPS 60W.
6 LAMPS 60W.
6 LAMPS 60W.
8 LAMPS 75W.
ALL LAMPS INSIDE FROST
NO. 16 MING JADE GREEN, DARK
P 2025 WATTS
EAST-TOWERS
18 STANDARDS
SODIUM
X 10,000 LUMENS
ROADS-ON-YERBA BUENA-ISLAND
82 STANDARDS
AMBER
TYPE 6 UNIT INSIDE 500W. I.F.
NO. 19 CHINA CLIPPER BLUE
G2 500 WATTS
COURT OF HONOR
24 STANDARDS
4 FT.
8 FT.
SCALE

Figure 4. Sketches of lighting standards used throughout the grounds

conceal the lighting equipment and their solution requires the co-operative efforts of architect and engineer.

Since the introduction of the incandescent lamp in 1879 until the Panama-Pacific International Exposition in 1915, the decorative lighting of expositions took the form of outline lighting by strings of incandescent lamps. This exposition at San Francisco in 1915 marked the advent of floodlighting, although the equipment was improvised for the occasion and the commercial floodlight subsequently developed. The incandescent-lamp floodlight still assumes the major role in modern lighting because the incandescent lamp lends itself to dimming, color correction, and control of the light distribution. At the Golden Gate International Exposition the incandescent lamp is supplemented by three new light sources which make possible effects hitherto difficult to obtain. These light sources (figure 1) are the fluorescent lamp, the high-intensity mercury lamp, and the red-purple-bulb mercury lamp for obtaining ultraviolet radiation. Each has its distinct field of application.

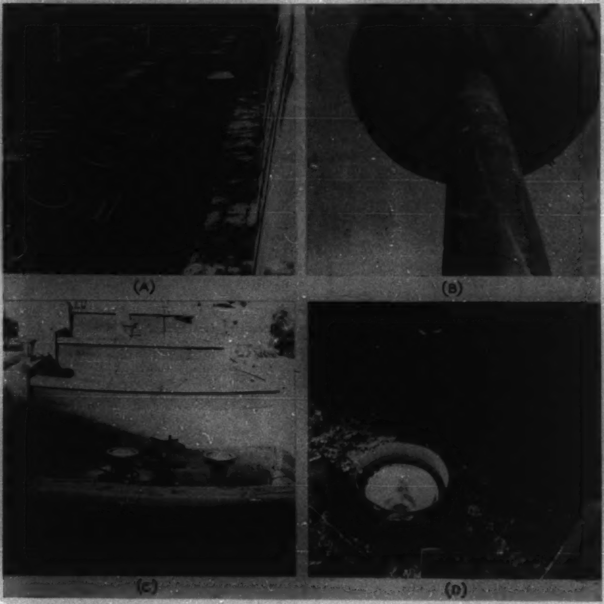

(A)
(B)
(C)
(D)

Figure 5

A—Floodlight trench for Court of the Moon

B—Glyptal-treated-fabric light tube (interior view)

C—Underwater fountain floodlights and Cel-o-glass-covered lip lights

D—Mercury ground floodlight for tree lighting

The fluorescent lamp is essentially a mercury arc generating ultraviolet radiation which is converted into visible light by the fluorescing of powders (phosphors) coated on the inside of the glass tube. The lamp used throughout the Exposition is 18 inches long and one inch in diameter, and available in seven colors. The lamp is housed in a trough-shaped floodlight (figure 2) having a double-parabolic section. The unit consumes 20 watts including the starting accessories and has an efficiency range depending upon the color produced. The fluorescent floodlight is used in various color combinations for illuminating building walls, setbacks, etc. (figures 5*A* and 6*C*) and has proved to be an invaluable tool for creating lighting effects.

The high-intensity mercury lamp in use is the 250-watt size housed in an Alzak-processed aluminum floodlight and the floodlight together with a transformer (figure 3) placed within a cylindrical-shaped container (figure 5*D*) buried in the ground. Concealed beneath trees and shrubbery these units serve to illuminate the planting with a moonlight quality of light and preserve for the night the charm of the landscaping at this unusual exposition.

The 100-watt red-purple-bulb mercury lamp for ultraviolet radiation is used for fluorescence excitation. It consists of a mercury arc contained within a filter-glass bulb which selectively absorbs the undesired radiation and transmits the near ultraviolet. This lamp is used in an Alzak-processed aluminum floodlight as a source of ultraviolet radiation for special features referred to later in this paper.

The exterior lighting of the buildings and courts is all done by the three types of light sources mentioned above together with standard types of incandescent lamps in a wide range of sizes, bulb and filament forms, and colors.

The west facade (figure 7) is the view one obtains from the San Francisco shore. The main wall and two setbacks are illuminated at increasing levels of illumination in the order of their occurrence from the ground. This preserves the architectural breaks as viewed from a distance. This lighting is accomplished by Alzak-treated aluminum floodlights with asymmetric door glasses and incandescent lamps. At the ground level the flood lights are installed in trenches provided for the purpose and completely concealed by trees and shrubbery. The floodlights illuminating the setbacks (figure 6*D*) are installed behind the parapets. Lighting equivalent to 1, 1.5, and 1.75 watts per square foot of wall surface is provided on the main wall, first setback, and second setback respectively. This general scheme is followed throughout for illuminating the outside walls of the exhibit palaces. The walls are finished with gypsum plaster of a light ivory color and a luminous luster is imparted to them by pressing into the plaster surface a mica product.

The elephant towers flanking the Northwest passage leading to the Court of Pacifica and the Portals of the Pacific leading to the Court of Honor are given emphasis by higher illumination levels relative to the building facades. Clear light provided by incandescent floodlights and searchlights is used for the general floodlighting of the pyramidal bases and formal elephants with red and ruby relief lighting along the steps and howdahs

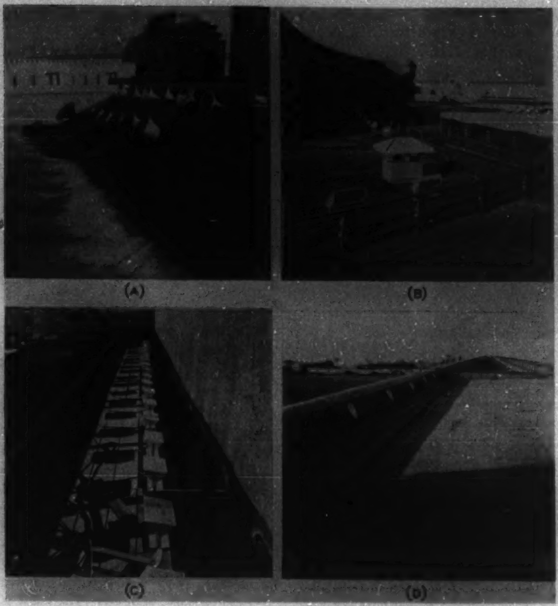


Figure 6

A—Bank of floodlights and searchlights for Tower of the Sun

B—Fluorescent floodlights on setback of Court of Honor

C—Fluorescent floodlights on setback of Court of the Moon

D—Floodlights for setback on west facade

to suggest the oriental theme of the setting.

The Court of Pacifica (figure 8) in design and location invited unusually striking lighting treatment tending toward the spectacular. Visitors pass through this court on their way to the Gayway or amusement section and it appeared fitting to prepare them psychologically for the bizarre effects to follow. The general illumination of the court is that of a warm glow emanating from cylindrical lanterns (figure 4*E*) 5 feet in diameter and 52 feet long, supported on a steel pole 86 feet high. The lanterns are formed of a Glyptal-treated fabric supported by a steel spider and contain incandescent lamps totaling 9,600 watts. Sixteen such standards or a total of 153.6 kw of general illumination are provided.

The great metal prayer curtain behind the 85-foot statue of Pacifica is silhouetted at night against a mat of mobile color consisting of amber, red, and blue light controlled by a thyratron reactor dimmer and passing through a cycle of changes. The lighted background is obtained by 13.6 kw of amber, 26.8 kw of red, and 52 kw of blue incandescent floodlights of 200 watts capacity each. Three 1,000-watt underwater floodlights in the pool at the foot of Pacifica illuminate the figure. The large sculptured and painted mural (figure 9) on the west face of the court symbolizing "The Peacemakers" is 144 feet by 58 feet and is illuminated by clear and amber light. Incandescent floodlights of 500 watts capacity and located at the base of the mural furnish the illumination totaling 12 kw of clear and 6 kw of amber light.

The cascades above which Pacifica is standing are illuminated by natural-green-bulb incandescent lamps behind Cel-o-glass risers. This lighting and the lip lighting on the central fountain is obtained by 60-watt lamps on six-inch centers. The large center bubble in the fountain is illuminated in mobile color controlled by thyratron reactor dimmers. A group of nine blue, four red, and three amber underwater floodlights of 1,500 watts capacity each provide the lighting effects. The larger trees are illuminated

Figure 7. West facade

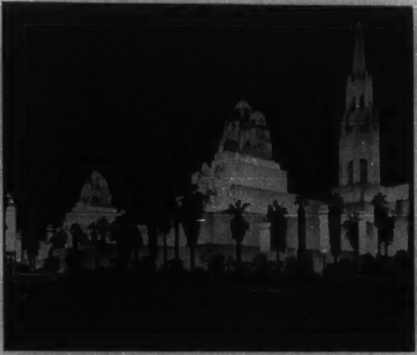


Figure 8. Court of Pacifica

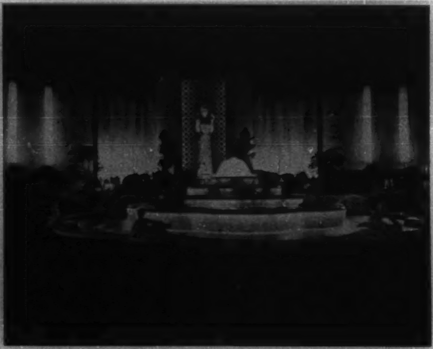


Figure 9. Court of Pacifica (mural "The Peacemakers")

Figure 10. Court of the Seven Seas and Tower of the Sun

by the mercury tree-lighting units referred to earlier in this paper and make interesting spots of contrast with the warmer tones in the court.

The walls flanking the grand stairs and also the walls of the east entrance are floodlighted in a golden amber tone by incandescent floodlights. The east entrance and Gayway plaza are lighted by decorative standards (figure 4*K*) with tiers of ivory lamps and banners, and serve as an introduction to the Gayway lighting (figure 4*L*). The total connected lighting load in this court is approximately 450 kw.

The Court of the Seven Seas (figures 10 and 11) is the longest court at the Exposition, measuring 1,000 feet by 200 feet and has a connected lighting load of approximately 225 kw. The lighting standards (figure 4*H*) provide the general illumination for the walks and are patterned in the marine spirit of the court symbolizing a ship's mast with the crow's nest housing the lighting equipment. This equipment for the "down" lighting consists of three 200-watt silvered-bowl lamps in open louvered reflectors. The "crows nest" is covered with Cel-o-glass and made luminous by transmitted light from four 75-watt inside-frosted incandescent lamps. The blue and white "sails" are floodlighted by means of eight 150-watt units concealed in the base. A spotlight concealed in the "rigging" is directed on the ship prows surmounting the facade piers between each pair of masts, giving prominence to these elements in the night picture. The lighting of the walls demonstrates the adaptability of the fluorescent floodlight for creating tints. A green apricot hue is obtained by mixing two 100-watt amber incandescent floodlights with one green and two pink fluorescent floodlights for each 12 linear feet. The main wall is 42 feet high and the castellated setback wall above it is illuminated by gold fluorescent tubes on the basis of approximately 0.2 watt per square foot of wall surface.

A very interesting effect is obtained in this court by the use of luminescent paints excited by ultraviolet-radiation sources. This feature occurs in the 16 niches depicting submarine life in bas-relief and represents considerable research work on the part of the manufacturers of luminescent paints in developing a product that would withstand the ravages of dust, sun, etc. The paint is applied to the bas-relief and activated by 16 100-watt red-purple-bulb ultraviolet lamps in Alzak-treated aluminum reflectors concealed at the base of each niche. A happy note is introduced by the use of 160 Glyptal-treated cloth lanterns suspended at random through the trees in the court and each equipped with a 25-watt inside-frosted incandescent lamp.

The Court of Honor is the focal point of the main group of Exposition build-

Figure 11. Details, Court of Seven Seas

Figure 12. Court of Reflections and Arch of Triumph

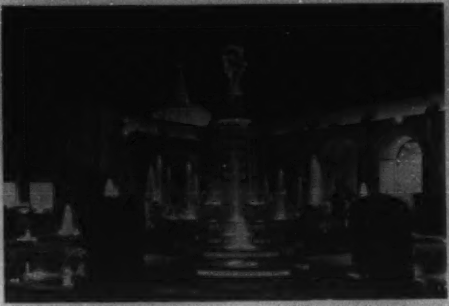


Figure 13. Court of Flowers

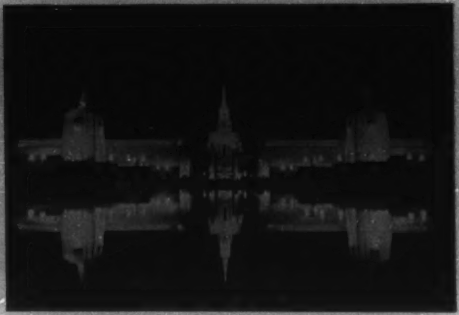


Figure 14. Temple Compound

ings and has as its central feature the Tower of the Sun rising to a height of 400 feet. At night the tower rises from a warm amber glow at the base to a brilliant white at the Phoenix surmounting the tower. The transition from amber to clear light is a very gradual one and scarcely discernible by reason of the blue and amber relief lighting at the openings at the several levels of the tower. A total of 450 kw of incandescent searchlights and floodlights (figure 6*A*) furnishes the lighting for the tower and the reflected light provides the general illumination for the court and flanking facades. Supplementary lighting takes the form of lighting standards (figure 4*G*2) equipped with single-light luminaires of 500 watts capacity each. Contrasting with the classical architecture of this court are illuminated trees, flags, and fountains.

The Court of Reflections (figure 12) adjoins the Court of Honor on the east and, as becomes a vantage point for observing reflections, the environment is one of placid quietness. From the east end of the reflecting basin one observes the reflection of the Tower of the Sun and from the west end the Triumphal Arch is mirrored in the pool. The Triumphal Arch is 107 feet in height and is floodlighted by means of incandescent floodlights and searchlights totaling 25 kw. The coral-colored walls of the court are lighted by pink fluorescent floodlights spaced on 4-foot centers, $3^{1}/_{2}$ feet from the base of the walls in trenches built for this purpose. The lighting standards (figure 4*I*) furnishing the general illumination of the court flank the reflecting pools and simulate Siamese ceremonial umbrellas. In design and scale they form a definite architectural element in the decorating scheme of the court. Each of the 20 standards is 53 feet high and has five tiers of painted-fabric shades assembled on a tapered steel pole. Incandescent lamps totaling 1,740 watts are installed in each standard. Recessed plaques illuminated by a blue fluorescent tube concealed at its base make interesting breaks in the large expanse of wall area. The total connected lighting load of the court is approximately 65 kw.

The Court of Flowers (figure 13) reflects the cheerful but restful mood that accompanies the play of fountains in a garden. The placid nature of this court provided the keynote for the lighting. The jets of the five fountains are all illuminated with clear light by means of underwater floodlights and the cascades emanating from the central fountain are illuminated in blue-green. The cascade lighting consists of incandescent lamps placed behind Cel-o-glass risers under the lip of the steps forming the cascade. Natural-colored-glass lamps of 40-watt size and spaced on six-inch centers were used throughout the exposition for this purpose. The equipment used for illuminating the jets comprised 68 250-watt cast-aluminum underwater floodlights and 8 1,000-watt units of the same type. These floodlights and others similarly installed throughout the Exposition are held in position by bolting them to a block of concrete resting on the basin floor as shown in process of installation in figure 5*C*. A lighted border of flowers completely encircles the fountain.

Similar standards (figure 4*J*) to the Siamese ceremonial umbrellas installed in the adjacent Court of Reflections provide the general illumination in this court and add a colorful contrast with the green foliage. The rear wall of the loggia surrounding the court is lighted in a warm tone by 60-watt pink Lumiline lamps arranged in luminaires made up of groups of 18 lamps each and spaced on approximately 30-foot centers. Light boxes containing three 150-watt incandescent floodlights illuminate the murals. The setback above the arcade is illuminated with pink fluorescent lamps on approximately five-foot centers. Occasional trees and shrubs are illuminated by mercury tree-lighting units and the green foliage silhouetted against the relatively darker piers and arches create interesting and ever-changing patterns.

The East Facade is lighted in clear light in much the same manner as the West Facade. Trenches of 6-foot width, completely concealed by shrubs and trees, house 500-watt asymmetric floodlights on eight-foot centers for lighting the 40-foot walls. The shrubbery brilliantly silhouetted against the wall, forms a pleasing break from the regularity of the facades. The illumination of the 14-foot setback is provided by 200-watt incandescent floodlights on 10-foot centers. This lighting is broken by large standards of a unique design at the building entrances and the beautiful East Towers and murals which form the east entrance to the walled city.

The East Towers (figure 14) rising from tiers of circular steps, are heavily ornamented in gold leaf and carried in golden light at night. The huge bas-relief gold-leafed murals are lighted by pink fluorescent and 150-watt amber incandescent floodlights concealed at their bases, and pink fluorescent floodlights light the setbacks. The terraces and stairs are lighted by Siamese umbrella-type standards (figure 4*P*) built of fabric and equipped with incandescent lamps totaling 2.4 kw per standard. This complete lighting picture is perfectly reflected as a gigantic panorama in the adjoining lagoon.

Figure 15. Fountain of Evening Star

Figure 16. Treasure Garden and Court of the Moon

The charm of the Court of the Moon (figures 15 and 16) with its decorations recalling the stories of Aladdin is carried into the night by a moonlighting effect relieved by high lights of amber from grilles, turrets, fountains, and lighting standards. The walls and towers of this court are illuminated by a total of 530 500-watt incandescent floodlights equipped with asymmetric blue lenses and 452 blue fluorescent floodlights mixed to obtain a blue-orchid-tinted tone. The 500-watt incandescent units are spaced on approximately 30-inch centers and the fluorescent units on approximately five-feet centers (figure 5*A*). In each of the half-cylinder reveals (16 feet diameter, 12 feet high) three blue fluorescent floodlights are installed and the top setback is similarly illuminated (figure 6*C*) in blue by fluorescent floodlights on approximately three-foot centers. A striking mottled effect is obtained on the tops of the towers by a mixture of blue and pink fluorescent floodlights.

The general illumination of the walks is accomplished by standards (figure 4*H*) equipped with two 300-watt silvered-bowl incandescent lamps in reflectors for down lighting and ten 60-watt incandescent lamps for illuminating the lantern. The lantern consists of an iron frame with Cel-o-glass covering and is mounted on a tapered steel pole 28 feet long. Ten of these standards are installed in the court.

Twenty-four jets of water emanating from both sides of the central lagoon form 12 water arches over the pool and are illuminated in an amber tone by 72 underwater floodlights of 400 watts capacity each. Statuary is spot lighted by incandescent projector lamps and trees are illuminated in the manner referred to elsewhere in this paper. The court has a total connected lighting load of approximately 425 kw.

Treasure Garden (figure 16) lies to the south of the Court of the Moon and in general treatment parallels the latter court. The central feature is an illuminated fountain consisting of a 60-foot center jet surrounded by eight 20-foot jets. The water is illuminated with clear light delivered from underwater incandescent floodlights grouped at the base of the jets. There are five 1,500-watt units at the center jet and three 400-watt units at each of the small jets, making a total of 17.1 kw for the fountain.

The basin wall is illuminated in green by 60-watt incandescent lamps concealed in a lip formed at the top of the wall and arranged in double sockets on 16-inch centers. The reflected light from these walls serves to illuminate the flower bed and walk surrounding the pool.

The four corner groups of trees are illuminated from mercury tree-lighting units and by reflection shed a moonlight quality of general illumination over the garden. Fabric lanterns distributed throughout the trees add cheerful spots of amber color to the scene.

A festive note is introduced into the general lighting of the amusement section by a star standard (figure 4*L*) using exposed inside-frosted incandescent lamps in the center and behind Cel-o-glass at the points. A total of 106 40-watt lamps and 10 150-watt lamps is used in each standard. The standards are 35 feet high and spaced 100 feet apart along the center of the walk.

A battery of 24 36-inch medium-intensity searchlights located on the north side of Yerba Buena Island and centered on the north-south axis of the Exposition, furnishes light for purely spectacular purposes. This searchlight operates at 125 amperes direct current with 65 volts at the arc and produces a beam of 60,000,000 candle power. The entire battery is maneuvered on special occasions by soldiers from the 30th Infantry, United States Army, and drills consisting of moving the beams in cadence or directing them on smoke shells and fireworks create a variety of spectacular effects. Each light is equipped with gelatin screens for color changes. When not manned for drills the battery is spread out as a fan in clear light or color to form a light canopy over the Exposition site.

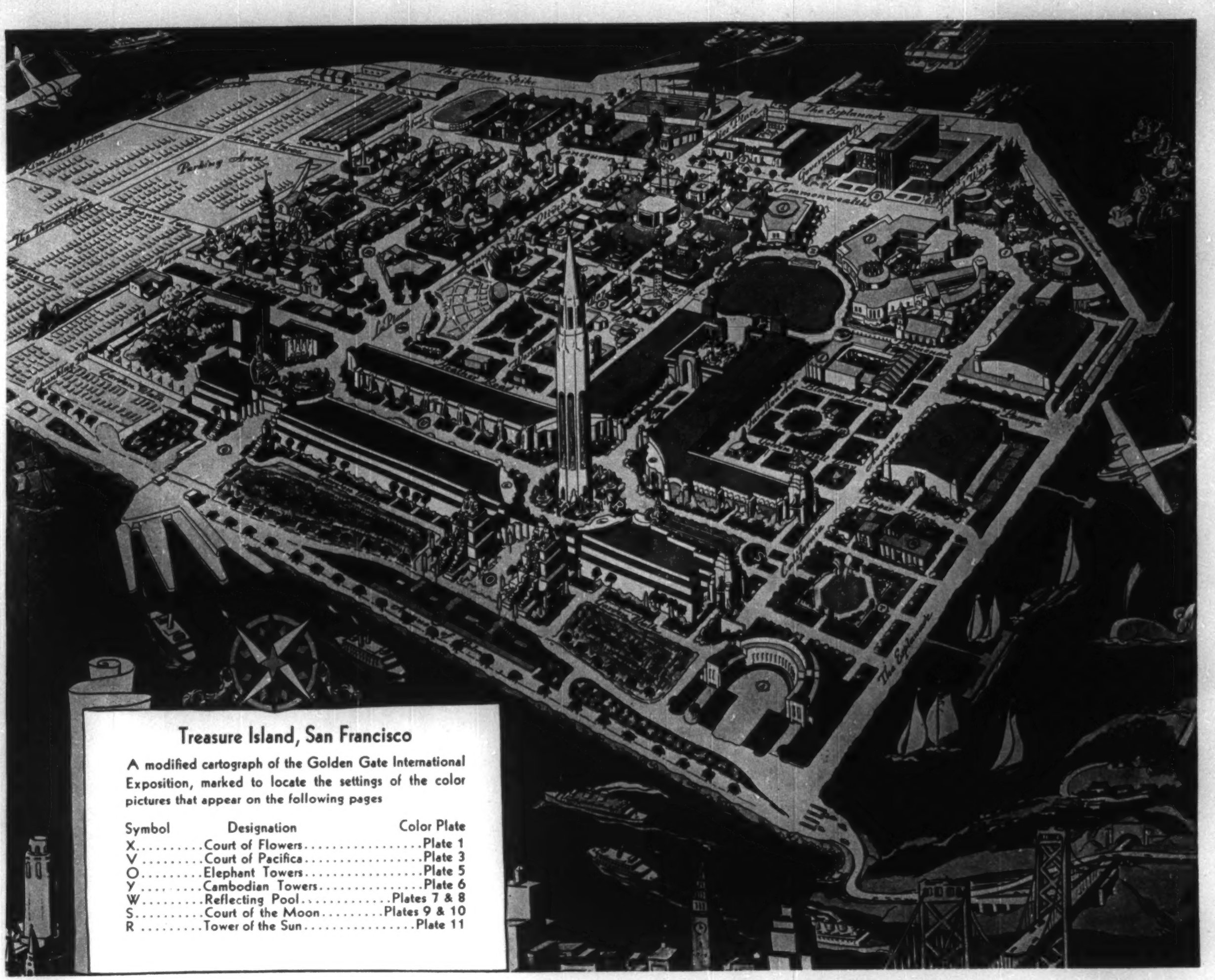
The utilitarian lighting for the roadways was designed to provide adequate visibility for pedestrians with a minimum of conflict with the decorative lighting. Series circuits independent of the general electrical distribution system were chosen for convenience of control and economy of installation. All of the series luminaires used throughout the grounds are equipped with 6.6-ampere 6,000-lumen lamps. The general lighting of roads and walks is done with a tubular steel pole supporting a pendent luminaire (figure 4*M*) with rippled alabaster-glass globe. Standards are located on both sides of the road with an average spacing of 120 feet between standards. The plaza at the entrance to the administration building is illuminated by an upright luminaire (figure 4*D*) equipped with a polycase glass cylinder. In order to avoid interference to observers approaching the island by ferry boats, it was essential to employ light sources along the high-speed service roads on the western water front, that had a cutoff below the horizontal. The luminaire and standard (figure 4*C*) installed for lighting these roads is a pendent type consisting of a stepped Alzak aluminum reflector and spun-on refractor globe. The standards are spaced 150 feet apart on both sides of the road.

The automobile approach to the Exposition from the San Francisco-Oakland Bay Bridge is illuminated by 10,000-lumen series sodium lamps of the type used on the bridge. These standards (figure 4X) are spaced approximately 150 feet apart on opposite sides of the roadway and have a height to light center of approximately 25 feet.

There are many exhibit buildings at this Exposition with lighting effects worthy of consideration in this paper but which are omitted because of space limitations. Also there are many of the lighting features referred to which appear to be hastily dismissed with an inadequate description of the technical details. Several reasons prompt the authors to do this. Decorative lighting as was mentioned at the opening of this paper is more of an art than a science and considerable nonuniformity exists in the actual placing of the light sources to obtain the effects desired. Also, parallel architectural conditions seldom occur in practice. Consequently attempts to state the conditions of the problem and the method finally adopted to solve it are both involved and of little value as a guide.

Power is delivered to the Exposition at 12 kv by three 500,000-circular-mil three-conductor cables from Oakland and San Francisco to a 16,000-kva substation situated at the south side of the Palace of Fine Arts. This power is transformed from three-phase delta to 2,300/4,000-volt three-phase four-wire and distributed by 15 circuits to approximately 35 transformer substations throughout the grounds. An auxiliary substation of 4,000-kva capacity located at the amusement section serves this section. The secondary distribution for decorative and general lighting is four-wire 208-120 volts.

The series street-lighting circuits are ten in number; four originating from the substation and the remainder cascaded from them. These circuits operate at 6.6 amperes regulated by constant-current transformers. The total connected load fed from these series circuits is approximately 225 kw. The total connected load for the exterior lighting exclusive of the street lighting is approximately 4,000 kw. The battery of searchlights on Yerba Buena Island requires approximately 450 kw.


The Golden Spike
The Esplanade
Parking Area
Commonwealth
California
The Esplanade
The Esplanade
W
S
R
O

Treasure Island, San Francisco

A modified cartograph of the Golden Gate International Exposition, marked to locate the settings of the color pictures that appear on the following pages

Symbol	Designation	Color Plate
X	Court of Flowers	Plate 1
V	Court of Pacifica	Plate 3
O	Elephant Towers	Plate 5
Y	Cambodian Towers	Plate 6
W	Reflecting Pool	Plates 7 & 8
S	Court of the Moon	Plates 9 & 10
R	Tower of the Sun	Plate 11

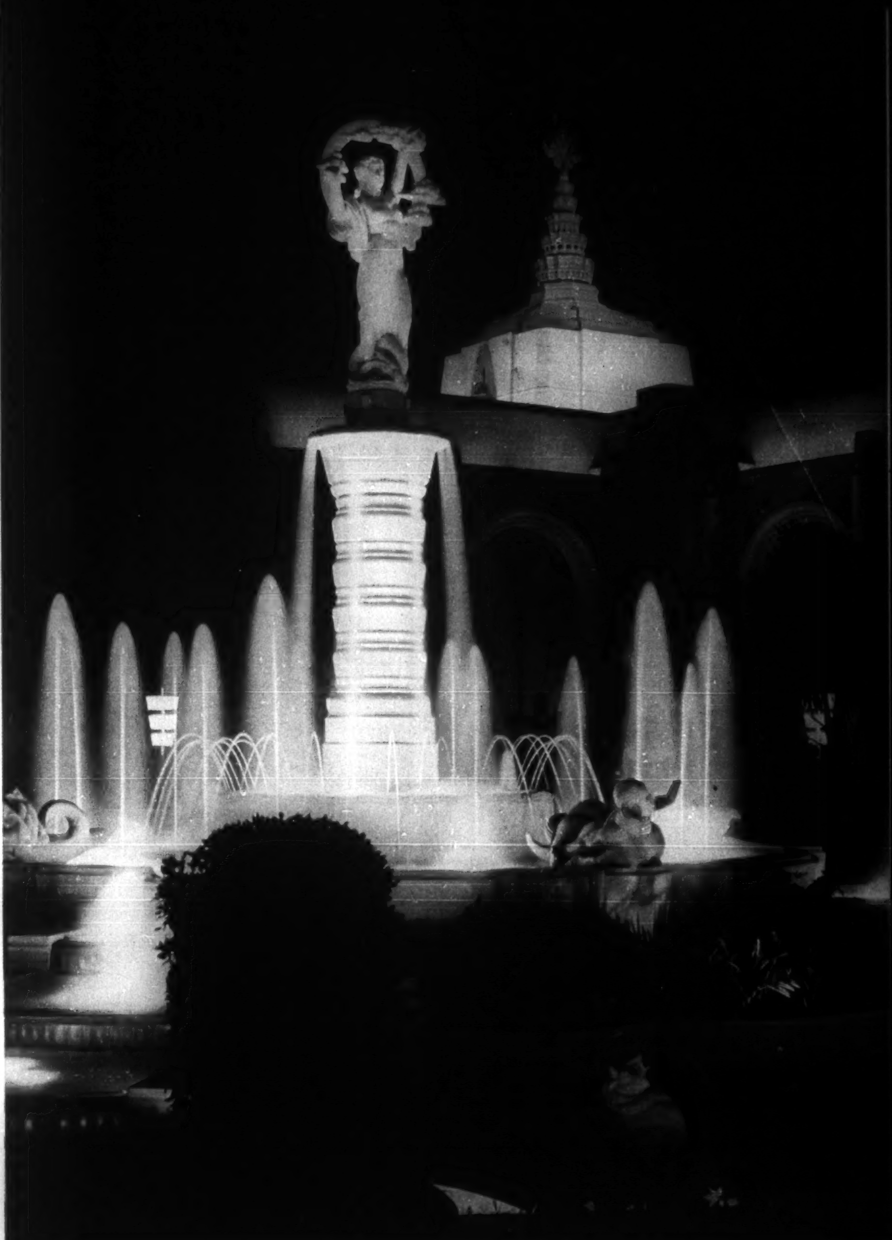


PLATE 1: "Court of Flowers," Louis P. Hobart, Architect.

COLOR: NEW SYNTHESIS IN THE WEST

PLATE 2. One of the most dramatic aspects of the Golden Gate International Exposition (seen here from Yerba Buena Island) is the clear integration of the exterior illumination system with the architectural design. Note the careful organization of both colors and intensities, the visual sequences achieved thereby, the subdued and largely decorative role of street lighting.

COLOR, LIGHT AND STRUCTURE AT GOLDEN GATE EXPOSITION

Almost unnoticed, the rapid development of new illuminants and lighting equipments has, in the recent past, added a distinct and prodigious dimension to the field of exterior illumination—that of color. But precisely because the application of color to buildings is both a complex and unevenly explored field, available examples are few and often inconclusive. A notable exception is this year's Golden Gate International Exposition. Here—with a scale and completeness seldom approached before—is an artificial illumination system which is an integral part of the project design. Mr. Dickerson's paper thus not only describes an illumination project of interest to light specialists but constitutes reference material of prime value to building designers, engineers, and others. All photographs were specially taken by Gabriel Moulin for ARCHITECTURAL RECORD.

by A. F. DICKERSON,
*Director of Illumination**

EACH OF THE great American expositions has marked progressive advances in the field of artificial illumination. Chicago's Columbian Exposition of 1893 startled the world with its wide use of the then-novel incandescent lamp. In 1915 the Panama-Pacific Exposition in San Francisco reached new levels in the use of exterior floodlighting. Chicago's Century of Progress showed further advances, particularly in the field of color. But it is safe to say that the current expositions employ a wider variety of new illuminants, equipments, and installations than all their predecessors combined. The Golden Gate International Exposition furnishes dramatic proof of such advances; and because the problems involved are applicable to all building design, description of its principal features seems worthwhile.

Daylight color: A standard palette of 19 colors and gold was adopted—with ivory, brown, gold and blues predominating. Seven colors are body or foundation colors; eight are used for embellishment of minor architectural features, four and gold for trim. Tints and shades, rather than pure colors, are employed throughout. All body colors are warm, with light intensities — yellow, ivory, fawn, ecru, taupe, brown, and blue; the secondary colors include yellow, jade (light and dark), emerald, four additional blues, red, mauve, and apricot.

In the majority of cases, this color was applied in pigment form in the plastered surfaces, in paint form on trim and detail. The plaster itself has an unusual luminosity, an effect achieved by pressing a mica product into plastered surfaces. The mica had first been heated to a high degree so that it became flaky and the laminations tended to separate. The result is a breaking up of the wall surfaces into innumerable small holes with a high reflective factor.

Night color: The artificial illumination must be understood as being superimposed on a base of the colors and surfaces described above. But the night color *does not seek to reproduce that of natural daylight.* On the contrary, many of the most successful effects are achieved solely with colored illumination on near-white surfaces. Compare, for example, the daylight and night colors of the Court of Pacifica (Pl. 3 and 4). Nor do the various *intensities, shadows, and silhouettes seek to duplicate those of natural daylight.* In fact, the illumination may be said to yield an additional —and entirely new—design over and above that of daylight.

The basic consideration in designing the illumination of the Exposition was to make it a truly integral part of the building design. Such integration of light and structure implied much more than mere concealment of the light sources, or avoidance of glare and shadows. That it has been achieved —and with spectacular success in installations such as that of the Court of the Moon (Pl. 9) and the Elephant Towers (Pl. 5)—is apparent. Here color achieves a luminosity which appears as coming from *within* the building rather than of being applied from *without.*

Once inside the bright exterior walls, the visitor moves through a series of color effects in a succession of walled courts—each lighted differently, with a harmony which makes the transition from one to another easy, yet distinct. In these courts, lighting has been used to fit the visitor psychologically to enjoy the setting. Emotions are excited toward gaiety by shades of red, rose, orange, and yellow— while greens, blue greens, and blues are used as tranquilizers. Colors are always in harmony and restricted in number from any viewpoint. Highlights, shadows, and silhouettes break up the monotony of the usual flatness of floodlighted surfaces. Advantage has been taken of every flag, niche, or unusual architectural element, to introduce a change of color or a highlight in order to increase the liveliness of the night scenes. Fountains throughout the grounds are illuminated, usually in static colors excepting in the Court of Pacifica (Pl. 3) where a shifting color cycle is controlled by thyratrons. In many places trees have been lighted by concealed mercury floodlights; but in general they count as dark masses against light.

Installations and fixtures: While the effect of the Exposition's illumination system may often appear extravagant, construction and operation were in fact relatively simple and inexpensive. All equipment is commercially available, including a weather-proof low-cost searchlight and a low-cost fluorescent floodlight, both of which were especially developed by General Electric for the Exposition. The various installations are in common use in ordinary commercial buildings: *floodlighting*—in trenches, set-backs, soffits, etc.; *spotlighting* of architectural features —towers, sculpture, niches, etc.; *groundlighting* by means of light standards and recessed fixtures; *water lighting* by underwater floodlights, lip lighting, etc. The chief interest here lies largely in demonstrating the extraordinary versatility possible with careful study at a sufficiently early stage.

But each of these installation types represents a high degree of specialization so that, when properly integrated, there is little need for supplementary lighting. This explains the relative unimportance of street-lighting units in the illumination design. In the major courts, these units serve more as architectural features than as sources of illumination. Outside the courts, street-lighting units of a more utilitarian character are used.

Illuminants: The range of lighting media includes all commercially available sources — incandescent, lumiline, arc, mercury- and sodium-vapor, neon, fluorescent, and ultra violet "black light" for use with luminescent paints. These media were used singly and in combinations, depending upon the colors, changes, and intensities desired. Quantitatively the most important were incandescent and fluorescent lamps — the specially developed fluorescent floodlights being widely used.

All illumination designs shown are by General Electric's Illuminating Laboratory.

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*The overall design of day-and-night color is the end product of close cooperation between the following Exposition officials: W. P. Day, Director of Works; Jesse E. Stanton, Director of Color; Edward L. Frick, Chief of Division of Architecture; William R. Van Bokkelen, Chief of the Electrical Division; George E. Garthorne, Assistant Chief and in charge of Exterior Illuminations; the entire Architectural Commission under the chairmanship of Arthur Brown, Jr.; and Mr. Dickerson, Director of Illumination.

No novice in the field, Mr. Dickerson played an important part in the illumination of the Panama-Pacific Exposition of 1915, the Brazilian Centennial in 1922, A Century of Progress in 1933, and in floodlighting of Niagara Falls. Since 1932, he has been head, Illuminating Laboratory and Lighting Sales of the General Electric Company at Schenectady, N. Y.

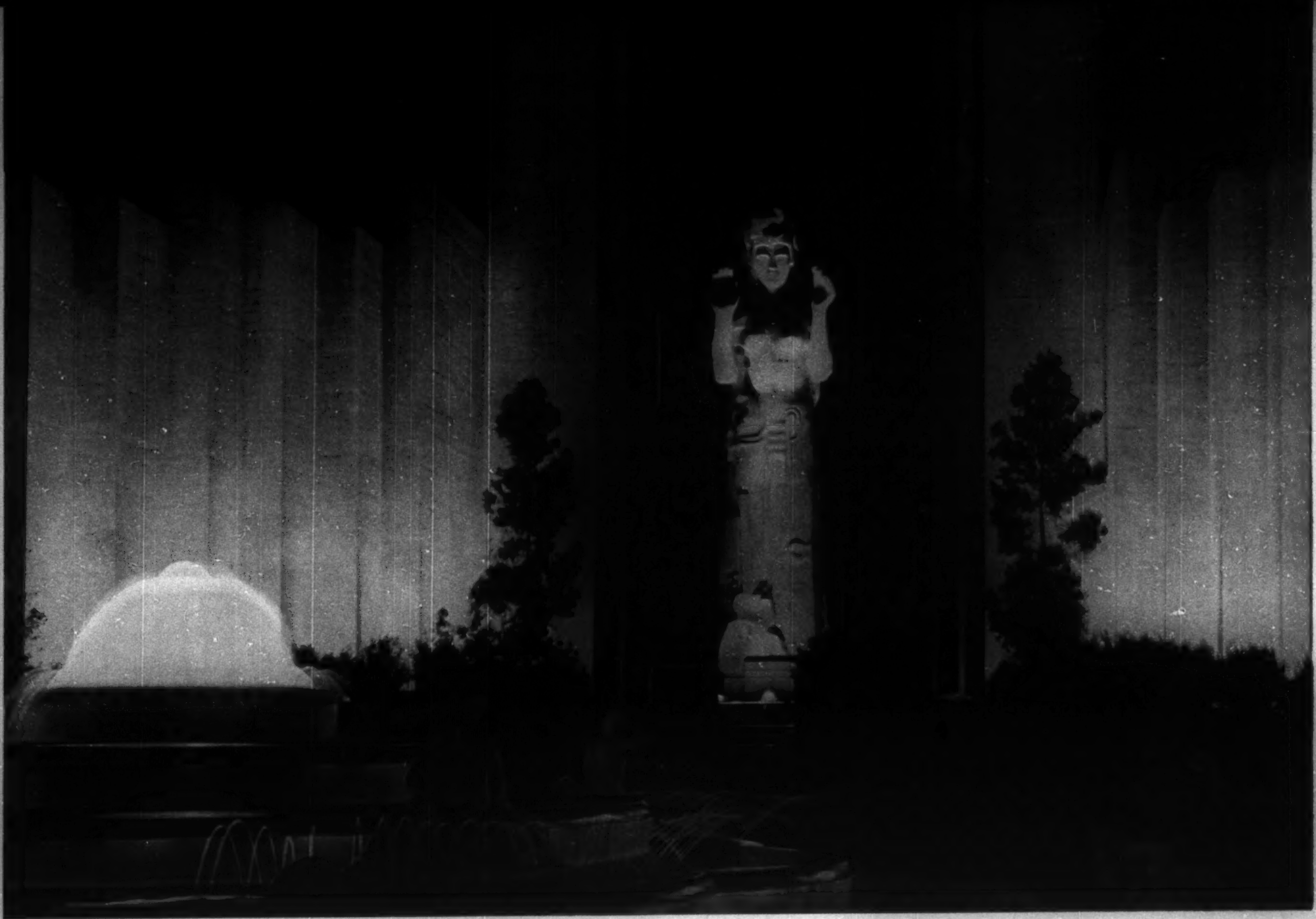


PLATE 3. Vivacity is here achieved not only by a wide variation of color and intensity of light but also by its movement. Synchronized thyratron controls give smoothly changing yet steady emphasis to the two most important architectural features—the statue and the fountain. Backgrounds have even values and harmonized colors which silhouette the planting.

THE COURT OF PACIFICA

TIMOTHY L. PFLUEGER, Architect

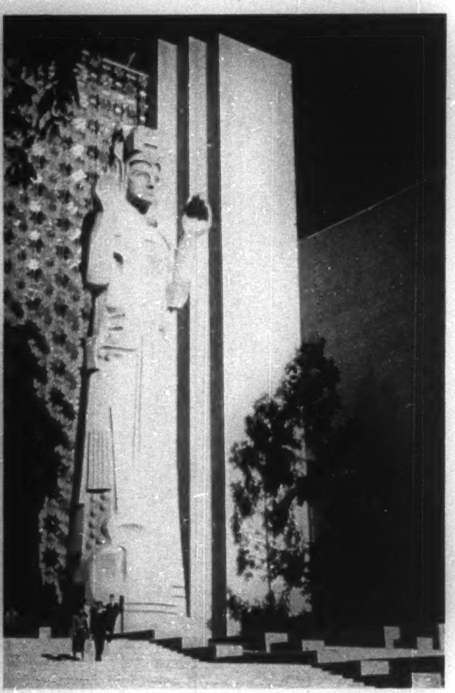


PLATE 4. This daylight view serves as a point of reference as regards the daylight-color base on which illumination is superimposed. All structures illustrated have similar colors and values.

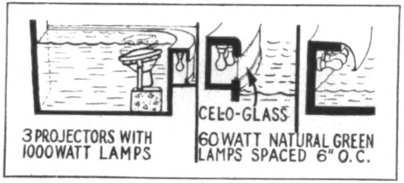

3 PROJECTORS WITH
1000 WATT LAMPS
CELO-GLASS
60 WATT NATURAL GREEN
LAMPS SPACED 6" O.C.

FIGURE 1

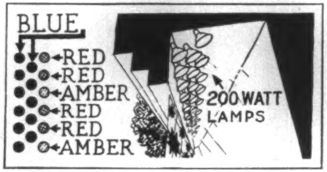

BLUE
RED
RED
AMBER
RED
RED
AMBER
200 WATT
LAMPS

FIGURE 2

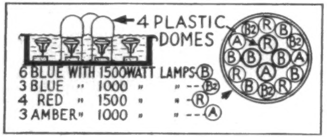

4 PLASTIC
DOMES
6 BLUE WITH 1500 WATT LAMPS Ⓑ
3 BLUE " 1000 " " B2
4 RED " 1500 " " Ⓡ
3 AMBER " 1000 " " Ⓐ

FIGURE 3

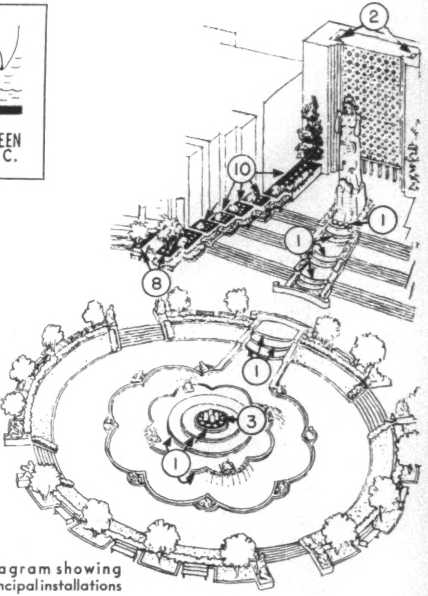


Diagram showing principal installations

THE DESIGN of this court, as well as its location adjacent to the most popular entrance to the Exposition, called for a lighting treatment tending toward the spectacular. The chief installations of the illuminating system are:

The Great Window with its stainless-steel screen and plaster backdrop lit by 75 amber, 150 red, and 300 blue 200-w. floods controlled by thyratrons. (Fig. 2);

Robert Stackpole's 85-ft. statue, "Miss Pacifica," lighted by two 1500-w. underwater floods (Fig. 1);

The cascades and central fountain, with lip lighting of green and changing colors from underwater floods where there are jets. (Figs. 1 and 3);

The 80-ft. walls flanking the Grand Stairs and also the walls of the East Entrance are flooded in a golden amber (similar to Fig. 10);

Sixteen 86-ft. cylindrical lanterns (not shown) of Glyptal-treated fabric shed a warm glow that completely envelopes the Court. Each lantern has three-hundred 60-w. lamps, or a total of 288 kws. is used for the 16 standards (design similar to Figs. 6 and 7);

All of the main trees are lighted with 250-w. mercury floods concealed in ground cylinders (Fig. 8);

The East Entrance and the Gayway Plaza are lighted by decorative units with tiers of amber-orange lamps and banners. This exposed lighting furnishes a transition to the Amusement Section. Approximately 550 kws. of lighting are used in this Court.

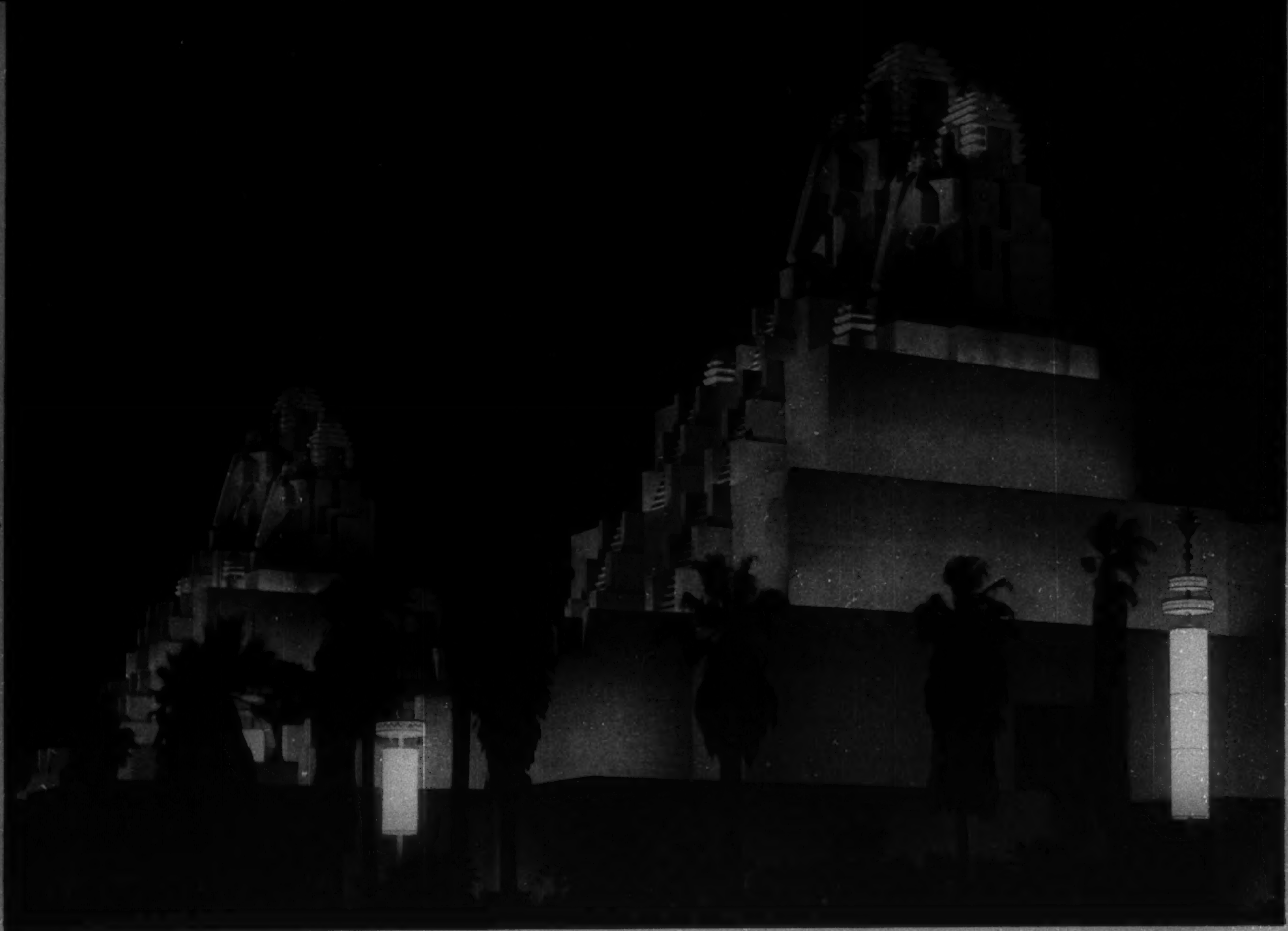


PLATE 5. Highest intensities and lightest colors have been widely used on those elements primarily designed to be seen from afar. Thus the brilliantly lit West Facade, with its monumental entrances, actually serves as a huge sign which dominates the city's eastern horizon and intersects its traffic lanes to the mainland. Colors are concentrated at the entrances.

THE ELEPHANT TOWERS AND WEST FACADE

ERNEST E. WEIHE, Architect

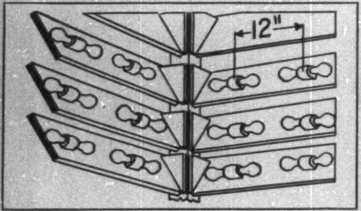


FIGURE 4

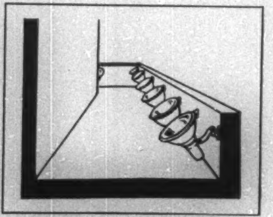


FIGURE 5

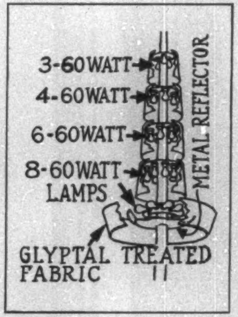


FIGURE 6

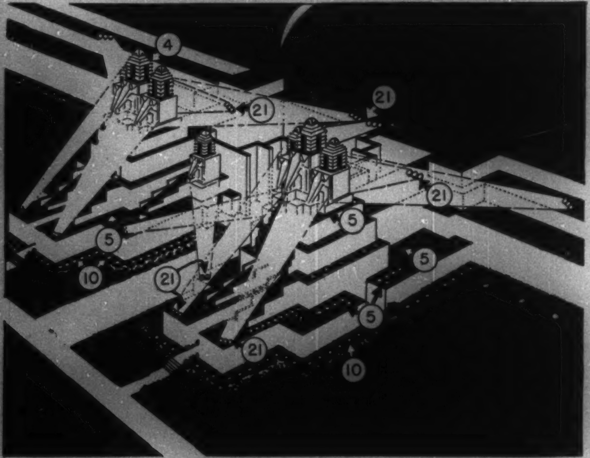


Diagram showing search- and floodlight locations for Towers and West Facade

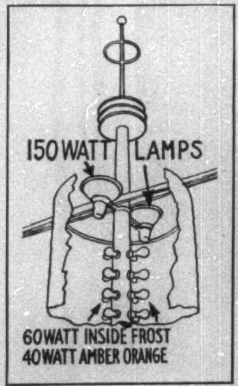


FIGURE 7

The West Facade, for its entire distance of 3600 ft. from the Administration Building north, is floodlighted in white with color reliefs at the South Tower, main Elephant Tower entrance, ferry entrance, and in the seven large green circles above the entrances. Thirty cylindrical lanterns, 50 ft. in height with 4920-w., and 102 similar small lanterns, 18-ft. high with 1620-w., add a festive note to this area. These lanterns are all steel framework on steel poles with decorated Glyptal-treated fabric as the transparency (Figs. 6 and 7).

The main wall is lighted by Novalux L-49 floods with polished Alzak-processed reflectors, asymmetric lenses and 500-w. I. F. MAZDA lamps. These are spaced on 10-ft. centers, 15 ft. from the base of the wall, in a specially provided trench that is completely concealed by trees and shrubbery. (Similar trenches of various widths are used for all floodlighting throughout the Exposition. See Fig. 10.) In general, illumination values increase with height (Pl. 5): on the first set-back 200-w. floods with etched reflectors and clear lenses are used on 8-ft. centers; 300-w. floods on 10-ft. centers light the second set-back (Fig. 5). The elephants themselves are picked up with 18-in. incandescent searchlights equipped with 1500-w. floodlighting lamps (Fig. 21). The louvered "howdahs" are in red, with 60-w. natural ruby incandescent lamps 12-in. o.c. (Fig. 4).

White light on the cream plaster yields surfaces whose values are approximately equal to those of daylight

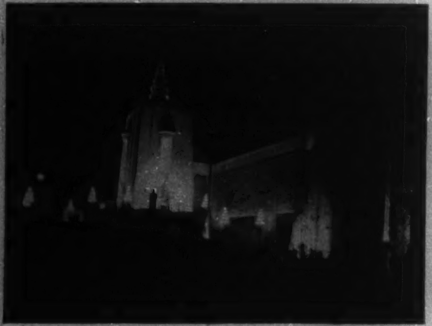
PLATE 6. The richly textured golds of Lulu Braghetta's and Jaques Schnier's bas-reliefs are carried in pink, the towers in golden amber, while the set-backs are carried in lavender. Here, again, a brilliant lighting effect is adequately framed by quiet, unlighted water. Thus the scale and balance of daytime are maintained at night while new colors are introduced.

THE CAMBODIAN TOWERS AND EAST FACADE

WILLIAM G. MERCHANT, Architect

THE ENTIRE East Facade (to the right but not shown) is lighted in uncolored light in much the same manner as the West Facade. Trenches of 6-ft. width, completely concealed by shrubs and trees, house 500-w. asymmetric floods on 8-ft. centers for lighting walls (Fig. 10); the illumination of the set-back is provided by 200-w. floods. This lighting is broken by large standards at the building entrances and the East Towers and murals (Pl. 6) which form the East entrance to the walled city.

The Towers, rising from tiers of steps, are heavily ornamented in gold leaf and carried in golden light at night. The huge bas-relief gold-leafed murals are lighted by pink fluorescent floods concealed at their bases (Fig. 12), and similar installations light the set-backs in lavender. The terraces and stairs are lighted by Siamese umbrella-type units built of fabric. Trees in the center are floodlighted by 250-w. mercury-vapor lamps (Fig. 8). The lighting of flags and flag masts uses the same installation employed elsewhere (Fig. 9).



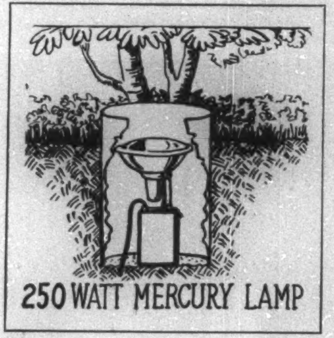


FIGURE 8

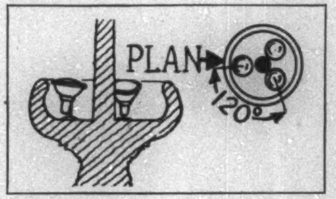


FIGURE 9

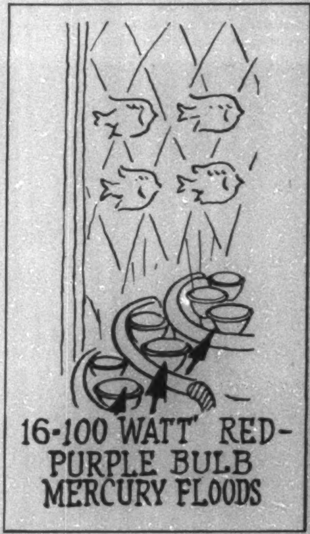


FIGURE 11. Luminescent murals used in the Court of the Seven Seas.

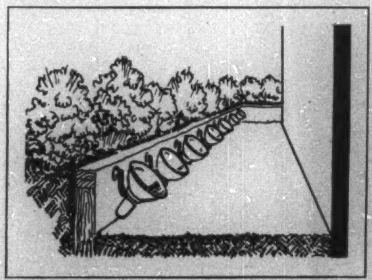


FIGURE 10

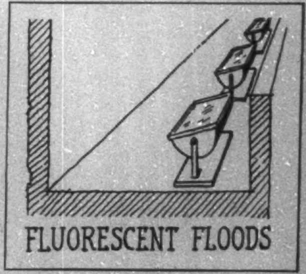
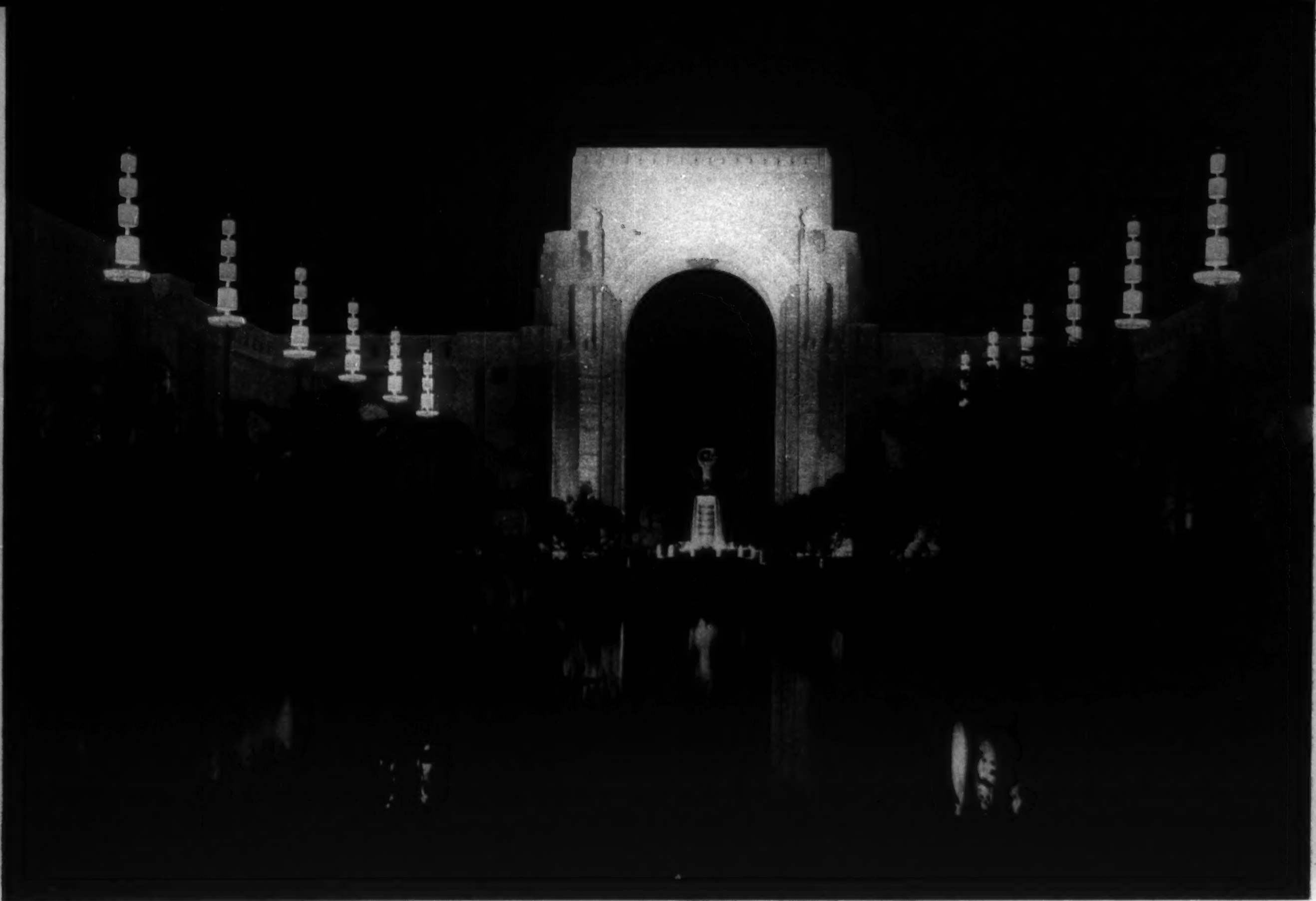


FIGURE 12



THE COURT OF REFLECTIONS
LOUIS P. HOBART, Architect

PLATE 7. Connecting the brilliant East Entrance (Pl. 6) and the Tower of the Sun (Pl. 11), the color and lighting levels of this court are adroitly handled. Walls and set-backs are lighted with pink fluorescent floods (Fig. 12), niches with blue fluorescent tubes (Fig. 22). The Arch is flooded from base and adjacent roofs with white 18-in. incandescent searchlights (Fig. 21).

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1940

PLATE 8. Looking west in the Court of Reflections towards the Tower of the Sun. As the name implies, water is here used as a reflecting medium to pick up and repeat the brilliantly lit Tower of the Sun. Elsewhere (Pls. 1, 10, and 11) water becomes part of the illumination system itself, adding not only color and movement but a considerable amount of light.

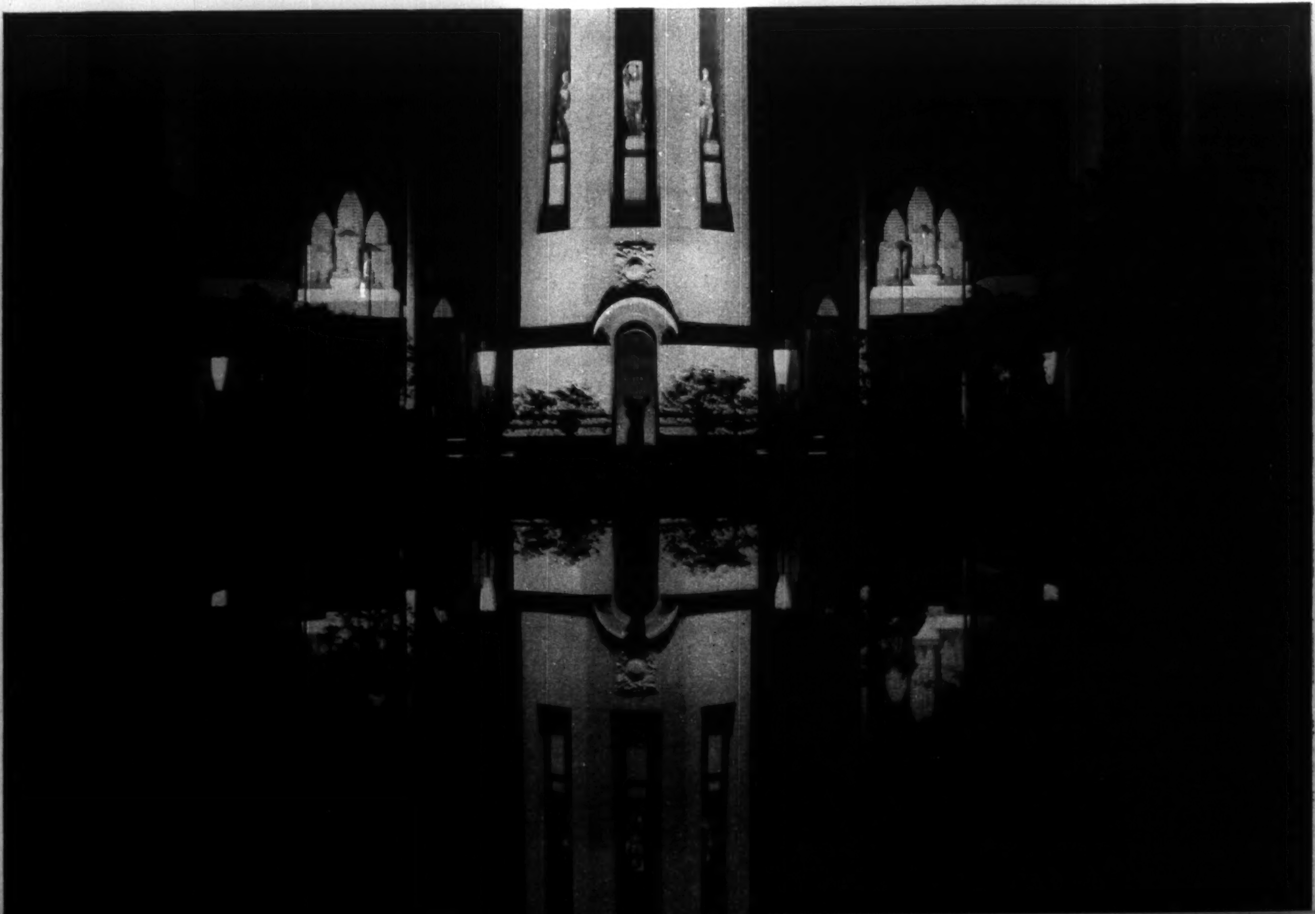


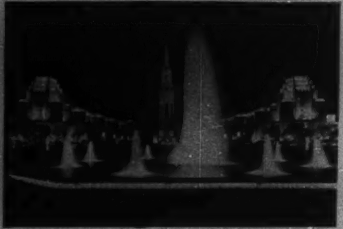
PLATE 9. Illumination of this Court is subdued, employing cool colors and low intensities. The main walls are carried in blue with a lavender overlay, while the set-backs are in blue; a mottled pink-and blue effect is used on the towers. All relief lighting—in grilles, niches, and louvers—is carried in amber to lighten the effect and tie it in with that of the other Courts.

THE COURT OF THE MOON

GEORGE W. KELHAM, Architect

HERE, AGAIN, illumination cannily implements both architecture and circulation: although physically one of the four major axes which radiate from the Tower of the Sun, this court is unimportant circulation-wise. The illumination is thus subdued, designed for relaxation, not for pulling heavy traffic. For the walls, 500-w. blue floods are used on 30-in. centers; in the same trench, pink fluorescent floods on 60-in. centers tint the blue to an orchid shade (Fig. 16). The crenelated set-backs are lighted with three 15-w. blue fluorescent floods (Fig. 15). Both blue and pink fluorescent floods are used on the tops of the towers for producing the striking mottled effect (Fig. 14).

Ground lighting is by downlights in ten units concealed in decorative elements of iron and Celoglas (Fig. 17). The other lighting features are: the 24 arches of water, lighted by seventy-two 250-w. amber underwater floods; the north fountain with its falling jets and cascades in amber (Fig. 13); mercury lighted trees; spotlighted statuary; grilles and turrets in amber relief lighting.



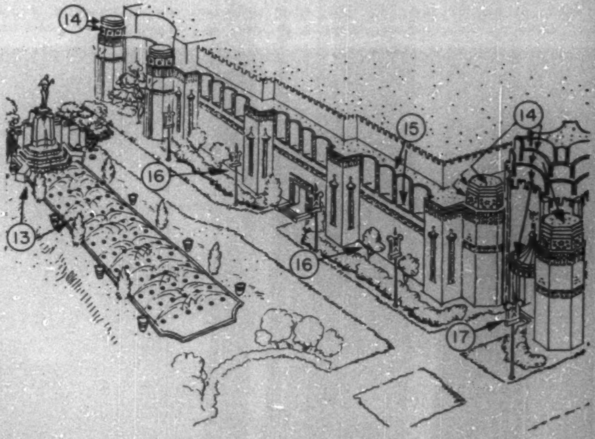

14
14
15
16
16
13
17

Diagram showing relationship of major installations in Court of the Moon

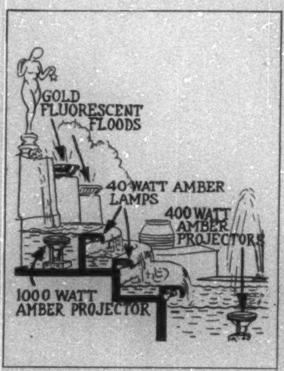

GOLD FLUORESCENT FLOODS
40 WATT AMBER LAMPS
400 WATT AMBER PROJECTORS
1000 WATT AMBER PROJECTOR

FIGURE 13

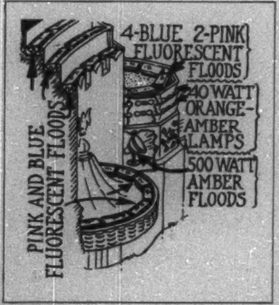

4-BLUE 2-PINK FLUORESCENT FLOODS
40 WATT ORANGE-AMBER LAMPS
500 WATT AMBER FLOODS
PINK AND BLUE FLUORESCENT FLOODS

FIGURE 14

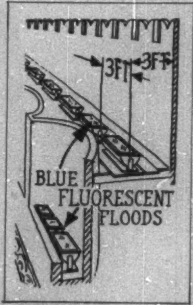

3FT
3FT
BLUE FLUORESCENT FLOODS

FIGURE 15

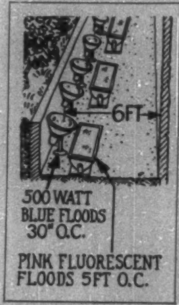

6FT
500 WATT BLUE FLOODS 30" O.C.
PINK FLUORESCENT FLOODS 5FT O.C.

FIGURE 16

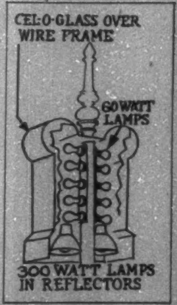

CEL-O-GLASS OVER WIRE FRAME
60 WATT LAMPS
300 WATT LAMPS IN REFLECTORS

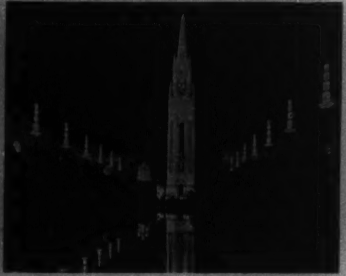
FIGURE 17

PLATE 10. The golden-amber floodlighting on both Cadorin's sculpture, "Evening Star" and the long pool save the lighting of the Court of the Moon from appearing too low or subdued in key — the 24 jets and the fountain itself give movement to the scene. Added color is achieved by the mercury-lit trees and the relief lighting in amber.

THE TOWER OF THE SUN

ARTHUR BROWN, JR., Architect

Since the Tower of the Sun is the "theme center" of the Exposition, and since the Court of Honor in which it stands is consequently the most important traffic intersection within the Exposition gates, it was important that the night effect be at least as dramatic as that of daytime. This has been accomplished (Pl. 11) by concentrating light at high intensities on the Tower, which in turn lights the Court by reflection. The shaft proper is lighted by 56 white 1500 w. searchlights (Fig. 21), 24 amber and 24 white 1000 w. floodlights, and 60 amber 1000 w. floodlights (Fig. 20). A ring of twenty-four 500 w. concealed street-lighting units (Fig. 23) supplements the court lighting. Other features are: the four illuminated fountains (Fig. 1), the eight flags (Fig. 9), the four belvederes and the lighted trees (Fig. 8).



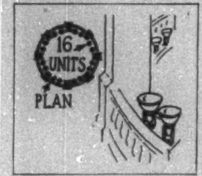

16 UNITS
PLAN

FIGURE 18

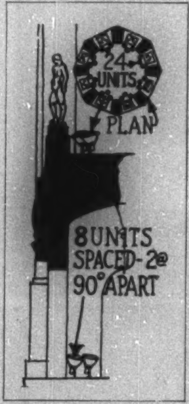

24 UNITS
PLAN
8 UNITS SPACED-2@ 90° APART

FIGURE 19

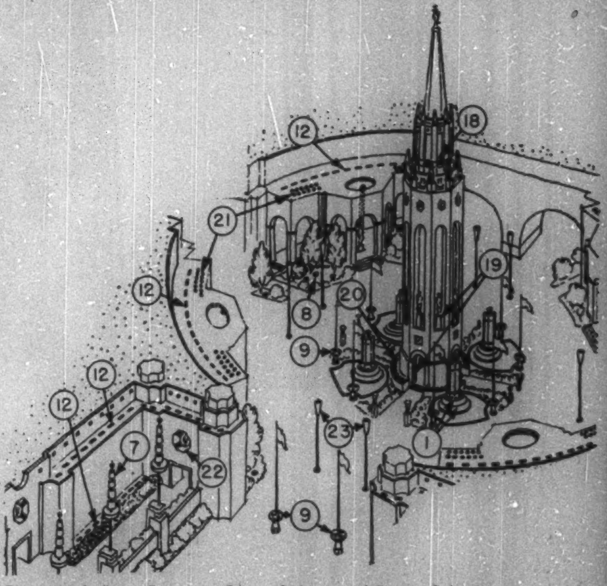

12
18
21
19
12
8
20
9
12
12
7
22
23
1
9

Diagram of all installations in Court of Honor and Tower of the Sun

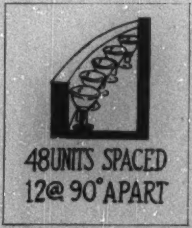

48UNITS SPACED
12@ 90° APART

FIGURE 20

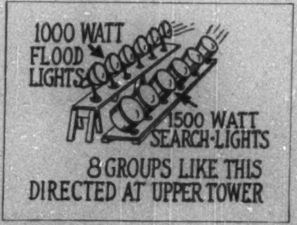

1000 WATT FLOOD LIGHTS
1500 WATT SEARCH-LIGHTS
8 GROUPS LIKE THIS
DIRECTED AT UPPER TOWER

FIGURE 21

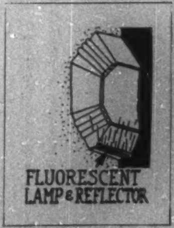

FLUORESCENT LAMP & REFLECTOR

FIGURE 22

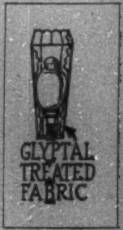

GLYPTAL TREATED FABRIC

FIGURE 23

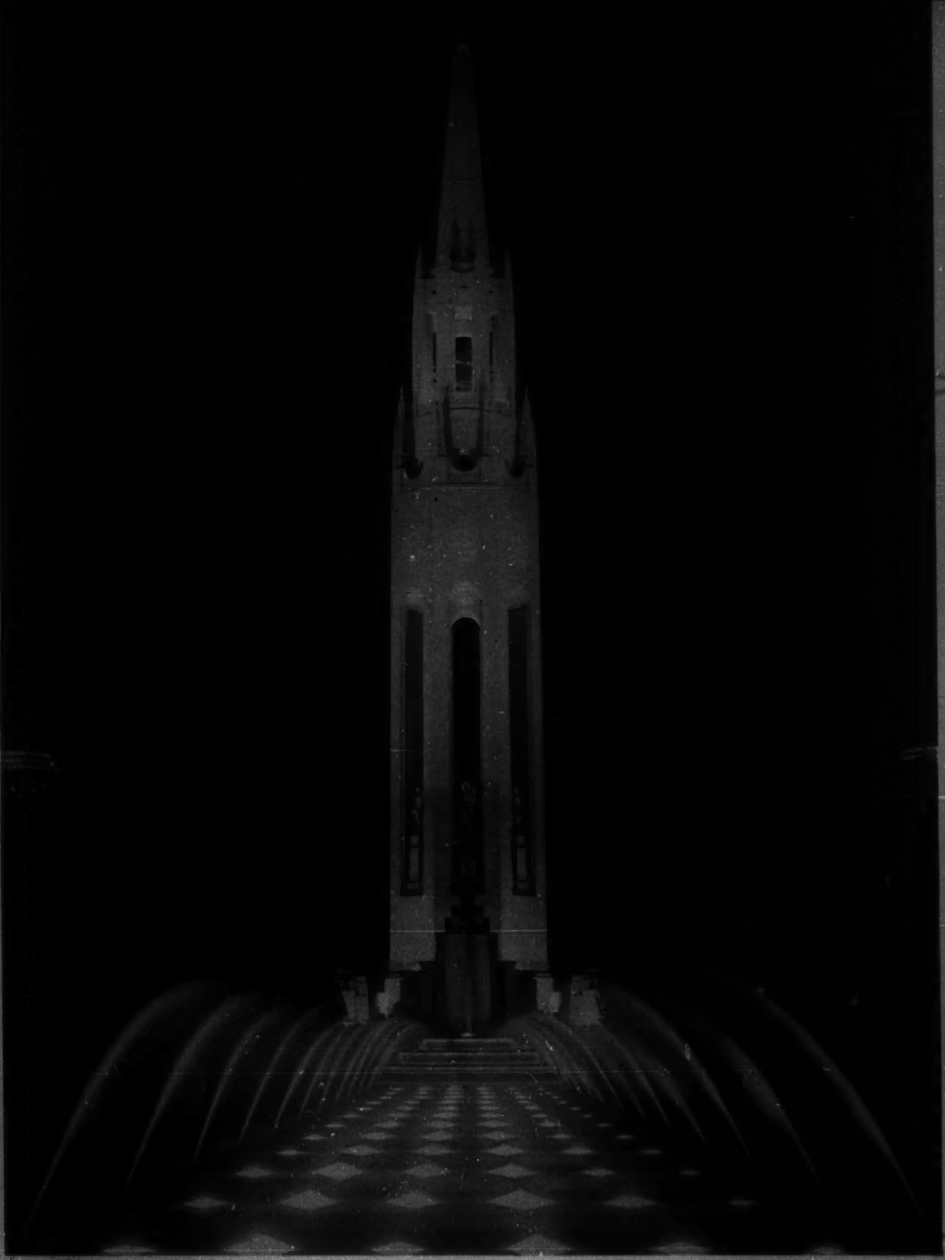


PLATE 11. Beginning with a warm amber at the base, the Tower of the Sun changes gradually to a white of increasing intensity at the top. Relief lighting is blue at the base, straw yellow at the belfry, amber at the pinnacle (Figs. 18, 19 and 20). The surrounding Court of Honor is mainly illuminated by this reflected light, although 24 decorative street lighting units supplement the design.

Ionization Time of Thyratrons

A. E. HARRISON
ASSOCIATE AIEE

Synopsis: Seventeen thyratron and grid-glow tubes, representing 12 different types, have been tested, and their ionization times have been measured with a cold-cathode-type oscillograph. These ionization times vary over a wide range, from a fraction of a microsecond to several hundred microseconds. Ionization time has been found empirically to be inversely proportional to grid overvoltage. Grid overvoltage is defined as the difference between the potential applied to the grid and the critical potential necessary to prevent firing. Argon, neon, and mercury-vapor tubes were included in the test. Mercury temperature was found to have no appreciable effect on the ionization time with low grid overvoltages. All mercury-vapor tubes have similar ionization-time characteristics. In fact, tubes of the same type and manufacture sometimes differed from each other more than one tube differed from another tube of different construction and manufacture.

GRID-CONTROLLED vapor-filled rectifiers, or thyratrons, have not been considered in the past for use in blocking and sweep circuits for cold-cathode-type oscillographs because of their inherent ionization time. A search of the literature on the subject, and reference to manufacturers' data sheets furnished little useful information on this ionization time. A research program to determine the minimum ionization times of various tubes was thus started in 1938, and first results showed that minimum ionization times of 0.3 microsecond were obtainable with mercury-vapor thyratrons if the grid was driven sufficiently positive. These results suggested that these tubes might be successfully used in blocking circuits for high-speed oscillographs, and work on this phase of the problem is being continued. The apparatus devised for measuring ionization times made possible an extended study of the relation between ionization time and grid overvoltage as a function of anode voltage and vapor temperature (for mercury tubes) and it is the results of this extension of the original problem which will be discussed.

Experimental Procedure

Ionization time of a thyratron, or the delay between the time of application of a square wave impulse to the grid and the time the tube voltage reaches its normal conducting value, is easily measured with a high-speed cathode-ray oscillograph arranged to record the voltage across the tube, provided that the time of application of the grid impulse is also shown. It is necessary to synchronize the unblocking of the high-speed oscillograph with the tripping impulse applied to the tube under test. (Tube *B* in figure 1.) This is accomplished by the use of a trip tube (tube *A*) which is fired by a surge from the oscillograph blocking circuit, delayed a fraction of a microsecond by R_5C_g. An impulse of magnitude determined by the ratio of $R_3/(R_3+R_4)$ is applied to the grid of the tube under test when tube *A* fires. Grid impulse voltages from 3 to 63 volts are selected with a tap switch connected to different values of R_3, allowing the grid overvoltage, or difference between the potential applied to the grid and the critical potential necessary to prevent firing, to be varied over a wide range. The time of application of grid impulse is recorded on the oscillogram through the coupling capacity C_4. C_4 and C_5 are small coupling capacities formed by one-inch lengths of twisted insulated wires.

R_1, R_6, R_7, R_8—1,000,000 ohms
R_2—20,000 ohms
R_3—50–1,350 ohms
R_4—3,700 ohms
R_5—100,000 ohms
C_1, C_2—1 microfarad
C_3—0.001 microfarad
C_4, C_5—Twisted wire capacities
C_G—Electrode capacity
Tube *A*—885 thyratron
Tube *B*—Tube under test

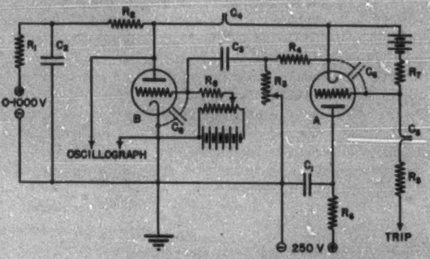


Figure 1. Circuit diagram of thyratron test apparatus

Values of grid impulse voltage were calculated from the ratios of $R_3/(R_3+R_4)$ for the various taps and were checked with the cathode-ray oscillograph. The apparatus was mounted as near the oscillograph as possible (see figure 2) in order to reduce the length of leads. Tests with the oscillograph verified that the time constant of R_4 and the grid-to-cathode capacity of tube *B* had no appreciable effect on the measurement of the ionization time.

First tests were made with the tube under test in a circuit similar to that of tube *A* without R_5, and ionization times were measured from the beginning of the oscillogram. Two factors made such a simple circuit unsatisfactory; there was no control of grid overvoltage independ-

Figure 2. Thyratron test apparatus connected to the high-speed oscillograph

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A. E. HARRISON, with Sperry Gyroscope Company, San Carlos, Calif., was a student at California Institute of Technology, Pasadena, at the time this paper was written.

1. For all numbered references, see list at end of paper.

Table I. Range of Ionization Times

Number of Tubes Tested	Tube	Manufacturer	Gas	Anode Voltage	Shortest Measured Ionization Time (Micro-seconds)	Approximate Maximum Ionization Time (Micro-seconds)
Hot-cathode types						
2	*FG*-17	General Electric	Mercury	1,000	0.50	50
2	*FG*-57	General Electric	Mercury	1,000	0.40	50
1	*FG*-67	General Electric	Mercury	1,000	0.60	150
1	*WE* 287-*A*	Western Electric	Mercury	1,000	0.65	50
1	*KY*-21	Eitel-McCullough	Mercury	1,000	0.45	20
1	866 special	*	Mercury	1,000	0.35	20
3	885	RCA	Argon	300	0.08	1
1	2*A*4*G*	Raytheon	Argon	300	0.10	10
1	*FG*-81	General Electric	Argon	180	0.25	50
2	*KU*-610	Westinghouse	Neon	750	1.80	300
Cold-cathode types						
1	0*A*4*G*	Raytheon	Argon	300	3.0	300–1,000
1	Strobotron	General Radio	Neon	350	12.0	300

* An 866 rectifier with a control electrode added. Manufacturer unknown.

ent of initial grid bias, and tests with an 885 indicated that the delay in the blocking circuit of the oscillograph was greater than the minimum ionization time of this tube. Both drawbacks were eliminated by the addition of a trip tube to the circuit. An 885 tube was finally chosen as a trip tube because its breakdown time was quite short and gave the best approach to a true square-wave impulse. R_5 was therefore added to delay the trip until the oscillograph sweep had started.

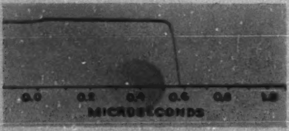

Figure 3. Typical oscillogram for an 885 thyratron

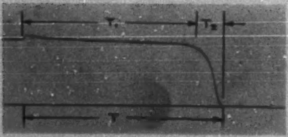

Figure 4. Different phases in breakdown of an FG-57

T_1—Time lag
T_2—Breakdown time
T —Ionization time

Direct coupling and capacitive coupling to the grid of tube *B* were tried and capacitive coupling was finally adopted because it allowed control of initial grid bias with a potentiometer and frequent check of the critical bias was possible. Originally, carbon resistors were used for R_3 and R_4; but this type of resistance has an odd behavior under transient conditions and the grid impulse voltage could not be calculated from the d-c resistance of the carbon units. Metallized resistor units for R_3 and ten metallized resistor units approximtely 400 ohms each in series for R_4 overcame this difficulty, giving a voltage divider with approximately the same characteristics for d-c and transient conditions.

Condensed-mercury temperatures were measured with a mercury thermometer wrapped with tinfoil and tied against the base of the bulb. This method did not give accurate measurement of the temperature, and did not allow tests at subnormal vapor temperatures, but the results do indicate qualitatively the effects of changes in mercury temperature.

Test data were obtained photographically for the 12 types of tubes listed in table I after the tubes had reached an equilibrium temperature. Three or more oscillograms were taken for each value of grid overvoltage and a fixed anode voltage. The sensitivity of the oscillograph was such that anode voltages greater than 1,000 volts could not be recorded easily. The time base was calibrated with a radio-frequency oscillator which was checked against broadcast stations. Figures 3 and 4 are representative oscillograms of anode-to-cathode potential. Ionization time is measured from the time of application of grid impulse, recorded as a slight rise in voltage at the beginning of the oscillogram, to the time at which the tube voltage has dropped to its conducting value. The ionization time for the Strobotron was an exception, since a Strobotron is used without a load resistance to discharge a capacitor, and its ionization time was measured to the beginning of breakdown.

Experimental Results

Ionization time as a function of grid overvoltage has been plotted in figure 5 for six of the tubes listed in table I. Inspection of the curves of figure 5 suggests a resemblance to a family of rectangular hyperbolas. Figure 6 was prepared to check this suggestion and shows the reciprocal of ionization time plotted as a function of grid overvoltage. The resulting curves are nearly linear, indicating that the hyperbolic form of the curves in figure 5 is not an illusion. The greatest deviation from linearity is shown by the *FG*-81; but this is not surprising since the maximum grid overvoltage is high compared with the anode voltage. All of the argon-filled tubes reached a low value of ionization time with a grid overvoltage of 20 or 30 volts, and further increase of grid impulse voltage failed to reduce the ionization time appreciably.

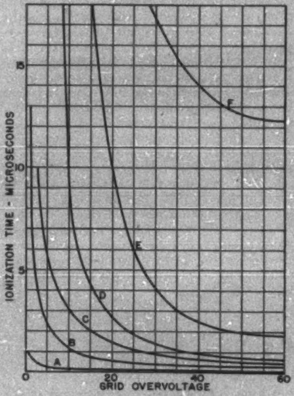

Figure 5. Typical ionization-time curves for thyratrons

A—885 argon-filled thyratron, 300 volts on anode
B—*FG*-81 argon-filled thyratron, 180 volts on anode
C—*KY*-21 mercury-vapor thyratron, 1,000 volts on anode
D—*FG*-67 mercury-vapor thyratron, 1,000 volts on anode
E—*KU*-610 neon-filled thyratron, 750 volts on anode
F—Strobotron, neon-filled, cold-cathode, 350 volts on anode

V. C. Rideout, graduate student at the California Institute who is continuing this study, has varied grid bias, grid impulse voltage, and anode voltage proportionately in an attempt to maintain proportional gradient values for each test. His conclusions are not yet definite, but there is reason to believe that this technique gives reciprocal curves which are more nearly linear.

Published data regarding the ionization times of thyratrons are meager. Hans Klemperer[1] in 1932 suggested a theory based on the mobility of the positive ions, and his calculated ionization times agreed in magnitude with his observations. Unfortunately the magnitude of the positive impulse applied to the grid by his method was not given, although it apparently was large. Klemperer's theory predicts that the time from the application of grid overvoltage until the start of voltage decrease across the tube, (T_1 in figure 4) and the time for the anode voltage to reach its final conducting value after decrease across the tube has begun, T_2, should be approximately equal. This relation is true for large grid overvoltages, but it is definitely not true for low grid overvoltages. The ionization time may depend on the initial number of electrons available to cause ionization, the so-called dark electron current.[4,6] Some vapor tubes have much larger dark electron currents just before breakdown than others. Figure 4 indicates the existence of a relatively large electron current by a gradual drop in anode voltage before final breakdown. No change in the ionization time of a *KY*-21 was observed when R_3 was changed from 20,000 ohms to 2,000 ohms if the grid overvoltage was greater than five volts. There is a substantial reduction in the ionization time for grid overvoltages of two or three volts, however, when the load resistance is reduced. As yet, a theory to predict or explain the observed relation between ionization time and grid overvoltage has not been suggested.

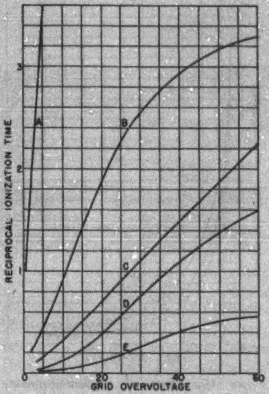

Figure 6. Reciprocal ionization times for figure 5

A—885 argon-filled thyratron, 300 volts on anode

B—*FG*-81 argon-filled thyratron, 180 volts on anode

C—*KY*-21 mercury-vapor thyratron, 1,000 volts on anode

D—*FG*-67 mercury-vapor thyratron, 1,000 volts on anode

E—*KU*-610 neon-filled thyratron, 750 volts on anode

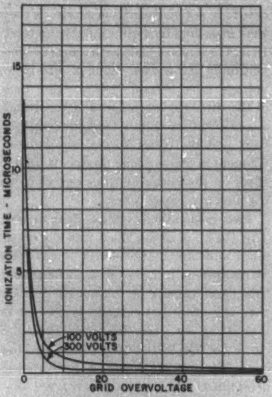

Figure 7. Effect of anode voltage on ionization time of a 2A4G argon-filled thyratron

Observations regarding individual tubes may be interesting. The electrode construction of an 885 differs radically from that of the other tubes and this thyratron has a very short ionization time, practically independent of anode voltage. In comparison with an 885, a *2A4G* has much longer ionization times at low grid overvoltages, but the ionization times are almost identical at the higher grid overvoltages. Compare figure 7 and curve *A* in figure 5. An *FG*-81, however, has an ionization time curve more nearly like that of the mercury-vapor tubes, although it is argon filled. Ionization times for neon-filled tubes were in all cases longer than the ionization times of argon or mercury-vapor tubes. Two *KU*-610 tubes were tested and gave slightly different results with 750 volts applied to the anode. These tubes would not operate consistently at anode voltages less than 500. These were old tubes, however, and this behavior may not be caused by the use of neon gas. Direct comparison of tubes filled with noble gases could not be made because the ionization times are affected by gas densities in the tubes and these were not known.

All tubes except the 885 show a variation of ionization time characteristic with different anode voltages. In all cases where anode voltage affected the ionization-time curve, an increase of anode voltage caused the knee of the curve to be more pronounced. Figures 7, 8, and 9 illustrate this effect for three types of tubes. The longer ionization time for large grid overvoltages with 100 volts on the anode is due primarily to an increase in the breakdown time (T_2 in figure 4), as the time lag T_1 is practically the same for all anode voltages when grid overvoltage is large. Note that there is little difference between the curves for an *FG*-57 and a *KY*-21 in figures 8 and 9. A second *FG*-57 showed ionization times consistently longer than those for the *KY*-21. It is common for mercury-vapor tubes of the same type and manufacture to differ from each other in characteristics, more than one differs from another tube of different construction and manufacture.

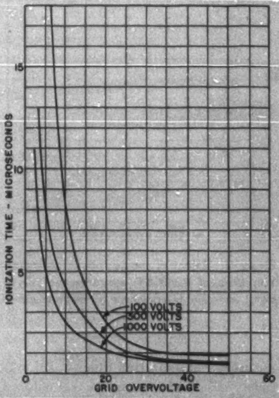

Figure 8. Effect of anode voltage on ionization time of an FG-57 mercury-vapor thyratron; mercury temperature 70 degrees centigrade

No change in the ionization time of several mercury-vapor tubes was found when the tube was biased to a large negative value (−50 volts bias for a tube with a critical bias of −5 volts) and the positive grid impulse increased so that the grid overvoltage was the same as before. Other circuits may give ionization times much longer than the values given here if they use high grid limiting resistors which introduce an additional *RC* lag.

An unusual phenomenon was observed while testing an 866 which had a special shield electrode placed around the cathode. The critical bias necessary with 400 volts on the anode changed within a few hours use from −4 volts to −60 volts. Loss of grid control continued as long as the filament was heated, although the anode voltage was zero and the grid bias −60 volts. A. W. Hull[2] mentioned this effect in 1929. The tube mentioned above was new, but the construction may be incorrect for use as a thyratron.

In some tubes, particularly the *FG*-17, a variation of ionization time for a given low grid overvoltage and low anode voltage was observed. The special 866 and the *KY*-21 also showed this effect, but the spread of ionization times was not as wide. An anode voltage of 300 reduced the variation, and 1,000 volts on the anode practically eliminated the effect. All of these tubes have filament-type cathodes and the effect is probably related to changes in effective cathode potential for different parts of the cycle. A difference in emissivity for different parts of the filament would exaggerate the effect, although the center of the filament supply might be at ground potential. The *FG*-17 tubes were several years old and probably not in the best condition, which may account for their pronounced variation of ionization time for a given low grid overvoltage.

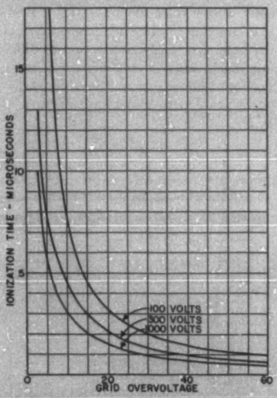

Figure 9. Effect of anode voltage on ionization time of a KY-21 mercury-vapor thyratron; mercury temperature 67 degrees centigrade

Variation of the ionization time characteristic of an *FG*-57 operated with an anode voltage of 300 is shown for three different mercury temperatures in figure 10. Mercury temperature has considerable effect at the higher grid overvoltages, but the curves coincide for low grid overvoltages. The temperatures indicated are not necessarily accurate, as they were measured by a questionable method. In fact, a series of tests with different temperatures and a fixed value of grid overvoltage gave erratic results. But each of the three curves in figure 10 represents a consistent set of data for some temperature, and the trend is certainly definite. Changes in temperature produce a similar effect on the ionization-time curves of all the mercury-vapor tubes.

Two cold-cathode tubes were tested. The *OA4G* is argon filled and its maximum ionization time is very long. The Strobotron is a neon-filled cold-cathode tube, and cannot be compared directly with the other hot-cathode types. It is interesting to note, however, that the curve for the Strobotron resembles the curve for the *KU*-610, a hot-cathode neon-filled tube, if the curves are displaced vertically until the minimum ionization times are equal (figure 5). Perhaps this is an indication that there is a fixed time lag necessary for initiation of emission, although the data are not sufficiently complete to prove this theory. Two values of capacitance were used across the anode and cathode of the Strobotron and the load resistor (R_2 in figure 1) was zero. Two microseconds were required to discharge a one-microfarad capacitor, and eight microseconds were required to discharge a ten-microfarad capacitor. These values are the time required for the capacitor to discharge. Discharge occurs after a delay, depending on grid overvoltage, as given in curve *F*, figure 5.

The results of these investigations show that the ionization time of mercury-vapor thyratrons may be as short as 0.3 microsecond if a positive impulse of sufficient magnitude is applied to the grid. Certain argon-filled types have even shorter ionization times. Characteristics of tubes of the same type may vary more than characteristics of tubes of slightly different construction. All tubes show the same general relation between ionization time and grid overvoltage. For most tubes the ionization time is inversely proportional to the grid overvoltage. There has been no theory suggested to explain this observed relation. However, these empirical data can be the basis for practical application and further investigation.

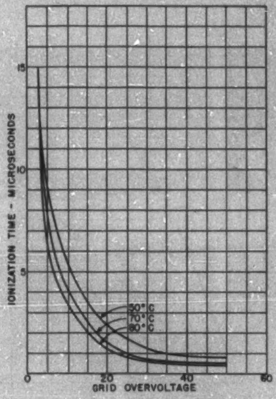

Figure 10. Effect of condensed-mercury temperature on ionization time characteristic of an FG-57 with 300 volts on anode

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Discussion

For discussion, see page 1079.

12-Kv Metal-Enclosed Bus and Switch Structure, Station C, Oakland, Pacific Gas and Electric Company

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MEMBER AIEE

H. E. STRANG
ASSOCIATE AIEE

THE East Bay metropolitan area, comprising the cities of Oakland, Berkeley, Alameda, Piedmont, and Richmond, is located on the east shore of San Francisco Bay. The population of these communities exceeds 500,000.

These cities are supplied with electric power from a high-voltage network consisting of 110-kv lines and substations located along the foothills in the easterly part of the cities; these high-voltage lines are fed from the hydroelectric transmission system; the substations connected to the high-voltage lines reduce the voltage to 12 kv, and these lower-voltage lines radiate to the several substations in these cities and, in turn, are connected to the steam-electric generating plant, station *C*, located on the Oakland water front.

The original steam-electric generating plant was built on the station *C* site during the late 90's; belt-driven, arc-light machines being the original installation. As the load in this area grew, the generating-station capacity was increased by means of engine-driven generators; these were replaced in due time with a 9,000- and a 12,000-kw vertical turbine generator. In 1922, a 12,000-kw horizontal turbine generator unit was added, and in 1929 further load growth necessitated the replacement of the 9,000-kw turbine, a 42,500-kva unit being installed in its place.

When the first turbines were installed about 1910, a new bus structure was located in the original building in which the early steam-driven units were installed, the steam units being located in a new structure adjoining the old. As the station was gradually increased in capacity, the bus structure was added to and some of the older oil circuit breakers were modernized to meet the increased rupturing duty.

By 1937 the load in this area reached a peak of approximately 140,000 kw. The steam-electric generating station had a maximum capacity of approximately 65,000 kva, and it was again necessary to increase the generating capacity of this station by the addition of a 50,000-kva steam-generating unit. The first steam turbines operated at 175 pounds steam pressure while the last unit put in in 1929 was a 425-pound unit. It was found that some simplification could be made in the boiler room and the efficiency of the plant increased by the addition of a 4,000-kva topping unit which would take steam at 425 pounds and discharge same at approximately 200 pounds into the older units. These additions would bring the steam-electric generating capacity up to approximately 120,000 kva.

As this plant not only supplies the surrounding communities but also acts as a steam standby to the general transmission system, it was necessary to make a connection of ample capacity between the high-voltage system and the steam station.

It was, therefore, decided to install a connection between station *X* (one of the high-voltage stations on the transmission system) and station *C* by means of a 110-kv underground oil-filled cable having a continuous capacity of 60,000 kva. The length of this cable run is approximately $3^1/_4$ miles. A 60,000-kva 110/12-kv transformer bank equipped with load-ratio control was to be installed at station *C* for the purpose of connecting the high- and low-voltage systems.

The addition in steam-turbine generating capacity and the connecting high-voltage cable greatly increased the interrupting duty on the 12-kv system at station *C*. The existing 12-kv switch equipment previously installed was found to be totally inadequate both electrically and mechanically for the new additions.

The arrangement of the old bus structure, the capacity of circuit breakers installed therein, the number of feeder positions available, together with the fact that there was no room for reactors on the feeders, made it impossible to use the existing structure. To have attempted to rebuild this structure in the existing space would have been most costly, to say nothing of the hazards involved in reconstruction.

After careful consideration it was found that the best and most economical plan would be to build a new switch house on property owned by the company, across a street from the main generating station. Circuit breakers and busses of the proper size could be installed without interfering with routine operating schedules; ample room could be provided for future growth; and most important, a serious congestion of existing duct lines and manholes adjacent to the present plant could be avoided.

Switchgear Design Objectives

After careful study the following specifications were established regarding the general type of switchgear and bus construction:

1. Operating conditions required a double bus with two oil circuit breakers for each feeder, transformer, and generator position.

2. The feeder oil circuit breakers should have an interrupting capacity of 1,000,000 kva; the generators, high-voltage transformer bank, and tie circuit breakers an interrupting capacity of 1,500,000 kva all at 12 kv. All oil breakers should contain as small a volume of oil as possible.

3. Each bus of the double bus should be in a separate fireproof room and each bus should be divided into two sections, these sections likewise to be isolated from each other by fireproof walls.

4. All oil circuit breakers should be isolated from the bus and the common feeder connection by gang-operated disconnecting switches, which should be mechanically interlocked with the oil circuit breaker so that they could not be opened when the oil circuit breaker is in the closed position.

5. The bus should be designed so as to be protected against short circuit by differential protection.

6. Current-limiting reactors should be installed on all feeders and generators.

7. The switchgear supporting and enclosing structure should be constructed of metal, using nonmagnetic metals where necessary to reduce cell heating due to eddy currents in the walls and barriers. Due attention should be given to design to prevent heating and damage to adjacent

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Paper 39-127, recommended by the AIEE committee on protective devices, and presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939. Manuscript submitted April 12, 1939; made available for preprinting May 31, 1939; released for final publication September 18, 1939.

F. S. BENSON is assistant engineer in the electric distribution department of the Pacific Gas and Electric Company, San Francisco, Calif.; H. E. STRANG is managing engineer of the large oil circuit breaker division of the General Electric Company, Philadelphia, Pa.

Credit is due S. J. Lisberger, chief of division of electric distribution, Pacific Gas and Electric Company, for his vision in the design of this new type of switching station and his courage in carrying it to successful completion. His assistance in the preparation of this paper is gratefully acknowledged.

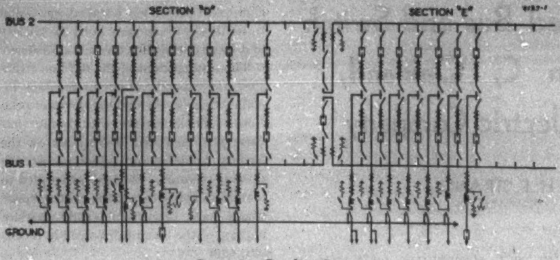

Figure 1. One-line diagram

parts of the structure in the event of short circuit. The entire structure should be well grounded.

8. The assembled switchgear at the factory should be given both a dielectric test and a heat run. The dielectric test should be in accordance with the standards of the AIEE. The heat run should consist of full-load current circulated through the main bus; the temperature of the bus bars and connections should not exceed a 35-degree centigrade rise above the air temperature in the cells. The temperature of the air in the cells shall not exceed a rise of 25 degrees centigrade above a room ambient temperature of 25 degrees centigrade.

In addition to the above main features there were many minor details such as position indicators, which should show open or closed position of the oil circuit breakers in order that the operator would not attempt to open a disconnecting switch without a visual marker indicating that the oil circuit breaker is open, even though a mechanical interlock prevents such operation; the insulated supports for the bus bar and all feeder taps should be designed to withstand a mechanical force produced by a short circuit of 70,000 amperes. The bus structure, switchgear, and all related 12-kv equipment should be designed for an insulating level equal to 23-kv-class equipment.

Suitable precautions should be taken to segregate the individual phases by suitable barriers and bushings to limit faults to single phase to ground, and to stop the migration of gas from one compartment to another.

A 23-kv test bus should be installed for the testing of feeder cables.

A ventilating system should be installed to provide clean, cool air to the switch house and also to remove quickly smoke and gases from the building in case of fire. Remote-controlled motor-operated dampers should be placed in the outlets of the forced-draft air ducts in each of the oil-circuit-breaker rooms to allow the operator to shut off the supply of fresh air if a fire occurs.

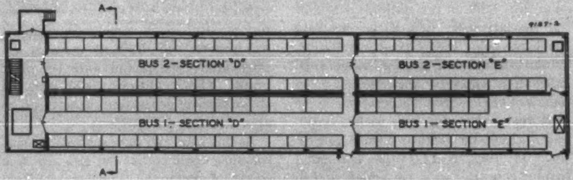

Figure 2. Main-floor plan—bus cells

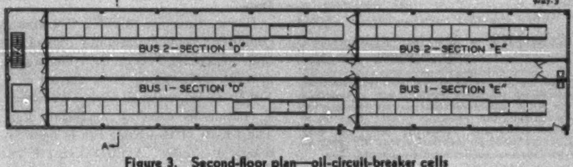

Figure 3. Second-floor plan—oil-circuit-breaker cells

After careful study it was decided that this new switch structure should be of radically different design than the older structures and the following features were decided upon:

No control conduits would be encased in the building structure, but would rather be run exposed; all of the heavy leads such as generator cables, outgoing feeder cables, etc., would be encased in duct structures which would be built in place after the building was finished. These features required a radical change in design, careful consideration of where the conduits were to run, and meant planning of conduit alleyways and runways. It also meant that the bus structure would have to be designed so that the control conduits from switches, instrument transformers, etc., would readily match the spaces provided for the conduit runs.

Accomplishment of Objectives

A. Switch-House Building

The new switch-house building was constructed with reinforced concrete walls, floors, and roof, which were supported by a structural steel frame. It is a two-story building and has a deep basement. Stairways are installed at the end of the building. Hatches are located in the first and second floors for hoisting switchgear compartments to the second floor or lowering generator reactors into the basements.

There are no windows or skylights in the building; their absence greatly reduces the amount of dirt and dust entering the station and also prevents leaks during inclement weather.

All illumination is artificial; ventilation of the building is obtained by means of an induced and forced draft fan and duct system. The air entering the building is cleaned by automatic self-cleaning air filters.

The first floor is divided into four large rooms, one for each bus and bus section. The metal-enclosed bus cells, feeder reactors, feeder-cable cells, and test-bus compartments are installed on this floor. See figure 2 for plan of cell arrangement.

The second floor is also divided into four large rooms, two of which are separated from the other two rooms by a long room. Control and similar conduits leading from the bus structure located on the floor below are brought into this room and racked in proper order and from there are run to the control house. Oil circuit breakers and their enclosing cells for each section are installed in each large room on this floor. See figure 3.

The ceiling of the second floor (above the oil circuit breakers) is built of sheet-steel shapes, which act as a protection for conduits in a conduit loft which is located between the ceiling of the second floor and the roof slab. See figure 4.

One side of the basement is walled off to provide a cable way. All the outgoing and incoming feeder cables enter the feeder cells from this subway.

The building was erected and completely finished by the contractors before any electrical installation was begun. No lighting nor control conduits were cast in the station walls or floor. All conduit work was run exposed and supported on the walls or ceilings. Thin-wall steel conduits with threadless fittings were installed for the control system.

Grooves, 12 inches wide and 2 inches deep, were cast in the first- and second-floor concrete slabs for receiving the steel channels forming the supports of the bus structure and oil-circuit-breaker cells. The channels furnished by the manufacturer of the cells were accurately leveled and then grouted in place, thus securely anchoring the channels in their proper position, also preventing any possible uplift of cells caused by the operation of the oil circuit breakers.

Figure 5. Switch house and yard

Holes were left in the first and second floors to allow for electrical connections between the oil circuit breakers and bus cells. Porcelain bushings were grouted in these holes providing the insulation required. Holes for bushings were also located in the partition wall on the first floor between the rooms for housing bus number 1 and bus number 2 and the reactor cell room. All holes were so accurately located that no trouble was encountered in lining up the cells on each floor and those on opposite sides of the wall.

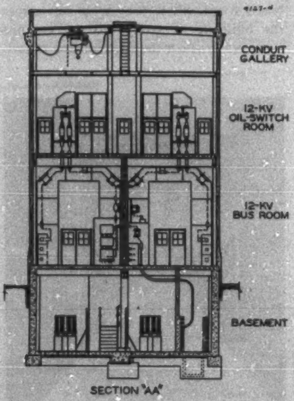

Figure 4. Typical cross section of switch house

Rectangular openings were also left in the floors and the partition walls for a variety of purposes, such as for the installation of lighting conduits, control conduits, communication ducts, and ventilating ducts, etc.

The company had undertaken no major construction of this character since 1929, at which time it was common practice to install 12-kv circuit breakers and bus equipment in concrete cell structures. In all these older designs, the control conduits had been encased in the building walls, floors, and the structure. In such installations the mass of control conduits was a real problem; they often interfered with the reinforcing steel in the building structure, were difficult to place, often requiring many bends which interfered with the pulling of the control wires; furthermore, they materially slowed up the construction of the building and often these conduits would be located in the wrong place.

Some difficulties had been experienced with heating of these conduits owing to their position with relation to the busses and feeder leads, and at times of short circuits, these conduits invariably picked up the ground current, often burning off and destroying the control system, then traversing the reinforcing steel, breaking out pieces of concrete from building walls far removed from the point of short circuit.

This new building design enabled the contractor to construct it at minimum cost and with minimum delay; there was no interference caused by other workmen attempting to install ducts and conduits in the floors and walls while building construction was in progress.

When the building was completed, the assembly of the bus structure and oil circuit breaker compartments began and was carried through to completion without delay, since there was no interference with other construction.

B. Switchgear Installed

The details of the metal-enclosed bus and oil circuit breaker, reactor, and test bus were designed by the manufacturer, and the structure was built to the specifications of the company's own engineering department.

The 12-kv bus and switch structure was built with two bus sections. Two generators, the 60,000-kva transformer bank, an auxiliary bus tie, and seven feeders were installed in section *D* and one generator, five feeders, were installed in section *E*. Spare feeder positions were provided in each section. The bus extension, when necessary, will be made on the end of section *E*.

The feeder oil circuit breakers are bottom connected, low-oil-content type, mounted in steel cells. Complete phase segregation is accomplished by grounded metal barriers between phase compartments. These oil circuit breakers are mounted on the floor above the bus structure. For ease in handling, the bus compartments were manufactured in units corresponding to each feeder, generator, or transformer. These were assembled at the factory in proper relation to each other and the disconnecting-switch compartments, and all copper permanently

Figure 6. Oil-circuit-breaker room

mounted in place. See figures 10 and 11. The main bus has a continuous rating of 3,700 amperes at 60 cycles. Each bus is made up of double copper channel sections mounted face to face. A special type of support clamp was developed to anchor this assembly to one of the porcelain insulators in alternate cubicles with flexible connections between each second unit. Joints between the balance of the cells were made with silver line-contact splice plates. Nonmagnetic steel was used where necessary in order to prevent undue heating, and the whole assembly given a full-current heat run. The bus insulators and structure were designed to withstand mechanical forces to be encountered up to the rating of the oil circuit breakers.

Figure 7. Conduit loft above oil-circuit-breaker room

The disconnecting switches mounted in steel compartments above each of the busses are gang operated, the six switches on both sides of a breaker being operated from one gear-type mechanism. They are mechanically interlocked to assure:

(*a*). Circuit breaker in open position before operating switches. This is accomplished by a positive interference block which prevents rotation of crank shaft, either opening or closing, except when breaker is in open position.

(*b*). Full operation of disconnecting switch before breaker can be operated either electrically or manually. Since the breakers and switches are on different floors, it is considered important to guard against partial operation of the switches. This is done by breaking the trip-free latch on the breaker mechanism for all disconnect positions other than fully open or fully closed.

The feeder reactors consist of three single-phase units installed in nonmagnetic housings.

To provide ventilation, the cells are built with louvers, backed with a fine wire mesh.

C. LIGHTING

The lighting for the oil-circuit-breaker cells on the second floor presents no difficulty. Prismatic lighting fixtures having an intensive characteristic were installed.

The lighting for the bus structure on the first floor proved much more difficult. The gang-operated disconnecting switches were installed at the top of the structure just below the steel crossover in such a position that an attendant on the floor can readily see the disconnecting-switch blades. The operation indicators showing the open and closed position of the coil circuit breaker are installed on the lower side of the crossover cells. To give proper illumination to these devices and prevent the light from blinding the operator required several trial installations.

A fixture used for kitchen illumination was obtained. The light was directed up against the top of the structure. The bottom of the fixture is made of a translucent material allowing some light to reach the floor; this source is not bright enough to blind the attendant while looking upward to see the operation indicators. These fixtures were suspended approximately eight feet below the bottom of the crossover cells. With the fixtures at that elevation, the illumination on the blades of the disconnecting switches was such that they could be plainly seen through the glass window.

One feature to be guarded against in future installations developed after the installation. The steel cells are wide and high, and when painted black both inside and outside, it is difficult to get enough light in the cells, even when using two 150-watt lamps supplied with portable cords, the black of the cell finish absorbing the light. To overcome this difficulty, it is planned, in future installations, to paint the cells a light color such as cream.

D. CONDUITS

The steel compartments were constructed with conduit boxes located at accessible places on the top of the cells.

Thin-wall conduits were installed through the openings in the walls, ceilings, and floors from the bus and switch cells to the conduit loft and conduit room. Here they were racked in orderly tiers and run exposed to a point in the switch-house building opposite the switchboard room, where they were run across the building in groups to the conduit room underneath the switchboard room.

Figure 8. Conduit room between oil-circuit breaker rooms, second floor

Figure 9. Bus room, first floor

Figure 10. Factory assembly of bus housings, showing spare positions

Figure 11. Factory assembly of bus housings

Such a conduit installation, using threadless fittings, enabled the construction department to keep all conduits out of the floors and walls of the building. The areas left around the conduits when installed in wall and floor openings were grouted in with cement, thereby preventing the migration of gas from one room to another.

E. GROUNDING

The metal structure made grounding relatively simple. Copper conductors having a cross-sectional area of 500,000 circular mils are connected to each end of the steel supporting channels for the bus structure. These cables were then connected to the general copper grid comprising the station grounding system. All equipment in the station is grounded by conductors installed as a loop connection. This reduces the resistance to ground and also provides a parallel path for the ground current to flow in event of trouble.

Conclusions

This type of structure and its installation at station *C* showed advantages which are summarized as follows:

1. With a complete factory design and fabrication the customer is relieved of the burden of preparing many detail drawings such as detailing of the bus bars, their enclosing cells, selecting of proper shape of current transformers to fit the structure, and the preparation of a mass of detailed instructions for type of connectors to be used, etc. It is also of material advantage to the field crews to have everything delivered complete and plainly marked as it saves much time in obtaining various fittings and devices from a large group of manufacturers. With the specifications requiring the furnishing of the entire structure complete in all details, the field erection time is greatly reduced.

2. The building can be completely erected with a minimum of delay, and therefore reduction in cost, because no conduits, miscellaneous equipment, etc., had to be installed in the building structure. This simplified the problem of the building contractor, completely eliminated errors in location of conduits, and reduced the number of spare conduits that must be installed since all of the conduits are run exposed.

3. The equipment is entirely self-supporting; the cells are all built in units. If any repairs have to be made, it does not affect the adjacent structures.

4. The cells and all related equipment being furnished by one manufacturer can be set up in the factory, given heat-run and voltage tests; any errors can be detected before the equipment leaves the factory. All parts of the structure can be completely marked so that ready assembly can be made in the field. By proper scheduling of shipments the apparatus can be set in place about as fast as it arrives, thus saving considerable time in the storing of equipment.

5. The metal-enclosed bus and circuit-breaker structure is entirely fireproof; the only inflammable materials are the insulation around the current transformers, the compound in the potential transformers, the limited quantity of oil in the type *H* oil circuit breakers, and short lengths of single-conductor varnished-cambric-insulated flameproof braided cable.

6. This type of metal cell structure is practically rodent proof, as there are no cracks or gaps of any size between the doors, cells, etc. With proper precaution taken for isolation between phases, ionized gases generated at the time of short circuits can be practically confined in a small area. The structure is much lighter than the similar type of concrete structure that would be required. It also occupies less space in the building. It does not have to be tied to the building structure as was the case with concrete cells. Where provision must be made for structures to stand earthquake stresses such as in the San Francisco Bay area, the reduction in the total weight is a decided advantage.

7. The cells present a very neat appearance. All live parts are totally enclosed. The structure is solidly grounded. It is believed that this is the safest type of switchgear that can be used, and it offers the minimum hazards to the operating and maintenance personnel.

8. Purchase of the complete structure from one manufacturer not only facilitates construction but reduces by astounding quantities the number of requisitions, bills, and similar bookkeeping details in handling the hundreds of parts that must be obtained and stored for the installation of this type of equipment if it were constructed in the manner previously used, namely, building concrete, buying each individual group of parts from separate manufacturers.

This equipment has been in service only about six months [up to June 1939]. No troubles have developed since it was put into operation; other than the painting of the cells we would make no changes in design if the job had to be done over. Field erection and handling met all expectations and the over-all cost of the installation is less than similar types previously used.

Discussion

For discussion see page 1080.

Electrical Equipment of the Union Pacific Steam-Electric Locomotive

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THE development of the steam-electric locomotive for the Union Pacific Railroad represents the culmination of more than two years of co-operative design and research by scientists, engineers, and officials of the railroad and of the various manufacturers of equipment for this outstanding example of modern motive power. It is the intention of the authors to deal primarily with the electrical details but a comprehensive picture of the general features of this recent development is included.

A number of departures were made from conventional designs in order fully to utilize the possibilities of this new type of motive power. New methods of construction were devised permitting material reductions in weight and space requirements.

New generator ventilating-fan designs were required for satisfactory noise levels at tip speeds much higher than usual. Armature-excited exciters were utilized to assist in maintaining uniform response of power and electrical braking under a variety of operating conditions, particularly in multiple unit.

The development of an electrical control system capable of insuring and controlling maximum output over the full range of steaming conditions also presented a number of interesting and difficult problems.

The braking resistor is particularly noteworthy from the standpoint of methods of construction, the energy dissipation rates per unit surface, and the method of forcing cooling water through the tubes.

Although multiple-unit operation of electric locomotives is commonplace and there are well-established design practices used in such applications, yet new problems were presented in designing multiple-unit control of steam-electric units. A complete system of electric control was devised which permits, for the first time, full multiple-unit operation of two steam locomotives from the operating cab of either locomotive.

The essential parts of each of the Union Pacific locomotive units are:

1. A high-pressure oil-burning steam boiler
2. An auxiliary set, turbine-driven, for operating pumps, blowers, and auxiliaries
3. A complete automatic control for supplying adequate steam as demanded
4. A main geared turbine generating set for traction power (direct current)
5. An a-c generator on same shaft for train air conditioning, traction-motor blowers, and other accessories
6. Air-cooled condensers for returning used steam to boiler as water
7. Six axle-hung traction motors geared to main drivers
8. A main control system for automatic and manual control of motor combinations during acceleration
9. An electric braking control system automatically or manually blended with the air brakes when the motors act as generators during braking
10. Electric couplers and accessories which permit operating the locomotive units singly or in multiple unit during acceleration and braking
11. Three separate train-control signal systems to conform to the different lines over which the locomotive will operate

The 5,000-horsepower locomotive is made up in two cabs, each powered with an oil-burning water-tube boiler of the forced circulation type, supplying superheated steam at 1,500 pounds and 920 degrees Fahrenheit. On level track a 1,000-ton train can be hauled at 90 miles per hour, and at lower speeds on the 2.2 per cent grades without a helper. In emergency, it may safely operate at speeds as high as 125 miles per hour. The steam from the boiler drives a turbine unit consisting of high- and low-pressure rotors driving a single helical gear. Direct connected to the driven shaft is the two-armature d-c generator, a 60-cycle auxiliary generator, and an exciter of the metadyne type.

A main handle and a selector handle on the master controller in the operating cab permits the engineer to control the supply of power to the six traction motors on the driving axles and to connect these motors in three combinations, series, series-parallel, and parallel. A braking handle on this same controller in conjunction with certain auxiliary devices, permits the engineer to convert the motors to generators for electric braking and to blend the electric braking with the air brakes automatically and manually for a wide variety of operating conditions. A novel design of braking resistor converts the electric braking energy into heat. This heat is transferred to cooling water, circulated through the stainless steel tubes of the resistor, and dissipated in the main air-cooled condenser. Especially noteworthy is the provision for multiple-unit operation of the boiler, the main power plants, and the motors on both units from the operating position on either unit. With the variation of demand from the turbine generating set, the steam equipment automatically responds with greater or less steaming by means of a complete automatic control.

Steam-Plant Control

When making a cold start a propane-fired auxiliary boiler is used for low pressure steam for heating and atomizing the "bunker *C*" fuel oil. The turbine-driven auxiliary set is next started and is driven by its battery-operated starting motor in order to provide feed water, combustion air, and fuel oil to the Babcock and Wilcox main boiler. With the auxiliary set in operation and atomizing steam available, each of the two burners in the main boiler is lighted by manually closing the ignition switch which lights a propane torch by sparking ignitors.

After a short delay fuel oil is admitted automatically to the burners and upon establishment of the oil flame, indicating flame failure photocells act to cut off the propane torch and ignitors. If the flame is not established within a definite time or subsequently fails, the lighting cycle is automatically repeated. This igniting cycle will be repeated five times if the oil does not ignite after which it locks out and must be reset by hand.

When the main boiler pressure rises to approximately 500 pounds, steam is admitted manually to the turbine of the auxiliary set. When the boiler pressure reaches 800 pounds the starting motor is automatically disconnected from the battery and operates as a d-c generator supplying all d-c driven pumps. Subsequently, when the battery-charging motor generator set is started the d-c pump motors are automatically transferred to this set and the starting motor of the

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auxiliary turbine set is electrically isolated.

The fuel and air supply to the burners and water supply to the boiler are regulated by the Bailey Meter steam-plant control when starting and when operating normally. The fuel and water pumps and the draft fan are built integral with the auxiliary set. The speed of this set is varied in response to changes in steam flow and water level providing an easy method of changing fuel, air, and water flow simultaneously in approximately the correct proportions.

The fuel pump and draft fan are designed to supply fuel and air slightly in excess of that required for a given water flow. Accurate adjustment of the air flow is made by an automatic damper under the control of a boiler pressure controller, and accurate adjustment of the fuel-oil flow is maintained by a by-pass fuel valve under the control of an oil-air flow ratio controller. The complete steam plant control, therefore, regulates fuel, air, and water to the boiler automatically in response to power demand, maintains water level, and insures correct combustion. The condenser inlet louvres are controlled by a thermal device actuated by condensate temperature.

In emergency, the automatic features may be cut out and the various functions controlled manually.

Main Traction Generator

The main traction generator unit consists of a pair of complete generators built in a single frame with armatures back to back. Because of the demand for a large amount of 60-cycle power to be used on certain of the trains hauled by this locomotive and because of the relatively large savings in cost and weight of the main gear reduction unit by using the highest possible speed on the driven shaft, 1,200 rpm was chosen as the operating speed of the generator set. While this was an abnormally high speed for which to design a d-c machine of the capacity desired, the advantages seemed sufficient to compensate for the mechanical design difficulties involved.

The high speed of the generator together with the severe weight and space restrictions made it necessary to depart somewhat from the usual mechanical construction. To provide rigidity with minimum weight, the tandem armatures are built on a single hollow steel cylinder with stub shaft extensions at each end. In order to avoid excessive commutator surface speed, it was necessary to reduce their diameter to a point which precluded passing the armature ventilating air under the commutators. Ventilation is through the risers which are of the involute type to serve also as equalizers.

The armature ventilating fans which are mounted in the central space between the two armatures, figure 2, are of the laid back aphonic type. By use of this specially developed blade shape and other acoustic considerations it was possible to design the compact self-ventilated generator for a noise level considerably below that which would have been obtained with conventional fan design at this tip speed. The exhaust ventilating air is collected in a scroll and is normally discharged through the cab roof, although the discharge may be regulated by a damper for cab temperature control.

The generator frame, split longitudinally for ease of assembly, is of skeleton construction and is illustrated in figure 3. The armature bearings are sleeve type, and the lubricating oil is circulated by pumps through coolers and filters before returning to the bearings.

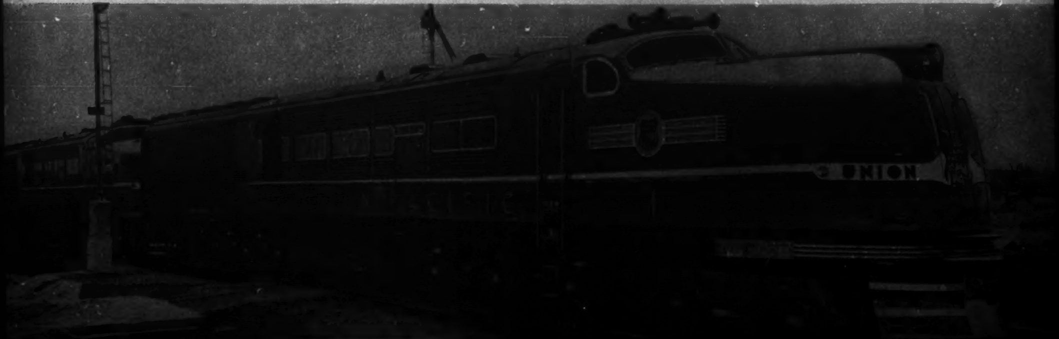
These two component generators built into one frame are normally connected in series with their mid-point grounded, thus limiting the insulation stress in all the transmission equipment to not over half of the total voltage generated. The traction generator unit is separately excited by a metadyne exciter which, through a speed sensitive control device and depending on the turbine power available, imparts to it output characteristics as shown in figure 4. The electrical proportions of the machine are such that it will rate full horsepower continuously at any point shown on these characteristic curves, from 3,200 amperes up to 1,340 volts. The nominal rating of the generator unit is 2,500-horsepower input at 1,200 rpm. The wide range of current and voltage over which the machine will rate continuously is typical of generators used on internal-power locomotives. By multiplying maximum continuous rated voltage by maximum continuous rated current, one obtains a much better measure of the service capacity of the generator than by merely stating the rated horsepower input.

Auxiliary A-C Generator and Metadyne Exciter

The auxiliary a-c generator and metadyne exciter are built into a compact two-unit machine having a centrally located ventilating fan and a single self-aligning antifriction bearing, the driven end of the set being supported on a steel-disk type of flexible coupling through which the auxiliary unit is attached to a shaft extension of the main traction unit.

The a-c machine is of the salient-pole rotor design with distributed stator windings. The continuous rating is 480 kva, 230 volts, three phase, 60 cycles. It is of conventional design except for the use of heatproof insulation and aluminum frame to reduce size and weight. The primary purpose of the alternator is to supply lighting, heating, cooking, and air-conditioning power to streamlined passenger trains. It is used on the locomotive to supply two traction-motor blowers, the speed-control governor, the main air compressor, the 130-volt and

Figure 1. 5,000-horsepower double-unit streamlined steam-electric locomotive for Union Pacific Railroad



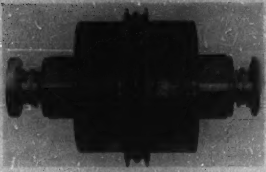


Figure 2. Armature of traction generator

64-volt motor generator sets, and through small transformers, the 12-volt headlights and part of the 115-volt lights. A dynamic type regulator is provided to hold the three-phase lines at a constant voltage over a wide range of load. Excitation of the alternator field is from the 130-volt d-c system.

In common with all metadyne-type machines, this exciter is characterized by the fact that it is armature excited. One of its most valuable properties lies in its inherent characteristic of delivering a definite current output regardless of load circuit resistance, the value of the current being approximately proportional to the current in one of its low-wattage control fields.

This exciter has two distinct functions. During the normal operation of the locomotive as a propulsion unit, it is used to excite the field of the main traction generator. However, when it is desired to use the locomotive to assist in decelerating the train or in holding it on long mountain grades, the exciter output connections are transferred from the generator fields to the traction-motor fields. It is here (as well as for multiple-unit propulsion operation) that the inherent constant-current characteristics of the metadyne are of special value, for while the resistance of the traction-motor fields may conceivably change by 50 per cent or more due to variations in temperature, the braking characteristics remain essentially unaltered.

Traction Motors

A locomotive with a weight of 354,000 pounds on its six driving axles and an operating range extending from a maximum speed of 125 miles per hour at one extreme to a maximum tractive effort of nearly 90,000 pounds at the other, presents a broad problem in traction-motor design. As usual in traction motors, much of the design was dictated by the necessity of mounting the motor between the wheels on standard track gauge. Minimum weight was of paramount importance. The motors, figure 5, are axle mounted, thus permitting the use of small wheels (44 inches). This gives the lowest possible cab floor height and the maximum room for apparatus in the cab.

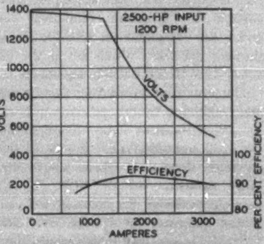


Figure 4. Characteristics of 2,500-horsepower traction generator with speed control

The characteristics of the traction motor on constant voltage are shown in figure 6. The motor has eight poles with multiple-wound fully equalized armature. Motor brushholders are supported on a movable brush yoke which can be rotated for inspection by means of a pinion engaged in a gear mounted on the yoke. Each brushholder can be brought in turn opposite the bottom inspection opening.

In service the motors operate on a constant-horsepower variable-voltage generator characteristic, and the six motors per cab will transmit 2,500-horsepower turbine output at any speed up to 125 miles per hour. Each motor is normally blown with 2,000 cubic feet of air per minute. The normal continuous rating is 32,000 pounds tractive effort per locomotive cab. Maximum grades of the order of two per cent, of considerable length, are encountered in transcontinental service. Such grades impose a heating problem in the electrical traction equipment, which is relatively difficult as compared with that encountered in operation on level track. To permit the the handling of heavy trains in transcontinental service without the use of helper locomotives on the heavy grades, high-speed operation of the traction blowers is provided, giving 4,000 cubic feet of ventilating air per minute per motor. The rating obtained with this ventilation is 40,500 pounds tractive effort per locomotive cab.

The traction-motor armature bearings are of the grease-lubricated cylindrical-

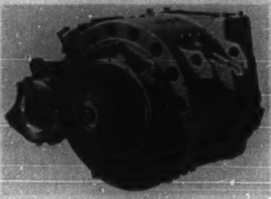


Figure 5. Traction motor

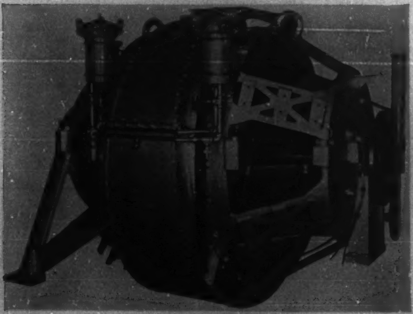


Figure 3. 2,500-horsepower traction generator

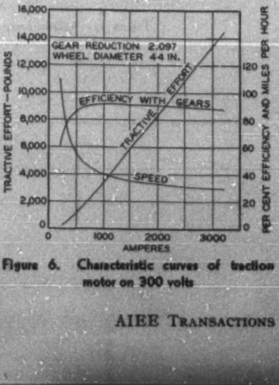
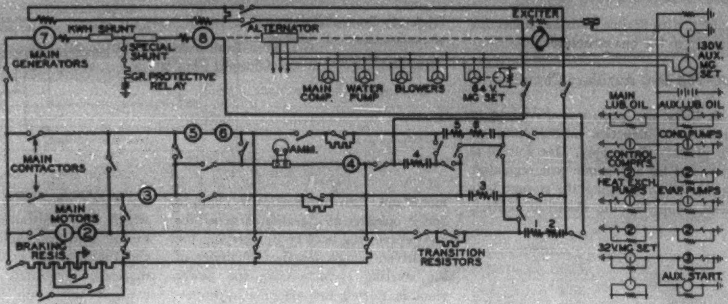


Figure 6. Characteristic curves of traction motor on 300 volts

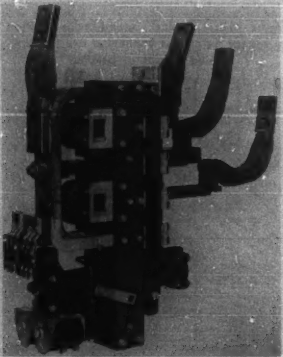
Figure 7. Schematic diagram of main and auxiliary power circuits



roller type. The suspension or axle bearings are split sleeves lubricated by wool waste wicks. The wicks are held in contact with the axles by a spring mechanism, and oil is supplied from a constant-level reservoir.

The high speed and heavy loading presents a difficult gear problem. In order to eliminate mounting distortion, the gear rims are bolted with body bound bolts to the gear centers which are finish machined after mounting on the axles. The stiff tacky gear lubricant generally used on axle-mounted traction motors was considered unsuitable for the high-speed operation, so provision was made to use oil with "extreme pressure" properties. In addition to providing a reservoir for the lubricant the gear case is constructed in such a way as to lubricate the gear mesh by splash at low speed and by jet action at high speed.

Figure 8. Electropneumatic reverser with flexible leads for connecting direct to bus structure



Compressors

On each unit of the locomotive there is a two-stage aluminum-frame air compressor having a piston displacement of 186 cubic feet per minute which furnishes air for braking and for control apparatus. The compressor is driven by a three-phase 60-cycle 220-volt induction motor, through herringbone gearing. The control is of the start and stop type. An unloader is installed in the discharge pipe between compressor and reservoir to reduce the starting-torque demand on the motor.

In addition to the main a-c motor-driven compressor, each locomotive unit has two d-c motor-driven compressors with a combined piston displacement of 36 cubic feet per minute. Their chief function is to charge the control system for the initial start of the locomotive before steam is generated. During regular operation, if the air demand is such as to cause the gauge pressure to fall below approximately 115 pounds per square inch, they also automatically assist in supplying air to the main reservoir.

Main Power Circuits

Figure 7 is a schematic diagram of the main power circuits. The double armature generator and a direct-connected metadyne exciter are driven by the main turbine through suitable gearing. The exciter supplies excitation for the generators when motoring, and for the traction motors during electric braking. A maximum generator voltage of 1,400 is used during motoring. The midpoint between the generators is grounded, so that no point in the main circuit is normally more than 700 volts from ground potential. The main circuits include a traction-motor ammeter shunt, a kilowatt-hour meter shunt, and a special shunt to energize the current-limit and backward-transfer relays as well as a differential field on the exciter to impart a differential characteristic to the shunt generators. Electropneumatically operated contactors, are used to connect the motors in three speed combinations of six, three, or two motors in series, with provision for two speed combinations of four and two in series with any pair of motors cut out. Transition resistors are used to prevent interruption of power to the motors during transfer of the motors from one combination to another. The motors are reversed by a unique design of reverser,

Figure 9. Master controller with cover removed

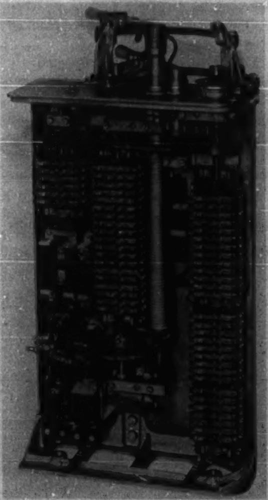


figure 8, with flexible leads for connecting directly to the bus structure.

130-Volt D-C Auxiliary Circuits

A 40-kw d-c generator driven by a three-phase induction motor furnishes the primary d-c supply. Its voltage is controlled by a dynamic-type regulator. It is used for charging the storage batteries, supplying the electrical control, boiler control, miscellaneous pumps, and other auxiliary circuits. It may also be used to supply power for the starting motor of the auxiliary turbine set when the induction motor is energized from an external source.

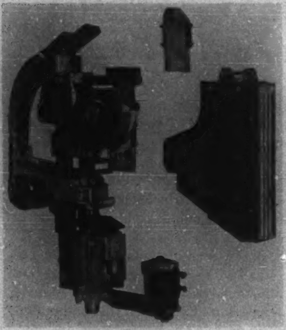


Figure 10. Electropneumatic contactor with arc chute removed

The starting motor drives the auxiliary turbine set, when starting the main power plant, in order to supply fuel oil, combustion air, and feed water to the boiler until sufficient steam is generated in the boiler to operate the auxiliary turbine. The electrical connections to the starting motor are then automatically changed and it operates as a regulated generator and relieves the battery by carrying the d-c driven pumps for the remainder of the starting cycle. After the power plant is started the machine is disconnected from the circuit.

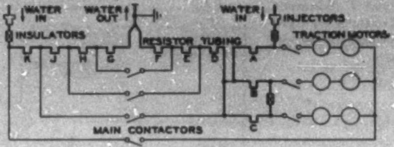


Figure 12. Schematic diagram of connections of braking resistor

64-Volt D-C Auxiliary Circuits

Direct current at this voltage is required for contemplated hauling of existing streamlined trains. It is supplied by a 7.5-kw generator driven by a three-phase induction motor. No voltage regulator is provided for this generator, but a three-position switch gives a choice of three battery-charging rates. Separate regulated excitation of the generator is taken from the 130-volt system. When pulling streamlined trains the set supplies electric control for the straight air brake system, control for the air-conditioning equipment, telephones, mail-room buzzer, emergency lights in the train, and charges the 64-volt battery in the baggage car. When pulling standard Pullman cars or freight, the set is used only for operation of the telephones on the locomotive.

Master Controller

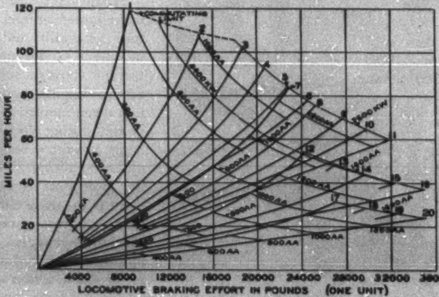
Figure 9 shows a front view of the master controller with the cover removed. This controller has three handles, mechanically interlocked to prevent improper operation, by which the main power circuits are controlled. The motoring handle operates a cam shaft and control fingers which vary resistance in series with the exciter field and establish the circuit for the magnet valves of the electropneumatically operated contactors which connect the main traction motors in various combinations required for accelerating and braking. There are 21 notches on the dial ring of this handle.

The brake handle drives its control cylinder through the long spring mounted concentric with the operating shaft. When the handle is moved into any one of the 20 notches on the dial ring the spring will exert a torque to turn the control cylinder through the gear and sector at the bottom of the controller to the position selected on the dial ring. The movement of the cylinder, however, is governed by the notching mechanism engaging the notched wheel on the end of the shaft. The notching mechanism is controlled by current and kilowatt relays connected in the motor circuits during the braking cycle.

The selector handle has seven positions; off, forward series, series-parallel, and parallel, and reverse series, series-parallel, and parallel. In the series positions the motors are connected six in series. In the series-parallel positions the motors are connected three in series and two groups in parallel while in the parallel positions the motors are connected two in series and three groups in parallel.

Figure 11. Water-cooled braking resistor

Figure 13. Typical speed-braking effort curves for six motors connected two in series, three groups in parallel



The dial rings have fluorescent-paint numerals and are illuminated at will by argon glow lamps mounted on the motoring and brake handles.

Main Contactors

Figure 10 shows one of the main electropneumatically operated contactors with arc chute removed. These contactors are mounted in dustproof compartments underneath the locomotive and are equipped with powerful magnetic blowouts for rupturing the main circuits. The flexible braided connections at the back of the contactors facilitate direct connection to the bus structure. The contactors are closed by air pressure against a powerful spring located in the operating cylinder. This spring insures a very rapid and positive opening of the contact tips when the air pressure is removed.

Braking Resistor

The braking resistor, a plan view of which is shown in figure 11, consists of approximately 350 feet of seamless drawn stainless-steel tubes which make up the resistor sections and through which cooling water is circulated. These tubes are connected at the ends by cast return bends of the same material and the complete unit is assembled on insulated supports in the form of an oblong spiral. Each tube is rigidly supported at the center and the ends are supported in such a way as to allow for contraction and expansion during electric braking. The tubing is arranged in two parallel hydraulic circuits with separate inlets and a common outlet which is rigidly grounded. The inlet pipes are insulated from ground by a special high-temperature moulded tubing.

Ahead of each insulator tube is mounted an injector, supplied by steam from the main boiler, and designed to force the cooling water through each hydraulic circuit at a rate of 12,500 pounds per hour under normal braking conditions with the water at an initial temperature of 230 degrees Fahrenheit. The resistor has capacity to dissipate 3,600 kw continuously but this maximum rating is used only when decelerating the train from maximum speeds. When braking at maximum capacity approximately 50 per cent of the cooling water is converted into steam and the mixture passes through a steam separator. The water is returned direct to the hot well while the steam is passed into the main condenser.

An electropneumatically operated valve controls the admission of water to the inlet end of the resistor and a similar valve of larger size is placed between the separator and the hot well to prevent bypassing the condenser when the resistor is not in use. An air-operated diaphragm type of valve controlled by a magnet valve governs the flow of steam into the injector. Protective relays prevent the establishment of the braking circuit through the resistor unless the tubes are full of water.

Figure 12 shows the connections of the tubes electrically and hydraulically. Sections *A*, *B*, and *C* are in series hydraulically, but in parallel electrically so as to serve as balancing resistors for each pair of motor armatures. Sections *D* to *K* are in series electrically, and carry the total current of all six motors connected two in series and three groups in parallel. Four values of resistance are available, the circuits being so arranged that each hydraulic half of the resistor is approximately equally loaded at all times. Each of these resistance values gives a group of braking notches as shown in figure 13, the notches in each group being obtained by controlling the motor field strength.

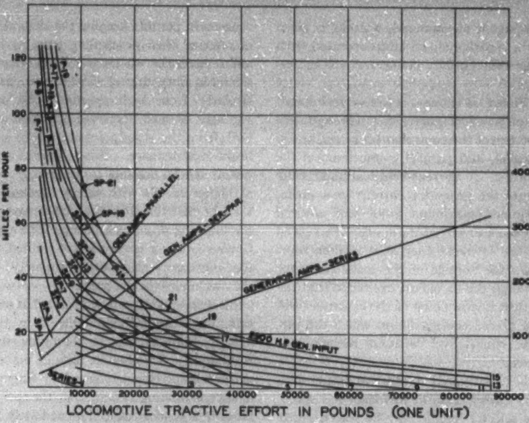
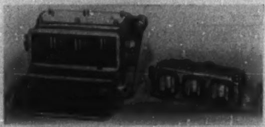


Figure 14. Typical speed-tractive effort curves for series-series-parallel and parallel connections of motors

Figure 15. Plug and socket of three-phase 450-ampere coupler



Motoring With Automatic Transition

When accelerating the train from rest the selector handle of the master controller is placed in the parallel position and the motoring handle is moved slowly through the first few notches to take slack and get the train in motion. The motoring handle may then be advanced as rapidly as desired up to the 21st position. The movement of the motoring handle cuts out sections of fixed resistance in series with the field of the metadyne exciter. However, the exciter field current is limited by the automatic load-regulating equipment which takes over control as soon as the motoring handle cuts out sufficient resistance to permit regulation at full output.

Transition of the motors from series to series-parallel and from series-parallel to parallel takes place automatically under the control of a voltage relay when the generator voltage reaches the proper predetermined value. The locomotive speeds at which transitions occur depend upon the position of the motoring handle, the transition points being higher for the higher notches.

If the maximum permissible generator current is approached as in ascending an increasing grade, a backward-transfer signal light flashes at the operator's position warning him to move the selector handle back to the next lower position. The backward transitions take place in response to the backward movement of the selector handle. If wheel slippage is encountered, warning is given by a separate signal lamp operated from four differentially connected voltage relays.

Figure 14 shows a set of typical speed-tractive effort curves for representative notches of the controller for series, series-parallel, and parallel connections of the motors. A current relay automatically limits the generator current to a predetermined maximum value and a speed governor driven at the synchronous speed of the 60-cycle alternator further regulates the voltage of the main generator through the metadyne exciter so that the output in any notch of the controller will not exceed the maximum which the turbine can deliver without reducing its speed.

If it is desired to start and operate a very heavy train at less than full speed, the selector handle may be placed either in the series or the series-parallel position, which prevents the motors transferring to the next higher speed combination. The main handle may be notched backward or forward at will to maintain a train speed best suited to the profile.

Motoring With Manual Control

If for any reason the engineer wishes to eliminate the automatic features, a switch may be thrown which by-passes the automatic devices and cuts out the automatic transitions. The locomotive then operates only in the connection called for by the selector handle, and the engineer must watch the generator speed indicator and motor ammeter to avoid overloading either the turbine or the generator.

With a light train, or on a downgrade, the engineer may start either in series, series-parallel, or parallel by placing the selector handle in the desired position before notching out the motoring handle. With heavy trains the start is made with the motors in the series connection and transitions are made manually by backing off the motoring handle a few notches before moving the selector handle to the next higher speed position. The backward transfer light, as in automatic operation, functions and acts as an additional warning if the maximum permissible generator current is approached.

Manual Electric Braking

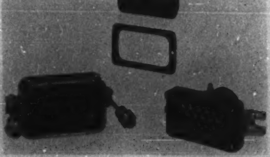
Manual control of the electric brake is provided to permit its use for slowing down the train and for controlling the speed and holding the train on grades without the use of air brakes. This arrangement permits keeping the air brakes in reserve when descending long grades and relieves the wheels and shoes of considerable duty during slow downs, particularly from high speeds. The advantages of such operation are apparent both from the standpoints of safety and brake maintenance. Where curves permit, it is also possible to descend grades at higher speeds with safety because of the fact that the air brakes are in reserve for stopping in an emergency. The continuous braking energy is not limited to the continuous dissipating capacity of the mechanical brake parts.

Electric braking can be initiated at any time the accelerating handle is in the "off" position by moving the brake control handle. This handle may be moved one notch at a time to control the amount of electric braking as desired by the operator or it may be advanced immediately to any notch. When advanced immediately, the notching takes place automatically and is controlled by a suitable current-limit and kilowatt-limit relay. If it is desired to descend a grade at approximately uniform speed the handle can be placed immediately in the correct notch for the desired speed. To maintain maximum electric braking as the train slows down, the handle is advanced immediately to the last notch. Under this condition the notching will be automatic and the braking will be under the control of the kilowatt and current-limit relay.

The settings of these relays during manual control of braking corresponds to the continuous braking ability of the motors because sufficiently long grades may be encountered to require operating the motors at this rating when using electric braking alone. The relay settings are lower than those used for maximum automatic electric braking where the train is slowed down by using air in combination with electric braking.

The progression of the braking-controller cam shaft either automatically or manually energizes the braking control circuits in proper sequence to change the braking-resistor connections and to control the field strength of the traction motors. This is accomplished by controlling the field of the metadyne exciter used for motor field excitation, in proper sequence to provide the desired graduations in braking effort.

Figure 16. Plug and socket of 24-point train-line coupler



Automatic Electric Braking

The locomotive is equipped with "automatic air-brake control equipment" for conventional steam trains and "high-speed straight air control equipment" for use on streamlined trains. Automatic control of the electric brake may be used in combination with both systems. When used, the full capacity of electric brake is available, assisting the air brakes in slowing down and stopping the train. The arrangement is such that air pressure to the driver brake cylinders is suppressed when the electric brake is functioning normally.

The control of the electric braking has been co-ordinated with the control of both air-brake systems to provide a braking effort on the drivers slightly less than that which would be obtained with the air brakes over the speed range involved. This adjustment is used in order to permit slightly heavier braking on the train than on the locomotive so that the locomotive will stretch the slack during a stop.

In order to make use of the electric brake in combination with the air, the operator places the manual braking controller handle in the first notch before he starts an air application. He is then free to make the complete stop without further attention to the electric brake, since the control of both the electric and air is by means of the regular air-brake control valve. Under such conditions the electric braking controller notches automatically under the control of the air, the maximum electric braking being limited by the kilowatt and current limit relays previously referred to. The air pressure is held off the driver brake cylinders by suitable suppression valves as pointed out above, but is admitted to the brake cylinders of guiding axles and the remainder of the train.

The setting of the kilowatt and current-limit relays is increased if the brake pressure in the pipe which supplies air to the brake cylinders is raised above 45 pounds per square inch. The setting of these relays is graduated in this manner up to 100 pounds per square inch. This permits the use of higher electric braking rates on streamlined trains where cylinder pressures as high as 100 pounds per square inch are used. This change in setting is effective in maintaining the electric braking on the drivers at a value only slightly less than that which would be obtained with the normal air brake over the range in pressure involved. The increase in kilowatt and current setting is permitted since the use of the air brakes in combination with the electric brakes means that the train must be slowing down even on the heaviest grades, and, therefore, that the motors can be subjected to these high braking rates for the relatively short periods involved.

Automatic blending of the air and electric brake, when both are used, is accomplished by means of suppression valves in combination with a pneumatic blending control incorporated in the air brake system. The arrangement is such that the air brakes can be suppressed only in the event the electric brake exceeds a predetermined minimum. Air brakes on the drivers are automatically applied at any time electric braking is lost. Air brakes are also gradually applied automatically as the electric braking fades out at very low speeds just preceding a stop.

A cutout button is provided which permits the operator to cut out the automatic feature of the electric brake and allows him to dispense with the automatic control of the electric brake from the air-brake control valve. This cutout button does not interfere with the use of the manual control of the electric brake in combination with the air brakes. By controlling the air and electric brakes separately, the amount of braking on the locomotive can be reduced to any desired value to permit control of slack during a stop.

When descending long grades it is possible to establish electric braking manually to hold some desired speed and apply the air brake for slow downs for curves without disturbing the electric brake handle. When the air brake is applied under this condition the electric brake will advance automatically to assist the air brakes in slowing down the train. It will return to the speed setting used prior to the air application, without attention from the operator when the air brakes are released.

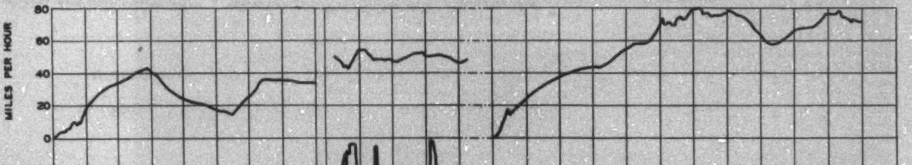


Figure 17. Graphical records of kilowatts, amperes, and speed. Main-line run

Every effort has been made to provide the greatest flexibility possible in the control of the electric brake, both separately and in combination with air, so that full advantage can be taken of this new type of braking equipment under all operating conditions.

Electrical Couplers

The streamlined trains of the Union Pacific system are equipped with three separate three-phase train lines throughout the train. The rear end of each unit of the locomotive is, therefore, equipped with three three-phase couplers, one of which is illustrated in figure 15. These are a new design of knife-blade type arranged especially for easy assembly and having a continuous rating of 450 amperes per phase. Threaded knobs assist in inserting or removing the jumpers, and act as a lock when the jumper is in place, or as a cover lock when the jumper is removed. One of these couplers is provided at the head end of each unit. If two units are operating in multiple, one train line is supplied from the alternator of the first unit, and the other two from the alternator of the second unit. This arrangement permits supplying three-phase power from one locomotive to the other for use when starting the plant, or for charging the batteries of both units with only one plant running. The alternators on the two units are never paralleled.

A new-design 24-point control coupler, figure 16 handles the train-line wires of the 130-volt control circuits, and the electrical air brake control, telephone, and special signal wires for streamlined trains. Two of these couplers are installed at each end and give a maximum of 48 control train lines in an extremely small space. Locking knobs similar to those on the three-phase couplers assist in inserting and removing the jumpers.

One two-point 100-ampere coupler is provided on the ends of each unit for the 64-volt battery bus line standard on Union Pacific streamliners. The baggage-car battery is charged from the 64-volt generator on the locomotive through this train line.

Multiple-Unit Operation

For the first time in the history of railroading two steam locomotives are completely controlled from the operating position in the cab of either locomotive. This multiple-unit control system, which is similar to that used on electric locomotives, is completely electric and makes use of conventional train lines connected between units through train-line jumpers. The train lines are energized in the proper sequence to operate the contactors which establish the various power-circuit combinations required for motoring, electric braking, and for controlling the metadyne exciter.

The loading of each power unit is independently maintained by individual turbine-speed control elements to insure maximum available total output when required. Motor circuit transitions are automatic and are controlled independently on each unit so that they take place at the proper speed to match the output.

When using electric braking, the multiple-unit circuits are energized in proper sequence by the notching of a single controller at the operating position and this notching is supervised by relays and protective equipment of each unit. This means that the kilowatt and current rating of the individual units cannot be exceeded and that the protective equipment of both is fully effective.

All visual indicators at the operator's position function normally when using multiple-unit control and respond to corresponding operating conditions of both units during acceleration and braking.

To obtain flexibility under unusual operating conditions cutout and change-over switches have been provided. By throwing a single switch either unit may be made inoperative and full control of the remaining unit from either operating position retained.

The motor cutout arrangement is of the type used quite generally on electric locomotives. The arrangement is sufficiently flexible so that any pair of motors or a single generator of either power unit may be cut out and still permit automatic control of the power plant and automatic transitions. This arrangement insures maximum available output in the event it is necessary to cut out motors.

With the multiple-unit control system the same flexibility is available regardless of whether the locomotive consists of a single power unit or two power units operating in multiple.

Train Control

The locomotives are equipped with three separate train-control systems to conform to the different lines over which they will operate. Power is obtained from a motor generator set driven from a 130-volt d-c motor and supplying 32 volts held constant by a voltage regulator.

A two-indication continuous cab signal system with two-speed control is provided for operation over the lines of the Chicago and North Western Railway. A two-indication continuous-cab-signal system with whistle and acknowledging feature is included for use on the lines of the Union Pacific Railroad. An intermittent magnetic inductive automatic stop system is provided for use on the Southern Pacific.

Discussion

For discussion, see page 1082.

Some Practical Measuring Devices

LLOYD F. HUNT
FELLOW AIEE

IN the operation of a large utility very often it is desirable to make practical measurements which are not generally obtainable from the usual manufactured devices. It therefore becomes necessary to make some unusual devices to record information that will assist in the operation of a large system. This discussion will present some devices that have proved very useful for years, and some more recent ones that are being tried out.

Travel Recorder

The Southern California Edison Company, Ltd., has made 116 short-circuit tests on their 220-kv system for determining the operation characteristics of various types of oil circuit breakers. The normal evolution in making these tests brought out the fact that one of the most important facts was not shown on the oscillograms. This was the actual position of the crosshead during any position of the oil-switch stroke. In order to make an accurate record on the oscillograph of this stroke, a simple recording transmitting device was made to use in conjunction with the oscillograph.

Figure 1. Oscillogram showing travel-recorder record

Figure 1 shows an oscillogram of an oil-switch test. The travel record is shown by the stair steps. Going from left to right, the stair steps go up four steps, then repeat. This gives an accurate position of the crossheads on the closing stroke. Each change in steps or dots indicates one-fourth inch in crosshead travel.

This oscillogram is a record of an OCO operation, so the stair steps go up when the crosshead is traveling up, and go down when the crosshead is traveling down. It will be noticed that every seven inches of travel the dot is one step lower than the usual one for this position. This point is used to indicate when the oil-switch contacts physically close and open. The device is so set to give this position before each set of tests by actually checking the closing of switch contacts with low voltage. With this accurate method of determining when the contacts are closed, it is possible to tell very accurately when the switch prestrikes on closing, and how long it arcs in opening. From this oscillogram it can be seen that this switch prestruck exactly one-quarter inch, and arced exactly seven-eighths inch on opening.

The construction of the transmitting device is very simple, as shown by figure 2. It consists of a small motor commutator with 28 bars. Every fourth bar is connected together, as shown by figure 3. The commutator is stationary, having a light double-contact brush rotating around the commutator. The travel device is connected to the crosshead by means of a small music wire which is wound and unwound on a drum that drives the brushes (one revolution of the drum equals seven inches of motion). The music wire is kept under considerable tension by a large clock spring. The inertia of the moving parts of this device has been kept to a minimum.

This device has been used on our last three sets of tests, and has been the means of gathering very important information regarding the behavior of oil-switch travels.

Another interesting circuit used on the oscillograph was that of recording trip-coil voltage and current. In some of the tests, it was necessary to give the point where the tripping relay closed, and in some cases the oil switch prestruck before the trip-coil auxiliary switch closed. Since all the available elements of the oscillograph were used for various records, it was necessary to devise this method of obtaining trip voltage and current on one element. Figure 4 shows the schematic of this connection. When relay contacts close, definite current flows through resistor *R*2, causing a deflection; then if *A* is closed at the time the relay closes contacts, the oscillograph measures the voltage drop across the shunt. Figure 5*a* is a trace of a normal oscillograph of trip-coil voltage and current. Figure 5*b* is a trace of an oscillograph where the relay closes contacts and a period later the auxiliary switch causing trip-coil current to flow.

Voltage Indication

To facilitate operation of a high-voltage transmission system, means for indicating at each end of a line whether it is

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"hot" or not are very desirable. The cost of potential transformers for the purpose is generally so high that other means have been sought. One of these is by using a very sensitive a-c instrument shunted across the ground insulator of a string, as shown in figure 6. The instrument is a 0 to 100-microampere meter with an adjusting rheostat of 15,000 ohms. This is a good "hot" line indicator, but the indication will vary with the humidity of the air. The scale can be marked in normal voltage of line and with the adjusting rheostat kept at approximate correct reading. In service this type of instrument has proved itself very useful. In some cases the two leads are in lead-covered cable for some 400 feet.

In the case of the original General Electric bushing with low-voltage capacitance taps, it was necessary to use a one-tube amplifier to get good voltage readings. With the new a-c microammeters now available it is possible to have a direct-reading voltmeter. It is only necessary to have a 0–300 microammeter connected direct to the capacitor tap. To adjust the meter, a suitable rheostat is connected in parallel with the instrument.

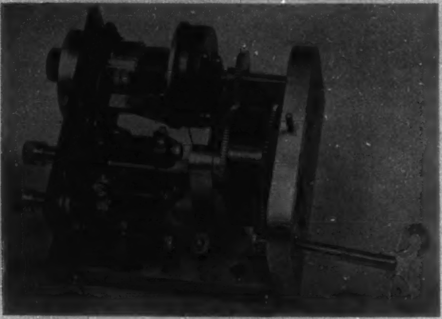
Figure 2. Travel-recording transmitter

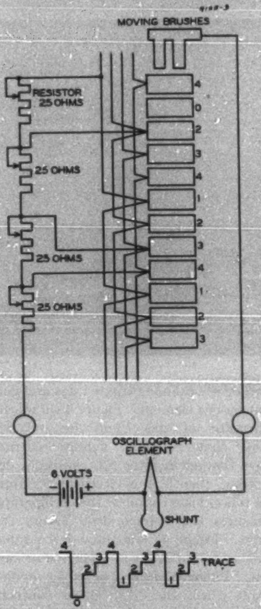

Figure 3. Layout of travel-recording transmitter

Disturbance Recorders

In order to study short-circuit conditions on a large transmission system, it is necessary to have high-speed recording devices. The Edison company has four oscillographs, six Hall recorders, and eleven disturbance recorders connected to the transmission system at the various important stations. These instruments operate to give records for system study of most all transmission faults. Most faults have a normal clearance, so the most value these records have is when the occasional abnormal clearance occurs.

The oscillograph and Hall-recorder films must be developed and calibrated before the information is available. The disturbance recorder is a high-speed device that has a motor running continuously, that engages the paper drive by means of a clutch that is operated by fault-detecting relays. At the same instant, a spark coil is energized that punctures the paper to give the trace of measuring device.

These instruments are made in various forms; one was made from a normal five-ampere recording ammeter by stiffening the restraining spring so it took 50 amperes full scale. This gives the instrument 100 times the torque it had originally. With this energy the instrument deflection is exceedingly rapid, being in the order of a few cycles. In order to damp the instrument while recording, it is necessary to use very heavy damping by means of an electromagnet. The speed of charts for the various recorders ranges from two to five inches per second.

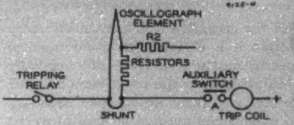

Figure 4. Oscillograph connections for measuring tripping voltage and current

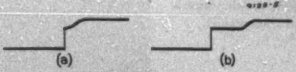

Figure 5. Oscillograph traces of tripping voltage and current

These ammeter-type disturbance recorders are being used to determine the length and magnitude of ground faults on the 220-kv system. The readings from the various locations of the system are immediately available, and enable the dispatchers to determine the approximate location of the fault.

Figure 7 shows the disturbance recorder installed at Chino substation, the receiving station for Boulder power. This instrument records ground current, voltage, and frequency. Figure 8 shows

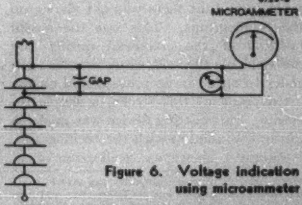

Figure 6. Voltage indication using microammeter

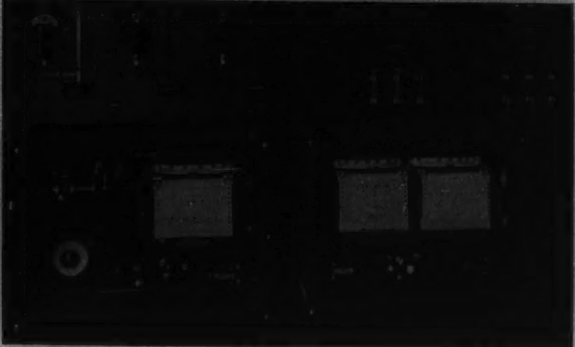

Figure 7. Three-element disturbance recorder

the auxiliary devices necessary for operating the disturbance recorder. Figure 9 gives the schematic connections of the various parts.

Figure 8. Disturbance-recorder auxiliary

The operation of this recorder can be easily followed by referring to figures 7, 8, and 9. The left element is for measuring 66-kv and 220-kv ground currents. The current transformers are in the neutral of the two banks—one on the 66 kv and the other on the 220 kv. In the case of the 220 kv, the current measured is the ground-fault-neutralizer current. Each of the neutral-current-transformer secondaries is connected to an *AX* relay and transformer. The transformers are so connected as to indicate on one element. The current element consists of a large D'Arsonval d-c element shunted heavily for damping supplied from the secondary of the transformers through a full-wave rectifier. The permanent magnet of the D'Arsonval unit is replaced by a saturating electromagnet to give the maximum flux possible. The end of the needle is made of tungsten to prevent burning from the spark required to puncture the paper.

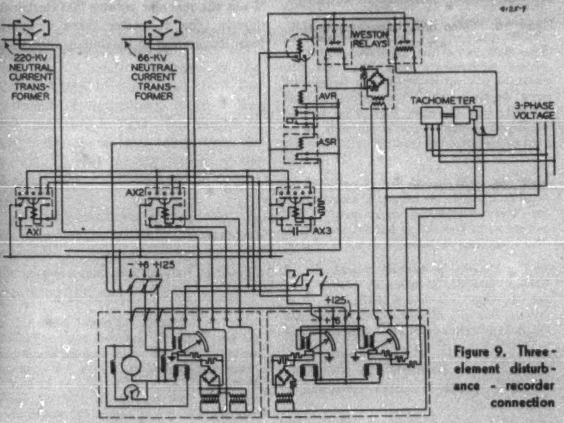

Figure 9. Three-element disturbance-recorder connection

The *AX*1 and *AX*2 relays are instantaneous relays, and the duration of the fault can be accurately measured by precalibrating the recorder by passing through it a given current for a given length of time. In most actual cases, the recorders have checked the duration of fault as recorded by the oscillograph within a couple of cycles.

The center element is similar in construction, but electrically it is a voltmeter. The right element is a frequency recorder. Both of these elements are normally energized and give indication of normal voltage and frequency. In the case of abnormal voltage, a sensitive Weston relay is used to close contact. This relay operates an auxiliary relay, which in turn energizes a vibrating relay *AVR*. The vibrating relay energizes time-delay contact-opening relay *ASR*, which in turn energizes relay *AX*3. When *AX*3 is energized, the clutch of the disturbance recorder is engaged to start the chart, and the spark coils for voltage and frequency charts are energized to puncture paper for the trace of the abnormal condition.

Since the voltage may swing to normal, then back to abnormal, the vibrating relay is set to continue the chart trace from five to ten seconds.

The frequency indication is obtained by using a small three-phase synchronous motor driving a tachometer. The voltage from the tachometer is proportional to the frequency. The movement has a suppressed zero, and records 40 to 70 cycles. The normal frequency is 50 cycles. Should high or low frequency occur, a sensitive relay is used to close contact under these conditions to start the same series of relays as used on the voltage recording. The continuing of recording upon return of normal frequency has proved very important. In many cases the frequency will change from one abnormal condition to the other. The chart should continue during this period. Many very accurate records of voltage and frequency have been obtained during system disturbances. Figure 10 gives a photograph of a voltage chart during a short circuit.

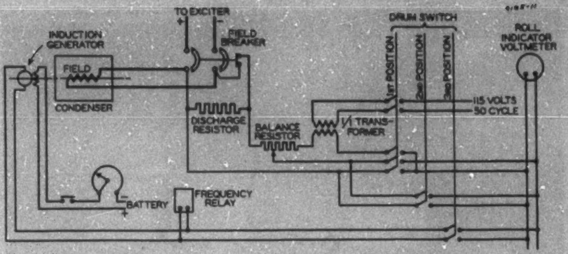

Figure 11. Wiring diagram of condenser roll indicator

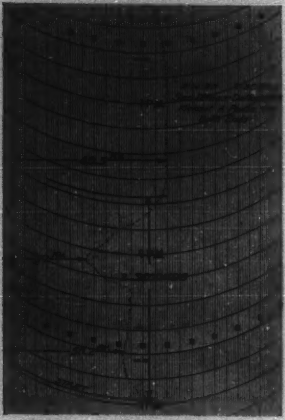

Figure 10. Chino disturbance-recorder voltage chart

Synchronous-Condenser Roll Indicator

At the new Chino substation, the 50,000-kva hydrogen-cooled synchronous condenser is an outdoor type. The operating room is approximately 150 feet from the condenser. When the condenser is started, a remote device for giving the operator an indication of the fact that the condenser is starting or not is necessary. A simple and effective device called the roll indicator is shown by figure 11. The indicator is a normal 0–150-volt a-c instrument. It is so connected that it measures the difference of a 50-cycle voltage produced by an auxiliary transformer and the induced voltage produced across the condenser field-discharge resistor. As soon as the condenser starts rolling, the voltmeter needle moves. When the machine rotates 360 electrical degrees, the voltmeter takes a swing from its position to maximum, then to a minimum back to its starting point. This action may be the reverse from this according to the actual phase location of the field when the machine is started. As the machine speeds up, this cycle of indication speeds up to a point when the voltmeter can no longer follow the fluctuations. At this point the fixed 50 cycles is removed from the instrument, and only the voltage drop across the discharge resistor is measured. As the machine comes into synchronism, the voltmeter will read the induced voltage in the field, and when nearly in step the meter will give a swinging indication. When the machine pulls into step, the voltmeter will indicate no swinging and zero on the dial. The voltmeter is then switched over to read voltage of the small shaft-induction generator which gives an indication of condenser speed.

Temperature Survey of the United States

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IN establishing the rating of electrical apparatus there are many factors involved. The maximum temperature reached by any part of the insulation is one of these. However, the relationship of temperature to rating is a complex function and the rating cannot be considered as a function of temperature alone.

In order to arrive at a rational system of rating, it is necessary to have standard rating specifications for various classes of apparatus, according to which all ratings in each class are established. In general these specifications give an allowable temperature range in terms of temperature rise. The temperature range must be referred to some maximum allowable temperature base. This base, from a rating standpoint, is called the ambient temperature. From the point of view of practical consideration the ambient temperature selected should reasonably fall in the range of normal conditions. At the same time it should not be too remote from readily obtainable temperatures, at which temperature tests may be conducted.

The selection of 40 degrees centigrade as a possible maximum temperature in the above procedure, seems to have been a fortunate one in nearly all cases. No difficulty is encountered with this basis of rating, even in the occasional cases where the actual ambient conditions are excessive, for the proper use of equipment under such circumstances should be handled as an application problem. It must be borne in mind, however, that the modification of the allowable temperature rise is not a direct function of the ambient. This means that a motor used in an ambient 10 degrees centigrade lower than usual cannot necessarily be used at 10 degrees higher temperature rise.

In order to obtain authoritative information on atmospheric temperature in all parts of the country, at all seasons of the year, and for long periods, the records of the United States Weather Bureau were consulted. Through the courtesy of the Weather Bureau, analyses of automatic thermograph charts for a limited number of stations were made. Studies were made of these data and of data taken from the reports of the Weather Bureau to suggest suitable forms in which they might be useful to engineers.

This paper contains the following information:

A—Summary of sources of weather data (see appendix I)

B—Temperature data for many well-distributed locations throughout the country

C—Data on electric motors in industrial uses and the power generated, arranged according to geographic locations

Temperature Data Presented in This Paper

The temperature data presented in this paper in items 1 to 3 have been taken from the "Climatic Summary of the United States" by the United States Department of Agriculture, Weather Bureau, published in 1930. For items 6, 7, and 8 data have been taken from the annual "Report of the Chief of the Weather Bureau" back to 1892 (see table I).

All temperatures reported by the Weather Bureau are from thermometers suspended in thermometer houses. The thermometers are not exposed to the direct rays of the sun.

1. *Annual Average Temperature.* The annual average temperature represents the average of the daily minimum and maximum readings for each day during the entire period of record. This period is about 50 years, so each annual average represents approximately 36,500 individual readings (52.8 degrees Fahrenheit for the entire United States).

2. *Average Yearly Variation of the Monthly Average Temperature.* This variation was taken as the difference between the highest average monthly temperature and the lowest average monthly temperature. The highest average temperature is not the highest average temperature on record but it is the average of the average temperatures for that month over the entire period during which records have been kept. (43.4 degrees Fahrenheit for the entire United States.)

3. *Average Daily Variation of Hourly Temperatures.* The average daily variation is taken as the difference between the average of the daily maxima and the average of the daily minima for the entire record period. This does not represent the true average daily variation to be expected in either the winter or the summer, because data from the Weather Bureau show that in most locations the winter daily variation is four to five degrees less than the summer daily variation. However, by using an average value, there will be an error of only about two degrees in either winter or summer, and it will be fairly accurate for the greatest portion of the time. (20 degrees Fahrenheit average for the entire United States.)

4. *The Absolute Maximum Recorded.* This temperature is as its name indicates, the maximum temperature recorded at that station at any time during the record period, see figure 1. (The highest temperature ever recorded at a Weather Bureau Station was 134 degrees Fahrenheit, and the average of the maxima for the entire United States is 104.7 degrees Fahrenheit.)

5. *The Absolute Minimum Ever Recorded.* This temperature is just as its name indicates, the minimum temperature recorded at a station at any time during the record period. (The lowest temperature ever recorded at a Weather Bureau station was −57 degrees Fahrenheit and the average of the minima for the entire United States is −18.6 degrees Fahrenheit.)

6. *Average Number of Days per Year Temperature Exceeded 90 Degrees Fahrenheit.* From the annual "Report of the Chief of the Weather Bureau" for the years 1892 to 1936 were taken the number of days each year the temperature exceeded 90 degrees Fahrenheit and averaged for 179 Weather Bureau stations. These averages were plotted on a map, figure 2, and climatic bands were developed. These averages for representative stations are recorded as part of table I.

7. *Maximum Number of Days per Year Temperature Exceeded 90 Degrees Fahrenheit.* These values were obtained from the reports mentioned in (6) and are recorded in table I for representative stations.

8. *Minimum Number of Days per Year Temperature Exceeded 90 Degrees Fahrenheit.* These values were also obtained from the reports mentioned in (6) and are recorded in table I for representative stations.

Duration of Elevated Ambient Temperatures

Consideration will first be given to certain limiting conditions of temperature. From a survey of the whole country of the highest temperatures ever observed, made by the United States Weather Bureau, the following results were obtained:

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6. For all numbered references, see list at end of paper.

Maximum Temperatures Never Exceeded (Deg F)	Percentage of Area of the United States Affected
100	10.8
105	35.2
110	73.1
115	95.0

Note: Temperature in only 5 per cent of the area of the country has ever exceeded 115 degrees Fahrenheit.

Table I. Temperature Characteristics of the United States for 60 Selected Stations (Fahrenheit System)

Degrees Fahrenheit

	1. Annual Average	2. Average Yearly Variation	3. Average Daily Variation	4. Absolute Maximum	5. Absolute Minimum	6. Average Number of Days Per Year Temperature Exceeded 90	7. Maximum Number of Days in a Year Temperature Exceeded 90	8. Minimum Number of Days in a Year Temperature Exceeded 90
Albany, N. Y.	48	49	17	104	−24	9	20	1
Albuquerque, N. Mex.	56	41	27	104	−10	57	71	46
Atlanta, Ga.	62	35	17	103	−8	29	75	9
Baltimore, Md.	56	44	16	105	−7	22	48	6
Birmingham, Ala.	63	34	19	107	−10	51	109	15
Bismarck, N. Dak.	40	62	23	108	−45	18	50	2
Boise, Idaho	51	45	24	121	−28	38	54	18
Boston, Mass.	50	44	16	104	−14	8	17	1
Buffalo, N. Y.	47	46	14	95	−14	1	5	0
Burlington, Vt.	45	51	17	100	−28	3	15	0
Charleston, S. C.	66	32	14	104	7	27	40	8
Cheyenne, Wyo.	45	41	25	100	−38	4	31	0
Chicago, Ill.	49	49	14	103	−23	12	26	1
Cincinnati, Ohio	55	45	17	105	−17	26	63	6
Columbus, Ohio	52	46	18	104	−20	20	52	3
Dallas, Tex.	66	38	19	105	−3	85	118	51
Davenport, Iowa	50	54	18	106	−27	22	53	1
Denver, Colo.	50	42	25	105	−29	19	41	3
Des Moines, Iowa	50	56	19	110	−30	26	66	2
Detroit, Mich.	48	48	15	105	−24	10	33	0
Duluth, Minn.	39	56	16	99	−41	2	14	0
El Paso, Tex.	64	37	25	106	−5	82	120	54
Helena, Mont.	44	47	21	103	−42	9	25	0
Indianapolis, Ind.	53	48	18	106	−25	22	53	3
Jacksonville, Fla.	69	26	17	104	10	49	75	25
Kansas City, Mo.	55	48	18	108	−22	35	88	3
Little Rock, Ark.	62	39	18	108	−12	52	85	24
Los Angeles, Calif.	63	16	21	109	28	11	28	3
Louisville, Ky.	57	44	18	107	−20	36	63	13
Memphis, Tenn.	62	40	17	106	−9	42	81	12
Miami, Fla.	75	14	12	96	27	6	26	0
Milwaukee, Wis.	46	50	15	102	−25	8	26	0
Minneapolis, Minn.	45	59	18	102	−33	13	35	0
Mobile, Ala.	68	30	17	103	−1	44	79	10
Nashville, Tenn.	60	40	19	106	−13	41	73	0
New Haven, Conn.	50	44	16	101	−14	6	4	0
New Orleans, La.	70	27	15	102	7	54	83	15
New York, N. Y.	52	43	15	102	−13	6	17	1
Oklahoma City, Okla.	60	43	21	108	−17	61	96	16
Omaha, Nebr.	51	55	19	111	−32	31	72	4
Peoria, Ill.	51	51	19	107	−27	30	62	2
Philadelphia, Pa.	54	43	15	106	−6	15	34	3
Phoenix, Ariz.	70	39	28	119	12	152	188	127
Pittsburgh, Pa.	53	43	18	103	−20	11	33	0
Portland, Maine	46	46	15	103	−21	3	8	0
Portland, Ore.	53	28	16	104	−2	7	16	0
Raleigh, N. C.	60	36	19	103	−2	35	62	10
Rapid City, S. Dak.	46	48	23	106	−34	21	53	2
Reno, Nev.	50	38	18	102	−19	33	55	9
Richmond, Va.	58	40	20	107	−3	32	63	13
Salt Lake City, Utah	52	47	20	103	−20	29	64	6
San Francisco, Calif.	56	11	12	101	29	1	4	0
Sault Ste. Marie, Mich.	40	49	17	98	−37	2	12	0
Seattle, Wash.	52	24	13	98	3	1	3	0
Spokane, Wash.	48	44	20	108	−30	19	29	4
St. Louis, Mo.	56	47	17	108	−22	38	72	5
Trenton, N. J.	54	44	17	105	−13	14	23	3
Topeka, Kans.	54	50	21	110	−25	43	91	10
Vicksburg, Miss.	66	33	18	104	−1	55	90	21
Washington, D. C.	55	43	18	106	−15	26	52	7

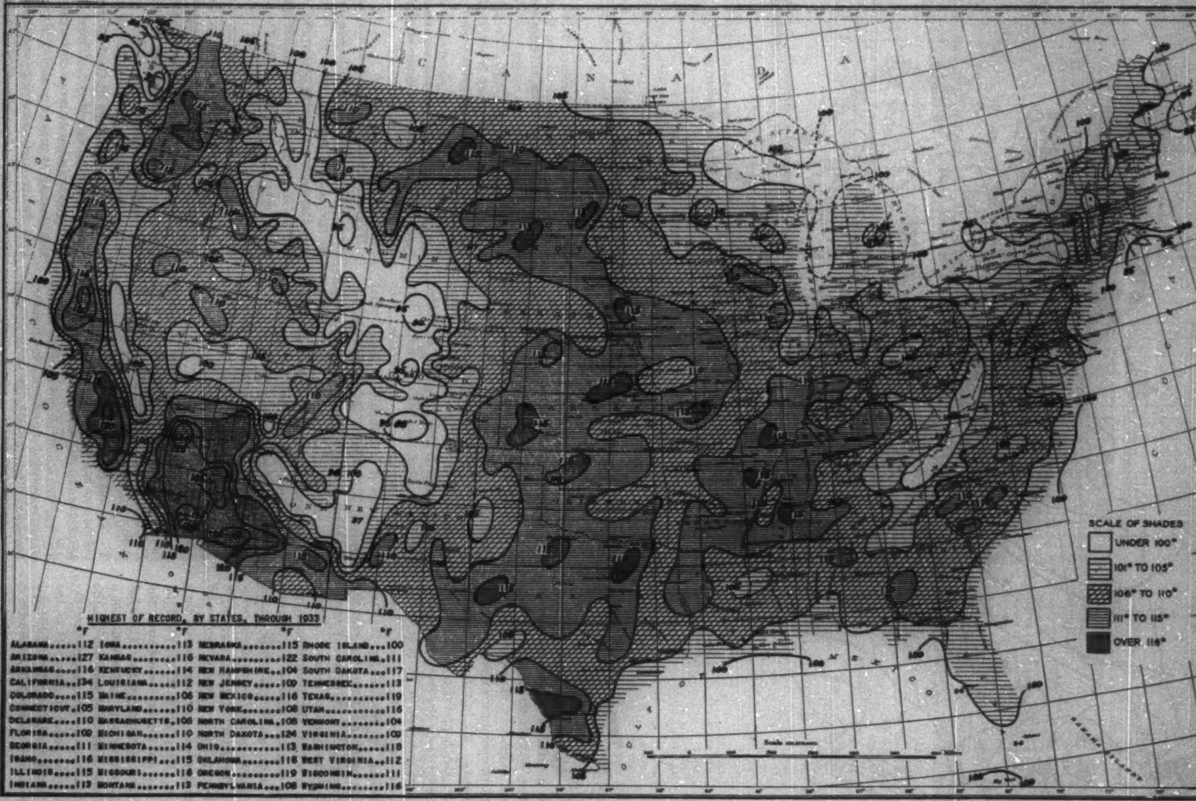
To obtain correlation of monthly temperatures throughout the country, temperature data from 23 stations were tabulated and averaged. Figure 3 is a composite graph showing the absolute station maximum and minimum, the average of the average monthly maximum, the average of the average daily maximum, the average of the average monthly temperature, the average of the average daily minimum and the average of the average monthly minimum.

All of the temperature curves are cyclic in character and change in an orderly way from season to season. The maximum temperature is usually recorded in July or August. The variation of the maximum and minimum from the average is a characteristic of the climate at the 23 stations and may cover extreme ranges in some cases.

The information from which the curves were derived was obtained from averages over a period of time ranging from 15 to 75 years. The 23 stations are representative of the extremes of climate and while they do not indicate the maximum that has ever been reached they are representative of the extremes in the inhabitable areas of the United States.

Consideration of the maximum temperatures ever reached over the country, as well as of the average conditions for 23 stations, representative of the whole country, gives some very valuable general information. However, cognizance must be taken of the fact that the length of time the higher ambient temperatures are sustained is responsible for insulation deterioration. As a basis for this study an analysis was made of information compiled by the Weather Bureau[6] showing the number of days per year the temperature exceeds 90 degrees Fahrenheit for 179 stations over the country. An average of data for the years 1892 to 1936 was obtained and the numerical values were spotted on a map. From this map, figure 2 was developed on which climatic bands are shown. Mark 10 indicates that all territory above this has had ten days or less per year in which the temperature has exceeded 90 degrees Fahrenheit. It will be noted from this map that taking all area above climatic band marked 60, practically all of the country in which there is a dense motor population is included.

From the Weather Bureau were obtained the number of hours per day the temperature remained above 90 for each day the temperature exceeded 90 degrees Fahrenheit for eight selected stations for a period of five years (1934 to 1938). These data were plotted and are shown in figure 4. With this curve it becomes


SCALE OF SHADES
UNDER 100°
101° TO 105°
106° TO 110°
111° TO 115°
OVER 115°
C A N A D A
M E X I C O
BAHAMA ISLANDS
Scale of miles

HIGHEST OF RECORD, BY STATES, THROUGH 1933

	°F		°F		°F		°F
ALABAMA	112	IOWA	113	NEBRASKA	115	RHODE ISLAND	100
ARIZONA	127	KANSAS	116	NEVADA	122	SOUTH CAROLINA	111
ARKANSAS	116	KENTUCKY	114	NEW HAMPSHIRE	104	SOUTH DAKOTA	117
CALIFORNIA	134	LOUISIANA	112	NEW JERSEY	109	TENNESSEE	113
COLORADO	115	MAINE	106	NEW MEXICO	116	TEXAS	119
CONNECTICUT	105	MARYLAND	110	NEW YORK	108	UTAH	116
DELAWARE	110	MASSACHUSETTS	106	NORTH CAROLINA	108	VERMONT	104
FLORIDA	109	MICHIGAN	110	NORTH DAKOTA	124	VIRGINIA	109
GEORGIA	111	MINNESOTA	114	OHIO	113	WASHINGTON	118
IDAHO	116	MISSISSIPPI	115	OKLAHOMA	118	WEST VIRGINIA	112
ILLINOIS	115	MISSOURI	116	OREGON	119	WISCONSIN	111
INDIANA	113	MONTANA	113	PENNSYLVANIA	108	WYOMING	116

Figure 1. Map of absolute maximum temperature

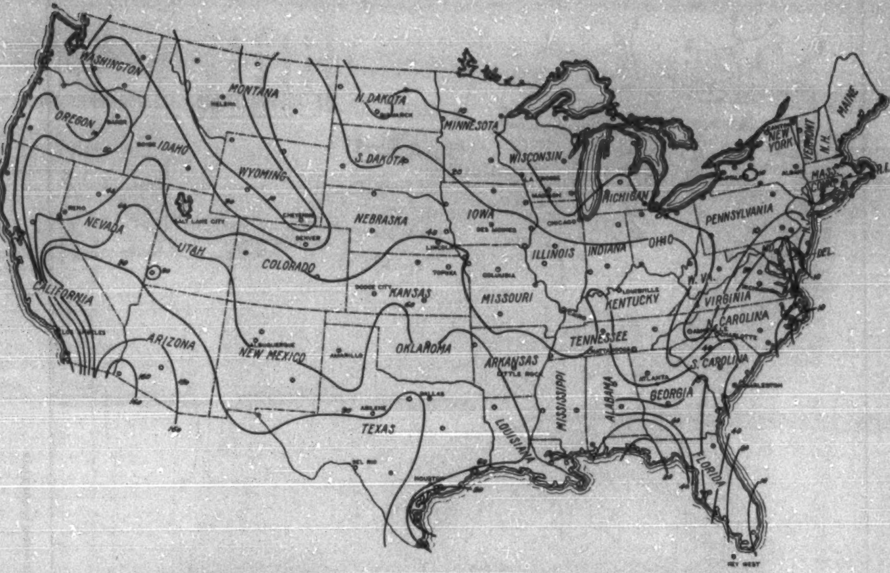

WASHINGTON
OREGON
IDAHO
MONTANA
HELENA
WYOMING
N. DAKOTA
BISMARCK
S. DAKOTA
MINNESOTA
WISCONSIN
MICHIGAN
NEVADA
RENO
UTAH
SALT LAKE CITY
CHEYENNE
DENVER
COLORADO
NEBRASKA
LINCOLN
IOWA
DES MOINES
CHICAGO
ILLINOIS
INDIANA
OHIO
PENNSYLVANIA
NEW YORK
ALBANY
VERMONT
N.H.
MAINE
MASS.
CONN.
R.I.
DEL.
MD.
W. VA.
VIRGINIA
KENTUCKY
LOUISVILLE
TOPEKA
COLUMBIA
KANSAS
DODGE CITY
MISSOURI
CALIFORNIA
LOS ANGELES
ARIZONA
NEW MEXICO
ALBUQUERQUE
AMARILLO
OKLAHOMA
TEXAS
ABILENE
DALLAS
DEL RIO
HOUSTON
ARKANSAS
LITTLE ROCK
TENNESSEE
CHATTANOOGA
N. CAROLINA
CHARLOTTE
S. CAROLINA
CHARLESTON
GEORGIA
ATLANTA
ALABAMA
MISSISSIPPI
LOUISIANA
FLORIDA
KEY WEST
10
20
40

Figure 2. Map of average number of days per year in which temperature exceeded 90 degrees Fahrenheit

possible to determine within reasonable limits the number of hours per year the temperature exceeds 90 degrees Fahrenheit, if the number of days per year on which the temperature exceeded 90 degress is known. For instance, it is found that where the temperature is above 90 degrees for 60 days—the temperature will on the average be above 90 degrees for approximately 410 hours. Figure 5 is a curve showing the relationship between the average number of hours per year the temperature exceeds 105 degrees Fahrenheit against the average number of days per year in which the temperature exceeds 105 degrees. If the curve were based on data covering a longer period the figures may be subject to some modification. This study of days exceeding a certain temperature may be continued at temperatures higher and lower than 90 degrees. Correlation of the resulting data for any one locality would show the average time the temperature exceeded any given temperature in the space of a year. From a series of such curves the per cent time that the temperature exceeded a given value in any part of the country could be obtained. Additional work, however, is required to obtain these results.

Probability of Occurrence of Temperature Extremes

Quite often it is necessary to design electrical equipment to withstand the maximum or minimum temperature to be encountered in a given locality. One such instance is the design of air-cooled rectifiers using an intermediate fluid between the air and the rectifier. In these cases it is difficult to decide what maxima and minima are to be expected. Certainly the highest and lowest on record for a period of 50 to 100 years cannot be expected more often than once in 50 or 60 years. After a brief study of the frequency of occurrence of extremes in representative locations the following frequency of occurrence was found to be approximately applicable:

1. The maximum temperature to be expected once in ten years is three degrees Fahrenheit below the maximum recorded to date.
2. The maximum temperature to be expected once in five years is 5 degrees Fahrenheit below the maximum recorded to date.
3. The maximum temperature to be expected every year will exceed a temperature ten degrees Fahrenheit below the maximum recorded to date.
4. The minimum temperature to be expected once in ten years is five degrees Fahrenheit above the minimum recorded to date.
5. The minimum temperature to be expected once in five years is six degrees Fahrenheit above the minimum to date.
6. The minimum to be expected every year will be lower than a temperature 25 degrees Fahrenheit above the minimum recorded to date.

These figures are only approximate and need to be interpreted with judgment. In the coastal regions, such as Florida and the west coast, the variations will not be so great. Likewise in the great plains region from Ohio to the Dakotas the variation will not be so great. Some of the eastern seaboard region has variations much greater than given in these figures. These values are only suggested as a benchmark so that the actual maximum and minimum recorded temperatures can be properly used.

In using maximum or minimum temperatures the duration of these temperatures should be considered. These temperatures are almost always of short duration, lasting at most a period of three or four hours and occurring only about once a year. Generally they are local

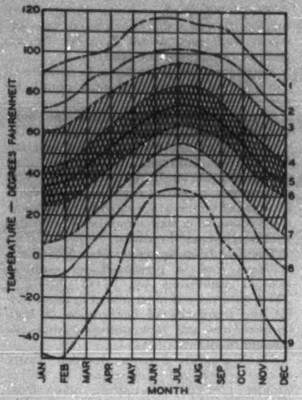


Figure 3. Composite temperature chart—23 Weather Bureau stations (over periods ranging from 15 to 75 years)

1. Absolute maximum temperature
9. Absolute minimum temperature

(These two curves show maximum and minimum temperatures, respectively, for each month of the year, which were ever recorded at any of the 23 Weather Bureau stations during period of record)

2. Mean of absolute monthly maximum temperatures
8. Mean of absolute monthly minimum temperatures

(These curves were obtained by averaging the absolute maximum and minimum temperatures, respectively, which were ever recorded at each station for each calendar month during the period of record)

The remaining curves were obtained by calculating the mean for the 23 Weather Bureau stations from the average data for each station for the period of record

3. Mean of the average monthly maximum temperature
4. Mean of the average daily maximum temperature
5. Mean of the average monthly temperature
6. Mean of the average daily minimum temperature
7. Mean of the average monthly minimum temperature

phenomena, seldom being approached within ten degrees at locations 50 miles distant. The time-temperature lag of most equipment is great enough to carry through these short peaks of either maximum or minimum temperature.

The estimates of the probable occurrence of high temperatures are more accurate than the estimates of the probable occurrence of low temperatures. That is, most localities will show high temperatures occurring approximately as given by these values though the low temperatures sometimes vary widely from the values given.

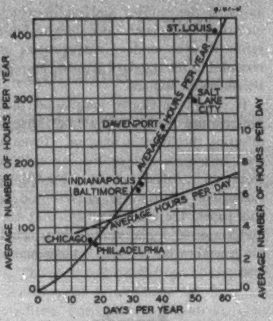


Figure 4. Curve of average number of hours per year and average number of hours per day versus average number of days per year temperature exceeds 90 degrees Fahrenheit (five-year period 1934 to 1938)

Distribution of Electrical Equipment

Complete information on the exact distribution and use of electrical equipment is not available. However, information is available[12,14,15] which will give an indication what the actual total distribution of electrical equipment is. Some of the available data have been summarized in tables II and III. Table II shows the distribution of electric motors in use or available for use in manufacturing and printing establishments at the close of the year 1929. This

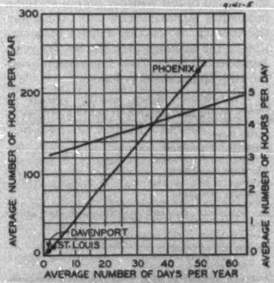


Figure 5. Curve of average number of hours per year and average number of hours per day versus average number of days per year temperature exceeds 105 degrees Fahrenheit (five-year period 1934 to 1938)

Table II. Electric Motors in Industry Available for Use*

	Number	Horsepower
Alabama	41,000	565,000
Arizona	4,000	115,000
Arkansas	10,000	121,000
California	144,000	1,398,000
Colorado	14,000	197,000
Connecticut	97,000	751,000
Delaware	7,000	101,000
District of Columbia	4,900	84,000
Florida	12,000	116,000
Georgia	43,000	439,000
Idaho	5,000	70,000
Illinois	271,000	2,436,000
Indiana	139,000	1,857,000
Iowa	39,000	331,000
Kansas	20,000	232,000
Kentucky	25,000	274,000
Louisiana	17,000	248,000
Maine	26,000	371,000
Maryland	47,000	660,000
Massachusetts	222,000	1,723,000
Michigan	252,000	2,007,000
Minnesota	47,000	444,000
Mississippi	7,000	97,000
Missouri	72,000	619,000
Montana	6,600	175,000
Nebraska	14,000	130,000
Nevada	1,100	15,000
New Hamsphire	26,000	244,000
New Jersey	181,000	1,381,000
New Mexico	1,300	14,000
New York	435,000	3,365,000
North Carolina	95,000	622,000
North Dakota	2,400	14,000
Ohio	296,000	3,745,000
Oklahoma	14,000	162,000
Oregon	23,000	312,000
Pennsylvania	440,000	5,005,000
Rhode Island	53,000	350,000
South Carolina	39,000	354,000
South Dakota	4,000	26,000
Tennessee	48,000	466,000
Texas	48,000	600,000
Utah	8,000	81,000
Vermont	13,000	124,000
Virginia	51,000	469,000
Washington	44,000	671,000
West Virginia	27,000	521,000
Wisconsin	115,000	1,048,000
Wyoming	2,700	32,000
United States total	3,577,000	35,132,000

* Data from "Special Report on Power Equipment in Manufacturing Establishments", Department of Commerce, Bureau of Census, 1932.

Table III. Current Generated in the United States*

Millions of Kilowatt-Hours

	1927	1932
New England (Maine, N. H., Vt., R. I., Mass., Conn.)	4,750	5,330
Middle Atlantic (N. Y., N. J., Pa.)	19,281	20,340
East North Central (Ohio, Ind., Ill., Mich., Wis.)	18,211	18,106
West North Central (Minn., Iowa, Mo., N. Dak., S. Dak., Nebr., Kans.)	4,549	5,357
South Atlantic (Del., Md., W. Va., Va., N. C., S. C., Ga., Fla.)	8,445	9,458
East South Central (Ky., Tenn., Ala., Miss.)	2,777	2,993
West South Central (Ark., La., Okla., Tex.)	3,275	4,308
Mountain (Mont., Utah, Wyo., Idaho, Colo., Ariz., N. Mex., Nev.)	3,389	2,346
Pacific (Wash., Ore., Calif.)	10,006	11,415
Total for United States	74,686	79,657

* Data from "Central Electric Light and Power Stations", United States Department of Commerce, Bureau of Census, 1932.

information is presented by states according to the number of motors and the total horsepower rating. These figures represent about $5^{1}/_{3}$ million motors in the United States with an aggregate horsepower rating of 35 million.

Table III shows the current generated in the United States according to geographic districts. It represented about 79 billion kilowatt-hours in 1932. This has increased[15] to 109 billion kilowatt-hours in 1938.

An examination of these tables will show that the majority of motors and power used is located in the district from Chicago east to the coast and up through the New England states and in another district on the Pacific Coast. The kilowatt-hours generated in these districts is just about of the same proportion.

If the total area where the temperature exceeds 90 degrees Fahrenheit for more than 60 days in a year in figure 4 is taken and it is assumed that the motor population as given in table II for each state is uniformly distributed over the state, the motor horsepower in the areas where the temperature exceeds 90 degrees Fahrenheit more than 60 days per year is less than five per cent of the total installed motor horsepower of the country. From figure 6 it is found that where the temperature exceeds 90 degrees on an average of 60 days per year, the temperature remains about 90 degrees for 6.5 hours per day or 390 hours per year, though in some of these places even this time is considerably less.

The present AIEE standards assume a normal design ambient of 40 degrees centigrade (104 degrees Fahrenheit). Study of the maximum temperature recorded at all points in the country shows that 104 degrees Fahrenheit is the average of these maxima although an absolute maximum of 134 degrees Fahrenheit has been recorded. If the average maximum recorded is to be the basis of the standards for design, 40 degrees centigrade is certainly the best figure.

Conclusions

In establishing ratings on electrical apparatus the problem is broadly one of selecting a reference temperature which satisfies the majority of applications. From the review of actual temperature conditions of the United States, it is seen that in most locations there is a small margin between the ambient of 40 degrees centigrade (104 degrees Fahrenheit) and the maximum outdoor temperatures. It must be clearly borne in mind, however, that this paper takes no cognizance of the outdoor ambient in which electrical equipment may operate, but refers only to outdoor temperatures as recorded in the standard Weather Bureau thermometer stations.

It should be clearly understood that temperature is only one of the factors affecting the life of a machine. From a vast experience in operation, as well as from an analysis of the conditions throughout the country as presented in this paper, the indications are that the ambient of 40 degrees centigrade selected for general rating purposes is a reasonable one.

Appendix

Summary of Existing Weather Data

In beginning a survey of the weather conditions existing throughout the United States, it was first necessary to determine the sources of information available and the nature of the information contained in these sources. The best sources of available information are:

1. "Climatic Summary of the United States," by the United States Department of Agriculture, Weather Bureau, published in 1930.

2. "Climatological Data for the United States by Sections," published monthly by the United States Department of Agriculture, Weather Bureau.

3. "Climatological Data for the United States by Sections," published yearly by the United States Department of Agriculture, Weather Bureau.

4. "Climatology of the United States," Bulletin *Q*, United States Department of Agriculture, by A. J. Henry, published in 1905.

5. "Monthly Summary of Temperature and Dew Points," published by the aerological division of the United States Weather Bureau.

6. "Yearly Reports of Chief of the Weather Bureau" from 1892 to date ("Meteorological Year Book" since 1936).

7. "Atlas of American Agriculture," Part II—Climate.

8. Automatic thermograph charts on file at the Weather Bureau in Washington, D. C.

Source number 1 contains the most data in the best form for use in this paper. For the United States Weather Bureau stations, numbering approximately 250 throughout the country, it contains detailed information of the averages over the last 50 years (approximately). The information contained is summarized as follows:

1. *Temperature*. Highest monthly average on record for any one year, lowest monthly average on record for any one year, for each month, season, and entire year—
Average temperature
Average of the maxima
Absolute maximum
Average of the minima
Absolute minimum

2. *Sunshine*. For each month, season, and entire year
Average hours with sunshine
Percentage of possible

3. *Miscellaneous Items*
Average number of days with maximum temperature of 90 degrees Fahrenheit or above
Average number of days with minimum temperature of 32 degrees Fahrenheit or below
Average number of days with minimum temperature of 0 degrees Fahrenheit or below
Average number of days with dense fog
Maximum precipitation recorded in periods of 5, 10, 15, etc., minutes up to 120 minutes

The information contained in source number 2 represents the annual monthly report from each of the Weather Bureau stations throughout the country.

Source number 3 contains summaries of the information presented each month by source number 2.

Source number 4 is perhaps the most complete collection and presentation of the climatology of the United States that has been made. However, it represents only the information available up to 1905. This information in general is the result of about 25 years of temperature records. The more recent data, as in source number 1, contains another 25 years of records.

The data from source number 5 has been analyzed and is presented for summer weather conditions by J. C. Albright. These data proved most useful in determining the frequency of occurrence of high temperatures and dew points during the summer months.

Source number 6 gives detailed yearly reports which are summarized in source number 1. The information taken from these reports is:

1. Number of days per year for each year since 1891 the temperature has exceeded 90 degrees Fahrenheit

(*a*). Average number of days per year the temperature exceeded 90 degrees Fahrenheit for 45 years for most of 179 stations.

(*b*). Maximum number of days the temperature has exceeded 90 degrees Fahrenheit in one year.

(*c*). Minimum number of days the temperature has exceeded 90 degrees Fahrenheit in one year.

Source number 7 gives temperature graphs for 23 stations. From these data a composite temperature graph was drawn showing:

1. Absolute station maximum temperature
2. Absolute station minimum temperature
3. Average of absolute monthly maximum temperature—23 stations
4. Average of average monthly maximum temperature—23 stations
5. Average of average daily maximum temperature—23 stations
6. Average of average monthly temperature—23 stations
7. Average of average daily minimum temperature—23 stations
8. Average of average monthly minimum temperature—23 stations
9. Average of absolute monthly minimum temperature—23 stations

These data are for a period of approximately 42 years.

From source number 8 the number of days and the number of hours of each day the temperature exceeded 90 degrees Fahrenheit was obtained for a five-year period.

Similar data were obtained for 105 degrees Fahrenheit.

The same information as given by items 1 and 2 above is given for 20 to 50 small stations located in each of these districts as well as for the two or three regular Weather Bureau stations. These data are tabulated giving the annual averages for temperatures and the actual monthly precipitation for each year on record.

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Discussion

For discussion, see page 1090.

Mingled Hydro and Steam Power Production in California—Past, Present, and Future

A. H. MARKWART

THE development in the United States of the central-station industry from its inception in San Francisco in 1879 and in New York in 1882, to the present, has been made in two broad phases. The initial or pioneer phase includes the first isolated steam-electric plants serving lighting customers, followed by small hydroelectric plants serving nearby customers and later (in Oregon in 1889) with the development of higher transmission voltages, serving customers remote from the generating plants. Mingled steam and hydro power was inaugurated in 1895 when the Folsom hydroelectric plant was connected to a steam-electric plant at Sacramento. In 1899, also at Sacramento, and again in the San Francisco Bay region in 1902, distant hydroelectric plants were connected with each other and with local steam plants. It may be said that these interconnections of remote hydro supplies with steam-electric plants culminated the initial phase.

The second phase was one of extensive development substantially all of which resulted from private initiative. The technique of production, transmission, distribution, and utilization of electric power was perfected and the field for utilization was broadened. The size of generating units became larger, efficiencies increased, transmission voltages and capacity were raised, more interconnections were made, and in the desire to derive all of the benefits resulting from complete co-ordination of all facilities serving a common market, the several properties were merged, first physically and later corporately, into integrated systems. The properties throughout the country now are well interconnected physically although something still remains to be done in so far as corporate integration is concerned.

During the last decade, a new idea has been advanced which contradicts that which has governed the central-station industry, namely, one of orderly development. Federal reservoir projects have been initiated throughout the nation, primarily to improve navigation, to control floods, and to supply water for irrigation—and, incidentally, to generate power. The generation of power, while incidental to the primary purposes, becomes a factor of major importance if revenue is to be provided to defray the cost of such projects. Projects of this character had been constructed by local private and public interests in California and in connection with Bureau of Reclamation projects in other states, but they were relatively small in size and the marketing of the power throughout the then existing agencies, did not present a serious problem.

The first of the large Federal multipurpose projects was the Colorado River development, which was thoroughly investigated in all of its phases and particularly with reference to the sufficiency of the revenue from the sale of power and water to reimburse the Federal Government for expenditures on that project. Firm long-time contracts for the sale of power and water were negotiated before work actually began.

When in 1933 the Federal administration in order to restore economic prosperity believed it necessary to expend Federal moneys on large projects throughout the country, it was inspired, possibly because of the success which accompanied the Boulder Canyon project, to launch a number of similar projects, generally without thorough investigation of potential power markets and in all cases with no arrangement for income to liquidate any substantial part of the cost. These projects are introducing large amounts of power in some regions and are creating major problems with respect to absorption. These projects, arising in what might be termed the third phase of the development of the industry, are closely related to its future progress.

It is proposed to discuss herein, the progress of the industry in California in the three phases of development just outlined and to attempt to forecast in a general manner what generating facilities will be required in the future.

Historical Beginnings—The Pioneer Phase of Development

It is generally stated that the first central-station plant in the United States was the Pearl Street steam-electric plant constructed in New York in 1882. Actually, however, this plant was preceded by a small steam-electric station in San Francisco constructed in 1879 by the California Electric Light Company, one of the many predecessor companies of the Pacific Gas and Electric Company. The demand for electric energy was germinated in this plant so beginning about

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1885, small generating units were installed in San Francisco and Oakland and by 1895 a total of nearly 6,000 kva had been installed.

The first hydroelectric plant in the world constructed to serve a distant demand was at Oregon City in 1889, the power being transmitted at 4 kv some 13 miles to Portland. The first hydroelectric plant in California was constructed at San Bernardino in 1887 and it served a load in its immediate vicinity. This was followed in 1891 by the 120-kva Pomona plant of San Antonio Light and Power Company which was the first hydroelectric plant in California constructed to transmit power at high voltage (10,000 volts) to distant loads (14 miles to Pomona and 28 miles to San Bernardino). The first hydroelectric plant in northern California was the Folsom plant, constructed in 1895 and still in operation—1,500 kva, transmitting power at 11,000 volts some 22 miles to Sacramento.

Hydroelectric development in northern California expanded rapidly owing to the presence of storage reservoirs and canal systems which had been constructed to serve the hydraulic mines in the Sierra Nevada mountains and foothills. Hydraulic-mining operations in this region declined rapidly after the famous Sawyer decree in 1884, when the United States Circuit Court issued an order restraining the North Bloomfield Mining Company from discharging debris into the streams. Mine after mine was closed by injunction and many of the storage reservoirs and the canals would have been rendered permanently obsolete except for the new type of power which could be generated from falling water and transmitted to points of use in distant towns. The Pelton water wheel had been developed to supply hydromechanical power for mining operations and it was a simple matter to employ it to drive electric generators.

The most ambitious undertakings of that period were the Colgate plant, 2,700 kw in capacity, constructed in 1899 and transmitting power at 40,000 volts some 76 miles to Sacramento, and later in 1901 with a total installation of 9,240 kw transmitting power 142 miles at 60,000 volts to Oakland; and the Electra plant constructed in 1902, 10,000 kw, transmitting power, also at 60,000 volts, 154 miles to San Francisco.

In 1898, because of a shortage of water at Folsom, the idea of connecting two remote hydroelectric power supplies was conceived. In the following year, Sacramento, which had a steam plant, received power from the Colgate-Folsom interconnected power system. This proved later to be an occurrence of tremendous importance for such a tying together of hydroelectric plants and a steam plant to supply light to cities and to serve industry and agriculture with power established the principles:

1. That advantage could be taken of the diversity which exists in stream flow at different hydroelectric plants.

2. That a steam reserve furnished the desired continuity in hydroelectric service, and

3. That pooling of a regional demand, which varied daily, seasonally, geographically, and with character of energy usage, resulted in economy, owing to the greater number of hours which a given plant capacity could be effectively operated.

The interconnection of steam and hydro plants at Sacramento was followed in 1902 by the interconnection at Oakland of the Colgate and Electra hydro plants with each other and with the steam plants of the Bay region. Interconnection on a large scale had become a reality and the first or pioneer phase of development was completed.

Second Phase of Development (1902–31)

The second phase of development began shortly after the turn of the century. It was natural that the many separate companies should become consolidated in order to realize all of the advantages of co-ordinated operation and corporate simplicity. State regulation of the electric utilities was inaugurated in 1911 under the California Railroad Commission which has very broad regulatory powers, including jurisdiction over capitalization, consolidation, mergers, valuation, accounting practice, rates, service standards, complaints, and the issuance of certificates of public convenience and necessity. Competitive situations which produced wasteful duplication of facilities and destructive rate wars, gradually became replaced by a situation in which a few large, thoroughly integrated systems served the California power market at rates which continually declined as more of the benefits inherent in co-ordinated operation were realized.

It may be well at this point to review briefly the economic principles involved in a mingled hydro and steam power supply. Going back somewhat to first principles, the cost of power delivered at load centers, whether in California or elsewhere, depends upon the capital cost of generating plants, including transmission lines, and the resulting annual cost. The capital cost of steam plants at load centers with relatively short transmission, in general, is less than that of hydro plants with long transmission, which occasions lower fixed charges for steam plants. Giving consideration to the cost of fuel and the relatively high running expense, however, the cost of steam-generated energy, for 100 per cent load factor, tends to exceed the cost of hydro-generated energy. On the other hand, for low load factors, on account of the greater amount of hydro capital which is unproductively employed, and because of the small quantity of fuel required to generate the relatively small number of kilowatt-hours which obtain with low load factors, the cost of hydro-generated energy tends to exceed the cost of steam-generated energy. A low load factor and cheap fuel, then, suggest steam-plant development, whereas a high load factor and expensive fuel make hydro development attractive. In either case, the load factor has a determining effect on the cost of power whether it be supplied by steam plants, by hydro plants, or by a combination of both. It follows then, with a system load factor, usually of the order of 45 per cent (municipal loads) to 60 per cent (regional loads), if the load be divided into two parts, and the bottom or high-load-factor portion placed on hydro and the top or low-load-factor portion placed on steam, that a composite economy in power production will result. This, of course, assumes that the capital cost of hydro is not so low as to preclude the use of any steam, and that fuel is not so cheap as to preclude the use of any hydro.

Practically all water-power plants in California are subject to the disadvantage which attaches to the variability seasonally and annually of precipitation and corresponding runoff. Complete co-ordination of steam and hydro supplies is obtained when no hydro energy is wasted in the spring of a wet year and all steam capacity, with the exception of a reserve against accidents, is used at maximum capacity factor in the summer of a dry year. With conditions approaching this optimum, hydro will operate at high capacity factor in wet periods while steam will comb off low-load-factor peak and in dry periods hydro will take the low-load-factor peak leaving high-load-factor operation for steam. In any operating system there are a great many other complexities which modify and tend to prevent the accomplishment of maximum benefits. For example, steam plants in metropolitan areas and those supplying process steam to industries must carry some base load at all times, some hydro plants, due to lack of storage facilities, must operate as "run of stream" plants and not only have reduced capacity in a dry period but must operate at or near a constant rate, and owing to mandatory requirements for irrigation some hydro plants with storage facilities have restrictions upon their operation so cannot be operated at low load factor.

The operating agencies, as they became more familiar with power-supply economies, to the extent that they could exercise control endeavored to maintain an economic ratio between the capacity installed in hydro and in steam facilities. In the early part of this period the efficiency of steam plants was low, requiring about 40,000 Btu of heat to generate one kilowatt-hour so steam plants were installed primarily for standby. In the first decade the capacity installed in hydro facilities was some 263,000 kw compared to 93,000 kw of steam, that is, only 26 per cent of the total was steam-electric capacity.

In the second decade, the steam turbine replaced the older reciprocating engines, fuel was cheaper, and efficiency of generators had been increased until the heat required was reduced to 25,000 Btu per kilowatt-hour. In this decade the amount of hydro capacity installed was about 375,000 kva and the steam 250,000 kva, steam being 40 per cent of the total capacity installed in that period. Between 1920 and the end of this phase of development in 1931, a total of 1,185,000 kva of hydro and 848,000 kva of steam-electric capacity was installed and for this period the ratio of steam to the total was about 42 per cent. By 1931, the efficiency of steam plants had further increased so less than 12,000 Btu of heat was required to generate a unit of energy and if normal growth had continued, the ratio of steam to hydro would have been further increased, probably to a point where a kilowatt of steam would match each kilowatt of hydro installed.

In the beginning California had a number of separate markets, the principal ones being the mining towns near the hydroelectric plants, the industrial centers in the valleys, and the metropolitan centers along the sea coast. Northern California has always been favored with an abundance of hydro supplies so many plants were constructed and the transmission lines, 60,000 volts at that time, crossing the valleys between the hydro plants and the metropolitan areas were tapped en route with economy so the smaller agricultural communities also could be served. The market in the northern part of the state grew rapidly and by 1930 it, including 60 per cent of the farms in the territory, was well served with electricity. The population in northern California increased from 1,174,000 in 1900 to 2,740,000 in 1930, a substantial growth, but the use of electricity increased amazingly, namely, from an estimated 100,000,000 kilowatt-hours to approximately 4,880,000,000 kilowatt-hours in the same period.

At the beginning of the 20th century the market in the southern part of the state was about equal to that of the northern market, but in the first 15 years it did not grow as rapidly. Close-in hydro supplies were very limited and since steam-generated power then was very costly, it became necessary to develop the more remote sources in the southern end of the San Joaquin Valley and transmit the power to the area south of the Tehachapi. Beginning in 1915, the population of southern California began to increase at a phenomenal rate and by 1930 the population of the ten southern counties had reached 2,940,000 which was $9^{1}/_{2}$ times the population of 311,000 in 1900. The use of electricity increased from an estimated 100,000,000 kilowatt-hours in 1900 to approximately 4,300,000,000 kilowatt-hours in 1930. The total period growth, while also very large, was somewhat less than that in the north, but toward the latter part of the period, it gave every indication that the two markets soon would be substantially equal in magnitude.

The market in the southern part of the San Joaquin valley developed separately and was served by hydroelectric plants in that area. The expansion of the southern companies into the San Joaquin valley in search of hydro supplies ultimately caused about one-third of this market to be attached to southern California. The remainder of this market by reason of rapid growth which utilized most of the available hydro supplies, finally was served by the generating plants located in the north.

At the end of 1931, the southern market was served at two different frequencies by a number of agencies. Fifty-cycle current predominated with the principal distributors and 60-cycles with the others, and because of this, complete large-capacity interconnection of facilities has not been accomplished. The installed capacity serving the southern market at the end of this period aggregated 1,420,000 kva of which 684,000 kva was in hydroelectric and 737,000 kva in steam-electric facilities. The dry-year load capability of the total installation with due allowance for reserve capacity was to the order of 900,000 kw and approximately 5,000,000,000 kilowatt-hours.

The northern market was served by 1,553,000 kva of installed capacity of which 1,139,000 was in hydroelectric and 414,000 in steam-electric facilities. The frequency in this area is 60 cycles and all systems are interconnected. The dry-year load capability of these facilities, with allowance for reserves, was about 1,000,000 kw and 5,500,000,000 kilowatt-hours.

By 1930, southern California agencies had brought in substantially all of the potential hydro supplies available to that area, and were compelled to meet load growth by installing steam-electric plants. Consideration was given to the development of favorable medium-sized power sites on Colorado River but the adoption of the Boulder Canyon project by the Federal Government precluded development by private enterprise. At the end of this phase of development, southern California had only one close-in power resource of any magnitude, that is, steam-electric power generated in plants near tidewater where circulating water and fuel are available in ample quantities.

The situation in northern California was quite different, because there is a relatively large amount of potential hydro power, a substantial amount of which can be developed and transmitted to load centers at costs which are competitive with steam-electric power. In the late 20s the operating utilities in northern California had definite plans for hydroelectric development on Pit, Feather, Bear, Mokelumne, and Kings Rivers. These plans contemplated construction on all of these rivers during the decade 1930–1940, but this situation changed owing to the depression and to the interest of the State of California in a water-supply project for the Sacramento and San Joaquin Valleys, with incidental generation of a large block of hydroelectric power.

Attention should be directed to the fact that up to this time the use of electricity in California had continually increased, that is, the use in one year always had been greater than in the preceding year. Following 1931, however, the great depression produced a sharp reduction in power demand, a situation new to the central-station industry in this state, and one which soon caused utility companies to become concerned about the future.

The Third Phase—Inauguration of Large Federal Multipurpose Projects

It may be said that the Boulder Dam project marked the entrance of the Federal Government into the realm of power production on a large scale. This project, justifiable through necessity for flood control and planned, as to power, with the cooperation of public and private distribution agencies and with due regard to the requirements of the region in economic proximity to it, is sound economically and socially. Before the advent of the Boulder project, except as Federal authorization was necessary for the use of public lands for water-power development, the Government was not concerned, save in those cases of land reclamation which involved the generation of small amounts of power incidental to irrigation operations. The intrusion of the depression, and during it, the changes made in Federal administration, brought a further and drastic change in the relation of Government to power development, in which generation facilities were provided in disregard of the economic principles governing power development.

Following the further advance of the Federal Government into the field of hydroelectric production and transmission, there has been a trend away from the orderly development of power to meet growth, and economic laws and regulatory practices which have heretofore prevailed are ignored. With Government as a power producer the matter of answering public convenience and necessity, a requirement of regulation with the private utilities, has been disregarded and certain elements of cost are made to disappear in power economics. With tax exemption generally in Government projects, together with the allocation to navigation, flood control, and national defense, of a large part of the capital cost, the Government can set low power rates. While it is not the intention to condemn the entrance of the Government into the power business, nor to discuss at length the sociological significance of such a process, it will have a marked influence on values; but just what the ultimate economic and social effect will be is difficult to foretell.

1. Boulder Canyon Project and the Southern California Power Market

As mentioned before, the first of the large multipurpose reservoirs of the Federal Government to be constructed was on the Colorado River at Black Canyon. It was conceived principally because of the early necessity for controlling floods and the movement of silt in the Colorado River which constantly menaced the rich Imperial Valley and which, because that valley is below sea level, ultimately would have destroyed it. It was also capable of providing an adequate irrigation supply for large areas in Arizona and Nevada and for the Imperial and Coachella Valleys of California and a water supply for the coastal plain of southern California, which would permit that section of the state to develop for years to come. Without this water supply, the growth of southern California soon would be restricted.

The project had been studied by engineers for many years and was thoroughly investigated after the first legislation which authorized the Colorado River Compact between the seven interested states (Arizona, California, Colorado, Nevada, New Mexico, Utah, and Wyoming) was passed by Congress in 1921. The Boulder Canyon Project Act was the final outgrowth of all of these investigations and received executive approval in December 1928, subject, however, to subsequent ratification of at least six of the seven interested states. This approval of the six states was soon had and on June 25, 1929, eight years after the first legislation, the Boulder Canyon Act became effective.

The act provided for the construction of a dam at Black Canyon or Boulder Canyon and the construction of an all-American canal connecting the Imperial and Coachella Valleys with the Colorado River and authorized an expenditure for these works of $165,000,000. The act stipulated that construction would not begin at the dam until contracts had been negotiated which would secure revenue sufficient to insure payment of all expenses of operation and maintenance and the repayment within 50 years of the capital cost with interest. Construction of the all-American canal was contingent on negotiating contracts which would secure revenue sufficient to meet the cost of operation and maintenance and to repay the cost of construction in 40 equal annual installments without interest. It is estimated that the power contracts as finally negotiated will produce in the 50-year contract period a total revenue sufficient to pay off all of the costs of the dam including amortization of capital at four per cent interest and to provide a surplus of $129,000,000 for the use of the states in the Colorado River basin. Consideration now is being given to reducing the payment for falling water at the dam so the revenue in the 50-year period will retire all of the cost except $25,000,000 at $3^{1}/_{2}$ per cent interest and will accumulate a surplus of $55,000,000 for the basin states. It is stated that this plan if adopted will reduce the payment for falling water from 1.63 mils to approximately 1 mil per kilowatt-hour.

The construction in Black Canyon of the Hoover Dam and the allocation of the waters of the Colorado made it possible for the communities around Los Angeles to organize a water district for the purpose of bringing Colorado River water to them. The Metropolitan Water District was thus created and in due course, at a cost of $220,000,000 constructed a diversion dam on Colorado River 150 miles below Black Canyon, together with conveyance facilities, that is, pumping plants, pipe lines, tunnels, canals, and regulating reservoirs to deliver the water 290 miles to the coastal area of southern California. The district contracted for 36 per cent of the firm power and has first call on all secondary energy, because with ultimate water-supply demands, it will require this power to pump 1,600 second-feet of water against a total lift of over 1,600 feet.

The firm power at the dam is about 500,000 kilowatts and the power plant is designed to accommodate a total installation of 1,315,000 kva. All of this capacity will not be employed in the southern California power market, because of allocation of power for pumping on the Colorado River Aqueduct of the Metropolitan Water District. The capacity which is likely to be installed to supply the southern market and the commitments of the several agencies are as shown in table I.

Table I

		Commitment (Millions of Kilowatt-Hours)	
	Installation Kva	Firm Power	Secondary Power
Municipalities of Los Angeles, Pasadena, Glendale, and Burbank	495,000	1,773	700
Southern California Edison Co.	165,000	917	
Southern Sierras Power Co.	40,000	114	
	700,000	2,804	700

The total firm and secondary power which these agencies have obligated themselves to take is 3,500,000,000 kilowatt-hours or over 80 per cent of the total load in southern California in 1930. The firm power alone is 2,800,000,000 kilowatt-hours, and because of its magnitude each contracting agency was permitted to assume its obligation progressively over a period of four years, and since the private agencies did not begin taking power until three years after the municipalities began, the total period allowed for absorbing all the firm power is seven years.

Steam-electric power was the only large resource available for southern California, so the price paid for Boulder power was determined by the cost of producing steam-electric power at the load center. As it works out the price paid for falling water at the dam, plus the cost of generating and transmitting the power to the load center, plus the cost of steam standby produces a cost at the load center which is equal to the cost of steam-electric power there.

The power demand in southern California fell off rapidly during the great depression and did not again equal the 1930 demand until 1935. It became unnecessary to construct any of the steam-electric plants which in 1930 were planned to meet the expected growth of load in the period before Colorado River power was made available. The agencies in southern California were obligated to pay for the power when it became available so during this period their hope was for a resumption of load growth so the power could be absorbed. Fortunately, the tide turned in 1934 and the demand for power again began to increase. In 1935 it became equal to the previous maximum demand of the year 1930 and continued to increase until the end of 1937. There was no substantial growth in 1938, but it appears that 1939 again will show an increase in demand.

Present facilities and Colorado River power now contracted for should be capable of meeting a total load of 8,500,000,000 kilowatt-hours. The load of 1938 was 5,450,000,000 kilowatt-hours so if an average annual growth of five per cent can be maintained and complete interconnection is had, the Colorado River power would be absorbed by 1947. (The power would be absorbed in 1943 with a ten per cent rate of growth.)

The southern California market appears to be supplied with ample capacity for some time to come. Any addition required from now on will of necessity have to be provided in steam plants, unless the section of Colorado River below the dam where some 300,000 kw is available subsequently is developed for power purposes.

2. Central Valley Project and the Northern California Power Market

In 1921 the legislature of the State of California, realizing that the future growth of California was dependent in large measure on an adequate water supply, made appropriation to provide for a comprehensive investigation of the water resources of the state. This engineering investigation took place over a nine-year period, was financed by successive appropriations by the legislature, and culminated in a report to the legislature of 1931 on a comprehensive plan for the development of the water resources of California. Certain selected units of this comprehensive plan were recommended for early construction in order to serve those areas where the water problems already had become serious and which therefore required immediate attention. The facilities selected for initial construction in southern California included the Colorado River aqueduct and conservation and flood control works on the Santa Ana River basin. The facilities proposed for northern and central California became known as the Central Valley Project and included the following features:

1. *Features to Employ Sacramento River Water.* Kennett reservoir, located on upper Sacramento River, to be constructed to provide for flood control of the Sacramento River and to supply water to the industrial area on the south side of Suisun Bay, to irrigators in the lower San Joaquin Valley, and to prevent the encroachment into the Sacramento and San Joaquin Rivers of salt water from San Francisco Bay in dry years, and Contra Costa County conduit to convey water to the industrial area south of Suisun Bay and the San Joaquin River pumping system to convey water up San Joaquin Valley for the use of irrigators in the lower part of the valley.
2. *Features to Employ San Joaquin River Water.* Friant reservoir, located on upper San Joaquin River, for the purpose of supplying water to areas in the upper San Joaquin Valley, and Madera Canal, San Joaquin-Kern Canal, and Magunden-Edison pumping system conveying Friant water to points of use.

The cost of the project including a power plant at Kennett was estimated in the 1931 report to be $156,000,000, and the annual cost with state financing was stated to be about $2,800,000 more than the revenue which could be secured from the sale of electricity and water, even after complete utilization was had. The report stated, however, that the flood control and navigation benefits which would be produced would be so substantial that financial participation could be expected from the Federal Government and that if other interests which would be substantially benefited by the project also were assessed, sufficient income might be derived to carry the burden not capable of being carried by revenues from the sale of water and electric energy.

In 1933, the National Industrial Recovery Act was passed by Congress and in the desire to make the Central Valley Project eligible to receive Federal assistance the state legislature passed the Central Valley Project Act of 1933. This legislation authorized the construction of the project and created the Water Project Authority of the State of California to carry out the provisions of the act. The Water Project Authority sought financial assistance from the Federal Government in order to construct the project (then estimated to cost $170,000,000) as a State undertaking but without success, so in 1935 the Central Valley Project finally became authorized as a Federal project under the direction of the Bureau of Reclamation and active construction on some of the units commenced in 1937. The size of many of the units has been modified by the Bureau of Reclamation and to this time no authentic figures have been released by the Bureau regarding the estimated cost of the project, the proposed regimen of the water, the power to be generated, or the revenue required to repay the Federal Government for the reimbursable costs of the projects. All of this was known in the case of the Boulder Canyon project. Moreover, long-time contracts for water and power were executed before actual work began. However, the Central Valley Project is being constructed under the revised Federal policy to expend money on projects which are not self-liquidating, but which nevertheless contain elements of value and which during their construction furnish employment. All that is known now about the economics of the project is that there is an agreement between the Water Project Authority and the Bureau of Reclamation which contemplates that a further contract will be entered into between the same parties which will provide for the operation and maintenance of the project by the Water Project Authority upon its completion and for the assumption by it of the responsibility of reimbursing the United States for the funds advanced for the construction of the project and for securing appropriate contracts with local agencies and interests for the disposal of the water and power to be made available by the project in order to obtain revenues required for repayment.

The principal change which has been made which affects power produced on the project is an increase in the capacity of the proposed Shasta (formerly Kennett) reservoir and power plant. The production of hydroelectric power on this project is incidental to the primary water supply, salinity control, flood control, and navigation objectives and because of this the power does not possess characteristics which are as favorable as a project operated primarily to supply power. The installation provided for at Shasta is 375,000 kva of which 300,000 kva is to be installed at this time. According to reports of the Water Project Authority, the output of the plant will vary from 1,000,000,000 kilowatt-hours in a dry year to 2,000,000,000 kilowatt-hours in a wet year, the average being about 1,500,000,000 kilowatt-hours. There is some uncertainty as to how much of the installed capacity will be dependable. However, it is possible in a plan of operation which co-ordinates Shasta plant with all other generating facilities in the region to render substantially all of the 300,000 kilowatts now being installed dependable in that they can be made to add that amount of load capability to the regional power system without sacrificing the water-supply objectives of the project. For present purpose it is assumed, therefore, that the Shasta plant will supply a dependable increment of 300,000 kilowatts to the systems now serving the northern power market. It is estimated that the Shasta plant will become available in 1943.

The Central Valley Project was uncertain as to the magnitude of facilities and the date of completion until it was adopted as a Federal enterprise. Studies of the characteristics of Shasta power demonstrated that it would require the installation of a large amount of steam-electric power to effect complete absorption of the hydro energy. Furthermore, a market for the energy from Shasta could be prepared to effect its early utilization if steam-electric capacity or equivalent could be installed to meet the increase in demand which would develop prior to 1943, when Shasta becomes available. Such steam-electric plants, together with steam-electric plants already available could be operated at a high rate prior to the advent of Shasta and afterward the operation could be curtailed so Shasta plant could supply the energy which previously had been supplied by burning fuel.

The Water Project Authority in view of its contemplated responsibility for repayment to the United States of Federal funds advanced for construction of Central Valley Project, has concerned itself to see that no program of power development or power supply for northern California is carried out which may in any way hinder or delay the marketing of Shasta power when it becomes available. Pacific Gas and Electric Company, as principal distributor of power in the region, has modified its construction program to comply with this requirement of the Water Project Authority in so far as it could do so without causing the cost of power to be greater than it should be.

The predepression load of northern California reached its maximum in 1931, because that year was extremely dry and greater agricultural use of electricity was had than in 1930. The construction program of the lat 20s was not completed until 1931 when Pacific Gas and Electric Company brought in 71,000 kw of hydroelectric capacity on Mokelumne River and 130,000 kw of steam-electric capacity in San Francisco. This brought the dry year capability of the facilities available to the region, with sufficient capacity held in reserve to insure dependable service, to about 1,000,000 kw and 5,500,000,000 kilowatt-hours.

As mentioned before, the operating agencies in northern California had plans for extensive hydroelectric development during the decade 1930–40. The principal operating companies came under one management in 1930, and it became possible to co-ordinate the combined facilities in a manner which left some of the then-existing capacity available to meet load growth. Immediately thereafter the depression began and the load fell off, so none of the proposed hydroelectric projects were commenced. The Central Valley Project was authorized in 1933 and it became evident that prior to its completion it would be better to meet the growth of load by constructing steam-electric rather than hydroelectric plants—at least to the extent that such a program did not cause the cost of power to be higher than it would be were it to be supplied from other low-cost source.

The load in northern California fell off after 1931, remained at a low level through 1932 and 1933, and began to recover in 1934. In 1935 it became apparent that if the demand continued to increase it would be necessary to have additional capacity in 1937. This capacity was provided by means of a 75,000-kw interconnection with Southern California Edison Company and was followed in 1938 by additional storage facilities and the installation of 54,000 kw of steam-electric capacity in Oakland. Construction of 50,000-kw dual-purpose steam-electric plants at each of the oil refineries at Avon, Martinez, and Oleum is now [1939] under way and is scheduled for completion in 1940. This program will produce a dry year capability in 1940 of over 1,250,000 kw. If an average growth of five per cent is attained after 1937, this capacity should be capable of meeting the load of 1941 but new capacity will be required immediately thereafter. Pacific Gas and Electric Company proposes to bring in another plant of its Feather River project in 1942 because it is the most economic thing to do. Engineers of the Authority concede this, but believe that even though more costly, it is more in the public interest to continue with the construction of steam-electric plants so as to facilitate the absorption of Shasta energy.

All of the new facilities since 1931 are either steam-electric plants or purchased standby capacity (Southern California Edison Company), so a market thus is being prepared to permit the early absorption of the energy to be made available from Shasta plant. The amount of steam-electric capacity which will be available in 1940 in addition to preparing a ready market for the absorption of the energy from Shasta plant also is capable of providing dry-year support not only for all existing hydro plants, but for Shasta and the proposed Feather River plants of the Pacific Gas and Electric Company. It will not be necessary to install any more steam-electric capacity solely to support hydroelectric plants, private or public, until the load in northern California reaches a peak of over 1,750,000 kilowatts, 70 per cent above the load of 1938. However, it may be necessary to add some steam-electric reserve capacity to serve as standby in the southern end of the San Joaquin valley and as support for the close-in hydro supplies there, which in dry years become deficient and which are now being supported through long transmission lines from the generating plants located farther north.

With 1,250,000-kw capability in 1940, an orderly development thereafter predicated on a five per cent average rate of growth will require new capacity about as follows:

1942—68,000 kw on Feather River (or equivalent steam)
1943—300,000 kw at Shasta
1946—80,000 kw on Feather River
1947—38,000 kw on Feather River
75,000 kw of steam in San Joaquin valley

While this program is speculative as to date, from an economic standpoint it is the most desirable sequence of construction. The dry year capability at the end of this period, with some increase in reserves, will be 1,700,000 kw.

It is quite apparent that for some years after 1940, hydroelectric plants will predominate in northern California. There is about 600,000 kw of hydro capacity in addition to that mentioned which can be brought in competitive with steam-electric capacity. Owing to the fact that one-third of this does not require support the amount of steam required will be not to exceed one-half kilowatt for each kilowatt of hydro, thus the program following the hydro program outlined will include a combination of hydro and steam of 900,000 kw divided 600,000 kw to hydro and 300,000 kw to steam.

Power From Federal Multipurpose Projects

The large Federal multipurpose projects essentially are conservation projects constructed to attain one or more of several objectives, namely, flood control, salinity control, navigation improvement, and water supply for irrigation and domestic use. Generation of power on these projects, while important economically in that it can produce revenue to defray part or all of the cost of such projects, is incidental to the other objectives. The cost of the generating facilities themselves is relatively small, so the revenue from the sale of power generally can be made more than sufficient to carry the annual cost of the generating facilities leaving some surplus to meet a portion or all of the cost of the reservoir and other works.

While there is no uniform Federal policy in effect for establishing the cost of firm and secondary power on such projects, the tendency is to allocate to the several functions, portions of the total cost of facilities jointly used for the primary objectives and for power. Such allocations have been and must be somewhat arbitrary even though based on an estimate of the value of each benefit or as an alternative, on the cost of providing the benefit in some other manner. Allocations made to date on Tennessee Valley dams show 60 per cent of the cost of joint facilities to be charged to navigation and flood control and 40 per cent to power. The allocation at Bonneville is 67$^1/_2$ per cent to navigation and 32$^1/_2$ per cent to power.

Up to this time, power has been offered to customers as firm power (continuously available) and secondary power (available only part of the time). The prices set for Bonneville power appear to be somewhat less than the cost of producing power by steam or by other hydro plants, but the price set for Colorado River power will produce a cost at the load center which is substantially equal to the cost of generation in steam-electric plants. Furthermore, the contracts in this case provide for periodic revision of the rates, either upward or downward based on competitive conditions at load centers at the time of revision, so apparently it always was intended that firm Colorado River power at the switchboard should be sold so its cost at the load center just meets the cost of alternate firm power at the same place.

The foregoing method of selling power can produce substantial revenue for Federal multipurpose projects where a very large proportion of the available power is firm. The power to be generated at the Shasta plant of the Central Valley Project, owing to the many water-supply objectives, will not be nearly so firm as the power of Boulder Canyon project or Bonneville. It was estimated that there were nearly 500,000 kw of firm power at Boulder Canyon. From published articles it appears that at Bonneville there is firm power without storage on the upper Columbia River of approximately 200,000 kw and with storage this can be brought to about 500,000 kw. Contrasted to these figures various reports indicate that 30,000 to 75,000 kw of firm power, or 18 per cent to 43 per cent of the average output, may be available at the Shasta plant.

It is obvious that if the sale price is based on firm and secondary power as ordinarily defined, the power revenue of the Central Valley project would not compare favorably with revenue of the adjacent Federal multipurpose projects where the bulk of the power is firm, so the burden of cost of the project will weigh heavily on its water users if it is to be truly self-liquidating, or on the taxpayers of the nation if it is not self-liquidating. However, this situation can be greatly improved if the project is co-ordinated with the existing regional system to create the greatest use of the installed capacity and the maximum market for its energy.

Owing to the diversity of the power available from other plants in the region and to the variation of the demand throughout the year, it is possible so to co-ordinate the Shasta plant with the other generating facilities that most and possibly all of the capacity now being installed can be considered as supplying a dependable increment of capacity to the regional system. This being so, the annual value of that dependable increment of capacity should be the same as the cost of supplying an equal amount of capacity (but no energy) from the most economic alternate source. The annual value of the energy which can be employed would be the same as the cost of the fuel which would be required if the energy were generated in a steam-electric plant. This suggests that Shasta power in the interest of obtaining maximum revenue should not be sold as firm and secondary power as ordinarily defined, but instead should be sold on a two-part rate which employs one unit for dependable capacity and another unit for energy.

It should be recalled that most of the Federal multipurpose projects were inaugurated at the bottom of the depression as a means of stimulating economic recovery through the expenditure of Federal funds. These projects, almost without exception, are not truly self-liquidating, so it is necessary for the Federal Government to contribute to their cost the difference between the total cost and the amount reimbursable by the anticipated revenue from the project.

It is useless at this stage to discuss the merits and justification of this policy because that is water over the dam. The projects were started and one by one they are becoming realities, introducing their products to markets which are not entirely prepared to receive them.

It is in the public interest that these products be utilized as rapidly as possible. Consequently the full co-operation of all of the agencies which can contribute to this end, that is Federal, State, and local governments on the one hand, and distributors of power on the other hand, should be had. Existing distributing agencies should view the situation rationally and should be willing to take the power from the projects, assign all growth of load, that is, requirements for additional capacity—and operate their systems in the interests of creating maximum use of and also maximum value for this power to the extent that this can be done without financial loss.

Such a policy will permit the new generating facilities to be co-ordinated with all of those existing in proximity thereto so as to serve the regional power market and because this co-ordination enhances both the use and value of such power, it will produce maximum revenue in the shortest period of time, thus contributing to the economy of a given project by either reducing the cost of water, if water supply is an objective, or the amount of Federal contribution required to secure other objectives such as flood control, navigation, and others of peculiar local significance. This policy on the part of the power distributing agencies can be adopted without detriment to the consumers because they will pay the same for power as though the agencies had developed their own resources.

Conclusion

What has been presented is a brief review of the development of the production of mingled hydro and steam power in California, an outline of the economic advantages of co-ordinating the operation of a number of hydroelectric and steam-electric plants to supply a regional load, and a showing of the power demand in California with its segregation historically into a northern and a southern market. The two great Federal multipurpose projects, Boulder Canyon project in the south and Central Valley project in the north, profoundly affect their respective markets prior to, during, and immediately following the absorption of the capacity they make available. The agencies in southern California were allowed from six to nine years after signing their contracts in which to prepare to receive Colorado River power, consequently, notwithstanding the intervening depression and consequent lack of growth they are absorbing it in an orderly manner. The picture in northern California is somewhat obscured, but because the facilities which have been provided since the project became certain are steam-electric or standby, a favorable market is being prepared to receive Central Valley power, so with the co-operation of all interested parties, its absorption should present no serious problem by the time it becomes available.

As to the future, Southern California agencies, except for a limited hydro supply on Colorado River below Hoover Dam, generally will have to construct steam-electric plants to provide the capacity to meet growth of load. Northern California is more fortunate in that there are a number of hydroelectric projects which can be developed and which in combination with steam-electric plants will produce mingled power agreeable to the load on a competitive basis.

With orderly power development resumed after absorption of Central Valley power an extensive program of hydroelectric construction is a reasonable expectation, particularly so because of the prevailing low money cost which condition is likely to continue if the easy-money policy of the Government can be maintained. The only thing that would interfere with such a program is a discovery of unlimited supplies of fuel in close proximity to the market, which might be available at a price so low as to more than offset the advantage of low-cost money which is essential to hydro development.

Discussion

For discussion, see page 1077.

Fractional-Slot and Dead-Coil Windings

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1. Introduction

THE expression "fractional-slot windings" is commonly applied to machines where the number of slots is not an integer multiple of the product of the number of poles and phases. Dead-coil windings (D.W.) refer to windings where some of the slots contain wedges instead of coil sides. We shall use the term (F.W.) in this paper to comprise all windings which are not (D.W.). The term (F.W.) therefore comprises both whole-slot as well as fractional-slot windings which contain no dead coils.

The advantages of fractional-slot and dead-coil windings have been long recognized by designers. The following are some of the outstanding advantages:

(*a*). A great increase in the number of possible combinations of slots, poles, and phases and hence a consequent saving in the number and total cost of dies required by a manufacturing concern.

(*b*). A reduction in the magnetic locking and hence a decrease in the magnetic losses and a more smoothly operating machine.

The subject has been discussed by several writers. Tingley[1] seems to be the first to have written about fractional-slot windings. He devised a means of laying out windings by what he calls the L.C.M. (lowest common multiple) method. While his work is summarized in the form of tables, the theory underlying the preparation of these tables is not fully developed. Hence a person using Tingley's method for cases where it is not applicable, would obtain incorrect results and find himself at a loss to explain his error. Thus for an *m*-phase machine (*m* even and >2) Tingley's method does not apply. Moreover, Tingley's tables are incomplete in that they exclude some possible combinations of slots, poles, and phases. Again the tables are worked out for two- and three-phase machines only and do not cover six and twelve phases which are commonly used in rotary converters. Finally, Tingley restricts his treatment to the case where the number of coils is equal to the number of slots (double-layer winding). Riker and Dudley,[2] Seike,[3] and Kauders[4] following Tingley's footsteps elaborated on his tables but not on his method or theory. Sequenz[5] seems to have been the first to develop a theory of fractional-slot windings. Unfortunately, however, his work is a combination of rules of procedure and theoretical analysis. While he generalizes Tingley's treatment to *m*-phases, he excludes some possible combinations of slots, poles, and phases. Neither does Sequenz give a general relation for the possible combinations of slots, poles, and phases. Finally, none of these papers deals with dead-coil windings. They therefore fail to reveal the relation persisting between dead-coil and fractional-slot windings and eliminate several possible combinations of slots, poles, and phases.

The objects of this paper are:

(*a*). To develop the theory underlying *m*-phase fractional-slot and dead-coil windings.

(*b*). To give means of obtaining windings which satisfy the following conditions:

1. The electromotive forces of all the *m*-phases are equal in effective value.
2. The electromotive forces of successive phases are displaced from each other by $2\pi/m$ electrical radians.
3. The winding of each phase is as uniformly distributed over the armature as possible in order to give optimum electromotive force.

(*c*). To give expressions for the chording and distribution factors for fractional-slot and dead-coil windings.

(*d*). To obtain relations for possible combinations of slots, poles, and phases which give windings satisfying the conditions of paragraph *b*.

(*e*). To illustrate the theory developed and give rules of procedure for the designer.

2. nth Roots of Unity

(*a*). Definition

Engineers are familiar with at least two operators, the *j* operator which rotates a number by $\pi/2$ counterclockwise and the Fortescue *a* operator which rotates a vector by $2\pi/3$ counterclockwise. These operators are but special cases of an operator known as an *n*th root of unity. Thus if we have an equation $x^4-1=0$, then by writing $1=e^{j2\pi}$ we have $x^4=e^{j2\pi}$ or

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[1] For all numbered references, see list at end of paper.

$x = e^{j2\pi/4} = e^{j\pi/2} = j$. j is a fourth root of unity which rotates a vector by $2\pi/4$. Again if we have an equation $x^3 - 1 = 0$, then by writing $1 = e^{j2\pi}$ we have $x^3 - e^{j2\pi} = 0$ or $x = e^{j2\pi/3}$ which is Fortescue's *a* operator. It is a third root of unity and rotates a vector by $2\pi/3$ radians.

In general if we want to rotate a vector by $2\pi/n$ radians we multiply it by the operator $e^{j2\pi/n}$ which is an *n*th root of unity.

Now consider the equation

$$x^n - 1 = 0 \tag{1}$$

By writing $1 = e^{j2q\pi}$ where q is any integer we can find all the roots of (1) as follows:

$$x^n = 1 = e^{j2q\pi}$$

$$\mathbf{x}_q = e^{j2q\pi/n} = e^{j(q/n)2\pi} = e^{j\alpha q},$$

$$q = 0, 1, 2 \ldots (n-1) \tag{2}$$

The roots $\mathbf{x}_q$ in equation 2 are known as the *nth roots of unity* and are shown in figure 1 for $n = 6$. The *n*th roots of unity may be *primitive* or *nonprimitive*.

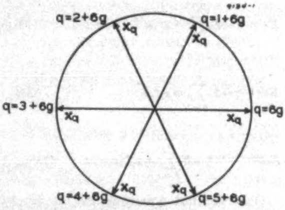

Figure 1. nth roots of unity for n = 6; g is any integer

If in equation 2 q is prime to n, then the *n*th root of unity is called a *primitive nth root* or a *root of order n*. If q is not prime to n then the root is *nonprimitive*. Hereafter the subscript q will be used to denote primitive *n*th roots of unity.

(*b*). PROPERTIES

The primitive *n*th roots of unity have the following properties which follow directly from equation 2:

1. Two integer powers of a primitive *n*th root of unity are equal if and only if the exponents differ by a multiple of *n* or:

If $k = r + gn$ then $\mathbf{x}_q^{\,k} = \mathbf{x}_q^{\,r}$ (3a)

and conversely

If $\mathbf{x}_q^{\,k} = \mathbf{x}_q^{\,r}$ then $k = r + gn$ (3b)

2. For every q in equation 2 which is prime to n, there exists an integer b such that

$$\mathbf{x}_q^{\,b} = e^{j2\pi/n} \tag{4a}$$

where

$$b = (1 + gn)/q \tag{4b}$$

In equation 4*b*, g must be properly chosen so that b is an integer. Thus if $n = 6$ and $q = 5$ then g can be 4, 9, 14, 19 ... etc., and b becomes 5, 11, 17, 23 ... respectively. Ordinarily the smallest possible b is selected because higher values of b simply mean an increase of the argument by a multiple of 2π.

3. All the *n*th roots of unity can be obtained from any primitive *n*th root of unity by raising that primitive root to a proper integer power or:

$$\mathbf{x}_q^{\,a} = \mathbf{x}_r \tag{5a}$$

where

$$a = br \tag{5b}$$

4. All positive-integer powers of a primitive *n*th root of unity between 0 and $(n-1)$ inclusive or between 1 and n inclusive are unequal or:

$\mathbf{x}_q^{\,k} \neq \mathbf{x}_q^{\,r}$ if $0 \leq k < r \leq n-1$ (6)

5. A primitive *n*th root of unity, when raised to integer powers k, will yield unity if and only if k is a multiple of n or:

$\mathbf{x}_q^{\,k} = 1$ if and only if $k = gn$ (7)

6. The smallest positive power to which a primitive *n*th root of unity may be raised to give 1 is n or:

$\mathbf{x}_q^{\,n} = 1$ and $\mathbf{x}_q^{\,n'} \neq 1$ if $1 \leq n' < n$ (8)

7. If $\mathbf{x}_r$ is an *n*th root of unity (primitive or not) then:

(*a*). $-\mathbf{x}_r$ is also an *n*th root of unity when n is even

(*b*). $-\mathbf{x}_r$ is not an *n*th root of unity when n is odd

8. If $\mathbf{x}_q$ is a primitive *n*th root of unity and if n is odd then the numbers $\pm\mathbf{x}_q^{\,kb}$, $k = 0, 1, 2, \ldots (n-1)$ represent all the $(2n)$th roots of unity.

3. The Phase Voltages of Fractional-Slot and Dead-Coil Windings

Let it be required to design a *c*-circuit winding for a $2p$-pole, m-phase, s-slot machine such that when the coils belonging to each phase or each circuit of each phase are equal and are connected in series we obtain a set of m-phase vector voltages which are equal in magnitude and equally displaced from each other by $2\pi/m$.

The following observations are pertinent:

(*a*). If we reduce the fraction s/p to its lowest terms we obtain:

$n/q = s/p$ in its lowest terms (9)

(*b*). From equation 9 we conclude that

$s/n = p/q$ is an integer (10)

(*c*). The angle α_q between slots (the slot pitch) is:

$$\alpha_q = p2\pi/s = (p/s)2\pi = (q/n)2\pi \tag{11}$$

Hence if we number the successive slots in a machine 1, 2, 3, ... s then:

1. Any two slots whose numbers differ by a multiple of n are 2π electrical radians apart and are thus in phase.

2. If n is even then any two slots whose numbers differ by an odd multiple of $n/2$ are π electrical radians apart and are mutually negative.

3. Any two slots whose numbers differ by $b = (1 + gn)/q$ (see equation 4*b*) are $2\pi/n$ electrical radians apart.

These statements can be readily verified by multiplying both sides of (11) by n, $n/2$, or b respectively.

(*d*). From observations *c* it follows that the number s_0' of slots, which are displaced by a multiple of π from any given slot is:

$s_0' = s/n$ if n is odd (12a)

$= 2s/n$ if n is even (12b)

(*e*). In an even-phased machine, any phase r is displaced from phase $r \pm m/2$ by π radians. This means that if m is even then, of two slots which are π apart, one is used for phase r and the other for phase $r \pm m/2$. In other words only half of the s_0' slots equation 12 can be used in any phase of an even-phased machine. We thus have the following relations for slots which are displaced from a given slot by a multiple of π in any given phase:

$s_0 = s/n$ if n and m are both even (13a)

$s_0 = 2s/n$ if n is even and m is odd (13b)

$s_0 = s/n$ if n and m are both odd (13c)

$s_0 = [\![s/2n]\!]$ if n is odd and m is even (13d)

The term $[\![s/2n]\!]$ is called the *bracket function* and is defined as follows:

$[\![x]\!] = x$ if x is an integer

$[\![x]\!] =$ the integer next smaller than x if x is not an integer

Thus $[\![4]\!] = 4$, $[\![3.6]\!] = 3$, $[\![-1.5]\!] = -2$

(*f*). From the foregoing observations and section 2 we conclude that the vector voltages $\mathbf{E}_0$ induced in the several coil sides have phase positions corresponding to the *n*th roots of unity. Moreover, since each coil may be connected in two ways, the vectors representing the induced voltages in the various coil sides may be positive or negative depending upon the mode of connection. Hence the balanced phase voltages $\mathbf{E}$, may be written as follows:

$$\mathbf{E} = \mathbf{E}_0 \sum_{k=1}^{h} \pm \mathbf{x}_q^{\,a_{0k}} \tag{14a$_0$}$$

$$\mathbf{E}e^{j2\pi/m} = \mathbf{E}_0 \sum_{k=1}^{h} \pm \mathbf{x}_q^{\,a_{1k}} \tag{14a$_1$}$$

$$\mathbf{E}e^{j2(2\pi/m)} = \mathbf{E}_0 \sum_{k=1}^{h} \pm \mathbf{x}_q^{\,a_{2k}} \tag{14a$_2$}$$

$$\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots$$

$$\mathbf{E}e^{j(m-1)2\pi/m} = \mathbf{E}_0 \sum_{k=1}^{h} \pm \mathbf{x}_q^{\,a_{(m-1)k}} \tag{14a$_{m-1}$}$$

where h is the number of used slots per phase and a_{jk} are, at present, unknown integers.

Equations 14 are the most general expressions for a balanced m-phase winding. The terms $\mathbf{x}_q^{\,a_{jk}}$ represent the individual voltages, generated in the various coil sides, which are connected in series to make up the winding of any one phase. Our object, henceforth, will be to evaluate the exponents $a_{\mu k}$ which enter into (14). Dividing equation $14a_1$ by equation $14a_0$ we obtain:

$$\epsilon^{j2\pi/m} = \sum_{k=1}^{h} \pm x_q^{a_{1k}} \Big/ \sum_{k=1}^{h} \pm x_q^{a_{0k}} \qquad (15)$$

It can be shown[7] that equation 15 holds only when one or the other of the following conditions is satisfied:

(*a*). n is divisible by m, or

(*b*). When n is odd, m is even and n is divisible by $m/2$.

This means that of all the cases mentioned in equation 13 only those cases which satisfy either condition *a* or condition *b* are possible. Moreover, in equations 13*a* to 13*c* condition *a* must be satisfied while in equation 13*d* condition *b* must be satisfied.

On the basis of the foregoing considerations, windings may be classified as outlined in table I.

Table I. Classification of Machine Windings

Type	n	m	Divisibility	a_0 (See Equations 13)
A	Even	Even	n is divisible by m.	s/n
B	Even	Odd	n is divisible by m.	$2s/n$
C	Odd	Odd	n is divisible by m.	s/n
D	Odd	Even	n is divisible by $m/2$.	$[\![s/2n]\!]$

Hereafter these types of windings will be referred to as types *A*, *B*, *C*, and *D* respectively. We shall find occasion to distinguish between two cases of *D*, namely, when $[\![s/2n]\!]=s/2n$ and when $[\![s/2n]\!]<(s/2n)$. The first type will be characterized as D_1 and the second as D_2. More specifically:

D_1 is a *D*-type winding where s is even
D_2 is a *D*-type winding where s is odd

4. Determination of h in Equations 14

The number of used slots per phase satisfies the following relations for fractional-slot and dead-coil windings respectively:

$$\text{For (F.W.) } h=s/m \qquad (16a)$$

$$\text{For (D.W.) } h=(s-Dm)/m=(s/m)-D \qquad (16b)$$

where D is the number of dead coils per phase.

5. Solution of Equations 14

We shall show how equations 14 may be solved for windings of types *A*, *B*, and *C* on the one hand and for windings of type *D* on the other hand.

(*a*). SOLUTION FOR TYPES A, B, AND C

To show how equations $14a_1$ to $14a_{m-1}$ are obtained from $14a_0$ if n is divisible by m we have the following identity:

$$\epsilon^{j(r/m)2\pi} = \epsilon^{j(2\pi/n)(rn/m)} \qquad (17a)$$

or

$$\epsilon^{j(r/m)2\pi} = x_q^{b(rn/m)} \qquad (17b)$$

Multiplying the right and left sides of (17*b*) respectively by the right and left sides of ($14a_0$) and using successively $r=0, 1, 2, \ldots (m-1)$ we have:

$$E=E_0\sum_{k=1}^{h} \pm x_q^{a_{0k}} \qquad (18a_0)$$

$$E\epsilon^{j2\pi/m}=E_0\sum_{k=1}^{h} \pm x_q^{(a_{0k}+bn/m)} \qquad (18a_1)$$

$$E\epsilon^{j2(2\pi/m)}=E_0\sum_{k=1}^{h} \pm x_q^{(a_{0k}+2bn/m)} \qquad (18a_2)$$

....

$$E\epsilon^{j(m-1)2\pi/m}=E_0\sum_{k=1}^{h} \pm x_q^{[a_{0k}+(m-1)bn/m]} \qquad (18a_{m-1})$$

It must be here emphasized that, once a sign plus or minus has been selected in ($18a_0$) for any one term of the sum that same sign should be used for the corresponding term in all succeeding equations of (18).

(*b*). SOLUTION FOR TYPE D

In this type, since m is even, equations 14 group themselves in pairs. Thus if $E\epsilon^{j\beta_y}$ is the voltage of phase y then $-E\epsilon^{j\beta_y}$ is the voltage of phase $(y \pm m/2)$. In particular the electromotive forces of phases 1 and $[(m/2)+1]$ are (see equations 14):

$$E\epsilon^{j0}=E \qquad (19a_0)$$

$$E\epsilon^{j(m/2)2\pi/m}=E\epsilon^{j\pi}=-E \qquad (19a_{m/2})$$

Since $m/2$ occurs rather frequently in this treatment we shall designate it by one letter μ thus:

$$m/2=\mu \qquad (20)$$

Substituting ($19a_0$) and ($19a_\mu$) in ($14a_0$) and ($14a_\mu$) we have:

$$E=E_0\sum_{k=1}^{h} \pm x_q^{a_{0k}} \qquad (21a)$$

$$-E=E_0\sum_{k=1}^{h} \pm x_q^{a_{\mu k}} \qquad (21b)$$

Now take equations 14 and divide them into two sets taking alternate equations to comprise any one set. Thus the *first set* would consist of equations $14a_0$, $14a_2$, $14a_4$, $\ldots$ $14a_{m-2}$ while the *second set* would comprise equations $14a_1$, $14a_3$, $\ldots$ $14a_\mu$ $\ldots$ $14a_{m-1}$.

The first set, with μ substituted for m, is identical in form with equations 14. Moreover since $\mu=m/2$ therefore μ is a divisor of n. Thus the *first set* is of the same form as equations 18 and may be written as follows:

$$E=E_0\sum_{k=1}^{h} \pm x_q^{a_{0k}} \qquad (22a_0)$$

$$E\epsilon^{j2\pi/\mu}=E_0\sum_{k=1}^{h} \pm x_q^{(a_{0k}+bn/\mu)} \qquad (22a_2$$

....

$$E\epsilon^{j(\mu-1)2\pi/\mu}=E_0\sum_{k=1}^{h} \pm x_q^{[a_{0k}+(\mu-1)bn/\mu]} \qquad (22a_{m-2})$$

As to the *second set* it consists of the following expressions:

$$E\epsilon^{j\pi/\mu}=E_0\sum_{k=1}^{h} \pm x_q^{a_{1k}} \qquad (23a_1)$$

$$E\epsilon^{j3\pi/\mu}=E_0\sum_{k=1}^{h} \pm x_q^{a_{3k}} \qquad (23a_3)$$

....

$$E\epsilon^{j\mu\pi/\mu}=E_0\sum_{k=1}^{h} \pm x_q^{a_{\mu k}} \qquad (23a_\mu)$$

....

$$E\epsilon^{j(m-1)\pi/\mu}=E_0\sum_{k=1}^{h} \pm x_q^{a_{(m-1)k}} \qquad (23a_{m-1})$$

Observe that equation $23a_\mu$ is simply the voltage $-E$. Now rearrange equations 23 starting with $23a_\mu$ and proceeding until you reach equation $23a_{m-1}$. Then take up equations $23a_1$, $23a_3$ $\ldots$ in succession. Equations 23 so rearranged will be the negative of the corresponding equations 23 and may be written as follows:

$$E\epsilon^{j\mu\pi/\mu}=-E=-E_0\sum_{k=1}^{h} \pm x_q^{a_{0k}} \qquad (24a_\mu)$$

$$E\epsilon^{j(\mu+2)\pi/\mu}=-E\epsilon^{j2\pi/\mu}$$

$$=-E_0\sum_{k=1}^{h} \pm x_q^{(a_{0k}+bn/\mu)} \qquad (24a_{\mu+2})$$

....

$$E\epsilon^{j(\mu-2)\pi/\mu}=-E\epsilon^{j(\mu-1)2\pi/\mu}$$

$$=-E_0\sum_{k=1}^{h} \pm x_q^{[a_{0k}+(\mu-1)bn/\mu]} \qquad (24a_{\mu-2})$$

It must be emphasized that, once a sign plus or minus has been selected in ($22a_0$) for any one term of the sum, that same sign should be used for the corresponding term in all the succeeding equations of (22) and (24).

Equations 22 and 24 comprise all the voltage equations of the m phases. Indeed equations $22a_0$ to $22a_{m-2}$ are respectively the voltage equations for odd-numbered phases, namely, 1, 3, 5, … $(m-1)$ and equations $24a_\mu$ to $24a_{\mu-2}$ are respectively the electromotive-force expressions for even-numbered phases, namely, $(1+m/2)$, $(3\pm m/2)$, $(5\pm m/2)$, …$[(m/2)-1]$ respectively.

Equations 18, 22, and 24 state that a balanced m-phase winding may be obtained in a variety of ways depending upon the values of $a_{01}, a_{02}, \ldots a_{0h}$ that are selected. Such a winding, however, may or may not give the optimum effective value E of the phase voltage, nor does it guarantee that s_0 of the coils which are π apart are in series per phase.

6. Optimum Voltage

In order to determine the exponents a_{0k} which give optimum E per phase we proceed in steps as follows:

(*a*). Determination of the Number of Times a Vector Is Repeated in (18a₀) and (22a₀)

Let an m-phase machine comprise c circuits per phase. Then the number of *used slots* per phase which are displaced from a given slot by π or 2π and which therefore yield equal vector voltages is:

$$s_{0u}=c[\![s_0/c]\!] \tag{25}$$

Now the number u of different vectors per phase is:

$$u=n/m \text{ for type } A \tag{26a}$$

$$=n/2m \text{ for type } B \tag{26b}$$

$$=n/m \text{ for type } C \tag{26c}$$

$$=2n/m=n/\mu \text{ for type } D \tag{26d}$$

But h is the total number of vectors per phase. Hence

$$h=us_{0u} \tag{27}$$

Equation 27 holds for both (F.W.) and (D.W.) and states that the terms h in (18), (22), (24) consist of u vectors each of which is repeated s_{0u} times.

In order to find the number of dead coils D per phase we simply substitute the value of h from equation 27 in (16b), solve for D and obtain

$$D=(s/m)-us_{0u} \tag{27a}$$

Making use of these facts, equations 18, 22, and 24 become respectively:

(*a*). *For Types A, B, and C*

$$E=E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{a_{0k}} \tag{28a$_0$}$$

$$E\epsilon^{j2\pi/m}=E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{(a_{0k}+bn/m)} \tag{28a$_1$}$$

$$E\epsilon^{j2(2\pi/m)}=E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{(a_{0k}+2bn/m)} \tag{28a$_2$}$$

……

$$E\epsilon^{j(m-1)2\pi/m}=E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{[a_{0k}+(m-1)bn/m]} \tag{28a$_{m-1}$}$$

(*b*). *For Type D*

$$E=E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{a_{0k}} \tag{29a$_0$}$$

$$E\epsilon^{j2\pi/\mu}=E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{(a_{0k}+bn/\mu)} \tag{29a$_2$}$$

$$E\epsilon^{j4\pi/\mu}=E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{(a_{0k}+2bn/\mu)} \tag{29a$_4$}$$

……

$$E\epsilon^{j(\mu-1)2\pi/\mu}=E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{[a_{0k}+(\mu-1)bn/\mu]} \tag{29a$_{m-2}$}$$

$$E\epsilon^{j\mu\pi/\mu}=-E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{a_{0k}} \tag{30a}$$

$$E\epsilon^{j(\mu+2)\pi/\mu}=-E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{(a_{0k}+bn/\mu)} \tag{30a$_{\mu+2}$}$$

$$E\epsilon^{j(\mu+4)\pi/\mu}=-E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{(a_{0k}+2bn/\mu)} \tag{30a$_{\mu+4}$}$$

……

$$E\epsilon^{j(\mu-2)\pi/\mu}=-E_0s_{0u}\sum_{k=1}^{u}\pm x_q^{[a_{0k}+(\mu-1)bn/\mu]} \tag{30a$_{\mu-2}$}$$

(*b*). Determination of a₀ₖ

In order to determine the a_{0k}'s in equations $28a_0$ and $29a_0$ which give optimum E we make use of theorems IIa and IIb which state that if we have equations of the form (28) or (29) then the values of a_{0k} which give an optimum E are given by (70) and (77) of the appendix respectively or:

(*a and b*). *For Types A and B*

$$E=E_0s_{0u}\sum_{k=1}^{u}x_q^{kb} \tag{31a$_0$}$$

$$E\epsilon^{j2\pi/m}=x_q^{bn/m}E_0s_{0u}\sum_{k=1}^{u}x_q^{kb} \tag{31a$_1$}$$

$$E\epsilon^{j4\pi/m}=x_q^{2bn/m}E_0s_{0u}\sum_{k=1}^{u}x_q^{kb} \tag{31a$_2$}$$

……

$$E\epsilon^{j(m-1)2\pi/m}=x_q^{(m-1)bn/m}E_0s_{0u}\sum_{k=1}^{u}x_q^{kb} \tag{31a$_{m-1}$}$$

(*c*). *For Type C*

$$E=E_0s_{0u}\left[\sum_{k=1}^{\frac12(u-1)}x_q^{kb}-\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{32a$_0$}$$

$$E\epsilon^{j2\pi/m}=x_q^{(bn/m)}E_0s_{0u}\left[\sum_{k=1}^{\frac12(u-1)}x_q^{kb}-\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{32a$_1$}$$

$$E\epsilon^{j4\pi/m}=x_q^{(2bn/m)}E_0s_{0u}\left[\sum_{k=1}^{\frac12(u-1)}x_q^{kb}-\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{32a$_2$}$$

……

$$E\epsilon^{j(m-1)2\pi/m}=x_q^{(m-1)bn/m}E_0s_{0u}\left[\sum_{k=1}^{\frac12(u-1)}x_q^{kb}-\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{32a$_{m-1}$}$$

(*d*). *For Type D.* For phases 1, 3, 5, … $(m-1)$,

$$E=E_0s_{0u}\left[\sum_{k=1}^{\frac12(u-1)}x_q^{kb}-\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{33a$_0$}$$

$$E\epsilon^{j2\pi/\mu}=x_q^{bn/\mu}E_0s_{0u}\left[\sum_{k=1}^{\frac12(u-1)}x_q^{kb}-\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{33a$_2$}$$

$$E\epsilon^{j4\pi/\mu}=x_q^{2bn/\mu}E_0s_{0u}\left[\sum_{k=1}^{\frac12(u-1)}x_q^{kb}-\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{33a$_4$}$$

……

$$E\epsilon^{j(\mu-1)2\pi/\mu}=x_q^{(\mu-1)bn/\mu}E_0s_{0u}\left[\sum_{k=1}^{\frac12(u-1)}x_q^{kb}-\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{33a$_{m-2}$}$$

and for phases 2, 4, 6, … m,

$$E\epsilon^{j\mu\pi/\mu}=E_0s_{0u}\left[-\sum_{k=1}^{\frac12(u-1)}x_q^{kb}+\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{34a$_\mu$}$$

$$E\epsilon^{j(\mu+2)\pi/\mu}=x_q^{bn/\mu}E_0s_{0u}\left[-\sum_{k=1}^{\frac12(u-1)}x_q^{kb}+\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{34a$_{\mu+2}$}$$

$$E\epsilon^{j(\mu+4)\pi/\mu}=x_q^{2bn/\mu}E_0s_{0u}\left[-\sum_{k=1}^{\frac12(u-1)}x_q^{kb}+\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{34a$_{\mu+4}$}$$

……

$$E\epsilon^{j(\mu-2)\pi/\mu}=x_q^{(\mu-1)bn/\mu}E_0s_{0u}\left[-\sum_{k=1}^{\frac12(u-1)}x_q^{kb}+\sum_{k=\frac12(n+1)}^{\frac12(n+u)}x_q^{kb}\right] \tag{34a$_{\mu-2}$}$$

7. The Winding Layout

(*a*). Phase 1

Equations 31 to 34 are expressions for the electromotive forces per phase in an m-phase machine. Their utilization in the actual layout of the winding involves their interpretation in terms of slots on the machine. To do this we simply utilize the facts cited in observation *c* of section 3 and proceed as follows:

(*a*). Number the slots of the machine consecutively 1, 2, 3, . . . *s*.

(*b*). Lay out the winding for phase 1 only. This means that we interpret equations a_0 only.

(*c*). Since ($32a_0$) and ($33a_0$) are identical we need only consider equations $31a_0$ and $32a_0$ and in this way cover all the four types of windings.

(*d*). Write equations $31a_0$ and $32a_0$ as follows:

$$s_0\sum_{k=1}^{u} x_q^{kb} = \sum_{k=1}^{u} x_q^{kb} + \sum_{k=1}^{u} x_q^{kb} + \ldots + \sum_{k=1}^{u} x_q^{kb} \qquad (35a)$$

$$\begin{aligned} s_0\left[\sum_{k=1}^{\frac{1}{2}(u-1)} x_q^{kb} - \sum_{k=\frac{1}{2}(n+1)}^{\frac{1}{2}(n+u)} x_q^{kb}\right] = {} & \left[\sum_{k=1}^{\frac{1}{2}(u-1)} x_q^{kb} - \sum_{k=\frac{1}{2}(n+1)}^{\frac{1}{2}(n+u)} x_q^{kb}\right] + \\ & \left[\sum_{k=1}^{\frac{1}{2}(u-1)} x_q^{kb} - \sum_{k=\frac{1}{2}(n+1)}^{\frac{1}{2}(n+u)} x_q^{kb}\right] + \\ & \ldots\ldots\ldots\ldots\ldots\ldots\ldots + \\ & \left[\sum_{k=1}^{\frac{1}{2}(u-1)} x_q^{kb} - \sum_{k=\frac{1}{2}(n+1)}^{\frac{1}{2}(n+u)} x_q^{kb}\right] \end{aligned} \qquad (35b)$$

where (35*a*) holds for types *A* and *B* while (35*b*) is true for types *C* and *D*.

(*e*). All the sums to the right of (35*a*) or (35*b*) are identical and there exists s_0 of them.

(*f*). For further discussion of (35) we shall consider types *A*, *B*, and types *C*, *D*, separately.

1. TYPES A AND B

Let the electromotive forces represented by the first sum to the right of (35*a*) be produced by the coil sides which occupy slots *b*, 2*b*, . . . *ub*. Now observe that:

(*a*). All the sums to the right of (35*a*) are identical. Hence corresponding terms in all sums represent electrically identical slots.

(*b*). Slots which are electrically identical differ from each other by ($n/2$).

(*c*). From section 3, part *e*, we conclude that if *m* is even (type *A*) only half of the electrically identical slots (those which are *n* apart) are used for any one phase, the other half being used for the electrically opposite phase, while if *m* is odd (type *B*) all the electrically identical slots (those which are $^1/_2n$ apart) are used for one phase.

Table II. Slot Numbers in Which Top Coil Sides of Phase 1 Are Placed. This Table Is Applicable to Windings of Type A

	Slot Numbers for Phase 1					
k	$kb=c_1$	$kb+n=c_2$	$kb+2n=c_3$	$kb+3n=c_4$	****	$kb+(s_0-1)n=c_{s_0}$
1	b	$b+n$	$b+2n$	$b+3n$	****	$b+(s_0-1)n$
2	$2b$	$2b+n$	$2b+2n$	$2b+3n$	****	$2b+(s_0-1)n$
3	$3b$	$3b+n$	$3b+2n$	$3b+3n$	****	$3b+(s_0-1)n$
*	**	**	**	**	****	**
u	ub	$ub+n$	$ub+2n$	$ub+3n$	****	$ub+(s_0-1)n$

See notes table IV.

From these facts we conclude that the coil sides belonging to the first phase occupy slots:

$$(b+rn), (2b+rn) \ldots\ldots (ub+rn) \text{ in type } A \qquad (36a)$$

$$(b+rn/2), (2b+rn/2) \ldots\ldots (ub+rn/2) \text{ in type } B \qquad (36b)$$

$$\text{where } 0 \leq r \leq s_0-1,\ n \text{ is even} \qquad (36c)$$

It must be observed that if two coil sides are an odd multiple of π or ($n/2$) apart then they would be equivalent only if the connections are reversed. Hence alternate coil sides in (36*b*) must be reversed. This is shown by making the slot number negative in the following winding tables.

2. TYPES C AND D

Let the electromotive forces represented by the first sum to the right of (35*b*) be produced by the coil sides which occupy slots *b*, 2*b*, . . . $^1/_2(u-1)b$, $^1/_2(n+1)b$, $^1/_2(n+3)b$, . . . $^1/_2(n+u)b$. Now observe that:

(*a*). All the sums to the right of (35*b*) are identical. Hence corresponding terms in all sums represent electrically identical slots.

(*b*). Slots which are electrically identical differ from each other by *n*.

(*c*). From section 3, part *e*, we conclude that if *m* is odd (type *C*) all electrically identical slots (those which are *n* apart) are used for one phase. On the other hand, if *m* is even (type *D*) only half of the electrically identical slots are used for one phase while the other half are used for the electrically opposite phase (see equations 33 and 34).

(*d*). From remark (*c*) the (s/n) electrically identical slots in type *D* must be even in order that they may be divided equally between two mutually opposite phases. This means that *s* must be even or the winding must be of type D_1. If *s* is odd (type D_2) then one slot among those which are electrically identical must be dead. The remaining slots are even in number and may be therefore equally split between two mutually opposite phases. From these facts we conclude that:

1. The coil sides belonging to phase 1 in type *C* occupy slots number

$$\left.\begin{array}{l} (b+rn), (2b+rn), \ldots [^1/_2(u-1)b+rn], \\ \quad [^1/_2(n+1)b+rn], [^1/_2(n+3)b+rn], \ldots \\ \qquad [^1/_2(n+u)b+rn], 0 \leq r \leq (s_0-1) \end{array}\right\} \qquad (37)$$

2. The coil sides belonging to phase 1 and ($\mu+1$) in type *D* occupy slots number

$$\left.\begin{array}{l} (b+rn), (2b+rn), \ldots [^1/_2(u-1)b+rn], \\ \quad [^1/_2(n+1)b+rn], [^1/_2(n+3)b+rn], \ldots \\ \quad [^1/_2(n+u)b+rn], 0 \leq r \leq (s/n)-1 \end{array}\right\} \qquad (38)$$

It must be observed that the negative sign in (35*b*) simply means reversed connection of the coil side. Hence all coil sides occupying slots $[^1/_2(n+1)b+rn]$ to $[^1/_2(n+u)b+rn]$ in (37) must be connected in reverse. This is shown by marking the slot numbers negative in the accompanying tables.

These considerations enable us to construct tables II, III, IV, V, V*a*, and V*b* for slot numbers carrying coil sides belonging to phase 1.

(*b*). PHASES 2 TO m

Tables II to V give the winding layout for phase 1 or 1 and ($\mu+1$) only. In order to obtain the slot numbers corresponding to any other phase we make use of equations 31 and 32 which state that in types *A*, *B*, and *C* any phase is obtained from phase 1 by merely adding multiples of (bn/m) to the slot numbers of phase 1 and leaving coil connections unchanged. More particularly, in order to obtain the slot numbers for phase *y* add, to the numbers of phase 1, the number $(y-1)bn/m$. Thus for any column c_r the slot numbers for phase *y* in tables II, III, and IV are:

$$s_y = [c_r + (y-1)bn/m] \qquad (39)$$

For windings of type *D*, the slot numbers for any odd-numbered phase (see equations 33) are obtained by adding multiples of (bn/μ) to the slot numbers in phase 1, while the slot numbers for any even-numbered phase (see equations 34)

Table III. Slot Numbers in Which Top Coil Sides of Phase 1 Are Placed. This Table Is Applicable to Windings of Type B

	Slot Numbers for Phase 1						
k	$kb=c_1$	$(kb+n/2)=c_2$	$(kb+n)=c_3$	$(kb+3n/2)=c_4$	$(kb+2n)=c_5$	***	$kb+(s_0-1)n/2=c_{s_0}$
1	b	$(b+n/2)-$	$b+n$	$(b+3n/2)-$	$b+2n$	***	$[b+(s_0-1)n/2]-$
2	$2b$	$(2b+n/2)-$	$2b+n$	$(2b+3n/2)-$	$2b+2n$	***	$[2b+(s_0-1)n/2]-$
3	$3b$	$(3b+n/2)-$	$3b+n$	$(3b+3n/2)-$	$3b+2n$	***	$[3b+(s_0-1)n/2]-$
*	*	**	**	***	***	***	********
u	ub	$(ub+n/2)-$	$ub+n$	$(ub+3n/2)-$	$ub+2n$	***	$[ub+(s_0-1)n/2]-$

See notes, table IV.

Table IV. Slot Numbers in Which Top Coil Sides of Phase 1 Are Placed. This Table Is Applicable to Windings of Type C

	Slot Numbers for Phase 1				
k	$kb=c_1$	$kb+n=c_2$	$kb+2n=c_3$	******	$kb+(s_0-1)n=c_{s_0}$
1	b	$b+n$	$b+2n$	******	$b+(s_0-1)n$
2	$2b$	$2b+n$	$2b+2n$	******	$2b+(s_0-1)n$
*	*****	****************	****************	******	**********************
$^1/_2(u-1)$	$^1/_2(u-1)b$	$^1/_2(u-1)b+n$	$^1/_2(u-1)b+2n$	******	$^1/_2(u-1)b+(s_0-1)n$
$^1/_2(n+1)$	$^1/_2(n+1)b$	$[^1/_2(n+1)b+n]-$	$[^1/_2(n+1)b+2n]-$	******	$[^1/_2(n+1)b+(s_0-1)n]-$
$^1/_2(n+3)$	$^1/_2(n+3)b$	$[^1/_2(n+3)b+n]-$	$[^1/_2(n+3)b+2n]-$	******	$[^1/_2(n+3)b+(s_0-1)n]-$
*	******	****************	****************	******	**********************
$^1/_2(n+u)$	$^1/_2(n+u)b$	$[^1/_2(n+u)b+n]-$	$[^1/_2(n+u)b+2n]-$	******	$[^1/_2(n+u)b+(s_0-1)n]-$

NOTE 1. If any number in the table is greater than s, then s or a multiple thereof must be subtracted from that number.

NOTE 2. The above slot numbers represent the slots in which the top side of a coil is placed. The bottom side of the same coil is placed in the slot number which meets the designer's requirement as to winding pitch.

NOTE 3. The negative sign following the slot number signifies that the coils in these slots are reverse connected.

Table V. Slot Numbers in Which Top Coil Sides of Phase 1 and ($\mu+1$) Are Placed. This Table Is Applicable to Windings of Type D

	Slots for Phases 1 and $\mu+1$				
k	$kb=c_1$	$kb+n=c_2$	$kb+2n=c_3$	******	$kb+[(s/n)-1]n=c_{s/n}$
1	b	$b+n$	$b+2n$	******	$b+[(s/n)-1]n$
2	$2b$	$2b+n$	$2b+2n$	******	$2b+[(s/n)-1]n$
*	**	*************	**************	******	**********************
$^1/_2(u-1)$	$^1/_2(u-1)b$	$^1/_2(u-1)b+n$	$^1/_2(u-1)b+2n$	******	$^1/_2(u-1)b+[(s/n)-1]n$
$^1/_2(n+1)$	$^1/_2(n+1)b$	$^1/_2(n+1)b+n$	$^1/_2(n+1)b+2n$	******	$^1/_2(n+1)b+[(s/n)-1]n$
$^1/_2(n+3)$	$^1/_2(n+3)b$	$^1/_2(n+3)b+n$	$^1/_2(n+3)b+2n$	******	$^1/_2(n+3)b+[(s/n)-1]n$
*	**	*************	**************	******	**********************
$^1/_2(n+u)$	$^1/_2(n+u)b$	$^1/_2(n+u)b+n$	$^1/_2(n+u)b+2n$	******	$^1/_2(n+u)b+[(s/n)-1]n$

NOTE 1. Notes 1 and 2, table IV, are applicable to table V.

NOTE 2. The number of columns in table V may be even or odd. If even, then the winding is type D_1. If odd, the winding is of type D_2 and one slot in each row must be left dead, thus leaving an even number of used slots in each row. Just what slot is left dead is a matter which the designer may himself determine.

NOTE 3. Half of the used slots in each row of the above table belongs to phase 1 and the other half to phase $\mu+1$. Just how to divide the slots in each row between the two phases is again optional with the designer.

NOTE 4. In connecting coil sides for phase 1 use direct connection for slot numbers occurring in the upper half of the table and reverse connection for slot numbers occurring in the lower half of the table. Use contrary connections for phase $\mu+1$.

NOTE 5. Having thus selected the slots which go into phases 1 and ($\mu+1$) with their proper signs construct two separate tables, the first for phase 1 and the second for phase ($\mu+1$). For example assume that the last column of table V is occupied by dead coils in case s/n is odd and that alternate columns are used for phases 1 and $\mu+1$. We then have tables V*a* and V*b*.

Table Va. Slot Numbers in Which the Top Coil Sides of Phase 1 Are Placed

	Slots for Phase 1				
k	$kb=c_1'$	$kb+2n=c_2'$	$kb+4n=c_3'$	******	$kb+2(s_0-1)n=c_{s_0}'$
1	b	$b+2n$	$b+4n$	******	$b+2(s_0-1)n$
2	$2b$	$2b+2n$	$2b+4n$	******	$2b+2(s_0-1)n$
*	**	*********	*********	******	**********************
$^1/_2(u-1)$	$^1/_2(u-1)b$	$^1/_2(u-1)b+2n$	$^1/_2(u-1)b+4n$	******	$^1/_2(u-1)b+2(s_0-1)n$
$^1/_2(n+1)$	$^1/_2(n+1)b-$	$[^1/_2(n+1)b+2n]-$	$[^1/_2(n+1)b+4n]-$	******	$[^1/_2(n+1)b+2(s_0-1)n]-$
$^1/_2(n+3)$	$^1/_2(n+3)b-$	$[^1/_2(n+3)b+2n]-$	$[^1/_2(n+3)b+4n]-$	******	$[^1/_2(n+3)b+2(s_0-1)n]-$
*	**	*********	*********	******	**********************
$^1/_2(n+u)$	$^1/_2(n+u)b-$	$[^1/_2(n+u)b+2n]-$	$[^1/_2(n+u)b+4n]-$	******	$[^1/_2(n+u)b+2(s_0-1)n]-$

See note, table V*b*.

are obtained by adding (bn/μ) to the slot numbers of phase ($\mu+1$). In both cases the mode of connection is unchanged. Thus for any column c_r' or c_r'' in tables V*a* and V*b* the slot numbers for phases ($2y+1$) and ($2y+1\pm\mu$) are respectively

$$s_{2y+1}=[c_r'+ybn/\mu] \qquad (40a)$$

$$s_{2y+1\pm\mu}=[c_r''+y(bn/\mu)] \qquad (40b)$$

8. Winding Comprising c Circuits

If the winding is to comprise c circuits then we have the following general principles to consider:

1. All slot numbers occurring in any one row of tables II to IV and V*a* to V*b* are electrically identical.

2. In order that the c circuits shall be balanced it is essential that each circuit comprise the same number of electrically identical coil sides.

3. Hence in splitting the winding into various circuits it is necessary to include in each circuit the same number of coil sides from any one row (tables II–IV and V*a*, V*b*). More specifically let us assume that the winding is to comprise c circuits. Then each row will furnish $[\![s_0/c]\!]$ coil sides to each circuit. Hence the total number of coil sides required for all the winding (see equation 25) is:

$$s_{0u}=c[\![s_0/c]\!] \qquad (41)$$

Now it sometimes happens that s_0/c is not an integer. In this case the remaining slots of any one table are occupied by dead coils Thus let s_d be the number of dead coils in each row then

$$s_d=s_0-c[\![s_0/c]\!] \qquad (42)$$

4. From the foregoing consideration it is obvious that only machines of types *A*, *B*, *C*, and D_1 in which s_0/c is an integer yield (F.W.). All others give (D.W.).

Just how the distribution of dead coils is to be carried out or in which way the winding is to be divided into the various circuits is a matter which has to be left to the discretion of the designer. The only caution being that given in paragraph 3 above.

9. Chording Factor

Let k_{WN} be the chording (winding pitch) factor for the *N*th harmonic. We

Table Vb. Slot Numbers in Which the Top Coil Sides of Phase ($\mu+1$) Are Placed

	Slots for Phase $\mu+1$				
k	$kb+n=c_1''$	$kb+3n=c_2''$	$kb+5n=c_3''$	******	$kb+(2s_0-1)n=c_{s_0}''$
1	$(b+n)-$	$(b+3n)-$	$(b+5n)-$	******	$[b+(2s_0-1)n]-$
2	$(2b+n)-$	$(2b+3n)-$	$(2b+5n)-$	******	$[2b+(2s_0-1)n]-$
*	*******	********	*******	******	***************
$^1/_2(u-1)$	$[^1/_2(u-1)b+n]-$	$[^1/_2(u-1)b+3n]-$	$[^1/_2(u-1)b+5n]-$	******	$[^1/_2(u-1)b+(2s_0-1)n]-$
$^1/_2(n+1)$	$^1/_2(n+1)b+n$	$^1/_2(n+1)b+3n$	$^1/_2(n+1)b+5n$	******	$^1/_2(n+1)b+(2s_0-1)n$
$^1/_2(n+3)$	$^1/_2(n+3)b+n$	$^1/_2(n+3)b+3n$	$^1/_2(n+3)b+5n$	******	$^1/_2(n+3)b+(2s_0-1)n$
***	*******	********	*******	******	***************
$^1/_2(n+u)$	$^1/_2(n+u)b+n$	$^1/_2(n+u)b+3n$	$^1/_2(n+u)b+5n$	******	$^1/_2(n+u)b+(2s_0-1)n$

NOTE: Tables V*a* and V*b* are to be understood as being simply one choice. Many other choices of splitting table V present themselves (see notes 2, 3, and 5, table V).

shall compute this factor for (F.W.) and (D.W.) windings.

Let ψ be the winding pitch and let E_{cN} be the effective voltage induced per coil due to the Nth harmonic. Let $E_{c'N}$ be the algebraic sum of the Nth harmonic electromotive forces induced in the two coil sides. Then by definition

$$k_{WN}=E_{cN}/E_{c'N} \tag{43}$$

But

$$E_{c'N}=2E_{0N} \tag{44}$$

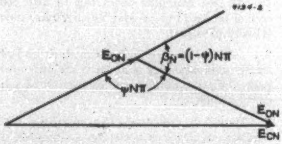

Figure 2. Vector voltages generated in two coil sides and their vector sum

As to E_{cN}, it may be computed by the use of figure 2 as follows:

$$E_{cN}^2=2E_{0N}^2-2E_{0N}^2\cos\psi N\pi \tag{45a}$$

$$=2E_{0N}^2(1-\cos\psi N\pi) \tag{45b}$$

But

$$1-\cos\theta=2\sin^2(\theta/2) \tag{46}$$

The substitution of (46) in (45) gives:

$$E_{cN}^2=4E_{0N}^2\sin^2(\psi N\pi/2) \tag{47a}$$

or

$$E_{cN}=2E_{0N}\,|\sin(\psi N\pi/2)| \tag{47b}$$

The substitution of (44) and (47*b*) in (43) gives

$$k_{WN}=|\sin(\psi N\pi/2)| \tag{48}$$

Equation 48 gives the chording factor for the Nth harmonic. This expression can be used effectively in eliminating any harmonic from the voltage equation. Thus in order to eliminate the Nth harmonic k_{WN} must be zero. This means

$$\psi N\pi/2=\sigma\pi \tag{49a}$$

or

$$\psi=2\sigma/N \tag{49b}$$

The Nth harmonic is eliminated by making the winding pitch $\psi=2\sigma/N$ where σ is such an integer that $2\sigma<N$. For example in order to eliminate the third or fifth harmonic make $\psi=2/3$ or $\psi=4/5$ respectively.

10. The Distribution (Slot) Factor

Let E_{aN} be the algebraic sum of the Nth harmonic electromotive forces generated in the coil sides of any one phase. Let E_{vN} be the magnitude of the vector sum of the same Nth harmonic electromotive forces. Then by definition:

$$k_{sN}=E_{vN}/E_{aN} \tag{50}$$

We shall develop the distribution factors for types *A* and *B* and types *C* and *D* separately.

(*a*). DISTRIBUTION FACTOR FOR TYPES A AND B

In equations 31 there are $(s_{0u}u)$ coil sides any one of which may be represented by the vector $E_{0N}x_e^{kbN}=E_{0N}\epsilon^{jkN2\pi/n}$ where k varies from 1 to u. Thus the sum of the moduli of these vectors is:

$$E_{aN}=(s_{0u}u)E_{0N} \tag{51}$$

The vector sum is:

$$E_{vN}=E_{0N}s_{0u}[\epsilon^{jN(2\pi/n)}+\epsilon^{j2N(2\pi/n)}+\epsilon^{j3N(2\pi/n)}+\ldots+\epsilon^{juN(2\pi/n)}] \tag{52a}$$

$$E_{vN}=E_{0N}s_{0u}[(\epsilon^{jN2\pi/n})+(\epsilon^{jN2\pi/n})^2+(\epsilon^{jN2\pi/n})^3+\ldots+(\epsilon^{jN2\pi/n})^u] \tag{52b}$$

Now observe that the terms within brackets in (52*b*) constitute a geometric series. Hence the expression for the sum of a geometric series may be used to represent the quantity within brackets and we have:

$$E_{vN}=E_{0N}s_{0u}[(1-\epsilon^{juN2\pi/n})/(1-\epsilon^{jN2\pi/n})]\times\epsilon^{jN2\pi/n} \tag{53}$$

Multiply the right and left sides of (53) respectively by their conjugates and obtain:

$$E_{vN}^2=E_{0N}^2s_{0u}^2[(1-\epsilon^{juN2\pi/n})\times(1-\epsilon^{-juN2\pi/n})]/[1-\epsilon^{jN2\pi/n}]\times[1-\epsilon^{-jN2\pi/n}] \tag{54a}$$

$$E_{vN}^2=E_{0N}^2s_{0u}^2[1-\epsilon^{-juN2\pi/n}-\epsilon^{juN2\pi/n}+1]/[1-\epsilon^{-jN2\pi/n}-\epsilon^{jN2\pi/n}+1] \tag{54b}$$

$$E_{vN}^2=E_{0N}^2s_{0u}^2[1-\cos(uN2\pi/n)]\div[1-\cos(N2\pi/n)] \tag{54c}$$

The use of relation (46) reduces equation 54*c* to the following expression:

$$E_{vN}^2=E_{0N}^2s_{0u}^2[2\sin^2(uN2\pi/n)]\div[2\sin^2(N2\pi/n)] \tag{55a}$$

or

$$E_{vN}=|E_{0N}s_{0u}\sin(uN2\pi/n)/\sin(N2\pi/n)| \tag{55b}$$

The substitution of (55) and (51) in (50) yields:

$$k_{sN}=\left|\frac{\sin(uN2\pi/n)}{u\sin(N2\pi/n)}\right| \tag{56}$$

which is the distribution factor for the Nth harmonic in windings of types *A* and *B*.

(*b*). DISTRIBUTION FACTOR FOR TYPES C AND D

In equations 32 and 33 there are $(s_{0u}u)$ coil sides any one of which may be represented by the vector $E_{0N}x_e^{kbN}=E_{0N}\times\epsilon^{jkN2\pi/n}$ where k varies from 1 to $^1/_2(u-1)$ and from $^1/_2(n+1)$ to $^1/_2(n+u)$. Thus the sum of the moduli of these vectors is:

$$E_{aN}=(s_{0u}u)E_{0N} \tag{57}$$

The vector sum is given by equation 78 of theorem II*b* of the appendix or:

$$E_{vN}=E_{0N}s_{0u}\sum_{r=1}^{u}(\epsilon^{jN2\pi/2n})^r \tag{58a}$$

$$=E_{0N}s_{0u}[\epsilon^{jN\pi/n}+(\epsilon^{jN\pi/n})^2+(\epsilon^{jN\pi/n})^3+\ldots+(\epsilon^{jN\pi/n})^u] \tag{58b}$$

since (58*b*) is a geometric series it may be written as follows:

$$E_{vN}=E_{0N}s_{0u}[(1-\epsilon^{juN\pi/n})/(1-\epsilon^{jN\pi/n})]\times\epsilon^{jN\pi/n} \tag{59}$$

Multiply the right and left sides of (59) by their conjugates and obtain:

$$E_{vN}^2=E_{0N}^2s_{0u}^2[1-\cos(uN\pi/n)]\div[1-\cos(N\pi/n)] \tag{60}$$

The use of relation (46) reduces equation 60 to the following expression:

$$E_{vN}=|E_{0N}s_{0u}\sin(uN\pi/n)/\sin(N\pi/n)| \tag{61}$$

The substitution of (61) and (57) in (50) yields

$$k_{sN}=\left|\frac{\sin(uN\pi/n)}{u\sin(N\pi/n)}\right| \tag{62}$$

which is the distribution factor for the Nth harmonic in windings of types *C* and *D*.

11. Possible Combinations of s, p, and m

In practical applications a designer is faced with the problem of ascertaining what combinations of slots, poles, and phases will give a balanced m-phase winding. More particularly the purchaser specifies the number of phases m, the frequency, and the speed of the machine. Thus m and p are specified and it is required to find the number of slots s which yield a balanced winding. We give below a method of determining s.

Referring to table I we observe that:

(*a*). In windings of types *A*, *B*, and *C*, n must be divisible by m.

(*b*). In windings of type *D*, n must be divisible by $m/2$. Moreover since n is odd, m must be the double of an odd integer.

(*c*). If m is odd then we have types *B* or *C*. In each case n must be divisible by m.

(*d*). If m is even then we have types A or D. Here we distinguish between the following two cases:

1. m is the double of an odd integer ($m=4k+2$). This may give types A or D and in either case n is divisible by $m/2$.
2. m is the double of an even integer ($m=4k$). This always leads to type A where n is divisible by m.

The above observations may be summarized in tabular form as table VI. Now let m_k be the different prime factors of m in cases 1 and 3 and of $m/2$ in case 2. Then:

$$m=\prod_r m_r^{a_r} \text{ in cases 1 and 3} \tag{63a}$$

$$m=2\prod_r m_r^{a_r} \text{ in case 2} \tag{63b}$$

$$\text{where } a_r>0 \text{ and } m_r\neq 1 \tag{63c}$$

From equations 63 and the conditions of divisibility of table VI we conclude that in all three cases n must be divisible by $\prod_r m_r^{a_r}$. But, by theorem III of the appendix, the only machines which can be wound are those having slots s such that:

$$s=HG \tag{64a}$$

$$\text{where } H=mp_1 \text{ in cases 1 and 3} \tag{64b}$$

$$=(m/2)p_1 \text{ in case 2} \tag{64c}$$

p_1 is defined by equation 83 of the appendix and G is any integer.

These considerations lead to the possible combinations of slots, poles, and phases of table VII.

Table VI. Relations Between n and m in the Various Types of Windings

Case	m	Possible Types of Winding	Divisibility Conditions
1	$2k+1$	B or C	n is divisible by m
2	$4k+2$	A or D	n is divisible by $m/2$
3	$4k$	A	n is divisible by m

Table VII. Possible Combinations of Slots, Poles, and Phases

No. of Poles 2p	Number of Phases m														
	2	3	4	5	7	8	9	11	12	13	15	16	20	24	28
		6		10	14		18	22		26	30				
2	1	3	4	5	7	8	9	11	12	13	15	16	20	24	28
4	1	3	8	5	7	16	9	11	24	13	15	32	40	48	56
6	1	9	4	5	7	8	27	11	36	13	45	16	20	72	28
8	1	3	16	5	7	32	9	11	48	13	15	64	80	96	112
10	1	3	4	25	7	8	9	11	12	13	75	16	100	24	28
12	1	9	8	5	7	16	27	11	72	13	45	32	40	144	56
14	1	3	4	5	49	8	9	11	12	13	15	16	20	24	196
16	1	3	32	5	7	64	9	11	96	13	15	128	160	192	224
18	1	27	4	5	7	8	81	11	108	13	135	16	20	216	28
20	1	3	8	25	7	16	9	11	24	13	75	32	200	48	56
22	1	3	4	5	7	8	9	121	12	13	15	16	20	24	28
24	1	9	16	5	7	32	27	11	144	13	45	64	80	288	112
26	1	3	4	5	7	8	9	11	12	169	15	16	20	24	28
28	1	3	8	5	49	16	9	11	24	13	15	32	40	48	392
30	1	9	4	25	7	8	27	11	36	13	225	16	100	72	28
32	1	3	64	5	7	128	9	11	192	13	15	256	320	384	448
34	1	3	4	5	7	8	9	11	12	13	15	16	20	24	28
36	1	27	8	5	7	16	81	11	216	13	135	32	40	432	56
38	1	3	4	5	7	8	9	11	12	13	15	16	20	24	28
40	1	3	16	25	7	32	9	11	48	13	75	64	400	96	112
42	1	9	4	5	49	8	27	11	36	13	45	16	20	72	196
44	1	3	8	5	7	16	9	121	24	13	15	32	40	48	56
46	1	3	4	5	7	8	9	11	12	13	15	16	20	24	28
48	1	9	32	5	7	64	27	11	288	13	45	128	160	576	224
50	1	3	4	125	7	8	9	11	12	13	375	16	500	24	28
52	1	3	8	5	7	16	9	11	24	169	15	32	40	48	56
54	1	81	4	5	7	8	243	11	324	13	405	16	20	648	28
56	1	3	16	5	49	32	9	11	48	13	15	64	80	96	784
58	1	3	4	5	7	8	9	11	12	13	15	16	20	24	28
60	1	9	8	25	7	16	27	11	72	13	225	32	200	144	56
62	1	3	4	5	7	8	9	11	12	13	15	16	20	24	28
64	1	3	128	5	7	256	9	11	384	13	15	512	640	768	896
66	1	9	4	5	7	8	27	121	36	13	45	16	20	72	28
68	1	3	8	5	7	16	9	11	24	13	15	32	40	48	56
70	1	3	4	25	49	8	9	11	12	13	75	16	100	24	196
72	1	27	16	5	7	32	81	11	432	13	135	64	80	864	112
74	1	3	4	5	7	8	9	11	12	13	15	16	20	24	28
76	1	3	8	5	7	16	9	11	24	13	15	32	40	48	56
78	1	9	4	5	7	8	27	11	36	169	45	16	20	72	28
80	1	3	32	25	7	64	9	11	96	13	75	128	800	192	224
82	1	3	4	5	7	8	9	11	12	13	15	16	20	24	28
84	1	9	8	5	49	16	27	11	72	13	45	32	40	144	392
86	1	3	4	5	7	8	9	11	12	13	15	16	20	24	28
88	1	3	16	5	7	32	9	121	48	13	15	64	80	96	112
90	1	27	4	25	7	8	81	11	108	13	675	16	100	216	28
92	1	3	8	5	7	16	9	11	24	13	15	32	40	48	56
94	1	3	4	5	7	8	9	11	12	13	15	16	20	24	28
96	1	9	64	5	7	128	27	11	576	13	45	256	320	1,152	448
98	1	3	4	5	343	8	9	11	12	13	15	16	20	24	1,372
100	1	3	8	125	7	16	9	11	24	13	375	32	1,000	48	56

NOTE 1. The number in each block is H in equation 64.

NOTE 2. Referring to theorem IV of the appendix and table XVII observe that if $m=4k+2$ and s is odd and if s and p are relatively prime then $s=n$ and $s_0=[\![s/2n]\!]=0$. Hence s should be so selected that the following relations are not simultaneously satisfied: (1) $m=4k+2$, (2) s is odd, and (3) s and p are relatively prime.

12. Possible Combinations of s, p, and m Which Yield c-Circuit (F.W.)

The slot combinations given in table VII hold for both D.W. and F.W. A matter of particular interest to the designer is when D.W. may be avoided. We shall concern ourselves here with the problem of possible combinations of s, p, and m, which yield a c-circuit (F.W.).

It must be emphasized at the outset that all windings of type D_2 are necessarily (D.W.). However windings of type A, B, C, and D_1 may be F.W. or D.W. depending on whether s_0/c is or is not an integer. In order to obtain F.W. the following conditions must be satisfied:

(*a*). The winding must be of type A, B, C, or D_1.

(*b*). s_0/c must be an integer.

It is the object of this article to answer the following question: What numbers s (obtained from table VII) yield F.W. for any given combination of m, p, and c?

This question is answered in theorem IV of the appendix. We here give the summary of the results in table VIII. In order to use the table proceed in steps as follows:

1. Determine whether m belongs to case 1, 2, or 3.
2. Ascertain whether $2p$ is divisible by c or p is divisible by c as the case may be.
3. Select s from table VII so that s is divisible by c or $2c$ as the case may be.

13. Illustrative Problems

We shall illustrate the foregoing theory by solving five problems which yield windings of types A, B, C, D_1, and D_2, respectively. Thus let it be required to lay out windings for the following machines:

Machine *a*—four-phase, 6- pole, 80 slots
Machine *b*—three-phase, 10-pole, 54 slots
Machine *c*—three-phase, 4-pole, 45 slots
Machine *d*—six-phase, 8-pole, 60 slots
Machine *e*—six-phase, 18-pole, 135 slots

The work will be tabulated in order to conserve space (tables IX, X, XI, XII, XIII, XVI, XV, and XVI).

Table VIII. Necessary and Sufficient Conditions for a c-Circuit F.W.

Case	m	s	Conditions to Be Satisfied
1	$2k+1$	Table VII	$2p$ is divisible by c and s is divisible by c
2	$4k+2$	Table VII	p is divisible by c and s is divisible by $2c$
3	$4k$	Table VII	p is divisible by c and s is divisible by c

Table IX. Constants Entering Into the Winding Formulas

Item Number	Symbol	Equation Number	Formula	a	b	c	d	e
				Value of Item for the Five Machines				
1	s	Given		80	54	45	60	135
2	p	Given		3	5	2	4	9
3	m	Given		4	3	3	6	6
4	n q	(9)	$n/q = s/p$ in its lowest terms	80 3	54 5	45 2	15 1	15 1
5	Type	Table I		A	B	C	D_1	D_2
6	u	(26)	$u = n/m$ for types A, C $u = n/2m$ for type B $u = 2n/m$ for type D	20	9	15	5	5
7	s_0	Table I		1	2	1	2	4
8	b	(4)	$(1+gn)/q$	27	11	23	1	1
9	s/n			1	1	1	4	9

Table X. Winding Layout for Phases 1, 2, 3, and 4 of Machine a (See Table II and Equation 39)

Phase Number	Equation for Slot Numbers	k: 1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19	20
1	kb	27	54	1	28	55	2	29	56	3	30	57	4	31	58	5	32	59	6	33	60
2	$kb+bn/m$	7	34	61	8	35	62	9	36	63	10	37	64	11	38	65	12	39	66	13	40
3	$kb+2bn/m$	67	14	41	68	15	42	69	16	43	70	17	44	71	18	45	72	19	46	73	20
4	$kb+3bn/m$	47	74	21	48	75	22	49	76	23	50	77	24	51	78	25	52	79	26	53	80

NOTES:

1. Numbers in the above squares represent the slot numbers in which the top coil side of the various phases are placed.

2. The above winding may be used for a two-phase machine by reversing the terminals of phase 3 and connecting in series with phase 1 to form phase a and reversing the terminals of phase 4 and connecting in series with phase 2 to form phase b.

3. It must be here emphasized that our method of winding gives, for a two-phase machine, two equal and opposite phases. To obtain a two-phase machine in which the phases are in quadrature the machine should be first wound as if it were four phase and then the winding connected as explained in note 2 above.

4. Since $s_0 = 1$ and since, by equation 25, $s_{0u} = [\![s_0/c]\!]c = 0$ if $c > 1$ the winding cannot have more than one circuit.

Table XI. Winding Layout for Phases 1, 2, and 3 of Machine b (See Table III and Equation 39)

	Slot Numbers Phase 1		Slot Numbers Phase 2		Slot Numbers Phase 3	
k	$kb - c_1$	$kb + n - c_2$	$c_1 + bn/m$	$c_2 + bn/m$	$c_1 + 2bn/m$	$c_2 + 2bn/m$
1	11	38−	47	20−	29	2−
2	22	49−	4	31−	40	13−
3	33	6−	15	42−	51	24−
4	44	17−	26	53−	8	35−
5	1	28−	37	10−	19	46−
6	12	39−	48	21−	30	3−
7	23	50−	5	32−	41	14−
8	34	7−	16	43−	52	25−
9	45	18−	27	54−	9	36−

NOTE: Since $s_0 = 2$ and since, by equation 25, $s_{0u} = [\![s_0/c]\!]c = 0$ if $c > 2$ the above winding cannot have more than two circuits. For a two-circuit winding column 1 of each phase may be used for circuit number 1 and column 2 for circuit number 2.

14. Summary

We have developed a theory of fractional-slot (F.W.) and dead-coil (D.W.) windings and have shown the relation between the two classes. This theory might appear formidable to some designers. It can be, however, reduced to some very simple rules of thumb which are easily understood and applied by even the most naive individual. Indeed the process of laying out a winding according to our theory involves the simplest and most elementary processes of algebra and could be readily summarized in steps as follows.

(a). From the customer's specifications of a machine the designer knows

1. The number of phases m.
2. The number of poles $2p$.

(b). By consulting table VII he can readily tell the slot numbers that could be used to obtain a windable machine. Thus for a 12-pole, three- or six-phase machine the slot numbers must be multiples of 9. The designer can therefore use 9 or 18 or 27 or 36 or . . . $9k$ slots. The actual number of slots he selects depends upon such factors as the number of conductors he obtains from the voltage equation, the stock dies available, the number of circuits c, etc.

(c). In case the designer wishes to make his winding of more than one circuit it is advisable that he refer to table VIII. Here he can at once ascertain whether c circuits are at all possible with F.W. Thus, referring to table VIII, if $m = 2k+1$ and if $2p$ is

Table XII. Winding Layout for Phases 1, 2, and 3 of Machine c (See Table IV and Equation 39)

Phase Number	Equation for Slot Numbers	k: 1	2	3	4	5	6	7	23	24	25	26	27	28	29	30
1	kb	23	1	24	2	25	3	26	34−	12−	35−	13−	36−	14−	37−	15−
2	$kb+bn/m$	8	31	9	32	10	33	11	19−	42−	20−	43−	21−	44−	22−	45−
3	$kb+2bn/m$	38	16	39	17	40	18	41	4−	27−	5−	28−	6−	29−	7−	30−

NOTE. Since $s_0 = 1$ and since, by equation 25, $s_{0u} = [\![s_0/c]\!]c = 0$ if $c > 1$, the winding cannot have more than one circuit.

divisible by c then by selecting s divisible by c he can obtain F.W. otherwise he will have D.W.

(*d*). The designer how has m, p, and s. By using table IX he can:

1. Compute the constants, n, q, u, s_0, b, s/n which enter into the winding formulas of the machines.
2. Ascertain the type of winding he gets.

These computations involve nothing but the simple algebraic processes of multiplication, division, and reduction of a fraction to its lowest terms.

Table XIII. Slot Numbers for Phases 1 and 4 of Machine d (See Table V)

k	$kb=c_1$	$kb+n=c_2$	$kb+2n=c_3$	$kb+3n=c_4$
1	1	16	31	46
2	2	17	32	47
8	8	23	38	53
9	9	24	39	54
10	10	25	40	55

NOTE: In order to divide the slot numbers among the two phases we use the principle given in note 3 following table V. Thus one way of splitting table XIII is to take columns c_1 and c_2 for phase 1 and columns c_3 and c_4 for phase 4. With this mode of splitting we obtain the following table for the winding layout of machine *d*.

(*e*). Knowing the type of winding and the winding constants the designer now constructs a table following the patterns of tables II, III, IV, or V for windings of type *A*, *B*, *C*, or *D* respectively thereby obtaining the slot numbers for phase 1 or for phases 1 and $\mu+1$ as the case may be.

(*f*). In order to obtain the slot numbers for any of the remaining phases (say phase *y*) he uses equation 39 for types *A*, *B*, and *C* and equations 40 for type *D* as illustrated in tables X to XVI.

(*g*). In order to compute the chording factor for the *N*th harmonic use equation 48.

Table XIV. Winding Layout for Machine d (See Table XIII and Equations 40)

	Slot Number for the Various Phases											
	Phase 1		Phase 3		Phase 5		Phase 4		Phase 6		Phase 2	
k	c_1	c_2	c_1+bn/μ	c_2+bn/μ	c_1+2bn/μ	c_2+2bn/μ	c_3	c_4	c_3+bn/μ	c_4+bn/μ	c_3+2bn/μ	c_4+2bn/μ
1	1	16	6	21	11	26	31 −	46 −	36 −	51 −	41 −	56 −
2	2	17	7	22	12	27	32 −	47 −	37 −	52 −	42 −	57 −
8	8 −	23 −	13 −	28 −	18 −	33 −	38	53	43	58	48	3
9	9 −	24 −	14 −	29 −	19 −	34 −	39	54	44	59	49	4
10	10 −	25 −	15 −	30 −	20 −	35 −	40	55	45	60	50	5

NOTE 1. See note following table XI.

NOTE 2. See note 4 following table V which explains mode of connection.

Table XV. Slot Numbers for Phases 1 and 4 of Machine e (See Table V)

k	$kb=c_1$	$kb+n=c_2$	$kb+2n=c_3$	$kb+3n=c_4$	$kb+4n=c_5$	$kb+5n=c_6$	$kb+6n=c_7$	$kb+7n=c_8$	$kb+8n=c_9$
1	1	16	31	46	61	76	91	106	121
2	2	17	32	47	62	77	92	107	122
8	8	23	38	53	68	83	98	113	128
9	9	24	39	54	69	84	99	114	129
10	10	25	40	55	70	85	100	115	130

NOTE: In order to divide the slot numbers among the two phases we use the principle given in notes 2 and 3 following table V. Thus one way of splitting table XV is to take the slot numbers in the first four columns for phase 1, the slot numbers in the next four columns for phase 4, and make the slot numbers in the last column dead. With this mode of splitting we obtain table XVI for the winding layout of machine *e*.

Table XVI. Winding Layout for Machine e (See Table XV and Equations 40)

	Slot Numbers for the Various Phases											
	Phase 1				Phase 3				Phase 5			
k	c_1	c_2	c_3	c_4	c_1+bn/μ	c_2+bn/μ	c_3+bn/μ	c_4+bn/μ	c_1+2bn/μ	c_2+2bn/μ	c_3+2bn/μ	c_4+2bn/μ
1	1	16	31	46	6	21	36	51	11	26	41	56
2	2	17	32	47	7	22	37	52	12	27	42	57
8	8 −	23 −	38 −	53 −	13 −	28 −	43 −	58 −	18 −	33 −	48 −	63 −
9	9 −	24 −	39 −	54 −	14 −	29 −	44 −	59 −	19 −	34 −	49 −	64 −
10	10 −	25 −	40 −	55 −	15 −	30 −	45 −	60 −	20 −	35 −	50 −	65 −

	Phase 4				Phase 6				Phase 2			
	c_5	c_6	c_7	c_8	c_5+bn/μ	c_6+bn/μ	c_7+bn/μ	c_8+bn/μ	c_5+2bn/μ	c_6+2bn/μ	c_7+2bn/μ	c_8+2bn/μ
1	61 −	76 −	91 −	106 −	66 −	81 −	96 −	111 −	71 −	86 −	101 −	116 −
2	62 −	77 −	92 −	107 −	67 −	82 −	97 −	112 −	72 −	87 −	102 −	117 −
8	68	83	98	113	73	88	103	118	78	93	108	123
9	69	84	99	114	74	89	104	119	79	94	109	124
10	70	85	100	115	75	90	105	120	80	95	110	125

NOTE: Since $s_0=4$ and since by equation 25 $s_{0u}=[\![s_0/c]\!]c=0$ if $c>4$ the above winding cannot have more than four circuits. For a two-circuit winding use columns 1 and 2 of each phase for the first circuit and columns 3 and 4 for the second circuit. For a four-circuit winding use one column of each phase for each circuit. A three-circuit winding could be had by sacrificing one column in each phase and occupying the slots in that column by dead coils.

(*h*). In order to compute the slot (distribution) factor for the *N*th harmonic use (56) for types *A* and *B* and equation 62 for types *C* and *D*.

(*i*). *Caution:* It must be emphasized that our theory gives, in the case of two-phase machines, phases that are 180 degrees apart. In order to obtain a two-phase machine with a phase displacement of 90 degrees proceed as though the machine were four-phase then connect phases 1 and 3 in series to obtain phase *a* and 2 and 4 in series to obtain phase *b*. This is illustrated by machine *a*, table X.

(*j*). It should be further emphasized that (see equations 14) the phase sequence throughout this paper is such that phase 2 *leads* phase 1 by $2\pi/m$, phase 3 *leads* phase 2 by $2\pi/m$, and in general phase *y* leads phase $y-1$ by $2\pi/m$.

Appendix. Theorems Used in the Paper and Their Proof

Theorem I

If:

$$(a)\quad \boldsymbol{v}=v\epsilon^{j\alpha} \tag{65a}$$

$$(b_1)\quad |\beta_1|<|\beta_2|\leqq\pi \text{ or } (b_2)\quad 0<\beta_1=-\beta_2<\pi \tag{65b}$$

then

$$|v\epsilon^{j\alpha}+\epsilon^{j(\alpha+\beta_1)}-\epsilon^{j(\alpha+\beta_2)}|>v \tag{66}$$

Graphically this theorem states that if two vectors of unit length are respectively added and subtracted from a given vector $\boldsymbol{v}$, and if the added vector is closer to $\boldsymbol{v}$ than the subtracted vector then the result of the operation is an increase in the modulus of $\boldsymbol{v}$.

Proof: Write the left side of the inequality (66) in the following form:

$$|v\epsilon^{j\alpha}+\epsilon^{j(\alpha+\beta_1)}-\epsilon^{j(\alpha+\beta_2)}|=|\epsilon^{j\alpha}|\,|v+\epsilon^{j\beta_1}-\epsilon^{j\beta_2}| \tag{67a}$$

$$=|v+\epsilon^{j\beta_1}-\epsilon^{j\beta_2}| \tag{67b}$$

Squaring both sides of (67*b*) we have:

$$|v\epsilon^{j\alpha}+\epsilon^{j(\alpha+\beta_1)}-\epsilon^{j(\alpha+\beta_2)}|^2=|v+\epsilon^{j\beta_1}-\epsilon^{j\beta_2}|^2 \tag{68a}$$

$$=[v+(\cos\beta_1-\cos\beta_2)]^2+(\sin\beta_1-\sin\beta_2)^2 \tag{68b}$$

If hypothesis (b_1) of the theorem (see $65b_1$) is satisfied then $\cos\beta_1>\cos\beta_2$ and (68*b*) is greater v^2 which proves the inequality (66). On the other hand if $\beta_1=-\beta_2$ (see equation $65b_2$) then again (68*b*) is greater than v^2 and again the inequality (66) is satisfied. Thus the theorem is proved.

Theorem IIa

If

$$A=\sum_{k=1}^{u}\pm x_q^{a_{0k}} \tag{69}$$

where x_q is a primitive *n*th root of unity, *n* is even, $\pm x_q^{a_{0k}}$ are all different roots of unity, and $u<n$, then the maximum absolute value of *A* occurs when $x_q^{a_{0k}}$ are adjacent *n*th roots of unity or:

$$A_m=\sum_{k=1}^{u}x_q^{kb} \tag{70}$$

Proof: Since *n* is even we can make use of statement 7*a*, section 2, namely that each $-x_q$ is equal to some other *n*th root of unity x_r. Hence (69) may, without any loss of generality, be written in the following form:

$$A=\sum_{k=1}^{u}x_q^{c_{0k}} \tag{71}$$

where $x_q^{c_{0k}}$ are all different *n*th roots of unity.

Let A_m (figure 3) be the maximum vector obtained from (71) and let the angles made by the *n*th roots of unity with A_m be such that:

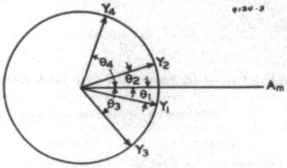

Figure 3. Maximum electromotive force per phase

$$|\theta_1|\leqq|\theta_2|\leqq|\theta_3|\leqq\ldots\leqq|\theta_n|\leqq\pi \tag{72}$$

Designate the *n*th root of unity x_r (see figure 3) by Y_s such that the subscript *s* of Y_s and the subscript *s* of θ_s correspond. Thus the *n*th root of unity Y_s makes an angle θ_s with the vector A_m.

If we can show that A_m is the sum of adjacent vectors or:

$$A_m=Y_1+Y_2+\ldots+Y_u \tag{73}$$

we shall have proved the theorem.

To show that A_m is represented by (73) assume that such is not the case. Then there must be two roots of unity Y_a and Y_b with $a<b$ such that Y_b is one of the vectors comprised in A_m and Y_a is not. This means that we can replace Y_b by Y_a and obtain a vector:

$$A_m-Y_b+Y_a=A\epsilon^{j\alpha}-\epsilon^{j(\alpha+\theta_b)}+\epsilon^{j(\alpha+\theta_a)} \tag{74a}$$

where

$$|\theta_a|<|\theta_b|\leqq\pi \text{ or } 0<|\theta_b|=|\theta_a|<\pi,\ \theta_b=-\theta_a \tag{74b}$$

Thus the conditions of theorem I are satisfied and

$$|A_m-Y_b+Y_a|>|A_m| \tag{75}$$

which is a contradiction of the fact that A_m (figure 3) is a maximum.

To show that A_m takes the form given in (70) observe that x_q in (71) is a primitive *n*th root of unity. Hence adjacent vectors take the form (see equation 4*a*) x_q^{kb}. Substituting $c_{0k}=kb$ in (71) we obtain (70).

Theorem IIb

If

$$A=\sum_{k=1}^{u}\pm x_q^{a_{0k}} \tag{76}$$

where x_q is a primitive *n*th root of unity, *n* is odd, *u* is odd, $x_q^{a_{0k}}$ are all different *n*th roots of unity and $u<n$, then the maximum absolute value of *A* occurs when *u* adjacent (2*n*)th roots of unity are added or:

$$A_m=\sum_{k=1}^{\frac{1}{2}(u-1)}x_q^{kb}-\sum_{k=\frac{1}{2}(n+1)}^{\frac{1}{2}(n+u)}x_q^{kb} \tag{77}$$

Proof: Referring to statement 8 of section 2 in the paper we conclude that the sum given in equation 76 consists of *u* terms each of which is a (2*n*)th root of unity. Hence we can apply theorem II*a* with 2*n* replacing *n* and obtain the result that the maximum absolute value of *A* in (76) occurs when *u* adjacent (2*n*)th roots of unity are added.

To show that A_m takes the form (77) observe that the terms of the sum in (76) are adjacent (2*n*)th roots of unity. Hence, for maximum *A*, equation 76 takes the form:

$$A_m=\sum_{r=1}^{u}(\epsilon^{j2\pi/2n})^r \tag{78}$$

In order to change (78) to the form (77) split (78) into two parts, depending on whether *r* is even or odd, as follows:

1. *r even.* Here replace *r* in (78) by 2*k* where *k* assumes successive integer values from 1 to $^1/_2(u-1)$ and obtain:

$$\sum_{k=1}^{\frac{1}{2}(u-1)}(\epsilon^{j2\pi/2n})^{2k}=\sum_{k=1}^{\frac{1}{2}(u-1)}(\epsilon^{j2\pi/n})^k$$

$$=\sum_{k=1}^{\frac{1}{2}(u-1)}x_q^{kb} \tag{79}$$

2. *r odd.* Here replace *r* in (78) by $(2t+1)$ where *t* assumes successive integer values from 0 to $^1/_2(u-1)$ and obtain

$$\sum_{t=0}^{\frac{1}{2}(u-1)}(\epsilon^{j2\pi/2n})^{(2t+1)}$$

$$=\epsilon^{j\pi}\sum_{t=0}^{\frac{1}{2}(u-1)}\epsilon^{j[(2t+1)/2n+\frac{1}{2}]2\pi} \tag{80a}$$

$$=-\sum_{t=0}^{\frac{1}{2}(u-1)}\epsilon^{j(2\pi/n)[t+\frac{1}{2}+n/2]} \tag{80b}$$

Now let $k=(t+{}^1/_2+n/2)$ and remember that $\epsilon^{j2\pi/n}=x_q^b$. Using this transformation (80*b*) becomes

$$\sum_{t=0}^{\frac{1}{2}(u-1)}(\epsilon^{j2\pi/2n})^{2t+1}=-\sum_{k=\frac{1}{2}(n+1)}^{\frac{1}{2}(n+u)}x_q^{kb} \tag{81}$$

Adding (79) and (81) we obtain (77).

Theorem III

If (1)

$$m=\prod_r m_r^{a_r} \text{ when } m\neq 4k+2 \tag{82a}$$

or

$$m=2\prod_r m_r^{a_r} \text{ when } m=4k+2 \tag{82b}$$

where $a_r>0$ and m_r are different prime numbers.

If (2)

$$p=\prod_r m_r^{b_r}p_2=p_1p_2 \tag{83a}$$

where

$$p_1=\prod_r m_r^{b_r} \tag{83b}$$

Table XVII. Conclusions Regarding n, Type, and s_0 Which Follow When p and s Satisfy Certain Conditions

Case	m	If		Then		
		p	s	n	Type	s_0
1	$2k+1$	Arbitrary	Divisible by 2^{d+1}	Even	B	$2s/n$
			Not divisible by 2^{d+1}	Odd	C	s/n
2	$4k+2$	Odd	Even	Even	A	s/n
			Odd	Odd	D_2	$[[s/2n]]$
		Even	Divisible by 2^{d+1}	Even	A	s/n
			Not divisible by 2^{d+1} but even	Odd	D_1	$s/2n$
			Odd	Odd	D_2	$[[s/2n]]$
3	$4k$	Arbitrary	Even	Even	A	s/n
Proof by the use of relations				Table VI Eq. 9, note 1	Table I	Eq. 13

NOTE. d is the highest power of 2 whic h is contained in p.

Table XVIII. Conclusions Regarding Relations Between c and p on the One Hand and c and s on the Other Hand Which Follow When Conditions a and b of Section 12 Are Satisfied. This Is a Proof of the Necessity of the Conditions

Case	m	p	s	n	Type	s_0	Conclusions From Condition b Namely That s_0/c Is an Integer
1	$2k+1$	Arbitrary	Divisible by 2^{d+1}	Even	B	$s/(n/2)=2p/q$	(1) $2p$ is divisible by c (2) s is divisible by c
			Not divisible by 2^{d+1}	Odd	C	$s/n=p/q$	(1) p is divisible by c. Hence $2p$ is divisible by c (2) s is divisible by c
2	$4k+2$	Odd	Even	Even	A	$s/n=p/q$	(1) p is divisible by c. Hence c is odd (2) s is divisible by c. Hence s is divisible by $2c$
		Even	Divisible by 2^{d+1}	Even	A	$s/n=p/q$	(1) p is divisible by c. Hence c is not divisible by 2^{d+1} (2) s is divisible by c. Hence s is divisible by $2c$
			Not divisible by 2^{d+1} but even	Odd	D_1	$s/2n=p/2q$	(1) p is divisible by $2c$. Hence p is divisible by c (2) s is divisible by $2c$
3	$4k$	Arbitrary	Even	Even	A	$s/n=p/q$	(1) p is divisible by c (2) s is divisible by c

Table XIX. Conclusions Regarding Type of Winding and Relation Between c and s_0 Which Follow When the Conditions of Table VIII Are Satisfied

Case	m	Condition of Table VIII	If		Then		
			p	s	Type	s_0	Other Conclusions
1	$2k+1$	$2p$ is divisible by c and s is divisible by c	Arbitrary	Divisible by 2^{d+1}	B	$2s/n$	c is a common divisor of $2p$ and s. Hence† it is a divisor of s_0
				Not divisible by 2^{d+1}	C	s/n	c is a common divisor‡ of p and s. Hence* c is a divisor of s_0
2	$4k+2$	p is divisible by c and s is divisible by $2c$. Hence s is even	Odd	Even	A	s/n	c is a common divisor of p and s. Hence* c is a divisor of s_0
			Even	Divisible by 2^{d+1}	A	s/n	c is a common divisor of p and s. Hence* c is a divisor of s_0
				Not divisible by 2^{d+1} but even	D_1	$s/2n$	c is a common divisor of p and $s/2$. Hence§ c is a divisor of s_0
3	$4k$	p is divisible by c and s is divisible by c	Arbitrary	Even	A	s/n	c is a common divisor of p and s. Hence* c is a divisor of s_0

* Since $n/q=s/p$ in its lowest terms therefore $s_0=s/n$ is the greatest common divisor of p and s. Therefore any common divisor of p and s is a divisor of s_0.

† c is a common divisor of $2p$ and s. Hence c is a divisor of their greatest common divisor. But this is $2s/n=s_0$. Hence c is a divisor of s_0.

‡ c is a divisor of s. Hence c is not divisible by 2^{d+1}. But c is a divisor of $2p$ which is divisible by 2^{d+1}. Hence c is a divisor of p.

§ c is a common divisor of p and $s/2$. Hence c is a divisor of their greatest common divisor. But this is $s/2n=s_0$. Therefore c is a divisor of s_0.

$b_r \geqq 0$, and p_2 does not contain m_k as factors.

If (3)

$$H=mp_1 \text{ when } m\neq 4k+2 \tag{84a}$$

$$=(m/2)p_1 \text{ when } m=4k+2 \tag{84b}$$

If (4)

$$n/q=s/p \text{ in its lowest terms.} \tag{85}$$

Then n is divisible by $\prod_r m_r^{a_r}$ if and only if:

$$s=HG,\ G \text{ any integer} \tag{86}$$

Proof: Write s in terms of the prime factors m_r as follows:

$$s=\prod_r m_r^{d_r} s_2=s_1 s_2 \tag{87}$$

where $d_r \geqq 0$ and s_2 does not contain m_r as a factor.

Divide (87) by (83*a*) and obtain:

$$s/p=\prod_r m_r^{(d_r-b_r)}(s_2/p_2)=n/q \tag{88}$$

Since neither s_2 nor p_2 contain m_r as prime factors and since $n/q=s/p$ in its reduced form therefore any prime factor m_r of n must be contained in the $\prod_r m_r^{(d_r-b_r)}$. This means that $(d_r-b_r)>0$. Hence a necessary and sufficient condition that n is divisible by $\prod_r m_r^{a_r}$ is:

$$d_r-b_r \geqq a_r \tag{89a}$$

or

$$d_r-b_r-a_r \geqq 0 \tag{89b}$$

Now write equation 87 in the following form:

$$s=\prod_r m_r^{a_r}\prod_r m_r^{b_r}\prod_r m_r^{(d_r-b_r-a_r)} s_2 \tag{90a}$$

$$=mp_1G=HG \text{ is } m\neq 4k+2 \tag{90b$_1$}$$

$$=(m/2)p_1G=HG \text{ if } m=4k+2 \tag{90b$_2$}$$

where

$$G=\prod_r m_r^{(d_r-b_r-a_r)} s_2 \tag{90c}$$

Since by (89*b*) $d_r-b_r-a_r \geqq 0$ therefore G is an integer. But by properly choosing d_r and s_2 in (90*c*) we can change the value of G in any arbitrary manner whatever. Hence G is an arbitrary integer and the theorem is proved.

Theorem IV

The conditions given in table VIII of section 12 are necessary and sufficient for the satisfaction of conditions *a* and *b* of that article.

Proof: It will be convenient to outline the proof in tabular form (table XVII).

From table XVII and conditions *a* and *b* of section 12 the following relations may be derived regarding types *A*, *B*, *C*, and D_1.

Type D_2 is necessarily omitted because of condition *a*.

In table XVIII we proved the necessity of the conditions given in table VIII. To prove the sufficiency of these conditions we assume that the conditions given in table VIII are satisfied and from this prove that conditions *a* and *b* of section 12 follow. This again will be arranged in tabular form (table XIX).

Notations

a = an integer (see equation 5), also the Fortescue operator
a_r = positive integers (see equation 63)
a_{jk} = integers (see equation 14)
A = type of winding (see table I)
$\mathbf{A}, \mathbf{A}_m$ = vectors (see theorems II*a* and II*b*)
b = an integer (see equation 4, also theorem II*a*)
b_r = non-negative integers (see equation 83)
B = type of winding (see table I)
c = number of circuits
c_r, c_r', c_r'' = integers (see tables II to V)
c_{0k} = integers (see theorem II*a*)
C = type of winding (see table I)
d = highest power of 2 contained in p (see note following table XVII)
d_r = non-negative integers (see equation 87)
D = type of winding (see table I), also total number of dead coils per phase (see equations 16, 27*a*)
D_1, D_2 = types of winding (see remarks following table I)
(D.W.) = dead-coil winding
$\mathbf{E}$ = vector electromotive force per phase
E = effective value of $\mathbf{E}$
$\mathbf{E}_0$ = electromotive force per coil side
$\mathbf{E}_{0N}$ = electromotive force per coil side due to the Nth harmonic
E_{aN} = algebraic sum of the Nth harmonic electromotive forces per phase
$\mathbf{E}_{cN}$ = vector sum of the Nth harmonic electromotive forces in two coil sides
E_{cN} = effective value of $\mathbf{E}_{cN}$
$E_{c'N}$ = algebraic sum of the Nth harmonic electromotive forces in two coil sides
$\mathbf{E}_{vN}$ = vector sum of the Nth harmonic electromotive forces per phase
E_{vN} = effective value of $\mathbf{E}_{vN}$
(F.W.) = fractional-slot winding
g = an integer
G = an integer (see equation 64)
h = total number of used slots per phase (see equations 16, 27)
H = an integer (see equation 64)
j = imaginary unit
k = an integer
k_{sN}, k_{WN} = distribution and winding pitch factors for the Nth harmonic
m = number of phases
m_k = prime factors of m or of $m/2$ (see equation 63)
n = an integer, defined by equation 9
n' = an integer (see equation 8)
N = number of harmonic
p = number of pairs of poles
p_1, p_2 = factors of p (see equation 83)
q = an integer, defined by equation 9
r = an integer
s = number of slots
s_1, s_2 = factors of s (see equation 87)
s_0 = number of slots which are displaced by a multiple of π from any given slot in any given phase (see equation 13)
s_0' = number of slots which are displaced by a multiple of π from any given slot (see equation 12)
s_{0u} = number of used slots per phase which are displaced by a multiple of π or 2π (see equation 25)
s_d = number of dead coils on each row of tables II to V (see equation 42)
t = an integer
u = number of different vectors per phase (see equation 26)
$\mathbf{v}$ = a vector (see equation 65)
x = unknown quantity or variable
$[\![x]\!]$ = bracket function
x_q = primitive nth root of unity
x_r = nth root of unity
y = an integer (see equations 39 and 40)
Y_s = vectors (see theorem II*a*)
α = argument of $\mathbf{v}$ (see equation 65)
α_q = argument of x_q (see equation 2), also slot pitch (see equation 11)
β_1, β_2 = angles (see theorem I)
β_y = arguments (see section 5*b*)
ϵ = base of Napierian logarithms
θ = an angle (see equation 46)
θ_r = angles (see theorem II*a*)
$\mu = m/2$ (see equation 20)
Π = symbol for product
σ = an integer (see equation 49)
ψ = per cent winding pitch

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Discussion

For discussion, see page 1079.

Analysis of Factors Which Influence the Application, Operation, and Design of Shunt-Capacitor Equipments Switched in Large Banks

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THE application of shunt capacitors to distribution circuits has been well treated in the technical press. These papers have in general dealt with conditions obtaining when relatively small banks of capacitors are connected to the circuit 24 hours of the day. The increase in knowledge of the benefits afforded by capacitors through experience and analysis, coupled with the price reductions effected through advances in design and manufacture, has logically stimulated their use in fields where switching during the day, as governed by load and voltage conditions, is advantageous, desirable, and economically sound. These types of applications impose different duties upon the capacitors as well as upon the disconnecting equipments and associated devices, and the question naturally arises in the engineers' mind as to their effect upon the design and operation of the equipment. This paper offers a discussion and an analysis of the more pertinent of these problems. The conclusions drawn pertain in general to installations of 6,900 volts and below. The higher-voltage equipments may require additional considerations.

The interesting and encouraging conclusion is that in general the cases analyzed show that the duties imposed are well within the limitations of the standard product.

Conclusions

1. Means for limiting the magnitudes of inrush currents should not be required in general. Circuit breakers as normally applied from the dictates of other circuit requirements should suffice. If current-limiting features are required, inductance is much more effective than resistance and since reactors may be required in the larger installations for the improvement of wave form, they can also serve as the current limiters at the same time.

2. Transient capacitor voltages attending the energizing of large banks will never exceed twice the normal operating voltage of the capacitor. Voltage transients across the capacitor coincident with de-energizing will not exceed normal unless the voltage builds up due to restriking of the arc in the breaker, but this is considered a very remote possibility in equipments of 6,900 volts and below, which appear to have more possibilities in this country than those of higher voltage.

3. In the higher-voltage hand-operated (6,900 volt and up) delta-connected banks it is advisable to use oil fuse cutouts or three-phase air switches or oil circuit breakers instead of three single-phase air disconnects, to facilitate switching operations.

4. Wave form distortions accentuated by shunt capacitors can be reduced to a negligible amount by the judicious use of reactors in series with part of a group that is switched as a unit.

A—Inrush Currents When Energizing Capacitors

Since a capacitor exhibits zero impedance to the flow of current the instant voltage is applied, one may easily err in concluding that enormous transient currents flow during this energizing period. The energizing current for a single bank is always less than the short-circuit current value at that point and the circuit constants generally met in practice makes the same point hold true for multiple banks. In general these magnitudes of inrush current can be easily handled by the circuit control devices selected as dictated by other circuit characteristics.

1. SINGLE BANK

The transients attending the energizing of a single capacitor bank (figure 1) are representative of conditions when capacitors are connected permanently to the motor terminals and switched with the motor, or located out on a distribution feeder and are switched on and taken off as the system voltage or load conditions dictate. The maximum value of inrush current for this condition, derived in appendix *A*, is given by the expression

$$I_{max} = I_0\left(1+\sqrt{\frac{x_c}{x_L}}\right)$$

where x_c and x_L are the circuit capacitive and inductive reactances respectively and I_0 is normal capacitor current.

This expression is simple and conservative and should be useful in determining the duty on a given contactor or switch being used to control capacitors. If a more accurate and complete current picture is desired, equation 2 of appendix *A* could be used which gives instantaneous values as a function of time, including circuit resistance.

A typical application of a 180-kva 4,000-volt bank that may be switched out two miles from a 600-kva substation is analyzed and the results plotted in figure 2*a*. Here it is seen that the maximum current is about six times normal. The square root of the reactance ratio is 6.25, hence the above approximation gives a conservative inrush current value of 7.25 times normal.

2. PARALLEL BANKS

The energizing of a capacitor bank that is located very close to an energized bank (figure 3) results in considerably more current inrush than a single bank since there is very little impedance between the charged and uncharged banks. This circuit is analyzed in appendix *A* and equation 6 yields conservative results for the magnitude of the inrush current since the effect of resistance has been neglected.

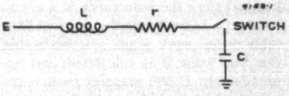

Figure 1. Circuit diagram representing a capacitor being energized through resistance and inductance

Normal circuit damping will reduce this value since skin effect increases the effective resistance considerably for the high-frequency components of current. The current magnitudes, however, neglecting damping, are not excessive or alarming, hence further refinement in the analysis is considered unnecessary.

Numerical results of a specific case, to which equation 6 pertains, are plotted as curve 1 in figure 4. This case is illustra-

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tive of four 1,080-kva banks installed at a point in a 4,000-volt system that will give a 150,000-kva short circuit. Such an installation might be made at a generation point to allow a certain generator to be shut down that would normally be run to carry reactive kilovolt-amperes or hold voltage during certain periods of the day.

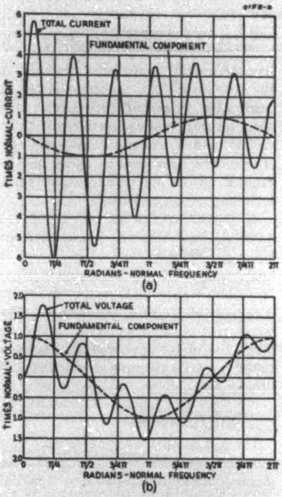

Figure 2. Energizing a single capacitor bank

(*a*)—Inrush current to capacitor
(*b*)—Circuit voltage at terminal of capacitor

The value of inductance (L_2) from the bus to each capacitor bank will probably never be less than 0.00377 ohm (10 microhenries) since the inductance of a circuit breaker, a current transformer, and little cable or bus work would amount to this. For this value it is calculated that approximately 11,500 amperes peak is obtained, which could be readily handled by the circuit breakers that would be normally applied at such a location.

3. Reduction of Inrush Currents

On rare occasions it may be desirable or necessary to limit or reduce the magnitude of these inrush currents, particularly in the larger multiple-bank arrangements. Since inductance or resistance may be used as the limiting agent each is analyzed in part 3 of appendix *A* and the numerical results for a particular installation are shown plotted in figure 4.

The curves in figure 4 show clearly the effectiveness of inductance and resistance in limiting the magnitude of this inrush current. For example, if it were desirable to limit this current to 10,000 amperes due to breaker limitations, or other reasons, it would require about 0.25 ohm of resistance while only 0.0062 ohm of inductive reactance would be required. Due to the continuous I^2R loss in the resistor it appears that inductance is the most economical and satisfactory way to reduce these currents when the occasion requires it. Resistance, however, could be switched in and then taken out with auxiliary equipment, but it would complicate the operation and arrangement. Other considerations as discussed in part *C* below, which may require the use of reactors should, in general, provide the necessary current limiting features in a practical case.

B—Transient Voltages Attending the Switching of Capacitors

The circuit voltage conditions resulting from the energizing and de-energizing of capacitor banks may be important from two standpoints. First, will the voltage be excessive for the capacitor and adjacent equipment and second, when energizing a bank will the voltage dip experienced while the capacitor is charging up the first half cycle be noticeable to consumers?

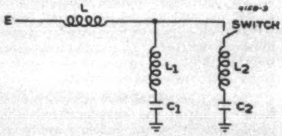

Figure 3. Circuit diagram representing the energizing of a capacitor bank adjacent to an already energized bank

1. Transient Voltages Above Normal

It has been spoken of before and is well recognized that a capacitor may be subjected to approximately twice normal voltage when energized. However, the conditions have not been fully determined that obtain when the supply circuit to which the capacitor is being connected, has a natural frequency of its own as well as a forced frequency voltage source.

(*a*) *Voltage Across Capacitor.* The equation for the voltage across a capacitor being energized from a bus on which there is already an energized bank is derived in appendix *B* and is given by equation 11.

Inspection of this equation shows that the coefficient of each term has in the denominator the product of the differences of two frequencies. If any two of these three frequencies approach each other, the coefficients could be numerically very large since resonance would be approached. In the normal application of capacitors however, these frequencies are widely separated. The lower natural frequency very rarely gets below 300 cycles per second and the higher frequency is usually above 2,000 cycles. These are both so far from the 60-cycle impressed frequency and from each other that the conclusion is drawn that the familiar twice-normal generalization also holds true for multiple banks of capacitors. In fact it may be shown that in all cases the voltage is no greater than twice the steady-state capacitor voltage. This is important, since these larger banks of capacitors are being considered more and more every day and fortunately the standard product is already designed for these transient voltages, hence this feature should not impede the development of this phase of the art.

(*b*) *Voltage Across Switch Contacts.* When de-energizing single or multiple capacitor banks, the analysis in appendix *B* further shows that with the normal sequence of contact opening and arc extinction, with no restriking of the arc the maximum voltage across the contacts of a three-pole breaker will be 3.46 times normal, and this occurs for delta-connected banks. Only twice-normal switch voltage can be obtained with wye neutral-grounded banks.

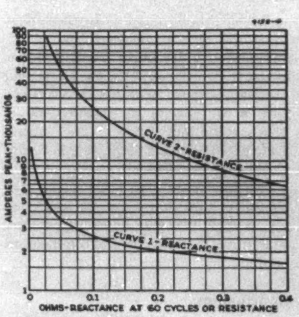

Figure 4. Effectiveness of resistance and reactance in reducing inrush currents to multiple capacitor banks

These duties are not considered abnormal since they are well within the normally encountered ranges of voltage recovery rates and magnitudes.

2. VOLTAGE DIP COINCIDENT WITH ENERGIZING A BANK

It is well known that when a capacitor is energized it appears as a short circuit until it accumulates a charge. Without further reasoning one may conclude that a voltage flicker would certainly be noticeable when throwing a bank on the line. The case discussed in part *A* under inrush currents of the 180-kva bank connected two miles from the substation is further analyzed to show the voltage conditions at the capacitor when it is energized. Equation 9, plotted for the constants applying, is shown in figure 2*b*. Here it is seen that the voltage is reduced to zero the instant the switch is closed, then as the capacitor charges up, the voltage is restored and it reaches normal value in about one-fourth of a natural frequency cycle. The natural frequency is 6.25 times normal, so normal voltage reappears in about 1/1,500 second which of course is a much faster recovery from zero than is experienced from a normal voltage zero at normal frequency.

Figure 2*b* further shows that the peak voltage reached is about 1.75 times normal, the reduction from twice normal being due to damping.

C—Miscellaneous Considerations

1. DE-ENERGIZING A THREE-PHASE BANK, ONE PHASE AT A TIME

Very often a bank of capacitors is switched manually with air-break disconnect switches or cutouts. In some cases it has been found difficult to extinguish the arc across the contacts of the switch, particularly in the case of the higher-voltage (6,900-volt) delta-connected banks.

In appendix *C* it is shown that the switch voltage of the first phase cleared in a delta bank may reach three times line-to-neutral voltage while the next phase opened may reach 3.46 *E*. In a wye neutral-grounded bank, it is shown that the maximum voltage is only twice normal. These values assume no building up of the voltage due to restriking of the arc across the switch.

To determine the effect of arc restriking on switch voltage, a typical installation was set up on the transient analyzer.[1] This gave accurate control of the restrike and it was found that with one restrike the switch voltages could be increased in each case (wye and delta) by a maximum of about 50 per cent.

From this it is seen that on occasion it may be desirable to switch the higher-voltage delta banks with oil fuse cutouts or oil circuit breakers instead of with single-phase air disconnecting devices if difficulty is experienced in breaking the charging current.

2. TRANSIENT VOLTAGES ON CURRENT-TRANSFORMER SECONDARIES

In the larger capacitor banks, it may be necessary to have current transformers in the circuit for control or indicating purposes. If adjacent capacitor banks are sufficiently closely coupled, the current transformers may represent a fair portion of the total inductance between the banks and as shown in appendix *C*, all the circuit voltage appears across these circuit inductances the instant the switch is closed. Hence a certain portion of this voltage appears across the current transformers and this voltage stepped up by the turn ratio may require special attention. Shunting resistors or current transformer surge absorbers should be used.

This is a rare phenomenon, but it is mentioned here in order to avoid confusion when it does arise.

3. MOTORS SWITCHED WITH CAPACITORS CONNECTED AT TERMINALS

The question of self-excitation of induction motors operating with capacitors has been adequately treated[2] and will be mentioned here only for completeness' sake. There are certain limits to the amount of capacitance that can be switched with motors to prevent abnormal self-excitation voltages and reference should be made to the appropriate literature for recommendations. It can be said, in general, that correcting the motor full-load power factor to 95 per cent will not result in unsatisfactory voltages.

Further, the application of capacitors in this manner changes the current required by the motor for a given load. It is necessary, therefore, to review the overload protection to make certain the motor is adequately protected after the installation of capacitors. It may at times be necessary to change the heater units in the motor thermal relays to conform to the reduced current drawn by the motor-capacitor combination.

4. IMPROVEMENT OF WAVE-FORM DISTORTIONS AMPLIFIED BY CAPACITORS

It is well known that any circuit combination of inductance and capacitance has a so-called natural frequency but this has no practical significance unless a voltage approaching that frequency is impressed upon the circuit. Experience and voltage measurements have shown that the fifth harmonic is generally the only harmonic voltage that is large enough to cause wave-form distortions of sufficient magnitude to require attention and these cases are rare. When capacitors are installed in large banks, primarily for voltage control, the wave distortion problem may require more attention than it has in the distribution field. The amount of capacitive kilovolt-amperes required to effect a four per cent rise in the 60-cycle voltage is about the amount to be in resonance with the fifth harmonic voltage, hence voltage improvements by capacitors approaching three or four per cent may need special attention.

A general method of using reactors for harmonic suppression is shown diagrammatically in figure 5. Here, a reactor is placed in series with a portion of the capacitor bank of sufficient ohmic value to cause that leg of the bank to be inductive to the harmonic current of the same magnitude as the other leg is capacitive, thus providing a wave trap. The total harmonic current taken from the line is then very small, just sufficient to supply the losses of the bank. The net corrective ability of the capacitors in series with the reactor is increased by the amount equal to the per cent voltage rise across the reactor. If the reactor is placed in series with one-half the bank, this increase in capacitive kilovolt-amperes amounts to 8.6 per cent. This scheme has been used on occasion in this country.

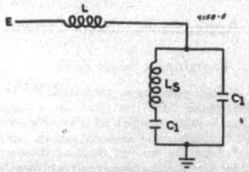

Figure 5. Circuit diagram illustrating the use of a reactor in series with part of a capacitor bank for harmonic current reduction

If it is not required practically to eliminate the flow of harmonic current in the line, the series reactor can be made a little smaller, of a size to cause that leg to have zero impedance, forming a resonant shunt, then the harmonic voltage will be effectively short-circuited at that point and the rest of the parallel banks are not affected by that particular har-

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1. For all numbered references see list at end of paper.

monic. The harmonic current that flows will be that which can be forced through the system impedance by the harmonic voltage that was present on the system before the capacitors were installed. This does not necessarily represent much current since the harmonic voltage will generally be low, and the system impedance will be high for the harmonic. Assuming a five per cent fifth harmonic voltage (which is high average) the magnitude of the fifth-harmonic current would be 1 per cent of the available short-circuit current at that point. This scheme has been used to a great extent in Japan where large banks are used for system voltage control.[4]

Either method utilizes the capacitors in the bank for harmonic suppression as well as power-factor correction and they are mentioned here to acquaint the trade with available corrective methods if and when they are needed.

Appendix A. Inrush Currents

1. Energizing a Single Bank

A single-phase capacitor installation or a three-phase bank energized by a three-phase breaker on which all three poles close substantially simultaneously can be represented by the one-line diagram shown in figure 1. The maximum current flows in the capacitor when the switch is closed at the peak of the voltage wave corresponding to the application of a cos ωt voltage.

The current equation, in operational terms may be written as

$$I=\frac{E\cos\omega t}{r+pL+\dfrac{1}{pc}}$$

$$=\frac{E}{L}\,\frac{p\cos\omega t}{p^2+2\alpha p+\omega_0^2} \tag{1}$$

where

$$\omega_0^2=\frac{1}{Lc}\qquad \alpha=\frac{r}{2L}$$

The solution of (1) for the oscillatory case is

$$I=\frac{E}{Z}\left[\cos(\omega t-\psi)-\frac{\omega_0^2\epsilon^{-\alpha t}}{\omega\omega_1}\cos(\omega_1 t-\psi_1)\right] \tag{2}$$

where

$$\omega_1^2=\omega_0^2-\alpha^2$$

$$\psi=\tan^{-1}\frac{\omega^2-\omega_0^2}{2\alpha\omega}$$

$$\psi_1=\tan^{-1}\frac{\omega_0^2(\omega^2-\omega_0^2)-2\alpha^2\omega^2}{2\alpha\omega^2\omega_1}$$

$$Z=\left[\left(\omega L-\frac{1}{\omega c}\right)^2+r^2\right]^{1/2}$$

Neglecting the decrement and the effect of resistance on the natural frequency ω_0, then $\omega_0=\omega_1$ and the maximum current will be

$$I_{\max}=I_0\left(1+\frac{\omega_0}{\omega}\right) \tag{3}$$

but since

$$\frac{\omega_0}{\omega}=\frac{1}{\omega\sqrt{Lc}}=\sqrt{\frac{x_c}{x_L}}$$

a simpler expression can be written as

$$I_{\max}=I_0\left(1+\sqrt{\frac{x_c}{x_L}}\right) \tag{4}$$

$I_0=\dfrac{E}{Z}$ = normal steady-state current

where

$Z=x_c-x_L$

Thus a simple conservative expression is obtained for the maximum inrush current. The normal current I_0 is increased by a factor equal to unity plus the square root of the ratio of the capacitive and inductive reactances of the circuit.

2. Energizing a Bank Adjacent to an Energized Bank

Neglecting resistance gives conservative results and is simpler to handle analytically hence this assumption will be made in the analysis of figure 3. This circuit represents a capacitor bank C_2 being switched onto the bus, on which there is already an energized capacitor bank. The maximum current will flow in C_2 when the switch is closed at the peak of the voltage wave, which corresponds to maximum charge on C_1. The response of the circuit to the closing of the switch at this time, and considering the discharge from C_1 as well as the feed in from the system can be determined by simply applying a voltage across the switch equal to that existing across the switch before it was closed.* With this concept, the current flow into C_2 upon closing the switch can be written as

$$I_2=\frac{E'\cos\omega t}{\dfrac{pL\left(pL_1+\dfrac{1}{pc_1}\right)}{pL+pL_1+\dfrac{1}{pc_1}}+pL_2+\dfrac{1}{pC_2}}=$$

$$\frac{E'}{L_0}\left[\frac{p(p^2+\omega_3^2)\cos\omega t}{(p^2+\omega_1^2)(p^2+\omega_2^2)}\right] \tag{5}$$

for which the solution is

$$I=-\frac{E'}{L_0}\left[\frac{\omega(\omega_3^2-\omega^2)}{(\omega_1^2-\omega^2)(\omega_2^2-\omega^2)}\sin\omega t+\right.$$

$$\frac{\omega_1(\omega_3^2-\omega_1^2)}{(\omega^2-\omega_1^2)(\omega_2^2-\omega_1^2)}\sin\omega_1 t+$$

$$\left.\frac{\omega_2(\omega_3^2-\omega_2^2)}{(\omega^2-\omega_2^2)(\omega_1^2-\omega_2^2)}\sin\omega_2 t\right] \tag{6}$$

where

$$\omega_1^2=A-\sqrt{A^2-B^2}$$

$$\omega_2^2=A+\sqrt{A^2-B^2}$$

$$\omega_3^2=\frac{1}{c_1(L+L_1)}$$

$$A=\frac{1}{2}\,\frac{c_2(L+L_2)+c_1(L+L_1)}{c_1c_2(LL_1+LL_2+L_1L_2)}$$

$$B^2=\frac{1}{c_1c_2(L_1L+LL_2+L_1L_2)}$$

$$L_0=\frac{1}{\left[L_2+\dfrac{L_1L}{L_1+L}\right]}$$

E' = maximum voltage existing across switch before closing
L = system inductance per phase in henries
L_1 = inductance per phase from bus to capacitor bank c_1 including current transformers, disconnecting switches, breakers, and connections
L_2 = same as L_1 except applying to c_2
c_{1-2} = capacity per phase in farads

3. Reduction of Inrush Currents

(*a*). Inductance as the Limiter

The analysis of the circuit represented by figure 3 given in part 2 above can be used for evaluating the effectiveness of inductance in the capacitor leads for reducing the inrush current. In this analysis resistance is neglected so the limiting effect of the inductance alone is given.

As a numerical example, the case referred to in the body of the paper will be taken which is commensurate with a large installation that might be made at a generation point. Let

C_1 = three 1,080-kva 4,000-volt banks
= 540×10^{-6} farads per phase
C_2 = one 1,080-kva bank
= 180×10^{-6} farads per phase
L = system inductance corresponding to that which would give 150,000-kva short circuit at the capacitor location
= 0.1065 ohms at 60 cycles
= 0.283×10^{-3} henries
$L_1=\dfrac{L_2}{3}$ since it represents three banks in parallel

Substitution of these values in equation 6 and letting L_2 vary, curve 1 in figure 4 can be plotted. The maximum current is obtained when all three components of equation 6 are in phase hence the sum of the coefficients represents the maximum. L_2 is plotted as ohms at 60 cycles for ease of comparison.

(*b*). Resistance as the Limiter

The solution of the circuit shown in figure 6 for closing the switch at the peak of the voltage wave will give a conservative value of inrush current through the switch when resistance only is considered as limiting the current flow from the charged bank. By neglecting the inductance in each branch leading to the capacitor bank the full effect of the current-limiting value of the resistance is evidenced.

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* This is known as Thevenin's theorem.

The maximum current which flows through the switch occurs at $t=0$ and is given by the simple expression

$$I_{\max}=\frac{E'}{r_1+r_2} \qquad (7)$$

Equation 7 can be used to evaluate the effectiveness of resistance in limiting current inrush through a breaker energizing a capacitor bank that is adjacent to an already energized bank.

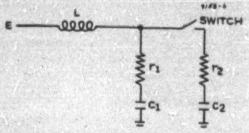

Figure 6. Circuit diagram of capacitors switched with resistors for limiting inrush currents

As a numerical example, the same case is considered as used above in evaluating the effectiveness of inductance.

Then

$$r_1=r_2/3$$

and varying r_1 over the same ohmic value as L_2 was varied, the results as shown in curve 2 of figure 4 are plotted.

Appendix B. Voltages Attending the Switching of Capacitors

1. Energizing a Single Capacitor Bank

The voltage at the terminals of a capacitor when it is energized may be readily obtained from the expression for current derived in appendix A, equation 1, such as

$$E_c=\frac{1}{pc}I$$

$$=\frac{E}{Lc}\,\frac{\cos \omega t}{p^2+2\alpha p+\omega_0^2} \qquad (8)$$

for which the solution is

$$E_c=\frac{EX_c}{Z}\left[\sin(\omega t-\psi)-\frac{\omega_0}{\omega_1}\epsilon^{-\alpha t}\times \sin(\omega_1 t-\psi_1')\right] \qquad (9)$$

$$x_c=\frac{1}{\omega c}$$

$$\psi_1'=\tan^{-1}\frac{\omega_1(\omega^2-\omega_0^2)}{\alpha(\omega^2+\omega_0^2)}$$

By neglecting damping

$$\omega_0=\omega_1$$

Then maximum transient voltage becomes

$$E_{c\max}=\frac{EX_c}{Z}2=\text{twice the operating voltage of capacitor}$$

2. Energizing a Bank Adjacent to an Energized Bank

The expression for the voltage at the terminals of the capacitor bank c_2, when the switch is closed in the circuit represented by figure 3 at the peak of the voltage, can be written directly from the expression for current given in equation 5 as

$$E_{c2}=\frac{1}{pc_2}I_2$$

$$=\frac{\omega_1^2\omega_2^2}{\omega_3^2}\,\frac{(p^2+\omega_3^2)}{(p^2+\omega_1^2)(p^2+\omega_2^2)}E'\cos\omega t \qquad (10)$$

for which the solution is

$$E_{c2}=E'\frac{\omega_1^2\omega_2^2}{\omega_3^2}\left[\frac{(\omega_3^2-\omega^2)}{(\omega_1^2-\omega^2)(\omega_2^2-\omega^2)}\cos\omega t+\frac{(\omega_3^2-\omega_1^2)}{(\omega^2-\omega_1^2)(\omega_2^2-\omega_1^2)}\cos\omega_1 t+\frac{(\omega_3^2-\omega_2^2)}{(\omega^2-\omega_2^2)(\omega_1^2-\omega_2^2)}\cos\omega_2 t\right] \qquad (11)$$

where

$$\frac{\omega_1^2\omega_2^2}{\omega_3^2}=\frac{1}{c_2L_0}$$

and the other terms are as defined in appendix A. At $t=0$, $E_{c2}=0$ which means that at that instant the fundamental-frequency voltage applied is opposed by the two natural-frequency voltages of the circuit. Hence by changing the sign of the two natural-frequency terms and adding all three coefficients, an expression for the maximum capacitor voltage is obtained, such as

$$E_{c2\max}=\frac{2E'\omega_1^2\omega_2^2(\omega_3^2-\omega^2)}{\omega_3^2(\omega_1^2-\omega^2)(\omega_2^2-\omega^2)} \qquad (12)$$

and since ω_1^2, ω_2^2, and ω_3^2 are all very much greater than ω^2,

$$E_{c2\max}\cong 2E'$$

which is twice the voltage that was applied to the oncoming bank.

3. De-energizing a Single Bank

The opening of a switch in a circuit is mathematically simulated by impressing through the switch the negative of the current that would be flowing if the switch were closed. Consider a single-phase or a three-phase wye neutral-grounded bank. With the above concept of opening a switch the expression for the voltage across the switch assuming the current goes out at a normal current zero, can be written as

$$E_{sw}=-I_0\sin\omega t\left[p(L_1+L)+\frac{1}{pc_1}\right] \qquad (13)$$

and solving

$$E_{sw}=E(E_{c1}-\cos\omega t) \qquad (14)$$

where,

$$E_{c1}=\frac{\omega_1^2}{\omega_1^2-\omega^2}=\text{voltage across the capacitor at the instant the switch is opened}$$

In general $\omega_1^2\gg\omega^2$ hence $E_{c1}=E$ and the maximum voltage across the switch in this case becomes approximately $2E$ if there is no restriking of the arc.

4. De-energizing a Bank Adjacent to an Energized Bank

Again considering a single phase on three-phase wye neutral-grounded bank (figure 3), and impressing $-I_0$ through the switch, the expression for the switch voltage may be written as

$$E_{sw}=-I_0Z(p)\sin\omega t$$

Where $Z(p)$ is the operational impedance of the circuit viewed from the switch as given in equation 5.

$$E_{sw}=-I_0\left[\frac{L_0(p^2+\omega_1^2)(p^2+\omega_2^2)}{p(p^2+\omega_3^2)}\right]\sin\omega t \qquad (15)$$

for which the solution is

$$E_{sw}=-I_0L_0\omega\left[\frac{\omega_3^4-\omega_3^2(\omega_1^2+\omega_2^2)+\omega_1^2\omega_2^2}{-\omega_3^2(\omega^2-\omega_3^2)}\times\cos\omega_3 t+\frac{\omega^4-\omega^2(\omega_1^2+\omega_2^2)+\omega_1^2\omega_2^2}{-\omega^2(\omega_3^2-\omega^2)}\times\cos\omega t+\frac{\omega_1^2\omega_2^2}{\omega_3^2\omega^2}\right] \qquad (16)$$

Equation 16 is composed of two oscillatory and one residual component. The maximum voltage will occur when they are all in phase, hence changing the sign of the oscillatory terms and adding their coefficients there is obtained an expression for the maximum voltage across the switch. Following out the above, there is finally

$$E_{sw}=E_{c2}\left(2-\frac{\omega^2\omega_3^2}{\omega_1^2\omega_2^2}\right) \qquad (17)$$

and since $\omega_1^2\omega_2^2\gg\omega^2\omega_3^2$ the maximum switch voltage comes out to be about twice that which was across the capacitor before the switch was opened.

5. De-energizing Delta-Connected Banks

Sections 3 and 4 above analyze the de-energizing of single-phase or three-phase wye neutral-grounded banks. The analysis of delta-connected banks requires separate consideration since the different phases cannot be considered as separate and independent. It is shown in appendix C that 180 degrees after the first phase of a delta bank opens, the switch voltage attains three times normal. However, when a three-phase breaker is used, the single-phase current flowing after the first phase clears, should clear 90 degrees later since it experiences a normal current zero at that time. This reduces the voltage across the first-phase-clearing contacts but if one of the two remaining contacts is slow to open or if one has a highly ionized path, two times line-to-line voltage or $3.46E$ can appear across the other.

Appendix C. Miscellaneous Considerations

1. Switch Voltages When One Phase at a Time Is Opened in a Three-Phase Installation

This condition may possibly occur in a three-phase interrupting device, or when three single pole air break disconnecting switches are used to control capacitor banks. In this analysis it is assumed that the switch opens at the normal current zero in which case the maximum residual charge is on the capacitors and further it is assumed there is no restriking of the arc.

(*a*). Wye-Connected Neutral-Grounded Capacitor Bank

Assuming the ground impedance from the capacitor bank to the nearest system ground point as negligible, the circuit in the clearing phase can be treated independently of the other phases. Hence the circuit of figure 1 and the analysis in section 3 of appendix *B* applies. Here it is shown that the switch voltage may reach twice normal.

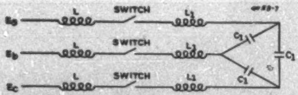

Figure 7. Circuit diagram of three-phase delta-connected capacitor controlled with three single-pole switches

(*b*). Delta-Connected Capacitors

Impressing the negative of the current through the switch, that would flow if it were closed, in the circuit shown in figure 7 and obtaining the expression for the voltage required to force this current through, the switch voltage is obtained as:

$$E_{sw}=-I_0\left[p(L+L_1)+p\left(\frac{L_1+L}{2}\right)+\frac{1}{2pc_1}\right]\sin\omega t \qquad (18)$$

solving and reducing

$$E_{sw}=\frac{3}{2}E(E_c-\cos\omega t) \qquad (19)$$

where

E = applied line-to-neutral voltage

$$=I_0\left[(L+L_1)\omega-\frac{1}{3c_1\omega}\right]$$

E_c = line-to-neutral voltage across capacitors before switch opened

$$=\frac{\omega_1^2}{\omega_1^2-\omega^2}$$

$$\omega_1^2=\frac{1}{3c_1(L_1+L)}$$

Neglecting the voltage rise through $L+L_1$ due to the capacitor current, the voltage E_c will be equal to E, hence when $\cos\omega t=-1$

$$E_{sw}=3E \qquad (20)$$

After the first phase is opened, there remains a single-phase circuit with a single-phase line-to-line voltage applied. Then opening the second phase at a normal current zero, two times line-to-line voltage or 3.46*E* will appear, 180 degrees later, across the switch. These voltages increase the degree of difficulty in de-energizing the higher-voltage and higher-current banks with single-phase air-break disconnecting switches.

2. Voltage Across Secondaries of Current Transformers

If adjacent capacitor banks are sufficiently closely coupled, the current-transformer inductances may represent a fair portion of the total inductance between the banks, in which case high transient voltages can appear across the current-transformer secondaries.

The voltage appearing across the inductance L_3 in figure 3 when the switch is closed is

$$E_{L3}=pL_3I_3 \qquad (21)$$

where I_3 is given by the operational equation (5) or its solution (6). At $t=0$ this becomes

$$E_{L3}=E'\frac{L_3}{L_0} \qquad (22)$$

Thus, it is seen that a good portion of E' might be impressed on the primaries of the current transformers, and this value stepped up through the secondaries may cause transient voltages requiring special attention. This phenomenon is truly rare since usually there are sufficient other inductances to share this burden, but it is mentioned here for the sake of completeness.

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3. Survey of Power-System Wave Shapes, Engineering Reports of the Joint Sub-Committee on Development and Research. Volume 2, pages 189–290.

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Discussion

For discussion, see page 1096.

Dynamic Characteristics of a Single-Phase Induction Motor

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FELLOW AIEE

SMALL-SIZE single-phase induction motors are used today principally on intermittent duty. In many cases this intermittent starting and stopping is automatically controlled. Loads are thus suddenly applied and as suddenly removed.

In order, therefore, to understand thoroughly the motor performance, it becomes necessary to observe its operation under impact conditions. Characteristics so obtained become the dynamic characteristics of the motor, as contrasted with its static characteristics.

It is the purpose of this paper, first, to outline the method and procedure employed in obtaining such characteristics, second, to present such characteristics for a typical single-phase repulsion-start induction motor, and third, to compare the mechanical output analytically determined with that actually recorded.

Rating of Motor. The motor used for this study, and the motor whose characteristics are shown hereafter, was rated as follows:

Three horsepower
110–220 volts
32.2–16.1 amperes
60 cycles
1,750 rpm

Laboratory Setup. Figure 1 shows a photograph of the laboratory test setup and figure 2 is a schematic diagram of the connections employed. In figure 1 the induction motor is shown directly coupled to a d-c generator which served as the load. The shunt field of this generator was separately excited, while the armature was directly connected to a resistance load. Closing or opening the field circuit would therefore apply or remove the load. A relay in the field circuit controlled by the oscillograph initiated the desired load impact.

Photoelectric Slip Meter. On the opposite end of the induction motor shaft the photoelectric slip meter, especially developed for these studies, is located. This slip meter[1] consists of two metal disks, each one having a series of small holes uniformly spaced on a circle. One of these disks is mounted on the shaft of the induction motor under test, while the other is mounted on the shaft of a small synchronous motor having the same synchronous speed as the induction motor. A lamp was so placed on one side of the pair of disks that its light would shine through corresponding holes in the two disks onto a photoelectric cell placed on the opposite side. The output of the cell is amplified and delivered to one of the galvanometer elements of the oscillograph.

Figure 1. View showing laboratory setup of single-phase induction motor study

Near end of motor shows special photoelectric slip meter developed for this test, and far end of motor shows d-c generator used as load. Oscillograph appears in the foreground

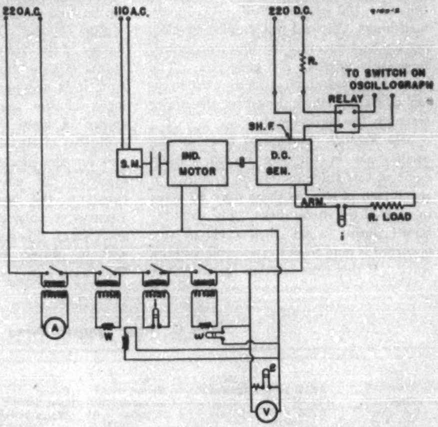

Figure 2. Schematic diagram of connections of the equipment shown in figure 1

Oscillograph. In the foreground of figure 1 also appears the six-element oscillograph employed in making the transient records during the load impacts. The following transients were simultaneously recorded: impressed voltage on motor, motor line current, motor input power, rotor slip, and generator armature current. The voltage record also served as the timing wave.

Application of 50 Per Cent Overload

Oscillogram. Figure 3 shows the oscillogram of the impressed voltage, line

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[1] For numbered reference, see end of paper.

Figure 3. Oscillogram recorded when 150 per cent load was applied to the single-phase induction motor

	E(a-c) Volts	I(a-c) Amperes	W(a-c) Watts	E(d-c) Volts	I(d-c) Amperes
No load	232	8.35	600		
Load on	226	20.25	3,880	214	12

$i = 0.530$ ampere per millimeter
$e = 21.9$ volts per millimeter
$w = 168$ watts per millimeter
$I(d\text{-}c) = 0.320$ ampere per millimeter
Load resistance = 17.8 ohms

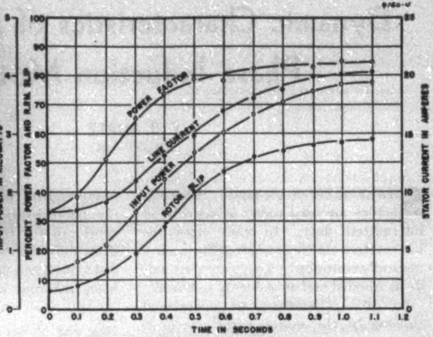

Figure 4. Graphs obtained from oscillogram (figure 3) showing variations of rotor slip, effective current, input power, and power factor during first second following the application of 150 per cent load

current, input power, rotor slip, and load variations recorded during the application of a 150 per cent load. As already mentioned, the load was applied by closing the field circuit of the directly coupled, separately excited, d-c generator. The instant of load application and the manner of its growth is shown by the upper trace on the oscillogram.

Slip Record. The lowest trace on the oscillogram is the slip record. Each cycle of this trace represents 1/30th of a revolution of slip, as there were 30 holes in each of the metal disks. The time interval during which each cycle of slip occurred is read from the 60-cycle voltage wave. If this time interval in seconds is denoted by t, the slip of the induction motor in revolutions per minute is $60/30t$ or $2/t$ rpm. From the readings for each cycle of slip the revolutions-per-minute slip of the rotor was computed. The calculated values of slip thus obtained are tabulated in table I and plotted in figure 4 for a period of approximately one second after the application of the load. It will be noted that at first there is little change in slip; this is followed by a rapid increase in slip, which in turn is followed by another period of decreasing change in slip as the final running speed is approached.

Line Current. To obtain the effective values of the line current at different instants of time during the transient period, it is necessary to sketch the envelopes of the maximum amplitudes as shown in figure 5. By measuring the ordinates of this envelope from the zero axis for any desired instant of time and applying the galvanometer calibration constant, the maximum amplitude in amperes is obtained. Assuming a sine-wave form, this must then be divided by $\sqrt{2}$ to obtain the effective current. Values so obtained are also tabulated in table I and plotted on figure 4 where comparison with the slip curve may be made. This comparison shows the current to vary approximately as the slip.

Power Input. Figure 5 also shows the envelope of the watt galvanometer deflections during the application of the load. By locating a locus midway between these two boundaries the average power variation is obtained. Applying the watt galvanometer calibration constant to the ordinates of this locus gives the average input power in watts at corresponding instants of time during the period of load impact. These values are also tabulated in table I and plotted in figure 4 alongside of the slip and current curves. The plotted graph shows the power to increase in value approximately as the slip, similar to the increase of current with slip.

Table I. Motor Transients During Application of 50 Per Cent Overload

(1) Time in Seconds	(2) Rotor Slip in RPM	(3) Applied Voltage in Volts	(4) Stator Current in Amperes	(5) Power Input in Watts	(6) Power Factor in Per Cent	(7) Power Released by Rotor in Watts	(8) Motor Losses in Watts	(9) Calculated Mechanical Power Output in Watts	(10) Recorded Mechanical Power Output in Watts
0	6	233	8.25	638	33.2	0	449	189	150
0.1	8	233	8.45	755	38.4	69	455	369	344
0.2	13	232	9.19	1,090	51.1	115	474	731	824
0.3	19	232	10.9	1,650	65.1	182	520	1,312	1,268
0.4	28	230	13.1	2,220	73.8	244	582	1,882	1,877
0.5	40	229	14.8	2,670	78.8	203	675	2,198	2,182
0.6	47	228	16.9	3,020	78.3	130	764	2,386	2,485
0.7	52	227	18.0	3,340	82.2	89	798	2,631	2,742
0.8	54	227	18.8	3,540	83.3	56	829	2,767	2,817
0.9	56	226	19.9	3,730	83.0	36	883	2,883	2,911
1.0	57	226	20.1	3,860	85.0	22	898	2,984	3,031
1.1	58	226	20.3	3,880	84.6	0	926	2,954	3,040

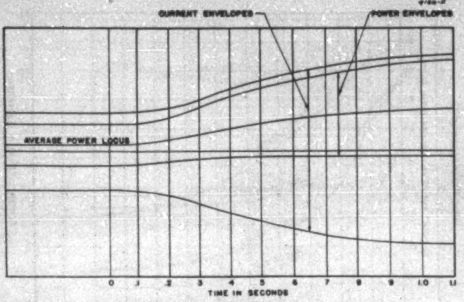

Figure 5. Boundaries of current and input power variations of oscillogram (figure 3) showing envelopes from which effective values of current and average power values were obtained

Locus drawn midway between upper and lower power envelopes gives average power ordinates. Amplitude of current variations must be divided by $\sqrt{2}$ to give effective values (sine wave assumed)

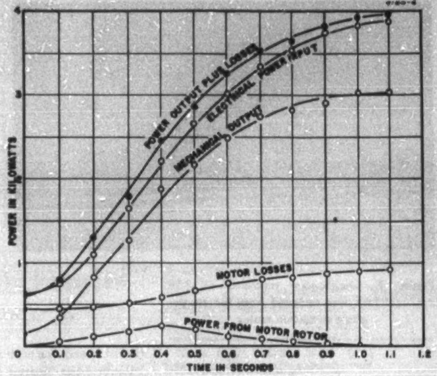

Figure 6. Graphs obtained from oscillogram (figure 3) showing variations in electrical input power, released rotor power, motor losses, and mechanical output, during first second following the application of approximately 150 per cent load

Power-Factor Variation. Having determined the effective value of line current and the average input power, the power factor may now be computed for corresponding instants of time, thus,

$$\text{p.f.} = \frac{P}{EI}$$

The effective value of impressed voltage was almost constant except for slight decreases during large inrushes in current. Computed values of power factor are given in column 6 of table I and are also plotted in figure 4. An inspection of the graph on this figure shows the power factor to start around 33 per cent at no load and to rise quite rapidly to approximately 85 per cent as the load is applied. After about 0.5 second the power factor has virtually reached its maximum value and is relatively constant during the period of further increase of load due to the approaching fixed relationship between current and power.

Mechanical Power Output. Ordinarily, under steady operating conditions, the output may be obtained directly by subtracting the motor losses from the input to the motor. Under transient conditions, however, where inertia effects must be considered, this is not possible. For example, sudden application of a load to an asynchronous motor, as in these studies, causes the rotor to slow up and consequently give up part of its kinetic energy. This kinetic energy then furnishes part of the power delivered to the motor shaft, thereby demanding a reduced electrical input. As soon as the new speed is reached this source of power disappears and the motor operates as under steady-state conditions.

As the purpose of this test was to determine the dynamic characteristics of the motor only, it was necessary to compute the kinetic effects of both motor and generator rotors separately. Ordinarily, all rotating equipment associated with the motor will contribute power in proportion to its moment of inertia. The equations used to compute either the power given up or absorbed by the rotating parts are given in the appendix. The essential data required for these calculations are the rate of change of speed at any given speed and the moment of inertia of the separate rotors. The first is obtained from the slip record and the second from comparison of frequency of oscillations with that of a body of calculable moment of inertia. Column 7 of table I gives the rotor power in watts, column 8 the motor losses, and column 9

Table II. Motor Transients During the Removal of 50 Per Cent Overload

(1) Time in Seconds	(2) Rotor Slip in RPM	(3) Applied Voltage in Volts	(4) Stator Current in Amperes	(5) Power Input in Watts	(6) Power Factor in Per Cent	(7) Power Released by Rotor in Watts	(8) Motor Losses in Watts	(9) Calculated Mechanical Power Output in Watts	(10) Recorded Mechanical Power Output in Watts
0	54	226	18.9	3,780	0.885	0	845	2,935	2,980
0.1	37	228	16.5	3,180	0.845	440	726	2,014	2,036
0.2	25	229	12.6	2,050	0.707	216	556	1,278	1,232
0.3	17	231	10.3	1,480	0.623	137	477	866	795
0.4	13	232	9.4	1,220	0.557	96	451	673	566
0.5	10	233	8.75	938	0.460	50	435	453	401
0.6	8	233	8.7	832	0.410	18	428	386	310
0.7	7	233	8.6	725	0.362		426	299	241
0.8	6	233	8.45	655	0.332		422	233	205
0.9	6	233	8.33	619	0.319		419	200	182
1.0	6	233	8.25	584	0.304		416	168	172

Figure 7. Oscillogram recorded as 150 per cent load was removed from the single-phase induction motor

	E(a-c) Volts	I(a-c) Amperes	W(a-c) Watts	E(d-c) Volts	I(d-c) Amperes
Load on	226	19.1	3,680	207	11.7
No load	231	8.2	562		

i=0.530 amperes per millimeter
e=21.9 volts per millimeter
w=177 watts per millimeter
i(d-c)=0.320 amperes per millimeter
Load resistance=17.8 ohms

the resulting mechanical output, obtained as follows:

Mechanical output = electrical input ± rotor power − motor losses

For a reduction in speed the algebraic sign of rotor power is positive, and for a rise in speed the sign is negative. Column 10 gives the actual recorded power output. The calculated output appears to be slightly low for low values of output and slightly high for large values of output.

The variations in these quantities are brought out in figure 6. It is of interest to note that the sum of the mechanical output and losses is greater than the electrical power input throughout the period of application of load. This, of course, is as it should be since the rotor is slowing down during this period and is giving up part of its kinetic energy.

At about t=0.4 second, at which time the slope of the slip curve in figure 4 is greatest, the power given up by the rotor has its maximum value. At this moment the rotor is supplying 13 per cent of the power output. Near the beginning and end of the transient period the rotor speed is changing very little, causing the "output plus losses" and "electrical power input" curves to almost coincide.

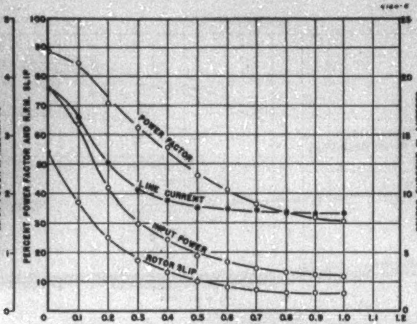

Figure 8. Graphs obtained from oscillogram (figure 7) showing variation of rotor slip, effective current, input power, and power factor during first second following the removal of approximately 150 per cent load

Removal of 150 Per Cent Load

Oscillogram. Figure 7 shows the oscillogram of the same quantities recorded during load application taken as the 50 per cent overload was removed. The load removal was accomplished by short-circuiting the field circuit of the d-c generator.

Figure 9. Graphs obtained from oscillogram (figure 7) showing variations in electrical input power, absorbed rotor power, motor losses, and mechanical output, during first second following removal of approximately 150 per cent load

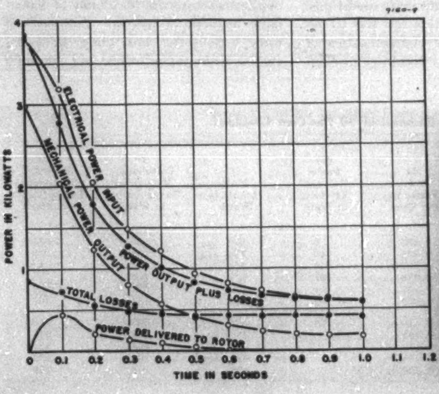

Figure 10. Oscillogram recorded as an excessive overload was suddenly applied to the single-phase induction motor

Extreme right end of oscillogram shows that governor has cut in the starting winding due to the large drop in speed

E(a-c) Volts	I(a-c) Amperes	W(a-c) Watts
233	8.25	600

i=3.03 amperes per millimeter
e=19.4 volts per millimeter
w=443 watts per millimeter
i(d-c)=0.808 ampere per millimeter
Load resistance=3.81 ohms

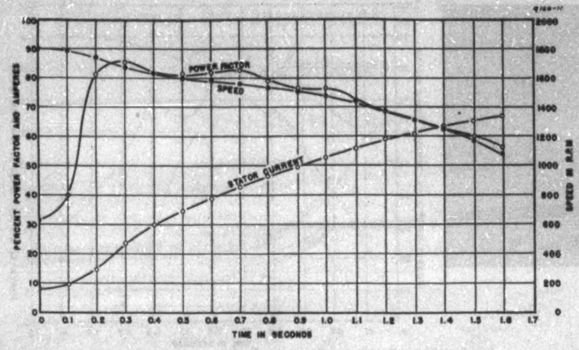

Figure 11. Graphs obtained from oscillogram (figure 10) showing variations in speed, power, power factor, and stator current during the first 1.6 seconds following the sudden application of an excessive load to the single-phase induction motor

The values tabulated in table II and plotted in figure 8 and figure 9 were obtained in the same manner as those of table I. In this case the electrical power input is always greater than the sum of the mechanical output and motor losses because of the absorption of kinetic energy by the rotor. During the removal of load the motor speed rises, which causes the motor rotor to absorb additional kinetic energy. The power input, hence, must be large enough to furnish this kinetic energy in addition to supplying the usual output and losses. Again, it will be observed that the calculated and recorded power outputs are in close agreement.

Load Application Versus Removal. In comparing the foregoing two sets of records it will be noted that the speed adjustment took place more promptly when the load was removed than when it was applied. Upon application of load the time of maximum speed reduction took place approximately 0.4 second after the load was applied, while the maximum speed rise took place approximately 0.1 second after the instant of load removal. Furthermore, the time interval during which most of the speed change took place was much shorter during the load removal than during application. This fact caused the rotor to absorb energy at a greater rate during removal than it released it during application. The computed values are 440 watts and 244 watts respectively. The 440 watts corresponds to 14 per cent of the motor input, and to 22 per cent of the mechanical power output.

Application of Excessive Overload

The oscillogram of figure 10 is a record of the same quantities heretofore considered when an excessive load is suddenly applied to the induction motor. In this case the load is large enough to cause the speed to drop to such a low value that the governor was caused to operate.

Inspection of figures 11 and 12 shows that the maximum power output obtainable from the motor is 6,280 watts. This is almost three times normal full-load value. Such a large output, of course, cannot be expected under steady-state conditions because approximately 1,300 watts of it is furnished by the kinetic energy of the rotor. During the entire period of load application the speed is decreasing rapidly, which makes it possible for the rotor to supply over one kilowatt of the mechanical output.

Further inspection shows that after $t=0.3$ second there is approximately a straight-line relationship between power factor and speed.

At the instant the governor operates, namely, $t=1.6$ second, the mechanical output has dropped to $1^1/_3$ times normal value, and the speed to approximately $^5/_8$th of rated speed. The motor losses, however, have increased to about seven kilowatts, which is equal to about $2^1/_3$ times the mechanical output. The line current at this instant is 67 amperes, which is about 4 times rated full-load current.

Starting From Standstill

For the sake of completeness the oscillogram, figure 13, showing the motor starting phenomena, is included. Figure 14 gives the characteristics as derived from the oscillogram.

At the instant of starting the motor draws a large starting current, 63 amperes, which decreases rapidly to 22 amperes at 0.6 second after starting. At this moment the governor operates, causing the motor to run as a squirrel-cage induction motor. Since the motor has only attained a speed of approximately 1,500 rpm at this moment another surge

Figure 12. Graphs obtained from oscillogram (figure 10) showing variations in electrical input power, released rotor power, motor losses, and mechanical output, during the first 1.6 seconds following the application of an excessive load

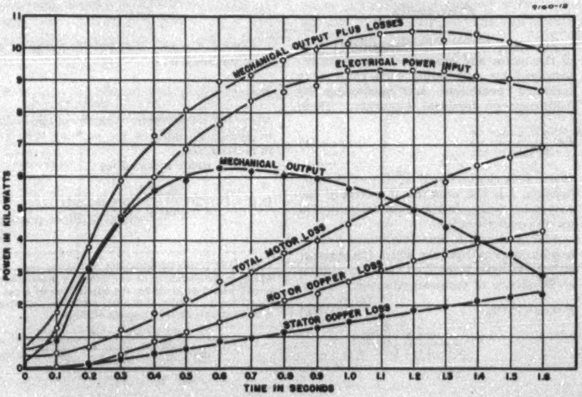

Figure 13. Oscillogram showing starting phenomena of the single phase, repulsion-start, induction motor

E(a-c) Volts	I(a-c) Amperes	W(a-c) Watts
233	8.3	600

$i = 1.90$ amperes per millimeter
$e = 19.4$ volts per millimeter
$w = 430$ watts per millimeter

of current takes place, reaching a magnitude of 59 amperes, which is almost equal to the initial starting current. This current, however, also drops off rapidly as the rated speed is approached. Full speed is reached in about 1.1 seconds after the line switch is closed.

Further inspection of figure 14 shows that the power fluctuates almost as widely as the current, attaining values in the neighborhood of 8,000 watts. The power factor starts at about 50 per cent, reaches a maximum value of about 80 per cent, and then drops to its normal no-load value of about 30 per cent.

Appendix

Moment of Inertia. The moments of inertia of the motor and generator rotors were determined by suspending each rotor as a torsional pendulum and measuring its frequency of torsional vibration. Then,

$$I = \frac{K}{4F^2\pi^2} \tag{1}$$

where K is the torsional rigidity of the suspension, I is the moment of inertia of the suspended body, and F is the frequency of its torsional vibration.

The constant K was determined by suspending a metal disk, for which the moment of inertia was calculated, and measuring its frequency of torsional vibration. Then,

$$K = 4\pi^2(F_d)^2 I_d \tag{2}$$

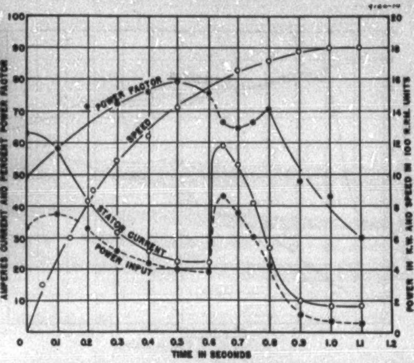

Figure 14. Starting characteristics of single-phase induction motor

where F_d is the frequency of vibration of the disk and I_d is the moment of inertia of the disk.

Equations 1 and 2 were then combined to give the expression for the rotor moment of inertia, thus,

$$I_R = \frac{4\pi^2 F_d^2 I_d}{4\pi^2 F_R^2} = \frac{F_d^2 I_d}{F_R^2} \tag{3}$$

The moment of inertia of a metal disk weighing 58 pounds was calculated and found to be 0.422 slug*-foot^2. This disk and the two rotors were each in turn suspended as torsional pendulums and their frequencies of vibration observed. These were as follows:

Metal disk	0.0881 oscillation per second
Motor rotor	0.198 oscillation per second
Generator rotor	0.310 oscillation per second

Then using equation 3:

$$I_{\text{motor rotor}} = \frac{(0.0881)^2}{(0.198)^2} \times 0.422 = 0.085 \text{ slug-foot}^2$$

and

$$I_{\text{generator rotor}} = \frac{(0.0881)^2}{(0.310)^2} \times 0.422 = 0.0341 \text{ slug-foot}^2$$

Kinetic Energy. The kinetic energy of a rotating body is:

$$\text{K.E.} = (1/2)I\omega^2 \text{ foot-pounds} \tag{4}$$

where I is the moment of inertia of the body in slug-feet^2 and ω is the angular velocity in radians/sec.

This is easily reduced to,

$$\text{K.E.} = (1/)2I\left(\frac{2\pi S}{60}\right)^2 \text{ foot-pounds} = 0.0055IS^2 \text{foot-pounds} \tag{5}$$

where S is the speed in revolutions per minute.

Multiplying equation 5 by $\frac{746}{550}$ gives,

$$\text{K.E.} = 0.00746\ IS^2 \text{ joules or watt-seconds}^{1} \tag{6}$$

Rotor Power. But

$$\text{Power} = \frac{d(\text{K.E.})}{dt} = 0.01492\ IS\frac{ds}{dt} \text{ watts} \tag{7}$$

The above power equation was used to calculate the power given up or absorbed by the motor and generator rotors. Values of S and ds/dt were taken from the slip curves which were plotted from the oscillographic slip records.

Substituting values of moment of inertia computed above, yields,

$$P_{\text{motor rotor}} = 0.01492 \times 0.085S\frac{ds}{dt} = 0.00128S\frac{ds}{dt} = \text{watts}$$

and

$$P_{\text{generator rotor}} = 0.01492 \times 0.0341S\frac{ds}{dt} = 0.000517S\frac{ds}{dt} = \text{watts}$$

Reference

1. Photo-Cell Recorder Measures Motor Slip, R. L. Witzke. *Electrical World*, July 18, 1936.

Discussion

For discussion, see page 1096.

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* A slug is a unit of mass and equals the mass to which an acceleration of one foot per second per second would be given by the application of a one-pound force. Under standard conditions one slug of mass equals 32.1739 pounds of mass.

Electronic Measurement of Surge-Crest Voltages

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Synopsis: An electronic voltmeter is developed for measuring surge-crest voltages, particularly in connection with laboratory lightning co-ordination tests of insulation breakdown. Simplicity of operation with high accuracy is obtained, as well as practical independence of time lags and of the form and duration of the waves encountered in engineering test work. A multiple bank of the crest-measuring units is applied in an instrument for giving directly the crest value for any particular surge in a single test to within one per cent accuracy. The instrument is applicable for general research on very short-time-duration surge-voltage phenomena as well as for convenient standardized laboratory testing of insulation.

QUICK determinations of crest surge potential values by a simple direct method are often desirable in the laboratory to supplement the cathode-ray oscillograph which records the entire surge phenomenon. The generally used sphere-gap method of potential measurement has, however, the disadvantages that a number of tests must be made in the process of adjusting for the breakdown distance, and sometimes the effect of time lag[1,2] of breakdown must be considered. In particular, it has been found in routine laboratory surge testing in connection with insulation co-ordination that sphere-gap measurements are erroneous for short duration potentials even with irradiation[3] of the gap when making tests of breakdown on the front of a sharply rising potential wave. Also on such chopped waves it is rather unlikely that the sphere-gap setting would be very close to the breakdown value and accordingly a large number of tests have to be made. It is at times essential that the actual breakdown voltage be recorded in a single test since some test objects can be broken down only once.

It was, therefore, considered desirable to develop an electronic potential-measuring device which would be about as simple as the spark gap, but which would give a direct peak reading in one test and would be practically independent of the test potential wave form and of time duration.

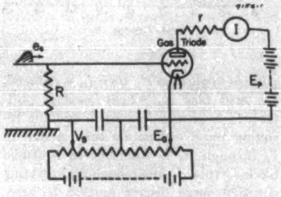


Figure 1. Measurement of positive surge-crest voltages by comparison with a negative bias on a grid-controlled gaseous-discharge tube

Surge-Crest Potential Measurements Principle, Using Gas Tubes

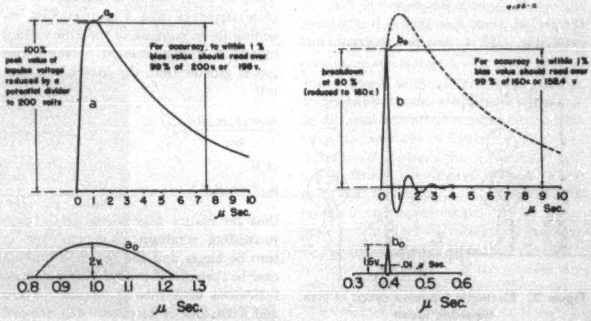
The bias-comparison method vacuum-tube crest voltmeter suggests itself as a possibility for measuring surge-crest voltages instead of steady-state crests if the vacuum tube is replaced by a grid-controlled gaseous tube. Thus in figure 1, with $V_s = 0$, the bias potential E_0 is adjusted just below the ionization discharge point for the tube. Then the additional negative bias voltage V_s is set roughly equal to the expected surge-crest value e_s and if e_s is greater positive than V_s, the grid rises above the critical ionization potential and the meter I indicates the resulting plate current due to neutralization of the space charge in the tube. The gaseous ionization process, once started, continues regardless of what later happens to the grid potential so that a permanent indication is obtained of when the crest surge value is greater or less than the bias V_s even though the surge is no longer present. The portion of the positive surge exceeding the negative bias potential serves as a triggering potential to set off the gaseous-discharge indication.

It is important that the triggering should respond even for very short surge duration. Two possible surge-potential waves whose crest-value measurements may be desired are shown in figure 2, for example. Curve 2*a* shows a standard 1x5-microsecond surge wave used in laboratory testing, reduced by means of a voltage divider to a value comparable to allowable grid potentials of the metering tube, for example, 200 volts; then if less than one per cent error is desired in the voltage indication the comparison bias potential would have to be at least

Figure 2. Typical surge-potential wave forms in laboratory test work

(*a*)—Standard 1x5-microsecond surge wave
(*b*)—Breakdown on the front of the wave

The crest values are measured accurately to within one per cent if a trip indication is obtained due to the resultant potential increments (a_0) and (b_0) of the part of the waves (*a*) and (*b*) exceeding their indicated comparison bias values



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1. For all numbered references, see list at end of paper.

198 volts. This leaves the very small resultant positive wave segment of two volts maximum magnitude and roughly 0.2-microsecond average time duration (as shown by a_0 in figure 2) for triggering. Curve *b* shows the same voltage wave with the breakdown of a test object occurring on the front of the test wave. Below it b_0 indicates the small resultant segment of 1.6 volts maximum magnitude and of the very short average time duration of less than 0.01 microsecond for which triggering should be completed to give measurement accuracies to within one per cent.

There is little doubt but that a gaseous tube could be designed to function as outlined above, for example, an ionization process in the Geiger counter[4] is initiated by the action of a very few ionizing electrons. Preliminary tests of available commercial gas tubes, however, indicated a dependence on surge duration such as to make doubtful a response to the tripping potentials of figure 2*b* within one per cent accuracy. The simple circuit of figure 1 would be quite suitable for a great deal of surge testing of greater time[5] duration; and in one application an assembly of ten surge-potential-measuring units using type 885 gas tubes was built up for measuring potentials at ten different points along a special transmission line. For greater sensitivity it was thought preferable to develop a scheme whereby the previous gas-ionization triggering action is obtained by a purely electronic relay method using available standard vacuum tubes.

An Electronic Relay of High Triggering Speed

A relay circuit* developed for very fast response to short-duration potentials is shown in figure 3. The high-vacuum triode *A* normally passes current I_A through R_2, and E_A is so adjusted that $E_A'-I_AR_2$ gives a net negative bias on the grid of triode *B* so that I_B is relatively negligible. If a negative current impulse is applied to R_1, then I_A is reduced, the grid bias $E_A'-I_AR_2$ becomes positive, I_B is greatly increased, and the resulting negative bias on tube *A* due to I_B through R_1 is sufficient to continue blocking tube *A* even though the initiating negative surge decays quickly to zero. Thus an ammeter in the plate circuit of tube *B* will show a permanent deflection after an impulsive tripping, while an ammeter in the plate circuit of tube *A* indicates an impulsive tripping by its new zero position. The instrument is reset by short-circuiting resistance R_1.

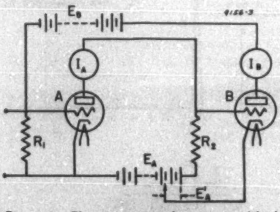

Figure 3. Electronic trip-relay circuit of high triggering speed

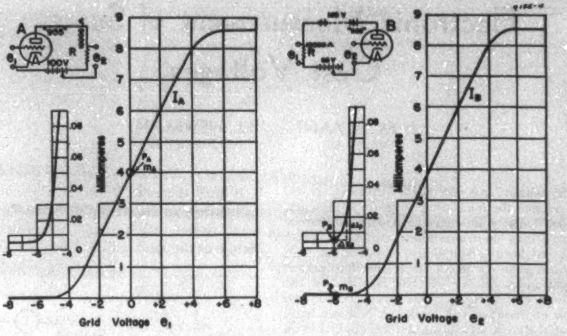

Figure 4. Characteristic operating conditions of the relay-circuit tubes, in the untripped position

The critical balance position as to which tube is passing current depends on the tube and circuit characteristics. The curves in figure 4 show the individual variations of the plate currents of tubes *A* and *B* as a function of grid voltages in their respective circuits Point P_A represents the untripped state of tube *A* plate current, and P_B that of tube *B*. An increase of the grid voltage e_2 of tube *B* by plus Δe_2 would cause a potential change of the grid of tube *A* equal to minus ΔI_BR_1, or minus $\Delta e_2m_BR_1$ where m_B denotes the slope $\Delta I_P/\Delta E_g$ at point P_B. This, in turn, causes a decrease in the plate current of tube *A* of $\Delta e_2m_BR_1m_A$ resulting in an increase of the grid voltage of tube *B* of $\Delta e_2m_BR_1m_AR_2$. Now if this last is greater than the initial Δe_2, that is if

$$\frac{\Delta e_2m_BR_1m_AR_2}{\Delta e_2}>1$$

or if

$$m_BR_1m_AR_2>1$$

then no matter how small Δe_2 is, each succeeding resultant increment will in turn be larger and the circuit will swing over to the reverse condition of a tripped indication with tube *B* passing current and with tube *A* blocked. The tripped position is stable, because of the high positive biasing resulting on tube *B* from the positive voltage E_A' (figure 3) with the disappearance of the negative biasing voltage I_AR_2, when I_A approaches zero.

The critical untripped balance position is obtained with the tube operating conditions of figure 4 just below the condition $m_BR_1m_AR_2=1$, by adjusting the value of E_A' in figure 3. It is apparent that the circuit might be adjusted so near the balance point that the slightest triggering impulse will cause the swing over. Actually there will be limitations, due to effects such as the Schottky[6] statistical variations of electron emission and temperature fluctuations, preventing too close approach to the critical balance position. However, the first tube *A* of the relay circuit acts also as an amplifier for the triggering impulse and, as may be seen in the following application of the relay circuit to potential measurements, very stable operation is possible, considerably below the balance point, with initiating impulses of even less than 10^{-8} second duration.

Surge-Potential Measurement With the Electronic Relay

A surge potential of positive polarity may be measured by determining the maximum additional negative bias it will balance on a screen-grid tube ahead of the relay circuit of figure 3, as shown in figure 5. Care must be taken in keeping the time constants determined by the stray capacitances as small as possible by careful choice and arrangement of the circuit elements to obtain short connecting leads and low interacting capacitances. For instance, the direct inclusion of a potential source E_B as in figure 3 for the relay circuit would introduce large capacitances to ground resulting in undesirably large circuit time constants. Instead, a small charged capacitor C_B, figure 5, furnishes a stable potential source in the transient process, during which the charging resistances r effectively isolate the capacitances of the supply source. The first tube, which compares the applied surge against its grid bias potential, must have a screened grid to prevent direct capacity transfer of the surge to the relay circuit through the tube grid-plate capacitance.

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* Similar circuits in which one tube blocks another in a reversible manner have been used in other applications; some of the earliest are by W. H. Eccles, and F. W. Jordan, *Radio Review*, volume 1, 1919, page 143, and by L. B. Turner, *Radio Review*, volume 1, 1920, page 317. Another application of a similar circuit was made by K. J. Sixtus and L. Tonks, *Physical Review*, volume 37, 1931, page 935, for measuring time intervals of the order of 10^{-4} second.

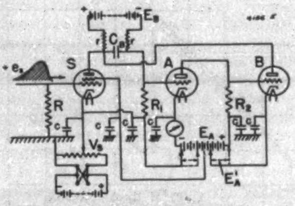

Figure 5. Electronic-relay surge-crest-voltmeter circuit diagram

Constants of the standard tube unit:

S—Type 36 tube
A, B—Type 37 tubes
R—1 megohm
R_1—10,000 ohms
R_2—17,500 ohms
r—15,000 ohms
C_B—0.001 microfarad
C—0.1 microfarad
E_A—220 volts
E_A'—120 ± 10 volts
E_B—320 volts

To reduce the vacuum-tube capacitances to a minimum, "acorn" type 954 and 955 tubes were used in the first experimental unit. It was found possible to substitute lower-cost standard radio tubes of type 36 and 37 and retain sufficiently high tripping sensitivity. Other types combining the functions of several tubes in one could be used, but the reliability and economy of modern vacuum tubes is such that the number of tubes is not of primary importance.

A Direct-Reading Surge-Crest Instrument

The single-unit vacuum-tube instrument is free from ionization difficulties, but the need for several adjustments still remains in order accurately to find the crest value by the trial method of adjustment. In addition, sometimes, as in testing an object which can break down only once, or in the testing of objects whose characteristics may vary with a large number of potential applications, it is desirable to have an instrument which will give direct readings on the potential application for which the crest value is required. A direct reading may be obtained with the bias comparison type of surge-crest meter previously considered if a number of units are used with the individual biases adjusted in regular steps to cover the range of voltages expected. The accuracy will, of course, now depend on the range of the biasing potentials in relation to the measured voltage value, as well as on the inherent accuracies of the individual voltage-measuring units. Such an instrument is shown in figure 6 which is composed of ten separate surge voltmeter circuits like figure 5, operating on individual power supplies, but connected to a common control board, thus allowing convenient change of the ranges of the regularly stepped biasing potentials.

It is generally desirable that the surge potentials be measured with reference to the ground frame of the instrument as zero potential. Consequently, in order to allow variations of the bias of any of the units independently, each unit is self-contained with its power supply quite separate. Each individual unit has the same circuit, shown in figure 5, and uses ordinary standard vacuum tubes. The potentials E_A and E_B are supplied, however, by two individual filtered rectifier circuits in each unit; the stability of the trip circuit is sufficient so that no trouble is encountered due to slight voltage variations in the regular power supply mains. The bias battery voltage, V_B, is checked by meter *A* in figure 6.

In operation the instrument with switch *R* (figure 6) in neutral position gives indications of the surge voltage in ten per cent steps of its range. After any indication the instrument is reset simply by depressing switch *S*. The instrument is changed to indicate one per cent steps by depressing switch *R* which shifts the bias of the component units to one per cent bias steps plus a common biasing voltage adjusted for the ten units simultaneously by a single tap switch to the nearest ten per cent step indication of the first run. Then in a second run the voltage can be determined to within one per cent. If the breakdown voltage of some test object is known approximately, as is often the case, to within ten per cent, then it is possible to obtain the actual breakdown voltage value to within one per cent in a single test.

Experimental Precision of the Surge Voltmeter

Verification of the accuracy of the electronic trip-circuit surge voltmeter on short-duration waves required the design of an accurate surge-voltage calibration circuit. A direct comparison with some other method of measuring surge voltages was not possible because neither of the commonly used methods of a carefully irradiated spark gap nor the usual cathode-ray oscillograph could be depended on to within one per cent accuracy in the case of the very short duration potentials encountered. In order to produce known surge potentials to within about one per cent calibration accuracy, an automatic-control impulse circuit was built, as indicated in figure 7. This control circuit determines accurately and automatically the desired voltage of the capacitor *C* before it is discharged to produce the surge wave. The surge peak potential $e^{\max}$ is directly a function of the initial charge potential *E*, which is accurately known. If the impulse-circuit constants are such that oscillations are well damped (roughly if $Z \leqq {}^1/_2 \sqrt{L_s/C_s}$,

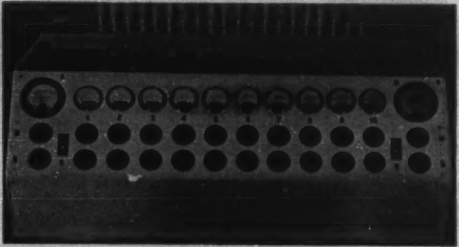

Figure 6. Direct-reading surge-crest voltmeter instrument

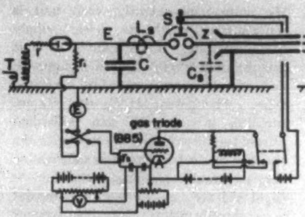

Figure 7. Surge-generating circuit used in calibration tests, with automatic control of voltage by a gas tube relay arrangement

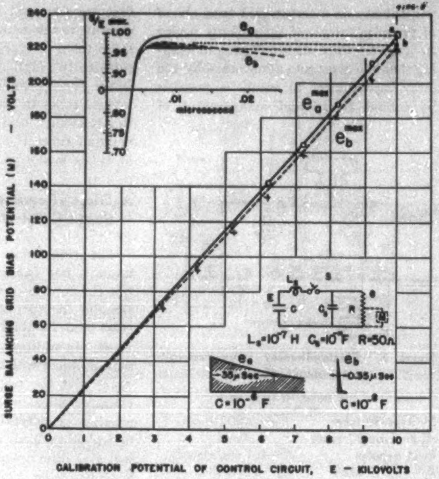

Figure 8. Experimental test of the accuracy of the electronic-relay-type surge-crest voltmeter

The circles indicate the experimentally obtained readings of the crest voltmeter for the wave (e_a) and should lie on the straight line giving the theoretical variation of (e_a^{max}) as a function of E

Similarly, the crosses obtained for (e_b) should lie on the dashed line. The deviations from the respective lines indicate the errors to be within one per cent for wave e_a, and within two per cent for wave e_b

which is the case in the circuits of figures 7 and 8) the surge peak potential reaches closely the same value as the initial potential of the capacitor C, providing the capacitance C is relatively large. This is illustrated by the curve e_a in the upper insert of figure 8; which is calculated for the given circuit constants with $C=1$ microfarad. Thus, for the wave e_a, it is established that the surge-crest value is practically the same as the potential of the capacitor C just *before* switch S closes; this gives a surge voltage whose crest values are known to be the d-c calibration potentials E for the control circuit.

The direct-reading voltmeter of figure 6 is connected to a section of R acting as a voltage divider in the position indicated by M in figure 8. For the wave e_a, a control setting of $E=10$ kv gives a surge crest known mathematically, by the previous analysis, to be very closely 10 kv; the corresponding bias reading on the crest voltmeter was found to be 228 volts. This experimental point was checked with other large values of C from one-half to four microfarads, which all gave the same e^{max}/E very closely to unity. These preliminary tests established the voltage divider ratio to be 228/10,000. A straight line drawn through the origin from the 10-kv bias reading, indicated by the square on figure 8, should give the theoretical true curve for the relation of the meter bias values to the surge-crest values, which in the case of the wave form e_a are the same as the potentials E. The relationship should be a straight line because the only circuit element which cannot be considered truly constant, the control spark-gap resistance which varies as a function of the current passed,[7] is reduced to a negligible factor by operation of the gap at high gas pressures. The surge-crest potentials, therefore, vary in magnitude directly as the calibrating potential E. Thus, if the surge-voltmeter bias values correctly indicate the peak value of e_a, they should lie on that straight line as E is varied; and the fact that the experimentally found points indicated by the circles do lie closely on that line proves an accuracy of within one per cent for a surge wave of the type e_a shown in the lower insert of figure 8. Thus it is directly determined by experiment that the surge voltmeter will record the standard 40-microsecond duration wave peak values to well within one per cent accuracy. Quite apparently also the accuracy on the standard 1x5-microsecond wave of figure 2*a* could, by comparison, also be expected to be within one per cent. The next test was made with an extremely short duration wave of 0.35 microsecond to half value (e_b) to correspond with the chopped test wave resultant of figure 2*b*. The short wave e_b of figure 8 is obtained by reducing the capacitance of C to a value of 10^{-8} farad; and now it is found by calculation[8] that the theoretical value of e^{max}/E is only 0.98 instead of closely unity as before. Therefore, the line on which the new experimental points should lie (for e_b) is the dashed line crossing the 10-kv ordinate at point *b*, corresponding to $e^{max}/E=0.98$, as indicated in figure 8. It will be seen that the experimental points for e_b, indicated by crosses, although in fair agreement, all lie somewhat below the theoretical dashed line. It is quite probable that for the very short duration peak of e_b an appreciable voltage magnitude is required to complete the tripping process, as indicated by the cross-sectioned part of the peak of e_b in the upper insert of figure 8. This indicates that for a wave as short as e_b the error may go as high as two per cent, although, knowing the character of the wave, a partial correction may be made allowing for that type of discrepancy. By comparing the above results with the requirements indicated in figure 2 and in previous discussion, it may be concluded that the surge voltmeter will readily measure peak potentials to within two per cent accuracy on any of the possible waves usually encountered in engineering laboratory testing. With some care in adjusting the ratio range so that the measured peak will fall near the full scale of the instrument, accuracies of one per cent are apparently possible.

It is of interest to note that the experimentally obtained accuracies include the accuracy of the control circuit of the impulse generator. The control circuit accuracy must, therefore, also have been better than within one per cent error. Detailed calculation including the distributed constants of large impulse generators[9] have shown the same direct proportionality of the resultant surge voltage to the charging voltage, providing the initiation of the surge generator is accomplished by simultaneous timing impulses applied at a number of the surge-generator intermediate gaps to insure uniform initiation. The same method of control is, therefore, applicable to large surge generators and would indeed be advisable for front-of-wave testing where it is essential to maintain the same wave-front form.

In the measurement of high surge potentials of short duration, the voltage divider used must be carefully designed[10,11] so as not to introduce appreciable errors. In the tests of this paper a resistance divider was used in shunt with a shielding capacitance divider of the same ratio.

Throughout the discussion the crest voltmeter was considered for only positive polarity surges. Experiments have indicated the adaptability of the instrument for negative polarities as well by introducing a switching arrangement to change the circuit to the dashed connection shown in figure 5. But it was found simpler and sufficient to leave the crest voltmeter responsive to only positive polarity and when necessary reverse the polarity of a negative surge in the coupling transmission-line system.

The crest voltmeter instrument has been found also particularly useful in the direct measurement of surge-current crests in conjunction with proper shunts. Other applications have been made which indicate the rapidity of response of the crest voltmeter, as, for example, in the measurement of time lags of electrical breakdown less than 10^{-8} second duration described in another paper.[12]

Summary

An electronic peak voltmeter is developed for measuring surge voltages of short duration. Simplicity of operation is obtained with practical independence of time lags and of formative-time ionization difficulties, such as those encountered by the usual spark-gap method, in the measurement of short-duration potentials in front-of-the-wave breakdown tests.

Direct recording of any particular surge-crest potential, without the requirement of a large number of tests, is obtained in an instrument assembly composed of ten surge-crest voltmeter units biased in regularly stepped intervals. The entire voltage range can be covered in ten per cent steps, and when the voltage is known to within ten per cent the instrument is switchable to cover any required ten per cent range in steps of one per cent.

The direct-recording surge-crest voltmeter was tested experimentally. A specially designed automatically controlled surge-generating circuit was used for calibration purposes. Accuracies of within one per cent were determined possible for measurements on the standard 1x5-microsecond and $1\frac{1}{2}$x40-microsecond surge waves. Less than two per cent errors were obtained even on a very short duration potential wave of less than 0.4 microsecond to half value, corresponding to the chopped waves resulting from front-of-wave breakdown.

With properly designed voltage dividers, the electronic peak voltmeter instrument may be used conveniently in place of the spark-gap method, particularly for very high impulse voltages, where the spheres and spacings become large and time lag difficulties are encountered for short duration potentials with the spark-gap voltmeter. By controlling also the output voltage of the impulse generator, after the manner used in the calibration circuit of this paper, it may be possible to obtain surge-voltage measurements of closely reproducible character, with over-all accuracy of measurements to within one or two per cent on general test work in the laboratory.

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Discussion

For discussion, see page 1093.

Measurement of Very Short Time Lags

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Synopsis: Direct-reading measurements of time lag in surge circuits were obtained by an electronic relay method using two surge waves on a calibrated artificial transmission line. Time definitions were obtainable down to 10^{-9} second. Although a cathode-ray oscillograph was designed to give comparable recording speeds, the electronic relay method had advantages of simplicity and direct readings, making it a useful auxiliary instrument for rapid work.

THE time lag of electrical breakdown of various air gaps and solid insulations is of particular importance in co-ordination[1] of insulation for transient lightning surges. Generally, time lag of breakdown is in part a function of chance ionization conditions in the breakdown path and therefore the measurements become often of a statistical nature.[2] Thus, where a large number of measurements are to be made, a direct method giving immediate readings free from the delays of photographic development would be a valuable auxiliary to cathode-ray oscillography.

In a previous paper[3] an electronic surge-crest voltmeter is described. Using such voltmeter units to determine the voltages of an incoming surge wave at definite time intervals in relation to another reference timing wave it is possible readily to make direct determinations of time lags down to 10^{-8} second with accuracy of definition of 10^{-9} second.

The Method of Measuring Time Lags

The element of time is introduced by means of an electrical transmission line on which the velocity of surge travel is easily calculable. The general principle of the time lag measurements is shown in figures 1 and 2, each of which shows the time relations of the traveling waves at four successive instants.

At intervals of distance along the transmission line corresponding to the desired time of travel positions, there are placed the series of electronic potential recording instruments, 1, 2, 3, . . . to 10, which are biased to respond to positive impulses of very short time duration and do not respond to negative potentials. At one end of the line there is shown arriving the surge S of positive polarity, and at the other end the negative timing impulse, T, which is synchronized in relation to the potential which causes surge S. Figure 1 indicates conditions where there is no time lag between the arrival of T and S at their respective ends of the line; the two waves will just meet at the center as at B and meters 10, 9, . . . to 6 inclusive will be tripped by the passage of the positive surge S. The rest of the meters will remain untripped because due to the large negative timing surge T there is only a dip in the negative potential as shown in D, but no net positive impulse resulting from S beyond the meeting point.

In the study of time-lag characteristics of a gap, the positive surge S is produced by the breakdown of the gap, and the time lag t caused by the formative spark process delays the arrival of the surge S in relation to the timing wave T, as indicated in figure 2A. Then, as may be seen in figure 2B, when the timing wave T arrives at the center, the surge S will be delayed by the distance $d=vt$, where v is the velocity of the wave travel along the line. The two waves will now meet at $d/2$ to the right of the center, and the meters 6 and 7 in that interval will also be untripped. It is clear, therefore, that the number of meters remaining un-

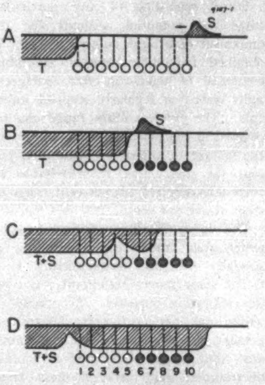

Figure 1. Traveling-wave potentials along the transmission line at the different instants of time, A, B, C, and D

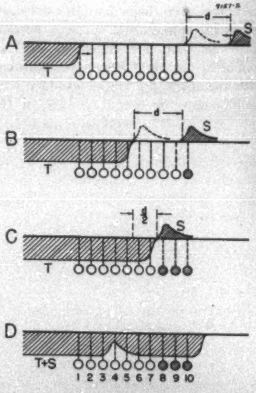

Figure 2. The traveling wave potential relations with the surge S delayed a distance d = vt in case of a time lag t

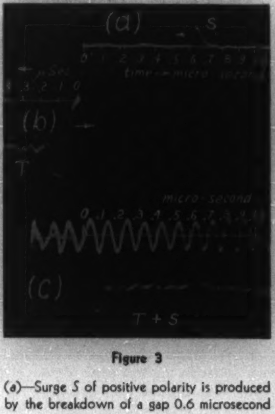

Figure 3

(a)—Surge S of positive polarity is produced by the breakdown of a gap 0.6 microsecond after voltage application

(b)—Surge T of negative polarity is synchronized with the initial time of voltage application to the gap studied

(c)—The trace $T+S$ shows how the component waves S and T applied at each end combine at the center of the transmission line

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[1] For all numbered references, see list at end of paper.

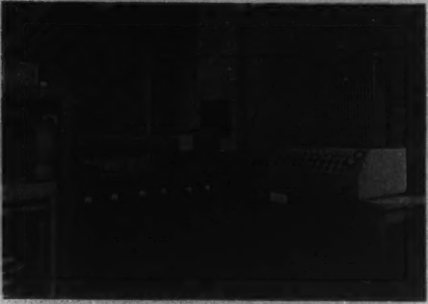

Figure 4. View showing the "dual wave" time-lag measuring instrument and the cathode-ray oscillograph interconnected at the middle point of the transmission line for comparative measurements within the oscillograph range

tripped on the positive side of the electrical center (the meeting point in the case of zero time lag) indicates the magnitude of the time lag, which determines the distance that the blocking negative timing surge *T* had a chance to travel in the additional interval of half the time lag of arrival of the surge *S*, before meeting the latter.

The oscillograms of figure 3 give experimental illustration of the wave relationships, but on a variable-time fixed-point basis as compared with the space distribution of figures 1 and 2. The cathode-ray oscillograph recorded the voltage at the electrical center of the artificial transmission line. Oscillogram of insert *a* gives just wave *S* coming in from the right side of the line with the other end short-circuited. Oscillogram *b* gives the wave *T* coming from the left side of the line with the right-side short-circuited. And oscillogram *c* shows how both waves appear combined. With the oscillograph located at the electrical center the oscillograms give the correct time shift corresponding to the time lag involved; the electronic lag-measuring assembly functioned at the same time and gave a direct lag indication of 0.6 microsecond in agreement with the oscillograms. A photograph of the apparatus in figure 4 shows the interconnection of the cathode-ray oscillograph, the electronic instrument, and the artificial transmission line.

Short Time Lags

On the front of a steeply rising voltage wave the time lag of breakdown may be very short depending on how quickly overvoltages are reached. To study the characteristics of such breakdown two methods of approach are possible and the results of both together are necessary for a complete picture of the phenomena involved. Thus, first, to study the basic characteristics of time lag it is desirable to eliminate the applied voltage wave-shape variation as a factor by making the wave shape nearly rectangular and to study time lag as a function of overvoltage. Second, it is desirable to study the time lag under conditions often encountered under lightning surge conditions in practice, that is, time lag as a function of a voltage rising linearly with time.

In the first, more fundamental approach, recent work of a quantitative character on time lags has been done by A. Tilles[4] in the interval between 10^{-5} and one second for low overvoltages; while for higher overvoltages the short time lags between 10^{-9} and 10^{-6} second have been further investigated by H. J. White[5] and R. R. Wilson[6]. The work by Tilles was done for relatively long time lags with a highly accurate direct-reading scheme in which a vacuum tube acted as a valve charging a capacitor in the interval between the application of potential to the gap studied and breakdown of the gap.

The work with very short time lags by White and Wilson was done by using Kerr cell shutters operated by the voltage initiating breakdown. The arrival of voltage at the Kerr cell was delayed by a transmission line which was manually varied in length until the Kerr cell shutter closed just as the gap broke down. The Kerr cell methods necessitated considerable adjustment and a large number of tests, and since there were considerable discrepancies between different methods of applying the Kerr cell measurements, the dual-wave method of this paper was developed and applied to the direct measurement[7] of very short time lags.

Results in the measurement of very short time lags by the dual-wave method and by Kerr-cell methods are given in figure 5 with the respective circuit diagrams used. If one takes into account small errors inherent in the Kerr-cell measurement setups mentioned by White and Wilson in their own respective papers and discussions, the results by the Kerr-cell methods are brought into close agreement with each other and would fit much more closely to the dual-wave results.

Diagram *C* in figure 5 gives the setup used. The measurements were made for the case of a pair of polished brass spheres $^{3}/_{4}$ inch in diameter separated for 10,000-volt breakdown under steady-state potential, and with strong ultraviolet irradiation of the surfaces from a quartz mercury lamp at a distance of 2.5 inches from the gap. The kenotron supply metered by *V* in the diagram was used to note the effect of a steady partial approach voltage across the gap *g* prior to the impulse application. It is required to find the lag between the beginning of electrical breakdown *g* and the initial application of surge potential to *g*. Potential is applied to *g* through the closing of *S* when the impulse-generating capacitor reaches the desired value of voltage. As soon as *S* closes current flows through *Z* sending a negative surge down the transmission line; this serves as the timing wave *T* since it is the same wave causing breakdown of the gap. When the gap *g* breaks down current flows through the other end of *Z* in the opposite direction for a time depending on the distance *h*, sending a positive surge corresponding to the wave *S* of figure 2. The time lag between the two surges is directly recorded as previously discussed.

It is important that the potential applied to gap *g* be initially as nearly rectangular in form as possible if the time lags are to be a function of overvoltage only and not of wave front. To keep the transient spark resistance of the control switch gap *S* from flattening the wave front, the switching gap assembly was operated at high pressures of about 30 atmospheres in order to lower the spark resistance to a negligible value. The pressure-chamber assembly is connected directly to the transmission line and mounted on a tank inside of which the potential-supply capacitor is immersed under oil to keep the length and therefore the stray inductance of the connecting leads to a minimum.

The Time Intervals and Their Calibration

The time intervals are directly proportional to the electrical length of the artificial-transmission-line section between the recording meters. Different

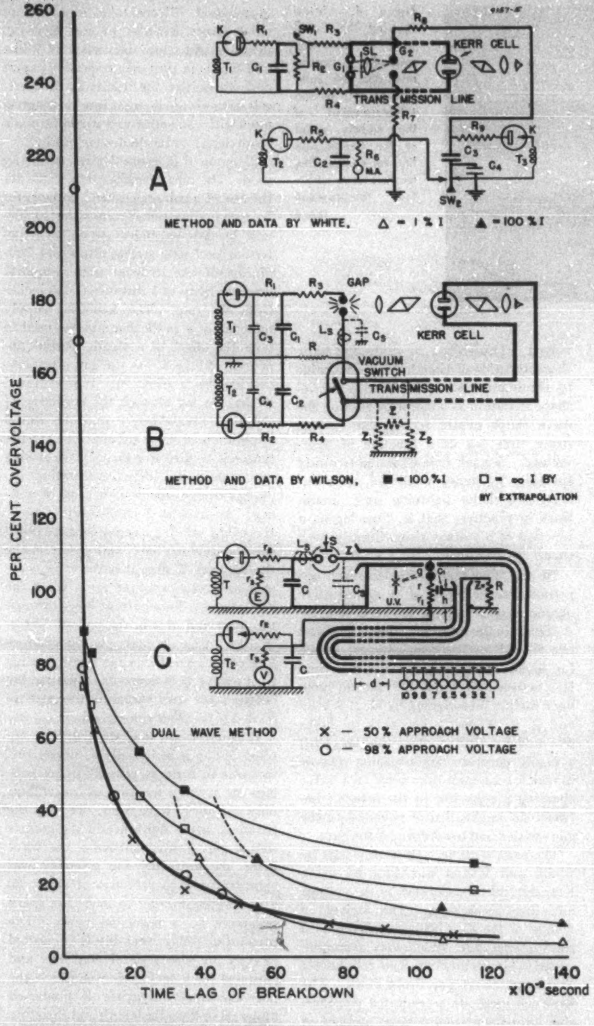

Figure 5. Time lag of spark breakdown as a function of over voltage for a short sphere gap with intense ultraviolet irradiation of the sphere surfaces

types of artificial lines were used for different time ranges. A transmission line covering the ranges of 1 to 10 and 10 to 100 microseconds was constructed using shielded sections of lumped inductance and capacitance. The transmission line used in obtaining the directly checking oscillogram of figure 3 covered the range of 0.1-microsecond units; that line was made in cable form by slipping rubber-covered wire inside small-diameter (one-quarter-inch) brass tubing. For shorter time lags, below 0.1 microsecond, the transmission line was made of concentric brass tubing with air dielectric throughout except for small centering washers of rubber. The transmission-line units were made into an assembly of 100 short lengths so designed that the inner pipes may be readily reconnected in relation to each other and the recording units. This transmission-line assembly is located (figure 4) just over the recording units of the electronic instrument, so that the coupling connection between selected points of the transmission line and the vacuum-tube control grids is kept very compact and does not affect appreciably the characteristics of the line. The connecting links and their shields were also designed to minimize any change in the transmission line characteristic; details of construction of the coupling arrangement are observable in figure 7, which shows a continuously variable section of transmission line built in the same manner.

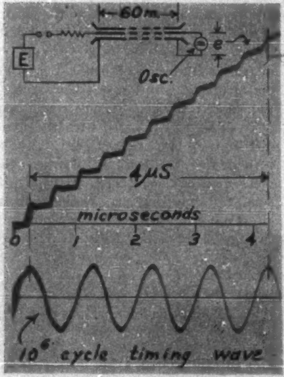

Figure 6. Calibration oscillogram of time of travel versus distance on a transmission line, recording the time for a given number of reflections on a given length of the line

The concentric-pipe assembly of figure 4 was designed to give a distance between recording units corresponding to time intervals of 0.01 microsecond on the basis of the waves traveling in this essentially air-dielectric cable at the velocity of light, or 3×10^{10} centimeters per second. A check was made by means of the cathode-ray oscillograph to determine whether the lengths including the effects of the couplings and supporting washers were correctly in agreement with the desired time interval.

The timing calibration on the concentric-pipe assembly was made on a large

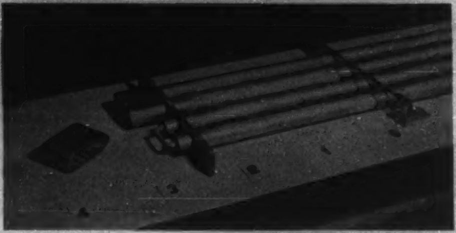

Figure 7. A telescoping variable-length transmission-line section calibrated in 10^{-9}-second units

number of line sections in series; a 0.01-microsecond section would be difficult to distinguish on a cathode-ray oscillogram, and consequently 20 such sections, or 60 meters, were taken in series. Furthermore, to obtain still greater calibration accuracy, the wave impedance, which in normal operation of the transmission-line system prevents reflections, was removed and the line was connected impulsively to a high-voltage source through a high resistance. The circuit diagram and the resulting oscillographic record of the voltage appearing at the open end are shown in figure 6. The first potential wave should arrive at the open end of the 60-meter line in a time $\frac{d}{v}$, or $\frac{6{,}000 \text{ cm}}{3\times10 \text{ cm/sec}} =$ 0.2 microsecond. At the open end this wave reflects and travels back to the resistance end in 0.2 microsecond. At the resistance end the resistance is so high as to present practically an open-circuit condition, and the first reflected wave is again reflected back; and in another 0.2 microsecond, or a total of 0.4 microsecond, reaches the open end at the oscillograph elements and steps the voltage up. This latter wave causes a reflection which in turn arrives in another 0.4 microsecond and steps the voltage up again, and so on. Thus the time between steps is the time it takes a wave to travel the full distance forward and full distance backward over the line. From the oscillogram, ten such step spaces were taken to increase the reading accuracy, and the recorded time was found to be 4.0 microseconds. The time for the ten step spaces corresponds to the time a wave would travel over 10×2×60 meters, or 1,200 meters, giving a velocity of $\frac{d}{t} = \frac{120{,}000 \text{ cm}}{4\times10^{-6} \text{ sec}}$, or in centimeters per second, 3.0×10^{10}, which is the velocity of light. The accuracy of the oscillographic determination was within one per cent, and thus it can be concluded that for the concentric-pipe assembly the time lags may be taken directly in terms of the line length and the velocity of light.

Determination of the Accuracy of the Time-Lag Measurement Method

The simplest checking test is to short-circuit *g* (figure 5*c*) so there is no time lag between the surge waves *S* and *T*. By introducing additional sections of transmission line with a plug system at the top of the transmission-line assembly of figure 4 it is possible to shift the electrical zero point. The introduction of additional sections on the positive-surge *S* side corresponds to artificial time lags and gives correct indications in every case over the entire range of the transmission-line assembly.

To check exact time-lag resolution conveniently, a trombone telescoping transmission line section was built up so that the timing wave could be shifted in fractions of the 10^{-8} range of the larger sections. It was found that a shift of the trombone section pictured in figure 7 corresponding to less than 10^{-9} second from the electrical zero would be sufficient to determine the tripping of the surge-recording unit involved. The possible resolving power in the measurement of time lags is, therefore, determined as at least as low as 10^{-9} second.

It is of importance to consider the exact relation between the number of transmission-line sections and the corresponding time of wave travel. The internal construction of the concentric-pipe transmission line may be seen in the trombone section of figure 7. The connecting-plug bends are designed with shielding covers to retain closely the same impedance characteristics as the rest of the line, and except for the small lengths covered by the higher-dielectric supporting washers we may quite accurately assume a velocity of wave travel equal to that of light. The velocity in the line fragments inside the washers will, however, be less, but the correction is calculable and the slowing down effect and also a sloping effect is illustrated in figure 8, for an initial rectangular wave after passing various numbers of supporting washers. The distinct time of arrival of the waves on the line is seen to be definite to within fractions of 10^{-9} second.

It was considered very desirable to check some of these very short time effects experimentally, and accordingly a cathode-ray oscillograph was designed with particular attention to fast operation. The dual-wave method responded to variations of a fraction of 10^{-9} second and an oscillographic record of its highest resolution or of the calculated curves of figure 8 would require comparable recording speeds. The highest recording speed reached with the cathode-ray oscillograph in the laboratory to date is shown in the oscillogram of figure 9. This oscillogram shows traveling-wave reflections on a concentric transmission line only 75 centimeters in length, connected to the oscillograph plates at the open side and short-circuited through a gap sparking over at the charging end. While figure 8 could not yet be checked

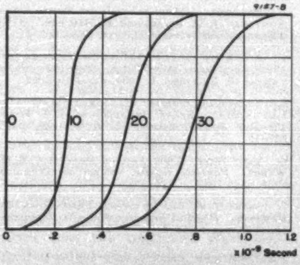

Figure 8. Calculated sloping and additional time lag of an initially rectangular wave as it passes 10, 20, and 30 supporting rubber washers of the concentric transmission line

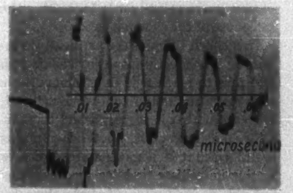

Figure 9. Fastest recording obtained on the oscillograph showing the short-circuit oscillations on a transmission-line section 75 centimeters long which are, even for so short a length, of definite traveling-wave type

directly since fractions of 10^{-9} second would scarcely be discernible, yet it is apparent that the oscillations are very definitely of a traveling-wave character even for such a short line length, and there is surprisingly little damping with most of that present largely attributable to the resistance of the shorting gap.

It is hoped to build the artificial transmission line directly into the oscillograph and thus to reach records of comparable time resolution to the dual-wave method, but even if that is accomplished successfully the dual-wave method remains useful, since it is so much more direct in getting immediate time-lag records without the necessity for photographic development.

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Discussion

For discussion see page 1094.

Importance of Gas in Electrodes for the Glow-to-Arc Transition

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IN an earlier work[1] on the probability of glow-to-arc transitions in a low-pressure mercury-vapor discharge it was pointed out that these transitions occur in a completely random fashion and that the probability of a transition increases with an increase of pressure, cathode-current density, or glow voltage. In particular, cathode temperature was found to be very important, the transition probability being several times higher in a narrow temperature range near 425 degrees centigrade than it is at either lower or higher temperatures. The possible causes of these transitions were also investigated and the only explanation that seemed consistent with the experimental results obtained was that each transition was due to a burst of gas from the electrode surface. It was felt that even if the amount of gas given off in each burst was very small, the random appearance of these bursts from various parts of the electrode surface could explain the experimental results obtained.

This paper deals with further studies of an unstable glow discharge in mercury vapor which lend support to the theory that the causes initiating glow-to-arc transitions* are probably small bursts of gas nonuniformly emitted from the electrode surface. The experimental studies show that the transition probability continually decreases when the electrode is kept hot and bombarded with positive ions over a period of several days, but when the electrode is allowed to remain cold, even overnight, the transition probability is greatly increased. Two quite different curves of transition probability as a function of oil-bath temperature can be obtained, depending upon the glow current used. With glow current densities of about 1 milliampere per square centimeter the probability of a transition increases with oil-bath temperature but with very low values of glow current (0.025 milliampere per square centimeter) the probability of a transition decreases with an increase of oil-bath temperature. Additional support for the theory has been found in an examination of many experiments by other investigators. In all of these, we find that it is easy to explain the results obtained if we admit the possibility of gas bursts occurring on the electrode surface. That the amount of gas emitted in the form of a burst can be very small is substantiated by some calculations which show that the conditions necessary for the formation of a cathode spot are fulfilled by the emission of as few as 10^{11} atoms. This is a much smaller amount of gas than can be detected by ordinary means. Finally, the results indicate that the complete elimination of this cause for the glow-to-arc transition is very unlikely since a complete outgassing of the electrodes is impossible in any vapor tube.

Experimental Results

The experimental tube used for these tests is shown in figure 1. In this figure, *T* is the electrode used for the cathode of the glow discharge. *A* is the anode of the arc discharge used to furnish ionization for the unstable glow discharge, and *K* is the anode used for the keep-alive arc. No mercury is shown in the tube but in operation enough mercury was used in the cathode pool to bring the level up to the tungsten spot fixer, *F*. In operation a high negative voltage—the glow voltage—was applied to the test electrode, *T*. The positive-ion current collected by this electrode we shall call the glow current. Under normal conditions this current was relatively small, amounting to a few milliamperes per square centimeter of electrode area at the most. Under some conditions, however, the form of the discharge would change spontaneously from the normal high-voltage low-current type to a high-current low-voltage discharge and a cathode spot would appear on the electrode surface. With the circuit arrangement used (see figure 2) this low-voltage discharge could not be maintained. Thus the cathode spot was soon lost and the glow (that is, high voltage) discharge re-established. The average

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[1] For all numbered references, see list at end of paper.

* When we speak of a glow-to-arc transition we will always mean those spontaneous transitions that cannot be explained as due to external causes or causes which, under special conditions, can be removed. For example, we are excluding such causes as mercury drops hitting the cathode, or patches of impurity on the electrode surface, or breakdown to sputtered layers on insulators, etc. (See reference 1.) Glow-to-arc transitions, when encountered in mercury-arc rectifiers, are commonly called backfires.

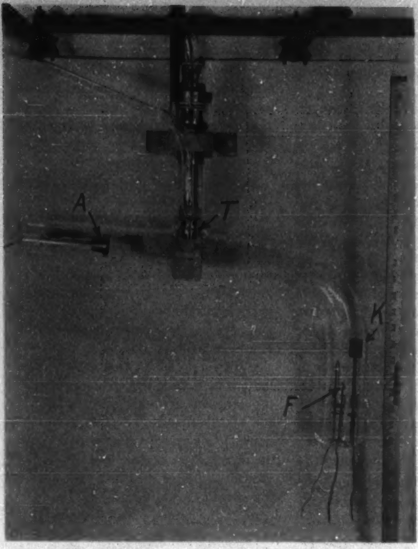

Figure 1. The experimental tube

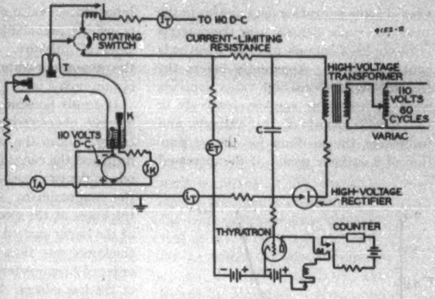

Figure 2. Circuit diagram

number of these glow-to-arc transitions occurring per second was measured by means of a counter and in the curves that follow it is this frequency that is called η. We see then that under the conditions of the test, η is a measure of the probability of a glow-to-arc transition.

Figure 3, curve 1, illustrates the important effect of gas in the high-voltage electrode on the transition probability. Throughout this test (with the exception of short intervals as noted below) electrode *T* was kept at 500 degrees centigrade by periodically using it as an anode of an arc discharge and the mercury-vapor pressure was kept constant by maintaining the oil-bath temperature at 51 degrees centigrade. For eight or ten hours each day the test electrode was bombarded by positive ions between the heating intervals. When this was done, the transition probability continually decreased. At the end of three days the heating and bombardment was stopped and the electrode was allowed to cool for 36 hours. It was then found that the transition probability under the same conditions of test had increased to the values observed on the first day.

It seems obvious from this test that, during the period when the test electrode was kept hot, gas was being continually emitted from the graphite test electrode under the combined action of heat and positive-ion bombardment. The emission of this gas in the form of small bursts results in the observed glow-to-arc transitions. When the electrode is allowed to cool, even though the tube remains evacuated, the electrode absorbs gas, mercury vapor probably, which is not emitted until the electrode is again heated and bombarded with positive ions.

A further indication of this effect is given by curve 2. The data for this were taken concurrently with the data for curve 1. The data for curve 2, however, were obtained by measuring the transition probability during the 10 or 15 minutes immediately after all arcs were extinguished in the tube. Obviously under these conditions the electrode could not be kept hot, but the cooling was only allowed to continue for the period of the test. This affected the results somewhat as the points at two days indicate. The average transition probability for the whole 12 minutes of the test is seen to be slightly higher than for the last 10 minutes. Although the ordinates are not the same, the similarity of the two curves is very striking. Here again it seems that the continued positive-ion bombardment and heating decreases the gas emission and the probability of the formation of gas bursts.

It seems hardly necessary to add that all the tests were performed on an electrode and with a tube that would ordinarily be classed as "thoroughly" treated. There was no visible increase in pressure during operation. The foreign gas pressure was less than 0.02 micron in all tests and the electrode had undergone a long period (more than $2^1/_2$ weeks) of heating and positive-ion bombardment before these tests were started.

This decrease in transition probability with heating and positive-ion bombardment is strikingly shown in some further tests made with no arcs in the tube. Figure 4 shows the results obtained. After the tube had been standing for some time at room temperature (that is, overnight), the curve marked 1 was obtained as the oil bath temperature was raised to 60 degrees centigrade. The electrode was then heated and bombarded with high-energy positive ions for about 72 hours. After this treatment another test of transition probability was made with all arcs out in the tube and it was found that, regardless of the oil-bath temperature, the transition probability was practically zero. This seems to show that

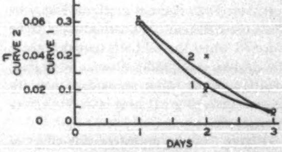

Figure 3. Effect of treating time on transition probability

Oil-bath temperature 51 degrees centigrade; temperature of electrode 500 degrees centigrade; $E_T = 15{,}000$ volts

Curve 1—$I_K = 11.5$ amperes; $I_A = 0$

Curve 2—$I_A = 0$ $I_K = 0$

even if some ionization does appear in the tube, the vapor density near the cathode is rarely high enough to cause a cathode spot to appear. Apparently before the treating process local high vapor densities were occurring at random intervals in the neighborhood of the cathode and furnishing the medium for the production of a cathode spot. If the increased transition probability is due to bursts of gas, we can easily see that an increase in the oil-bath temperature will, by increasing the vapor pressure in the tube, increase the relative importance of any small burst of gas from the electrode. In other words, a burst of gas which may not be large enough to initiate the formation of a cathode spot when the oil-bath temperature is 30 degrees centigrade may be entirely sufficient for a cathode spot to be formed when the oil-bath temperature is 60 degrees centigrade, since at 60 degrees centigrade the vapor pressure of mercury is ten times as great as it is at 30 degrees centigrade.

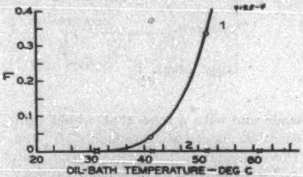

Figure 4. Effect of pressure on cold breakdown before and after treating

$E_T = 15{,}000$ volts, electrode cold
Curve 1—Before treating
Curve 2—After treating
$I_A = 0$
$I_K = 0$

Figure 5 shows an interesting effect of pressure on the transition probability when there is sufficient ionization present from auxiliary arcs to have a space-charge sheath. Curve 2 in this figure is similar to the curves in the previous paper showing the dependence of transition probability on gas pressure. Curve 1 taken at a slightly higher voltage and drawn to a larger scale shows the effect of greatly reducing the current density. The striking difference between the curves is evident. With current densities of the order of 1 milliampere per square centimeter the transition probability increases with pressure but with a current density of 0.025 milliampere per square centimeter the transition probability actually decreases as the mercury-vapor pressure is raised. It would seem that at some intermediate value of current density the transition probability would be independent of pressure. Unfortunately with the present apparatus it was impossible to obtain such a test.

It seems possible to explain these divergent characteristics at least qualitatively. From the values of the voltage drop and the current density at the cathode in the two cases it is easy to calculate the approximate space-charge sheath thickness at the electrode.[2] In the case of the larger current (curve 2) the sheath thickness was thus found to be of the order of 2 centimeters, whereas in the case of the low current density (curve 1) the calculation gives a sheath thickness of 16 centimeters. The mean free paths of the mercury ions varied (in both cases) between approximately 1.4 centimeters at 30 degrees centigrade to approximately 0.17 centimeter at 60 degrees centigrade. Because the mean free path is not large compared to the sheath thickness, the positive ions will lose a certain amount of energy in crossing either sheath. In the case of the 2-centimeter sheath the average number of collisions will vary between one and ten and the energy of the positive ion on striking the cathode will be practically the maximum value attainable in falling through the cathode dark space. In this case as the pressure is increased the decrease in energy of the positive ions when they strike the cathode will be slight. The upper curve in figure 6 might thus be a representation of the positive-ion energy at the cathode surface.

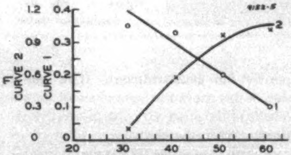

Figure 5. Effect of pressure on transition probability

Temperature of electrode 500 degrees centigrade

Curve 1:
$E_T = 15{,}000$ volts
$J = 0.0268$ milliampere, per square centimeter
Curve 2:
$E_T = 10{,}500$ volts
$J = 1.007$ milliampere per square centimeter

If, however, the sheath thickness is large (16 or 20 centimeters) the positive ions will make many collisions in crossing the sheath; in fact, about ten times as many as with the 2-centimeter sheath. The total loss in energy—assuming the same loss in energy at each collision—will thus be ten times as great as with the narrow sheath and the energy of the positive ion when it strikes the surface could be represented as in the curve marked $S = 16$ centimeters in figure 6.

Changes in pressure can thus influence the transition probability in two ways. With an increase in pressure and the corresponding increase in vapor density the effect of a small burst of gas is much more pronounced and a cathode spot develops much more easily because of the increased vapor available for current conduction and amplification by ionization by collision. However, with an increase in vapor density the energy of the positive ions striking the cathode is reduced and the number of secondary electrons produced in the neighborhood of a burst is also reduced, reducing the chance that a self-maintaining discharge will be established. We can easily see then that with the narrow sheath (2 centimeters) the loss in energy of the positive ions with increased pressure will be slight and an increase in the transition probability will be observed because of the increase of the vapor density. When the sheath is thick (16 or 20 centimeters), however, the loss in energy of the positive ions is large (each ion making about 100 collisions in crossing the sheath at the highest pressure) and even though the vapor density increases the transition probability decreases because of the reduction in energy of the positive ions at the cathode and the corresponding decrease in secondary electron emission.

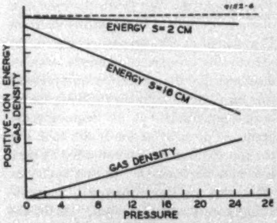

Figure 6. Effect of pressure on energy of incident positive ions

Other Evidence

Although the experimental results seem conclusive in themselves, it seems worth while to go further and determine what other evidence there is substantiating this theory of the glow-to-arc-transition. One is immediately struck with the fact that in any work involving a high degree of vacuum or relatively gas-free surfaces prolonged treating is always necessary. The usual process is to heat the electrodes and walls of the container to drive out as much gas as possible and then bombard them with electrons or positive ions to clean up still more of the gas. Anyone who has worked with high-vacuum apparatus is familiar with this procedure and it seems unnecessary to point out specific references.

Even though a high-voltage discharge is very useful in outgassing an electrode it is not completely successful, since the procedure is very slow and often introduces other complications. For example, any prolonged high-voltage treatment results in a very large amount of sputtering or electrode disintegration. Even with graphite electrodes, which probably exhibit as little sputtering as any, the tube is blackened in a relatively short time if a high-voltage discharge is used. In addition to this, it is well known[3,4] that when a discharge passes through a gas at reduced pressure there is a "cleaning up" effect present which tends to reduce the pressure in the vessel. The amount of "clean up" observed simply by the action of the discharge in trapping the gas on the electrodes or walls may sometimes be very pronounced resulting in a pressure drop from a centimeter or more to a fraction of a millimeter. In general, it has been observed that the materials which sputter most easily are those which make possible the most rapid clean up of residual gas. As far as high-vacuum work is concerned this is probably a very fortunate relation. Thus one of the most useful means of purifying a gas at low pressure is through the use of the so-called "Misch-metal" discharge which employs an easily vaporized and easily oxidized cathode material. On the other hand, graphite is about the best material to use for high-vacuum work when a clean, relatively unsputtered, tube is desired.

Solids in contact with a gas always have a layer of gas on their surfaces. This gas is only partly removed by heating and positive ion or electron bombardment. There is considerable evidence[3] that this gas is held on the surface in the form of complex and unstable compounds having formulas like $A_xB_yC_z$ where x, y, and z may have any value whatever. In the presence of the electric discharge, these compounds or combinations of elements may be continually forming and disintegrating. Whether complex compounds are formed or not, there is every reason to believe that there is a continuous process of adsorption and re-emission of gas going on at all times in the discharge.[5] Under some circumstances this adsorption and re-emission may proceed slowly and produce no perceptible change in the discharge tube and under other circumstances it may produce quite violent changes in the conditions in the tube.

A very striking experiment illustrating this effect was performed at the Westinghouse research laboratories a few years ago.* In this experiment a small U tube was constructed which had two mercury electrodes. Nitrogen was added until the pressure was about one centimeter. It was found, then, that when an arc was passed through the tube, the pressure rapidly decreased to less than a millimeter, but when the discharge was stopped and the electrodes were heated with a Bunsen burner, the gas was re-emitted in a very violent fashion, flashes of light and small explosions occurring on the mercury surfaces. This continued until the pressure returned to its original value. It is easy to see that if gas is held on the electrode in any such combinations as was apparently the case in this tube, gas bursts of sufficient size to initiate the discharge can easily occur.

The importance of unstable compounds in causing the formation of a cathode spot is illustrated by some work of Cobine[6] and Suits.[7] In studying the reignition characteristics of metallic arcs at pressures below 50 centimeters they found that oxides or impurities are necessary on the electrode surfaces if reignition is to occur. Essentially they were interested in determining the circumstances under which a cathode spot was or was not easily obtained on a negative electrode in a region of high ion density. Before arc reignition they had a nonself-maintaining glow discharge and when arc reignition occurred this discharge changed to a low-voltage arc. They found that impurities or oxides were necessary if arc reignition was to occur. Because they were working at relatively high pressures and with 60-cycle voltages they needed large gas bursts at frequent intervals to obtain arc reignition. In the absence of the impurities the nonself-maintaining glow discharge simply changed to a self-maintaining glow and no arc was formed.

Along this same line, some recent tests of H. Klemperer[8] show that it is possible to have breakdown and arc formation in low-pressure tubes at voltages much below those necessary for a stable glow discharge. He found that the voltage at which these arcs formed was extremely variable. These tests were very similar to the cold breakdown tests described in the preceding section and the variation in breakdown voltage which he obtained is probably due to the electrodes being treated differently in the various tests. The conclusion seems to be that gas bursts were occurring on his electrodes which caused the observed transitions. Because the tests were probably made over a period of many days with periods between during which the electrodes could take up gas, there is no reason why the results should be consistent. It has been found in the tests reported above that only when the electrode is maintained hot and the tests do not extend over too great a time is it possible to get consistent results.

Quite aside from the importance of adsorbed gas in causing cathode-spot formation, it is possible for this gas when emitted to influence other observations. For example, it is well known that untreated surfaces and surfaces with layers of oxide exhibit much greater secondary emission than well-cleaned surfaces.[9,10] This seems to substantiate the presence of gas bursts from the untreated surfaces. The true secondary emission from the untreated surfaces, in many cases, is probably no larger than from treated surfaces. It *seems* greater, however, because if bursts of gas appear, the secondary electrons will produce ionization by collision in the emitted gas and the current measured will be the secondary electrons *plus* the electrons formed by collision with neutral gas atoms.

A simple approximation of conditions in the positive-ion space-charge sheath near the cathode and the conditions necessary for the formation of a cathode spot can be made by assuming that the boundaries of a space charge sheath of thickness S are infinitely extended plane parallel surfaces and that the number of positive ions diffusing into the sheath per unit area per second is P_s.

If α is the average number of ionizing collisions made by an electron per unit distance and γ is the number of secondary electrons produced at the cathode by each incoming positive ion it is easy to obtain an expression for the current density in the form of equation 1.

$$J=qP_s\left[\frac{1+\gamma}{1-\gamma\left(e^{\int_0^s \alpha dx}-1\right)}\right] \qquad (1)$$

Here q is the charge on each ion and e is the base of the natural system of logarithms. This equation fits in very well with experiment, for from observations on the current collected by a negative electrode in a region of high ion density we know that the current is directly proportional to the number of ions diffusing into the sheath. We also know that the coefficient of secondary emission due to positive-ion bombardment increases approximately linearly with the energy of the positive ions when the energy is of the order of several thousand volts.[11] Independent experiments have shown[12] that the current collected by a negative electrode increases approximately linearly with the voltage and the dependence of J on γ and voltage is thus checked, at least qualitatively.

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* We are indebted to Doctor J. Slepian for permission to mention this research.

The equation above also states that there are certain conditions under which we may have a self-maintaining discharge in the sheath at the cathode. The conditions are exactly the same as the conditions necessary for sparking to occur in gases at higher pressures[10,13], namely,

$$\gamma\left(e^{\int^{d}\alpha dx}-1\right)=1 \tag{2}$$

This equation, has two distinct interpretations. One is that if the pressure or voltage is varied until this equality is fulfilled a self-maintaining discharge will be established which will be a stable glow discharge utilizing the whole or a large part of the cathode surface. This condition is readily obtained in a discharge tube by raising the pressure to a high enough value (50 or 100 microns, for example).

A more important characteristic of equations 1 and 2 is that it is possible to see how a gas burst could initiate a glow-to-arc transition. Suppose a gas burst occurred over a very small area of the cathode surface. The vapor density would be increased near this local spot and the current density would increase greatly. It is conceivable that, in the region of the high vapor density, during the burst the condition for a self-maintaining discharge could be fulfilled, and since the area involved is small and the current density large a cathode spot may form. The discharge then would take the form of an arc rather than a glow discharge. We can see also that if the voltage drop across the sheath is increased, γ will be increased and it may be possible to initiate the transition with a smaller amount of gas in a burst. In this respect, the theory predicts the observed increase in transition probability with voltage. When the sheath is not too thick the observed increase of transition probability with pressure can also be predicted. As the pressure is raised, the amount of gas appearing in a burst or the size of a burst can be reduced and still the condition necessary for a self-maintaining discharge will be fulfilled. Since in our theory we have postulated that bursts of all sizes are probable, it is obvious that as smaller bursts become effective, the transition probability will increase.

Some estimates of the amount of gas necessary in a burst in order to provide for the initiation of a cathode spot can easily be made. For a 0.5-ampere arc the area of the cathode spot is about 10^{-5} or 10^{-4} square centimeters.[14] Thus, taking the larger of these values, if we assume that the voltage drop across the positive-ion space-charge sheath at the cathode of an arc is about 10 volts and that the field intensity at the surface of the cathode is of the order of 10^6 volts per centimeter (10^8 volts per meter) and at the edge of the sheath is zero, we can estimate the charge density in the sheath. The equation

$$E=\frac{\rho d^2}{2p} \tag{3}$$

gives the voltage drop across a uniform space charge sheath when the electric intensity at one edge of the sheath is zero. Here ρ is the charge density, d is the thickness of the sheath, and p is the permittivity of free space. If $d=E/F_{\max}$ where F is the electric intensity, d will have a value in this case of about 10^{-7} meters. Solving equation 3 for ρ and multiplying by the area (10^{-8} square meters) and the thickness (10^{-7} meters) of the sheath, the total charge contained in the sheath is found to be about 1.77×10^{-11} coulombs. This corresponds to about 10^8 singly charged positive ions. There is evidence[15] supporting the view that at the cathode spot of an arc the gas is essentially 100 per cent ionized. If this is the case, then the emission of 10^8 molecules of gas would be sufficient to permit the formation of a cathode spot. This amount of gas is equivalent to a mass of mercury vapor of 3×10^{-14} gms. At atmospheric pressure it would represent a volume of 4×10^{-12} cubic centimeters and in a vessel of one liter volume it would raise the pressure by only 3×10^{-12} millimeters of mercury. We see then that this amount of gas is much too small to be detected by any ordinary means. Volumes of gas 1,000 or 10,000 times as large would still be undetectable.

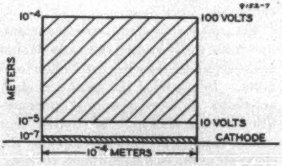

Figure 7. Conditions in the neighborhood of a gas burst

Referring now to figure 7, if we assume that the burst of gas consisted of as much as $10^3\times10^8$ or 10^{11} molecules and that before the cathode spot formed it had spread out so that it covered an area of 10^{-8} square meters to a depth of 10^{-4} meters, the gas density would be increased in this volume from about 10^{20} molecules per cubic meter to 10^{23} molecules per cubic meter. This increase in density would reduce the mean free path of the secondary electrons from about 10^{-2} meters to about 10^{-5} meters. In the glow with 10,000 volts across a two-centimeter sheath the maximum field intensity at the cathode would be approximately 10^6 volts per meter. Thus by the time an electron had fallen through a distance of 10^{-5} meters (one free path) it would have acquired an energy of 10 volts and in the distance from 10^{-5} to 10^{-4} meters from the cathode it will produce most of its ionization. Obviously with the building up of space charges the secondary electrons will gain sufficient energy for ionization in shorter distances and the ion density will increase near the cathode, as it should if a cathode spot is to be formed.

The question now arises of whether or not the gas will remain in the neighborhood of the cathode long enough for a cathode spot to form. Loeb[16] in discussing the breakdown of spark gaps says that the observed values of time lag of breakdown are so short (10^{-7} to 10^{-8} seconds) that neither positive ions nor electrons could possibly have crossed the gap in the intervals used. Because of this it was necessary for him to postulate other mechanisms for building up space charges, or current densities in very short times. One of these is photoelectric emission of electrons at the cathode due to the resonance collisions that the secondary electrons make in moving away from the cathode. Other explanations are the formation of large space charges in short distances by the action of "electron avalanches" or by the increase in α/p with F/p. Here F and α have the same meaning as in the discussion above, but p now is the gas pressure. Regardless of the mechanism, the physical processes involved in the breakdown of a spark gap and in the transition from a glow to arc are essentially the same. In either case we start with a small amount of ionization and through some ionizing process establish a self-maintaining discharge.

When considering the glow-to-arc transition we realize that the distance involved in the establishment of a self-maintaining discharge is much less and consequently it becomes easier to explain the rapid increase in ionization. It is even possible for ionization by collision alone to account for the large space charge necessary for the cathode spot of an arc. If we assume that the field intensity at the cathode in the glow is 10^6 volts per meter and that the electrons move with constant acceleration, it will only take 10^{-11} seconds for the electron to travel 10^{-5} meters and less than 10^{-8} seconds for the positive ions to travel the same distance. This is a time interval smaller or at least of the same order of magnitude as that required for the breakdown of a spark gap. Thus we could attribute the increase in ionization necessary for cathode-spot formation simply to ionization by collision.

Up to now we have been tacitly assuming that the high gas density would remain in the neighborhood of the cathode long enough for a cathode spot to form. To estimate the speed with which the emitted gas molecules would move away from the cathode, we can assume that the gas is emitted with the temperature of the electrode (500 degrees centigrade) and moves away from the electrode with the gas molecules making no collisions. This should give an upper limit for the velocity of the gas particles. On this assumption we calculate an average velocity of about 1,000 meters per second. This would mean that it would take at least 10^{-7} seconds for the gas to move 10^{-4} meters. This also is a time of the same order of magnitude as that required for spark-gap breakdown. Certainly then if a gas burst occurs, the gas will not diffuse away too rapidly for a cathode spot to form.

Conclusions

Any analysis of the results presented here seems to substantiate the theory that the glow-to-arc transition is caused by the emission of gas from the electrode surface in the form of small bursts. The continual decrease in transition probability as long as the cathode is being heated and bombarded with positive ions and the increase in transition probability when this process is stopped can only indicate that gas is being absorbed or adsorbed at the electrode surface during periods when the tube is cold and is reemitted when the electrode is heated and bombarded with positive ions.

That this gas is the most important factor also seems evident, for in order to have a cathode spot form we must provide a medium in which a very rapid increase in ion density can occur. We conclude that this gas must be given off in the form of small bursts appearing at random intervals on the electrode surface, for only thus can we explain the dependence on glow current and voltage, pressure, electrode temperature, and time.

After reviewing all the evidence, the conclusion seems obvious. Without a doubt, the results obtained in this laboratory have done more than point out again the large effect that gas trapped in or on electrodes can have in influencing experimental results. This has been known for a long time. What the experiments here have shown is that it is practically impossible to degas electrodes as long as any gas or vapor such as mercury is allowed to remain in the tube. In a mercury-vapor tube the electrodes are probably continuously adsorbing and reemitting a certain amount of the gas. All the evidence seems to point to the fact that this gas is not emitted uniformly over the electrode surface but instead is emitted in very small bursts. The gas contained in the burst may represent only 10^8 or 10^{11} molecules yet this amount is still sufficient, as shown by the calculations above, to initiate a cathode spot if the burst occurs on a negative electrode. Finally, the results indicate that the likelihood of ever being able completely and permanently to outgas an electrode in a mercury-vapor tube is very remote.

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Discussion

For discussion, see page 1095.

Lightning Investigation on a 220-Kv System—III

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Synopsis: This paper briefly describes surge-crest-ammeter measurements of lightning strokes and structure network currents, and summarizes transmission-line experience with and without overhead ground wires, buried-cable grounding networks, expulsion protective gaps, station-type lightning arresters, and spillway gaps.

LIGHTNING research has been conducted for the past 13 years[1–6] by the Pennsylvania Power and Light Company, the General Electric Company, and the Ebasco Services, Inc., chiefly upon that portion of the Pennsylvania-New Jersey 220-kv interconnection circuit between Wallenpaupack and Siegfried, Pa. This 65-mile-long circuit is of aluminum-conductor steel-reinforced three-conductor horizontal-configuration steel-tower construction, having originally 14-unit insulation. Since 1932 the 28-mile section from Wallenpaupack to Bushkill has been called the Wallenpaupack tap, and the remainder constitutes part of the Siegfried-Roseland line.

The original line was without overhead ground wires. Because reference 6 ably describes the construction and the historical background leading to the various improvements made from time to time, this paper will merely enumerate them briefly in table I.

Records of insulator flashovers, line tripouts, magnetic oscillograms, and surge indicators[4] have been kept regularly. By these means it was frequently possible to not only estimate[7] approximate locations of faults but to determine actual towers and assemblies flashed during specific tripouts. When available, surge-crest-ammeter[8] records were of much assistance.

Of various measuring devices used from time to time, such as surge-voltage recorders, lightning-stroke recorders, storm-severity meters, cathode-ray oscillographs, field-intensity recorders, etc. none are now used except surge crest ammeters. During 1936, 1937, and 1938 these were used as follows:

1936. One corner leg on about half of the 314 structures, all four corner legs on remaining structures including *SR* 8–1 to 10–1 (expulsion gaps); lightning rods and overhead ground wires at 5 towers *WT* 12–2 to 12–6; lightning rods at 5 towers *SR* 23–2 to 24–1; one 50-foot crowfoot on 46 towers *WT* 6–5 to 18–4 (at mid-point or 25 feet from tower); 18 long 250-foot crowfeet (at points 25, 125, and 225 feet from tower); on High Knob continuous counterpoise *WT* 11–4 to 14–1 (at points 3, 100, 200, and 300 feet from tower); and on Wallenpaupack Thyrite arresters (*Y* and *W* phases).

Table I. Locations of, and Time of Installation of Construction Additions to Wallenpaupack Tap and Siegfried-Bushkill Section of Siegfried-Roseland Line

	Name	Location	Length in Miles	Year
1.	Conventional overhead ground wires	Siegfried to *SR* 5–3	4.6	1927
2.	Conventional overhead ground wires	Wallenpaupack to *WT* 20–2	19.4	1927
3.	Continuous counterpoise (High Knob)	*WT* 11–4 to 14–1	2.6	1929
4.	Two more insulator units on outside insulator assemblies	Wallenpaupack to *SR* 24–5	40.0	1929
5.		*SR* 24–4 to 21–5	4.0	1930
6.	Diverting cables (overhead ground wires)	*WT* 22–1 to 25–4	3.8	1930
7.	Special crowfoot cables	*WT* 22–1 to 25–4	3.8	1930
8.	Short crowfoot cables	Wallenpaupack to *WT* 20–2	16.8*	1930
9.	Surge (flashover) indicators	Entire line	65.0	1930
10.	Short crowfoot cables	Siegfried to *SR* 5–3	4.6	1931
11.	Thyrite lightning arresters	Wallenpaupack *Y* and *W* phases		1934
12.	Ten expulsion gaps, *W* phase	*SR* 8–1 to 10–1	2.2	1935
13.	200-foot extensions to crowfeet	24 towers of item 8		1936
14.	Continuous counterpoise	*SR* 5–3 to 17–4	12.5**	1937
15.	Ten expulsion gaps, *Y* phase	*SR* 8–1 to 10–1	2.2	1938

* Does not include 2.6-mile section of item 3.

** Nonoverhead-ground-wire section of line, includes items 12 and 15.

1937. Four corner leg brackets at 10 towers *SR* 8–1 to 10–1 and 21 other scattered towers as well as 51 towers *WT* 6–5 to 19–5; on structure diagonal braces at 15 towers *WT* 10–1 to 10–5, 12–2 to 12–6 and 14–2 to 15–1; overhead ground wires at these towers; lightning rods and long crowfeet same as 1936 except that latter were extended to *WT* 19–5 (3 more towers) and inner installation moved from 25 to 3 feet from tower; short crowfeet same as 1936 except they were extended to 19–5 (2 more towers) and installations were moved to or made on all crowfeet at distance of 3 feet from tower; High Knob counterpoise and Wallenpaupack arresters same as 1936.

1938. Two corner legs on 50 towers *SR* 5–3 to 17–4 (new continuous counterpoise); four corner legs on 10 towers *SR* 8–1 to 10–1 (expulsion gaps) and 28 towers *WT* 10–1 to 15–1; overhead ground wires and structure diagonal braces at these latter 28 towers; High Knob counterpoise at distances of 3 feet only from towers; long and short crowfeet same as 1937 but only at 14 towers *WT* 10–1 to 15–1; lightning rods at *WT* 12–2 to 12–6 only; and Wallenpaupack arresters same as 1936 and 1937.

Results

MAGNITUDES OF STRUCTURE CURRENTS

During 1936 to 1938 some data permit evaluation of structure current magnitudes, and which are assumed to be the sum of currents in all 12 structure members directly connected to tower footings. These members are the four corner legs and eight diagonal braces. Within four or five feet of the ground these members are interconnected by others. Measurements were made before interconnection occurred, and magnetic links were so located that they were not appreciably affected by current in any member except the one to which they were coupled.

Table II lists data where all three measurements (leg and connected diagonals) exceeded traces. Ratios (of total current over corner-leg current) range from 1.64 to 2.75 but a tentative figure of 2 has been selected for general use in correcting records of leg current only.

MAGNITUDES AND FREQUENCY OF OCCURRENCE OF LIGHTNING STRUCTURE CURRENTS

Figure 1 shows frequency of occurrence of lightning structure currents of various magnitudes. Only 9 per cent of the total records exceeded 50,000 amperes and less than 1 per cent exceeded 100,000 amperes. The polarities of these 370 records were

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1. For all numbered references, see list at end of paper.

Table II. Wallenpaupack Tap Line—1937 and 1938; Relation of Currents in Structure Legs and Diagonals

	Year	Tower	Leg	Leg Amp	Sum Diagonal Amp	Sum Leg and Diagonal Amp	Ratio
Leg with short 50-foot crowfoot	1937	14-4	NE	2,270	2,700	4,970	2.19
	1938	10-6	NW	6,130	4,000	10,130	1.65
	1938	10-6	SW	3,200	3,800	7,000	2.19
	1938	10-6	NE	2,780	2,900	5,680	2.04
	1938	10-6	SE	2,940	3,200	6,140	2.09
	1938	14-4	NE	3,280	2,600	5,880	1.80
				20,600		39,800	1.93
Leg with long 250-foot crowfoot	1937	14-2	SW	2,270	3,600	5,870	2.58
	1937	14-3	NE	5,360	2,900	8,260	1.54
	1937	14-4	SW	7,150	5,700	12,850	1.80
	1937	14-5	NE	7,300	4,700	12,000	1.64
	1938	14-3	NE	6,400	4,800	11,200	1.75
	1938	14-4	SW	13,700	10,200	23,900	1.75
				42,180		74,080	1.76
Leg with continuous counterpoise	1937	12-2	NW	8,900	7,700	16,600	1.86
	1937	12-2	SW	9,150	11,100	20,250	2.22
	1937	12-3	NW	3,500	3,200	6,700	1.92
	1937	12-6	NE	8,060	14,100	22,160	2.75
	1937	12-6	SE	11,200	15,000	26,200	2.36
	1938	12-2	NW	6,550	7,200	13,750	2.10
	1938	12-2	SW	6,650	4,500	11,150	1.70
	1938	12-6	NE	6,730	8,200	14,930	2.22
	1938	12-6	SE	7,400	8,700	16,100	2.18
	1938	13-1	NE	10,850	17,300	28,150	2.60
	1938	13-1	SE	12,000	19,200	31,200	2.60
	1938	13-2	NW	17,500	17,100	34,600	1.98
	1938	13-2	SW	13,000	19,500	32,500	2.50
	1938	13-3	NE	3,780	3,900	7,680	2.03
	1938	13-3	SE	3,440	3,700	7,140	2.08
				128,710		289,110	2.25
Leg without any connected buried cable	1938	13-1	SW	3,700	2,500	6,200	1.68
Grand average 1937 plus 1938				195,190	214,000	409,190	2.10

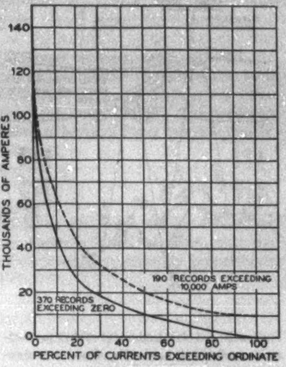


Figure 1. Numbers and magnitudes of structure currents caused by lightning

negative 96 per cent, positive 3 per cent, and uncertain 1 per cent.

The structure currents represented by figure 1 are twice the sums of corner-leg currents, to allow for the generally unmeasured currents in the structure cross bracing. Somewhat similar curves[9,10,11] have been previously published. Those entitled "Tower Currents" are of corner-leg currents only. Others entitled "Stroke Currents" are summations of corner-leg currents from two or more adjacent structures.

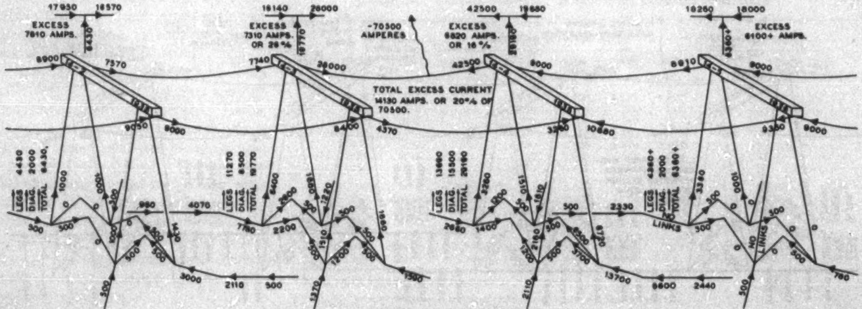
It will be shown later that measurements in overhead ground wires or lightning rods are necessary for proper determination of stroke currents, and that summations of structure currents only from adjacent towers can at least in individual cases, sometimes lead to erroneous conclusions.

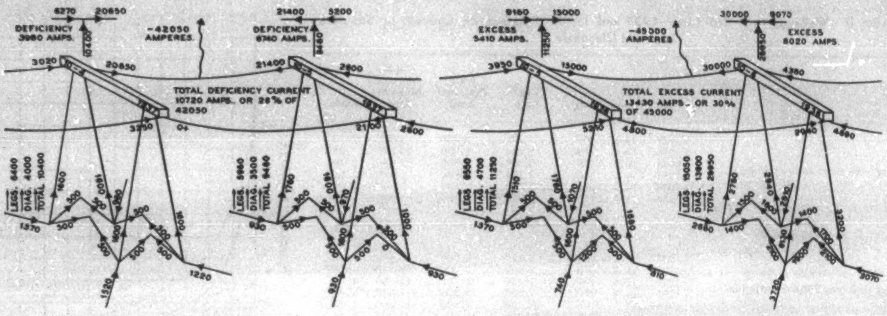
Numbers and Current Magnitudes of Lightning Strokes to Overhead Ground Wires and Structures

It is impracticable to present all of the 1936–1938 records from which stroke locations and/or magnitudes were satisfactorily determined. These were obtained from short sections of line ranging from 2.6 miles in 1936 to 5.2 miles in 1938 and showed that at least 23 well-defined strokes occurred, 6 to structures and 17 to spans. On this basis and for this line, it can be assumed that 75 per cent of the strokes occur to overhead ground wire spans. Some 400 German[12] records from lines of generally lower voltage ratings and greater numbers of structures per mile show ratios ranging from 51 to 64 per cent each year. A few Japanese[13] records indicated 95 per cent.

Figures 2, 3, and 4 are typical records from strokes to overhead ground wires. Figure 6 is typical of lightning-rod stroke

Figure 2. Lightning stroke to mid-span overhead ground wire, showing typical excess current




DEFICIENCY 3980 AMPS.
6270
20650
10260
-42050 AMPERES.
3020
20650
TOTAL DEFICIENCY CURRENT 10720 AMPS., OR 26% OF 42050
3200
LEGS 6400
DIAG. 4000
TOTAL 10400
1370
1220
1320
DEFICIENCY 6740 AMPS.
21400
5200
9460
2800
2100
2600
LEGS 5960
DIAG. 3500
TOTAL 9460
930
EXCESS 5410 AMPS.
9160
13000
11250
-45000 AMPERES
3950
13000
TOTAL EXCESS CURRENT 13430 AMPS., OR 30% OF 45000
5210
4800
LEGS 6550
DIAG. 4700
TOTAL 11250
1370
810
740
30000
9070
28950
EXCESS 8020 AMPS.
4380
9940
4860
LEGS 15050
DIAG. 13900
TOTAL 28950
2680
3070
3720

Figures 3 and 4. Lightning strokes to mid-span overhead ground wire, showing respectively non-typical deficiency current and typical excess current

records. Figure 5 is presented to show how two nearby strokes can cause records to be complicated and only partially intelligible.

In general, the sum of upward-flowing structure currents and in-flowing overhead-ground-wire currents exceed by some 20 to 35 per cent the stroke current as measured in lightning rods or stricken overhead wires. Figure 3 is an exception, and shows a 26 per cent deficiency instead of an excess. The reason for apparent current differences at junction points is probably associated with the facts that magnetic-link records are of maximum or crest currents instead of instantaneous values, and that surge-current changes may occur at impedance junctions.

Five lightning-rod records were 33,000, 33,000, 38,000, 57,000 and 62,000 amperes. Six well-defined mid-span strokes were 22,300, 32,950, 42,050, 45,000, 62,500, and 70,500 amperes. The average for each group is nearly 45,000 amperes.

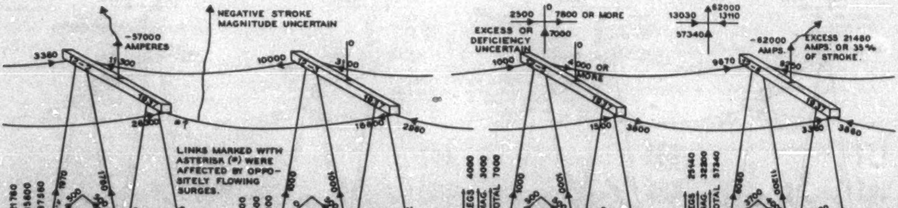
Table III. Records Caused by Three Negative Strokes to Overhead Ground Wires

Tower Number	Summation of Leg Currents	Total Structure Current	Actual and Apparent Stroke Magnitudes and Locations
13-3	3,000		(*a*). Apparently 50,000-60,000, or 100,000-120,000 amperes to 14-1. Actually to overhead ground wire between 13-5 and 14-1. Probable magnitude 70,000 amperes
13-4	2,000		
13-5	16,000	32,000*	
14-1	27,200	54,400*	(*b*). No apparent stroke. Actually 22,300 amperes to overhead ground wire between 14-2 and 14-3
14-2	5,720	11,370	
14-3	9,230	15,130	(*c*). Apparently 40,000, or 80,000 amperes to overhead ground wire between 14-4 and 14-5. Location correct but actual magnitude 62,500 amperes
14-4	13,620	24,820	
14-5	13,590	21,290	
15-1	4,500		

Tower Number	Direction and Magnitudes of Overhead-Ground-Wire Current				Tower Number	Continuous Counterpoise Current	
	NE	SE	NW	SW		North	South
14-2	4,450S	9,100S	3,700S	9,300S	13-4	5,000S	3,480S
14-3	5,200S	9,800S	13,000N	10,500S	13-5	9,400S	9,900N
14-4	9,700S	2,900N	10,500S	32,500S	14-1	20,000S	
14-5	2,180S	12,300N	30,000N	9,350N			
15-1	7,380N	8,000N	8,820N	8,000N			

* Double leg current measurement.

Many surge-crest-ammeter records are quite small, registering only as traces on the indicating meter. Interpretation of small records has been to classify those less than 10 on the indicating meter as traces, and to assign nominal values to them. For counterpoise, crowfeet, and tower diagonal brackets, traces have been marked 500 amperes. For lightning rod,


-57000 AMPERES
NEGATIVE STROKE MAGNITUDE UNCERTAIN
3360
11300
10000
3100
26000
16000
2960
LINKS MARKED WITH ASTERISK (*) WERE AFFECTED BY OPPOSITELY FLOWING SURGES.
LEGS 21780
DIAG. 15600
TOTAL 37380
LEGS 7600
DIAG. 5500
TOTAL 13100
2500
7600 OR MORE
EXCESS OR DEFICIENCY UNCERTAIN
7000
1000
4000 OR MORE
1500
3600
LEGS 4000
DIAG. 3000
TOTAL 7000
13030
62000
1310
57340
-62000 AMPS.
EXCESS 21480 AMPS. OR 35% OF STROKE.
9670
8200
3300
3060
LEGS 25140
DIAG. 32200
TOTAL 57340

Figures 5 and 6. Lightning strokes to lightning rod. Figure 5 is complicated by an adjacent stroke. Figure 6 is a single record showing typical excess current

overhead ground wire, and tower leg brackets, traces have been marked 1,000 amperes.

It is already apparent that the most satisfactory method of determining locations of transmission-line lightning strokes is from measurements in lightning rods and overhead ground wires. Stroke magnitudes are also most accurately measured by these means. Table III presents data from nine adjacent structures caused by three separate and distinct strokes. It shows that structure currents alone give an incomplete picture and may lead to erroneous conclusions as to stroke locations, magnitudes and numbers.

Table IV. Currents in Towers, Crowfeet, and Footings

Tower Number	Leg Number	Current: Leg	Diagonals		Sum	250-Foot Crowfeet	Footing*	Per Cent of Current Supplied by Crowfoot	See Figure
14–3	NE	6,400	2,200	2,600	11,200	7,780	3,420	70	2
14–4	SW	6,720	6,500	3,700	16,920	13,700	3,220	81	2
14–2	SW	2,270	1,900	1,700	5,870	4,950	920	84	
14–3	NE	5,630	1,600	1,300	8,530	5,030	3,500	59	
14–4	SW	7,150	3,600	2,100	12,850	12,000	850	93	
14–5	NE	7,300	2,600	2,100	12,000	8,000	4,000	67	
						50-Foot Crowfeet			
14–4	NE	3,280	1,400	1,200	5,880	2,660	3,220	45	2
14–4	NE	2,270	1,400	1,300	4,970	1,920	3,050	38	
10–6	NW	6,130	2,100	1,900	10,130	3,720	6,410	37	4
10–6	SW	3,200	1,700	2,100	7,000	3,070	3,930	44	4
10–6	NE	2,780	1,500	1,400	5,680	2,660	3,200	47	4
10–6	SE	2,940	1,800	1,400	6,140	2,810	3,330	46	4

* Computed as difference between sum (of legs and diagonals) and crowfeet (three feet from leg).

CURRENTS IN CONTINUOUS COUNTERPOISE ON OVERHEAD-GROUND-WIRE LINE

Figure 2 of reference 5 shows typical and detailed counterpoise records. Figures 5 and 6 herein show a few more. A single stroke may affect a mile or more of cable. Currents entering a span are less than those leaving the preceding span because of current diverted up the structure. Currents leaving a span are greater than those entering because of current collected in that span.

This 2.6-mile-long counterpoise has effectively prevented flashovers since its installation early during 1929. During the $1^1/_2$ preceding years while overhead ground wires alone were in use, at least two towers were flashed. The only published oscillogram[9] of a lightning surge obtained at the point of stroke contact showed a wave front of 5–6 microseconds. If such value is typical the effectiveness of 1,000-foot-span continuous counterpoise can be explained by the multi-velocity wave theory.[14]

CURRENTS IN NONCONTINUOUS COUNTERPOISE, OR CROWFEET

Figures 2, 3, and 4 show the two types of crowfeet in use. Most installations consist of four 50-foot cables at right angles to each other and 45 degrees to the direction of the line. A total of 118 overhead-ground-wire structures were so equipped during 1930 and 1931. During 1936, at 24 of the *WT* structures having the highest resistances, a 200-foot extension parallel to the direction of line was made on one crowfoot.

Table IV lists 12 sets of records in which the corner leg and diagonal member currents all exceeded traces. These records show the currents in tower corners and crowfeet. Tower-footing currents are computed on the assumption that they are the arithmetical difference between currents in structures and crowfeet. This assumption is not strictly correct but is the best that can be done lacking knowledge of time-current surge characteristics.

These data indicate that for 50-foot crowfeet about 37–47 per cent of the tower current is supplied by such cables, and the remainder by the footings. For the longer crowfeet, indicated ratios are erratic, due possibly to the fact that such cables may lie partly in loose dry rock, firm soil, and swamp. The tendency seems to be for the long cables to supply about 76 per cent of the tower corner current as contrasted with about 43 per cent for short cables. It should not be assumed that 250-foot cables are only 76/43 as efficient as 50-foot cables; on the contrary it will be shown that the efficiency is approximately proportional to length.

In the case of 250-foot cables, current measurements were made at distances of 125 and 225 feet from the tower. During 1936 measurements were also made at a distance of 25 feet, but during 1937 and 1938 this distance was changed to 3 feet. A total of 47 sets of records was obtained, but only 14 of these were sufficiently large so that all three exceeded traces. Table V lists these and also measurements from 50-foot crowfeet at the same towers.

Items 8–11 and 13–14 indicate that the ratio of currents 3 feet from the tower, in long and short crowfeet, vary from 2.4 to 6.9. Averaging the 18 records from short cables and the corresponding six from long ones shows the ratio of currents to be 9307/1864 or 5.0. This is the same ratio as the cable lengths. We can conclude that the rate of collection of current is generally proportional to the length of installed cable, at least up to 250 feet.

From items 4–6, 8–11, and 13–14 of table V we can also determine relative rates of collection of current for the different sections of long cable. Percentages of current measured at 3 or 25 feet, 125 feet, and 225 feet from the tower average 100, 55, and 15.5. From these,

Table V. Currents Measured at Following Distances From Tower

Item	Year	Tower	250-Foot Crowfeet: 3 Ft	25 Ft	125 Ft	225 Ft	50-Foot Crowfeet: 25 Ft	3 Ft	3 Ft	3 Ft
1	1936	7–1		6,600	2,400	1,700	1,700			
2	1936	11–3		2,660	2,700	760	500*			
3	1936	16–2		16,800	14,500	3,440	1,370			
4	1936	16–3		16,300	9,350	2,300	2,300			
5	1936	19–2		17,800	8,500	2,890	2,920			
6	1936	19–3		5,250	2,820	930	760			
7	1936	19–4		8,150	8,500	1,890	500*			
8	1937	14–3	5,030		2,180	600		930	1,070	500*
9	1937	14–4	12,000		6,650	1,960		1,740	1,920	1,810
10	1937	14–5	8,000		4,000	1,700		1,590	1,930	3,280
11	1937	18–2	9,330		6,700	1,440		2,110	2,630	3,180
12	1937	19–4	3,220		2,600	?		†	†	†
13	1938	14–3	7,780		4,070	960		1,370	1,590	1,220
14	1938	14–4	13,700		8,600	2,440		2,110	2,660	1,810

* Trace value assumed as 500 amperes.

† Probably greater than zero but less than lower limit of measurement.

Table VI. Towers Flashed by Lightning—Wallenpaupack-Siegfried Line

Section of Line	Period of Time	Number of Towers	Rate Per Mile Per Year
Towers still unequipped with overhead ground wires	1926–38	356	0.740
Towers† without crowfeet or counterpoise	1927–30	12*	0.200
Towers† having four short crowfeet	1930–38	3**	0.015
Towers† having continuous counterpoise	1929–38	0	0.000
Towers† having one long and three short crowfeet	1936–38	0	0.000

† Towers equipped with overhead ground wires.

* One two-phase and two three-phase flashovers.

** None of these flashovers was multiphase.

average rates of collection of current can be computed; these are respectively, 0.37, 0.395, and 0.62 per cent per foot for the near, middle, and distant sections. It is believed that at the extreme end of a cable rates as great as 5 may be reached. It may also be of interest that at none of the towers shown in tables IV or V or figures 2 to 6 did insulator flashover occur.

EFFECTIVENESS OF OVERHEAD GROUND WIRES AND BURIED COUNTERPOISE AND CROWFEET

The effectiveness can be judged from table VI which briefly summarizes flashed tower data.

EXPERIENCE DURING ONE SEASON ON $12^1/_2$-MILE SECTION OF SIEGFRIED-ROSELAND NONOVERHEAD-GROUND-WIRE LINE HAVING CONTINUOUS BURIED COUNTERPOISE

At least nine strokes or groups of strokes were measured although ten seem to have occurred. Currents were measured or indicated in 27 structures, and on 14 of these, plus 2 others, insulators were flashed. Four of the strokes occurred to the 2.2-mile section where expulsion protective gaps are in use. No flashovers or tripouts occurred and it is believed that the gaps avoided flashover in three cases. One apparent stroke was too small to measure, but it caused a single-phase flashover. The remaining five measured strokes or groups of strokes are tabulated in table VII.

Stroke number 5 is most unusual in a number of respects. It seems to have been a group of several strokes, two of which were quite large and of opposite polarity. Also, these two strokes either were highly oscillatory, or were multiple and consisted of opposite-polarity components. We cannot determine the actual stroke magnitudes, but two were apparently of the order of 100,000 amperes (summation of structure currents less 20–35 per cent). Had records all been of negative polarity, the summation would have been 236,000 amperes, and there would seem to be some justification for such addition since the larger currents were in the two middle towers. It has previously been shown that such summations can be misleading.

These strokes occurred on December 10, 1938. A magnetic oscillogram shows all three phases faulted at the very beginning of the record. One vibrator has a six-cycle acoustic time delay; its record shows about one cycle of residual current, indicating that all phases were not faulted simultaneously.

A tentative conclusion is that during 1938 the counterpoise performed about as well as expected. From records available in 1937 from overhead ground-wire sections of line, it was anticipated that about 25 per cent of strokes would contact structures, or a total of two or three strokes. At least one stroke so occurred, and perhaps several others, but in these cases the exposed conductors seem to have been struck as well. Possibly successive components of a multiple stroke involved a line conductor at some time, and the counterpoise was effective only in limiting the fault to such stricken conductor. The counterpoise can be effective in preventing tower flashover only in those cases where no stroke or side streamer contacts a line conductor.

EXPERIENCE WITH 220-KV EXPULSION PROTECTIVE GAPS

Late in 1935 gaps were installed on phase *W* (west 14-unit insulator assemblies at ten nonoverhead-ground-wire towers *SR* 8–1 to 10–1 inclusive. During the winter of 1936–37 two gaps were removed for manufacturer's inspection. In March 1937 while surge crest ammeters were not in use, a stroke destroyed two gaps. Shortly thereafter the previously removed gaps were replaced, and during

Table VII. Lightning Strokes to Line Having Continuous Counterpoise But no Overhead Ground Wires or Expulsion Gaps

Stroke Group Number	Tower Number	Estimated Structure Current*	Insulator Phases Flashed	Did Line Trip	Current in Second Loop	Remarks
1	16–5	−21,000	None	No		
2	13–1	− 5,500	W	Yes		Had counterpoise not been in use, strokes 2 and 4 would likely have caused three-phase faults similar to five multitower faults in the 12th and 13th miles during 1931 to 1937 inclusive
	13–2	− 9,500	W			
	13–3	−37,000	W			
	13–4	−10,000	W			
3	15–3	− 7,000	Y	Yes		
	15–4	−45,000	Y			
4	13–1	−17,500	W	Yes		
	13–2	− 8,500	W			
	13–3	+ & −?	W			
5	16–3	− 3,000	None	Yes		
	16–4	+ 5,500	None			For structures shown herein current measurements were made only in the two corner legs connected to the counterpoise. Currents shown are 2.67 times sum of leg measurements
	16–5	+ & −?	None			
	17–1	+40,000	W		−11,000	
	17–2	+81,000	X		−25,000	
	17–3	−78,000	YXW		+34,000	
	17–4	−37,000	X		+ 2,500	

* These values are 2.67 times sum of currents measured in the two legs which are connected to continuous counterpoise. This factor has been derived from the following tentative ratios of corner current to leg current: (*a*). Counterpoise legs 2.25. (*b*). Noncounterpoise legs 1.68.

Table VIII. Experience on Towers Partially Equipped With Expulsion Protective Gaps During 1936-38

Item	Flashed Towers	Period of Time	Number of Towers	Rate Per Mile Per Year
1	Nonoverhead-ground-wire towers (excluding 8–1 to 10–1)	1926–38	328	0.72
2	Total nonoverhead-ground-wire towers	1926–38	356	0.74
3	*SR* 8–1 to 10–1 only (13 years)	1926–38	28	0.98
4	*SR* 8–1 to 10–1 only (3 years) (flashovers on nongap-equipped assemblies)	1936–38	8	1.21
Towers Having				
5	Successful gap operations	1936–38	10–15*	1.5–2.3
6	Gaps destroyed	1936–38	2	0.30

* Actual minimum and probable total figures.

the rest of 1937 eight gaps were used. During 1938 the phase *W* replacement gaps and ten new gaps on phase *Y* (east) assemblies were installed.

Table VIII shows that lightning severity in the region traversed by these ten towers is greater than for the total nonoverhead-ground-wire sections of line. For example, items 1 and 3 show that the relative numbers of towers flashed was $1^1/_2$ times as great, and items 4 and 5 indicate that these ten towers were affected even more severely during the period when gaps have been used. Consequently, this short section of line has made an excellent proving ground for the gaps.

Item 4 involved eight towers and 11 assemblies, only two of which were equipped with gaps. During 1938 when all 20 outside insulator assemblies were gap equipped, at least two and probably six or perhaps seven gap operations occurred, but no insulators were flashed. Comparison of items 5 and 6 suggests that gap operations have been successful in from 83 to 88 per cent of the cases where they have been called upon to operate. This seems to be typical of experience with gaps of lower voltage rating, and represents fair protection.

THYRITE LIGHTNING-ARRESTER RECORDS, AND WALLENPAUPACK SPILLWAY-GAP FLASHOVERS

Since April 1934 two 16-unit Thyrite arresters have been in use on phases *Y* and *W* (outside conductors) at Wallenpaupack. At this location 42.5-inch ring-ring spillway gaps are in use on all three conductors. Table IX lists records which have been obtained.

It seems significant that no gap flashovers have occurred on the arrester-equipped phases and it can be concluded that the arresters are functioning satisfactorily.

Results and Conclusions

1. Most lightning strokes to this transmission line occur between negatively charged clouds and positive earth, and are termed "negative".

2. Approximately three out of every four strokes to an overhead-ground-wire section of this line occurred to such wires between structures. In no case did a measured stroke involve more than one of the two wires, nor cause insulator flashover.

3. Stroke-current magnitudes are believed to be most accurately measured by lightning-rod currents, or summations of currents in the two ends of stricken overhead ground wires. Twelve such strokes, all negative, have ranged in magnitude from about 20,000 to 70,000 amperes.

Table IX. Wallenpaupack Records From Thyrite Arresters and Spillway Gaps

Thyrite Arrester Records		Number of Spillway Gap Flashovers		
		Phases		
Phase, Polarity, and Amperes	Year	Y	X	W
	1926–1933	7	6	8
Y-650	1934	0	0	0
Y-240, *W*-440, *Y*-750	1935	0	0	0
Y-1600, *Y*-1600	1936	0	1	0
Y-3,700, *W*-1,500, *Y*-2,950, *W*-200	1937	0	1	0
None recorded	1938	0	0	0

4. Of 370 structure current records, 355 or 96 per cent were negative, 3 per cent were positive, and 1 per cent were uncertain. Of the 190 records exceeding 10,000 amperes, all but 4 were negative, as were the 3 records exceeding 100,000 amperes.

5. Structure currents are roughly twice as great as sums of corner-leg currents.

6. The practice of adding currents from adjacent structures is not recommended. It can give erroneous results for both numbers and apparent magnitudes of strokes.

7. Summations of total structure currents and in-flowing overhead-ground-wire currents are usually in excess of the lightning-rod current or out-flowing currents in the stricken conductor. Instantaneous surge-current changes are expected to occur at impedance junctions. Another factor is that current records are usually maximum or crest magnitudes, and not instantaneous values.

8. Buried counterpoise when used in conjunction with overhead ground wires provides multiple paths to ground for lightning, and distributes such current over long distances. Continuous counterpoise seems ideal, and a 2.6-mile section has had a perfect ten-year record. Noncontinuous or crowfoot cables have had an excellent but not quite perfect nine-year record in preventing insulator flashovers.

9. The effectiveness of overhead ground wires and efficient tower-footing grounding is concisely shown by the following:

Type of Construction	Towers Flashed Per Mile Per Year	Relative Effectiveness
No overhead ground wires	0.740	1.0
Overhead ground wires alone	0.200	3.7
Overhead ground wires and four short crowfeet	0.015	49.3
Overhead ground wires and continuous counterpoise	None	Infinity

10. The current (measured near the tower) in noncontinuous counterpoise seems to be in proportion to length of cable.

11. The rate of current collection for 250-foot crowfeet is nearly uniform for most of the cable but seems to increase rapidly near the free end.

12. Current measured near the tower in 50-foot crowfeet is about 43 per cent as much as in the three directly-connected structure members. For 250-foot cables this figure is 76 per cent.

13. Experience during one season with 12.5 miles of continuous counterpoise on nonoverhead ground wire is not conclusive because of incomplete measuring facilities. Performance seems to be as good as was expected. This includes:

(*a*). Avoidance of flashover for strokes to structures only, and

(*b*). Prevention of spread of flashover from stricken to nonstricken line conductors.

14. 220-kv expulsion protective gaps installed on one or two phases at ten towers are believed to have avoided tripouts in eight cases, and failed once. In the former cases the three largest structure currents, all from separate strokes, were 35,000, 26,000, and 22,000 amperes. In the latter case currents were not measured.

15. Discharge currents ranging up to 3,700 amperes have been measured in the Wallenpaupack Thyrite lightning arresters. Prior to arrester installation, an average of one flashover per year occurred on each of the three $42^1/_2$-inch spillway gaps. None have occurred on arrester-equipped phases during their five years of use.

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The Application of Class B Insulation to Auxiliary-Type D-C Motors in Severe Duty Service

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IN three papers[1,2,3] recently presented before the AIEE the life characteristics of class *A* insulation were discussed along with the rating, duty cycles, temperature measurements, and application of class *A* insulated induction motors. It is believed that a paper covering the application of class *B* insulation to electric motors would be in order. This paper is intended to show the advantages of class *B* insulation for certain applications, especially in d-c motors for severe duty service. The d-c motors covered by this paper are for:

1. Steel-mill auxiliary drives (totally enclosed and forced ventilated): screwdown, manipulators, side guards, table, shear, pinch roll, cranes, etc.
2. Electric shovel motors (both self-ventilated and forced ventilated): hoist, crowd, and swing
3. Other applications (enclosed, self-ventilated, and forced ventilated): dredges, hoists, cranes, and oil-well drilling rigs

In the d-c motors used in the several services outlined above it is of prime importance that the winding insulation be of such material and construction that long winding life and trouble-free service will be realized. This is especially true in some of the auxiliary steel-mill drives, as well as on electric shovels and gold dredges where a few hours delay as the result of insulation failure may cause a loss of an amount many times the price originally paid for the motor.

To put all motors on a standard, consistent, and reliable basis of rating, it is suggested in this paper that the resistance method of determining temperature rises on d-c severe-duty motors, based on higher temperatures fully to utilize the advantages of class *B* insulation as explained in this paper, be adopted as the preferred method, instead of the currently accepted thermometer method, as the basis for rating motors. The present thermometer method would, no doubt, be continued as an alternative method for a period of time because of the many motors now rated on that basis.

Once an accurate method of determining temperature rise has been standardized, it remains but to give the user operating recommendations relative to magnitude and time of overloads to the end that a motor may be properly applied and the user profit by obtaining the full economic life of the winding insulation.

The data presented in this paper are necessarily of a somewhat theoretical nature due to the lack of exact information in regard to the life of class *B* insulation at widely different temperatures.

For a proper understanding of the information given in this paper it must be remembered that this paper is written as applying particularly to auxiliary steel-mill motors, electric-shovel motors, and other applications as given in the first paragraph. The discussion that follows is not intended to apply to other classes of machines where the type of construction and nature of application differ.

Classes of Insulation

The two common types of insulation used in electric motors, and as described in the AIEE Standards, are:

1. *Class A*. Class *A* insulation consists of cotton, silk, paper, and similar organic materials when impregnated or immersed in oil; also enamel as applied to conductors.
2. *Class B*. Class *B* insulation consists of inorganic materials such as mica and asbestos in built-up form combined with binding substances. If class *A* material is used in small quantities in conjunction for structural purposes only, the combined material may be considered as class *B*, provided the electrical and mechanical properties of the insulated winding are not impaired by the application of the temperature permitted for class *B* material. (The word "impair" is here used in the sense of causing any change which could disqualify the insulating material for continuous service.)

Class A Insulation

Before attempting to point out the need for class *B* insulation on some applications of d-c motors it might be well to consider the merits of class *A* insulation.

The salient advantages distinguishing class *A* insulation from the class *B* type are:

1. Lower first cost. If two motors of similar construction and the same torque rating be compared, one of which is built with class *A* insulation to operate at allowable class *A* temperature rise, and the other built with class *B* insulation to operate at allowable class *B* temperature rise, it will be found that the class *A* insulated motor will be larger physically, but the difference in cost will not be proportional to the weight, because of the less expensive class *A* materials. In the case of small shunt motors, the cost of the somewhat larger class *A* insulated motor might even be the same or less.
2. Greater flexibility and tensile strength. Class *A* materials, such as cotton tape and varnished cloth, are quite flexible and may be wrapped tightly around corners without damage to the varnish film or cotton fibers.
3. Superior adaptability to the insulation of irregular shapes. By virtue of (2) above, class *A* material can be readily applied to irregular shapes such as channels for pole pieces and head insulation for armature

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Discussion

For discussion, see page 1098.

back-end windings. Class *A* insulation in the form of phenolic resins with a cloth or rag base can be molded onto metal parts of very irregular shapes.

The greatest disadvantage of class *A* insulation is that by virtue of its chemical and physical properties, all of its apparent merits are nullified when the insulation is subjected to high temperatures for any appreciable length of time.

It has been pointed out elsewhere[1] that the life of class *A* insulation varies exponentially with the temperature to which it is subjected. A rough rule based on laboratory tests and extensive service experience is that the life of class *A* insulation is reduced to one half for each increase of 8 degrees centigrade in temperature, and that the total life at a continuously maintained temperature of 100 degrees centigrade is of the order of ten years. The 105-degree-centigrade hot-spot temperature allowed by the AIEE standards for class *A* insulation corresponds to an expected life of seven years. Thus, if class *A* insulation were subjected to a temperature of 150 degrees centigrade the life would be reduced to about $^1/_{64}$ of ten years (approximately two months) and under temperature conditions of 180 degrees centigrade the anticipated life would be but four days.

Class B Insulation

Class *B* insulation as applied to the d-c motors covered by this paper consists of either mica or felted-asbestos conductor insulation and mica ground insulation with an outside taping of asbestos. "Fiberglas" insulation is sometimes used in place of asbestos. The amount of class *A* structural material used in the class *B* insulation generally consists of thin sheets of paper or cloth to hold the mica splittings in place during the process of insulating and assembling the windings, along with the insulating varnish which fills the air spaces in the asbestos or Fiberglas taping. Field coils are generally impregnated with an asphalt-base compound.

The principal advantages of class *B* insulation over class *A* insulation are:

1. Ability to withstand high operating temperatures. The prime reason for using class *B* insulation is its ability to withstand high operating temperatures for long periods of time. Inherently, neither mica nor asbestos is affected by the high operating temperatures to which motor insulation is subjected. The class *A* structural material and the insulating varnishes used in class *B* insulations may be practically destroyed by excessive temperature over long periods of time but the remaining mica and asbestos will still give service if the insulation is held securely. Discussion in this paper is confined to class *B* insulation of the types used for d-c machines of normal voltage rating for steel-mill auxiliary drives and other applications previously outlined, where experience has shown that the mica insulation remains in place even after the destruction of the class *A* bonding material. The principles given may not be applicable to other classes of machines.

2. Greater mechanical strength of mica. Mica will withstand compressive stresses and abrasion better than any class *A* insulating material. All coils are subjected to concentrated pressures and abrasion during the following processes:

(*a*). Winding, insulating, handling, and assembling of coils in the motor
(*b*). Frequent starting of the motor
(*c*). Vibration during operation
(*d*). Expansion and contraction of windings from heating and cooling
(*e*). Rotation of armature resulting in centrifugal forces on the coils

Varnish film tends to distribute the normal pressure on coils and when this film is destroyed the insulation is subjected to concentrated pressures. The ability of mica to withstand high concentrated pressures without mechanical breakdown and subsequent dielectric failure makes it a valuable asset to coil insulation.

3. Moisture resistance. The moisture resistance of class *B* insulated coils with mica insulation is superior to class *A* coils. Class *A* coils depend entirely on the integrity of the varnish film to resist moisture absorption. Class *B* insulated coils depend primarily on the varnish film but not entirely. After the varnish film has dried out and cracked, due to service conditions, openings conducive to the entrance of moisture will be in evidence regardless of the type of insulation employed. The entrance of moisture to the conductors of class *B* insulated coils should be considerably less and at a slower rate than into the class *A* insulated coils because the individual mica splittings are impervious to moisture. The only way the moisture can penetrate the coil is by traveling the long creepage path between the laps of the mica tape.

All motors are subjected to moisture which is prevalent in the atmosphere to a greater or lesser degree. Since class *B* insulated motors normally operate at higher temperatures than class *A* insulated motors, there will be less tendency to moisture absorption by the class *B* insulation while the motors are at operating temperature. During idle periods moisture will be absorbed by either type of insulation. Upon the resumption of operations the moisture in class *B* insulation will be evaporated more readily because of the higher operating temperature under which the class *B* insulated equipment is operated.

Overload and Temperature Limitations

D-c motors for severe-duty service have relatively high torque capacity. It is possible for a d-c compound- or series-wound motor to develop more than ten times the normal torque obtained at the one-hour rating. Since it is possible to obtain such a high torque it is necessary to limit the torque to such values as will not cause damage to the motor. The generally accepted limiting torque values for 230-volt class *B* insulated motors for severe duty, expressed in terms of the one-hour enclosed rated torque (see appendix I) are:

Maximum starting torque:
500 per cent for series-wound motors
450 per cent for compound-wound motors
360 per cent for shunt-wound motors

Maximum running torque:
400 per cent for series-wound motors
350 per cent for compound-wound motors
300 per cent for shunt-wound motors

The maximum starting torque of the series-wound motor is limited by the mechanical construction of the motor, and principally by the shaft stresses and deflection, and bearing loads. The maximum running torque is limited by the commutating ability of the motor. The frequency with which the maximum torques are developed and the duration of time are both limited by the allowable hot-spot temperature in the motor.

Concomitant with the progress in the development of insulating materials to withstand higher temperature, and the wider knowledge of the service life of such materials, the natural trend in new designs would be to assign higher ratings based on the higher temperature and at the same time to provide the necessary mechanical strength and commutating capacity. This procedure would allow greater loads to be applied to a given size of motor so that the user could better realize the full economic life of the insulation.

There are some applications of auxiliary d-c motors in steel mills where the high ambient temperature makes it imperative to use class *B* insulation, for example, many roll drives, screwdowns, ladle cranes, and other drives. The high ambient temperature would leave little room for temperature rise in the motor windings if class *A* insulated motors were used and operated at the permissible hot-spot temperature of class *A* insulation. It is imperative that all of the above-mentioned motors be either totally enclosed or forced ventilated with clean dry air, because of the dirt, moisture, and fumes in the atmosphere. Quite often it is not feasible to force-ventilate these motors with clean air, and in view of this it is necessary for all of the heat produced by the motor losses to be dissipated from the external surface of the motor. If a class *A* insulated motor were used in place of a class *B* insulated motor the motor would have to be considerably larger to hold the temperature rise down to that permissible for class *A* insulation. The higher the ambient temperatures the greater is the ratio of the allowable temperature rise of class *B* insulation over that of class *A* insulation, maintaining their same respective allowable hot-spot temperatures.

The temperature rise of a d-c totally enclosed motor is assumed to be roughly proportional to the watts loss per square inch of surface. The allowable temperatures and rises as given in the AIEE standards are:

	Class A	Class B
Limiting hot-spot temperature (deg C)	105	125
Limiting observable temperature by resistance method (deg C)	95	115
Limiting observable temperature rises by resistance (deg C) (based on 40 deg C ambient)	55	75

With a 40-degree-centigrade ambient and a given duty cycle the class *A* insulated motor could have a rise by resistance of 55 degrees centigrade and the class *B* motor a rise by resistance of 75 degrees centigrade. From the ratio of 75/55 or 1.36 it is found that the class *B* insulated motor can dissipate 36 per cent more watts per square inch than the class *A* insulated motor. If a 60-degree-centigrade ambient be assumed the rise by resistance of the class *A* insulated motor becomes 35 degrees centigrade and that of the class *B* insulated motor 55 degrees centigrade, and the ratio of 55/35 or 1.57 indicates that the class *B* insulated motor can dissipate 57 per cent more watts per square inch than the class *A* insulated motor. As the difference in watts per square inch increases, so do the size and weight of the class *A* insulated motor increase over those of the class *B* insulated motor, although not in direct proportion. Quite often an increase in size and weight of a motor will make its application uneconomical.

In applications where starting and stopping constitute a major portion of the duty cycle, the rotor WR^2 must be considered in choosing the proper motor to perform the service required. When class *B* insulated motors are designed to operate at the allowable class *B* temperature rises a lower WR^2 is possible, as less active material is required than if the motor were designed to operate at allowable class *A* temperature rises. In applications requiring rapid acceleration and deceleration, the WR^2 of the motor often represents an appreciable portion of the total WR^2 involved. By reducing the motor WR^2, through the use of class *B* insulation, the whole drive can be accelerated and retarded in less time. Also, by reducing the total WR^2 of the drive, the actual root-mean-square load of the duty cycle is reduced. Thus class *B* insulation serves a twofold purpose—it reduces the motor heating and it enables the motor to withstand a higher temperature rise.

In service such as that encountered in steel mills and on electric shovels, overloads are applied to the motors with great frequency. Some of these overloads are part of the normal cycle of operation and some of them are accidental and unpredictable. An overload which would increase the temperature rise on a class *B* insulated motor to twice its allowable rise would do less damage to the class *B* insulation than the same per cent overload for the same length of time on a class *A* insulated motor would do to the class *A* insulation; that is, a 150-degree-centigrade rise (or 190-degree-centigrade temperature) will take less life out of class *B* insulation than a 110-degree-centigrade rise (or 150-degree-centigrade temperature) will take out of class *A* insulation.

Class *B* insulation also plays an important role in the rewinding of motors that were originally designed and applied with class *A* insulation, where it is desired to increase the duty cycle to such an extent that the class *A* insulation would have an uneconomical life.

From the foregoing advantages and uses it can be seen why class *B* insulation plays such an important part in helping to maintain continuity of service under operating conditions involving extreme dirt, moisture, vibration, high ambient, and overloads.

The expected life of class *A* insulation as previously mentioned, is approximately seven years at 105 degrees centigrade; that is, if class *A* insulation is steadily maintained at a temperature of 105 degrees centigrade, tests indicate that the insulation will reach the end of its useful life theoretically in seven years, or at the end of that period be subject to immediate failure under any mechanical or electrical shock. Such a life curve based on tests is not available for class *B* insulation. Although the AIEE standards allow the hot-spot temperature for class *B* insulation to be only 20 degrees centigrade greater than for class *A* insulation, actual service tests on some railway motors have shown the peak operating temperature to be from 180 to 200 degrees centigrade as measured by resistance. In one particular case some class *B* insulated machines were found operating in an 80-degree-centigrade ambient with a peak temperature of over 200 degrees centigrade and had been operating under these conditions for an appreciable length of time without insulation failure. It is not recommended that class *B* insulated motors operate at peak temperature of 200 degrees centigrade but it is known that there are many applications of class *B* insulated motors and generators where the peak temperature of the windings exceeds 200 degrees centigrade and the average operating temperature exceeds 150 degrees centigrade and the windings give long satisfactory service.

If, for some reason or other such as an emergency condition or through some fault, a class *B* insulated motor is called upon to operate at prolonged excessive overloads that result in abnormally high winding temperatures, the class *A* structural material will be destroyed, but the life of the remaining class *B* insulation can be greatly prolonged simply by dipping the armature or field coils in insulating varnish.

It would be a rather difficult problem to establish a life expectancy curve for class *B* insulation as has been done for class *A* insulation, because different amounts and kinds of class *A* structural materials are used by the various electric-motor manufacturers. There are many kinds of so-called class *B* insulations. In general, the class *B* insulation with the least amount of class *A* structural material should have a greater allowable hot-spot temperature for the same length of life. This applies to the d-c motors discussed in this paper and not to large high-voltage machines. The general tendency is toward the use of less class *A* material for structural purposes and better grades of insulating varnish which will maintain their desirable qualities at higher temperatures.

The 125-degree-centigrade hot-spot temperature, which is allowed for class *B* insulation according to the AIEE standards, therefore, seems too conservative for class *B* insulation as applied to d-c motors of normal voltage rating in severe duty service. That is, if such class *B* insulation be subjected continuously to a temperature of 125 degrees centigrade, a life expectancy far in excess of the seven-year life expected of class *A* insulation at 105 degrees centigrade would be in order.

In order to show the relation that exists between overloads and allowable temperature rise it is assumed that the life-expectancy curve for class *B* insulation varies exponentially with time in a manner similar to that of the curve for class *A* insulation. It is also assumed that this curve passes through the point of ten years at 125 degrees centigrade, and that the slope or exponent is such that the life of the class *B* insulation is reduced by 50 per cent with each 12-degree-centigrade increment of temperature. This assumed life curve is shown in figure 1, with the life curve for class *A* insulation. Available information on the expected life of class *B* insulation is rather meager, but from the few laboratory tests that have been made on samples of class *B* insulation as applied to d-c severe duty motors and the knowledge of operating temperatures on some applications of transportation motors, it is felt that the assumed life characteristic of class *B* insulation is very conservative. It must be remembered that this life curve (figure 1) for class *B* insulation is an assumed curve for purposes of discussion and to show the general relation existing between overloads and allowable temperature rise. The curves derived from the class *B* curve, figure 1, are not intended to show actual values that may be used in the application of any motor.

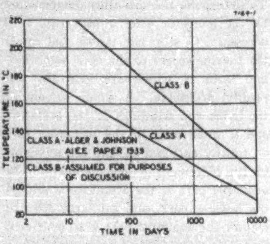

Figure 1. Life expectancy versus temperature for class A and class B insulating materials in continuous service

From the assumed life curve for class *B* insulation it can be seen that if a motor is operated continuously or 100 per cent of the time, the allowable hot-spot temperature rise for a 40-degree-centigrade ambient would be 85 degrees centigrade to give an expected life of ten years. The same motor operated only 50 per cent of the time, or on a long duty cycle of 50 per cent time on where the actual time of "power on" is three or four hours or more, may be operated in a 40-degree-centigrade ambient with a hot-spot temperature rise of 97 degrees centigrade and have an expected life of ten years. The additional 12 degree-centigrade hot-spot rise for one-half the time on would reduce the insulation life 50 per cent, but since the motor is operated for only one-half the time, the insulation would last ten years. This reasoning is based on instantaneous heating and cooling of the motor which, on account of the time lag of the temperature, is never realized, but is approached on long duty cycles.

The curves in figure 4* (see appendix II) indicate the advisability of using class *B* insulation in motors on applications resulting in a high ratio of peak temperature rise to average temperature rise, even though the average temperature rise does not exceed the maximum allowable rise for class *A* insulation, for example, if an application is assumed where the minimum permissible relative life of the motor insulation is 0.5 and the motor is operating with a ratio of average temperature to maximum allowable temperature of 0.8 (approximately 90 per cent of maximum continuous load), a change from class *A* insulation to class *B* insulation in the motor will permit the ratio of peak temperature to average temperature to be increased from 1.5 to 2.25 or 50 per cent. The corresponding increase in load will be about 25 per cent. The curves in figure 4 are based on a rectangular temperature cycle and instantaneous heating and cooling and are derived from the expected life curves for class *A* and class *B* insulation (assumed) in figure 1.

Since the aging effect on the insulation is less than assumed by instantaneous heating and cooling, greater loads may be applied for the same per cent "time on" to give the expected ten-year life. There are, however, two compensating errors both of which tend to offset the additional life due to temperature lag:

1. Increase of resistance with temperature causing greater watts loss for the same current load.
2. Starting current, which is generally greater than the running current during "power on", causes additional heating which has not been taken into account.

For cycles of short duration, for example, less than three or four hours, the effect of the temperature lagging behind the time becomes greater. This effect is shown graphically in figure 2 for a long and a short cycle. For relatively short cycles, each successive application of load to the motor raises the winding temperature less than the previous application. Each successive cooling period reduces the temperature more than the previous period. This process continues with each additional cycle until the temperature increase during "power on" equals the temperature decrease during "power off". For relatively long cycles, the temperature rise during "power on" approaches a constant value and the temperature lag has little or no effect on the motor heating. For long periods of "power on" and long cycles, the effect of temperature lag approaches that of instantaneous heating and cooling. Figure 3, curve 1, shows the permissible loading to give 90-degree-centigrade rise by resistance (peak rise) for a continuously repeated five minute duty cycle in which during the "power on" the starting amperes were equal to

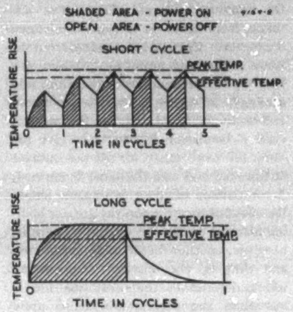

Figure 2. Effect of temperature lag on cyclic heating

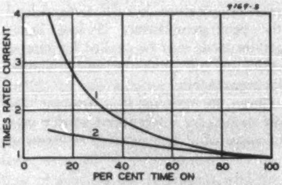

Figure 3. Permissible current loading of typical d-c totally enclosed motor in intermittent service for the same insulation life expectancy

Curve 1—For 90-degree-centigrade rise by resistance on a five-minute duty cycle. Based on tests with starting amperes equal to running amperes

Curve 2—For same insulation life expectancy as curve 1, but based on instantaneous heating and cooling, assumed life curve of class *B* insulation in figure 1, and ambient of 25 degrees centigrade the running amperes. This curve was taken from tests on a typical enclosed d-c motor. Curve 2 is estimated from the losses in the same motor but is based on a long cycle (instantaneous heating and cooling), 25-degree-centigrade ambient, and on the assumed expected life curve of class *B* insulation (figure 1) to give the same life expectancy for all percentages of time on, and also the same as curve 1. A comparison of these two curves shows the effect of time lag and the greater loading allowed on shorter cycles. There is, however, another factor to be considered and that is, the effective temperature which determines the insulation life is less than the peak temperature upon which curve 1 is based. The peak hot-spot temperature of curve 1 for a 25-degree-centigrade ambient would be 125 degrees centigrade (10 degrees centigrade allowed for hot-spot temperature above resistance temperature) which exists momentarily at the end of the "power on" part of the cycle. During the remaining part of the cycle the hot-spot temperature is less than 125 degrees centigrade. For 50 per cent time on (curve 1) the effective hot-spot temperature is approximately 120 degrees centigrade or 5 degrees less than the peak hot-spot temperature. This difference between the effective temperature and the peak temperature is so small for a five-minute cycle that it has been neglected in the comparison made on curves 1 and 2. For 100 per cent time on the difference between effective and peak temperature is zero and for 10 per cent time on it would be slightly greater than 5 degrees centigrade. As the time of the duty cycle is shortened, the effective temperature approaches the peak temperature, but for a given load the peak temperature is less, hence greater loads may be applied for shorter cycles and still obtain the same insulation life expectancy.

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* Calculations for the curves of figure 4 as described in appendix II were made by T. C. Johnson, General Electric Company.

From the foregoing it is apparent that d-c motors for intermittent service may be operated at temperatures in excess of the allowable hot-spot rise and still have the same insulation life as though they were operated continuously at the allowable temperature. The amount of excess temperature rise depends on the following:

1. Ambient temperature—the lower the ambient temperature the higher the allowable rise
2. Per cent of "time on"—the lower the per cent of "time on" the higher the allowable rise
3. Heating time constant of motor—the greater the heating time constant the higher the allowable rise
4. Cooling time constant of motor—the less the cooling time constant, the higher the allowable rise

Figures 1, 3, and 4 are not intended to give actual values since they are based on an assumed expected life curve for class *B* insulation, which is felt to be conservative. These curves are intended to illustrate the general relations existing between the effects produced by the several variables that must be considered in applying a motor to a particular service.

Method of Temperature Measurement

The AIEE standards recognize three methods of temperature measurement in the rating of electrical machinery and apparatus.

1. Thermometer method—"This method consists in the determination of the temperature, by mercury or alcohol thermometers, by resistance thermometers, or by thermocouples, any of these instruments being applied to the hottest part of the machine accessible to mercury or alcohol thermometers."
2. Resistance method—"This method consists in the determination of temperature by comparison of the resistance of a winding at the temperature to be determined with the resistance at a known temperature."
3. Embedded-detector method—"This method consists in the determination of the temperature by thermocouples or resistance temperature detectors, built into the machine as specified in the section of the standards dealing with the specific kind of machine."

The basic standardized horsepower ratings of auxiliary-type d-c motors are limited to 75-degree-centigrade rise by thermometer.

This method of determining the temperature of d-c motors by thermometers does not give the most consistent results. The armature is generally the hottest, hence the limiting, part of the motor. To determine the armature temperature by thermometer after a heat run, it is necessary to stop the motor and apply the thermometers to the core and winding, both of which are generally not readily accessible. A small portion of the front end windings of the armature and a small area of the armature core on totally enclosed motors are available for the placement of thermometers. The available part of the armature is seldom the hottest part, and on some sizes of motors the available part is so restricted that it is quite difficult properly to place and to protect the thermometer bulb so that it gives a true indication of the actual temperature at that point. Even if the thermometer could be placed on the hottest spot, an intimate contact with the insulation made, and the thermometer bulb properly protected from the surrounding air, a certain time would be required for the thermometer to register the temperature of the spot on which it is placed. While the thermometer is coming up to temperature, the temperature of the spot being measured may in some cases be falling. Hence the maximum temperature indicated by the thermometer may not always be the temperature at the end of the heat run. This time lag of thermometers can be reduced by preheating the thermometer to about 10 or 20 degrees centigrade less than the anticipated thermometer temperature.

Temperature measurements by the resistance method can be conveniently and accurately made, and, when properly made, give very consistent results. There are certain precautions that must be observed in measuring the winding resistances, such as using the proper type of bridge; separate voltage and current leads, and making sure that all connections are clean and tightly made. In addition, when determining the cold resistance or resistance at a known temperature good practice dictates the placement of thermometers on all windings to be measured and the taking of resistance measurements only when the thermome-

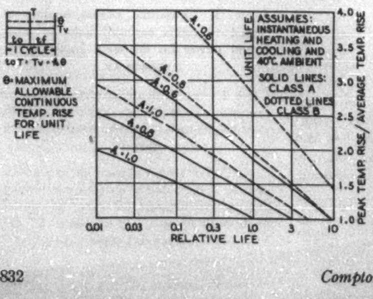

Figure 4. Comparison of relative life of class A and class B insulation for rectangular temperature cycles having same average temperature for different percentages of the maximum continuous temperature based on life expectancy curves figure 1

ters on the windings are within three degrees centigrade of the ambient temperature. If the difference between the winding temperature and ambient temperature is much greater than three degrees, the winding resistance is changing and an error in cold resistance is likely to be introduced.

At the end of a heat run the stopping of the motor and the measurement of the resistance of all windings can be accomplished in one to two minutes time. Thus determination of the temperature of an armature winding by the resistance method in much less time after shutdown is possible than by the thermometer method (thermocouple excepted). From resistance readings taken at various intervals after shutdown a cooling curve of temperature versus time can be plotted. By extrapolation of this curve back to zero time, actual winding temperature at the time the power was cut off can be determined.

In a previous paper[3] a very complete discussion is given on the advantages of the resistance method over the thermometer method of determining temperatures, with supporting evidence taken from tests on various induction motors, which show the possible inaccuracy of the thermometer method and the accuracy and consistency of the resistance method.

The AIEE standards are based on a certain allowable hot-spot temperature, which for class *B* insulation is 125 degrees centigrade. This hot-spot temperature determines the life of the winding insulation. Since it is not convenient or practical to measure the hottest spot in a winding, the AIEE standards specify a "conventional allowance" by which the observable temperatures are assumed to be lower than the "hottest spot" temperatures. The conventional allowances in degrees centigrade are:

Thermometer method	15
Resistance method	10
Embedded-detector method	5

Since it is recognized that there is less difference between the hottest spot and the resistance temperature than between the hottest spot and the thermometer temperature, it follows that the resistance temperature would give a truer indication of the insulation life than the thermometer temperature.

A study was made of the difference between thermometer and resistance temperature rises from 91 different heat runs made on ten different frame sizes of d-c class *B* insulated totally enclosed auxiliary-type steel-mill motors. Each heat run was made for a definite length of time starting cold with the motors loaded to give approximately 75-degree-centigrade rise by thermometer on the hottest winding, which in all cases, was the armature. The per cent difference between the rise by resistance and the rise by thermometer plotted against length of time of heat run is shown for one size of motor in figure 5.

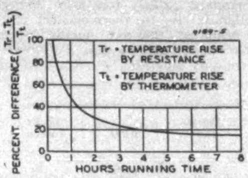

Figure 5. Relation of per cent difference of resistance rise over thermometer rise for various lengths of heat runs

In all cases the rise by resistance is greater than that by thermometer. The general shape of this curve is characteristic of the curves obtained on all sizes of motors tested. However, the smaller motors show flatter curves, and the larger motors show slightly steeper curves. The general range of resistance rise minus thermometer rise for one-hour heat runs was 10 to 35 degrees centigrade for the various frame sizes, while for eight-hour heat runs the range was from 5 to 15 degrees centigrade.

With the present practice of rating d-c motors on a 75-degree-centigrade-rise-by-thermometer basis, a greater rise by resistance is obtained on the short-time 15-minute or 30-minute ratings than is obtained on the one-hour or five-hour ratings. This is not quite so inconsistent as it may appear because the short-time-rated motors are applied on duty cycles with less per cent "time on" than are the longer-time-rated motors. In the foregoing discussion on overload and temperature limitations it was pointed out that with less per cent "time on" for a given duty cycle the allowable temperature rise is greater for the same expected insulation life.

Continuous heat runs on ventilated motors in contrast to totally enclosed motors, show resistance rises exceeding thermometer rises over a range of from 20 to 50 degrees centigrade depending on the size of motor and type and amount of ventilation. The ratio of the rise by resistance over the rise by thermometer will increase as the amount of ventilation is increased.

Figure 6 shows graphically the general relation of the input watts to the watts dissipated. The area under the input curve represents the energy input in losses while the area under the dissipated curve represents the energy dissipated. The difference between these two areas represents the energy absorbed which raises the temperature of the winding and is an indication of the winding temperature as measured by resistance. The amount of watts being dissipated at the end of the heat run is an indication of the surface temperature of the insulation as measured by thermometer. Approximate relations of watts loss to copper and insulation temperature rises are given in appendix III.

Although all frame sizes of motors are rated on the same basis, that is, 75-degree-centigrade rise by thermometer, the actual rise by resistance is greater than 75, and the amount the resistance rise exceeds the thermometer rise varies for different frame sizes. The larger motors, in general, have a greater rise by resistance than the smaller motors for the same rise by thermometer.

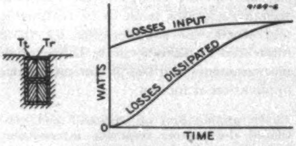

Figure 6. Relation of losses input and dissipated to time

See appendix III

The International Electrotechnical Commission has recently adopted the resistance method as standard for electric traction motors. The following method of determining the motor temperatures at time of shutdown was agreed upon according to the minutes of the meeting held in Torquay on June 22, 23, and 25, 1938. The measurement of resistance of the armature shall be made in a time less than, or equal to, $1^1/_2$ minutes from the time of shutting off the current, and the other resistance measurements on fixed windings shall be made within a maximum time limit of 2 minutes. The temperature rise at the moment of cutting off the load to be determined by linear rectification of the first part of the cooling curve (first 5 minutes) plotted by using a logarithmic scale for the temperature rises and a linear scale for the time.

The resistance method of temperature determination for railway motors has been adopted by the AIEE and it seems reasonable that the same method should be adopted for the d-c motors covered by this paper.

In order that

1. The temperature rating of d-c motors for severe duty service gives a more accurate indication of the actual temperature within the winding at the time of shutdown.

2. The temperatures of the windings may be more accurately and conveniently measured, whether on the test stand or in service.

3. The user may be assured that nameplate temperature rises are measured on a more consistent and comparable basis.

4. Any controversy over how, when, and where to place thermometers to determine winding temperatures may be eliminated.

It is recommended that the resistance method of determining temperature rise be adopted as the preferred standard for both totally enclosed and ventilated d-c motors for severe duty service.

Conclusions

In view of the arguments and exhibits set forth in the foregoing, the following recommendations seem to be reasonable and worthy of consideration by those interested in more accurate temperature measurements and the proper rating and application of motors.

1. In applications on enclosed and ventilated d-c motors requiring intermittent high torque (high ratio of peak torque to rms torque) or severe duty of any kind, full use of the torque capacity, operation in extremely high ambients, or in damp atmosphere or locations, the use of class *B* insulation seems advisable.

2. For future standards adopt resistance method of determining winding temperatures as adopted for railway motors.

3. If resistance method is used, substitute some higher value than the present AIEE 75-degree-centigrade rise, such as 105 to 120 degrees centigrade by resistance, as the basis for establishing the basic ratings.

4. A possible method of rating would be to adopt the one-hour rating based on the rise by resistance, and have operating recommendations, giving curves of permissible overloads for different percentages of "time on" with same years of life and various lengths of duty cycles.

Appendix I

Standard ratings (one-hour 75-degree-centigrade rise by thermometer) of class *B* insulated 230-volt totally enclosed series-wound auxiliary-type d-c motors for severe-duty service:

Horse-power	RPM	Lb-Ft Torque
5	900	29
7 1/2	800	49
10	725	72
15	650	121
25	575	228
35	525	350
50	500	525
75	475	830
100	460	1,140
150	450	1,750

Appendix II

In order to show the effects of increasing peak temperatures in duty cycles where the rms load is held constant let it be assumed that temperature rise is proportional to the square of the load; this means a comparison of temperature cycles with the same average temperature. Figure 4 shows the relative life of class *A* and class *B* insulation as a function of the ratio of peak temperature rise to average temperature rise for the same average temperature for various values of k, which is the ratio of average temperature rise to the maximum allowable rise for unit life on class *A* insulation. These curves are based on the life-expectancy curves of figure 1, instantaneous heating and cooling, rectangular load cycle at 40-degree-centigrade ambient, and a unit life of 9.2 years at 102 degrees centigrade for class *A* insulation. The curves show the permissible increase in ratio of peak to average temperature when the insulation in a motor is changed from class *A* to class *B* with no other changes. The solid lines are for class *A* insulation and the dotted lines for class *B* insulation and based on the assumed life curve of class *B* insulation figure 1.

Appendix III

Figure 6 shows the relation of the input losses and dissipated losses as a function of time. The difference in watts between these two curves represents the watts absorbed and the area between the two curves represents the watt-hours of energy absorbed by the winding which raises the copper temperature. For any length of heat run, if the rise by resistance is known the difference between the rise by resistance and rise by thermometer (on surface of insulation) can be approximated from the following relations:

T_r = temperature rise of copper—as measured by resistance

T_t = temperature rise on surface of insulation—as measured by thermometer

Watts input = watts absorbed + watts dissipated

$$T_r = \frac{\text{watt-hours absorbed}}{0.0495 \times \text{pounds copper}} \text{ in degrees centigrade}$$

$$(T_r - T_t) = \frac{\text{watts dissipated} \times L}{0.528 \times k \times A} \text{ in degrees centigrade}$$

A = area in square feet of dissipating surface

L = length of path in inches

k = thermal conductivity in Btu per hour per square foot per degree Fahrenheit per inch

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Discussion

For discussion, see page 1098.

New Developments in Current-Transformer Design

G. CAMILLI
MEMBER AIEE

Synopsis: In the past, instrument transformers, with few exceptions, have been designed along the lines of small power transformers and have frequently been unnecessarily bulky with other disadvantages which necessarily attend bulk. This paper describes a new line of high-voltage current transformers with relatively novel principles of construction by means of which volume and weights have been cut down by about a half and factor of safety increased, while more than meeting the NEMA standards of accuracy.

This accounts for the appearance of these transformers as mostly bushings. The most striking novel feature of construction of these transformers is the adoption of the highly efficient method of cable insulation to them by so arranging and shaping the design that the major insulation between the two windings is a closed torus with no ends and hence free from creepage problems. The execution of this ideal is made possible by designing the bushing as a truly integral and inseparable part of the transformer. Other features of these transformers include Spirakore magnetic circuits of exceptionally high magnetic performance, and adaption of the new method of insulation to "cascade" designs.

VERY noteworthy progress has been made in recent years in the design of current transformers for medium and high voltages. Previously most current transformers were designed and built according to the same general practices as a small power transformer, and its size, weight, and general appearance was that of a conventional transformer. The latest current transformers, described below, scarcely resemble their predecessors—their weight, size, etc., are but a small fraction of the older designs. A comparison of the principal data of two current transformers—one designed in 1929 and the other of recent development—may be of interest (table I).

For comparative outlines of the two transformers see figure 1.

The transformers compared in table I are of "single-stage" type, that is, they consist of a primary (high-voltage) winding and a secondary (low-voltage) winding fully insulated from each other. Progress has also been made in the design of those transformers in which the major insulation is subdivided into stages (cascade type).

Table II gives the comparison between the conventional transformers and two transformers of the cascade type, one designed in 1935 and the other of recent design.

The progress made in the design of current transformers for medium and high voltages can be best explained in the light of the progress made in the method of insulation; and the very new developments made in the manufacture of a wound core transformer under the trade name of "Spirakore".

Insulation

The high-voltage winding of a current transformer consists of a coil (having one or more turns), fully insulated from the low-voltage winding at ground potential.

In the high-voltage coil the insulation between turns is easily provided for. Consequently the coil may be considered as a single conductor which is insulated from ground. The analogy between this problem and that occurring in the design and manufacture of cables for medium and high voltages is apparent.

In the development of the new type of insulation advantage has been taken of the refined technique of the high-voltage cable design.

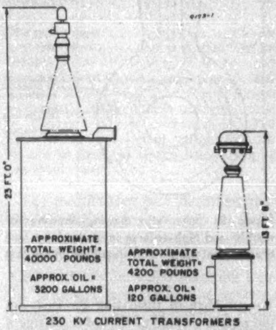
23 FT. 0"
APPROXIMATE TOTAL WEIGHT = 40000 POUNDS
APPROX. OIL = 3200 GALLONS
13 FT. 8"
APPROXIMATE TOTAL WEIGHT = 4200 POUNDS
APPROX. OIL = 120 GALLONS
230 KV CURRENT TRANSFORMERS

Figure 1. Comparative outline of conventional design and the newly developed transformer

Referring to figures 2, 3*a*, and 3*b*, the high-voltage coil is enclosed in a split hollow metal ring which leads to a pipe which encloses the terminals of the coil. The coil, the enclosing ring, and the pipe are electrically connected together and therefore are at the same potential.

Table I. Current Transformers for 230-Kv Circuit

	1929 Design	1939 Design
Weight (pounds)	40,000	4,200
Total height	23 ft	13 ft 8 in.
Gallons of oil	3,200	115
Accuracy*	¼*X*, ¼*Y*, ¼*Z*	¼*X*, ¼*Y*, ¼*Z*
Overload accuracy (number of times normal primary current with *Z* burden at which the ratio error reaches 5 per cent)	30	30

* Transformers have errors within the limits specified for class ¼, according to NEMA classification, at burdens *X*, *Y*, and *Z*.

Table II. Current Transformers for 287-Kv Circuit

	Conventional Design (Single Stage)	Cascade Design	
		1935	1939
Weight (pounds)	50,000	8,800	6,000
Total height	28 ft 6 in.	11 ft 8.5 in.	11 ft 8.5 in.
Gallons of oil	4,200	300	150
Accuracy	¼*X*, ¼*Y*, ¼*Z*	¼*X*, ⅓*Y*, ⅓*Z*	¼*X*, ¼*Y*, ¼*Z*
Overload accuracy (number of times normal primary current with *Z* burden at which the ratio error reaches 5 per cent)	30	10	12

Paper 39-173, recommended by the AIEE committees on instruments and measurements, and electrical machinery, and presented at the AIEE Middle Eastern District meeting, Scranton, Pa., October 11-13, 1939. Manuscript submitted July 24, 1939; made available for preprinting September 19, 1939; released for final publication December 8, 1939.

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The writer gratefully acknowledges his profound indebtedness to many of his associates (too numerous to list here) both in the engineering department and the insulation research laboratories of his company.

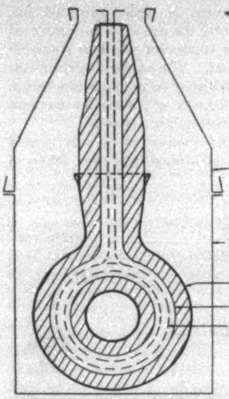

Figure 2. Schematic drawing showing assembly and high-voltage insulation of a new type current transformer described in the text

The problem of insulating the ring and the pipe is similar to that of insulating a high-voltage oil-filled cable, in that the insulation consists of crepe paper impregnated with mineral oil. This paper is lapped helically on the high-voltage sheath (ring and pipe) in the form of strips. After the required thickness of paper has been applied, the portion containing the high-voltage coil (figure 2) is covered with a metallic screen or shield which is connected to ground. This screen is equivalent to the lead sheath of the ordinary cable. The similarity between this type of construction and the well-known oil-filled cable is evident.

Figure 3a. View of high-voltage shield which enclosed high-voltage winding

Figure 3b. View of the high-voltage coil and casting

The grounded coil is ordinarily placed inside a small steel tank (figure 2), while the lead-in is placed inside a porcelain shell (bushing)—the performance of the latter being similar to the pressure reservoirs of the oil-filled cable. The hollow pipe allows the oil to flow out into the bushing when the load increases and to flow back when it decreases. Void formations in the insulation are entirely suppressed under working conditions. Needless to say, the transformer is hermetically sealed.

By proper choice of paper and a very careful heat vacuum treatment, a very flat power factor/temperature curve is obtained, and the danger of thermal instability is eliminated. However, manufacturing procedure dictates that the maximum dielectric gradients used in the design of current-transformer insulation of this type be of somewhat lower values than those used in cable design.

The "Spirakore" as Applied to Current Transformers

While the accuracy of a potential transformer is primarily a function of its impedance characteristics, the exciting current is the primary cause of current transformer errors. Any reduction in the exciting current will improve the accuracy of current transformers.

The magnetic properties of the Spirakore[3] when used in conjunction with the type of high-voltage construction just described, seem ideal for current-transformer applications. In this type of core, with the grains rolled in the same direction as the working flux, the hysteresis loss has been reduced to the very minimum, commensurable with the type of core material used. The elimination of the joints (which are present in the conventional core), has increased its accuracy up to the overload working range of the current transformers.

Referring to figure 4*a*, the annealed core is rewound over the ground shield to link the high-voltage coil, and then the turns of the low-voltage coil of the current transformer are evenly distributed over the circumference of the core. The latter feature is typical of the construction used in the manufacture of bushing current transformers.

It is recognized that the bushing-type current transformer has ideal characteristics, with the exception of the accuracy. With this new design the number of primary ampere-turns has not been limited to one, as in the usual bushing current transformer, and hence very high accuracy can be easily obtained. A method of compensation to increase the accuracy of the new transformers is discussed under the title of "Method of Compensation".

Current transformers for the highest voltages have now been designed, with the ideal characteristics of the bushing current transformer, using the insulation and construction previously described. Among the advantages of this combination it suffices to say that the secondary reactance of the current transformer is negligible, and as a result, the size and weight of the core are reduced to a minimum for a specified overload accuracy (see table I). Figure 4*b* shows a 230-kv current transformer built in accordance with the above principles of construction.

A slightly different form of construction is shown in figures 5*a* and 5*b*. The high-voltage winding in each case is wound directly over the core (which may have a cruciform cross section). In figure 5*a*

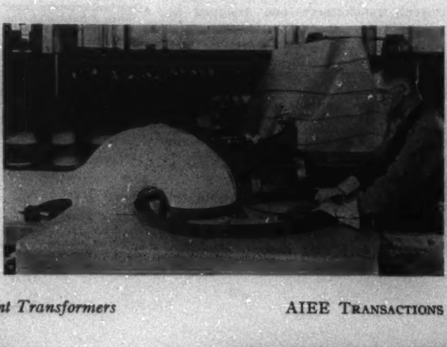

Figure 4a. Winding of the core over the insulated high-voltage coil

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[3] For all numbered references, see list at end of paper.

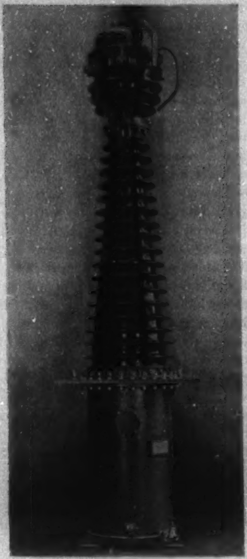


Figure 4b. View of a 230-kv current transformer using cable-type insulation and Spirakore wound over the ground shield of the high-voltage winding

suitable for low primary currents, the high-voltage winding is distributed directly over the core. In figure 5*b* suitable for very high current, the primary current is first stepped down by means of a small auxiliary current transformer which is maintained at high potential and therefore is placed on top of the current transformer. The low-voltage winding is distributed over the ground shield of the transformer. In this type of construction there is an internal secondary reactance which is in series with the burden and therefore, in order to obtain the same degree of accuracy as the scheme shown in figure 2, larger cores are necessary.

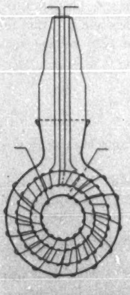


Figure 5a (left). Schematic diagram of a new design current transformer for low primary currents

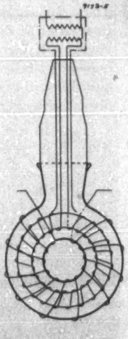
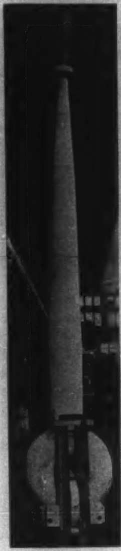


Figure 5b (right). Schematic diagram of a new design current transformer for very high primary currents



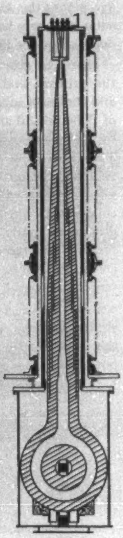


Figure 6 (left). View of the 287-kv current transformer proposed for the Boulder Dam line

Figure 7a (right). Cross section of a new-design current transformer proposed for the 287-kv Boulder Dam installation

This construction, although not as convenient as that of figure 2, will result in a small and compact transformer. A transformer proposed for the 287-kv Boulder Dam-Los Angeles transmission line,* is shown in figure 6 and in diagram figure 7*a*. Incorporated in the design are the same general principles of the new type insulation and Spirakore construction previously described. In addition, electrostatic shields have been applied to control the radial and longitudinal stresses in the lead, and tests have shown them to be very effective for both normal-frequency and impulse voltages. These shields are similar in principle to the shields of a capacitance bushing. The voltage distribution along the shields is shown in figure 7*b*. A sample of these transformers has been designed and tested to the following specifications:

One minute test....................650 kv
Dry flashover.......Not less than 780 kv
Impulse tests—Applications of two chopped waves, followed by one application of a full wave. Chopped wave should have a crest voltage of 1,595 kv and a maximum time to flashover of three microseconds. Full wave should have a crest voltage of 1,380 kv and no flashover of the transformer or gap should occur.

The transformers just described provide a satisfactory solution to the problem of high-voltage current-transformer design by putting the entire insulation which separates the high-voltage side from the grounded core, upon the primary winding. In a very high-voltage transformer the effective iron and copper are only a fraction of the total bulk of the materials incorporated in such designs. Any change in the construction which results in a substantial reduction in the amount of insulation required, will result in a correspondingly large reduction in the volume and weight of the transformer. For the very highest voltages some saving in the insulation may be obtained by us-

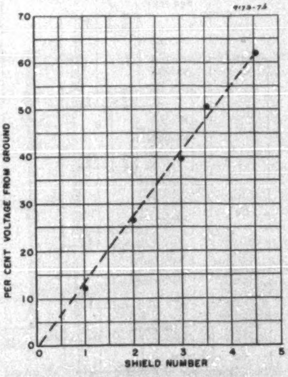


Figure 7b. Voltage distribution in the metal shields inserted in the high-voltage lead of the transformer shown in figure 7a

Dashed line—Ideal distribution
Circles—Distribution obtained by test

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* The transformers which are used in the impulse breakers for the 287-kv Boulder Dam line are of the cascade type and are shown in figures 11 and 14.

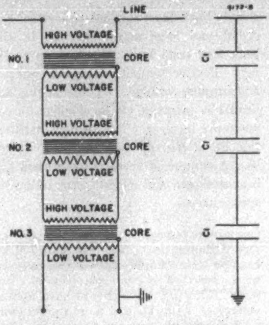


Figure 8. Elementary diagram of a three-voltage-step cascade current transformer

ing the "cascade principle".[4] The cable type of insulation has been extended to this type of transformer with good results, especially in reducing the size and increasing the accuracy of the apparatus. The principle of cascade-connected current transformers is similar to that employed in the so-called "chain connected" potential transformers.[5]

Figure 8*a* illustrates an elementary scheme comprising three subunits connected in cascade. The line-to-ground potential is divided among the three subunits in accordance with their series capacitances (figure 8*b*). If the three capacitances are alike, the major insulation of each subunit needs to be designed for only one-third of the line to ground potential, and each subunit may, therefore, be made correspondingly smaller than a conventional single-unit design.

A much more compact design than the foregoing can be obtained by the construction shown in figure 9*a*. Here also there are three subunits in cascade, but with this important difference—that the voltage across the major insulation of each subunit is only half of that of figure 8, or one-sixth of the line-to-ground potential.

It will be observed that in this design the primary and secondary windings *A* and *B* of each subunit are on opposite legs of the core and are coupled to each other by means of auxiliary windings *C* and *C*. The *A* winding of the top unit is connected to the line and is at line potential—whereas the *B* winding of the bottom unit connects to the burden with one side grounded, and is therefore at ground potential. The remaining windings and the cores are floating, and thus acquire potentials between line potential and ground.

From the practical standpoint, it is desirable that the subunits be designed for the same fraction of the line-to-ground potential. The voltage distribution among the subunits evidently depends upon the capacitance relationships. The capacitance circuit is shown in figure 9*b* and it is obvious that to enforce a linear distribution the capacitance *C* between each main winding and the core should be large compared to core-to-core, core-to-line, core-to-ground capacitances *K*, *C''* and *C'* respectively, so that *C* can dominate the distribution.

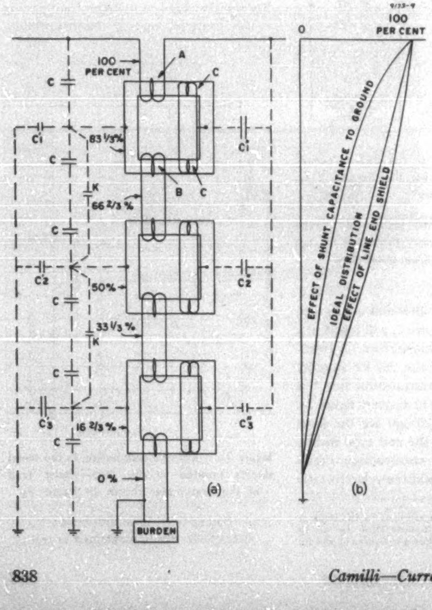


Figure 9. Schematic diagram of a cascade current transformer having six voltage-steps as shown in figure 11

The effect of shunt capacitances to ground is to cause a drooping distribution, just as on a suspension insulator string—while the effect of the capacitance to the line is to pull the distribution up (see figure 9*b*). It is theoretically possible to design a shield so that the line capacitance just neutralizes the shunt ground capacitance—thus allowing the series capacitance to determine the distribution, and indeed this is the principle used in the nonresonating transformer. But in the present application it is more feasible to obtain the desired linear distribution by making the coil-to-core capacitance very large compared with the other capacitances present. In some designs this end is secured by insulating the main windings from the cores with porcelain spools, "schooped" (sprayed with metal) on both sides so as to provide a large-surface high-permittivity capacitor. Consequently the distribution is substantially uniform and of course the distribution remains so under surge conditions.

The question naturally arises—in what way does such a construction result in a saving of insulation—and what is the criterion for the number of units? The strength of insulation is not a linear function of its thickness, but increases at a slower rate as thickness is increased. Thus, for oil the thickness of insulation varies as the 1.6 power of the test voltage. If therefore, the insulation required for a test voltage is $KE^{1.6}$, that required for the same test voltage subdivided into n steps, is

$$Kn\left(\frac{E}{n}\right)^{1.6}=\frac{1}{n^{0.6}}KE^{1.6}$$

and the ratio of the insulation thickness is $1/n^{0.6}$.

This ratio is plotted in figure 10. It is clear that a substantial reduction in total insulation thickness results from subdivision, but the curve is one of diminishing return and there appears to be little excuse for carrying subdivision any further than six steps, unless for other reasons—such as keeping the size of the units small enough to fit inside porcelain shells of given dimensions. Additional reasons for keeping the number of units to a minimum are that the accuracy may be

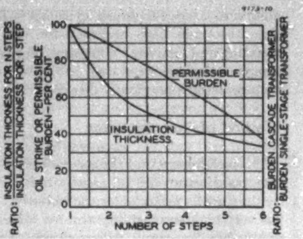


Figure 10. Ratio of insulation thickness and ratio of permissible burdens (to obtain a given accuracy) of a cascade current transformer and a conventional transformer for different numbers of voltage steps

seriously impaired by too many units (see figure 10) and that manufacturing objections increase.

Figure 11 shows an inside view of one of the current transformers which are integral parts of the Boulder Dam 287-kv impulse circuit breaker[6] shown in figure 12. These current transformers were built to withstand 650-kv 60-cycle test. The current transformers are in the outside supporting columns of the circuit breaker—two current transformers per phase being required. Each transformer has three units. Each unit has a core, two main windings, two coupling windings, and a number of compensating windings. The whole structure is supported and clamped by four Herkolite columns with angle-iron braces at the top and bottom. In this case the transformer is contained in an oil-tight Herkolite cylinder which also supports the weight of the circuit breaker.

The outside porcelain shell supports no weight and is equipped with a flexible bellows connection at the top to allow for expansion and contraction and mechanical adjustments in assembly. There is a Thyrite by-pass at the line terminals for surge protection.[7]

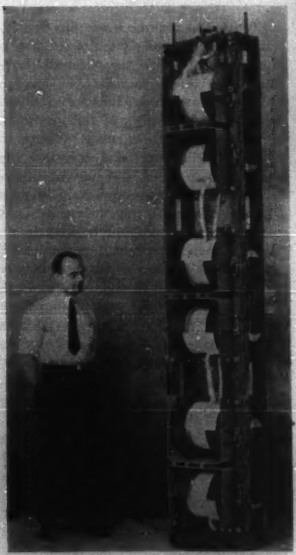


Figure 11. Cascade current transformer, 287-kv, installed in the impulse circuit breaker shown in figure 12

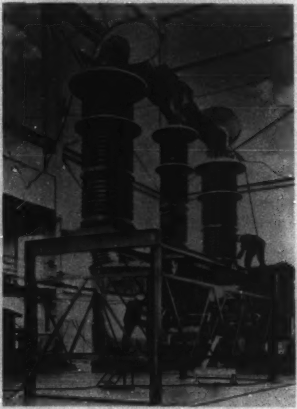


Figure 12. View of the 287-kv impulse circuit breaker

The two outside porcelains enclose cascade current transformers shown in figure 11

Figure 13 gives a comparative outline of the above current transformers with those of a conventional design and for the same voltage and rating; calling the conventional design 100 per cent, there is for the cascade type:

Height—40 per cent
Weight—18 per cent
Floor space—45 per cent
Oil—7 per cent

As mentioned previously the cable type of insulation has been applied to the cascade design also. The porcelain of the previous design has been replaced by oil-impregnated paper, and uniform large capacitances between stages have been obtained by enclosing the windings in metal shells. Uniform capacitance has thus been obtained regardless of dimensions of the windings.

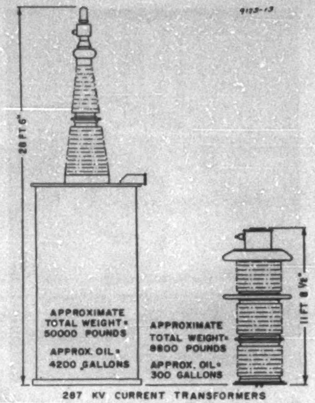
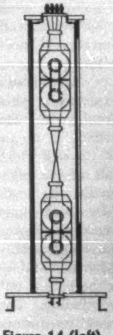

9173-13
28 FT 6"
APPROXIMATE TOTAL WEIGHT = 50000 POUNDS
APPROX. OIL = 4200 GALLONS
APPROXIMATE TOTAL WEIGHT = 8800 POUNDS
APPROX. OIL = 300 GALLONS
11 FT 8 1/2"
287 KV CURRENT TRANSFORMERS

Figure 13. Comparative outline of conventional design and the cascade transformer, 287 kv, used in impulse oil circuit breaker

Figure 14 shows a cross section of the new 287-kv cascade current transformers which will be an integral part of the impulse breaker for the new Boulder Dam line.

By the use of a new method of insulation it was possible to reduce the number of insulation stages from six (as in the pre-



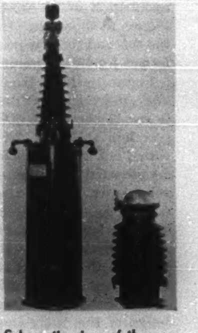


Figure 14 (left). Schematic view of the new 287-kv cascade current transformer using cable-type insulation

Figure 15 (right). 69-kv current transformers. Comparison between the old type and the new current transformer

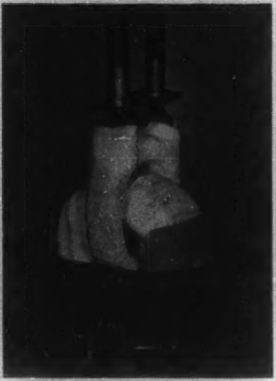


Figure 16. Shows core and coils, 15-kv current transformer assembled in the tank

vious porcelain design) to four. Likewise the size of the transformer has been reduced. Thus, while the preceding porcelain spool design was contained in a 24-inch-diameter Herkolite cylinder, the new transformers are enclosed in an 18-inch-diameter cylinder. No reduction is possible in the height of the transformers since the height is determined by the creepage and the flashover characteristics of the external porcelain shell (bushing).

Progress in Low-Voltage Current-Transformer Design

The new type of insulation also finds application in the lower-voltage classes. Figure 15 shows the comparison between the old- and the new-design current transformer for 69-kv circuit.

Figure 16 shows the core and coils of a new 15-kv current transformer, while figure 17 shows its external view. The porcelain shell has the shape of an inverted pot with two holes, through which are inserted the terminals of the high-voltage winding. Internally the high-voltage coil is insulated by crepe paper from the low voltage, evenly distributed on the Spirakore. The complete assembly is filled with a mixture of Pyranol and inorganic material through the hollow terminals of the high-voltage winding. The design of the 15-kv current transformer contains several features of special note. One of these is found in the construction of the core and coils as indicated in figure 15. In one method of construction the low-voltage winding is evenly distributed over the core and is partially insulated with crepe paper by a special winding machine. The high-voltage winding is pulled into a helix and screwed into a loop with the low-voltage winding. The turns making up the high-voltage winding are then closed together, and after connecting the terminals the coil is insulated with several layers of crepe paper.

In another method of construction the high-voltage winding is first insulated and the Spirakore is wound over it. The turns of the low-voltage winding are then uniformly distributed over the Spirakore. The well-recognized superiority of a circular coil for the high-voltage winding, to better withstand mechanical forces, has been maintained. The circular high-voltage coil passing through the center of the circular Spirakore with its distributed secondary winding, gives a transformer which approaches the ideal mechanical characteristics of the bushing-type current transformer. After being filled, these transformers are hermetically sealed, similar to the higher-voltage units. An adequate amount of free space is provided in the shell to accommodate the expansion of the liquid filler.

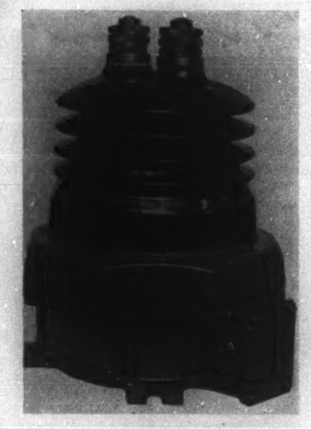


Figure 17. New design 15-kv current transformer

The low-voltage bushings of these new transformers shown in figure 18, should be mentioned. These are made of special glass which is fused in a metal shell which has exactly the same coefficient of thermal expansion as that of the glass under a very wide range of temperatures.

Another distinct advantage of the Spirakore construction is its unique adaptability to a special means of compensation to obtain high accuracy for both metering and relaying. The same type of compensation has been used in the high-voltage designs previously described.

Method of Compensation

It is well known that the errors of a current transformer can be greatly reduced by the use of a material for the core which has very high permeability and low losses at low flux densities.[8] Alloys of iron and nickel, especially those generally known under the name of "Permalloys" would be very desirable in the manufacture of current transformers if it were not for some disadvantages which will prevent their general use. Among these disadvantages, as compared with those of ordinary silicon steel, we may mention their high cost, their susceptibility to mechanical shocks, and the low saturation value.

Without resorting to special alloys other methods are available for the reduction of the exciting current and consequently of the errors of current transformers. Limiting our discussion to current transformers using cores made of silicon steel, it is well known that this material reaches the maximum permeability for values of flux density around 5,000 lines per square centimeter. Current transformers are normally used at a much lower flux density and consequently at a point of the saturation curve at which the permeability is much lower than maximum.

If the cross section of the core is reduced so as to reach at full load the maximum permeability, even for small variation in load, the reluctance, and therefore the exciting current of the current transformers, will increase very rapidly with the inevitable result that the errors of our current transformers will be very large. The ideal performance will be obtained if the permeability remains approximately at maximum value throughout the working range of the load current (normally from 10 per cent to 100 per cent). This can be obtained by adopting large cross sections for the core and "premagnetizing" it by some other current of the same frequency in such a manner that it has no influence on the current to be measured.

This auxiliary current must be such as to bring the magnetization of the core up

Figure 18. Bushing for secondary terminals of current transformers

to the region of maximum permeability. If the cross section of the core is relatively large, the magnetization of the iron due to the working flux is relatively small, and the variation of the flux density with variable load does not deviate too much from the value of maximum permeability.

To premagnetize the core it is possible to use[9] an auxiliary current furnished by an external source of the same frequency. To avoid the mutual effect of the two currents, the current of premagnetization and the current to be measured, the core is divided into two equal parts, which are magnetized by the auxiliary current in opposite direction.

Without going to a separate source for the auxiliary current, auxiliary magnetization may be furnished from a separate core (see figure 19), excited from the same current that is to be measured.[10] The number of auxiliary turns and the cross section of this auxiliary core are selected in such a manner that under average normal load conditions it is almost saturated, and therefore the auxiliary current which it furnishes to the main core of the current transformer changes very little during the working range of the current transformer.

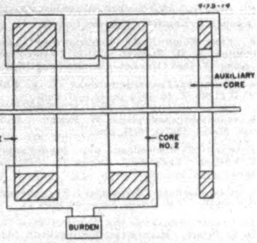


Figure 19. Schematic diagram of a current transformer separately excited by an auxiliary core

The above scheme represents a marked improvement in the normal (10 per cent to 100 per cent) working range of the current transformers. Under overload condition, however, the induction of the core reaches the point of saturation too soon and the errors of the current transformer increase too rapidly.

Instead of using an auxiliary core to furnish a current for the premagnetization of the working core of the current transformer, the auxiliary magnetizing current can be derived directly from the secondary current of the current transformer.[11] (See figure 20a).

The flux in each core is due to the result of the primary and secondary ampere turns. By the proper selection of the direction of winding and of the number of turns of the auxiliary windings in core 1, the ampere turns of the primary can be made to exceed those of the secondary, while in core 2 the secondary ampere turns can be made to exceed those of the primary. In other words, for core 1:

$$NI_1 = (N_s - n_2)I_2$$

and for core 2:

$$NI_1 = (N_s + n_3)I_2$$

The connections shown in figure 20*b* are equivalent to those of figure 20*a*, in which

$$N_s - n_2 = N_1$$

$$N_s + n_3 = N_2$$

Figure 21*a* shows the diagram of the ampere turns of a normal current transformer. Figure 21*b* shows the diagram of the ampere turns of core 1 of our premagnetized current transformer. In this core the primary ampere turns exceed those of the secondary, and the resultant ampere turns are:

$$I_1N + I_2N_1 = I_1W_1 \tag{1}$$

It should be noted that the resultant ampere turns in this core are approximately in phase with the primary ampere turns. Likewise figure 21*c* represents the diagram of the ampere turns of core 2. In this core the secondary ampere turns exceed those of the primary, and the resultant ampere turns I_2W_2 are approximately in phase with the secondary ampere turns according to the equation:

$$I_1N + I_2N_2 = I_2W_2 \tag{2}$$

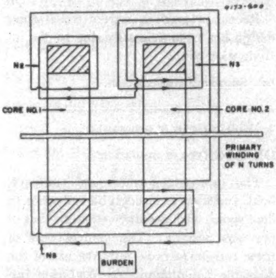


Figure 20a. Schematic diagram of a current transformer with auxiliary excitation derived from the secondary side of the current transformer

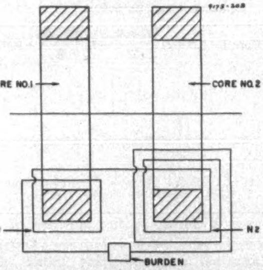


Figure 20b. Simplified diagram of the connections of the current transformer shown in figure 20a

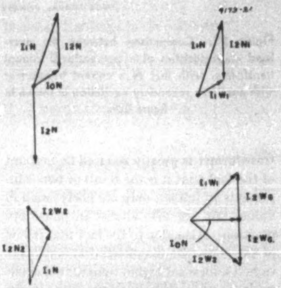


Figure 21. Diagram of the ampere turns of a current transformer with auxiliary excitation from the secondary current as shown in figure 20b

(*a*)—Vector diagram of the ampere turns in a normal current transformer

(*b*)—Vector diagram of the ampere turns in core number 1 of the transformer shown in figure 20*b*

(*c*)—Vector diagram of the ampere turns in core number 2 of the transformer shown in figure 20*b*

(*d*)—Resultant vector diagram of the transformer shown in figure 20*b*

For a given burden the induced voltage E_2 determines the value of the common flux ϕ in the two cores, and the no-load ampere turns of the current transformer.

The above no-load ampere turns are the result of the two ampere turns I_1W_1 and I_2W_2. Due to the fact that the two ampere turns I_1W_1 and I_2W_2 are almost in phase opposition, each one must be much larger than the no-load ampere turns of the current transformer. Their value is such that the flux density in the two cores in the region of maximum permeability is increased. (See appendix I.) It follows that the exciting current of the current

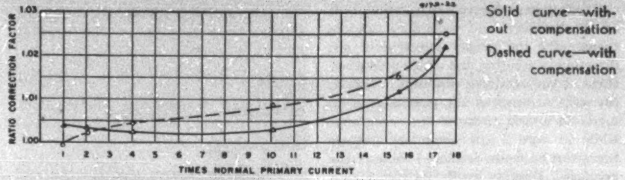


Figure 22. Comparison between the overload characteristics of a conventional current transformer with that of a current transformer with auxiliary secondary excitation as shown in figure 20b

transformer is greatly reduced on account of the fact that it is the result of two components which not only are fairly small in value, but are also almost in phase opposition. Also, due to the fact that in one of the cores the magnetization is proportioned somewhat higher than that of maximum permeability, and in that of the second core somewhat lower than maximum permeability, it follows that satisfactory operation of the transformer is obtained throughout the working range of the current transformer.

The above theoretical considerations have been limited to two cores. However, the Spirakore construction permits the same results to be obtained with a single core by placing some of the turns between the layers of the core spiral.

Performance of "Auxiliary Magnetized" Current Transformer on Overloads

Let us suppose that the primary current increases above the normal value—the flux in the first core increases also, and for a given value of overload, reaches the point of saturation. In the second core (that having an excess of secondary ampere turns), on account of the saturation of the first core, the secondary current increases at a lower rate than the increase in primary current (the flux being the result of the excess secondary ampere turns over the primary ampere turns), decreases, becomes a minimum and starts to increase only when NI_1 becomes larger than I_2N_2. The saturation of the second core is reached for very large values of the primary current. The overload characteristics therefore are quite satisfactory. Figure 22 gives a comparison of the overload characteristics of a 15-kv current transformer with and without compensation. It is noted that the compensation does not appreciably reduce the overload characteristics of the current transformer.

Summary

Recent progress in current-transformer design has been primarily due to the introduction of:

(*a*). Spirakore construction.

1. With lower magnetizing current and losses.
2. With its facility of compensation.

(*b*). Cable type of insulation.

The new transformers are compact, light, and show a considerable reduction in floor space, with accuracy equal to that of previous designs. The compactness of these transformers permits the use of the desirable noninflammable insulation liquid—Pyranol.

Appendix I

Equation 1 can be written:

$$\text{Core 1: } I_1N+I_2\frac{N_1+N_2}{2}-I_2\frac{N_1-N_2}{2}=I_1W_1$$

Similarly, equation 2 can be written:

$$I_1N+I_2\frac{N_1+N_2}{2}-I_2\frac{N_1-N_2}{2}=I_2W_2$$

Obviously the first two terms of the sum are the exciting ampere turns of the ordinary current transformer I_0N; the third term is the ampere turns of "auxiliary magnetization"

$$(I_2W_g),\ \frac{N_1-N_2}{2}$$

being the corresponding number of turns. Figure 21*d* shows the diagram of the ampere turns acting on the current transformer. As can be seen, the components of "premagnetization" cancel each other and take no part in the transformation of the current.

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Discussion

For discussion, see page 1099.

Ultrahigh-Frequency Loop Antennas

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ANTENNAS which are discussed in this paper were developed for two applications: (1) to serve as elements in radiating systems of localizers and radio ranges used to guide aircraft; (2) to act as receiving antennas carried by aircraft.

These antennas are in some respects similar to the low-frequency loop antennas and for this reason will be referred to as ultrahigh-frequency loops. The unique property which makes them useful in the above applications is that they radiate only horizontally polarized waves. The directional characteristics of these antennas are similar to those of a vertical dipole except that the waves emitted are horizontally instead of vertically polarized.

Calculation of the Field

Let us consider an antenna system composed of four straight-line radiators, 1, 2, 3, and 4, which are arranged in the form of a square as shown in figure 1.

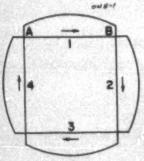

Figure 1. Current distribution in ultrahigh-frequency loop antenna of type I

We shall assume that the currents which flow in radiators 1, 2, 3, and 4 are all in phase and that their directions at any given instant are either as shown by the arrows in figure 1 or reverse. The amplitudes of the currents along each radiator will be assumed to vary sinusoidally with the crest of the current at the center of each radiator.

The electromagnetic field produced by this antenna system at a distant point P may be obtained by adding the fields produced by the individual radiators.

Since the electric and magnetic fields at a distant point are always simply related, so that one may be obtained from the other, it is necessary to calculate only one of them, for example, the magnetic field.

Magnetic Field of One Radiator

The magnetic field due to a linear radiator with a chopped-off sine-wave distribution of figure 1 may be readily calculated by the method described, by one of the authors.[1] In fact, one may assume that the chopped-off sine-wave distribution is the result of two traveling waves proceeding in opposite directions and so timed that the currents are in phase at the center of the element. If the current in one of the two traveling waves is

$$i = i_0 \sin(\omega t - kz) \tag{1}$$

then the current in the other is

$$i' = i_0 \sin(\omega t + kz - lk) \tag{2}$$

The magnetic field due to the first traveling wave is

$$H = \frac{2i_0}{r_1 c}\cot\frac{\theta_1}{2}\sin\left[\frac{\pi l}{\lambda}(1-\cos\theta_1)\right]\times \cos\left[\omega\left(t-\frac{r_A}{c}\right)-\frac{\pi l}{\lambda}(1-\cos\theta_1)\right] \tag{3}$$

while the magnetic field due to the second traveling wave is

$$H' = \frac{2i_0}{rc}\tan\frac{\theta_1}{2}\sin\left[\frac{\pi l}{\lambda}(1+\cos\theta_1)\right]\times \cos\left[\omega\left(t-\frac{r_A}{c}\right)-\frac{\pi l}{\lambda}(1-\cos\theta_1)\right] \tag{4}$$

The total magnetic field is

$$\begin{aligned} H_1 &= H+H' \\ &= \frac{2i_0}{r_1 c}\left\{\tan\frac{\theta_1}{2}\sin\left[\frac{\pi l}{\lambda}(1+\cos\theta_1)\right]+ \cot\frac{\theta_1}{2}\sin\left[\frac{\pi l}{\lambda}(1-\cos\theta_1)\right]\right\}\times \\ &\quad \cos\left[\omega\left(t-\frac{r_A}{c}\right)-\frac{\pi l}{\lambda}(1-\cos\theta_1)\right] \end{aligned} \tag{5}$$

In this equation r_A is the distance between one end A of the radiator and the distant point P; θ_1 is the angle between AP and radiator 1; l is the length of the radiator; c is the velocity of light; λ is the wave-length; $k = 2\pi/\lambda$ and $2i_0$ is the loop current, that is, the current at the center of the radiator.

If in place of r_A we use distance r_1 measured from the center of radiator 1 to point P, then since

$$r_1 = r_A - \frac{\ }{2}\cos\theta_1$$

equation 5 reduces to the following:

$$H_1 = \frac{2i_0}{r_1 c}\left\{\tan\frac{\theta_1}{2}\cdot\sin\left[\frac{\pi l}{\lambda}(1+\cos\theta_1)\right]+ \cot\frac{\theta_1}{2}\cdot\sin\left[\frac{\pi l}{\lambda}(1-\cos\theta_1)\right]\right\}\times \cos\left(\omega t - kr_1 - \frac{\pi l}{\lambda}\right) \tag{6}$$

which shows that the phase of H_1 is the same at all points equidistant from the center of the radiator.

When the length l of the radiator is small in comparison with λ equation 5 reduces to a much simpler form, namely,

$$H_1 = \frac{2i_0}{r_1 c}\left(\frac{2\pi l}{\lambda}\sin\theta_1\right)\cos\left(\omega t - r_1 k - \frac{1}{2}kl\right) \tag{7}$$

While this elementary equation is exact only when l/λ is very small it remains a fair approximation up to $l/\lambda = 1/8$. This may be seen from table I, in which values of H obtained from equations 6 and 7 are given side by side for $l/\lambda = 1/8$ and $l/\lambda = 1/4$.

Magnetic Field of the Loop

The magnetic field produced by the four radiators of the loop antenna is simply a vector sum of the individual fields. That is

$$\boldsymbol{H} = H_1 + H_2 + H_3 + H_4 \tag{8}$$

where H_1, H_2, H_3, H_4 are the magnetic fields as given accurately by equation 6 or approximately by equation 7.

The magnetic vectors H_1 and H_3 due to radiators 1 and 3 are collinear and may be added directly. Similarly vectors H_2 and H_4 may be added directly for the same reason. The resultant (H_1+H_3), however, in general makes an angle with the resultant (H_2+H_4) and this angle depends on the position of point P with respect to the loop, so that it is necessary either to add the two vectorially or to resolve them into mutually perpendicular components and add the latter. This second procedure is preferable and will be followed.

In any case, the first step is to add H_1 and H_3. In accordance with (7)

$$H_1 = \frac{2i_0}{rc}\left(\frac{2\pi l}{\lambda}\sin\theta_1\right)\cos\left(\omega t - r_1 k - \frac{lk}{2}\right) \tag{9}$$

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[1] A. Alford, *Electrical Communications*, July 1936; see page 78.

Table I

θ (degrees)		0	10	20	30	40	50	60	70	80	90
$l/\lambda=1/8$	H from equation 6	0.000	0.130	0.252	0.372	0.482	0.577	0.656	0.716	0.750	0.766
	H from equation 5	0.000	0.137	0.270	0.393	0.506	0.602	0.681	0.739	0.774	0.786
$l/\lambda=1/4$	H from equation 5	0.000	0.273	0.536	0.785	1.01	1.20	1.36	1.47	1.54	1.57
	H from equation 6	0.000	0.221	0.436	0.654	0.857	1.039	1.194	1.313	1.386	1.414

$$H_3=\frac{2i_0}{rc}\left(\frac{2\pi l}{\lambda}\sin\theta_3\right)\cos\left(\omega t-r_3k-\frac{lk}{2}\right) \quad (10)$$

From figure 2 it follows, however, that $\theta_1=\theta_3$. r_3 is not equal to r_1, but both may be readily expressed in terms of r_0, the distance from the center of the loop to point P, and θ_3 the angle between element 2 and OP. From figure 2

$r_1=r_0-l/2\cos\theta_3$

$r_3=r_0+l/2\cos\theta_3$

by substituting these values of r_1 and r_3 into (9) and (10), then adding H_1 and H_3 and finally simplifying the result of addition with the aid of the well-known formula

$$\cos A-\cos B=-2\sin\frac{A-B}{2}\sin\frac{A+B}{2}$$

we obtain

$$(H_1+H_3)=\frac{4i_0}{cr}\left(\frac{2\pi l}{\lambda}\sin\theta_1\right)\sin\times$$
$$\left(\frac{\pi l}{\lambda}\cos\theta_3\right)\sin\left(\omega t-kr_0-\frac{lk}{2}\right) \quad (11)$$

Similarly, since

$$(H_2+H_4)=\frac{4i_0}{cr}\left(\frac{2\pi l}{\lambda}\sin\theta_2\right)\times$$
$$\sin\left(\frac{\pi l}{\lambda}\cos\theta_1\right)\sin\left(\omega t-kr_0-\frac{lk}{2}\right) \quad (12)$$

If, in place of approximate equation (7), we had used the exact equation (6), the results would have been the same as (11) and (12), except that in place of $\left(\frac{2\pi l}{\lambda}\sin\theta_1\right)$, we would have

$$\left\{\tan\frac{\theta_1}{2}\sin\left[\frac{\pi l}{\lambda}(1+\cos\theta_1)\right]+\right.$$
$$\left.\cot\frac{\theta_1}{2}\sin\left[\frac{\pi l}{\lambda}(1-\cos\theta_1)\right]\right\}$$

and a similar expression in place of $\left(\frac{2\pi l}{\lambda}\sin\theta_2\right)$.

The resultant vector (H_1+H_3) is at right angles to the plane defined by OP and OB. This vector may be resolved into two mutually perpendicular components, namely H_g directed along the great circle APC and H_s at right angles to this great circle, that is, along the small circle PSQ through P. Both cosine and sine of angle y_1, which vector (H_1+H_3) makes with H_G may be readily found from figure 2. In fact, since (H_1+H_3) is perpendicular to plane OPB it follows that $\angle CPB=x_1=90°-y_1$. From spherical triangle CPB by the rule of sines

$$\frac{\sin x_1}{\sin\alpha}=\frac{\sin 90°}{\sin\theta_1}$$

Hence

$$\sin(90°-y_1)=\cos y_1=\frac{\sin\alpha}{\sin\theta_1}$$

From the same triangle it follows that $\cos\theta_1=\cos\beta\cdot\cos\alpha$. With the aid of this relation $\sin y_1$ may be expressed as follows:

$$\sin y_1=\sqrt{1-\cos^2 y}=\frac{\sqrt{\sin^2\theta_1-\sin^2\alpha}}{\sin\theta_1}$$
$$=\frac{\sqrt{1-\cos^2\beta\sin^2\alpha-(1-\cos^2\alpha)}}{\sin\theta_1}$$
$$=\frac{\cos\alpha\sin\beta}{\sin\theta_1} \quad (13)$$

Therefore,

$$H_{G_{1,3}}=(H_1+H_3)\cos y_1=(H_1+H_3)\frac{\sin\alpha}{\sin\theta_1} \quad (14)$$

and

$$H_{S_{1,3}}=(H_1+H_3)\sin y_1$$
$$=(H_1+H_3)\frac{\cos\alpha\cdot\sin\beta}{\sin\theta_1} \quad (15)$$

Vector (H_2+H_4) is perpendicular to the plane OPD which is defined by OP and the line OD drawn from O, parallel to elements 2 and 4. Angle y_2 which vector (H_2+H_4) makes with meridian APC is equal to $90°-x_2$ where $x_2=\angle CPD$. From right spherical triangle CPD it follows that

$$\frac{\sin x_2}{\sin(90°-\alpha)}=\frac{\sin 90°}{\sin\theta_2}$$

and

$$\cos\theta_2=\cos(90°-\alpha)\cos\beta$$

Hence

$$\cos y_2=\sin x_2=\frac{\cos\alpha}{\sin\theta_2};$$

$$\sin y_2=\frac{\sqrt{\sin^2\theta_2-\cos^2\alpha}}{\sin\theta_2}=\frac{\sin\alpha\cdot\sin\beta}{\sin\theta_2} \quad (16)$$

Thus, the great circle component of (H_2+H_3)

$$H_{G_{2,4}}=(H_2+H_4)\frac{\cos\alpha}{\sin\theta_2} \quad (17)$$

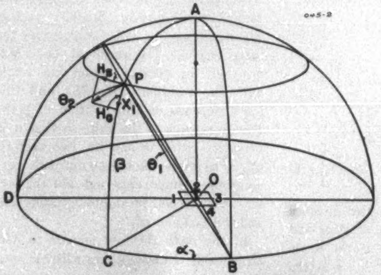
and the small circle component is

$$H_{S_{2,4}}=-(H_2+H_4)\frac{\sin\alpha\cdot\sin\beta}{\sin\theta_2} \quad (18)$$

The negative sign used in the last equation is used to indicate that the small circle component of (H_2+H_4) points to the right when the small circle component of (H_1+H_3) points to the left. Thus, if one of them we call positive, the other has to be negative.

The total great circle component due to the antenna system of figure 1 is thus:

$$H_G=(H_1+H_3)\frac{\sin\alpha}{\sin\theta_1}+(H_2+H_4)\frac{\cos\alpha}{\sin\theta_2} \quad (19)$$



α = CB
β = CP
θ₁ = BP
X₁ = ∠CPB

Figure 2. Schematic diagram

Table II

Values of $H_G\frac{rc}{2i_0}$ calculated from (19) using accurate equation 6 for H_1, H_2, H_3, H_4

$\alpha=0$ degrees

l/λ	$\beta=0°$	$\beta=20°$	$\beta=40°$	$\beta=60°$	$\beta=80°$	$\beta=90°$
0.05	0.097	0.092	0.075	0.049	0.017	0.000
0.10	0.380	0.358	0.294	0.192	0.067	0.000
0.15	0.822	0.788	0.641	0.422	0.148	0.000
0.20	1.39	1.33	1.09	0.727	0.258	0.000
0.25	2.00	1.90	1.64	1.09	0.384	0.000

$\alpha=45$ degrees

l/λ	$\beta=0°$	$\beta=20°$	$\beta=40°$	$\beta=60°$	$\beta=80°$	$\beta=90°$
0.05	0.098	0.092	0.074	0.051	0.017	0.000
0.10	0.380	0.350	0.290	0.200	0.067	0.000
0.15	0.826	0.780	0.666	0.432	0.148	0.000
0.20	1.38	1.30	1.08	0.735	0.262	0.000
0.25	2.01	1.90	1.61	1.09	0.387	0.000

Values of $H_G\frac{rc}{2i_0}$ from approximate equation 23 for any value of α

l/λ	$\beta=0°$	$\beta=20°$	$\beta=40°$	$\beta=60°$	$\beta=80°$	$\beta=90°$
0.05	0.099	0.093	0.076	0.049	0.017	0.000
0.10	0.402	0.378	0.309	0.201	0.070	0.000
0.15	0.888	0.835	0.680	0.444	0.155	0.000
0.20	1.58	1.49	1.21	0.790	0.275	0.000
0.25	2.46	2.31	1.87	1.23	0.428	0.000

Values of $H_G\frac{rc}{2i_0}$ from semiempirical equation

$$H_G=\frac{8i_0}{rc}\left(\sin\frac{\pi l}{\lambda}\right)^2\cos\beta \qquad (23a)$$

which represents accurate values much better than (23)

For any value of α

l/λ	$\beta=0°$	$\beta=20°$	$\beta=40°$	$\beta=60°$	$\beta=80°$	$\beta=90°$
0.05	0.097	0.091	0.074	0.048	0.017	0.000
0.10	0.382	0.359	0.292	0.191	0.066	0.000
0.15	0.824	0.775	0.630	0.412	0.143	0.000
0.20	1.38	1.30	1.06	0.690	0.240	0.000
0.25	2.00	1.88	1.53	1.00	0.348	0.000

Values of $H_G\frac{rc}{2i_0}$ from accurate equations 19 and 6 for $l/\lambda=0.25$, $\beta=0$, and variable α

α	0	10	20	30	40	50	60	70	80	90
$H_G\frac{rc}{2i_0}$	2.00	2.00	2.00	2.00	2.00	2.00	2.00	2.00	2.00	2.00

while the small circle component is

$$H_S=(H_1+H_3)\frac{\cos\alpha\sin\beta}{\sin\theta_1}-(H_2+H_4)\frac{\sin\alpha\sin\beta}{\sin\theta_2} \qquad (20)$$

Both of these equations are accurate if the values of H_1, H_2, H_3, and H_4 are calculated from equation 6 and others like it for H_2, H_3, and H_4. When the values of H_1, H_2, H_3, and H_4 are taken from (7) and similar equations for H_2, H_3, H_4, then equations 19 and 20 will be only approximate. Such approximate equations, of course, are very much simpler. By using the approximate values for H_1, H_2, H_3, and H_4 we obtain

$$H_G=\frac{8i_0}{rc}\frac{\pi l}{\lambda}\left[\sin\alpha\cdot\sin\left(\frac{\pi l}{\lambda}\cos\theta_2\right)+\cos\alpha\cdot\sin\left(\frac{\pi l}{\lambda}\cos\theta_2\right)\right] \qquad (21)$$

$$H_S=\frac{8i_0}{rc}\frac{\pi l}{\lambda}\left[\cos\alpha\cdot\sin\beta\cdot\sin\left(\frac{\pi l}{\lambda}\cos\theta_2\right)-\sin\alpha\sin\beta\sin\left(\frac{\pi l}{\lambda}\cos\theta_1\right)\right] \qquad (22)$$

If we again assume that l/λ is small so that

$$\sin\left(\frac{\pi l}{\lambda}\cos\theta_2\right)=\frac{\pi l}{\lambda}\cos\theta_2,$$

$$\sin\left(\frac{\pi l}{\lambda}\cos\theta_1\right)=\frac{\pi l}{\lambda}\cos\theta_1$$

then (21) and (22) reduce to a very simple result which is easy to visualize, namely

$$H_G=\frac{8i_0}{rc}\left(\frac{\pi l}{\lambda}\right)^2\cos\beta \qquad (23)$$

$$H_S=0 \qquad (24)$$

While these equations are exact only when l/λ is very small, they remain reasonably accurate for most purposes even when $l/\lambda={}^1/_8$ and the horizontal characteristic is still substantially circular when $l={}^1/_4\lambda$ as may be seen from table II. A comparison of calculated "vertical characteristic" with measurements is given in figures 2*a* and 2*b*. In these figures the solid curves are calculated from equation 19 using accurate equation 6 for H_1, H_2, H_3, H_4. The measured values are obtained by putting the plane of the loop antenna at right angles to ground and then measuring the resulting horizontal characteristic.

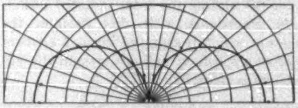


Figure 2a. Vertical characteristic of ultra-high-frequency loop antenna in plane $\alpha=0$ degrees (at right angles to elements 18.3)

Solid curve calculated; points from measurements

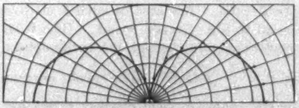


Figure 2b. Vertical characteristic of ultra-high-frequency loop antenna in plane $\alpha=45$ degrees (through diagonal) solid curve calculated; points from measurements

At a distant point from any antenna, the small circle component E_S of the electric field is equal to the great circle component H_G of the magnetic field and the great circle component E_G of the electric field is equal to the small circle component H_S of the magnetic field. Accordingly, in view of (23) and (24)

$$E_G=0 \tag{25}$$

$$E_S=\frac{8i_0}{rc}\left(\frac{\pi l}{\lambda}\right)^2 \cos \beta \tag{26}$$

This result may be stated in more familiar language. Let us suppose that the loop of figure 1 is in a horizontal plane. Then E_S is the horizontal component of the electric field. The great circle component E_G may be resolved into two mutually perpendicular components, one of which is vertical while the other is "longitudinal." Both of these are equal to zero when $E_G=0$, so that the total electric field of the loop antenna may be said to be purely polarized. There is no vertical component present in any direction.

Calculation of Radiation Resistance

Since the efficiency of the ultrahigh-frequency loop antenna depends primarily on the ratio of radiation resistance to the loss resistance of the conductors, it is important to know the value of the radiation resistance of the loop antenna at least approximately. Equation 23 provides us with a very simple and yet reasonably accurate value of the magnetic field at any point in space and permits us to apply the Poynting theorem for calculating the total radiated power. In accordance with this theorem the power which is radiated through element dS of a sphere of radius r around the antenna is given by

$$\frac{c}{8\pi}H^2dS$$

Since field H depends only on β and not on α, we may subdivide the sphere into narrow strips of which width $rd\beta$ included between adjacent small circles, such as for example are defined by $\beta=\beta_1$, and $\beta=\beta_1+d\beta_1$. The area of such a strip is equal to $rd\beta \cdot 2\pi r \cos \beta=dS$. The total power radiated through the entire sphere is $P=\frac{c}{8\pi}\int H^2dS$. The peak current at the center of such element is $2i_0$ and the effective current is $2i_0/\sqrt{2}$. If we define as the radiation resistance of the loop antenna that quantity R which when multiplied by the square of the effective current gives us the total radiated power then

Table III

l/λ	0.05	0.10	0.15	0.20
R	0.194	3.11	15.8	49.8

$$R\left(\frac{2i_0}{\sqrt{2}}\right)^2=P=\frac{c}{8\pi}\int_{-\pi/2}^{+\pi/2}H^2 2\pi r^2 \cos \beta d\beta$$

$$=\frac{16i_0^2}{c}\left(\frac{\pi l}{\lambda}\right)^4\int_{-\pi/2}^{+\pi/2}\cos^3 \beta d\beta$$

$$=\frac{64}{3c}i_0^2\left(\frac{\pi l}{\lambda}\right)^4$$

and hence

$$R=320\left(\frac{\pi l}{\lambda}\right)^4 \text{ ohms} \tag{27}$$

Table III shows how R varies with l/λ.

Equation 27 gives values of R which are too large, and this error increases with l/λ. The difficulty, lies in the fact that equation 23 used in deriving (27) gives too large a value of H for the greater values of l/λ as may be seen from table II. When more accurate values of radiation resistance are desired it is necessary to calculate R by numerical integration using (6) in place of (7) for H_1. A fair approximation is obtained from the following semiempirical equation

$$R=320\left(\sin \frac{\pi l}{\lambda}\right)^4 \tag{27a}$$

Equation 27 is a special case of a more general result which holds for loops of rectangular shapes and probably for loops of any shape, namely

$$R=320\left(\frac{\sqrt{A}}{\lambda}\right)^4 \tag{28}$$[*]

where A is the area of the loop. This more general equation like (27) is also subject to the condition that l/λ is small.

Before closing the subject of radiation resistance of loop antennas, it may be of interest to note that if several loop antennas (for example N) were superposed and connected in some manner so that they were in series and yet the currents were in phase, then each antenna would produce a field H and total field produced by all working together would be NH and the power radiated would be proportional to N^2H^2. It follows that the radiation resistance of a loop antenna made of N turns of wire is N^2 times that of a single-turn loop, that is

$$R_N=320N^2\left(\frac{\pi\sqrt{A}}{\lambda}\right)^4 \text{ ohms}$$

Design of Ultrahigh-Frequency Loop Antennas

In practice the current distribution illustrated in figure 1 may be obtained by the use of the circuit illustrated in figure 3. In this arrangement, portions BB', DD', AC of conductors 1, 2, 3, and 4 are very close to each other, so that radiations from them cancel, and the effective current distribution, so far as radiation is concerned, is as in figure 1.

The impedance looking into terminals (K, K') of network of figure 3 is usually largely reactive and is thus not well suited for being fed directly by a shielded transmission line. In order to secure a more convenient input impedance an ultrahigh-frequency loop antenna is usually provided with what may be called a *resonant transmission line*. This feature is illustrated in figure 3 $K, K'-T$.

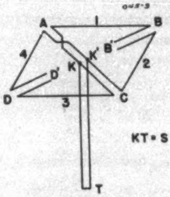


Figure 3. Ultrahigh-frequency loop antenna of type I

The optimum design of the resonant transmission line, as well as the design of the radiators themselves depends on factors which are seldom considered. For this reason, some of the details will be described.

First, we shall discuss the design of the resonant line. For a given field at a distant point it is necessary to maintain a certain value of current in the radiators. In turn, this value of current in the radiators corresponds to a certain fixed value of voltage across the terminals (K, K') of the resonant line. The power lost in this resonant line depends primarily on the current which flows in it and on the conductors which have to carry this current. It may be readily shown that the "loop current" I which flows through short-circuited end T of a transmission line section of lengths (see figure 3) when the voltage is V at its terminals is given by the following relation:

$$I=\frac{V}{Z_0 \sin \frac{2\pi s}{\lambda}}$$

---

[*] Equation for H for a rectangular loop may be derived by the same method used to obtain H for the square loop. Incidentally the field of a small rectangular loop is circular.

The power which is lost in the section of line is therefore

$$P = R \cdot \frac{V^2}{\left(Z_0 \sin \frac{2\pi s}{\lambda}\right)^2}$$

From this equation it may be readily seen that in order that the losses may be kept at a minimum, it is desirable

1. To keep V which corresponds to a given current in the radiators as low as possible.
2. To make R as low as possible and
3. To keep Z_0 as high as possible.

Voltage V for a given current in the radiators can be kept down by using conductors of low surge impedance, that is, by making the radiators of wide strips of metal. The second and third requirements have to be met by making R/Z_0^2 a minimum, because the two quantities R and Z_0 are mutually interrelated. It may be pointed out that the requirement that R/Z_0^2 be a minimum is different from the usual requirement that "attenuation" in a matched line be a minimum because the latter requirement demands that R/Z_0 and not R/Z_0^2 be a minimum. Thus, for example, in case of a coaxial line the requirement that R/Z_0 be minimum leads to the well-known ratio of a diameter of the outer to the inner conductors be around 3.8 while the requirement that R/Z_0^2 be minimum results in an optimum ratio around 9.0.

While the proportions of the conductor used in the "resonant line" are not critical, the controlling relations must be kept in mind if an efficient design is to be achieved, particularly when the dimensions of the loop antenna and hence its radiation resistance are relatively small.

Measurement of Radiation Resistance

The radiation resistance of a loop antenna may be measured by several different methods.

Method 1. The loop antenna is placed in the field of another radiator (for example, a half wave) which is a number of wave lengths away. One of the radiators of the loop antenna is cut in two as to its center, and the circuit is completed by means of a half wave length of transmission line as illustrated in figure 4. The complete circuit is resonated by moving short-circuiting bar T of the "resonant line" of the loop. The current which is induced in the loop antenna is measured by a very sensitive receiver M so designed that it does not couple in any appreciable resistance into the loop circuit. This freedom from coupled-in resistance may be established by using another control receiver or very sensitive meter M' while varying the coupling between receiver M and the loop.

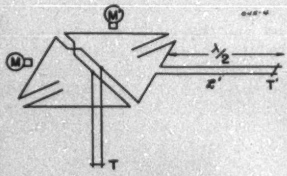


Figure 4. Method 1

Receiver M must be calibrated against a linear current meter so that the reading may be translated into relative values of current.

The measurements consist of introducing known amounts of reactance in series with one of the radiators of the loop by moving short-circuiting bar T' of half-wave line L and measuring the corresponding decrease in current in the radiators by means of receiver M. The result of such measurements is a resonance curve from which the total resistance, that is, the radiation resistance plus loss resistance can be calculated.

In order to separate the radiator resistance from the loss resistance the radiators of the loop are replaced by others of similar construction, but which are bent in such a way that the area of the loop is decreased to negligible size so that the radiation resistance practically vanishes. Then another resonance curve is obtained from which the loss resistance can be calculated.

Method 2. One of the radiators of the loop antenna is cut as in method 1, but the gap bridged by a short piece of wire W which is used as a shunt for measuring current. The loop antenna placed in the field of another radiator as in method 1 is resonated by means of short-circuiting bar T of the resonant line, and current I_W in wire W is compared with current I_T in short-circuiting bar T. Then short-circuiting bar T is translated by known amounts and a resonance curve is obtained. The resistance R_T which is calculated from this curve is not the sum R_W of radiation resistance plus loss resistance but is a resistance seen at short-circuiting bar T if the latter were out. The two resistances, however, are quite simply related because the dissipated power must be equal to

$I_T^2 R_T$ as well as to $I_W^2 R_W$

so that

$$I_T^2 R_T = I_W^2 R_W$$

and

$$R_W = R_T \frac{I_T^2}{I_W^2}$$

Since the ratio of these two currents is measured, the value of R_W can be calculated from R_T.

Method 3. The loop antenna is supplied with power in the normal way by a transmission line L which is very nearly matched so that the ratio of maximum to minimum of current in the standing waves is very nearly unity. The power which is supplied to the antenna can then be accurately measured by measuring the maximum and minimum currents or voltages along the transmission line. This delivered power is

$$P = Z_0 i_{\max} \cdot i_{\min}$$

where Z_0 is surge impedance of the transmission line. Current I_n which this known power produces in the radiator of the loop antenna is also measured. Then the total resistance R_W of the antenna (that is, radiation resistance + loss resistance) is calculated from the following equation.

$$RI_n^2 = Z_0\, i_{\max} \cdot i_{\min}$$

In order to separate the radiation resistance from the loss resistance, the radiators of the loop are then bent so as to reduce the radiation resistance to a negligible value and the process is repeated, this time yielding only the loss resistance.

All three of these methods plus separate measurements of resistance of the resonant line were used to check the radia-

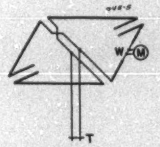


Figure 5. Method 2

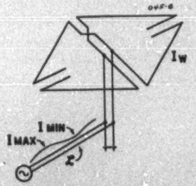


Figure 6. Method 3

tion calculated from equation 27. The results of some of these measurements are summarized in table IV.

Table IV

Method 1	23 ohms
Method 2	20 ohms
Theory, equation 27	44 ohms
Theory, equation 27*a*	35 ohms

Frequency used 109.9 megacycles.

Loop Antennas of Types II and III

It is sometimes desirable to make a loop antenna as compact and as simple as possible. Under such circumstances the design illustrated in figure 7 may be adopted. The loop antenna of figure 7 has a current distribution which results in somewhat greater radiation in direction *AA'* than in direction *BB'*. This deviation from a circular pattern, however, is rather small when the antenna dimensions are small in comparison with (12) as is usually the case. Antennas of this design will be referred to as ultrahigh-frequency loop antennas of type II.

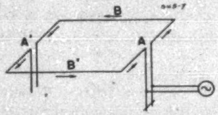


Figure 7. Ultrahigh-frequency loop antenna of type II

The loop antenna of figure 8 is probably the simplest one of all loop antennas. The main difficulty with this type of loop is that when this antenna is made large enough to have sufficient radiation resistance to be efficient, the current distribution becomes quite asymmetric, with most of the current in radiator 2 and little current in radiator 4. This current distribution results in maximum radiation in direction *CC* and some vertically polarized radiation in directions other than *CC'*. Such antennas will be referred to as ultrahigh-frequency loop antennas of type III.

Gain of Loop Antennas and Radiation Efficiency

Since the field pattern of a small loop antenna is identical except for polarization with that of a small dipole, it is manifest that for the same amount of input power, the fields at a distant point should be equal. The difference, if any, will be due to losses in associated feeding arrangements.

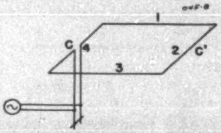


Figure 8. Ultrahigh-frequency loop antenna of type III

The loop antennas of type I with radiation resistance around 25 ohms, if carefully designed, have radiation efficiencies somewhat greater than 95 per cent.

A direct comparison of the fields produced by a loop of type I and a horizontal half wave when the two were fed with exactly the same power showed that the fields were very nearly equal with a slight preference (about one-fourth decibel) for the field due to the loop.

With the smaller loop antenna having radiation resistances of the order of five ohms, the radiation efficiency is naturally below 95 per cent because the loss resistance of the associated resonant circuit is usually of the order of one or two ohms.

In general, the radiation efficiency is equal to

$$\eta = \frac{\text{radiation resistance}}{\text{radiation resistance} + \text{loss resistance}}$$

When the radiation resistance is greater than loss resistance, the efficiency is high, but when it is less than the loss resistance, the efficiency is low. When the radiation resistance is low, the radiation efficiency is proportional to the radiation resistance.

Applications of Loop Antennas of the Three Types

Ultrahigh-frequency antennas of type I illustrated in figures 3 and 9, because of their higher efficiency, are particularly well suited for use as transmitting antennas. Thus, for example, an array of these antennas may be used as the radiating system of a localizer for guiding aircraft to an airport.

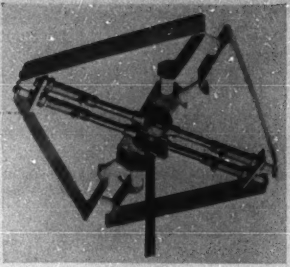


Figure 9. Ultrahigh-frequency loop antenna of type I

Figure 10. Ultrahigh-frequency loop antenna of type II installed on a transport plane

Antennas of type II are particularly well adapted for use as receiving antennas carried by aircraft.

These antennas consist of fewer parts and are therefore lighter than antennas of type I. When installed on aircraft about two feet above the fuselage, their direction characteristics in the horizontal plane are nearly circular which is exactly what is desired in such applications.

Figure 10 illustrates an experimental antenna of this type installed on a Douglas transport plane.

Antennas of type III are best suited for use in portable instruments such as, for example, field-exploring devices.

Ultrahigh-frequency loop antennas can be built for frequencies of the order of ten megacycles. At these frequencies the loop antennas make excellent "omnidirectional" horizontally polarized standard antennas for measuring gain of horizontally polarized directional arrays.

The Development of the Civil Aeronautics Authority Instrument Landing System at Indianapolis

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Historical and General Observations

THE most important problem in flying today is that of landing a plane under adverse weather conditions. A modern transport plane can take off and fly to the vicinity of its destination through nearly any type of weather by means of modern methods of air navigation. The use of the radio range together with flight and navigation instruments has made this possible. However, it is extremely hazardous to land aircraft with standard equipment under conditions of low ceiling and poor visibility. This limitation is the reason for the majority of flight cancellations of scheduled air transport.

Under poor weather conditions when landing is permitted by present regulations the difficulties in landing are still acute. It usually takes a plane as much as 15 or 20 minutes to make a landing at a busy airport after being cleared to land by the airport traffic control tower. Concurrently, other planes will arrive at the cone of silence over the range station and will have to circle at various altitudes above the radio range and each await its turn to land. Thus it is possible that a plane may have to wait an hour or more before it is able to make a landing. When there is little fuel left, the problem becomes even more critical.

Various methods of instrument landing have been attempted; however, the most promising method is one which utilizes radio waves. These waves are used to give lateral guidance, vertical guidance, and position fixes. With the aid of such a system the entire process of radio instrument landing would take from four to five minutes. This would help to reduce the traffic congestion at an air terminal and avoid delay in landing.

If air transportation is to compete with other established modes of transportation, it must continue its safety record and at the same time increase the regularity of scheduled flight operations. This can best be done by the adoption of a standard reliable instrument landing system.

The Civil Aeronautics Authority, realizing this to be the bottleneck of safe flying under conditions of low ceiling and poor visibility, has endeavored to overcome this difficulty by fostering the development of a suitable instrument landing system. In May 1938, bids were issued on specifications covering the development of a system which would comply with the rigid requirements of airline service. The specifications were based on the development of the art of instrument landing prior to this date.

Development of Instrument Landing

The history of the development of instrument landing dates back approximately ten years. The first serious work on instrument landing was done by the Bureau of Standards for the Aeronautics Branch of the Department of Commerce in 1928. This system used a visual radio range beacon as a runway localizer and marker beacons located on the course to inform the pilot of his location with respect to the point of landing. The altitude was controlled by means of a barometric altimeter. Using this system without the markers, Lieutenant J. H. Doolittle of the Daniel Guggenheim fund made the first successful instrument landing in history on September 24, 1929, at Mitchell Field.

In 1929, Diamond and Dunmore of the Bureau of Standards proposed a method providing guidance in the vertical plane using a constant-intensity glide path. This, along with the runway localizer, gave a three-dimensional system. The runway localizer operated on 278 kilocycles and utilized small multiturn loops. The glide path operated on 90.8 megacycles and utilized a horizontally polarized array. The marker beacons used long low transmission line antennas operating on 3,105 kilocycles. Installations were made at College Park in 1931; at Newark, N. J., in 1933; and Oakland, Calif., in 1934. The Newark and Oakland installations used two marker beacons, one at the airport boundary and one along the line of approach, some distance out from the airport. The system was designed to simplify the interpretation of the radio signals by the pilot. A single cross-pointer instrument provided visual runway-localizer and landing-beam course indications and distinctive modulation of the approach and boundary marker beacons was employed. On March 20, 1933, a completely blind flight was made from College Park to Newark (except for a slight break over Baltimore), on a day when the weather grounded all other aircraft on the Atlantic seaboard. Altogether over 100 blind landings were made at Newark.

Test flights of the Newark installation indicated the practicability of the fundamental principles of the system and pointed toward further development. Reduction of cost of ground station equipment, elimination of bends in the localizer course due to presence of railroad tracks, power lines, etc., and increased slope of the landing path were desired. Since the localizer course is more susceptible to bending at low frequencies, it was decided to use ultrahigh frequencies for the runway localizer as well as the glide path. Tests were made on a combined runway localizer and landing beam operating on a single ultrahigh frequency. Further study indicated that horizontal polarization was preferable for the glide path.

In 1933, the Airways Division of the Department of Commerce installed an instrument landing system using the conventional aural-type radio range augmented by a radio marker and a sensitive Kollsman altimeter on the plane. The plane is guided over the radio range cone of silence which is approximately two miles from the field at a specified altitude, and from this point the pilot glides along the radio range course and over the marker to the field at a rate of descent of 400 feet per minute. This system was not considered feasible as an instrument landing system since the ac-

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curacy of the barometric altimeter is not great enough to prevent the possibility of undershooting or overshooting the airport. However, this is the system which is used at present for coming into airports under low ceilings.

In 1932 and 1933, the Army Air Corps at Wright Field developed and tested a system utilizing a radio compass for guidance and a sensitive altimeter for checking the altitude over ultrahigh-frequency markers. Although installations were begun at a number of airports throughout the United States, it was abandoned since a majority of the airlines felt that it did not give sufficiently precise indications for safe commercial use. Its disadvantages were that a barometric altimeter cannot be relied upon and that a heading established by radio compass is subject to error when cross winds are present. The major advantage was that the system was simple to fly and required a relatively short pilot-training period.

In 1935, the Washington Institute of Technology completed an instrument landing system with elements similar to those of the original Bureau of Standards system. The ground equipment, except the marker beacon, was mounted in an automobile trailer to permit the operation of the system in any direction. The glide-path transmitter operated at a frequency of approximately 93 megacycles, its antenna array utilizing horizontal polarization. The localizer operated on 278 kilocycles and transmitted into small multiturn loops, keying I in one loop and A in the other. The A-I visual indication method was discarded in favor of a double modulator type of localizer; that is, modulating one loop at 65 cycles and the other at 86.7 cycles. Originally, two separate power amplifiers were used, each of which was modulated at one of the two frequencies mentioned above. Each power amplifier independently excited one of the loops. Any change of output in either amplifier caused localizer course variations. To overcome this difficulty, a single power supply was used, and a mechanical modulator connected to the output of the tank circuit was used to supply each of the loops, one at 65 cycles and the other at 86.7 cycles. Thus, any change in amplifier output varied both patterns and the course alignment was not affected. This system was found to work quite effectively. The major disadvantage with this and all other systems using relatively low radio frequencies for the runway localizer is that bends and multiple courses may be obtained when operated in the vicinity of transmission lines, railroad tracks, mountains, and broken terrain.

Transcontinental and Western Air, Inc., in 1935 developed a combination glide path and localizer unit at the Kansas City airport using a frequency of 85 megacycles. Considerable difficulty was encountered due to variations in the glide path when crossing over a river. It was not until later that it was determined that vertically polarized waves were responsible for the discontinuity.

In 1935, the Bureau of Air Commerce conducted further tests on several modifications of the basic Bureau of Standards system. It was decided to abandon keying systems for course identification in favor of a double-modulation system, using 65 cycles in one loop and 86.7 cycles in the other, giving a visual indication. Later it was found that bent and multiple courses which prevailed at the low frequencies could be eliminated by the use of an ultrahigh-frequency localizer, provided care was exercised in locating the localizer with respect to reflecting objects such as hangars, gas tanks, towers, and buildings.

In 1934 and 1935, Doctor E. Kramar of the Lorenz Company in Germany developed a blind landing system using the same elements originally used by the Bureau of Standards, that is, glide path, runway localizer, marker beacons, and monitor system. The transmitter was operated on 33.3 megacycles and excited a vertical half-wave radiator. On each side of the vertical radiator, a reflector was located with a relay at its center. One reflector was keyed by dots and the other with dashes. By interlocking the dots and dashes, two elliptical patterns were obtained, producing an equisignal zone which gave two courses. A visual indicator was obtained by means of a rectifier circuit and amplifier. However, the indicator gave a kicking needle indication. The glide path was obtained by following a constant-field-intensity path. The outer marker was located about 1.9 miles from the airport and the inner marker about 0.19 mile from the airport. Each of the markers transmitted on 38 megacycles, the outer marker modulated at 700 cycles and keyed with dashes $^{4}/_{10}$ second long, and the inner marker modulated at 1,700 cycles and keyed with dots $^{1}/_{18}$ second long. Each marker gave an aural indication as well as lighting an individual light on the instrument panel. A complete monitoring apparatus was provided.

Through the courtesy of the International Telephone and Telegraph Corporation, one complete set of equipment was installed at the Indianapolis airport for test purposes. These tests indicated that approaches could be made to the field under conditions of poor visibility and low ceilings with good reliability. However, several limitations to this system were found. The radio range could be flown better aurally than visually. Originally, the course was about six degrees broad, the width desired in Europe. The pilots in this country who flew this equipment felt that the courses were too wide. The course was reduced to two degrees but even further reduction was desired.

Another difficulty observed was the fact that the glide path was smooth down to a point just beyond the end of the runway, at which point the glide path took a definite dive into the ground. The reinforcing steel in the runway was probably the cause for this abrupt change in the glide path. It was later determined that the use of horizontally polarized waves instead of vertically polarized waves could be used to eliminate this difficulty.

The frequencies used, namely 33.3 and 38 megacycles, are not believed satisfactory for a system which would be used universally, since reflection from the Heaviside layer would occasionally cause interference between stations several thousand miles apart. The Lorenz system, however, had the advantage that the entire system operated on ultrahigh frequencies which were free from atmospheric disturbances.

In 1936 and 1937, United Air Lines and the Bendix Corporation constructed and tested a system having the same elements as the Bureau of Standards system using a combined glide path and localizer transmitter operating on 91 megacycles. Horizontally polarized waves were utilized and to avoid directional effects, a horizontal loop which had a nondirectional characteristic was used for the receiving antenna. An automatic pilot was occasionally utilized in making instrument landings. This enabled the pilot to concentrate on the indications of the cross-pointer instrument and follow them more accurately. During 1936 and 1937 approximately 3,000 hooded landings were made on this system.

It is evident that a considerable fund of knowledge has been gained from the numerous systems which have been described. Many notable contributions have been made by Lorenz, the Washington Institute of Technology, Bendix, United Air Lines, Transcontinental and Western Air, Inc., and the various Government agencies which have worked on the problem. Each of the systems has its limitations although some are better than others for making instrument landings consistently under service conditions.

It was desired to set up specifications which could be used as the basis of a standard instrument landing system for installation on a national scale. The use of ultrahigh frequencies was felt desirable not only because of its freedom from atmospheric and interstation interference, but because the present overcrowding of the low-frequency spectrum makes it impossible to procure frequencies for use in instrument landing on a national scale. This is in accordance with the general plan of the CAA eventually to operate the majority of air navigation aids on the ultrahigh frequencies.

Recommendations of the Radio Technical Committee for Aeronautics

The matter was considered on December 17, 1937, at the sixth meeting of the Radio Technical Committee for Aeronautics, which is a co-ordinating group to recommend policies in regard to radio development as applied to aeronautics. The Radio Technical Committee for Aeronautics is made up of representatives from the airlines, manufacturers, and various Government agencies. The Radio Technical Committee for Aeronautics subcommittee on instrument landing made the following recommendations which are quoted here.

1. Runway Localizer

(*a*). The runway localizer shall operate on an ultra-high frequency, preferably in the band 92–96 megacycles, or, if the localizer transmitter is operated as a separate unit, in the band 108–112 megacycles.

(*b*). Straight course, i.e., one which has no bends or multiple courses perceptible to a pilot flying in still air.

(*c*). The difference in the magnitude of the two patterns of the localizer shall be 0.5 decibel at 1.5 degrees either side of the center line as measured with a linear detector.

(*d*). The vertical needle of the cross-pointer indicator shall give a 10-degree deflection indication for a 1.9-degree angular deviation from the center line of the runway.

(*e*). The range of use as a runway localizer should be 20 miles at 3,000 feet.

(*f*). Freedom from interference pattern effects perceptible to the pilot both in elevation and azimuth.

2. Glide Path

(*a*). The glide path shall operate on an ultra-high frequency, preferably in the band 92–96 megacycles.

(*b*). A smooth glide path shall be provided, i.e., one which is free from interference pattern effects perceptible to the pilot when on the localizer course.

(*c*). The system shall be capable of adjustment to provide a suitable glide path.

3. Markers

(*a*). The markers shall operate on 75 megacycles.

(*b*). It shall be possible to positively identify each marker both aurally and visually by modulation and keying. Modulation frequency of the inner marker shall be 1,300 cycles and that of the outer marker shall be 400 cycles.

(*c*). A normal arrangement of markers would be:

1. At the normal intersection with the glide path.
2. Near the boundary of the airport, the exact location to be determined by local conditions.

(*d*). The marker beacons shall have an array adjustable so that when installed in the boundary position the beam will cause useful indications of a visual device within 700 feet either side of the on-course path and for 300 feet along the glide-path trajectory. Indications from this marker shall be receivable to an altitude of 2,000 feet.

(*e*). The outer markers shall have sufficient power to accomplish a similar visual indication with the same beam pattern at 2,000 feet.

4. Monitor System

(*a*). Satisfactory means for indicating visually the operation of all equipment, shall be provided at a central point.

(*b*). Whatever form of visual indication may be employed shall be smooth in performance and have no irregular characteristics.

5. General Characteristics

(*a*). Frequency of emission of all of the elements of the system shall be equivalent to that obtained with a low-temperature-coefficient quartz crystal.

(*b*). The number of fixed or portable equipment required will depend on conditions prevailing at individual airports.

(*c*). The installation of the foregoing equipment shall not constitute an obstruction to a normal approach to a runway.

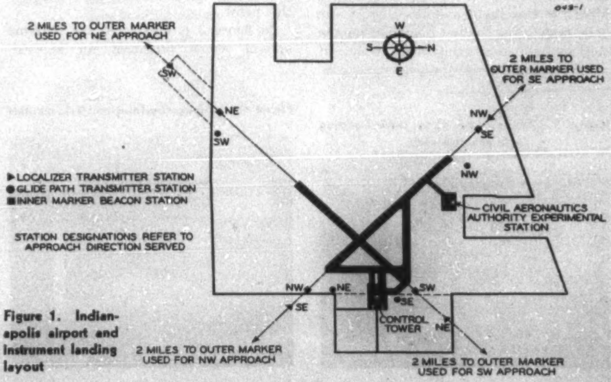
6. The installation of the best known type of approach and runway lights appears to be a most desirable measure in combination with instrument landing facilities.

These are the basic recommendations which were used by the Bureau of Air Commerce in preparing specifications for the development of an instrument landing system. The Bureau of Air Commerce advertised bids and in June 1938 awarded a contract to International Telephone Development Company, Inc., to carry on this development. The complete experimental landing system provides facilities for landing in four directions. The equipment was installed at Indianapolis and was accepted by the Civil Aeronautics Authority in October 1939.

Indianapolis Instrument Landing Installation

Airport Facilities

Four complete groups of instrument landing transmitters have been installed at the Indianapolis Municipal Airport permitting landings in any of four wind directions; northeast, southeast, southwest, and northwest (see figure 1). Each landing direction requires the installation of four separate transmitters; localizer, glide path, inner marker, and outer marker. Each of these installations is connected to the airport control tower by telephone lines so that the group of four transmitters for any of the landing directions can be turned on to meet local wind conditions.


049-1
2 MILES TO OUTER MARKER USED FOR NE APPROACH
W
S
N
E
2 MILES TO OUTER MARKER USED FOR SE APPROACH
SW
NE
SW
NW
SE
NW
▶LOCALIZER TRANSMITTER STATION
●GLIDE PATH TRANSMITTER STATION
■INNER MARKER BEACON STATION
STATION DESIGNATIONS REFER TO APPROACH DIRECTION SERVED
CIVIL AERONAUTICS AUTHORITY EXPERIMENTAL STATION
NW
NE
SW
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2 MILES TO OUTER MARKER USED FOR NW APPROACH
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Figure 1. Indianapolis airport and instrument landing layout

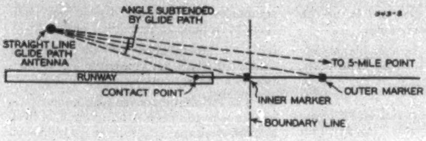


Figure 2. Location of straight-line glide path array

At the outset of the airport installations, the plans called for four installations of constant-intensity glide paths of parabolic shape. This is a well-known type of path obtained by using a simple antenna with, preferably, a broad radiation pattern. The transmitter and radiators may be placed either off the end of the runway or to the side of the runway but the latter position, if opposite or slightly forward from the far end of the runway, will usually place the contact point at a more favorable location near the beginning of the runway and give the ship a longer space in which to land and roll to a stop.

Glide-Path Development

Before these installations were complete, however, a system for producing a glide path of the constant-intensity type which is substantially straight from the outer marker to the point of contact, had been developed and in order to test the new type of path an installation was made at Indianapolis in place of one of the parabolic-glide-path installations.

Previous to the time of this test installation, belief had been expressed by a number of authorities on the subject that a straight-line glide path, as described above, would be ideal because it could be flown with a constant throttle setting and because the path would clear local obstacles with a greater margin. After a number of flight tests by competent pilots including airline pilots, it developed that a path of this shape had too great a rate of descent at the contact point causing the ship to land excessively hard, which, if adopted, would cause the passengers considerable alarm and discomfort. After these and subsequent tests, it developed that the ideal shape for a glide path would be a substantially straight line from a point 1,500 feet at five miles from the airport to the airport boundary, from where the path would become slightly parabolic in shape intersecting the runway surface at approximately one degree. The path would pass over the outer marker, two miles from the airport boundary, between 500 and 700 feet elevation. A path of approximately this shape has since been produced at Indianapolis.

The method by which constant-intensity paths of various shapes, including the straight line and improved arrangements, have been produced involves locating the glide-path antennas at a considerable distance to one side of the runway and forward along the runway so that the various points along the glide path appear in different directions as viewed from the glide-path antenna as shown in figure 2. In this way, the angle subtended by the glide path, as viewed from the glide-path antenna, is opened up so that it becomes possible, by making the antenna directive in the horizontal plane, to proportion the radiation along the extent of the glide path and control the height of the path at the various points. Thus, for example, by sending less energy toward the outer marker the glide path may be made higher there and by sending more energy toward the five-mile point, the path may be lowered at this point.

In figure 3 is illustrated the antenna system which produced an approximately straight-line glide path from the outer marker to the point of contact. This system consisted of two ultrahigh-frequency loop antennas spaced approximately one wave-length apart, fed 180 degrees out of phase with unequal amounts of energy and located in front of a reflecting screen. The antenna system which produces the more suitable type of path involves the use of a greater number of elements to produce the required field pattern shape.

The installation for the "straight line" glide path is located 1,350 feet off the center line of the runway and 1,050 feet forward from the end. The parabolic-curve glide path installations are located 400 feet off the center of the runway and approximately opposite the far end of the runway.

Localizer Installation

The localizer installation consists of a wooden building approximately 12 feet square located off the end of the runway and a localizer monitor unit situated about 100 feet in front of the building on a line with the runway (see figure 4). Inside the building are the localizer antennas mounted under and close to the apex of the roof, the localizer transmitter, the modulator unit, the voltage regulator, the service switches, and telephone (see figure 5). A large wire screen is placed horizontally across the inside of the building just below the antennas primarily to minimize the radio-frequency field in the lower part of the building, in order to avoid induction of currents in and

Figure 3. Straight-line glide path building and antenna array

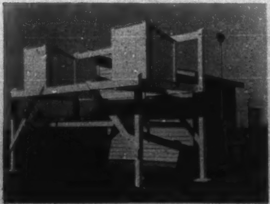


Figure 4. Localizer building and field monitor

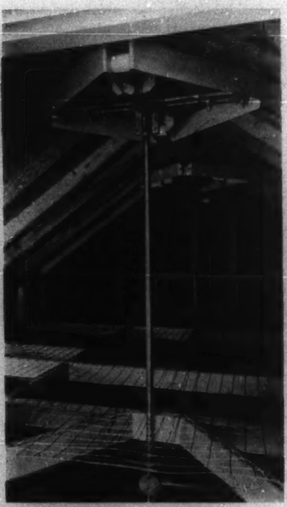
Figure 5. Interior of localizer building showing transmitter and mechanical modulator

consequent reradiation from the irregular objects below the screen. The transmitter and modulator units are mounted on the floor underneath the wire netting.

Localizer Antennas

Early work has shown that fewer common objects around an airport reflect horizontally polarized waves to the same extent as vertically polarized waves. Consequently, it is to be expected that, in general, cleaner patterns can be obtained with horizontally polarized waves than with vertically polarized waves. This point has been strikingly demonstrated at Indianapolis by making direct comparison between two localizers placed in the same location and having identical radiation patterns, the only difference being that one was horizontally polarized and the other vertically polarized. Both of these patterns were fairly broad so that radiation was sent out to various surrounding objects which could reradiate and superimpose their reradiated fields onto the primary field. As a result of the addition of these fields bends appeared in the course. The amplitude of the bends was considerably greater with vertically polarized radiation than with horizontally polarized radiation. In addition, recordings made of the field radiated in other directions lead to the same conclusions.

Figure 6. Localizer array of ultrahigh-frequency loop antennas



These tests showed that a greater number of objects in the field were more effectively reflecting the vertically polarized waves than the horizontal waves. These objects included a hangar, a power line, several buildings, a group of trees, and several fences. While it is of course possible to find objects, for example horizontal wires, which will reflect horizontally polarized waves more effectively than vertically polarized waves, the fact remains that fewer of the common objects which are confronted in practice reflect the horizontally polarized wave to the same extent that they reflect the vertically polarized waves.

In addition to the use of horizontal polarization, it is very important to use pure polarization. If both vertical and horizontal components are present it is possible with certain arrangements of the plane's receiving antenna that either the main course appears displaced from its true location or, which is even worse, a new apparent course is obtained which would not be present if the antenna were operating properly. This situation might arise if the receiving antenna were injured or improperly installed. It is apparent that the presence of vertical components with the horizontal components may actually be dangerous.

In addition to this, the presence of vertical components leads to an effect which has been referred to as "pushing" of the course. This is noticed when the ship is maneuvering to locate the course. The apparent course appears to be displaced depending on the orientation of the ship and when the pilot attempts to turn into the course, he finds that it has disappeared. This effect, under some conditions, may amount to pushing the course as much as one mile off the true course at a distance of ten miles from the localizer while with pure polarization, this effect does not appear.

In order to realize the advantages of horizontal polarization, a special antenna element was developed primarily for localizer use which radiates pure horizontally polarized waves in all directions. The field pattern in the horizontal plane is circular and in the vertical a figure of eight with zero radiation upward. This element is known as the ultrahigh-frequency loop antenna, three of which are shown in figure 6 mounted under the roof of a localizer house. When these loop antennas are used in the localizer antenna system, the maximum vertical component throughout the entire field pattern does not exceed 5 per cent of the horizontal component at that point.

By using three of these elements arranged in line at right angles to the axis of the runway, it is possible to obtain a two-course radio range with patterns similar to those illustrated in figure 7. The course attained will be relatively broad and there will be a substantial amount of radiation directed toward surrounding objects. While this pattern is suitable for relatively clear locations and would be excellent for use as a radio-range pattern where the antenna is located well above surrounding objects,

Figure 7. Ultrahigh-frequency field patterns from localizer antenna

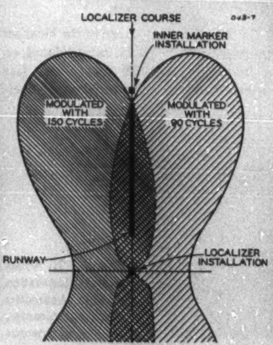
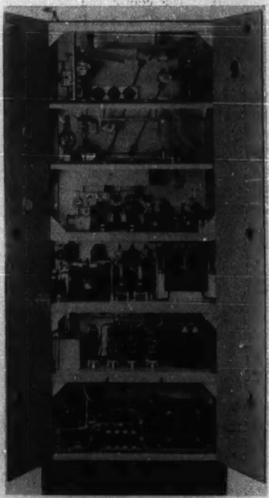


Figure 8. Interior of localizer transmitter from rear



such a pattern may not be entirely satisfactory in an airport in the presence of hangars, buildings, and other reflecting objects. For this application, a pattern which results in much sharper courses and much less off course radiation is desirable in order to secure freedom from course irregularities. In order to produce a more suitable pattern, more radiators are required. Radiating systems with five, six, and seven elements have been used at Indianapolis and a localizer building with a five-element array is shown in figure 4. The fourth and fifth elements, placed in line with the center three, are located in the extensions built onto the ends of the buildings. These extra elements are parasitically driven.

With the antenna systems outlined above, the courses produced at Indianapolis are entirely free of multiple courses and the bends in any of the courses do not exceed 0.15 degree and these bends could be further reduced if not completely eliminated by the results of more recent localizer development work. However, since these bends are so small that they are barely perceptible on the cross-pointer instrument, further improvement is not warranted. Experience has shown that unless bends exceed 0.18 degree or more in amplitude, they are not objectionable.

The two overlapping patterns in the radiated field give side indications of the guiding path which may be easily located at distances of 20 miles from the airport boundary. The course can be picked up at a distance of 70 miles from the airport while flying at an elevation of 4,000 feet or over.

The four localizers installed at Indianapolis have various degrees of sharpness; the sharpness being the ratio in decibels of the field strength of the 90-cycle and the 150-cycle patterns at $1^1/_2$ degrees off course. The sharpest of the patterns exceeds six decibels while others range in the neighborhood of two decibels.

The courses are straight and reliably stable; maintaining their alignment to within one-tenth of one degree under all normal weather conditions.

Localizer Modulator

The localizer modulator is a mechanical modulator constructed to furnish modulated radio-frequency energy for the two overlapping localizer field patterns (see figure 7). Inasmuch as the position of the localizer course is largely dependent on the relative amplitude of the two field patterns, the mechanical type of modulator is used because of its inherent stability and freedom from aging such as would be found in vacuum-tube modulators. The modulator divides the output of the localizer transmitter equally, modulating one-half of the power at a 90-cycle rate and the other half at a 150-cycle rate. Bridge networks, built into the modulator, divide the transmitter output and prevent cross talk or interaction between the 90- and 150-cycle modulator sections as well as feed the carrier and side bands with proper amplitude and phase to the three main localizer antenna radiators. Controls are provided in the unit for adjusting the position of the course with respect to the runway.

The normal percentage of modulation obtained with the mechanical modulator ranges between 95 and 100 per cent although any percentage below this range may be obtained. The radio-frequency-modulated envelope shape from either the 90 or the 150 modulator section, independently, is such that the audio-frequency distortion as measured in the output of a nondistorting detector does not exceed ten per cent and normally ranges close to six per cent. Cross modulation between channels does not exceed four per cent; this percentage being the per cent of 150-cycle modulation appearing in the 90-cycle channel or the per cent of 90-cycle modulation in the 150-cycle channel.

Localizer Transmitter

The rated power output of the localizer transmitter is 300 watts, unmodulated, on a frequency of 109.9 megacycles (see figures 5 and 8). The transmitter operates from 220 volts, single phase, 60 cycles, and receives its entire power of approximately 2,000 watts through a voltage regulator which holds the voltage within plus or minus one per cent for all normal feeder variations.

Figure 10. Marker transmitter

The transmitter is crystal controlled from a 4,579.17-kilocycle temperature-controlled crystal and the crystal oscillator tube functions also as a frequency multiplier. Three frequency multiplier stages follow the crystal oscillator tube to produce the output operating frequency. In order to raise the output of the final multiplier to the rated transmitter output level, the last two stages are push-pull amplifiers operating on the output frequency.

In order to insure reliable operation of the transmitter during cold weather, the ventilating and modulator motors are electrically heated to maintain their bearing temperature above the freezing point. Special lubricating oils which remain viscous to very low temperatures are used in the bearings of contactors and other mechanical parts. The circuit-breaker dash pot also contains a special fluid which remains viscous down to the low temperatures in which this unattended equipment will be required to operate.

The transmitter contains control relays so that it can be operated from the tower. A monitoring arrangement is provided which operates in conjunction with the field monitor (to be described below) so that the position of the course with respect to the runway as well as the strength of the radiated patterns may be ascertained from meters located on the front of the transmitter.

Localizer Monitor

In order to monitor the operation of the localizer installation, from the control tower, a "localizer monitor" is installed about 100 feet from the localizer building on a line between the latter and the end of the runway as shown in figure 4. The monitor contains an insensitive receiver

Figure 9. Inner-marker installation showing transmitter house, counterpoise, and antennas

Figure 11. Monitor and control desk in airport control tower

which operates a course indicating instrument in the localizer transmitter and a duplicate instrument in the control-tower desk. The indication is the same as would be obtained from the vertical needle in the cross-pointer instrument of an airplane were it stationed at the monitor site. The monitor facilities are adjusted to indicate the extent of off course deviation in fractions of one degree.

Underground Localizer Installation

In one localizer installation where insufficient space was available off the end of the runway, making it necessary to locate the building close to the runway, the equipment was installed in a concrete pit below ground level to lower the building height and reduce the hazard. The floor of the pit is approximately eight feet below ground level and accommodates the same equipment as the normal building with the addition of the high-voltage distribution transformer. The pit is covered by a wooden structure shaped like the roof of the normal localizer building. Under the roof are the localizer antennas situated about six feet above ground level (see figure 6). The only additional equipment required for this installation is a sump pump to protect against the possibility of water gathering in the pit.

The lowering of the antenna system close to the ground in this installation in no way affected the operation of the localizer except that the distance from the airport at which the course could be picked up was somewhat reduced.

Glide-Path Installation

There are two types of glide-path installations at Indianapolis; the installation for producing a parabolic path and the installation for producing a straight-line path. In both installations, the transmitter is located inside a wooden building approximately the same size as the localizer house. The antennas for the straight-line installation are erected outside the building as shown in figure 3 while the antennas for the parabolic installation are erected inside the building. The antennas for the latter installation consist of two half-wave radiators fed in phase and mounted horizontally end to end along one wall about ten feet above the floor. A vertical wire screen the length of the building and extending from the roof to about five feet above the floor is located one-quarter wave length behind the antennas. The building is so oriented that the maximum of radiation is directed toward the approach end of the runway served by the installation.

Glide-Path Transmitter

The glide-path transmitter is identical in construction and size to the localizer transmitter except that it operates on a frequency of 93.9 megacycles; requires a crystal with a frequency of 3,912.5 kilocycles; the output is modulated at 60 cycles and no monitoring facilities for use with a field monitor are required. The 60-cycle modulation is accomplished by applying 60-cycle plate voltage to the two tubes in parallel in the output stage. Means for adjusting this voltage are provided in the transmitter so that the field strength along the constant-intensity path can be adjusted. In general, the field strength for all constant-intensity paths must be identical at a given height over the outer markers.

Marker Installation

Marker beacon installations consist, mainly, of a small waterproof aluminum house situated alongside of a wire screen counterpoise 20 feet square, over the center of which is located two half-wave radiators mounted end to end (see figure 9.) The waterproof house is approximately $3^1/_2$ feet long, $2^1/_2$ feet wide, and 3 feet high with a sloping roof, mounted above ground on angle legs. The wire screen is supported by an angle framework and also mounted above ground on angle legs. Concentric gas-filled transmission lines joining the waterproof house with a matching box located between the ends of the radiators, are used for feeding the marker transmitter output to the radiators.

The marker transmitter and its associated line-voltage regulator along with a telephone box and cable terminal are mounted inside the waterproof house. The transmitter is mounted on a sliding shelf so that it can be easily pulled out of the small house for servicing while in operation. While in this position, complete access may be had to all parts of the transmitter.

The installation is oriented so that the end-on radiators are parallel to the center line of the runway. In this way, the major axis of the elliptically shaped pattern in the horizontal plane is at right angles and situated over the runway center line. In outer-marker installations, the radiators are located one-quarter wave length over the counterpoise whereas the radiators in

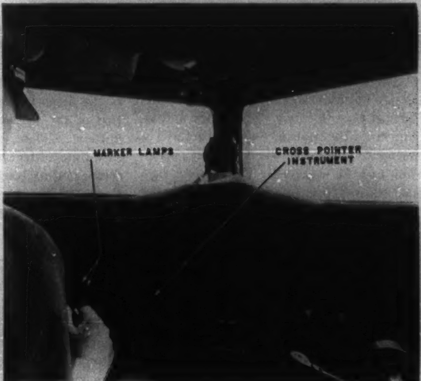


Figure 12. Airplane instrument panel showing cross-pointer instrument and marker lights

the inner marker installation are only one-eighth wave length above the counterpoise. In the latter installation, where the equipment is located close to the end of the runway, the maximum over-all height of any piece of equipment does not exceed four feet.

MARKER TRANSMITTERS

The marker transmitters are approximately 18 inches high, 27 inches wide, and 15 inches deep (see figure 10). The transmitter is built as a single portable unit, containing all radio-frequency and power-supply circuits, audio oscillator and modulator, keying and monitoring equipment. All electrical connections are made to the transmitter through easily removed plugs. The rated output of the transmitter is five watts on 75 megacycles and the carrier frequency is stabilized by a crystal on 4,166.7 kilocycles.

In the radio-frequency-stage lineup, the crystal oscillator tube serves as a frequency multiplier in addition to its duty as an oscillator. Following the oscillator stage are two frequency-multiplier stages and one amplifier stage operating on carrier frequency. The radio-frequency carrier in the outer marker transmitter is modulated with 400 cycles keyed so as to produce equal-length pulses at the rate of two per second. The inner marker carrier is modulated with 1,300 cycles and keyed so as to produce equal-length pulses at the rate of six per second. Keying of the modulating voltage is accomplished with electrical contacts mounted on a small electric motor located within the transmitter. The motor operates on a low direct potential and requires a relatively small amount of power from the d-c plate-supply source. The motor is especially designed and constructed for reliable operation in the extreme temperature ranges of minus 40 to plus 75 degrees centigrade in which the marker equipment is required to operate.

The transmitters are intended for remote operation and monitoring from the airport control tower. A monitor unit is built into the transmitter which returns a signal to the tower where correct operation may be observed. The monitor signal returns over one pair of telephone wires which also carries direct current for controlling the transmitter as well as voice and ringing frequencies for telephone service between tower and marker.

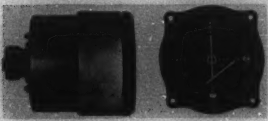


Figure 13. Cross-pointer instrument

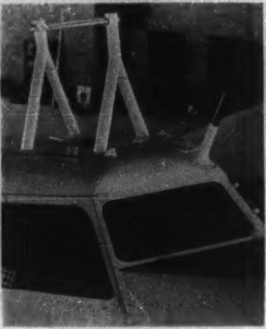


Figure 14. Ultrahigh-frequency loop antenna for reception of localizer and glide-path indications mounted on top of ship

EQUIPMENT CONTROL

The airport tower is equipped with a monitor and control desk by which the tower operator can select landing directions at will (see figure 11). Calibrated instruments and signal lamps give quantitative and qualitative indications of the various transmitters. These indicators are operated by the transmitter monitors and field monitors and indicate the position of the glide path, alignment of the runway localizer courses, and correct operation of the marker transmitters. If the output of any transmitter falls below a predetermined level, visual and aural alarms indicate the trouble. The alarms also operate if the runway localizer course should shift sufficiently to cause an airplane following the course to land off the runway.

The top of the desk is a pictorial map of the airport showing clearly the position of the various runways and transmitters. Colored signal lamps represent each transmitter and are lighted when the corresponding transmitters are in operation. At the head of each runway is located a translucent miniature airplane which is lighted to show the direction of approach.

A pen recorder incorporated in the unit supplies a permanent record of the operation of the system. This instrument records the elapsed time a runway has been in operation, the output of each transmitter, and the localizer course alignment. This multiple function is accomplished with a single pen by recording successive short intervals of the operation of each unit.

Telephone wires emanating from this desk and terminating at the field installations provide means for starting and stopping the equipment as well as monitoring the operation of the equipment. Telephone-type cables were laid on the airport property to provide wire connections between the control tower and the localizer installations, glide-path installations, and the inner-marker installations. Telephone lines from the control tower to the outer marker positions are provided by the local telephone company.

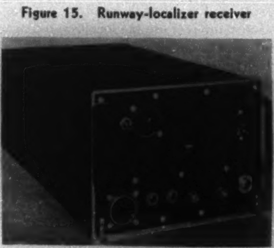
The outer markers, which operate from the 115-volt mains, receive their power from the local power company mains closest to the installation site. Power for operation of the localizer, glide-path, and inner-marker equipment is fed from the control tower at 2,300 volts in high-voltage cables also laid on the airport property.

Airplane Instrument-Landing Receiving Equipment

AIRPLANE FACILITIES

In order to utilize the various signals sent by the transmitters on the ground the pilot observes the position of the two needles of a cross-pointer meter and also notes when either of two marker indicator lamps is illuminated. A view of a typical airplane instrument panel showing the location of the meter and the lights is shown in figure 12 while a view of the cross-pointer meter is shown in figure 13. The two needles of the meter are operated by the outputs of two special radio receivers which receive the transmissions from the glide path and the runway localizer transmitters. Three indicator lights are operated from the output of the marker beacon receiver. Two of these are used in connection with the instrument landing system and show when the plane is over the outer marker and the in-

Figure 15. Runway-localizer receiver



ner marker. The other light shows the pilot when he is over one of the fan markers located at important points along the airway or over the towers of the radio-range transmitting station which are equipped with cone-of-silence markers. The marker-beacon receiver is in continuous operation during the course of a flight as it has other important uses in addition to its part in the instrument-landing procedure while the glide-path and the localizer receiver are used only while making an instrument landing.

One antenna, shown in figure 14, is used to receive both the runway-localizer and the glide-path signals. This antenna is of the horizontal ultrahigh-frequency loop type tuned to 93.9 and 109.9 megacycles and is designed to receive horizontally polarized waves. As mounted on a plane it has an essentially circular horizontal reception pattern. A horizontal dipole antenna mounted underneath the plane is used to receive the marker-beacon signals.

In common with all receivers designed for use in airplanes the receivers for this service must be compact, lightweight, and rugged. In addition to these basic requirements the demands of this new service impose a very rigorous requirement for stability of operation. In spite of wide variations in ambient temperature, humidity, and primary power-supply voltage, the characteristics of the receivers must remain substantially constant as any variation during the landing maneuver will result in an apparent change in the path. The principal characteristics of these three receivers are described in the following sections.

Runway-Localizer Receiver

This receiver, a front view of which is shown in figure 15, employs a superheterodyne circuit and has a crystal-controlled beating oscillator for operation at a fixed frequency of 109.9 megacycles. As explained in a previous section of this paper the runway-localizer transmitter is continuously modulated with 90-cycle and 150-cycle tones. In order to insure that there is no apparent error in the course, it is essential that the relative gain for these two modulation frequencies remains constant.

A simplified schematic of the circuit arrangement is shown in figure 16. The radio-frequency input is applied to the control grid of the first detector $V1$ while the beating-oscillator voltage is impressed on the screen grid. The output of the tube is fed to a three-stage intermediate-frequency amplifier system operating at a frequency of ten megacycles and having four inductively coupled double-tuned circuits. These highly selective circuits in connection with the tuned input circuit provide the radio-frequency selectivity shown in figure 17. The output of the intermediate-frequency system is fed to diode $D1$ of the detector tube $V5$ and the audio-frequency voltage developed across the diode load resistance $R1$ is impressed upon the grid of the triode section of this tube. The amplified audio-frequency output appearing across the cathode resistance $R2$ is applied to the grid of the second audio-frequency amplifier tube $V6$. The plate of this tube is connected to the double-band-pass audio-frequency filter which separates the 90-cycle and the 150-cycle components of the signal modulation. Two copper-oxide rectifiers containing selected and balanced units are used to rectify the two audio voltages for operating the vertical needle of the cross-pointer meter. In order to compensate for any unbalance which exists in the audio-frequency filter due to temperature variation, a temperature-sensitive resistance element $RT1$ having a high negative temperature coefficient is connected across one of the rectifier load resistors.

Signals received from the localizer transmitter vary over a wide range of intensity as the plane approaches the airport and therefore a wide-range automatic-volume-control system is required to maintain a nearly constant audio output. The control grids of the intermediate-frequency amplifier tubes $V2$ and $V3$ obtain their normal minimum bias from the drop across resistance $R3$ and the gain adjusting potentiometer $P1$. When a signal is received, the automatic-volume-control circuit increases the bias applied to the control grids in proportion to the signal level applied to the receiver. An increase in signal results in an increased negative bias on the grid of $V5$ which reduces the plate current of the triode section flowing through the cathode load resistance $R2$. The voltage drop across the resistance is reduced and the potential of the cathode of $V5$ is lowered until it becomes more negative than the normal bias applied to diode plate $D2$ of the $V5$. At that point diode $D2$ draws current which flows through $R4$ and the voltage drop across that resistor is added to the normal bias to secure automatic volume control on the grids of $V2$ and $V3$. Bridged across the minimum-bias resistor is another temperature-sensitive element $RT2$ which causes the bias voltage appearing across $R4$ to decrease with increasing ambient temperature in order to maintain nearly constant receiver gain.

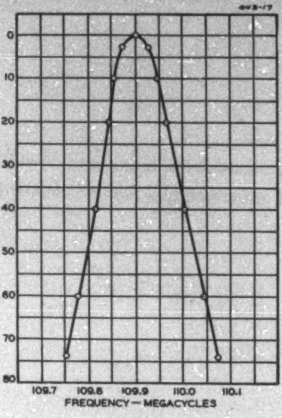
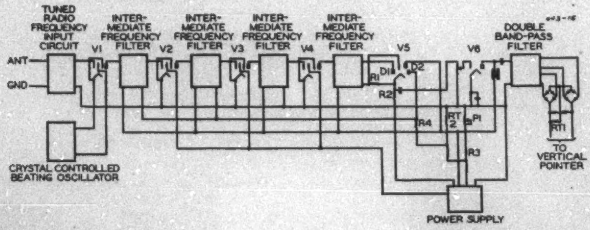


Figure 17. Selectivity of runway receiver

Figure 16. Simplified schematic of runway-localizer receiver



TUNED RADIO FREQUENCY INPUT CIRCUIT
INTER-MEDIATE FREQUENCY FILTER
INTER-MEDIATE FREQUENCY FILTER
INTER-MEDIATE FREQUENCY FILTER
INTER-MEDIATE FREQUENCY FILTER
643-16
DOUBLE BAND-PASS FILTER
ANT
GND
V1
V2
V3
V4
V5
V6
R1
D1
D2
R2
R4
RT 2
P1
R3
RT1
TO VERTICAL POINTER
CRYSTAL CONTROLLED BEATING OSCILLATOR
POWER SUPPLY

Glide-Path Receiver

This receiver, which is quite similar in appearance to the localizer receiver, is a crystal-controlled superheterodyne set operating at a fixed frequency of 93.9 megacycles, intended for the reception of tone-modulated continuous-wave transmission from the glide-path transmitter located at the airport. It is essential that the over-all receiver gain remain practically constant for a wide range of variation of temperature, battery voltage, and

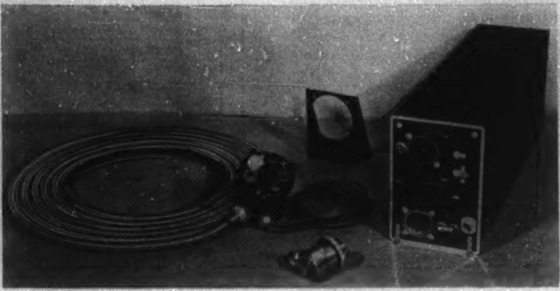


Figure 18. Marker-beacon receiver

humidity as the determination of the path of descent of the airplane is based upon a fixed receiver sensitivity. In order to meet these requirements, voltage regulators, ballast lamps, and temperature-sensitive resistance elements are used within the unit.

Marker-Beacon Receiver

This receiver, a photograph of which is shown in figure 18, is a crystal-controlled superheterodyne unit operating on a fixed frequency of 75 megacycles and arranged to provide both visual and aural indication of the signal being received.

Visual indications are provided by three indicator lamps mounted on the ship's instrument panel. As previously pointed out, two of the lamps are used for instrument landing purposes and the third is used to indicate the position of fan markers and cone-of-silence markers. The modulation frequency for the latter is 3,000 cycles; the inner and outer markers employ 1,300 and 400 cycles respectively.

Three filters are provided in the receiver to separate the 400-, 1,300-, and 3,000-cycle modulating tones. The output of each filter is applied to a copper-oxide rectifier and the resulting current flows through a d-c winding on a saturable-core reactor. An a-c winding also on this core is in series with the indicating lamp and the lamp-voltage supply source. When the impedance of the reactor is lowered by saturation due to the direct current, sufficient alternating current flows to operate the lamp.

Results

After the equipment was installed a meeting of the Radio Technical Committee for Aeronautics was held at Indianapolis in September 1939, for the purpose of demonstrating the equipment to the members and to determine if the system developed was suitable for airline use. The committee unanimously agreed that the general Radio Technical Committee for Aeronautics specifications had been complied with fully. However, in view of the improvement in the glide path which was demonstrated as being entirely feasible, the airline pilots present felt that neither a straight line nor a curved glide path was entirely satisfactory and made the following recommendations as to the most desirable shape of the glide path when on the localizer course:

> "A glide path intersection shall be obtained at 1,500 feet altitude at a distance of six miles from the transmitter end of the runway. The point of contact shall be not closer than 3,000 feet from the far end of the runway. At the point of contact the glide path shall have an angle with the runway not less than one degree and not more than two degrees. The glide path shall pass through the following points not less than 500 feet nor more than 700 feet altitude at a distance of three miles from far end of runway, and 1,500 feet altitude at six miles. The rate of descent shall not exceed 800 feet per minute."

Further work has been conducted by the International Telephone Development Company, which has succeeded in demonstrating a glide path meeting the pilots' recommendations.

Approximately 50 hooded landings have been made in various types of aircraft such as Douglas *DC*-3, Boeing 247, and Waco *N*. Numerous instrument approaches have been made under conditions of heavy rain, snow, low ceilings, and poor visibility. A program of flight training and testing is being organized in order that the airlines, army, and navy may flight test the equipment either under the hood or under bad weather conditions.

The Civil Aeronautics Authority is planning to install a number of similar systems throughout the United States in the near future for service tests and pilot training.

Conclusions

1. To effect precise landings with a minimum of effort on the pilot, experience has shown that four separate elements are advantageous, a localizer to furnish lateral guidance, a glide path to provide the path of descent, and two markers to indicate the progress along the course determined by the localizer and glide path.

2. Ultrashort waves may be successfully employed to provide both horizontal and vertical guidance as well as marker indications.

3. Localizer courses must be much sharper than heretofore employed to avoid mutilation by reflections. The difference in amplitude of the patterns should be at least 2.2 decibels $1^1/_2$ degrees off course in the majority of installations. This is approximately four times sharper than stipulated in specifications of the Radio Technical Committee for Aeronautics. In some instances it is necessary to have a sharpness as great as 6 decibels in order to eliminate bends in the course.

4. Localizer polarization must be pure to secure independence of the aircraft antenna characteristics and direction of approach of the plane to the localizer course.

5. The localizer should be independent from the glide path in order to permit its installation well beyond the end of the runway so that it is not a hazard to aircraft landing or taking off.

6. Experience has indicated that the two-course localizer is inherently more simple to use than localizers with four or more courses.

7. It is advantageous for the glide path to be of a controllable type so that path shape can be adjusted during installation to accommodate the landing characteristics of aircraft.

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3. Ultrahigh Frequency Loop Antennas, A. Alford and A. G. Kandoian AIEE Transactions, volume 59, 1940 (see annual reference index for page numbers).

Discussion

For discussion, see page 1120.

The CAA-MIT Microwave Instrument Landing System

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THE PROBLEM of developing an instrument landing system in which the pilot is kept informed as to both his location and orientation by the indications of a single instrument was put to the Massachusetts Institute of Technology several years ago by I. R. Metcalf of the Civil Aeronautics Authority. The solution of this problem includes the application of centimeter waves, the development of a straight-line glide path, and the realization of a novel instrument combination. Intrinsically and because of recent trends in aircraft navigation these features, we believe, represent significant advances in the art. The experimental development described here in general outline was carried on under the sponsorship of the Authority and was built on MIT's background of research on the broad problem of navigation in fog and on work in the short-wave field per se as a foundation.[1] The apparatus was designed to demonstrate feasibility only. Commercial embodiment of the results is it is felt now within reach of the industry.

The General Problem

The problem as presented was based on the idea that a serviceable instrument landing system could be realized if, under conditions of no visibility, it were possible to present to the pilot the same information he would obtain were he to land on a clear night by means of three lights arranged on the landing field as shown in figure 1. These lights would indicate to the pilot in a simple, natural, and continuous manner not only his location but also his attitude and heading.

To visualize the problem in greater detail imagine the three lights as seen through a transparent screen in front of the pilot. Assume that the screen has vertical and horizontal cross hairs intersecting at its center. When the airplane is in the proper orientation for landing, and is on the reference landing path defined by the three lights as shown, the pilot sees these lights equally spaced along the horizontal cross hair, the center light coinciding with the center of the screen. Any departure of the lights from this pattern on the screen indicates a corresponding departure of the airplane from the proper location or orientation.

In the simplest interpretation, the location of the light group indicates orientation and the relative configuration of the lights as a group indicates location. Thus turning of the airplane to the right or left moves the lights as a group left or right respectively; moving to the right or left of the reference landing path moves the image of the center light nearer to the image of the right- or left-hand light respectively.*

The Metcalf problem then was to provide on the instrument panel of an airplane the same information indirectly by means of the visual image of three hypothetical lights with movements corresponding to those of the real light arrangement under corresponding conditions. The solution MIT proposed was to synthesize the image of the three hypothetical lights by a combination of radio- and gyro-instrument means. Information regarding orientation was to be derived from gyro instruments on the airplane, and information regarding location to be derived from electromagnetic radiation from transmitting means on the ground. The combined information was in turn to control three spots on the face of a cathode-ray tube used as the indicating instrument. This use of the gyro group suggested at once an instrument that could serve not only for landing but also for cross-country flying.

The radiation from the ground was to define a straight-line reference path down which the airplane could be flown at its normal glide angle. It is easier to control an airplane when landing at constant air speed and rate of descent, that is, when flying down a straight glide path than when following a curved glide path. Hence, aside from the fact that a straight-line path is inherent to the three-light landing procedure, from flying considerations alone, there is justification for a path of this form. The information must be given to the airplane as to whether it is above, below, to the right, or to the left of the reference path. It was planned to supply this information by the use of crossed or equisignal beams: one pair with separate modulations for the up-down information and a corresponding pair for the right-left information. Utilization of the equisignal beams made available the advantages of automatic volume control, including freedom from effects of change of received signal with antenna orientation, and of change in receiver sensitivity.

To the location information obtained from equisignal beams and the orientation information from the gyroscopic instruments† must be added distance information. Distance information, if it is desired to complete the three-light illusion, could be obtained from a signal intensity control moving the two outer spots on the indicating instrument farther apart as the landing field is approached, or it could be given independently by means of marker beacons.

The research resolved itself into two principal aspects, one dealing with the production of the instrument in the air-

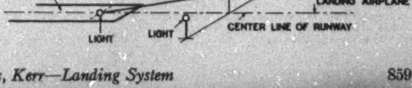

Figure 1. Arrangement of lights on landing field

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E. L. Bowles is professor of electrical communications and W. L. Barrow and W. M. Hall are assistant professors of electrical communications at Massachusetts Institute of Technology, Cambridge; F. D. Lewis and D. E. Kerr are associated with the same institution.

1. For all numbered references, see list at end of paper.

* During the study and testing of various approximations to the true image of the three lights on the landing field, the fact developed that the pilot, when landing in clear weather, makes use of the horizon as well as the view of the airport in front of him. It is possible to give the equivalent of this information by controlling the relative vertical positions of the outer two of the three spots on the instrument by means of the gyro-horizon alone and the center one by means of the landing reference path alone. The general behavior of the three spots will still conform to the basic principles set forth in the original problem.

† The heading information, it was contemplated, could also be obtained from a radio compass.

plane and the other with the production of the landing reference path. The rigid self-imposed requirement that this path should be substantially free from extraneous effects indicated concentrated beams and this in turn made it desirable to use as short a wave length as possible for beam energy. On the other hand the limitations of high-frequency commercial vacuum-tube equipment made it desirable initially to set 100 centimeters as the lower practical limit of this wave length. Although this wave length was much longer than desirable, it was hoped that as the research progressed the ultrahigh-frequency art would develop to a stage where generators and receivers of much shorter wave length would be available. In the experimental investigation wave lengths of the order of 50 centimeters were used for convenience with the idea that the data could subsequently be applied to the design of longer or shorter wave length radiators by the use of dimensional multiplication. Later promising developments in the art made it feasible to adhere to approximately 50-centimeter waves in all our work.

The possibility of using infrared radiation as well as radio waves to produce the reference path was also investigated. The MIT work on light penetration of fog had indicated that there was possibly a pass region in the spectrum between 8 and 12 microns. This possibility together with the feeling on the part of many that infrared waves offered a solution made it important to examine the applicability of infrared radiation. After investigation including experimental confirmation covering wave lengths out to 15 microns, the use of these long infrared waves was ruled out because of the unobtainable power that would be required to provide the desired range over a sufficiently wide field for this specific purpose.‡

The Instrument

The instrument aspect of the problem very quickly reduced itself to three divisions: the obtaining of electrical signals from the gyro instruments, the development of instrumental means of synthesizing the images of the three hypothetical lights for presentation to the pilot, and the study of approximations to the true image patterns that might be utilized as practical variations or simplifications.

A simple electrical take-off for obtaining signals from the gyroscopic instruments was sought and it was decided to investigate first the applicability of a voltage divider or slide-wire means whereby rotation of the gyro element would cause a fine wire brush to move across a resistor card and deliver a voltage of magnitude and sign proportional to angular displacement. Should this idea prove workable it promised direct control of the spots and a simple means of controlling instrument sensitivity by the adjustment of the direct voltage across the dividers. It also offered the possibility of grading these resistors so as to make possible sensitivity which varied with angular displacement.

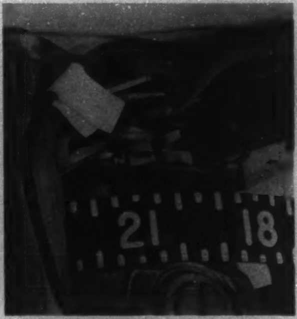

Figure 2. Electrical take-off mechanism in one of the gyro units

In taking off electrical control voltages from the gyro instruments it is essential that friction and other disturbing forces be kept low, if the gyros are to function properly. The bank and climb gyro is stabilized by gravity, and will eventually return to its proper orientation if displaced, but the directional gyro has nothing but its "memory" to rely on, once it has been oriented. Therefore, the very practical criterion that the frictional and other forces added by the electrical take-off mechanism be small compared to the friction already existing in the gyro mounting, was used as a guide in this work. Doctor C. S. Draper of the MIT department of aeronautics was fortunately able to provide not only the necessary information about commercial gyro instruments, but also the means for measuring the added friction of the electrical take-off. The development of the electrical take-off was carried out largely under his direction by his colleague Doctor J. D. Trimmer. On the basis of their work the simple expedient of obtaining voltages directly from a resistor by a contact brush carried on the gyro mounting ring appeared to be practical. The added friction amounted to about ten per cent of that already in the gyro and produced no detectable effect on the behavior of the instrument. A photograph of the electrical take-off in one of the gyro units is shown in figure 2.

Since the voltage delivered by the gyro instrument was sufficient to deflect the spots on the cathode-ray tube directly, amplifiers with their attendant complexity were unnecessary. As negligible currents were required, the area of contact could be kept very small and contact pressures kept sufficiently high to make the resistor card self-cleaning without producing undue drag. The brushes were shaped from tungsten wire 0.003 inch diameter.

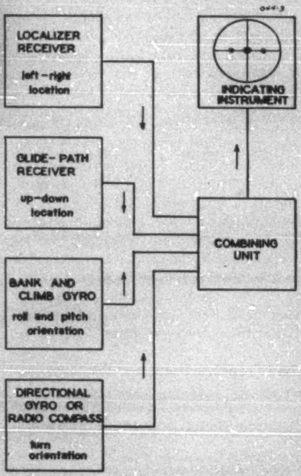

Figure 3. Block diagram of airplane equipment

Instead of a three-gun cathode-ray tube, which might at first thought appear to be ideally suited to produce the three-light pattern, a standard tube was used in conjunction with mechanical commutation** to produce the three-gun effect. The control circuit was somewhat simpler and it was also a definite advantage to have only one spot to be focused and centered as well as to have the same sensitivity for each virtual spot. Furthermore, a wide variety of single-gun tubes were readily available.

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‡ The work of MIT in the general field of light scattering by fog, including the work done in connection with the instrument landing project, will be published in the near future.

** Electronic commutation was examined; however mechanical commutation was more readily adaptable to this particular instrument.

These elements were combined as shown in the block diagram of figure 3 which summarizes the broad features of the instrument and indicates their functional interrelationship.

Thus, a method and mechanism have been developed for synthesizing a visual image simulating three lights on the landing field. This system, like any other which makes use of electrically operated indicating instruments, makes possible the removal of the gyro instruments from the instrument panel, the duplication or repetition of their indication at several locations in the airplane, and the concentration of essential landing information on a single instrument. The use of the equipment during flight is also feasible. The instrument gives gyro information and the runway localizer signals may be replaced by range signals.

Finally the instrument indication is not limited to the three-spot image but can, for instance, be in the form of a central figure moved by the reference path or by the radio range signals and a bar image, simulating the bar on the gyro horizon. The bar image can also be controlled in translation so as to convey not only bank and climb information but also heading. A bar-and-circle pattern has been incorporated in the Flightray[2] developed by the Sperry Gyroscope Company, Inc.*

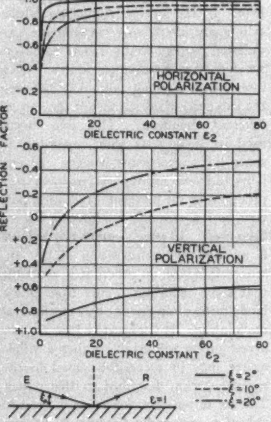

Figure 4. Reflection factor for ultrahigh-frequency waves as a function of dielectric constant

Figure 5. Electromagnetic horn radiator

The Landing Reference Path

A straight-line landing reference path with an adjustable angle of from three to six degrees with respect to the horizontal was desired. It was recognized at the outset that to realize this path relatively free from ground, weather, and reflection effects highly directive component beams would be required. This would be particularly true for the lower of the two beams making up the glide path. To fulfill this requirement by radio means called for wave lengths as short as possible in order to obviate radiating systems of undue physical size. A comparative investigation was therefore made of broadside and end-fire arrays of half-wave elements, long-wire antennas, parabolic reflectors, and horn radiators.

Preliminary analysis and tests indicated that a horn of rectangular cross section could by appropriate design be made to produce smooth beams of the requisite degree of directivity in horizontal and vertical planes.[3] On the basis of the results of this investigation the horn type of radiator was selected because it was simpler to adjust, freer from spurious radiation from feed wires, and had inherently a better radiation pattern than was obtainable from the other devices examined.

Even with wave lengths as short as 50 centimeters and antennas of reasonable size, it is not possible to produce landing beams of sufficient sharpness to be entirely free from the effects of reflected energy from the ground. It was therefore desirable to determine which of the two polarizations, horizontal or vertical, would make these reflections least. Past experiments with instrument landing systems had resulted in a divided opinion as to the preference of one polarization over the other for this service. A theoretical investigation of this problem was therefore carried out to aid in selecting proper polarization. The earth may be treated with a fair degree of accuracy as a perfect dielectric for the ultrahigh frequencies employed in this system. Its effect may be expressed by means of a reflection factor, equal to the ratio of the intensity of the wave reflected from it to that incident upon it. A change in the physical condition of the earth caused by rain, drought, sleet, and snow is accompanied by a change of the dielectric constant. That polarization will be preferable which causes the least change in reflection factor for given changes in the dielectric constant. A typical set of curves showing the variation of the reflection factor with the dielectric constant for several angles of incidence is reproduced in figure 4. Clearly, horizontal polarization provides the more uniform behavior with changing ground conditions. Since making this study, confirmatory experimental evidence from several sources has been obtained in favor of horizontal polarization of beams for instrument landing.

Making use of a theory of horn design evolved in a separate research program[4] a horn was constructed specifically for the production of a landing beam. The body of the horn was made of three-eighth-inch bakelite-bonded plywood and was lined inside with thin copper foil. Figure 5 shows the completed horn. It is about

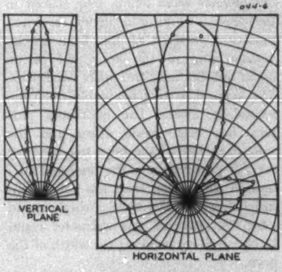

Figure 6. Measured relative electric intensity in vertical and horizontal planes for the horn of figure 5

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* The Sperry Gyroscope Company at about the time the MIT program was developed undertook the development of a cathode-ray-tube flight instrument which included information from the gyro group. It is with pleasure that we acknowledge here the cordial constructive exchange of ideas with this organization in connection with related aspects of our project.

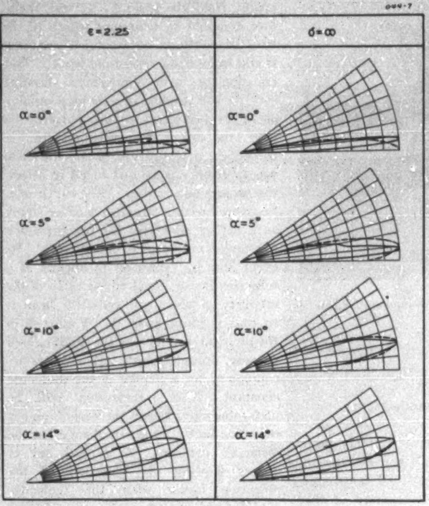

Figure 7. Calculated effects of a perfect dielectric earth ($\epsilon = 2.25$) and a perfectly conducting earth ($\sigma = \infty$), on horizontally polarized radiation from the horn of figure 5. Axis of horn inclined at the angle α; mouth resting on the earth

26 feet long by 10 feet high by 2.5 feet wide (at the mouth) and has an angle of flare of 20 degrees in the direction of height. At a wave length of 50 centimeters this corresponds to a height of 6 wave lengths and a width of 1.5 wave lengths. The theoretical power gain compared to a Hertzian doublet is 50. The exciting antenna is a straight wire about one-quarter wave length long placed in the throat. Figure 6 shows measured radiation patterns of this horn in the vertical and in the horizontal planes under substantially free-space conditions. The vertical pattern is notably free from secondary lobes. The horizontal pattern has been made sharp and the secondary lobes have been tolerated for sake of power gain. The preferred pattern in this plane, were less power gain satisfactory, would have a single broad lobe. A beam of this character could be obtained by reducing the width of the horn.

The next step was to determine the best position of the horn with respect to the ground for the production of a landing beam. Patterns were therefore calculated for different heights of the horn above ground, for different angles of the axis of the horn with the horizontal, for perfect dielectric and perfectly conducting earths, for horizontal and for vertical polarizations, and for different sizes of horns. Examples of these patterns are shown in figure 7. Measurements of radiation patterns were made with the aid of captive balloons. These measurements agreed well with the theoretical predictions. From these calculations and measurements it appeared that the disposition of two identical horns for the glide path beams might be as follows: the lower edges of the mouths of both horns touch the ground, with one horn at an angle of elevation of five degrees, and the other at an angle of ten degrees. With this disposition, additional sharpness of the lower beam is obtained at present by helpfully utilizing the reflections from the ground. When transmitting with equal strength, these two horns should yield a glide path angle of about six degrees; a smaller angle could be obtained by reducing the power output or the modulation of the lower horn. A pair of horns of the type shown in figure 5 was set up in the field in accordance with this information. These radiators were fed from separate triode oscillators operating at slightly different frequencies and modulated at 90 and 150 cycles per second respectively. Balloon measurements of the resultant equisignal straight-line glide path were then made over a distance of 700 feet with highly satisfactory results as indicated in figure 8. Its angle with the ground was adjustable at will to values between 6 and 11 degrees by adjusting the relative sensitivities of the 90- and 150-cycles-per-second audio channels of the receiver.

Excitation of Beam Radiators

In the equisignal beam tests separate transmitters operating at slightly different carrier frequencies were used to excite the two horn radiators in order to avoid space interference of the carriers and to obviate any difficulty of cross modulation. Cross modulation would cause a broadening of the on-course or equisignal zone. Cross modulation through a common power source could be avoided by the use of buffer amplifiers, or by a bridge connection[5] of the two output lines to a single power source. Numerous tests have established that there is no appreciable mutual impedance effect between the horn antennas employed here. In the absence of phase modulation, space interference between the two radiated carriers occurs if both radiators are fed from a common source. Such interference does not take place between the two pairs of side bands for they are of different frequency. The two horns used in these tests were separated in space by several wave lengths. Strong lobes and zeros might therefore have been produced in the horizontal radiation pattern had a common modulation source been used. In some systems this trouble is avoided by juxtaposition of the antennas in space; in others it is probable that some reduction occurs from phase modulation introduced by mechanical modulation. It is expected that further investigation will establish that a single source may be successfully used in the specific system described in this paper. The use of separate oscillators is open to objection principally because it doubles the transmitting apparatus.

For most of the experimental investigations carried on during this research,

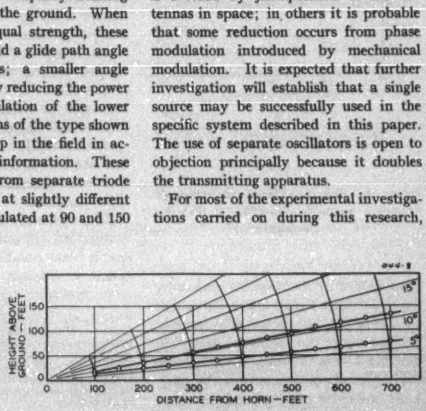

Figure 8. Balloon measurements of equi-signal paths. The two paths correspond to different adjustments of the audio channels of the receiver

simple low-powered oscillators[6] were employed as sources. These oscillators used the Western Electric 316-*A* triode in a feedback circuit with short-circuited coaxial transmission lines as circuit elements. They operated at wave lengths from several meters down to the limit of oscillation of the tube (about 40 centimeters) delivering about five watts at 50 centimeters and about one watt at 42 centimeters. These small amounts of power when used with sharply directive horn radiators have proved sufficient both for measurement of radiation characteristics with very simple detectors and for flight tests. Modulation of the radio-frequency output was accomplished by inserting in series with the direct plate voltage an audio-frequency voltage obtained from a vacuum-tube oscillator-amplifier unit. When overlapping radio beams were used for production of glide-path signals, the oscillators were modulated at 90 and 150 cycles per second respectively. Voltage-regulated power supplies in both plate and filament circuits of the oscillators were necessary for the production of stable signals. With constant voltages applied to the oscillators and with reasonable care in their adjustment, however, the frequency drift was small enough so that the oscillators would operate for several hours without retuning them or the receiver.

The first application of the "klystron" outside the Stanford Laboratories was made in this research through the courtesy of the Sperry Gyroscope Company, Incorporated, Stanford University, and the United States Army. Oscillators of this general type[7] are capable of producing several hundred watts of power at wave lengths in the neighborhood of 40 centimeters with an efficiency considerably greater than that of ordinary vacuum-tube circuits operating in the same wave-length range. Modulation of the klystron oscillator was accomplished either by variation of the voltage accelerating the electron beam, or by modulation of the radio-frequency output in the transmission line connecting the klystron to its load.

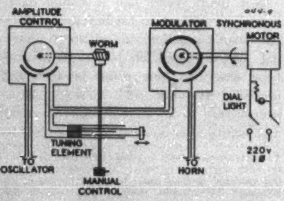

Figure 9. Schematic diagram of transmission-line modulator

Figure 10. Triode grid-leak detector with cover removed

For modulation in the transmission line, a mechanical modulator of the type shown in figure 9 was developed. The operation of this type of modulator is based on the fact that a variable capacitor inserted in series with the center conductor of a coaxial transmission line may be used to control the power transmitted through the line. A capacitor is rotated at such a speed that its reactance is varied at the desired modulation frequency, thus modulating the power delivered to the load. A second series capacitor, adjusted manually, serves as an amplitude control to determine the average amount of power transmitted through the modulator. An additional control is provided in the form of a short-circuited coaxial transmission line of adjustable length, connected across the transmission line between the amplitude control capacitor and the modulating capacitor. When correctly adjusted, the modulator will produce very nearly sinusoidally modulated signals with a modulation factor of about 0.95.

The Airplane Receivers

A preliminary calculation showed that a power of less than one watt delivered to an antenna with a power gain of 30 would produce a signal of one millivolt per meter at seven miles if free-space conditions prevailed. This is a fairly strong signal even for simple superheterodyne receivers. With a receiver of this sensitivity adapted for 600 megacycles, ranges of about five miles should be possible with at least one currently available triode (Western Electric 316-*A*). Since a power gain of 50 is provided in the actual antenna, distances of five to ten miles should therefore be expected. On the other hand, if a great deal more power is available at the transmitter, much simpler receivers should prove satisfactory.

The ultrahigh frequencies (approximately 700 megacycles) used necessitated the development of special radio receiving equipment for use in the airplane. The first receiver constructed was a triode grid-leak detector followed by an audio amplifier. A photograph of this unit with the cover removed is shown in figure 10. This simple receiver was satisfactory for use with a high-powered transmitter such as the klystron, but experience with it demonstrated that greater sensitivity and greater freedom from vibration effects would be very desirable. A novel superheterodyne receiver which fulfilled both of these requirements was therefore constructed.

In this superheterodyne receiver the 700-megacycle signal is fed directly into the converter which mixes the incoming signal with the third harmonic of the local oscillator and produces a 10-megacycle beat-frequency signal. This beat frequency is fed to the intermediate-frequency amplifier where it is amplified with no greater difficulty than any other 10-megacycle signal. The converter uses a diode and a combination of lumped and distributed circuit elements that are tuned to the frequencies of the 700-megacycle signal, the 230-megacycle local oscillator, and the 10-megacycle intermediate-frequency amplifier. An equivalent circuit diagram of the converter is shown in figure 11. The circuits *A*, *B*, and *D*, are all connected in series with the diode in such a manner that a voltage appearing in any one of them is impressed across the diode substantially independently of those appearing in the others. This independence is accomplished by making use of the difference in impedance of the

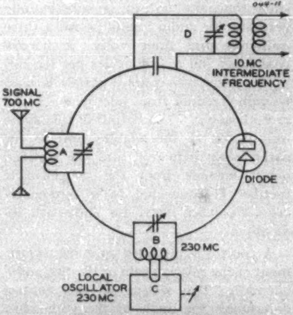

Figure 11. Equivalent circuit of converter of the superheterodyne receiver

three tuned circuits at widely separated frequencies. The local oscillator voltage is fed into the 230-megacycle circuit, *B*, and the diode generates in itself the third harmonic needed to mix with the 700-megacycle signal from circuit *A*. The special diode (*D*157653) used in this circuit was made available to us by the Western Electric Company.

The local oscillator *C* is of the type developed by A. P. G. Peterson[8] with the exception that the negative high-voltage lead is grounded to make possible the installation of the oscillator on the receiver chassis. The tube used is an RCA 955. The oscillator frequency is approximately 230 megacycles, adjustable over a small range. This frequency is low enough so that relatively stable oscillations may be obtained. Since the oscillator frequency is so far removed from that of the incoming signal, there is no danger of the oscillator synchronizing or locking-in with the received signal.

The 10-megacycle intermediate-frequency amplifier consists of two 1852 stages feeding a 6*SQ*7 second-detector audio-amplifier tube. A small amount of automatic volume control is incorporated in this amplifier, which has a gain of roughly 5,000 and a band width of approximately 300 kilocycles.

An elaborate audio-frequency amplifier follows the superheterodyne receiver proper and incorporates four stages of resistance-coupled amplification with automatic volume control, a separating filter for 90 and 150 cycles per second, separate one-stage amplifiers following the filter, and a copper-oxide rectifier system to feed a d-c indicating meter. The audio automatic volume control was provided mainly to allow the use of this amplifier with the grid-leak detector unit which has no automatic volume control of its own. The regulation is such that the output is substantially constant for audio inputs between three millivolts and three volts. A narrow pass band (50 to 400 cycles per second) in the resistance-coupled stages is used to gain freedom from microphonics and motorboating. The output of this audio unit is fed to the adapter unit of the cathode-ray indicator, and the rectified direct current from the copper-oxide rectifier is connected to a zero-center crossed-pointer meter for glide-path indication.

A control is provided to allow adjustment of the relative gain of the two audio channels. The setting of this control determines the angle of the glide path with respect to the ground. This control may be set so that the approach path angle is correct for a given type of airplane. This arrangement does not require a readjustment of the ground station to set the glide angle.

Figure 12. Complete superheterodyne receiver

The complete superheterodyne receiver was mounted on a plywood board supported by double shock-proof mountings—an arrangement which was fairly effective in suppressing vibration. A photograph of the equipment as it was installed in an airplane for flight tests is shown in figure 12.

The antenna was a coaxial type[9] connected to the receiver by a one-quarter-inch copper coaxial line. A short section of the line next to the receiver was made flexible to prevent transmission of mechanical vibration.

Flight Tests

Exploratory flight tests of a beam from a single horn were made at the Boston Municipal Airport mainly to determine workable range with a klystron power source and the simple receiver of figure 10. These tests also demonstrated that a substantially straight constant-intensity glide path could be realized by means of a single-horn radiator. Signals were received up to a distance of eight miles and simulated instrument landings were made repeatedly on this beam by the constant-intensity method over distances of about four miles.

Flight tests of the complete system were made in September 1939.* Two horns, energized by separate low-power triode oscillators, were used to provide the overlapping-beam pattern. Utilizing the superheterodyne receiver and the instrument already described, a straight-line landing path at an angle of about three degrees with the ground was flown consistently over a distance of somewhat more than five miles. These tests established the straightness of the microwave path over its entire length, and the feasibility of using the cathode-ray indication and three-light illusion as initially contemplated.

At this same time a klystron was again made available. With an estimated power of 50 watts supplied to one of the horns and using the superheterodyne receiver, flight tests demonstrated that extremely strong signals were received at a distance of 25 miles and an altitude of 2,500 feet. This performance leads us to believe that usable landing signals may be obtained at distances of the order of 50 to 75 miles.

Conclusions

In the course of the solution of the problem set forth at the beginning of this paper a landing system has been evolved embodying both a unique indicating instrument and landing path. The two elements, although used in combination in the system described, can also be applied independently. For example, the instrument may be used with constant intensity glide path and the straight-line glide path may be used with a conventional crossed-pointer instrument.

The landing path described in this paper marks a significant advance not simply because it is straight, and of an equisignal nature, but because it demonstrates a means by which a path essentially free from the effects of ground and surrounding objects may be realized. In this respect also the system describes an advance over conventional ones.[10]

The radio beam being thus substantially free from the effects of its surroundings makes the landing path reproducible and therefore makes the system truly portable.

The beams used for this landing path have other applications. For instance they may be utilized as vertical markers† where they are of value because of their extreme sharpness.

Not many years ago it would have been improvident to predict that within one year a practical 50-centimeter landing system based on the experimental type described here could be installed for pilot training, and it would have been heresy to say that within two years a 10-centimeter system could be made available for this purpose, yet now such is our confidence that on the basis of contemporary developments in the industry and elsewhere these predictions may now be made with reasonable assurance of fruition.

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* The system as projected contemplated the use of centimeter waves for both pairs of beams. It was felt however that a standard localizer could also be utilized. This was done in these tests for which an Airtrack localizer was lent and operated by the Washington Institute of Technology.

† In this research horn radiators were used as markers both singly and in pairs. It is safe to say that the radiated pattern may be made as sharp as necessary by suitable design. The use of the modulated cross beams yields an approach signal, an instantaneous "over the marker" signal, and a departure signal.

In conclusion we wish to acknowledge the interest of the United States Army and the industry in this project. The Army made itself familiar with the work not long after its inception and has since, through co-operation, confirmed many of our observations. This interest can, we think, be taken not only as an index of the importance of centimeter-wave technique in air navigation but also as a recognition of the possibilities of research on critical problems through the co-operation of the government and the industry with technical institutions such as the Massachusetts Institute of Technology.

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Discussion

For discussion, see page 1120.

Compressed Powdered Molybdenum Permalloy for High-Quality Inductance Coils

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MEMBER AIEE

F. J. GIVEN
ASSOCIATE AIEE

Synopsis: Molybdenum-Permalloy is now produced in the form of compressed powdered cores for inductance coils. Its high permeability and low losses make possible improved quality or decreased size without sacrificing coil performance. Its low hysteresis loss reduces modulation enough to permit application where large air-core coils would otherwise be required.

THE introduction of loading coils in the telephone system at about the turn of the century brought special demands on magnetic and electrical properties of core materials, and set in motion investigations which have had wide influence on the theoretical and practical aspects of ferromagnetism. The first step in this development led to cores of iron wire, which sufficed for loading coils on circuits of moderate length.[1] With the development of telephone repeaters and the extension of circuits to transcontinental length some 25 years ago, there arose need not only for loading coils, but also for network coils, which would have high stability with time, temperature, and accidental magnetization. Magnetic stability was at first secured[2] by employing iron-wire cores provided with several air gaps. Later, commercial and technical considerations led to a core structure made from compressed insulated powdered material, first electrolytic iron[3] and later Permalloy powder.[4] This type of core is mechanically stable; it introduces in an evenly distributed fashion the requisite air gaps, while avoiding undesirable leakage fields; and it subdivides the magnetic material so as to reduce eddy-current losses. Although other means have been suggested,[5,6] no way has yet been devised which provides these features so well and at so low a cost as the compressed powdered type of core.

Loading-coil cores made from electrolytic-iron powder generally satisfied the stability requirements for long lines, but on account of their low magnetic permeability they were large and costly. The search for materials with higher permeability and lower hysteresis loss led to Permalloy[7] which, by 1925, had been produced in powdered form and fabricated into cores. This development provided coils for voice-frequency applications (loading coils and filter coils) which were cheaper and yet superior electrically to those made from electrolytic iron. These coils became available at a time when the telephone plant was undergoing a very large extension of loaded cables. As a result, large economies in cost and space were realized in the more than 6,000,000 coils involved in this plant expansion.

Iron powder, and later Permalloy powder, ground to a finer size and diluted to lower permeability than used in loading coils, also found application in coils for oscillators, filters, and networks of multiplex carrier telephone and telegraph systems employing frequencies up to 30 kilocycles[8] and in receivers for transoceanic radiotelephone communication employing frequencies up to approximately 60 kilocycles.[9] Permalloy powder improved the electrical characteristics—particularly modulation—of coils for use in high-frequency circuits, because of its low hysteresis losses.

Continued research for a powdered material having still better intrinsic properties has recently made available new compressed powder cores which permit further important gains in coils for voice-frequency circuits and in coils for high-frequency carrier system applications. The latter take on considerable significance at this time because they play an important part in making practical for commercial use the new broad-band carrier telephone systems intended for use on existing open wire and cable lines and on new types of cable. Again, therefore, the advent of a new core material is well-timed to be of assistance in further growth of the telephone system.

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[1] For all numbered references, see list at end of paper.

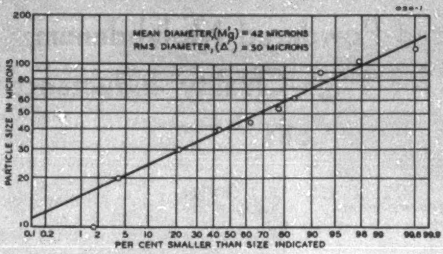


Figure 1. Distribution of particle size of 120-mesh powder, by weight

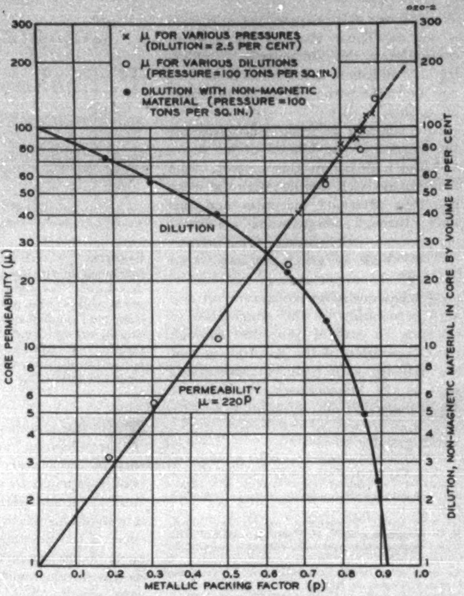


Figure 2. Relation between metallic packing factor, permeability, and percentage dilution

The development of this core material was based on the discovery[10] that the addition of a small percentage of molybdenum to Permalloy increases its permeability and electrical resistivity, and decreases its eddy current and hysteresis losses. Decreased losses are necessary for improvements and economies for both voice-and carrier-frequency operation. The increased permeability of this alloy is essential for the improvement of voice-frequency coils. It is readily reduced to the proper values for high-frequency coils by diluting the powdered magnetic material with insulating material before compressing into core form. In this development many problems of alloy embrittlement, pulverization, insulation, and heat treatment had to be solved both on a laboratory and factory scale. The alloy composition finally selected as giving the best combination of desirable properties, contains approximately 2 per cent molybdenum, 81 per cent nickel, and 17 per cent iron, and is designated as 2–81 molybdenum-Permalloy. This new alloy is manufactured commercially by the Western Electric Company for use in loading coils and filter coils.

Physical and Magnetic Characteristics

The raw materials and necessary embrittling agents[11] are melted together and cast into ingots which are rolled to develop the desired grain structure. The density of this alloy is 8.65 grams per cubic centimeter. The brittle material is pulverized to the desired fineness and finally annealed to soften the alloy particles before insulation and pressing into core form.

The distribution by weight of the particle sizes of a sample of 120-mesh powder is given in figure 1, showing a root-mean-square size of 50 microns.[12] Since the effective resistance of a coil due to eddy-current losses in its core is proportional to the mean-square particle diameter[13] it can be decreased when desired by the use of more finely pulverized material.

The problem of insulating 2–81 molybdenum-Permalloy powder is to coat the particles with a minimum thickness of a material which will not break away during the pressing operation, which will not fuse and flux the magnetic particles together during the core heat treatment, which will prevent the flow of eddy currents between metallic particles, and which will be chemically inert throughout the lifetime of the magnetic core. The difficulty of the problem will be appreciated from the fact that the separation between adjacent particles of a core of 125 permeability is approximately equal to the wave length of visible light (0.5 micron). This thickness of insulating film may be shown to be approximately $rt/300p$, where r is the percentage of insulating material by volume, p is the packing factor of the magnetic material, and t is the rms particle diameter (assuming spherical particles).

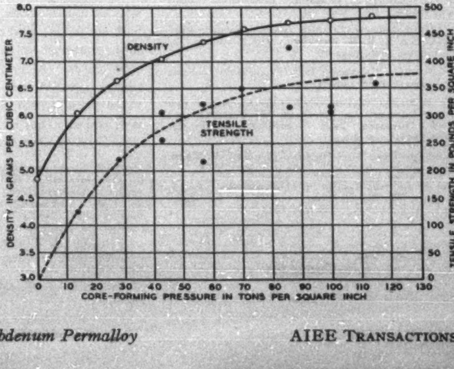


Figure 3. Effect of core forming pressure on density and tensile strength

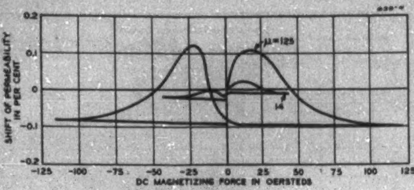


Figure 4. Residual effect of d-c magnetization on initial permeability—measured three minutes after release of direct current

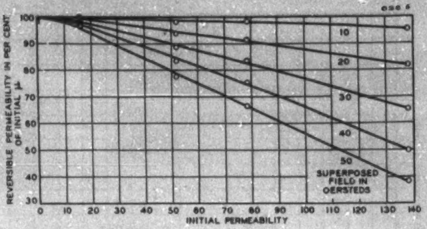


Figure 5. Effect of superposed magnetization on permeability

A new type of ceramic insulation has been introduced with these cores which fulfills the above requirements and which is more inert than the previous type. This new insulating material is free from water-soluble residue. It thus eliminates the final washing treatment which was required with the earlier type.

For applications where a low permeability is desired, nonmagnetic powder is added to further dilute the magnetic material. The permeability of the finished core depends largely on the quantity, particle size, and thoroughness of admixture of nonmagnetic powder. Various attempts to derive theoretical relations between core permeability and dilution have been made,[14,15] but they generally fail in some detail. An empirical representation of this relationship is found to be

$$\mu = \mu_i^p$$

or

$$\log \mu = p \log \mu_i,$$

where μ_i is the intrinsic permeability of the magnetic material, and p is the packing factor, or fraction of the core volume occupied by magnetic material. This equation is found to be valid for a wide range of dilution, effected either by adding insulating material or by reducing the load during core compression. However, the intrinsic permeability must be determined experimentally for each type of particle, size distribution, method of admixture of nonmagnetic powder, and annealing process. Figure 2 shows curves of permeability and percentage diluting material versus metallic packing factor. A permeability of 125 has been selected for most loading-coil cores, while permeabilities of 14 and 26 have been chosen for two important types of high-frequency filter coils.

A pressure of 100 tons per square inch is employed in forming molybdenum-Permalloy cores, to attain proper density and mechanical strength. The effect of pressure on core density and strength is shown in figure 3 for cores having 2.5 per cent dilution. The tensile strength of diluted cores is decreased somewhat; for example, cores with 25 per cent nonmagnetic materials have a tensile strength of about 250 pounds per square inch.

In annealing the compressed cores to remove stresses incident to pressing, it has been found that the insulating material remains intact at a considerably higher temperature if oxygen is excluded. An improved annealing treatment has therefore been introduced by which the cores are heated in an atmosphere of hydrogen, with the attainment of high core permeability and low hysteresis loss. The ability of the insulating material to withstand such a heat treatment testifies to its extreme stability and recommends it in preference to organic materials.

An essential core requirement of precision inductance coils is that the permeability remain unaltered during the life of the coil. The greatest difficulty with cores having no air gaps is the large and more or less permanent shift of permeability due to accidental strong magnetization. Such variations have recently been overcome to a degree in continuous

Figure 6. Permeability-induction characteristics

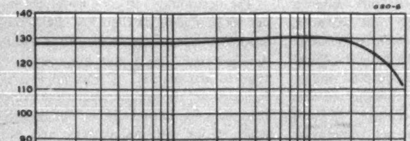
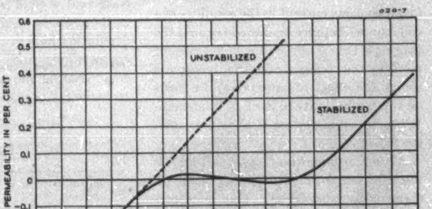


Figure 7. Effect of temperature on permeability of stabilized and unstabilized cores



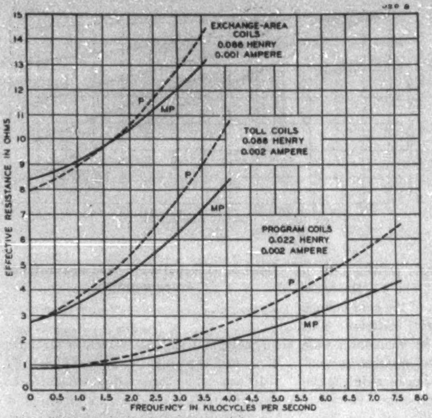


Figure 8. Effective resistance-frequency characteristics of typical loading coils

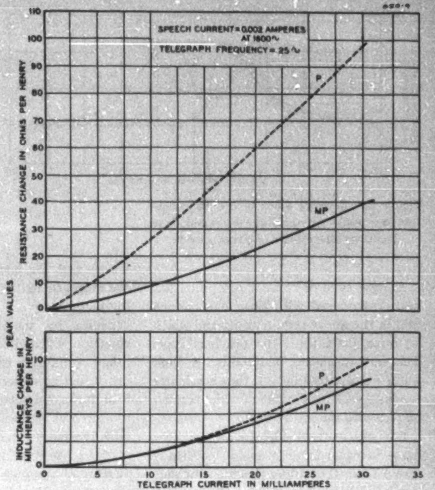


Figure 9. Flutter characteristics of typical toll loading coil

P—With Permalloy core
MP—With molybdenum-Permalloy core

cores made of hard rolled nickel-iron alloy sheet,[8] but they have been found to be rather large immediately after strong magnetization, decreasing slowly to tolerable limits only after two or three days. With compressed powdered molybdenum-Permalloy cores, permeability shift due to strong magnetization is remarkably small, even within a fraction of a minute after the magnetization is released, and any further drift of permeability with time is negligible. In typical cores of the new material, the shift in permeability after strong magnetization is less than 0.2 per cent for cores of permeability 125, and less than 0.05 per cent for cores of permeability 14. Figure 4 shows the residual effect of the application and removal of various magnetizing forces on cores of both these permeabilities.

When a direct current is superposed on an alternating current in the windings of a coil, the inductance is altered because the magnetic field set up by the direct current modifies the core permeability. Figure 5 shows the effect of superposed d-c fields on the permeability of 2–81 molybdenum-Permalloy powder cores of various permeabilities.

A further important core property is the constancy of permeability with respect to flux density *B*. This is of particular importance in precision filters, to insure that changes in transmission level do not produce serious alterations in the frequency discrimination characteristics. Figure 6 shows the superiority of the new material over the earlier Permalloy.

A new requirement for cores has been introduced by quartz-crystal filters used in wide-band carrier systems. In order to secure the necessary precision in this type of filter, measures have to be taken to prevent departures from the initial frequency adjustment due to changes in core permeability ordinarily occurring with room-temperature changes. Extremely small temperature coefficients of permeability have now been achieved by adding to the new 2–81 molybdenum-Permalloy powder a very small percentage of special Permalloy powder having a molybdenum content of about 12 per cent. Such an alloy has a nonmagnetic or Curie point close to room temperature, and for a small temperature range just below its Curie point, it has a negative temperature coefficient several hundred times as large as the positive coefficient of 2–81 molybdenum-Permalloy. By choosing suitable compositions and percentages of such compensating alloys, the net temperature coefficient of permeability of a core can be adjusted to any reasonable value, positive or negative, over a desired temperature range. Figure 7 shows a permeability versus temperature curve for a core stabilized to give a small negative coefficient, compared to a similar curve for a core not stabilized.

Core Losses

The desirability of core materials increases in general as their loss characteristics decrease. Low total eddy-current and hysteresis losses give low contributions to attenuation. Hysteresis loss is frequently of especial importance because it appears fundamentally as a resistance which varies with coil current, and because it incidentally generates harmonic

Table I. Loss Coefficients of Powdered Core Materials

Material	μ	Hysteresis $a \times 10^6$	Hysteresis $\mu a \times 10^3$	Residual $c \times 10^6$	Residual $\mu c \times 10^3$	Eddy Current $e \times 10^9$	Eddy Current $\mu e \times 10^6$
Grade *B* iron	35	49	1.7	109	3.8	88	3.1
Grade *C* iron	26	81	2.1	139	3.6	31	0.8
81 Permalloy	75	5.5	0.41	37	2.8	51	3.8
	26	11.5	0.30	108	2.8	27	0.7
2–81 molybdenum-Permalloy	125	1.6	0.20	30	3.8	19	2.4
	26	6.9	0.18	96	2.5	7.7	0.2
	14	11.4	0.16	143	2.0	7.1	0.1

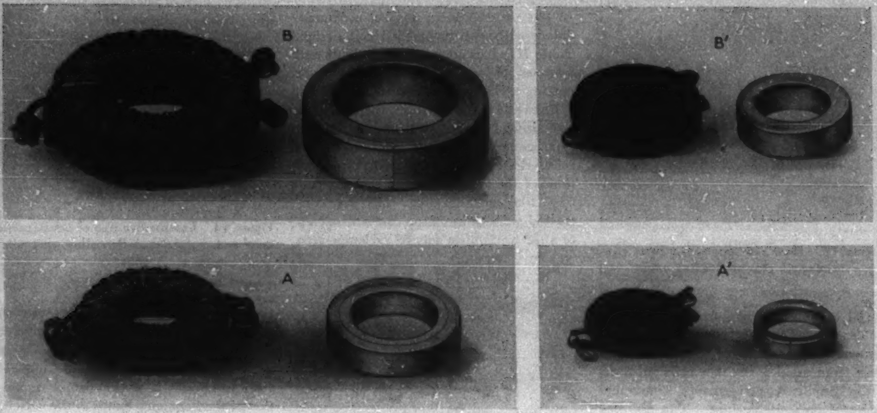


Figure 10. Comparative sizes of the new molybdenum Permalloy (A,A′) and the superseded Permalloy (B,B′) cores and coils

Left—Program-circuit loading coils
Right—Exchange-area loading coils

voltages. A low value of hysteresis loss thus simplifies circuit problems arising from resistances which vary with energy level, and it avoids troublesome modulation conditions.

The total resistance per unit inductance arising from eddy-current and hysteresis losses may be expressed as[16]

$$R_m/L=\mu(aB_m+c)f+\mu ef^2$$

where the symbols are as given in the appendix.

Table I gives the loss coefficients for various core materials, and for 2-81 molybdenum-Permalloy insulated to several permeabilities. The low loss coefficients of the new material as compared with the best previous materials are of importance from two standpoints. First, core permeabilities as much as 50 per cent greater can be now utilized in coils without increasing the total core-loss resistance. Second, by utilizing the same permeabilities, core-loss resistances about 60 per cent smaller can be obtained.

In many coil-design problems, harmonic generation or modulation assume controlling importance. The modulation factor *m* which denotes the ratio of the generated third harmonic to the applied voltage may be expressed as follows:[17]

$$m=E_3/E_1=3\mu aB_m/10\pi$$

The low values of *a* obtained with the new material yield values of *m* that are about 6 decibels and 20 decibels lower than possible with powdered Permalloy and electrolytic-iron cores, respectively.

The wide range of core permeability available with this new material permits a ready choice of the proper values of permeability and core size to suit any particular needs. In the usual design problem the following main requirements must be considered, in addition to providing the desired inductance.

1. D-c resistance, R_c
2. Coil quality factor, $Q = \omega L/(R_c+R_m)$
3. Modulation factor, *m*
4. Coil size (which depends directly on core size)

Table II. Comparative Size and Weight Data of Typical New and Superseded Coils

Type of Coil	Type of Compressed Powdered Core	Inductance (Henrys)	Coil Volume (Cubic Inches)	Coil Weight (Pounds)
Small exchange area	Permalloy	0.088	2.5	0.4
	Molybdenum-Permalloy	0.088	1.5	0.2
Program circuit	Permalloy	0.022	11.8	1.6
	Molybdenum-Permalloy	0.022	4.4	0.6
Toll-side circuit	Permalloy	0.088	13.5	1.7
	Molybdenum-Permalloy	0.088	5.1	0.7

These requirements cannot be satisfied independently however, as fixing any two of them automatically fixes the values of the others. In each case, a particular value of core permeability is required for the proper fulfillment of the conditions. The appendix lists the formulas essential to the determination of these factors. Although these formulas imply an entire freedom of choice of core permeability and size, it becomes necessary for practical purposes to standardize on a limited number of values of permeability and a limited number of sizes of core. It is possible by the proper choice from these types to approach rather closely to an ideal solution for each problem.

Improved Designs of Loading Coils

A study of alternate ways of utilizing the advantages offered by the new ma-

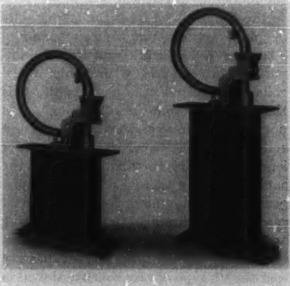


Figure 11. New and superseded cases containing 200 exchange-area coils

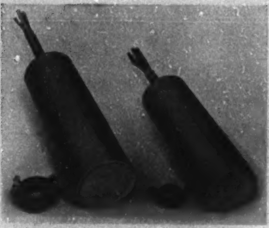


Figure 12. New and superseded cases containing six program-loading coils

terial in coils for voice-frequency loaded cables showed that the greatest immediate benefit to the telephone plant would accrue from making the new coils substantially duplicate performance characteristics of previous designs. The new designs chosen are in fact better in most respects and have been made approximately 50 per cent smaller in volume by using a molybdenum-Permalloy core with a nominal permeability of 125. Table II summarizes data comparing the electrical characteristics, sizes, and weights of coils commonly used on exchange and toll cables. Figure 8 shows the improved resistance-frequency characteristics for typical coils. Figure 9 shows the improved telegraph flutter[18] characteristics of a commonly used toll-type coil.

Figure 10 pictures the reduction in size of cores and coils which are commonly used in toll and exchange area circuits. In the preparation for commercial manufacture of the smallest of the new coils, a difficult problem in the development of winding machinery was involved because of the small dimensions of the hole in the finished coil. This problem has been successfully solved by the Western Electric Company.

Aside from manufacturing economies, the reduction in coil size is of importance to the Bell System from the standpoints of plant construction and installation. The size reduction is particularly important in instances where only a few coils are required at a given point, as is the case in program-circuit loading. It is now practicable to enclose as many as six coils, even of the larger size employed for loading program circuits, directly in the cable splice at a loading point. This dispenses with the need for conventional cases, and reduces both manufacturing and installation costs. In the field of small complements using the inexpensive lead-sheath type of case construction, it is now possible to furnish as many as 100 of the small exchange area coils whereas 15 was the maximum number accommodated with the superseded coil design. Table III gives comparative weights and volumes of typical cases provided for potting exchange area and toll-type coils.

Figures 11 and 12 show the comparative sizes of typical steel and lead-sleeve type cases for the new and superseded coils. In figure 13, the midget proportions of the latest design of case for potting 100 exchange-area coils are contrasted with those of the case utilized up to 1922 containing only 98 exchange-area coils. The reduction in size of cases for this type of coil is of particular importance in larger cities where underground vault space is at a premium.

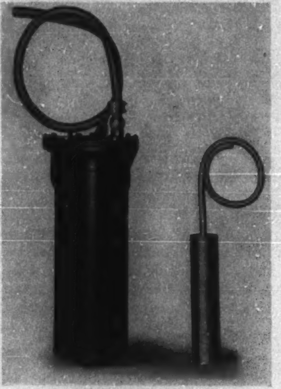


Figure 13. Comparative size of equivalent 1939 and 1922 cases

Improved Inductance Coils for Filters

The trend of development of toll transmission circuits is now very definitely toward multiple-channel carrier systems utilizing a much wider frequency band than has heretofore been employed on wire circuits.[19,20] These systems involve extensive use of selective or equalizing networks at terminals and at repeater stations in order to obtain proper separation of the frequency bands of the various channels or to insure suitable transmission properties of the individual channels. While these networks involve coils, capacitors, and crystals, it is frequently the case that their size, cost, and performance are determined chiefly by the quality factor Q of the inductance coils. This follows from the fact that Q values of coils are usually considerably lower than those obtainable readily in capacitors and crystals. Accordingly, it is very desirable to have as high a value of Q as possible economically. In addition, such coils must have low hysteresis resistance to limit modulation, and a low temperature coefficient of inductance to secure stability of attenuation or impedance characteristics of the filters and networks.

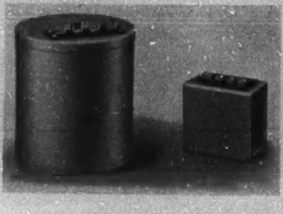


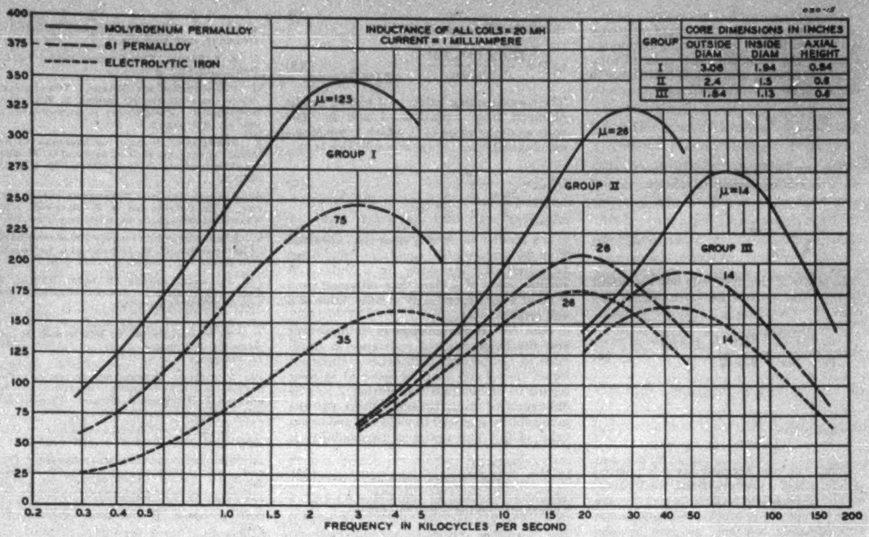
Figure 14. Comparative size of nonmagnetic core and molybdenum-Permalloy core filter coils

Due to the improvements in these respects, molybdenum-Permalloy core coils can be used quite extensively in new types of carrier telephone systems. In such systems for existing lines and cables, as well as for projected new types of cables, those filters are of key importance which separate individual message channels in the frequency range from 3 to 108 kilocycles. By using coils of powdered molybdenum-Permalloy insulated to permeabilities of 14 or 26, valuable economies in space and cost of filters are

Table III. Comparative Data on Representative Cases for New and Superseded Loading Coils

Type Cable	Size of Complement	Type Coil or Unit	Approximate Volume (Cubic Feet)	Approximate Weight (Pounds)
Exchange area	200	Permalloy	1.7	480
		Molybdenum-Permalloy	0.8	350
Program-toll	6	Permalloy	0.15	70
		Molybdenum-Permalloy	0.11	50
Toll	50 units*	Permalloy	5.5	1,350
		Molybdenum-Permalloy	3.5	950

* A unit consists of one phantom and two side-circuit coils.



GROUP	CORE DIMENSIONS IN INCHES		
	OUTSIDE DIAM	INSIDE DIAM	AXIAL HEIGHT
I	3.06	1.94	0.84
II	2.4	1.5	0.8
III	1.84	1.13	0.6

Figure 15. Comparative Q-frequency characteristics on typical filter coils using new and superseded materials

realized.[21,22] Figure 14 shows a typical coil employing a 14 permeability core designed for use in one of these channel filters having its transmitted band in the vicinity of 108 kilocycles, together with a shielded solenoidal air-core coil which might be employed for the same purpose. The molybdenum-Permalloy coil has a Q at 100 kilocycles about twice that of the air-core coil, yet it occupies approximately one-tenth as much space. The third-order modulation products are approximately 80 decibels below the level of the normal channel currents. This is considered to be tolerable from the standpoint of interchannel cross talk on circuits used for one-way transmission. An inductance-temperature coefficient of about -20×10^{-6} per degree Fahrenheit has been chosen to compensate for the positive capacity-temperature coefficient of associated capacitors.

In figure 15 data are presented illustrating the Q-frequency characteristics that can be obtained on typical coil designs using the new material in the frequency range from 300 cycles to 200 kilocycles. The characteristics shown apply to coils wound on three sizes of cores that are suitable for use in this range. For comparison, similar characteristics are also included for coils using cores of equal size but made of Permalloy and electrolytic-iron powder. These data include all effects on Q resulting from winding capacities and losses, which have been made tolerably small by suitable choice of insulating materials, stranding of conductor, and configuration of winding.

Conclusion

Compressed powdered cores of 2–81 molybdenum-Permalloy have properties which are superior to those of earlier powdered cores in respect to permeability range, hysteresis loss, and eddy-current loss. Because of the lower losses and greater permeability range, inductance coils are now possible which have greatly increased Q values for a given volume. Because of the low hysteresis losses and attendant lowering of modulation effects to tolerable levels, magnetic-core inductance coils can now be employed where nonmagnetic core coils have previously been necessary, with a very great increase in Q values for a given volume. Temperature coefficients can now be obtained which are equal to the temperature coefficients of other high-grade electrical elements such as mica capacitors and quartz crystals. Moreover, the ability to make the temperature coefficient of coils negative or positive at will permits the attainment of remarkable stability in resonant combinations of coils and capacitors Two important applications have been made in the field of communication apparatus. For voice-frequency circuits, new loading coils of improved quality and reduced size have been standardized. For broad-band carrier systems, the compactness of channel separating filters is largely due to the volume economies introduced by molybdenum-Permalloy coils.

Appendix

The following formulas illustrate the essential steps in selecting the size and permeability of an annular core best suited for a coil having a desired set of characteristics. For sake of simplicity, they assume throughout a coil of arbitrarily chosen proportions, in which all dimensions bear fixed ratios to the mean core diameter, approximating those that are practicable from a manufacturing standpoint. The core is assumed to be rectangular in section. It is assumed that wire of any diameter can be used and that the winding efficiency is independent of the wire diameter. Any inductance due to air space outside of the core is neglected. Because of these simplifications, the expressions are of somewhat restricted applicability. However, they yield solutions for optimum permeability and corresponding values of Q and core size which are sufficiently accurate for most practical purposes.

The inductance in henrys due to a core of permeability μ, and mean diameter d centimeters, wound with N turns of wire is

$$L=\frac{3}{8}N^2\mu d\times10^{-8} \qquad (1)$$

If the coil is wound with wire of resistivity ρ_c ohm-centimeter, with winding efficiency s (that is, the ratio of copper area to total available winding area), the d-c resistance in ohms will be

$$R_c=\frac{19\rho_c L\times 10^9}{s\mu d^2} \tag{2}$$

The maximum flux density due to sine-wave measuring current of effective value I amperes is

$$B_m=\frac{8I}{5}\sqrt{\frac{\mu L\times 10^9}{3d^3}} \tag{3}$$

In terms of the hysteresis loss coefficient $k_2=\mu a$, the modulation factor thus becomes[17]

$$m=\frac{E_3}{E_1}=\frac{3k_2B_m}{10\pi}=\frac{4k_2I}{25\pi}\sqrt{\frac{3\mu L\times 10^9}{d^3}} \tag{4}$$

or

$$d^3=3\mu L\times 10^9\left\{\frac{4k_2I}{25\pi m}\right\}^2 \tag{5}$$

When the core permeability μ is reduced by dilution of a given material, the hysteresis and residual loss coefficients a and c vary so as to make the products $\mu c=k_1$ and $\mu a=k_2$ approximately constant, as may be seen by reference to table I. The core-loss resistance in ohms at frequency f cycles per second may therefore be expressed with reasonable accuracy as

$$\begin{aligned}R_m&=Lf(k_1+k_2B_m+\mu ef) \qquad (6)\\&=Lf\left(k_1+\frac{10m\pi}{3}+\mu ef\right)\end{aligned}$$

where the eddy-current coefficient e depends upon the particle diameter t, and the alloy resistivity ρ, being proportional[18] to t^2/ρ.

The coil quality factor is thus

$$\begin{aligned}Q&=\frac{2\pi fL}{R_c+R_m}\\&=\frac{2\pi f}{\dfrac{19\rho_c\times 10^9}{s\mu d^2}+f\left(k_1+\dfrac{10m\pi}{3}+\mu ef\right)} \qquad (7)\end{aligned}$$

Case I. If the value of m is fixed and d and R_c can be freely chosen, it is desirable to know the value of μ which will yield the highest possible value of Q. By substituting in (7) the value of d^2 obtained from (5) and setting the derivative with respect to μ equal to zero, the following is obtained for the optimum permeability:

$$(\mu')^8=\frac{52.5\times 10^{18}\rho_c^3m^4}{e^3s^3f^3k_2^4I^4L^2} \tag{8}$$

The corresponding values of d and R_c can be obtained from equations 5 and 2. The corresponding value of Q, which is the greatest obtainable under these conditions is

$$Q'_{\max}=\frac{\pi}{\dfrac{5m\pi}{3}+\dfrac{k_1}{2}+\dfrac{4}{5}\mu' ef} \tag{9}$$

If a smaller value of Q than that obtained from (9) is acceptable, equations 7 and 5 can be solved simultaneously for d and μ. A smaller value of μ than that obtained from (8) and a correspondingly smaller value of d will result.

Case II. If modulation is unimportant and the hysteresis-loss resistance is negligible in comparison with other component losses, then d and μ can be selected without regard to modulation. Equation 7 can be differentiated directly and solved for the permeability required to yield the maximum value of Q. This optimum permeability is

$$(\mu'')^2=\frac{19\rho_c\times 10^9}{sed^2f^2} \tag{10}$$

The corresponding value of Q is

$$Q_{\max}''=\frac{2\pi}{k_1+2\mu'' ef} \tag{11}$$

The values of $Q_{\max}''$ and μ'' depend on the value of d chosen. For a desired value of Q, (11) can be solved for μ'', and (10) for the corresponding diameter.

In any case, the ideal wire size has a cross-sectional area of conductor equal to

$$0.15sd^2\sqrt{\frac{\mu d\times 10^{-9}}{L}}\text{ square centimeters}$$

It is usually desirable to subdivide the wire into insulated strands to minimize eddy-current losses in the coil winding.

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Discussion

For discussion, see page 1131.

High-Speed Voice-Frequency Carrier Telegraph System

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NARROW-BAND voice-frequency carrier telegraph systems for the operation of a single teleprinter over each carrier channel have been developed to a high degree of perfection both in this country and abroad. These have been admirably described in technical literature. The theory and basic principles of carrier systems have also been well covered. This paper therefore will confine itself to a working description of a wide-band system developed to meet the requirements of a telegraph plant in which all trunk traffic between large centers is handled by the multiplex telegraph method of operation in which three or four automatic printers are operated over each carrier channel. For those not familiar with telegraph terminology and practices a brief exposition of the differences between single-printer operation and multiplex operation is perhaps in order. The single-printer method of operation secures synchronism between transmitting and receiving machines by means of what is known as the start-stop principle. A start pulse is transmitted by the sending machine preceding each group of five letters-code pulses and a rest pulse follows each group of code pulses. For 65 words per minute maximum printing speed the single-printer method results in a line frequency of 22.75 cycles per second. In the multiplex method of operation synchronism between transmitter and receiver is maintained without recourse to start and rest pulses, resulting in a more favorable ratio between printer words per minute and line signaling frequency. At a printer speed of 66 words per minute, the line frequency by the multiplex method is $16^1/_2$ cycles per second for each printer or 66 cycles per second on the four-printer circuit commonly used.

It is beyond the scope of this paper to compare the relative economies of the two methods of operation but it becomes at once apparent that the multiplex method makes for greater efficiency in the use of the frequency spectrum. There is first the improvement which results from the absence of the start and rest pulses amounting to over 40 per cent and second, the increase resulting from the use of wider carrier bands and the attendant reduction in the waste of spectrum required for channel separation in the tuners. The multiplex-carrier system here described operates normally at 66 words per minute on each of four printers, or 264 words per minute for each carrier channel. To each carrier channel, 300 cycles of spectrum is assigned. Single-printer systems require 150 to 170 cycles of spectrum for 65-word-per-minute signaling. The ratio of spectrum efficiencies secured is therefore at least two to one in favor of multiplex working. Practically, an added advantage results from the difficulty of operating start-stop printing machines at maximum speed, whereas the tape-storage feature of multiplex operation makes full theoretical speed easily attainable.

With somewhat over 700 multiplex circuits, on the conventional polar duplex ground-to-ground plan connecting the larger cities of the country, it was mandatory that the multiplex carrier system meet the well-established conditions of operation. Provision must of course be made for extending the circuits assigned to carrier channels in either or both directions beyond the terminal points of a given carrier system. The derived carrier channels must therefore conveniently and flexibly interconnect with the existing polar-duplex terminal and repeater equipment. Repeater spacing for carrier operation should preferably, though not necessarily, be the same as for grounded-duplex operation. Repeater-section distances for physical operation average about 250 miles, somewhat in excess of what in the past was considered reasonable repeater distances for voice-frequency circuits. Power plants available at physical telegraph repeater offices are not of the type commonly employed for voice-frequency operation, being in practically all cases only motor generator sets, not particularly closely regulated, nor free from the noises of load reaction and commutator ripple. In practically no case do these plants include stabilizing storage batteries.

General Design Features

Presuming at the outset of the development some years ago, a nominal operating speed of 60 cycles per second, but making allowance for the definite trend toward higher speeds, a channel band width of 180 cycles was established. This choice has long since been shown to be justified. Speeds have increased and standards of transmission efficiency have risen. A tuner design which has proven very practical for economy and space considerations permitted a channel spacing of 300 cycles. The term tuner employed in this paper indicates a network which passes a desired range of frequencies while rejecting those above and below. The system ultimately standardized employs frequencies of 450, 750, 1,050 cycles and so on with entirely practical extension up to 22 channels with the last at 6,750 cycles. In no case, however, have more than ten channels been required on any circuit although the upper channels have been tested and on one circuit the 16th channel, 4,950 cycles, has been in commercial service for a number of years. Generating equipment has been installed for all 22 channels for use as traffic increases may demand. All trunk-route circuits are operated on the four-wire

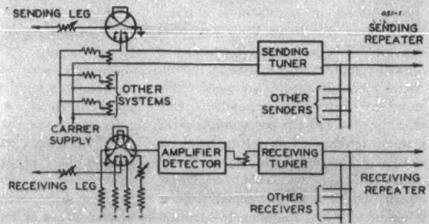

Figure 1. Schematic diagram of channel terminal

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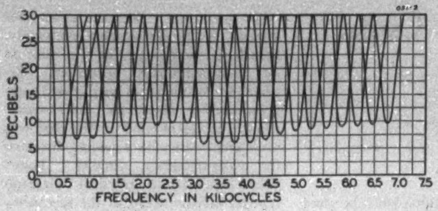


Figure 2. Transmission characteristics of sending tuners

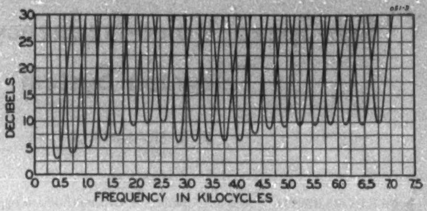


Figure 3. Transmission characteristics of receiving tuners

basis, one pair for transmission in each direction.

Wherever carrier routes have been established, open-wire transposition has been done with a view to utilizing frequencies at least as high as 7,000 cycles. Cable loading, while designed for this frequency has, on some routes, not been completely installed. On those routes where it appears that the need to use more than half this spectrum is not immediate, economies have been effected by, initially, installing only partial loading.

Impedance of all circuit elements has been established at 600 ohms, this being a value widely used in this country and sufficiently close to the nominal impedance of open-wire lines of standard telegraph construction.

Terminal Apparatus

The transmitting equipment comprises, in addition to a source of carrier frequency, the control or transmitting relay, and a transmitting tuner. Figure 1 shows a schematic diagram of the transmitting and receiving apparatus.

Transmission is accomplished by opening the carrier source as opposed to short-circuiting the source as is done in many other systems. This practice requires that the input side of the transmitting tuner be terminated in a matching impedance to prevent the appearance of objectionable transients which otherwise arise.

The tuners are multipled to a transmitting bus, the design being such that the impedance outside the pass range is high in comparison to 600 ohms. Ordinarily, level adjustments are made in the generator distributing equipment such that the power at the bus multiple point is minus ten decibels, or 0.1 milliwatt per channel. The aggregate signal of all channels of the system is led to a terminal repeater which, in every essential respect, is electrically and functionally identical to the intermediate repeaters, the description of which is left for a succeeding section.

The transmission characteristics of the transmitting and receiving tuners for the complete 22-channel system are shown by figures 2 and 3. The band width of the receiving tuners is 180 cycles and the discrimination against the side bands of the adjacent channels when modulated at 75 cycles is not less than 25 decibels. This amount of discrimination results in a telegraph transmission loss resulting from direct interchannel interference not in excess of five per cent when both the channel under test and the interfering channels are operated at 75 cycles, the nominal maximum speed of the system.

The transmitting tuners are somewhat wider in order not to restrict appreciably the useful band as determined by the receiving tuners. With an exception for the first two channels these tuners suppress the second-order side bands by at least 12.5 decibels. The above statement on direct interchannel interference presumes this degree of side-band suppression.

The incoming signals are delivered to the receiving bus at a level of plus ten decibels by a conventional repeater. Receiving tuners, with impedance characteristics similar to those at the sending end, deliver each channel band to an amplifier-detector by way of a simple potentiometer level control.

With the signal level at the output of the tuner in the neighborhood of 0.0 decibels, and the amplifier-detector capable of operating on a level of −8.0 decibels, ample margin for adjustment is afforded. The detector tube is preceded by a single stage of amplification, transformer coupled. The detector delivers a rectified signal of 60 milliamperes to the receiving relay, a polar relay operating with an electromagnetic bias. Incidentally, since the initial development work was done a number of years ago a great number of signal-shaping circuits, current-limiting rectifiers, and polar rectifiers have been investigated. The broad conclusion from this work has been that the effect of interference cannot be mitigated by such devices and that the only good they can be hoped to accomplish is in improving the speed and positiveness of the relay action. The relays now commonly used in high-speed telegraph service do not require or justify any signal-shaping device.

The local circuits operating and operated by the sending and receiving relays are designed to permit extension of the channel on either a single-wire polar-duplex basis or a two-wire sending and receiving leg basis, the former for extension of the channel to a distant point by the conventional method of working and the latter for connection to terminal equipment. Multiplex sets in the carrier terminal office are split for two-wire sending and receiving operation by means of a switch. Polar operation is employed on these two legs to avoid any unnecessary transmission loss. Morse monitoring equipment is provided with every channel, experience having shown that a common monitoring set sometimes entails delays in testing and circuit line-up work. Selectors are provided on a plug-in basis in order that they may be had where required, depending upon the circuit-channel assignment. Artificial lines, too, are provided on this basis. The local and monitoring circuits of the channel terminals may be altered to meet the mode of operation of five distinct classes of service by means of a multipoint switch. These include, besides local and extended multiplex, full or half-duplex teleprinter working either to separate operating positions or to concentrator units.

Mechanical design of channel terminal and repeater equipment is perhaps somewhat novel, being such as to permit full use of both sides of the mounting rack for live or adjustable units where required. Very little equipment is mounted on the rear of the panels and in no case does such apparatus project beyond the rear face

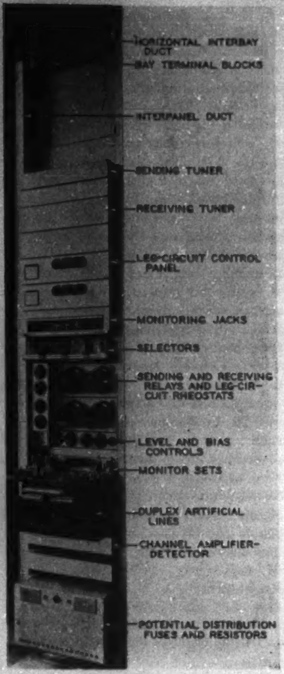

HORIZONTAL INTERBAY DUCT
BAY TERMINAL BLOCKS
INTERPANEL DUCT
SENDING TUNER
RECEIVING TUNER
LEG-CIRCUIT CONTROL PANEL
MONITORING JACKS
SELECTORS
SENDING AND RECEIVING RELAYS AND LEG-CIRCUIT RHEOSTATS
LEVEL AND BIAS CONTROLS
MONITOR SETS
DUPLEX ARTIFICIAL LINES
CHANNEL AMPLIFIER-DETECTOR
POTENTIAL DISTRIBUTION FUSES AND RESISTORS

Figure 4. Channel terminal sets, two per bay

of the panel more than half of the depth of the rack channels. All wiring and interpanel connecting terminal blocks are accessible from the front. Figure 4 shows the layout of the channel terminal sets. Two are mounted per bay and four per mounting rack, the opposite face being an identical layout. Interpanel wiring is concealed in the metal duct with hinged doors to the left of the bay.

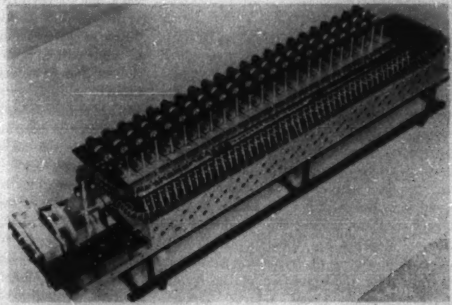
The sending and receiving busses, which multiple a group of channel terminal sets, appear on jacks on a carrier switchboard which controls the common sending and receiving amplifiers. These bus patch points are treated no differently however from a pair of line circuits, one east and one west, at an intermediate repeater point. The terminal repeater need therefore not be described in detail and those slight differences which do appear, having only to do with monitoring and system patching methods, will be pointed out in the section on repeaters. Before proceeding to the description of line and repeater equipment, however, the carrier-frequency generating and distributing equipment will be shown briefly.

Carrier-Frequency Supply

The carrier frequencies are derived from an inductor alternator. Those installed with the earlier systems were of a type designed to deliver, without amplification, sufficient power to supply ten carrier systems. These machines were driven from a primary source of d-c power with eight generating wheels on a common shaft and with the speed controlled by a centrifugal governor. A number of these are still in operation, supplying eight frequencies of ten channel systems, the last two frequencies being furnished by separate single-frequency auxiliary machines.

More recently installed systems employ an adaptation of the generator used in an electric organ. This machine, with minor modifications, fills the need admirably. New machines are equipped to deliver the carriers, spaced 300 cycles apart, for 22 channels. The generating wheels, while not on a common shaft nor rigidly locked in phase position, do not deviate

Figure 5. 22-channel carrier generator



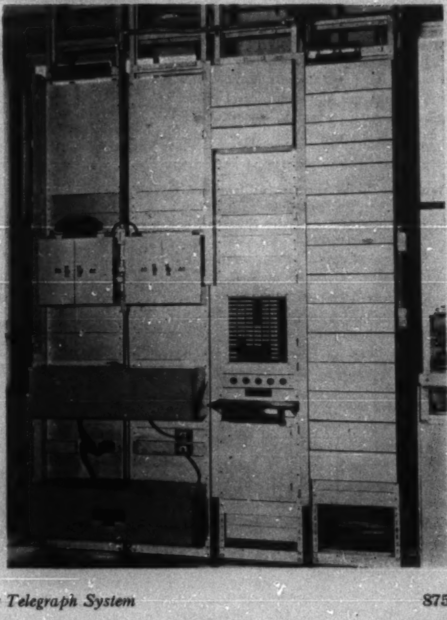


Figure 6. Carrier-supply bays

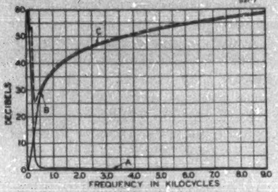


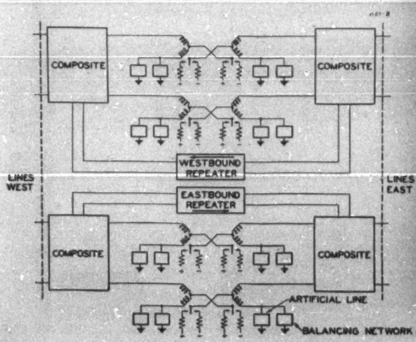
Figure 7. Transmission characteristics of composite

A—Line to carrier
B—Physical to line
C—Physical to carrier

appreciably from the nominal speed and experience has indicated that little is to be gained from exact phasing. This machine, as altered for carrier generation, is shown by figure 5. The 60-cycle synchronous driving motor has a total power consumption of 11 watts, small enough to be conveniently derived from an amplifier driven by a standard-frequency power source. In many of the larger centers where the terminals of carrier systems are located, the commercial power is of frequency stability adequate for the operation of the 6,750 cycle channel. Local reserve power plants, however, are not of adequate stability, so it is required to install standard-frequency power sources from which the motor may be driven in emergency.

The carrier pickup winding is wound directly on a permanent magnet which has its end suitably shaped and properly adjusted with respect to the rotor teeth. Two of these assemblies are located on diametrically opposite sides of the wheel, with the two pickup windings in multiple. Each carrier frequency, generated at a level of −30 decibels (one microwatt), is amplified by a two-stage amplifier to +30 decibels level.

Figure 8. Block diagram of four-wire repeater



A typical installation of generators, working and spare, together with amplifiers and distributing apparatus is shown by figure 6. The 22 frequencies are led to a distributing panel where multiples are made for all the carrier systems in an office. Associated with each carrier system but mounted on the generator bays is a pad panel which contains adjustable potentiometers of high attenuation, one for each frequency. These prevent interaction between like channels of different systems due to the load variation of modulation. An over-all level regulation of plus or minus 0.2 decibel is maintained by the generating, amplifying, and distributing equipment. The whole of the carrier-generation plant is designed to be subject to an absolute minimum of interruptions and every precaution is taken to be able to locate failures and restore service without delay.

In New York a plant of this type supplies carrier power for four systems with ten channels in operation on each. This plant has a capacity, as installed, of ten systems.

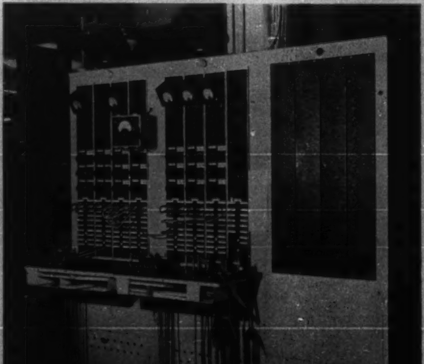
Compositing Equipment

All carrier wires are composited and the normal grounded-duplex operation on them is maintained without loss of flexibility in circuit assignment and with little loss of efficiency. The transmission characteristics of the composite are shown by figure 7. The presence of a low-pass filter section, required to suppress the voice-range components of the physical-circuit transmission, necessitates a compensating network in the artificial line of the physical repeater or terminal set. The connection of this network is so arranged in the switchboard circuit of the wire that it is associated always with the wire rather than the set, permitting complete freedom of set assignment and regulation. For the most part carrier repeaters are located at points which are also physical-circuit repeaters for economy of office facilities and attendance personnel. Near large centers of population, however, cable sections require the use of carrier repeaters intermediate to the physical repeaters. At such repeaters two composites are employed but are so connected as to insert no unnecessary attenuation in the physical telegraph circuit. The physical wire circuit beyond such an intermediate repeater may or may not carry the superimposed voice circuit.

Repeaters

Meeting, in the general case, the requirement that the physical and carrier

Figure 9. Carrier-frequency switchboard



repeaters be located in the same offices, the attenuation of the repeater sections averages around 20 decibels for the highest carrier frequency. Certain sections may vary widely from this figure but a transmitting level of +10 decibels is rigidly maintained. Figure 8 is a block diagram of a four-wire repeater at an office where both carrier and physical circuits are repeated. The repeating amplifier has a maximum gain of 46 decibels, substantially uniform throughout the range from 400 cycles to 7 kilocycles. This and all of the repeater equipment was designed with a view to the use of a larger spectrum range. In fact, all new repeater equipment installed will be designed to be operable up to 60 kilocycles, dropping perhaps a portion of the extreme low end of the range when high-frequency operation is inaugurated. The amplifier maintains a margin between signal and modulation products of 50 decibels when the aggregate signal level for all channels is +21 decibels. This corresponds to 22 modulated carriers at +10 decibels per channel. It is relatively free from the effects of supply-battery variations, the change in gain being 0.05 decibel per volt. The residual noise of the amplifier, operated on the telegraph battery plant which carries all manner of fluctuating loads from signal circuits to clock circuits and time stamps of heavy drain, is not in excess of −50 decibels.

A line-attenuation equalizing network precedes each repeating amplifier. This, while not adjusted to make the over-all transmission equivalent of the repeater section absolutely flat with frequency, is adequate to prevent overloading of the amplifiers due to a preponderance of energy of any one or group of channels. In general any irregularities in the frequency characteristics of individual repeater sections tend to average out from section to section. The remaining discrepancies of the over-all circuit are taken up in the channel level controls at the terminal.

The design and mounting of the repeater equipment is very similar to that of the channel terminal. One difference is noticeable, however, in the segregation of the apparatus which must be accessible for monitoring and testing from that which need not. The latter is mounted in a remote corner of the office and the former is built into what is known as a carrier-frequency switchboard. In this board are located, in addition to the patch points for the carrier side of the composites and the repeater terminal jacks, all gain controls and all measuring and testing facilities. We have then in the broad picture, the line wires terminating in the regular telegraph switchboard and the carrier circuits and their associated controls in a second board, operated by an especially trained personnel. Figure 9 shows a typical carrier-frequency-board installation. A four-wire repeater switching and control panel occupies a single vertical strip assembly, five inches in width, comprising four rows of jacks and the necessary control and signaling apparatus. Figure 10 is a schematic diagram showing the circuit arrangements of one-half of a carrier board repeater control panel. The equalizing network provided for operation of the pair in the direction opposite to that of the normal assignment comes into play when the pair-reversing patch is made. A common monitoring set serves a number of repeaters. Each repeater is equipped with two amplifier-detectors and associated selectors, and visible and audible alarm apparatus. The selectors operate on both a station and a master call. When a four-wire circuit is idle the terminal repeater attendants can call any or all intermediate repeater stations for trouble location work or for circuit line up. The amplifier-detectors or "monitoring amplifiers" as they are called, operate from a leak circuit on the repeating amplifiers and respond to any frequencies that may be present in the repeater. For normal line-up and regulating work a pilot channel of 1,050 cycles is used. This pilot frequency, applied at the terminals through a skeleton channel terminal set accounts for the principal details by which a terminal repeater differs from an intermediate repeater. At the terminals sending and receiving tuners are used, making the monitoring equipment at these points responsive to but one channel frequency and permitting signal-noise ratio measurements which show the true operating margin of that channel. Intermediate repeaters are not provided with a source of carrier power. The repeater communicates with stations beyond and with the distant terminal in either direction by opening and closing the repeating amplifier output circuit. The response of the repeater monitoring sets to all frequencies facilitates the work of making margin or crosstalk measurements at frequencies other than the normal pilot-channel frequency. The normal gain adjustments of the daily line-up, and routine margin measurements are made on the pilot frequency however. Sufficient spare routes are provided in carrier networks to permit a daily line-up and level check on each. Spare routes are continuously monitored to insure that they are always in readiness to assume a traffic load. In case of line failures, system restoration is made by a terminal-to-terminal patch with very little lost operating time.

A further monitoring convenience, for use on either working or spare circuits, is an amplifier and loud-speaker combination which may be connected to any repeater. One or more of these devices are provided at each repeater station as needed. A multiple push-button circuit connects this equipment to either side of any repeater in order that the attendant may determine in an instant just which circuits are in service and be warned against making any tests which might interrupt a traffic-handling system. A further important use of the loud speaker is in the location of wire trouble. A master call on an idle circuit calls all repeaters. The attendants are asked to connect their speakers and report the firs hit or swing. Through this practice the terminal attendant who

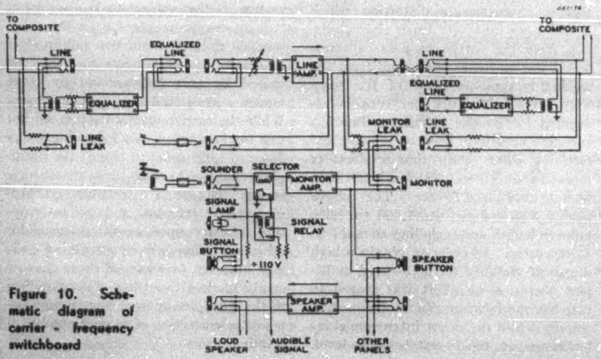


Figure 10. Schematic diagram of carrier - frequency switchboard

may have been warned of a circuit interruption by a swing bell learns almost immediately which repeater section is in trouble.

Incidentally, the use of the spare circuit as an order wire to which the working system is to be moved in case of failure of the normal working circuit is a practice once frowned upon and generally avoided. In carrier working it has been highly satisfactory, and fortunately so, since it is not convenient to have private order or pilot wires extending over the great distances involved between carrier terminals.

Power Level Measurement and Control

Intermediate and terminal repeaters are furnished with a power-level indicating instrument for the measurement of signal and interference levels. This instrument, of the dry-rectifier type, was developed to meet the requirements of high maximum sensitivity and wide range. It is used as a circuit terminating meter rather than as a voltmeter as are many of the similar devices employed in broadcast monitoring. It maintains a purely ohmic impedance of 600 ohms throughout its range. Its accuracy is within ±0.2 decibel from 60 to 10,000 cycles.

An adjustable attenuator located within the instrument extends the range from a full-scale sensitivity of −5.0 decibels to +35.0 decibels and on the lower range it can be read with good accuracy down to −30 decibels. At large repeater stations smaller panel-type instruments are mounted in the carrier switchboard for routine repeater-output-level measurements. These are not adjustable in range and are calibrated only from +5.0 to +15.0 decibels.

By means of the pilot-channel set on spare routes the terminal attendant is able to keep all repeaters properly aligned so as to insure that there will be no repeater overloading when these routes are placed in service. The working systems are kept in proper level alignment by always assigning a manually operated or a single teleprinter circuit to the pilot channel, the 1,050 cycle channel. A duplicate relay-current meter for this channel is mounted in the switchboard where the system level indication is under constant surveillance. The gain of the terminal repeater is adjusted to maintain proper relay operating current on this channel. Only sudden drastic weather changes covering wide areas make it necessary to absorb more than three or four decibels attenuation change at the terminal. This change is not sufficient to cause repeater overloading nor generally to cause excessive bias in channels remote from the pilot frequency. When wider attenuation changes do occur the operating system is moved to a spare circuit and the repeaters are realigned.

Carrier Transmission Standards

It has been determined that a high degree of telegraph transmission efficiency is maintained if the carrier signal-noise margin within the channel bands is not allowed to fall below 30 decibels. This figure is not difficult of attainment for channels above the first. As measured between repeater stations, without benefit of tuners, this sets the requirement at approximately 35 decibels between the signal level and the combined interference, cross talk, and extraneous noise. With repeater section attenuation not exceeding 20 decibels at the highest frequency employed, the cross-talk return loss per repeater section can easily be maintained at 60 decibels or more. With reasonably good pair balances and by insuring that all polar circuits operated on the lead are protected with antinoise filters the line noise can be reduced to a level −45 decibels. The operation of the physical circuits through the composite sets increases the apparent aggregate interference by as much as 5 decibels but the preponderance of energy in this is well below the carrier range. The 450-cycle channel is the only one materially affected by this disturbance.

Telegraph Transmission Standards

On all except the first two channels the total distortion loss at 66 cycles modulation speed does not normally exceed ten per cent. Roughly half of this is characteristic distortion which results from imperfect tuner characteristics, from the carrier by-pass capacitor across the receiving relay, and from the residual-magnetism effect of the relay itself. The other five per cent is accounted for by the extraneous interference, the cross talk between sending and receiving pairs, modulation products of repeaters, and a small amount of direct interchannel interference. The above figures hold for circuits 1,000 to 1,200 miles in length and including as many as 12 repeaters. In order to obtain a high degree of stability for high-speed multiplex operation no effort was spared to keep the modulation effects of repeating amplifiers and the direct interchannel interference at the lowest possible level. Certain other factors such as level changes and occasional cross talk and noise always have their adverse effect on transmission. By keeping the controllable losses to a minimum the highest degree of immunity to the uncontrollable ones is secured. Performance records kept on multiplex circuits assigned to carrier channels indicate the continuity of service to be far better than is obtainable by grounded duplex operation and at least equivalent to that secured by two-wire metallic operation.

The transmission efficiency of the first and second channels is lower than that of the higher channels; first, because of the unfavorable ratio of modulation frequency to carrier frequency; and second, because of a certain amount of interference from the operation of the grounded physical circuits. In addition, the first channel is subjected to a slight handicap due to phase shift between carrier and side-band frequencies. These factors all combine with the result that channel number one while capable of three-printer multiplex working, for absolute stability is normally used in two-printer service. The second is customarily used for three-printer multiplex working.

Maintaining three-printer multiplex operation on the four physical wires of the carrier system, there is little question of the possible economy of abandoning the first channel in favor of slightly improved physical circuit operation. There is, however, no tenable argument for abandoning the physical circuits to secure only a minor improvement in the efficiency of the lower-frequency channels.

Lost time per day on a four-printer multiplex circuit, operating over a carrier system 1,000 miles in length, averages about 12 minutes per day. The carrier system itself is subject to an average of two interruptions per day with a restoration time of about two minutes per interruption. These interruptions are caused by wire failures and by route moves necessitated by various causes. While the carrier system itself is subject to a total outage of only four minutes per day per 1,000 miles of circuit the multiplex is subject to three times that outage because, in case of wire failure synchronism is frequently lost, and each interruption is seized upon as the occasion for other regulating or line-up work. Generally, however, prearranged route changes can be made without loss of synchronism to the multiplex or even interruption beyond the correction of a small number of printing errors.

Conclusions

The employment of carrier in the plant of the Western Union has been a large factor in reducing the new wire construction almost to the vanishing point. A further and perhaps almost as important economy will ultimately be realized in attendance costs of repeaters. Experience indicates that this charge is no more for a carrier repeater than for a single physical repeater handling the same class of service as is assigned to each carrier channel. Economy of repeater office space is another large factor, the carrier repeater requiring less space than a single physical repeater. The loading of cable sections adds to the fixed charges on lines and maintenance costs are increased, but these increases are not large. For long trunk circuits the balance is of course very much in favor of carrier operation and even for circuits of 200-mile length carrier is preferable to new line construction.

Four trunk systems varying in length from 250 to 1,000 miles with a total of 39 channels in operation radiate to the north, south, and west from New York City. In normal assignments 31 of these channels are multiplex operated, with the remainder held in reserve or operated in less important services which may be quickly released for multiplex working.

Numerous single- and double-channel systems operate over short distances where it has been found necessary to secure added facilities. The equipment employed for this class of service differs only in the carrier supply and frequently in the absence of the terminal repeaters.

Discussion

For discussion, see page 1132.

Calculation and Design of Resistance-Coupled Amplifiers Using Pentode Tubes

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RESISTANCE-COUPLED amplifiers employing pentode tubes are used more widely than any other type of voltage amplifier for audio frequencies. In spite of this, no method of analysis is available for correctly calculating the complete characteristics of such amplifiers and design procedures ordinarily followed involve considerable cut and try. The present paper presents (1) an analysis taking into account all of the factors of significance in determining the amplification and phase-shift characteristics of ordinary audio-frequency amplifiers employing pentode tubes, together with charts which make it a simple matter to use the analysis; and (2) a systematic procedure for proportioning the coupling network in the plate circuit when the main objective is: (*a*) maximum possible voltage gain, and (*b*) large output voltage.

Calculation of Voltage Amplification

The circuit of a typical resistance-coupled amplifier is shown in figure 1*a*. The magnitude and phase of the amplification obtained with such an arrangement is affected by the coupling network in the plate circuit, by the impedance in the screen circuit external to the tube, and by the impedance between cathode and ground.

If A_0 is the amplification on the assumption that the cathode-ground and screen circuit impedances are zero and the capacitors C_c and C_s in figure 1*a* have no effect, then from conventional amplifier theory[1]

$$A_0 = g_m R \tag{1}$$

where

g_m = mutual conductance of tube,

$R = \dfrac{R_c}{1+\dfrac{R_c}{R_{gl}}+\dfrac{R_c}{R_p}}$ = equivalent resistance formed by coupling resistance R_c, grid-leak resistance R_{gl}, and plate resistance R_p, all in parallel.

The actual amplification will be less than A_0 according to the relation

$$\text{Actual amplification} = A = \alpha\,\beta\,\gamma\,A_0 \tag{2}$$

where

α = factor taking into account the effect of capacitors C_c and C_s in the coupling network in the plate circuit

β = loss of amplification due to impedance in the screen circuit

γ = loss in amplification due to impedance between cathode and ground

Effect of Plate Circuit Coupling Network on Amplification

The factor α that takes into account the effect of the coupling network in the plate circuit upon the amplification can be evaluated with the aid of the equivalent circuit of figure 1*b*. At low frequencies the output falls off and leads in phase because of the loss of voltage in the coupling capacitor C_c, while at high frequencies, the output falls off and lags in phase because of the shunting capacitance C_s. In practical amplifiers the shunting effect of C_s is negligible at low frequencies, while C_c can be assumed to be a short circuit at

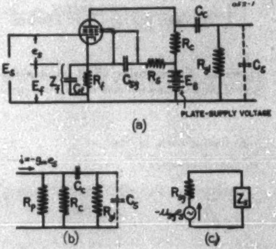

Figure 1. Actual and equivalent circuits of resistance-coupled amplifier using pentode tubes

(*a*)—Circuit of resistance-coupled amplifier using pentode tube

(*b*)—Equivalent plate circuit

(*c*)—Equivalent screen-grid circuit

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[1] For all numbered references, see list at end of paper.

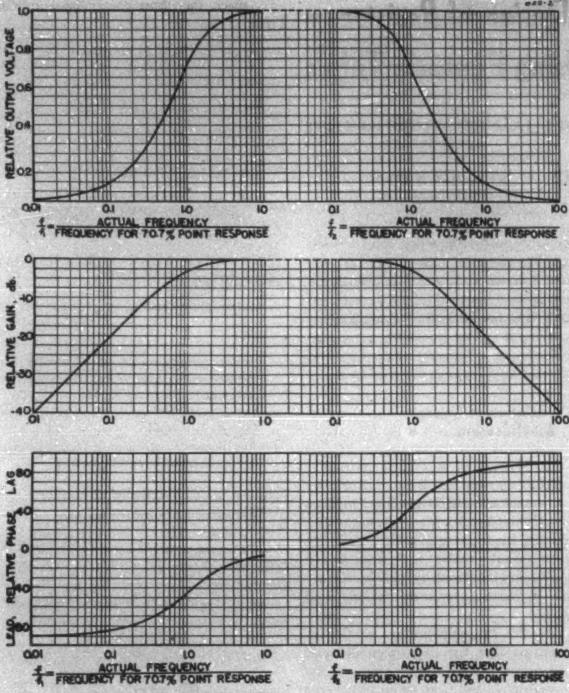

Figure 2. Curves giving effect on amplification of the coupling network in the plate circuit

high frequencies, giving[1]

For low frequencies

$$\alpha = \frac{1}{1 - j\frac{f_1}{f}} \tag{3}$$

For high frequencies

$$\alpha = \frac{1}{1 + j\frac{f}{f_2}} \tag{4}$$

where

f = actual frequency

$$f_1 = \frac{1}{2\pi C_c\left(R_{g1} + \frac{R_p R_c}{R_p + R_c}\right)}$$

= frequency at which the reactance of C_c equals the resistance formed by the grid leak in series with the combination of plate and coupling resistances in parallel

$$f_2 = \frac{1}{2\pi C_s R}$$

f_2 = frequency at which reactance of C_s equals the resistance R as defined in connection with equation 1

Values of α are plotted in figure 2 in a convenient form.

The phase shift and falling off in amplification represented by α is usually taken as the frequency-response characteristic of the amplifier. The actual characteristics may differ appreciably from this at low frequencies because of the impedances in the screen and cathode circuits.

Effect of Impedance in the Screen Circuit

An impedance in the screen-grid circuit such as supplied by the voltage-dropping resistance R_s and the by-pass capacitor C_{sg} in figure 1*a* affects the amplification by the following process: A signal voltage e_s acting between grid and cathode produces variations in screen current as well as variations in plate current. This causes a voltage to be developed between screen and cathode, which affects the plate current and so modifies the gain.

A quantitative analysis can be carried out with the aid of the equivalent screen-grid circuit shown in figure 1*c*. Here the effect on the screen current of the signal voltage e_s applied between control grid and cathode is represented by an equivalent voltage $-\mu_{sg}e_s$ acting in a circuit consisting of the dynamic screen-grid resistance R_{sg} in series with the impedance Z_s in the screen circuit. This equivalent circuit is analogous to the equivalent plate circuit of a triode, with the constant μ_{sg} representing the μ-factor of the screen-grid relative to the control grid with respect to the screen current.[3] The only approximation in this equivalent circuit is that it assumes the screen-grid current is independent of the plate potential, a requirement realized under the practical operating condition of plate current independent of plate potential.

Referring to figure 1*c*, the voltage developed across the impedance Z_s is $-\mu_{sg} \times e_sZ_s/(R_{sg}+Z_s)$. Now this voltage which exists between screen and cathode has the same effect on the plate current as a voltage $[-\mu_{sg}e_sZ_s/(R_{sg}+Z_s)]/\mu_s$ acting on the control grid, where μ_s is the μ-factor of the screen grid relative to the control grid with respect to the plate current. The effect of the impedance in the screen circuit on the gain is accordingly

$$\beta = \frac{e_s - (\mu_{sg}/\mu_s)e_sZ_s/(R_{sg}+Z_s)}{e_s}$$

or

$$\beta = 1 - \left(\frac{\mu_{sg}}{\mu_s}\right)\frac{Z_s}{R_{sg}+Z_s} \tag{5}$$

The ratio μ_{sg}/μ_s appearing in equation 5 represents the relative effectiveness of the control and screen grids on the screen current compared to their relative effectiveness on the plate current. Under the usual pentode operating conditions of plate current independent of plate voltage, this ratio is unity.

In the common case where the impedance in the screen circuit is a resistance-capacitor combination as in figure 1. then for $\mu_{sg}=\mu_s$, equation 5 becomes*

$$\beta = \frac{\frac{K}{1+K} + j\frac{f}{f_s}}{1 + j\frac{f}{f_s}} \tag{6}$$

where

$$K = \frac{R_{sg}}{R_s} = \frac{\left(\text{dynamic resistance of screen-grid circuit of tube}\right)}{\left(\text{voltage dropping resistance in screen circuit}\right)}$$

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* This assumes that the impedance between the cathode and ground is negligible in comparison with R_s, a condition always satisfied in ordinary designs.

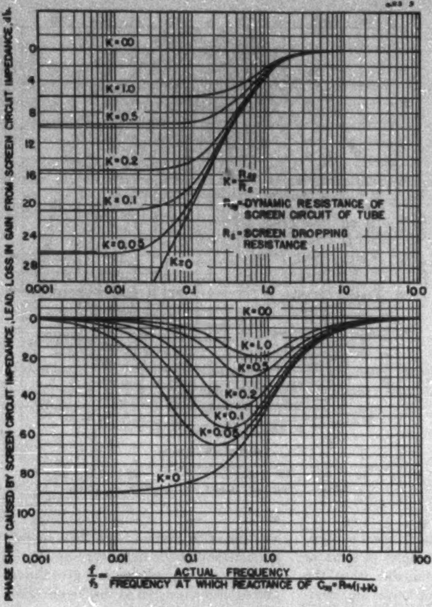

Figure 3. Curves giving effect on amplification of resistance-capacitor combination in screen circuit

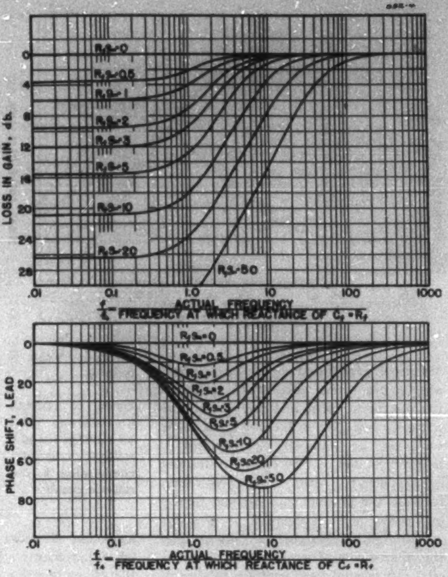

Figure 4. Curves giving effect on amplification of a resistance-capacitor combination in the cathode circuit when the screen impedance has negligible effect

$$f_s = \frac{1+K}{2\pi C_{sg} R_{sg}}$$

= frequency at which the reactance of C_{sg} equals the resistance formed by R_s and R_{sg} in parallel

Values of β are given in convenient form in figure 3 for this case, under the assumption that $\mu_{sg}/\mu_s = 1$.

Examination of figure 3 shows that the resistance-capacitor combination in the screen circuit can cause a large loss in amplification at low frequencies and give rise to considerable phase shift. *In order for these effects to be negligible at a given frequency, it is necessary practically that the reactance of the screen by-pass capacitor* C_{sg} *at this frequency be small compared with the dynamic screen resistance* R_{sg} *of the tube.*

The lack of appreciation of the influence of impedance in the screen circuit on the amplification characteristic is indicated by the fact that information upon the essential constant R_{sg} of the pentode tube is not given in any of the tube data books. The designer must accordingly either measure this constant himself or infer its value from the plate resistance of the same tube operated as a triode. If m denotes the ratio of plate to screen d-c currents for pentode operation then

$$R_{sg} = (1+m)R_p \tag{7}$$

where R_p is the plate resistance of the tube connected for triode operation with the plate current the same as the total d-c space current in pentode operation.

Effect of Bias Impedance on Amplification

Impedance between the cathode and ground such as supplied by R_fC_f in figure 1*a* affects the amplification as a result of the fact that amplified signal currents flow through this impedance. This develops a voltage drop between cathode and ground that is superimposed upon the signal voltage and modifies the amplifier output.

The effect of an impedance Z_f between cathode and ground can be determined as follows:* The current through the impedance Z_f produced by the voltage e_s between cathode and control grid of a pentode tube is $g_m e_s \beta$, which develops a voltage E_f across the impedance Z_f that is $g_m e_s \beta Z_f$. The voltage e_s acting between grid and cathode is related to E_f and the applied signal E_s by the expression $e_s = E_s - E_f$. This gives

$$\frac{e_s}{E_s} = \gamma = \frac{1}{1+g_m Z_f \beta} \tag{8}$$

In the usual case where the impedance Z_f is supplied by a resistance-capacitor combination, as in figure 1*a*, equation 8 can be written

$$\gamma = \frac{1}{1+\dfrac{g_m R_f \beta}{1+j\dfrac{f}{f_4}}} \tag{9}$$

where

$$f_4 = \frac{1}{2\pi C_f R_f}$$

= frequency at which the reactance of C_f equals the resistance R_f

---

* This derivation assumes that the screen grid is bypassed to the cathode so that the signal currents flowing in the screen circuit do not pass through the bias impedance, and so that voltage developed between cathode and ground does not produce voltage between screen and cathode. This is reasonably near the actual case under most conditions, but even when not the case the error will not be important under most conditions.

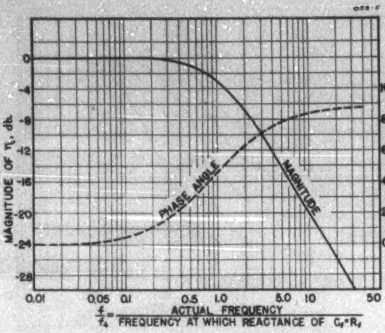

Figure 5. Value of factor η

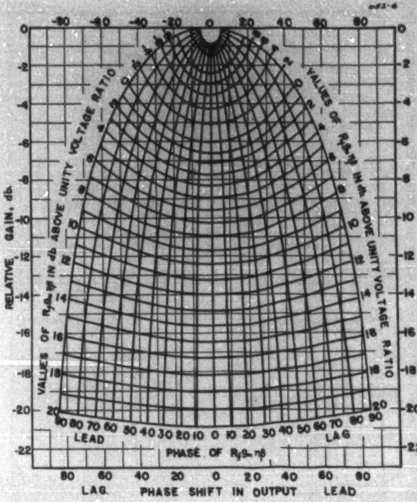

Figure 6 (right). Curves giving effect on amplification of a resistance-capacitor combination in the cathode circuit

Values of γ for the case $\beta=1$ (zero impedance in the screen circuit) are presented in convenient form in figure 4. In the more general case, where $\beta\neq 1$, the value of γ can be obtained with the aid of figures 5 and 6 by first evaluating $\eta=1/(1+jf/f_4)$ from figure 5, then forming the product $g_m R_f \eta\beta$, and finally evaluating γ from figure 6.

Examination of equation 9 shows that the effect of impedance between the cathode and ground in a pentode tube cannot be calculated independently of the impedance in the screen circuit, although this has been the customary procedure in the past.

Discussion

The application of the above analysis to a resistance-coupled amplifier with typical circuit proportions is illustrated in figure 7. Here curve 1 gives the magnitude and phase of the factor α. It represents the amplification characteristic as commonly calculated and is the actual characteristic when the screen and cathode impedances have negligible effect. Curve 2 gives the added phase shift and added loss in gain resulting from the screen impedance. Curve 3 gives the effect of the cathode impedance on the gain and phase shift, and is to be compared with number 4 which gives results that would be obtained by the usual procedure of neglecting the effect of the screen impedance on the action of the cathode impedance. Finally, number 5 gives the actual over-all characteristic, which is a summation of the first three curves. At low frequencies number 5 shows decidedly more falling off in amplification and greater phase shift than number 1.

A full understanding of all of the factors controlling the magnitude and phase shift of the amplification opens the way for designs that will more exactly meet particular requirements. Examples of some of the characteristics obtainable when the screen and cathode impedances are resistance-capacitor combinations of various proportions are given in figure 8. It will be noted that a sharp low-frequency cutoff is obtained by proportioning the screen impedance so that f_3 approximates f_1, and minimizing cathode impedance effects. On the other hand, when minimum phase shift is important, as is the case in amplifiers employing negative feedback, the screen and cathode impedances should both be designed so that f_3 and f_4 are considerably less than f_1.

The formulas that are presented for α, β, and γ have been subjected to extensive experimental checks, both as to magnitude and phase angle. The observed results agreed with the theoretical calculations with an accuracy about the same as that obtained when making triode-amplifier calculations based on the equivalent plate circuit of the triode amplifier.

Design of Amplifiers for Maximum Gain and for Large Output Voltage

In the design of resistance-coupled amplifiers using pentode tubes, the principal problems center about the selection of the proper coupling resistance R_c and the proper d-c plate current. When these are fixed, it is possible to select a combination of bias voltage and screen potential that will give this current, and then to proportion the cathode and screen impedances to develop these voltages and give the required characteristics at low frequencies. The grid-leak resistance is taken as the highest resistance that it is permissible to place in the grid circuit of the succeeding tube, and the coupling capacitor C_c is chosen to give the desired low-frequency response.

In selecting the proper coupling resistance and plate current it is necessary to distinguish between designs in which the principal objectives are: (1) maximum possible voltage gain, and (2) largest possible output voltage without excessive distortion and with at least reasonable gain.

Design for Maximum Voltage Gain

With a given coupling resistance R_c and plate-supply voltage E_B the voltage gain of an amplifier will increase with d-c plate current up to the point where the voltage drop in the coupling resistance is so great that the remaining voltage available for the plate of the tube is insufficient to make the plate current substantially independent of plate voltage. Practically, a slightly larger plate potential is desirable to permit a reasonable tolerance in the coupling resistance R_c. A good practical rule to follow is to consume about 80 per cent of the total voltage in the coupling resistance.

With a specified voltage drop in the

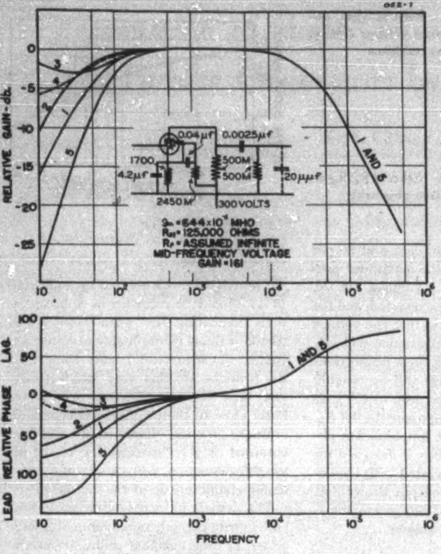

Figure 7. Characteristics of actual amplifier

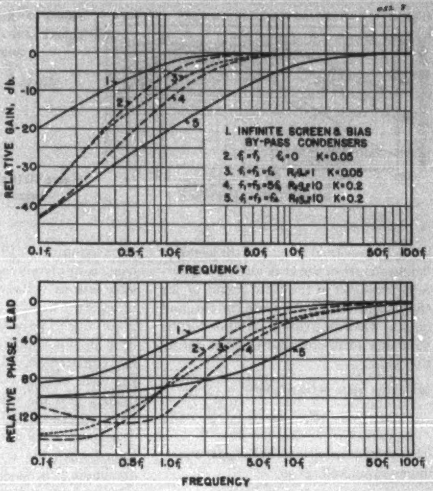

Figure 8. Typical low-frequency amplification characteristics obtainable with different resistance-capacitor combinations in screen and cathode

coupling resistance R_c, maximum gain is obtained when the coupling resistance has a definite relation to the grid-leak resistance. To obtain this relation, one first rewrites equation 1 considering the plate resistance R_p so great as to have negligible effect, an assumption very closely realized in practice.* This gives

$$A_0=g_m\frac{R_cR_{gl}}{R_c+R_{gl}} \tag{10}$$

One also has

$$\text{Plate current}=i_p=k\left(e_g+\frac{e_{sg}}{\mu_s}\right)^n \tag{11}$$

$$g_m=\frac{di_p}{de_g}=nk\left(e_g+\frac{e_{sg}}{\mu_s}\right)^{n-1} \tag{12}$$

$$\text{Voltage drop in coupling resistance}=E_d=i_pR_c \tag{13}$$

In these equations n and k are constants, e_g and e_{sg} are the direct voltages applied to the control and screen grids respectively, and μ_s has the same meaning as in equation 5. Substitution of (12) into (10), elimination of (e_g+e_{sg}/μ_s) by (11), and using (13) to eliminate i_p gives

$$\text{Amplification}=A_0=nk^{\frac{1}{n}}E_d^{\frac{n-1}{n}}R_g^{\frac{1}{n}}\frac{(R_c/R_{g1})^{1/n}}{1+\dfrac{R_c}{R_{g1}}} \tag{14}$$

Differentiation of A_0 with respect to R shows that

$$R_c\text{ for maximum gain}=\frac{R_{gl}}{n-1} \tag{15}$$

Equations 14 and 15 express a number of important properties of resistance-coupled amplifiers using pentode tubes. The exponent n in these equations can be expected to have values of the order of 1.5 to 2.25, with values of 2 or a little less typical for the usual operating conditions where the plate current is somewhat less than the rated value. Accordingly, the optimum coupling resistance R_c is roughly equal to the grid-leak resistance R_{gl} although the gain is not critical with respect to the ratio R_c/R_{gl}, as shown in figure 9.

The proper design procedure to obtain maximum gain is to make $R_c=R_{gl}$ and then check f_2 to see if the high-frequency response is satisfactory. If the high-frequency response is poorer than desired, R_c is lowered as necessary, and the resulting loss in gain accepted as the price paid to obtain the desired high-frequency characteristics.

Design for Large Voltage Output

The maximum output voltage is limited on the negative half cycle of the applied signal by the fact that the instantaneous plate current can never be less than zero, and on the positive half cycle by the fact that the instantaneous potential at the plate of the tube (direct voltage at plate minus peak a-c output voltage) must not be so low as to cause the plate current to cease to be independent of plate potential. With a given ratio R_c/R_{gl}, maximum output voltage occurs when these two limits are simultaneously reached with a sine-wave signal. The relations involved are approximated by the following analysis. Let

E_B = plate-supply voltage

$$r=\frac{\begin{pmatrix}\text{lowest plate potential at which plate}\\ \text{current is independent of plate voltage}\\ \text{when plate current is twice the d-c}\\ \text{value}\end{pmatrix}}{E_B}$$

$E_d=i_pR_c$ = direct-voltage drop in coupling resistance

i_p = d-c plate current

If the instantaneous plate current is to

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* This approximation can be avoided by interpreting R_{gl} in equations 10, 14, and 15 to be the resistance formed by the plate resistance of the tube in parallel with the actual grid-leak resistance. The results then obtained are correct to the extent that the combined resistance varies much more slowly with plate current than does the transconductance. These remarks also apply to equations 16 and 17 and the related text.

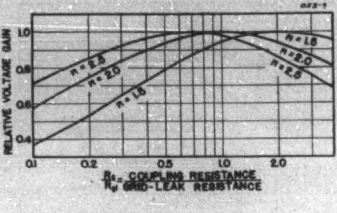
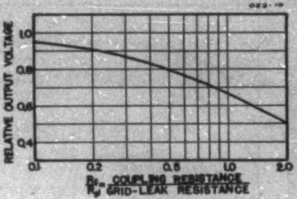


Figure 9 (left). Relative gain obtained with constant voltage drop in coupling resistance

Figure 10 (right). Effect of R_c/R_{gl} on output voltage obtainable



swing from normal to zero on the negative half-cycles, then the peak alternating current developed in the plate circuit of the tube is equal to the d-c plate current, and one can write the following relation for the minimum plate potential reached during the cycle:

Minimum plate potential =

$$E_B - E_d - i_p\frac{R_cR_{gl}}{R_c+R_{gl}} = rE_B$$

This gives the voltage drop E_d for maximum output voltage as

$$E_d = (1-r)\left(\frac{1+\frac{R_c}{R_{gl}}}{2+\frac{R_c}{R_{gl}}}\right)E_B \qquad (16)$$

The corresponding output voltage is $\frac{R_{gl}R_c}{R_c+R_{gl}}i_p$, or

$$\text{Maximum output voltage} = \frac{(1-r)}{2+\frac{R_c}{R_{gl}}}E_B \qquad (17)$$

This equation shows that the relative output voltage increases as R_c/R_{gl} is reduced, with the exact relation given by figure 10.

The practical procedure for obtaining maximum output voltage is to select coupling resistance R_c that makes R_c/R_{gl} a reasonable compromise between maximum gain and maximum power output. Usual values are in the range 0.2 to 0.5, corresponding to 91 per cent and 80 per cent, respectively, of the maximum possible output voltage. A value of r of the order of 0.1 to 0.2 is then assumed, and the plate current adjusted so that the voltage drop in this coupling resistance will satisfy equation 16. With these proportions the peak output voltage will be roughly $0.35E_B$, and the voltage drop in the coupling resistance will approximate 0.5 E_B.

A comparison of the amplification obtained when the amplifier is designed for maximum gain as contrasted with the design for large voltage output shows that in the former case the voltage amplification is about one-third greater.

Effect of Plate-Supply Voltage and Grid Leak Resistance on Amplification

Examination of equation 14 shows that for a fixed value of R_c/R_{gl} the gain is proportional to the voltage drop $E_d^{(n-1)/n}$ (and hence to $E_B^{(n-1)/n}$), and also to $R_{gl}^{1/n}$. In practice this means that the amplification obtainable from a resistance-coupled amplifier, irrespective of whether designed for maximum gain or large output voltage, can be expected to be proportional to something between the square root and cube root of the plate-supply voltage, and between the square root and two-thirds power of the grid-leak resistance.

Discussion

The relations represented by equations 14–17 should be regarded as approximations indicating the main factors and trends without giving highly accurate answers. Experimental results show that the formulas do this very satisfactorily, and that the quantitative results are fairly close to those considered typical in ordinary practice. Exact results could be obtained only by specification of the allowable distortion, and a study of the dynamic characteristic of the amplifier.

The designs for resistance-coupled amplifiers with pentode tubes commonly suggested in tube manuals[3] approximate the proportions recommended here for large output voltage, and lead to allowable output voltages that are generally within ten per cent of those calculated by equation 17.

References

1. RADIO ENGINEERING (a book), F. E. Terman. Second edition, pages 174–81.

2. See IRE Standards on Electronics, 1938, page 6.

3. RCA RECEIVING TUBE MANUAL, RC-13, page 176.

Discussion

For discussion, see page 1133.

Determination of Short-Circuit Torques in Turbine Generators by Test

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THE consideration of short-circuit stresses in turbine-generator shafts and frames must be preceded by a knowledge of the developed electrical torque. The theoretical work for this subject has been done by Kilgore and Stone, and Nickle, Pierce and Henderson. As far as is known there have been no extensive measurements of short-circuit torques on modern turbine generators. The object in performing these experiments was to obtain data to check previous equations derived from theoretical considerations and, if necessary, make revisions.

Description of Equipment and Test

A 3,600-rpm 12,500-kva 80 per cent power-factor 13,800-volt three-phase 60-cycle turbine generator was used in making the tests. This machine was of standard construction except that the stator core was flexibly supported with respect to the frame. The machine was driven as a synchronous motor at speeds slightly above 60 cycles. Immediately preceding the short circuit, the generator was disconnected from the power supply and the short circuit applied as it coasted down through 60 cycles. This was done to eliminate driving equipment consisting of another rotor mass and connecting shaft which would have introduced unnecessary complications in the analysis of the measured torques. The currents and voltages were measured in the usual manner and the torques by a strain gauge, which will be described later. The short circuit was applied with a solenoid-operated three-phase oil circuit breaker of suitable rating. The breaker was closed by relay action and was set to open approximately 16 cycles after closing.

The tests were made with various types of short circuits including three phase, line to line, and line to neutral at both 75 per cent and 100 per cent voltage. The line-to-neutral short circuits were taken with two different values of reactance in the neutral.

The accelerometer for measuring the shaft torque is shown in figure 1 and is fundamentally the same as that described in Mr. Penney's paper.[1] The torque rod of the instrument is fastened at the rear to the case of the accelerometer and at the front end to the flywheel. The fundamental mode of operation of the instrument is as follows: When a torque accelerates the generator rotor there is a torque imposed on the flywheel which is proportional to the acceleration. This displaces the flywheel relative to the case. The displacement is limited by the torque rod so that it is proportional to the torque applied to the rotor. The displacement is measured by an ordinary magnetic strain gauge. The armature of the strain gauge is fastened to the flywheel and the pole pieces to the accelerometer case. There is no damping arrangement provided in this instrument so that the natural-frequency vibration of the flywheel appears on the torque record. However, the ratio of the natural frequency of the accelerometer to the electrical torques in the machine is sufficiently high so that little difficulty is encountered in interpreting the record.

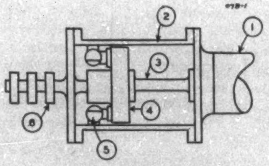

Figure 1. Accelerometer

1—Generator shaft to which accelerometer is fastened
2—Case of accelerometer
3—Torque shaft
4—Flywheel
5—Magnetic strain gauge
6—Collector assembly

The strain-gauge circuit was modified so that it contained two gauges and an electrical filter, as shown in figure 2. The two strain gauges were connected so that the torsional sensitivity was doubled, and lateral movement was cancelled. The filter removed the power input frequency from the record. This circuit has been extensively used in other places and is described at some length in previous papers.[2]

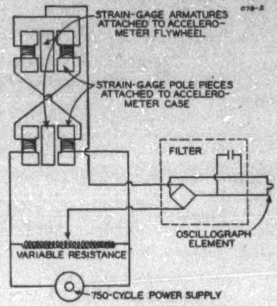
078-2
STRAIN-GAGE ARMATURES ATTACHED TO ACCELEROMETER FLYWHEEL
STRAIN-GAGE POLE PIECES ATTACHED TO ACCELEROMETER CASE
FILTER
OSCILLOGRAPH ELEMENT
VARIABLE RESISTANCE
750-CYCLE POWER SUPPLY

Figure 2. Strain-gauge circuit

In order to eliminate the need of measuring extremely small displacements, the natural period of the accelerometer was set at 270 cycles per second and the magnification of the various components of the records taken into account in the calibration.

Since it was impossible to prevent other portions of the instrument besides the torque rod from deflecting, it was considered necessary to calibrate this instrument dynamically. A setup was made (see figure 3) whereby an oscillatory acceleration could be applied to the rear end of the accelerometer case. Oscillograph records were taken of the flywheel movement (see figure 4) and at the same time the vibration amplitude at the flange of the case was measured. With these data it was

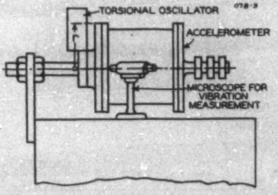

Figure 3. Dynamic-calibration setup

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1. For all numbered references, see list at end of paper.

Table I

Type of Short Circuit	Torque Equations T = Per Unit Torque Based on Kw Equal to Rated Three-Phase Kva
Three phase	$T=\frac{e_0^2}{x_d''}FA\sin\omega t+\left(\frac{e_0}{X_d''}\right)^2(R_1+R_e)F^2+\left(\frac{e_0}{X_d''}\right)^{1.8}\left(\frac{R_2-R_1}{\sqrt{2}}\right)A^{1.8}$ where $F=\frac{X_d''}{X_d}+\frac{X_d''(X_d-X_d')}{X_dX_d'}\epsilon^{\frac{-t}{T_d'}}+\left(1-\frac{X_d''}{X_d'}\right)\epsilon^{\frac{-t}{T_d''}}$ $F\approx\frac{X_d''}{X_d'}\epsilon^{\frac{-t}{T_d'}}+\left(1-\frac{X_d''}{X_d'}\right)\epsilon^{\frac{-t}{T_d''}}$ $A=\epsilon^{\frac{-t}{T_a}}$
Line to line	$T=\frac{e_0^2}{X_d''+X_2}[2FA\sin(\omega t+\alpha)-F^2\sin 2(\omega t+\alpha)]+\left(\frac{e_0}{X_d''+X_2}\right)^2\times$ $(R_1+R_e)(1-\cos 2(\omega t+\alpha))F^2+\left(\frac{2e_0}{X_d''+X_2}\right)^{1.8}\left(\frac{R_2-R_1}{\sqrt{2}}\right)A^{1.8}+$ $\left(\frac{e_0}{X_d''+X_2}\right)^{1.8}(R_2-R_1)\left[\frac{(2A)^{1.8}}{\sqrt{2}}+2F^{1.8}\right]$ where $F=\frac{X_d''+X_2}{X_d+X_2}+\frac{(X_d''+X_2)(X_d-X_d')}{(X_d+X_2)(X_d'+X_2)}\epsilon^{\frac{-t}{T_{d0}}\left(\frac{X_d+X_2}{X_d'+X_2}\right)}+$ $\frac{X_d'-X_d''}{X_d'+X_2}\epsilon^{\frac{-t}{T_d''}\frac{X_d''(X_d'+X_2)}{X_d'(X_d''+X_2)}}$ $F\approx\frac{X_d''+X_2}{X_d'+X_2}\epsilon^{\frac{-t}{T_{d0}}\left(\frac{X_d+X_2}{X_d'+X_2}\right)}+\frac{X_d'-X_d''}{X_d'+X_2}\epsilon^{\frac{-t}{T_d''}\frac{X_d''(X_d'+X_2)}{X_d'(X_d''+X_2)}}$ $A=\epsilon^{\frac{-t}{T_a}}\cos\alpha$
Line to neutral	$T=\frac{e_0^2}{\sqrt{(X_d''+X_2+X_0+3X_e)^2+(R_1+3R_e)^2}}[2FA\sin(\omega t+\alpha)-F^2\sin 2(\omega t+\alpha)]+$ $\frac{1.5e_0^2(R_1+R_e)(1-\cos 2(\omega t+\alpha))}{(X_d''+X_2+X_0+3X_e)^2(R_1+3R_e)^2}F^2+\left(\frac{e_0}{X_d''+X_2+X_0+3X_e}\right)^{1.8}\times$ $(R_2-R_1)\left[\frac{(2A)^{1.8}}{\sqrt{2}}+2F^{1.8}\right]$ where $F=\frac{X_d''+X_2+X_0+3X_e}{X_d+X_2+X_0+3X_e}+\frac{(X_d''+X_2+X_0+3X_e)}{(X_d'+X_2+X_0+3X_e)}\frac{(X_d-X_d')}{(X_d+X_2+X_0+3X_e)}\times$ $\epsilon^{\frac{t-}{T_{d0}}\left(\frac{X_d+X_2+X_0+3X_e}{X_d'+X_2+X_0+3X_e}\right)}+\frac{X_d'-X_d''}{X_d'+X_2+X_0+3X_e}\epsilon^{\frac{-t}{T_d''}\frac{X_d''[X_d'+X_2+X_0+3X_e]}{X_d'[X_d''+X_2+X_0+3X_e]}}$ $A=\epsilon^{\frac{-t}{T_a}}\cos\alpha$ $F\approx\frac{X_d''+X_2+X_0+3X_e}{X_d'+X_2+X_0+3X_e}\epsilon^{\frac{-t}{T_{d0}}\left(\frac{X_d+X_2+X_0+3X_e}{X_d'+X_2+X_0+3X_e}\right)}+\frac{X_d'-X_d''}{X_d'+X_2+X_0+3X_e}\times$ $\epsilon^{\frac{-t}{T_d''}\frac{X_d''(X_d'+X_2+X_0+3X_e)}{X_d'(X_d''+X_2+X_0+3X_e)}}$
Line to line to ground	$T=\frac{e_0^2}{3(X_0X_d''+X_2X_d''+X_0X_2)}\times$ $\sqrt{\left(X_2^2+\frac{X_0X_2}{2}+X_0^2\right)\left[2\sin^2\theta\cos^2 m+\sin^2(\theta+m)\right]}FA\sin(\omega t+\theta)+$ $\frac{e_0^2X_0}{X_0X_d''+X_d''X_2+X_0X_2}F^2\sin 2(\omega t+\theta)+\left(\frac{X_0e_0}{X_0X_d''+X_d''X_2+X_2X_0}\right)^{1.8}2(R_2-R_1)F^{1.8}+$ $\left[\frac{e_0\sqrt{X_2^2+\frac{X_1X_0}{2}+X_0^2}}{3(X_0X_d''+X_d''X_2+X_0X_2)}\right]^{1.8}\frac{R_2-R_1}{\sqrt{2}}A^{1.8}+\left[\frac{e_0\sqrt{X_2^2+\frac{X_2X_0}{2}+X_0^2}}{X_0X_d''+X_d''X_2+X_0X_2}\right]^2\times$ $\left(\frac{R_1+R_e}{3}\right)F^2[1-\cos 2m\cos 2(\omega t+\theta)]$ where $m=30°+\tan^{-1}\frac{0.866X_0}{0.5X_0+X_2}$, θ = angle of the voltage wave of the unshort-circuited phase at the instant of short circuit $A=\epsilon^{\frac{-t}{T_a}}$ $F\approx\frac{X_d''(X_2+X_0)+X_2X_0}{X_d'(X_2+X_0)+X_2X_0}\epsilon^{\frac{-t}{T_{d0}}\frac{X_d'(X_2+X_0)+X_2X_0}{X_d(X_2+X_0)+X_2X_0}}+\frac{(X_d'-X_d'')(X_2+X_0)}{X_d'(X_2+X_0)+X_2X_0}\times$ $\epsilon^{\frac{-t}{T_d''}\left(\frac{X_d''}{X_d'}\right)\left(\frac{X_d'(X_2+X_0)+X_2X_0}{X_d''(X_2+X_0)+X_2X_0}\right)}$

possible to calculate the torque imposed on the flywheel and relate it to the amplitude measured from the oscillogram. In order to check and correlate the sensitivity of the instrument between setups, static torque versus oscillograph deflection records were taken and are plotted in figure 4. The 11 per cent difference shown is due to the aforementioned displacement of case, gauge mounting, etc., and corresponds to values calculated for these displacements. The actual use of this calibration is described in the appendix.

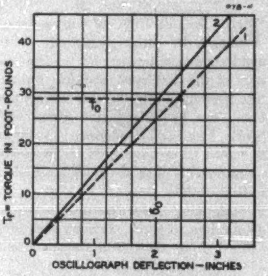

Figure 4. Accelerometer calibration

1—Static calibration curve (by application of static torque to flywheel)

2—Dynamic calibration curve

Calculation of Electrical Air-Gap Torque During Short Circuit

It is known that in all machines high alternating torques are developed during short circuit. It is also known that during some types of short circuits the machine seems to "load up" during the first few cycles rather than dump load as might be expected. The calculation of the alternating components of torque caused by the reaction of the fundamental fluxes and currents in the machine during short circuit is handled in a very thorough manner by Nickle, Pierce, and Henderson.[5] These component values checked the test values very closely. It appears from this test that these calculated values tend to be between five and ten per cent high due to flux distortion and several other factors. However, this accuracy can be considered very good for this type of calculation, especially since it tends to be pessimistic. The tendency of a machine to "load up" during short circuit is due to loss torques, which have been neglected by most previous authors. Mr. Kilgore in a later paper[4] attempted to account for the

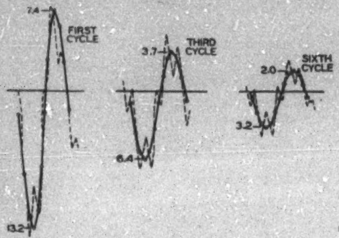

Figure 5. Three-phase short-circuit torques

Solid lines—Calculated torque
Dash lines—Test torque
Numbers are per-unit torque

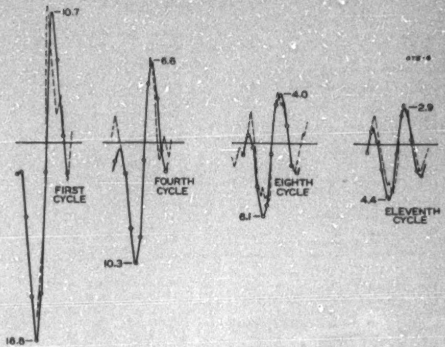

Figure 6. Line-to-line short-circuit torques

Solid lines—Calculated torque
Dash lines—Test torque
Numbers are per-unit torque

loss torques. However, he assumed that every loss in the machine resulted in torque and with this in mind arrived at an equation which gives results which are much too large. The loss or unidirectional torque is of prime importance to the central-station engineer in the calculation of stability. There are three types of unidirectional torque, namely, a steady torque, a displaced rated-frequency component, and a displaced double-frequency component. The rated-frequency component of loss torque appears to be negligible in all types of short circuits and consequently will be disregarded.

Since this paper is primarily interested in turbine generators, all equations will be given as for turbine generators. For modifications to suit salient-pole machines, the fundamental flux components (sin ωt and sin $2\omega t$) may be modified in line with Nickle, Pierce, and Henderson's paper[3] to take care of higher order harmonics due to saliency.

It will be noted that in the equations presented in table I, the torque does not start from zero. This is due to several neglected components which die out very rapidly.

It will also be noted in table I that several of the loss torque components vary as the 1.8 power of the current. This has been shown to be approximately correct for a turbine generator.[3] For salient-pole machines this exponent will be 2.

The approximations for the factor F found in the equations are the result of neglecting the sustained short-circuit current and result in only slightly lower torque after eight or nine cycles. Since few torque calculations extend beyond ten cycles, this approximation is justified.

The mechanical design engineer will be interested chiefly in the alternating torques, while the stability engineer will be interested in the unidirectional components. It will be noted that the $(1-\cos 2\omega t)$ terms are unidirectional, but for stability calculations the $\cos 2\omega t$ may be omitted.

It is believed the above equations present a good engineering calculation for the torques encountered in a turbine generator under short circuit. The oscillograms shown in figures 5, 6, and 7 have the test record plotted in dotted lines, while the calculated torque is plotted in solid lines to the same scale. The peak torques are marked in terms of per unit torque based on the three-phase kilovolt-ampere rating of the machine, which in this case is 12,500 kw.

The natural frequency of the accelerometer adds to some peaks while it apparently detracts from others. Considering this when observing the oscillograms, the agreement between test and calculated peaks is better than ten per cent in the three-phase and the line-to-line cases. The line-to-neutral short circuits do not agree quite so well in the case of a reactor in the neutral when the current is asymmetrical. The calculated sin ωt term appears to be about 30 per cent larger than the test value indicates. However, the sin $2\omega t$ term agrees very well.

The rated voltage values of the test machine reactances are listed in table II. These reactances are slightly lower than the average machine of this rating, resulting in large currents during short circuit. The test currents and torques for various types of short circuits are also tabulated.

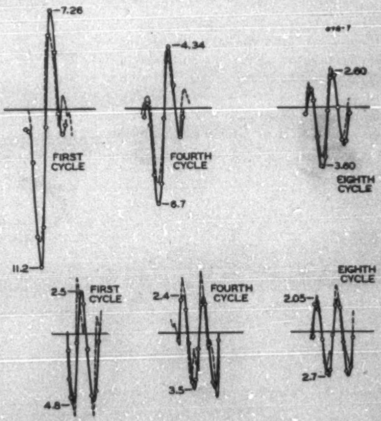

Figure 7. Line-to-neutral short-circuit torques with 0.357 ohm reactance in neutral

Upper curve—Current completely asymmetrical
Lower curve—Current symmetrical
Solid lines—Calculated torque
Dash lines—Test torque
Numbers are per-unit torque

Table II

Constant	X_d''	X_d'	X_d	X_2	X_0	T_d''	T_d'	T_{d0}	T_a
Test Value	0.087	0.118	1.20	0.087	0.010	0.040	0.48	4.88	0.108

Type of Short Circuit	Per Unit	Scalar
Three phase		
(*A*). Peak current recorded in worst phase	22.3	16,500 amp
(*B*). Average rms instantaneous symmetrical current	11.5	6,000 amp
(*C*). Peak torque	13.2	165,000 kw
Line to line		
(*A*). Current totally asymmetrical		
1. Peak measured current	22.0	16,300 amp
2. Rms symmetrical instantaneous current	11.8	6,180 amp
3. Peak torque	16.8	210,000 kw
(*B*). Current practically symmetrical		
1. Peak measured current	11.5	8,500 amp
2. Rms symmetrical instantaneous current	10.2	5,300 amp
3. Peak torque	7.6	95,000 kw
Line to neutral with 0.148 ohm or 0.0097 per unit reactance in neutral		
(*A*). Current totally asymmetrical		
1 Peak measured current	28.1	20,800 amp
2. Rms symmetrical instantaneous current	15.0	7,850 amp
3. Peak torque	13.0	162,500 kw
(*B*). Current practically symmetrical		
1. Peak measured current	15.3	11,300 amp
2. Rms symmetrical instantaneous current	9.2	6,800 amp
3. Peak torque	5.7	71,300 kw

All data are for 100 per cent voltage short circuits with comparisons for various conditions of asymmetry for the single-phase cases.

Conclusion

The torque discussed in this paper is the electrical torque developed at the air gap of the machine and is not the torque at the coupling or that transmitted to the foundation. The torque in couplings, shafts, and foundations must be calculated from a consideration of the masses and mechanical spring constants involved.

Previous published equations for the torque developed in a synchronous machine during short circuit have been almost entirely theoretical. As far as is known, the test results of this paper constitute the first experimental check of the theoretical equations as applied to a turbine generator. The test results show that the alternating components of torque exist in substantially the same magnitude as calculated theoretically. The test results also indicate the existence of loss torques of a magnitude sufficient to require a method of calculation superior to any heretofore available. It is believed that the terms added to previous torque equations by the authors represent the retardation torques quite accurately.

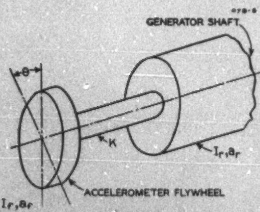

Figure 8. Analytical diagram of accelerometer

Appendix I

An analysis of the accelerometer is as follows:

I_r = moment of inertia of the rotor
a_r = angular acceleration of the rotor
K = spring constant of torque rod
I_f = moment of inertia of flywheel
a_f = acceleration of flywheel. $a_f'' = \frac{d^2a_f}{dt^2}$
θ = angular displacement of flywheel from the equilibrium position

$$\theta'' = \frac{d^2\theta}{dt^2}$$

$$p^2 = \frac{K}{I}$$

See figure 5.

The acceleration of the rotor is equal to the acceleration of the flywheel plus the first derivative of the rate of increase of the angle θ.

$$a_r = a_f + \frac{d^2\theta}{dt^2} \qquad (1)$$

The torque in the torque shaft equals the torque due to the flywheel acceleration.

$$Ia_f = T = K\theta$$

differentiating

$$I\frac{d^2a_f}{dt^2} = K\frac{d^2\theta}{dt^2}$$

or

$$\theta'' = \frac{I}{K}a_f''$$

substituting in (1)

$$a_r = a_f + \frac{I}{K}a_f''$$

rewriting in the usual form

$$a_f'' + \frac{K}{I}a_f = \frac{K}{I}a_r$$

or

$$a_f'' + p^2a_f = p^2a_r \qquad (2)$$

The rotor acceleration may be expressed in two forms:

$$a_r = \Sigma A_0\epsilon^{-\beta t} + \Sigma A_1\epsilon^{-\alpha t}\sin(\omega t + \theta) \qquad (3)$$

where β, α, A_1, ω, and θ may have at least two different values.

The complementary function of the solution of (2) will have the following form:

$$a_f = C_1\cos pt + C_2\sin pt$$

so that the rotational velocity of the free vibration will have the value p.

The above expression will not be considered after this for the reason stated in the discussion of the instrument.

The particular solution will be the sum of the solutions for each component given in (3). The particular solution for the form given in the first member of (3) will be

$$a_f = A\epsilon^{-\beta t}\frac{1}{\left(\frac{\beta}{p}\right)^2 + 1} \qquad (4a)$$

For the second form it will be

$$a_f = \frac{p^2A_1\epsilon^{-\alpha t}[(\alpha^2 + p^2 - \omega^2)\sin(\omega t + \theta) + 2\omega\alpha\cos(\omega t + \theta)]}{(\alpha^2 + p^2 - \omega^2)^2 + 4\omega^2\alpha^2} \qquad (4b)$$

(4*b*) may be simplified to

$$a_f = \frac{A_1\epsilon^{-\alpha t}\sin(\omega t + \theta + \phi)}{\sqrt{\left(\frac{\alpha}{p}\right)^2 + 1 - \left(\frac{\omega}{p}\right)^2 + \left(\frac{2\omega\alpha}{p^2}\right)^2}} \qquad (4c)$$

where

$$\phi = \tan^{-1}\frac{2\omega\alpha}{\alpha^2 + p^2 - \omega^2}$$

Now in this particular case α and β are small compared to ω and p. Therefore, a further simplification can be made in the above equations which introduces negligible errors. The amount of error may be ascertained by substitution of proper values in (5*a*) and (5*b*) and comparing the results.

The simplified equations will be

$$a_f = A\epsilon^{-\beta t} \qquad (5a)$$

$$a_f = A\epsilon^{-\alpha t}\sin(\omega t + \theta)\cdot\frac{1}{1 - \left(\frac{\omega}{p}\right)^2} \qquad (5b)$$

The largest effective α or β in this case is about 25, $\omega = 120 \times 2\pi$, $p = 274 \times 2\pi$; substituting in the above equations.

$$\left.\begin{array}{l}\text{From (4}a\text{), } a_f = 0.991A\epsilon^{-\beta t}\\ \text{From (5}a\text{), } a_f = 1 \times A\epsilon^{-\beta t}\end{array}\right\}\text{ error 1 per cent}$$

$$\left.\begin{array}{l}\text{From (4}c\text{), } a_f = 1.22A\epsilon^{-\alpha t} \times \sin(\omega t + \theta)\\ \text{From (5}b\text{), } a_f = 1.236A\epsilon^{-\alpha t} \times \sin(\omega t + \theta + \phi)\end{array}\right\}\text{ error } 1^1/_2\text{ per cent}$$

From (4*c*), $\phi = \tan^{-1} 0.002$
Therefore ϕ is assumed 0

This can only be done if β and α are small. The error will increase rapidly with larger values.

Calibration Relationships

Refer to discussion on instrument calibration

T_f = torque on the flywheel during calibration
r = radius of flange
R = one-half of measured amplitude
$w_c = 2\pi f_c$, where f_c is the vibration frequency secured from the calibration record
T_0 = value from calibration curve
δ_0 = value from calibration curve
δ = measured value of deflection from record

It may be deduced from (5*b*) that

$$T_f = \left[\frac{R\omega_c^2 \sin \omega t}{1-\left(\frac{\omega_c}{p}\right)^2}\right]\frac{I_f}{r}$$

T_f was plotted against δ_0 to secure curve of figure 4.

Calibration Constants Applied to Test Data

It may be shown that

$$\delta = \left[\frac{1}{1-\left(\frac{\omega}{p}\right)^2}\cdot\frac{I_f}{I_r}\cdot\frac{\delta_0}{12T_0}\right]T_r$$

In order to transfer T_r into the ordinarily used T_{el} or times rated torque units, it is only necessary to multiply by 0.511/rated kw so that

$$\delta = \left[\frac{1}{1-\left(\frac{\omega}{p}\right)^2}\cdot\frac{I_f}{I_r}\cdot\frac{\delta_0}{12T_0}\cdot\frac{\text{rated kw}}{0.511}\right]T_{el}$$

For this particular case

$\delta_1 = 0.196T_{el}'$—unidirectional components
$\delta_2 = 0\ 206T_{el}''$—60-cycle components
$\delta_3 = 0\ 243T_{el}'''$—120-cycle components

Appendix II

The terms which have been added to previous torque equations by the authors have been derived from theoretical considerations, although it is realized that a good many small factors are omitted. The factors considered include the following: First, the torque caused by the rotor cutting the flux trapped in the stator winding. Second, the torque caused by I^2R, where R includes the positive-sequence resistance and external resistance. Third, the torque caused by the negative-sequence component of the stator current in the case of single-phase short circuits.

When flux is trapped in the stator winding at the instant of short circuit, the flux remains stationary in physical position while the rotor continues to rotate. In principle this is exactly the same as a locked-rotor starting condition with sufficient current in the stator to produce a flux equal to that trapped in the stator winding. The resultant torque equals

$$\left(\frac{e_0}{Z}\right)^{1.8}R_{rs}A^{1.8}$$

where Z is the impedance for the particular short-circuit condition and R_{rs} is the rotor resistance component of the starting impedance. Since* R_{rs} is very nearly equal to $\frac{R_2-R_1}{\sqrt{2}}$* this substitution is made to eliminate unnecessary additional constants. This loss in a turbine generator varies approximately as the 1.8 power of the current.[6]

The second component is the loss due to positive-sequence resistance. The d-c component of stator current is neglected because this current is the result of changing stored magnetic energy and results in no direct torque. Hence, only the alternating current is considered. The resultant equation is

$$\left(\frac{e}{Z}\right)^2(R_1+R_e)F^2$$

for a three-phase short circuit. For the single-phase case this is a pulsating unidirectional double-frequency loss so that this term becomes

$$\left(\frac{e}{Z}\right)^2(R_1+R_e)\frac{[1-\cos 2(\omega t+\alpha)]}{2}F^2$$

The third component of unidirectional torque is the result of the negative-sequence component of stator current (I_2) squared times the negative-sequence resistance (R_2). It is a well-known fact that when negative-sequence voltage is applied to the armature of a machine only one-half the total loss appears as input to the armature and the other half appears as torque. However, in the case of a single-phase short circuit all the loss appears as torque due to transformer action between the sequences. Thus, the loss torque due to negative-sequence current becomes $I_2^{1.8}2R_2$. This loss also varies approximately as the 1.8 power of current. In the equations of table I, all current terms are converted to a per unit equivalent three-phase current. Since the loss due to R_1 has been taken care of in another component, this component becomes

$$I_2^{1.8}2(R_2-R_1)F^{1.8}$$

which for the line-to-line case is

$$\left(\frac{e_0}{X_2+X_d''}\right)^{1.8}2(R_2-R_1)F^{1.8}$$

List of Symbols

All units are in per unit terms.

T = per unit electrical torque based on rated kilovolt-amperes
X_d'' = subtransient reactance
X_d' = saturated transient reactance
X_d = synchronous reactance
X_2 = negative-sequence reactance
X_e = external reactance
X_0 = zero-sequence reactance
T_{d_0} = open-circuit transient time constant
T_d' = transient time constant
T_d'' = subtransient time constant
T_a = armature time constant
t = time after application of short circuit
e_0 = per unit voltage just previous to short circuit
ϵ = 2.718 = base of natural logarithms
R_1 = positive-sequence resistance
R_2 = negative-sequence resistance
R_e = external resistance
R_{rs} = rotor resistance component of the starting impedance

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Discussion

For discussion, see page 1195.

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* See authors' concluding discussion.

Application of and Operating Experience With Hydrogen-Cooled Synchronous Condensers and Alternators

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I. Introduction

UNTIL about ten years ago the only cooling medium utilized for electrical rotating machinery was air. Obviously this was occasioned not by the fact that air has any particularly suitable characteristics but because it is the daily common surrounding medium which, of necessity, must be used for open-type machinery. Before that time, the need for construction of machines of constantly increasing size and speed had brought about the development of closed systems of ventilating. In these, facilities had been provided for conditioning the cooling air, but this had been primarily for the purpose of removing dust particles. The elimination of the particularly objectionable oxygen had not been attempted although for some time inert gas had been used in transformers to prevent oxidation of oil or insulation.

Since the use of any medium other than air necessitates a gastight enclosure, it is desirable to use that particular gas or mixture of gases, which offers the most advantages to outweigh the fact that more expensive construction is required. The desirability of using hydrogen for this purpose was first suggested by Doctor W. R. Whitney of the General Electric Company about 18 years ago, and as a result a large amount of study and numerous tests were made to determine the advantages of employing this particular gas. The superiority of hydrogen was definitely indicated especially in the case of large high-speed alternators. The difficult problem of a simple and reliable shaft seal, as well as the ability of designers to utilize air cooling satisfactorily on machines required by the power industry, delayed the use of hydrogen in generators until several years ago.

In the meanwhile attention was directed to the hydrogen-ventilated synchronous condenser, and in 1928 two such machines were placed in service, one of which was the 20,000-kva unit at the Turner station of the Appalachian Electric Power Company. This was followed at intervals, by seven others on the systems with which the authors are associated, comprising in all a combined capacity of 161,000 kva. The successful operation of these machines led to the installation in 1937 of a 50,000-kva hydrogen-cooled generator at Logan, W. Va. This was followed by a 66,667-kva generator placed in service the early part of 1939. An additional condenser was placed in operation in July 1939; still another in November 1939, and two more generators are in process of installation and will go into service early in 1941, making a combined total of 432,000 kva on the American Gas and Electric Company systems.

There has thus been obtained an experience with hydrogen, on the systems with which the authors are associated and under their operating supervision, of combined continuity and on size and rating of equipment far greater than has been obtained anywhere else. It is the purpose of this paper to describe that experience and indicate its bearing on the design, application, and operation of hydrogen-cooled rotating equipment.

II. The Basic Problems Presented by Hydrogen Cooling

Before proceeding with a description of the installations involved, it may be desirable to review briefly the advantages of hydrogen and to discuss the basic problems involved in its application.

A. ADVANTAGES OF A HYDROGEN ATMOSPHERE

The basic advantages which are obtained because of the characteristics of hydrogen have been covered elsewhere.[1-5] In briefest summary they are: low density, high thermal conductivity, high forced heat convection, prevention of detrimental effect from corona, no combustion or oxidation, cleanliness of atmosphere, ease of adaptation to outdoor service, and quiet operation.

B. BASIC PROBLEMS IN DESIGN, APPLICATION, AND OPERATION OF HYDROGEN-COOLED EQUIPMENT

The basic problems involved in the design, application, and operation of hydrogen-cooled rotating machinery, and particularly synchronous condensers* are, in order of importance, as follows:

1. *Gastight and Explosionproof Outer Shell*

While the possibility of an explosion is extremely remote, it is nevertheless necessary to take every reasonable precaution to minimize the effect of such an occurrence and therefore an explosionproof shell (that is, a shell which will not blow apart) consisting of boiler plate has been employed from the first designs on. In general, the ends of the shell are closed by means of two dome-shaped end bells, with the joints gasketed to prevent leakage of hydrogen. In most cases, castings, which are frequently porous, have been eliminated. This has helped in obtaining a gastight construction. The problem of welding these shells has been a difficult one particularly from the standpoint of gas leakage. The carrying through the shell of the miscellaneous oil, water, and gas piping as well as electrical connections has been solved quite satisfactorily. Continued study and design changes for the purpose of decreasing gas leakage at pressures of 15 pounds per square inch or more have been required to obtain the maximum benefit from hydrogen cooling.

2. *Water Cooling*

The development of internal surface coolers of utmost reliability was essential, since the coolers are ordinarily inaccessible. It was desirable to install the coolers in such a manner as to permit periodic cleaning without removal of gas from the machine. As far as operation is concerned this also introduces the problem of a suitable and adequate water supply and some system for cooling the water such as a spray pond or cooling tower. Water cooling of the bearings also introduced some difficulties such as adequate flow to the cooling coils, and insulation of

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[1] For all numbered references, see list at end of paper.

* The problem of shaft sealing applicable to generators is discussed later in section IX.

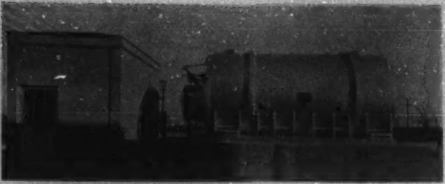

Figure 1. 36,000-kva 11,500-volt hydrogen-cooled synchronous condenser at the Fostoria station of The Ohio Power Company

the piping to prevent the flow of shaft currents.

3. *Collector Compartment, Rings, and Brushes*

For all synchronous condensers, excitation has been obtained externally from motor-driven or electronic exciters. The problem of finding the proper design and material for brush rigging, brushes, and collector rings for the fields of the condensers suitable for operation in hydrogen, was one that had to be determined largely by actual experience. The ventilating of the collector compartment, and the accumulation of brush dust, brought up the questions of how often to clean the compartment and how to prevent any appreciable amount of this dust from being carried into the stator and rotor.

4. *Provision for Maintenance and Inspection of Condensers*

In general, manholes have been provided for inspection of the interior and for minor repairs, including bearing inspection or replacement. Provision of overhead lifting facilities for complete disassembly of the machine has been taken care of more simply on the later installations. For periodic inspection of the collector compartment, various types of seal have been developed, with experience indicating what changes were desirable.

5. *Hydrogen Accessory Equipment*

Reliable equipment was required for measuring gas purity and for feeding hydrogen to the machine automatically to maintain purity and pressure. Apparatus and instruments for this purpose were available for the most part and there has been no major difficulty to overcome.

6. *Scavenging*

The question of the proper procedures to be used in changing from an air to a hydrogen atmosphere and vice versa, has been given much study and field testing. Provision is included on all machines for scavenging with carbon dioxide if desired, but on the systems with which the authors are associated scavenging is today not employed on condensers.

III. Description of Condenser Installations

In table I are listed all the hydrogen-cooled synchronous condensers installed on the properties of the American Gas and Electric Company systems. Pertinent data covering each machine are included. Since the Fostoria unit is one of the machines more recently installed and because it is representative of the latest developments, this particular installation will be described in detail. A good many of the other units are installed in a more or less similar manner. Where, however, important or interesting features at these other locations exist, they will be described specifically farther on in the text.

A. Fostoria 36,000-Kva Synchronous-Condenser Installation

1. *General*

This 36,000-kva synchronous condenser was installed in December 1936 at the West End station, Fostoria, Ohio, on the system of The Ohio Power Company. Connected through a 40,000-kva four-winding transformer bank to the 132-kv bus, it provides necessary voltage regulation and stability for the 132-kv transmission system in the northwestern portion of Ohio.[6] The unit is quite similar to the 30,000-kva machine at Fort Wayne, Ind., which was installed in 1931. By modifying the design of the frame and accessories for operation at 15 pounds per square inch the increased capacity was obtained. A view of this installation is given in figure 1. The condenser is automatically controlled but supervised by the substation operator in an adjacent control building.

2. *Foundations and Housing for Equipment*

As previously stated, the hydrogen-cooled machine because of its gas-tight construction is naturally weatherproof and therefore adapts itself easily to an

Table I. Hydrogen-Cooled Synchronous Condensers in Service

Listing as of November 30, 1939

			Rating			Type of		
Location	Operating Company	Starting Date for H_2 Operation	Kva	Kv	Speed in RPM	Installation	Water Cooling	Excitation
Turner	Appalachian Electric Power Company	12/16/28	20,000	11.5	900	Outdoor	Spray pond	Motor generator set
Atlantic City	Atlantic City Electric Company	9/30/30	15,000	4.15	900	Indoor	Plant makeup*	Motor generator set
Fort Wayne	Indiana & Michigan Electric Company	2/1/31	30,000	11.5	1,200	Outdoor	Spray pond	Motor generator set
Benton Harbor	Indiana & Michigan Electric Company	2/17/31	15,000	11.5	900	Outdoor	Spray pond	Motor generator set
Scarbro	Appalachian Electric Power Company	2/25/31	15,000	24.0	900	Outdoor	Cooling tower	Motor generator set
Scranton	The Scranton Electric Company	9/9/33	15,000	4.15	900	Indoor	Plant makeup	Electronic
Kenova	Appalachian Electric Power Company	10/20/35	15,000	11.5	900	Indoor	Cooling tower	Motor generator set
Fostoria	The Ohio Power Company	12/18/36	36,000†	11.5	1,200	Outdoor	Cooling tower	Electronic
Torrey	The Ohio Power Company	7/25/39	36,000†	11.5	1,200	Outdoor	Spray pond	Electronic
New Carlisle	Indiana & Michigan Electric Company	**	30,000†	11.5	3,600	Outdoor	Cooling tower	Electronic

* Originally used salt water—changed in 1933 to fresh water.

† Rating at 15 pounds per square inch hydrogen pressure. Other machine ratings at 1/2 pound per square inch hydrogen pressure.

** This unit now installed. Operation in hydrogen expected in December 1939.

outdoor installation, thus dispensing with a building for housing it. The Fostoria installation is a typical example of this. The concrete foundations are constructed to provide a suitable pit for housing incoming leads, pumps, piping, and other related equipment. Reference to figure 1 will show that a small entrance structure is located to the left of the condenser. This not only provides an entrance to the pit, but also furnishes space for the hydrogen supply bottles and gas control equipment shown in figure 2. From here steps lead down to the chamber immediately below, in which is located the electronic exciter as shown in figure 3. This chamber is adjacent to the pit immediately beneath the condenser. A compact and economical arrangement thus results. This chamber as well as the pit are both well ventilated to remove the heat losses of the excitation equipment and also to eliminate any possibility of leakage hydrogen being pocketed.

3. *Outer-Shell Construction*

The outer shell is in three sections, a central portion which contains the stator and rotor and supports the bearings, and two end sections which house the coolers. This arrangement is similar to that of the Turner 20,000-kva condenser described in a previous paper.[2] The shell is constructed of boiler plate and is explosion-proof even for operation at 15 pounds per square inch gas pressure. It is made gas-tight by the use of rubber rings installed in grooves at the main joints between shell sections. In addition the usual vellumoid gaskets are installed on these same joints but on the outer portion of the surfaces to assist in sealing the shell and also to prevent deterioration of the rubber rings. Referring to figure 1 there can be seen at the left end of the machine, the housing which is provided for the collector rings and brush rigging. An easily removed cover plate facilitates inspection and adjustment.

Figure 2. Front view of hydrogen-control equipment—Fostoria 36,000-kva synchronous condenser

4. *Cooling System*

Each end of the machine contains two vertical fin-type surface coolers. The cooler heads at the bottom of the machine can be removed and the tubes cleaned without dismantling the machine or removing the gas. By means of integrally mounted rotor fans the hydrogen is caused to circulate inward along the rotor, then outward radially through the stator, and finally through the four coolers and back to the rotor fans. The collector compartment is cooled by means of small radial fans on the shaft which circulate gas from the main shell to this chamber and back again through a connecting pipe at the top of the compartment.

The external water-cooling system consists of a cooling tower to which the water is pumped through the surface coolers by means of two vertical motor-driven pumps mounted on the wall of the pit. The surface coolers are designed to remove full-load heat losses from the machine when 200 gallons of ingoing water per minute at a temperature not exceeding 85 degrees Fahrenheit are used. Each pump has a capacity of 275 gallons per minute permitting normal operation with only one pump. This cooling system also supplies necessary water for the water-cooled bearings, and for the heat exchanger required for cooling the tubes of the electronic exciter.

5. *Bearings and Lubrication System*

The machine uses bearings of standard design with oil rings. Particular attention has been directed to the problem of preventing emission of oil vapor. A copper cooling coil is cast into the bottom half of each bearing. An indicating thermometer bulb and a resistance temperature detector are installed in the bottom half of each bearing. Both bearings are insulated from the frame of the machine, and obviously the water piping must likewise be insulated. Insulated ground connections are brought out from each bearing and normally one of these is connected to ground.

Pipe connections are brought outside the shell for conveniently filling or draining the bearing wells. Magnetic-type gauges located on the side of the condenser shell show the level of oil in each bearing. Connections are brought to each bearing for floating the shaft when starting the machine and two small high-pressure oil pumps are installed for this purpose.

6. *Excitation Equipment*

For simplification purposes separate excitation has been used for hydrogen-ventilated machines. This offers, therefore, an ideal application for an electronic exciter,[7] and such an exciter is installed on the Fostoria machine. A view of the excitation equipment is given in figure 3. This shows seven cabinets, consisting of six tube panels with control, disconnecting and grounding devices, and one regulating and control panel shown on the extreme right.

Figure 3. Front view—electronic-exciter cabinets—Fostoria 36,000-kva synchronous condenser

7. *Collector Compartment, Collector Rings, and Brushes*

To permit inspection and maintenance work on the brushes and collector rings without removing hydrogen from the machine, a separate accessible compartment is included. With the machine stationary, a seal between this compartment and the main shell is closed to prevent loss of gas. This consists of a solid rubber ring normally held a short distance away from a rotating sealing plate on the shaft. To close the seal, a rubber tube arrangement is inflated, thus moving the rubber ring horizontally against the plate above mentioned. This seal is satisfactory for preventing leakage from the main shell for pressures up to 15 pounds per square inch.

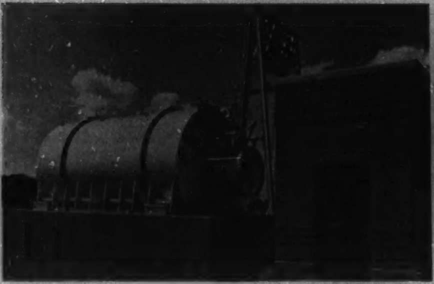

Figure 4. The Torrey 36,000-kva hydrogen-cooled synchronous condenser of The Ohio Power Company at Canton

Figure 5. The 30,000-kva 3,600-rpm hydrogen-cooled synchronous condenser at the New Carlisle station of the Indiana and Michigan Electric Company

Several windows in the collector housing permit inspection of the interior without opening the compartment. The collection of brush dust has always been a problem. Particularly bothersome has been the problem of preventing the carrying of it into the windings of the stator and rotor. A dust trap has therefore been installed on the end of the return pipe connecting from the collector compartment to the main shell, to catch the small amount of solid matter which otherwise would be blown into the main shell.

Grooved bronze-alloy collector rings are employed. Originally, metallized carbon brushes were used of the type common on practically all other similar machines, but because of unsatisfactory operation initially, nonmetallic brushes were tried and are giving excellent performance. The insulation of the brush rigging is greater than is ordinarily used for open-type collector rings in air, a greater leakage surface being required because of the accumulation of brush dust during the periods between inspection and cleaning of the compartment.

8. *Hydrogen Accessories*

Clean dry hydrogen with a purity of 99.6 per cent or more is obtained in steel bottles compressed to a pressure of approximately 2,000 pounds per square inch. By means of a double-diaphragm reducing valve, the pressure is reduced to approximately 25 pounds per square inch for feeding into the machine when operating at 15 pounds per square inch. These bottles and the other accessory equipment are shown in figure 2. The differential draft gauge shows the pressure being developed by the rotor fans, and since change in purity causes a corresponding change in density, and in fan pressure, this instrument gives a reliable and instantaneous indication of gas purity when the machine is running.

With the machine stationary, a continuous sample of gas from the main shell is allowed to pass through the filter, drier, and thermal cell (shown just below the gauge panel), and out through a bubbler bottle to the atmosphere. This entire arrangement has been previously described.[2,3] The range in variation in hydrogen purity that is registered on the indicator is from 90 to 100 per cent. With the machine running the pressure of the rotor fans is utilized to continuously circulate the gas through the analyzing equipment. The purity indicator is located on the condenser panel in the substation control room. Gas pressure in the machine is automatically maintained for either high- or low-pressure operation.

The gas is fed into the machine through a horizontal perforated pipe at the top of the inside of the shell, but the feed pipe actually enters the shell at the bottom, so that all piping, including that for water and oil, is taken care of at the bottom of the unit. This facilitates all connections, and improves the outside appearance of the unit.

9. *Facilities for Inspection and Maintenance*

The provisions supplied for periodic cleaning of the collector compartment and of the surface coolers have been already mentioned. For minor repairs inside the condenser shell, four manholes are included, two at the top and two at the bottom of the shell. There is considerable working space at each end of the machine between the coolers, which permits maintenance work on bearings, piping and other parts, without any disassembly of the shell. A small lifting frame has been developed for bolting to the top of the condenser shell. A portable hoist is easily attached to this frame and facilitates the handling of parts at the collector compartment. In conjunction with the top manholes this frame and hoist can also be used for inspection and replacement of bearings. For complete dismantling of the machine, slots have been provided on the bottom surfaces of the shell sections and by the insertion of rollers in these slots, these heavy pieces can readily be moved.

B. INTERESTING FEATURES OF OTHER SYNCHRONOUS-CONDENSER INSTALLATIONS

The Turner machine, the first unit to be installed, was purchased as a 15,000-kva air-cooled synchronous condenser, but designed for operation in hydrogen at 20,000 kva. Initial tests and early operating experience indicated entirely satisfactory and reliable performance under hydrogen cooling and therefore this machine with almost 11 years of continuous service has operated longer in hydrogen than any other similar machine in this country. The general arrangement is the same as that of the Fostoria unit with the exception that a motor-driven generator is used for excitation purposes. A brief description of this installation was included in a paper presented about ten years ago.[3]

It will be noted, that although the gas-tight shell makes this type of machine suitable for outdoor service, three of the installations are indoor. This was because floor space was available in the power plants at Atlantic City, Scranton, and Kenova at the time it was necessary to install condensers at these locations. Salt water was initially utilized at Atlantic City for the surface coolers, but difficulties with mussels, made it desirable to change over to fresh water. In the case of the Scranton unit, the first application of an electronic exciter for a synchronous condenser was employed.[7]

The Fort Wayne machine operates at 1,200 rpm and is unique in that it was the first synchronous condenser to operate at that speed. This required a new type of rotor design, the same as that later used on the Fostoria and Torrey units. The increased speed permits taking full advantage of the low windage losses of the hydrogen-ventilated machine. As a matter of fact, the design was projected into 1,200 rpm, so as to permit economies possible at that speed, knowing that the increased windage would be substantially nullified by the use of hydrogen. A view of the 1,200 rpm machine at the Torrey station is given in figure 4.

The Benton Harbor unit differs somewhat in design from the Fostoria machine in that the size of the machine was reduced by eliminating entirely the working space at each end on the inside of the unit. Therefore any inspection or maintenance work inside the shell requires the removal of one or both end bells. However, the weight of the latter is small, and therefore a light lifting structure is sufficient to facilitate work on this unit. Of interest is the fact that this machine was made completely automatic in July 1937, indicating the conviction of the authors in the reliability and safety of hydrogen-cooled machinery.

The outstanding feature of the Scarbro condenser is the design of the windings for 24,000-volt operation; the unit is thus the highest-voltage a-c rotating machine operating commercially in the United States. The connection of the condenser to the 44-kv system at this point, is made through a two-winding three-phase transformer. Tests which have been previously covered,[1] indicate that the effect of corona in a hydrogen atmosphere is relatively negligible. It was to obtain experience on this point, that a potential rating higher than that used in general practice, was chosen.

C. New Carlisle 30,000-Kva Synchronous Condenser

The possibility of taking maximum advantage of the low density of hydrogen gas by developing a 3,600-rpm synchronous condenser had occurred to any number of designers and power-system engineers. It had, as a matter of record, been discussed by one of the authors, at length, with engineers of two prominent manufacturers. There seemed to be some doubt, however, as to the point of cross over for most economical design, of the size and cost curves for distributed (two-pole) and salient-pole construction. When, however, the engineers of one of the companies evolved an economical two-pole design in a 30,000-kva size and a need for such a machine arose at the New Carlisle station of the Indiana and Michigan Electric Company, it was decided to make such an installation there.

Table II. Availability and Outage Data, Hydrogen-Cooled Synchronous Condensers

Data as of September 30, 1939

Machine Location	Total Hours From Time Put Into Service (a)	Total Hours Down for Inspection and Maintenance (b)	Total Hours Available for Service (c)	Availability Factor (d)	Total Hours Out of Service for Any Reason (e)	Total Running Hours in Hydrogen (f)	Total Running Hours in Air (g)	Total Outage Hours Due to H_2 Construction (h)	Availability Factor on Basis of H_2 Construction (i)
Atlantic City	78,888	3,723	75,165	95.28	4,301	70,694	3,893	1,621	97.95
Benton Harbor	75,552	1,450	74,102	98.08	1,834	71,840	1,878	1,142	98.49
Fort Wayne	75,936	4,230	71,706	94.43	4,685	69,186	2,065	1,116	98.53
Fostoria	24,408	3,969	20,439	83.75	4,094	18,544	1,770	301	98.77
Kenova	34,584	1,006	33,578	97.10	16,863	17,673	48	58	99.83
Scarbro	75,120	3,250	71,870	95.67	4,309	70,811		846	98.87
Scranton	52,824	442	52,382	99.16	11,055	41,769		165	99.69
Turner	94,440	1,890	92,550	98.00	2,856	91,584		720	99.24
Total	511,752	19,960	491,792	96.10	49,997	452,101	9,654	5,969	98.83
Total—machine years	58.44	2.28	56.16	96.10	5.70	51.63	1.11	0.68	98.83

At the time of writing this paper (October 1939), the installation of this unit is just being completed and it is expected that the machine will be in operation in hydrogen at an early date in December. A view of the unit as installed at New Carlisle is given in figure 5. The general arrangement is similar to that of the Fostoria station, except that the electronic exciter is located in a nearby control building. The machine is suitable for operation at a gas pressure of 15 pounds per square inch and the rating of 30,000 kva is on that basis. The results of factory tests made on this condenser in air and hydrogen are included in section VII.

IV. Operating Experience With Synchronous Condensers

A. General

So many factors influence the operation of synchronous machines in general, and of hydrogen-ventilated machines in particular, that it is extremely difficult to give an absolutely fair picture of operating experience in spite of the fact that over 58 machine years of service are covered by the eight condensers under consideration. In compiling the data, the authors have used as a starting date the first day that each particular unit was operated in hydrogen. Obviously this includes many troubles which occur on most machines only in the first few months or in the first year of operation. On the other hand, to omit any part of the actual operating experience on any of the machines might, by glossing over some pertinent information tend to distort the facts. Although as far as the authors know, no general figures are available regarding the operation of standard air-cooled condensers, they believe the tabulated data which follow, will compare very favorably with similar information on conventional machines running in air.

B. Availability and Outage Time

In table II, there has been condensed information covering the running and outage times of the first eight machines placed in service. The hours given in column *b* include any "forced" outage for inspection or repairs, and cover not only the synchronous condenser but also its accessory equipment such as excitation, oil, water, and gas systems. It does not include switching, main cables, or transformers used for supplying energy to the machine. In the case of two machines, Kenova and Scranton, which were out of service a considerable number of hours because they were not required, there have been obtained outage figures covering any "forced" inspection or maintenance which was carried out while the unit was shut down.

On this basis, availability was determined for each machine and it will be noted that each is above 94 per cent with the exception of the Fostoria unit which, as of the date this was written, had been in service somewhat less than three years. It is fully expected that over a longer period this machine will show results similar to the others.

The total running time in hydrogen for all machines was 51.63 machine years or approximately 88 per cent of the time in service. In case of troubles which necessitated the removal of hydrogen, and which could be repaired immediately, it was usually found desirable, when possible to run the unit in air at reduced capacity. Column *g* includes this running time in air.

There has been included in column *h*, outage time occasioned because of hydrogen accessories or because of constructional details necessary for operation in hydrogen. In this there have been included all inspections of the collector compartment as well as of the main shell. All lost time because of troubles with collector rings and brush rigging has been included, although it is common knowledge that collector-ring and brush trouble is all too frequent on standard synchronous condensers. Any difficulties with oil piping, water connections, or coolers inside the shell have been attributed to hydrogen construction. On this basis there has been determined an availability figure in so far as construction for operation in hydrogen is concerned, and it will be noted that the lowest availability is 97.95 per cent, with the average for all machines being 98.83 per cent.

In order to present a detailed picture of difficulties which have been encountered on these machines, there are given in table III, itemized figures showing the causes for the forced outages on the eight condensers. Further information regarding these troubles is included in the paragraphs which follow, for the principal causes of trouble. More could be given but is being withheld for lack of space. It is interesting to analyze quantitatively the various types of outages which have occurred. Inspections of main shells and collector compartments represent only 8.17 per cent of the total forced outage time. The majority of the hours lost was because of rotor difficulties (38.82 per cent) and troubles with the water cooling systems (20.07 per cent). Of importance also were the following three: excitation equipment (7.83 per cent), collector rings and brushes (7.24 per cent), and vibration (5.46 per cent). Of the forced outage only 29.9 per cent can be attributed to construction for operation in hydrogen. Hydrogen piping and accessories accounted for 0.81 per cent of the forced-outage time.

1. *Rotors and Stators*

On most of the machines no difficulties occurred in connection with the rotating fields or the stator windings. This can be attributed to the exclusion of dust, moisture, and other objectionable components which ordinarily tend to affect adversely the windings and insulation. The condition of the windings on all machines, as revealed by periodic inspections, was found to be excellent, and apparently just as good as when first placed in service. Rotor troubles on three of the machines resulted in long outages in each case, but insulation failure or deterioration did not contribute to these faults. The difficulties were primarily because of new rotor designs for 1,200-rpm operation.

2. *Water Cooling Systems*

Outage time of appreciable amounts occurred on all but one machine in connection with the water cooling systems. The utilization of spray ponds and cooling towers necessarily requires periodic cleaning of the apparatus involved. Experience with these external water cooling equipments has been satisfactory both for summer and winter operation. Some difficulty has occurred because of strong winds blowing away the spray and thus causing excessive water consumption adversely affecting the efficiency of this equipment.

Experience with the internal surface coolers has been likewise very satisfactory. In only one case has leakage occurred on an internal cooler unit and in this instance a slow leak developed at one of the vertical tube connections to its horizontal header plate. A slow but steady accumulation of water in the oil-and-water-leakage detector at the bottom of the machine gave ample warning of this difficulty, which was readily rectified by expanding the bell-shaped end of the tube into the socket of the cooler header plate.

Table III. Segregated Outage Data, Hydrogen-Cooled Synchronous Condensers

Data as of September 30, 1939

			Atlantic City	Benton Harbor	Fort Wayne	Fostoria	Kenova	Scarbro	Scranton	Turner	Total	Per Cent of Forced Outage
Total years in service			9.02	8.62	8.67	2.79	3.95	8.58	6.03	10.78	58.44	
Hours out of service	Forced outage hours	Routine inspection: Collector compartment	112	107	136	25	16	34	31	63	524	2.63
		Routine inspection: Main shell	95	180	229	*	*	160	28	414	1,106	5.54
		Bearings		247	304	157	60	20			788	3.95
		Collector-compartment seal		334					83		417	2.09
		Collector rings and brushes	406	300	166	79		492			1,443	7.24
		Excitation equipment	73	11	21	181	2	23	290	960	1,561	7.83
		H_2 piping and accessories				2				160	162	0.81
		Lubricating system	127		42	50		10		90	319	1.60
		Rotor			2,942	3,346		1,464			7,752	38.82
		Temperature equipment				12			10		22	.11
		Vibration			92			998			1,090	5.46
		Water cooling equipment	2,910	241	247	44	408	49		108	4,007	20.07
		Miscellaneous		30	51	73	520			95	769	3.85
		Total	3,723	1,450	4,230	3,969	1,006	3,250	442	1,890	19,960	100.00
		Outage because of construction for H_2	1,621	1,142	1,116	301	58	846	165	720	5,969	29.90
	Shutdown for miscellaneous construction, or not required		578	384	455	125	15,857	1,059	10,613	966	30,037	
	Total hours out of service		4,301	1,834	4,685	4,094	16,863	4,309	11,055	2,856	49,997	
Running hours	In air		3,893	1,878	2,065	1,770	48				9,654	
	In hydrogen		70,694	71,840	69,186	18,544	17,673	70,811	41,769	91,584	452,101	
	Total		74,587	73,718	71,251	20,314	17,721	70,811	41,769	91,584	461,755	
Total hours in service			78,888	75,552	75,936	24,408	34,584	75,120	52,824	94,440	511,752	

* Indicates interior of machine was inspected at time of other major maintenance work.

In another case one of the vent pipes for a cooler failed inside the machine, allowing water to collect in the bottom of the condenser. Although a water-leakage detector was installed, unfortunately a small isolating valve ahead of the detector was closed, and therefore, the operator received no alarm, until the machine tripped from its differential relays. Sufficient water had collected in the bottom of the shell to provide a conducting path between the terminal bushings. A short circuit resulted but no fire ensued since there was no oxygen present to support combustion, and therefore the extent of the damage was minimized.

In two other instances, after about five years' operation, slow leakage developed on the brass water piping to the bearings and it was found at the time of general inspection that dezincification action had eaten pin holes in these connections. All internal water piping on these units has been changed to "red" brass or copper to eliminate any possibility of further difficulty. Similar changes have been or will be made on all other machines.

3. *Outage Due to Operation in Hydrogen*

(*a*). *Bearings and Lubrication System.* Although the bearings are normally inaccessible, their reliability has been found to be such that maintenance on these parts of the machine is relatively small. Temperature indication is obtained by two methods from each bearing for check purposes, and therefore ample warning of overheating is generally obtained to prevent wiping of the babbitt or other damage to the machine. Bearing difficulties have been principally on the two 1,200-rpm machines. Failure of a bearing on one of the lower-speed machines was attributed to shaft currents, which were able to flow because of insufficient clearance between the outboard end of the shaft and the end bell. In another instance, during a general inspection it was found that one of the oil rings was considerably worn on a portion of the inside periphery indicating that it had not been turning for a long period. Nevertheless, no increase in temperature was noted on this bearing, the one remaining ring providing sufficient oil for proper lubrication, as evidenced by the normal condition of the shaft.

One of the problems has been to maintain an adequate flow of water through the bearing cooling coils. Initially these coils were connected in parallel with the main coolers on all machines, and therefore the small pressure drop across the latter determined the effective pressure for circulating water through the bearing coils. In some cases this resulted in slow circulation, clogging of the coils and overheating of the bearings, and it was necessary to discharge the water to the atmosphere to obtain sufficient cooling. Considering the exceptional cooling qualities of hydrogen it seems that a self-cooled bearing might be satisfactorily designed for this service and thus eliminate the somewhat objectionable water-cooled arrangement.

Since the bearings are entirely enclosed inside the shell of the machine, provision must be made for filling the bearings or changing the oil without removing the hydrogen. Oil tanks and pumps are provided on most machines for this purpose, these same pumps being used for obtaining the high oil pressure necessary for lifting the shaft at times of starting the condenser. The only difficulties experienced with this system have been slight leaks which have occurred in the high-pressure piping to the bearings.

Due to the operation in hydrogen, there can be no oxidation of the oil and therefore aside from dissolving a limited amount of hydrogen, the oil remains in such condition that it is unnecessary to periodically renew or recondition it. On most of the machines under discussion, there is still being used the oil which was originally placed in the bearings. Tests were made on samples of oil from the Turner machine as originally installed and after approximately seven years' operation with results as given below:

	Original Oil	Oil After 7 Years' Operation
Specific gravity at 60 deg F	0.889	0.885
Neutralization number (milligrams of potassium hydroxide per gram of oil)	0.07	0.14
Viscosity, Saybolt time, seconds		
at 100 deg F	313	257
at 200 deg F	58	54

There was also a slight change in color. However, for all practical purposes, the oil was the same as it was originally. After almost 11 years this same oil is still in service. This oil is heavier than that used on most machines.

(*b*). *Collector Compartment Seals.* The first five machines installed were equipped with sealing arrangements, which, while satisfactory for the purpose intended, did allow some leakage of gas to the collector compartment. If there is an appreciable leakage of hydrogen at this location, it means that an explosive mixture may be present to a limited extent, and hazard to men working in and near the compartment presented. Only in one case was there sufficient leakage to make this possible and this condition has since been rectified. Later machines are equipped with seals which permit absolutely no leakage. A brief description of the various types is given below together with operating experience.

The Turner machine has a seal, consisting of a carbon ring held continually against a shoulder on the shaft by means of a bellows and six springs equally spaced around the shaft. With this arrangement the collector compartment is sealed from the main shell when the machine is running and therefore two pipe connections are included to permit circulation of the gas between the main shell and this compartment. This arrangement has worked out satisfactorily, although there is a slight leakage through the seal during inspections of the compartment. After approximately seven years' operation the carbon rings were carefully checked and it was found that no appreciable wear had occurred.

The arrangement used on the next three machines installed, consists of a rubber ring normally held a short distance away from a rotating sealing plate bolted to the shaft, thus allowing small fans on the shaft to circulate gas through the compartment. By a system of levers and plates, this ring can be moved horizontally in order to close the gap and thus seal the compartment. Although this has proved to be a good arrangement, there is an uneven pressure applied around the ring, so that there tends to be a slight leakage at some points.

Another scheme similar to that just described was used on the fifth unit but in this case, when the collector compartment end plate is removed, springs which are normally in compression, automatically close the seal. Cork was originally used for the sealing material but because of excessive leakage when the seal was closed, this was changed to composition rubber asbestos. Tests indicate that practically all leakage has been eliminated by this slight alteration.

On later machines, in order to provide an even pressure on the sealing plate, the rubber ring above mentioned is replaced by an annular rubber diaphragm. By pumping air behind the diaphragm, expansion of the rubber is obtained, which closes the seal evenly around the circumference. The experience of the authors, indicates that, while this arrangement is satisfactory, there is a tendency for the rubber to gradually take a permanent set, or pull out of position and therefore, when deflated there may not be sufficient clearance between the rotating sealing plate and the rubber. In one instance, the rubber was worn through, and a replacement was required.

A later development which is being used on the 1,200-rpm machines is an arrangement in which a complete circular rubber tube is used, which, when inflated, moves horizontally a solid rubber ring, similar to that previously described. Experience to date, indicates that this is by far the most efficient sealing arrangement and it has been possible to prevent leakage from the main shell at gas pressures up to 15 pounds per square inch.

Table IV. Gas Consumption and Leakage Data, Hydrogen-Cooled Synchronous Condensers

Data as of September 30, 1939

Location	Kva Rating	Years in Service	Years in Hydrogen	Gas Pressure (Pounds Per Square Inch)	Gas Consumption in Cubic Feet*				
					Total			Average Per Week	
					Normal	Inspection	Total	Normal	Total
Atlantic City	15,000	9.02	8.19	¼ to 1¼	28,613	10,674	39,287	67.1	92.2
Benton Harbor	15,000	8.62	8.29	¼ to 1¼	28,512	10,777	39,289	66.0	91.0
Fort Wayne	30,000	8.67	8.05	¼ to 1¼	66,005	41,985	107,990	157.0	257.0
Kenova	15,000	3.95	3.92	¼t o 1¼	6,164	2,525	8,689	30.2	42.5
Scarbro	15,000	8.58	8.27	¼ to 1¼†	21,450	8,945	30,395	48.0	70.6
Scranton	15,000	6.03	6.02	¼ to 1¼	22,043	2,760	24,803	70.4	79.0
Turner	20,000	10.78	10.69	¼ to 1¼	17,135	10,212	27,347	30.7	49.0
Total		55.65	53.42		180,922	87,878	277,800	68.5	100.0
Fostoria	36,000	2.79	2.17	14 to 16‡	25,431	15,380	40,811	225.0**	360.0
Total		58.44	55.60		215,353	103,258	318,611	74.4	110.0

* Cubic feet of gas consumption calculated at atmospheric pressure and 77 degrees F.

† Operated at 3 to 12 pounds per square inch for approximately three months.

‡ Operated at lower pressures for short periods only.

** Average normal leakage for nine months of 1939 was 12 cubic feet per week.

(*c*) *Hydrogen Equipment and Accessories.* Experience with the miscellaneous equipment necessary for operation in hydrogen has been most satisfactory. The automatic charging equipment for admitting gas to the machine has been entirely reliable and no failure to maintain the desired machine pressure has occurred. A few cases of trouble have been experienced with slow leakage through the solenoid-operated valve when closed and through the pressure-reducing valves located at the hydrogen bottles. It has been found that the hydrogen-purity indicator is not extremely accurate, and depending upon the temperature and humidity of the gas sample, it may indicate as much as two per cent above or below the actual purity. However, in the case of synchronous condensers, since the pressure is maintained above atmospheric, the gas leakage is always outward and therefore it is impossible for the purity to drop. The purity indicator is therefore of real service only at times when the machine is being filled with gas. The main disadvantage of this arrangement is that a considerable period of time elapses before a representative gas sample reaches the analyzing equipment, and therefore there is a time lag of from 15 to 30 minutes, between the purity of the sample and that of the gas in the machine.

With the machine running, a satisfactory and accurate indication of purity is obtained on most units by means of an inclined differential draft gauge, since it responds immediately to changes of purity or density of the gas in the machine. This device has proved to be very reliable for this service. On one of the machines, an arrangement is used consisting of an external fan, which circulates gas from the machine and through an orifice. The differential pressure thus developed is used as a measure of the density, and is therefore calibrated to read hydrogen purity. This arrangement has the advantage that it provides immediate indication of purity changes at all times. It has proved to be satisfactory although extremely accurate results are not obtained.

C. Gas Leakage and Consumption

The problem of hydrogen-gas leakage is one which can easily be over emphasized because of the desire to make the shell as tight as possible regardless of the cost of extra maintenance charges as compared with the low price of the gas which can be saved. Experience indicates that it is a much more difficult matter to prevent hydrogen leakage than air leakage from the same machine. Condensers which tested airtight at 15 pounds per square inch pressure, have shown appreciable hydrogen leakage at pressures below 2 pounds per square inch. It has seemed to the authors from the first, that with a gas pressure only slightly above atmospheric, it should be possible to design joining surfaces, gaskets, and connections and to produce gastight welds so that leakage could be practically eliminated without introducing excessive costs for maintenance and leakage testing. With an appreciable gas loss at low pressure, it means that higher gas pressures cannot be used, if required, without excessive gas consumption, since leakage increases rapidly as the pressure is raised.

With the above in mind, the authors have endeavored to keep to a minimum the gas consumption on these eight machines, without unduly expending funds for material and labor for this specific purpose. There are listed in table IV, gas consumption and leakage data which give a complete picture not only of losses during normal operation, but also for inspection of the collector compartments and the main shells. The total hours in hydrogen include stationary as well as running time. For the seven low-pressure machines, the normal average weekly leakage per unit was 68.5 cubic feet and the total average weekly gas consumption per unit was 100 cubic feet.

In the case of the Fostoria unit, which has been operated at an average pressure of 15 pounds per square inch, the leakage as might be expected, has been quite high. However, in nine months' operation in 1939 the average normal leakage was only 12 cubic feet per week.

While the price of delivered hydrogen gas varies with the location, the authors have found that the average cost, including handling and rental charges on cylinders is approximately two cents per cubic foot for gas at atmospheric pressure and 77 degrees Fahrenheit. On this basis, the average cost of hydrogen per machine year has amounted to $114.

V. Handling of Hydrogen and Its Scavenging Gases

A. Characteristics of Hydrogen-Air Mixtures

It has been reported[8] that 4.1 per cent is the minimum percentage mixture and 74 per cent is the maximum percentage mixture of hydrogen in air, which will burn under the most favorable conditions. This is true for the ranges of temperature and pressure ordinarily encountered but it depends upon the size, material, and shape of the container. In the case of mixtures of low-hydrogen content complete combustion under the best possible conditions will not occur until a mixture of approximately 10 per cent is reached. In igniting various mixtures of hydrogen in air, tests indicate that no appreciable or rapid increase in pressure is obtained in a sealed chamber for mixtures below 10 per cent or above 70 per cent. These values are therefore generally used as the lower and upper explosive limits for hydrogen-air mixtures.

The above figures are based on uniformly diffused mixtures of hydrogen in air and do not take into account the cooling effect of surrounding objects, such as stator, rotor, cooling coils, etc., as they exist inside a synchronous machine. Incomplete diffusion and absorption of heat by adjacent material considerably decrease the range in which the mixture is inflammable. However, it is absolutely certain that a thoroughly diffused hydrogen-air mixture at temperatures and pressures ordinarily encountered, will not be inflammable below 4.1 per cent or above 74 per cent with the practical upper and lower limits closer together than this. This lower limit is the one which has been used in maintenance work on the authors' systems as a basis for determining when it is safe to enter the machine or start it in air after removing the hydrogen. For normal operation of the condenser in hydrogen a lower limit of 90 per cent hydrogen in air is used thus allowing an ample safety margin.

B. Scavenging

When the first machine was placed in service in 1928, the manufacturer recommended the use of an inert gas such as nitrogen or carbon dioxide as an intermediate medium for scavenging purposes when filling the machine with hydrogen, or when removing the gas from the machine. These procedures were intended to eliminate the possibility of ever having explosive mixtures of hydrogen and air inside the condenser. However, when the second machine went into operation in 1930, it was felt that the use of carbon dioxide was unnecessary as long as reasonable precautions were taken in the handling of air and hydrogen mixtures. With the advice of the manufacturer there was adopted at that time the procedure of replacing the air by blowing in the hydrogen gas without utilizing any scavenging operation, and vice versa for removing the gas. There is only a very short period of time during which there is a possibility of having such an explosive mixture, and at that time the gases are only partially diffused into each other With the machine stationary and deenergized there is only a very remote possibility of igniting the mixture. Furthermore, if an explosion should take place, the shell is able to withstand any pressures which might be developed, and little if any damage would result internally. This practice of employing no intermediate scavenging medium for the gassing and degassing operations is now consistently followed on all synchronous-condenser installations of the authors' systems. However, in the case of turbogenerators carbon dioxide is used as a precautionary measure because the machine is generally being rotated by the turning gear during the period when it is desired to change from air to hydrogen or vice versa. Furthermore, in an emergency it is possible to change from hydrogen to carbon dioxide to air without disconnecting the unit from the line by decreasing the load to the air-cooled rating. Outage time on a large turbogenerator is obviously a much more expensive proposition than it is on a synchronous condenser.

C. Procedures Used for Introducing Hydrogen Into Condensers

On all the machines, the hydrogen is admitted inside the unit at the top through a perforated pipe running horizontally the entire length of the machine. Initially, gas is admitted slowly, thus permitting the hydrogen to flow evenly, forming a layer which gradually moves downward, forcing out the air below. The speed is gradually increased until an amount somewhat less than the machine gas volume has been used. The rate of feeding is then decreased to avoid unnecessary waste of time and gas in blowing out the remaining air. Very little mixing of the gas and air is thus obtained and as a result, the desired purity of above 90 per cent is obtained in about three hours depending upon the gas volume of the condenser. This is accomplished by the use of approximately 50 per cent more gas than the machine will hold. In other words $1^1/_2$ volumes of gas are required to obtain a purity of approximately 95 per cent at the bottom of the condenser. After diffusion with the pure gas at the top of the unit, the gas purity throughout the machine becomes approximately 97%.

D. Procedures Used for Removing Hydrogen From Condensers

None of the first eight condensers has been equipped with similar facilities for spreading out the air (or carbon dioxide) when it is blown in at the bottom of the machine. In fact, on the earlier machines it was necessary to introduce the air at one point only, thus producing a great amount of turbulence and mixing of the gases. On the later machines additional openings have been provided at several points so that air may be introduced more uniformly and thus replace the gas more quickly and efficiently. In the case of turbogenerators which the authors have installed, a V-shaped inverted and perforated trough has been provided at the bottom of the units to facilitate the proper entrance of scavenging gas or air.

After blowing in air for several hours, samples are taken from the top of the machine and from the collector compartment, and when these samples indicate the presence of three per cent or less of hydrogen this is taken as an indication that it is absolutely safe to start or enter the machine. For making these purity determinations, there is being used a portable sampling device and combustible-gas indicator, which is reasonably accurate for the lower percentage of hydrogen-in-air mixtures, that is from 0 to 20 per cent. This device has proved to be very reliable under all operating conditions. It is also a very helpful accessory for locating gas leaks and for checking the percentage of hydrogen present at various locations in and around the machine.

E. Characteristics of Hydrogen-Air-Nitrogen and Hydrogen-Air-Carbon-Dioxide Mixtures

The inert gases ordinarily used for the scavenging of hydrogen-cooled machines are nitrogen and carbon dioxide. It is necessary to know definitely the inflammable limits of mixtures of these gases with air and hydrogen in order to determine when the proper amount of either gas has been used in changing from air to hydrogen and vice versa. If these scavenging gases are used in insufficient amounts or in a haphazard manner, the effect is worse than if no inert gas were employed, because there is not only the possibility that an explosive mixture may exist at some location in the machine, but also a false sense of security may be given to the operator.

The inflammability limits of hydrogen-air-nitrogen and hydrogen-air carbon-dioxide mixtures have been reported elsewhere.[8,9] The amount of gas required for scavenging purposes has likewise been discussed previously.[9] On the basis of perfect diffusion and theoretically, 1.4 machine volumes of nitrogen or 1.3 machine volumes of carbon dioxide are required in changing from air to hydrogen in order to eliminate the possibility of having an inflammable mixture of gases present in the machine at any time. Similarly, in changing from hydrogen to air, 3.2 machine volumes of nitrogen or carbon dioxide are required.

These theoretical figures are reduced by the fact that the cooling effect of the parts inside the machine raise and lower respectively the lower and upper inflammable limits. Furthermore, the amount of gas actually required is considerably less than that calculated, especially with the use of carbon dioxide, since, if the gases are handled smoothly, properly, and with reasonable speed there is very little mixing or diffusion.

As a result of these factors, it has been found in the case of the Logan and Windsor units, that for the filling operation, after approximately 0.8 machine volumes of carbon dioxide have been introduced, a 75 per cent concentration of carbon dioxide is obtained at the top of the machine, with approximately 100 per cent carbon dioxide at the bottom, which means an average of about 90 per cent. In the case of removing hydrogen, a concentration of 95 per cent carbon dioxide is obtained at the top of the unit, after approximately 1.2 machine volumes of scavenging gas have been used. Everything considered, it is believed best to obtain as a minimum a 75 per cent concentration of carbon dioxide at the top of the unit when changing from air to hydrogen, and a 90 per cent concentration when changing from hydrogen to air.

F. Nitrogen Versus Carbon Dioxide as a Scavenging Medium

While either nitrogen or carbon dioxide can be satisfactorily employed for the scavenging of hydrogen-ventilated machines, the latter is generally used for the following reasons:

(*a*). The inflammability range for hydrogen-air-carbon-dioxide mixtures is less than for hydrogen-air-nitrogen mixtures and therefore less gas is required;

(*b*). The cost per cubic foot of carbon dioxide is approximately one-third that for dry nitrogen;

(*c*). Liquid carbon dioxide is more widely available throughout the country than is compressed nitrogen;

(*d*). Carbon dioxide is approximately 50 per cent heavier than air and thus better adapted for efficient and rapid removal of air or hydrogen without excessive mixing of gases. Nitrogen is approximately the same weight as air.

The only disadvantages which are offered by the utilization of carbon dioxide are as follows:

(*a*). Nitrogen could be more readily handled since, in the case of carbon dioxide it has been found desirable to apply heat to the bottles and connections to prevent freezing of the liquid gas, unless the operation were to be carried on at an excessively low or high speed. However, heating facilities can economically be provided and introduce very little complication into the scavenging procedures. Several methods for introducing carbon dioxide without heating have been developed and undoubtedly these will further simplify the scavenging problem.

(*b*). For a rapid change from hydrogen to air without shutting down of the unit, nitrogen would be preferable since obviously the machine will run just as well in nitrogen as in air, and therefore blowing in of air would be eliminated. However, such emergency changes of gas with the machine in service, would seldom, if ever, be required, and even then, it is possible to continue operation while changing to carbon dioxide and then to air.

G. Handling of Carbon Dioxide

Pure dry liquid carbon dioxide is obtained in cylinders containing 50 pounds which, when converted to gas at atmospheric pressure and at 70 degrees Fahrenheit produce approximately 435 cubic feet. During this change from the liquid to the gaseous state, it has been preferable to apply heat to the bottles. There has been provided for this purpose, a steam-heated hot-water bath in which the cylinders are placed in pairs. Gas is generated at a pressure of approximately 900 pounds per square inch. The cold mixture of gaseous and slightly liquid carbon dioxide is passed through a heating coil in another small hot-water bath, and thence through a reducing valve, from which gas at a pressure of approximately 20 pounds per square inch is obtained. It is then passed into the bottom of the generator, through the perforated trough previously mentioned, and in this manner the gas is uniformly distributed along the length of the machine. In the case of changing from air to carbon dioxide, the air above being approximately 35 per cent lighter than the carbon dioxide, is displaced and allowed to escape to the atmosphere. Test samples of gas are taken from the top of the machine during the latter part of the scavenging process to check that sufficient carbon dioxide has been used. The carbon dioxide is then replaced by hydrogen in the same manner as described in paragraph *C* of this section.

For changing from hydrogen to air, carbon dioxide is admitted at the bottom as described above. Since hydrogen weighs only about five per cent as much as carbon dioxide, there will be practically no mixing of the gases. When tests indicate that only ten per cent of hydrogen remains at the top of the machine, the mixture of carbon dioxide and hydrogen is then removed by blowing in dry compressed air through the perforated pipe at the top of the generator. When test samples taken at the bottom indicate that the carbon dioxide has been practically all removed, and that there is less than three per cent of hydrogen left in the gas mixture, the machine is ready for operation in air, or for inspection and maintenance work if necessary at this time.

VI. Maintenance Routine and Inspections

A. Maintenance Routine

After one year's operation the Turner synchronous condenser was opened for inspection and the interior found to be in such excellent condition that it was decided to allow the machine to operate five years before the next inspection. This original more or less at random chosen, but highly desirable nevertheless, inspection schedule has become established as a permanent maintenance routine. In other words, one year after a new machine is cut into service the gas is removed and an inspection made through the manholes to locate any difficulties which are apt to develop in the initial year of operation. Following this first inspection, unless there is some other evidence of trouble inside the machine, a period of approximately five years is allowed to elapse before making the second inspection. At this time the machine is partly dismantled by removing the end bells, the bearings are inspected, and a careful examination made of the rotor, stator, connections, piping, etc. These inspections require approximately five days.

The original recommendation for inspection of collector compartments was twice a year. In some cases the amount of dust collected in six months seemed to make it unnecessary to clean the compartment oftener than once a year, and in several cases this practice was adopted. However, in the past few years there have been experienced several instances of collector ring and brush trouble which occurred quite unexpectedly. A suggestion was therefore made to the operating companies that the collector compartments be opened not less than twice a year, and preferably three times a year. The time required for this work in general is approximately four hours. Periodic cleaning of the exciters is taken care of at the same time.

Cleaning of the surface coolers is necessary every two or three years depending upon local conditions and primarily upon the characteristics of the cooling water. In several cases water-softening equipment has been installed to prevent formation of scale in the coolers and piping. In general the time required for this cleaning varies from 24 to 72 hours. The spray ponds or cooling towers require cleaning at least once a year.

B. General Inspections

General inspections which have been made on these machines all revealed items of interest. Below are included brief descriptions of a few of these inspections together with views of disassembled machines which will thus present a more complete picture of the maintenance and inspection problem on hydrogen-cooled equipment.

1. *Inspection of Turner Condenser*

An inspection of this machine was made in September 1935, after practically continuous operation of five years and seven months subsequent to the first inspection (which occurred 13 months after being placed in service). The condition of the various parts of the machine as found is given below.

The stator and field windings were found to be in excellent condition. It was noted that a slight amount of carbon dust had been carried in from the collector compartment. Figure 6, taken before the interior parts were touched, shows clearly the clean and excellent condition of the coils and other parts.

There were no apparent leaks in the cooler tubes and connections, or in the brass water piping to the bearings. However, upon test with water pressure, leakage was indicated on the piping to the bearings and there were found several pin holes which had been caused by dezincification.

Lubricating oil originally placed in this machine was never changed. Samples of the oil in this machine were taken and the old oil pumped back into the bearings for continued use. Slight oil leaks were found at the two ends of the machine amounting altogether to about two quarts. Both bearings were inspected and the shaft and bearing surfaces found to be in excellent condition. The oil rings showed no signs of wear.

The carbon ring used for the collector compartment seal, and which is continuously held pressed against the shoulder of the shaft with the machine running or stationary, was found to be polished smooth but there was no appreciable wear. The shaft shoulder had a fine smooth surface. Upon check it was found that the sealing arrangement was working more satisfactorily than originally. The bronze-alloy collector rings were polished smooth with no ridges and no wear. The 24 brushes originally installed on the machine were still in service, the average wear being $^{1}/_{2}$ inch per brush or approximately $^{1}/_{16}$ inch per year. The vent pipe leading back to the main shell was found to contain an appreciable amount of carbon dust, but only a negligible amount of dust was located inside the main shell. All of the various devices and instruments for the handling of the hydrogen gas were found to be in good operating condition.

2. *Scarbro Condenser*

After $4^{3}/_{4}$ years' operation, the Scarbro 24,000-volt machine was shut down because of vibration difficulties and because the five-year general inspection was about due. The condition of the various parts of the machine as found was similar to that of the Turner unit, with the following exceptions.

Figure 6. A view of the windings and interior after $6^{3}/_{4}$ years' service on the Turner 20,000-kva synchronous condenser of the Appalachian Electric Power Company

It was found that the wedges for holding the rotor pole pieces in place were loosened and it was assumed that this accounted for the vibration which had developed over a period of years. All of these wedges were driven in at each end of the machine and trial runs at light load indicated that practically all the vibration had been eliminated.

One of the bearing oil rings was worn down approximately $^{3}/_{16}$ inch for a short distance on the inside periphery of the ring. Apparently this ring had not been turning for a long period. Most of the brass dust from this worn ring had collected in the bottom half of the bearing but some had been carried throughout the lubricating system and it was necessary to thoroughly flush and clean all connections. New oil but of original specification was used for replacement. There was a considerable amount of oil collected at one end of the machine which had apparently leaked from the high-pressure pipe connection. One of the collector rings was in satisfactory condition but a groove $^{1}/_{2}$ inch wide and approximately $^{1}/_{64}$ inch deep had been worn in the center of the other ring. The 20 brushes originally installed were still in service. The average wear was $^{19}/_{32}$ inch per brush or approximately $^{1}/_{8}$ inch per year.

After the tightening of the pole pieces on the rotor in 1935, it was thought that the vibration difficulties had been eliminated. However, in September 1936 vibration was experienced intermittently regardless of load or other variable factors. The machine was opened up at this time for thorough inspection and testing to determine the cause of this unbalancing. It was found that one of the field coils was partially short-circuited and a new coil was installed. Figure 7 shows the rotor removed from the machine and indicates clearly the construction of the rotor and other parts of the machine. After reassembly and test, it was found that vibration still occurred under certain conditions and it was finally decided that the vertical members supporting the bearings were not sufficiently rigid. Additional bracing was installed. Since that time no further difficulty has been experienced with vibration on this unit.

3. *Fort Wayne Condenser*

In 1936 trouble developed in the collector compartment resulting in a short cir-

Figure 7. A view of rotor and stator after $5^1/_2$ years' service on the Scarbro 15,000-kva 24-kv synchronous condenser of the Appalachian Electric Power Company

cuit between rings which froze all brushes to their holders on one of the brush rigging segments, and badly burned one of the rings. This failure occurred primarily because there was insufficient insulation on the brush rigging and secondly because of the large amount of carbon dust collected on these insulating surfaces although this compartment had been cleaned several months previously. At this time the hydrogen was removed by the usual method of blowing air into the bottom of the machine. After it was thought that all of the gas had been removed from the machine, an attempt was made to start for the purpose of truing the collector rings. At this instant a minor explosion occurred. The spark necessary for igniting the explosive mixture was obtained from the amortisseur winding during the starting operation. Although inspection indicated no severe damage had occurred, it was thought advisable to disassemble the machine for a thorough inspection, especially since up to this time the machine had been in service approximately $5^1/_2$ years. The condition of the various parts of the machine as found is given below.

The stator and rotor windings were found to be in excellent condition and to have been unaffected whatsoever by the explosion. Many of the baffle plates around the coolers (as shown in figure 8) were bent or torn loose. It was necessary to straighten or replace this material. Fiber portions of the deflectors at one of the bearings were completely blown off. The only indication of heat was the slight charring of the felt padding used for the metal baffling plates. Since the collector compartment end plate was removed at the time, the force of the explosion was directed against the seal and out through this compartment to the atmosphere. The seal was damaged somewhat but was reinstalled and was satisfactory for continued service. There were no signs of leakage in the coolers, the water piping, or the oil piping. Both bearings were found to be in satisfactory condition.

Both collector rings were turned down, damaged brush holders replaced, and most of the 14 brushes replaced. The average wear over the $5^1/_2$ year operating period was $^{15}/_{32}$ inch or approximately $^3/_{32}$ inch per year. The design of the brush rigging insulation was modified to obtain an increase in the length of the insulating surfaces. During this complete disassembly, thorough inspection, and reassembly, the machine was out of service for a period of 14 days. It was operated in air several weeks before again being filled with hydrogen.

VII. Synchronous-Condenser Losses and Test Data

A. Operation in Air and Hydrogen

Test data covering the first machine installed in 1928 are presented for the purpose of comparing operation in air and hydrogen. Factory test data are also included for the 3,600-rpm machine about to be placed in service at New Carlisle, Ind.

1. *Temperature Runs and No-Load Tests on Turner Condenser*

The Turner machine is rated 15,000 kva as an air-cooled condenser, and 20,000 kva as a hydrogen-cooled machine operating at gas pressures slightly above atmospheric. When the machine was placed in operation in December 1928, temperature runs and no-load tests were made in air and hydrogen at the Turner station. The results of the temperature runs are given in table V. It will be noted that the temperature rises for rated loads in air and hydrogen (at low pressure) were of the same order, thus demonstrating that the use of hydrogen increased the machine capacity by approximately 5,000 kva. A better comparison is obtained from runs numbers 2 and 4, since the maximum possible load was carried in each case without exceeding the guaranteed temperature rises. The increase in capacity from 19,000 to 24,000 kva for this particular machine, indicates that in general an additional capacity of 25 per cent above the air-cooled rating can be obtained by the use of an atmosphere of low-pressure hydrogen. The results of test number 5 are analyzed in section VIII-*B*.

The measured no-load loss in air, excluding excitation losses, amounted to 172.5 kw. This comprises a core loss of 70.5 kw, a friction loss of 12 kw, and a windage loss of 90 kw. For operation in hydrogen the similar no-load loss was 90 kw with the windage amounting to 7.5 kw. At 15 pounds per square inch operation, the windage loss is approximately doubled. The effect upon losses of operation in air and in hydrogen at low and high pressures is shown by the curves in figure 9, which include the total losses of the Turner machine at various loads. Making a comparison at 20,000-kva load, the total losses in air are 1.98 per cent of

Figure 8. A view of surface coolers taken during 1936 general inspection—Fort Wayne 30,000-kva synchronous condenser of the Indiana and Michigan Electric Company

the machine rating and in the case of hydrogen 1.53 per cent. For 15 pounds operation, the total loss is 1.57 per cent of the machine rating. For operation in hydrogen the windage loss is such a small part of the total that only a slight increase in total losses results when changing from low pressure to high pressure.

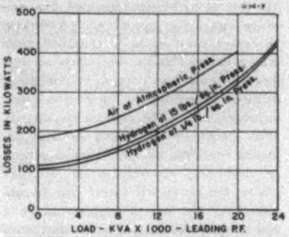

Figure 9. Curves of total losses in air and hydrogen (at normal voltage and frequency, and at leading power factor); 20,000-kva 11.5-kv synchronous condenser, Turner station of the Appalachian Electric Power Company

2. *Temperature Runs on New Carlisle Condenser*

The New Carlisle machine is rated 25,000 kva as a hydrogen-cooled machine when operating at gas pressures slightly above atmospheric, and 30,000 kva when operating at a pressure of 15 pounds per square inch. Temperature runs in air and hydrogen were made on the factory test floor at East Pittsburgh, as given in table VI. It will be noted that in test number 1, a load of 25,000 kva was carried in air, with temperature rises well within safe limits. In test number 2 a load of 30,000 kva was maintained in a hydrogen atmosphere of two pounds per square inch and the temperature rises were of the same order as in test number 1. For this reason the authors consider this unit to have an actual safe rating of 30,000 kva in hydrogen at low pressure and therefore the loss curve in figure 10 was computed on that basis. The unusually small temperature rises of the field can be attributed to the use of a new field coil and slot design which allows the conductors to be cooled directly by the ventilating medium as is the case in salient-pole construction.

B. Losses Versus Speed

In figure 10 there have been plotted curves showing the percentage losses from no load to full load leading power factor for typical machines now in service. The percentage losses of the 15,000-kva 900-rpm units are somewhat less than those of the original 20,000-kva 900-rpm unit at Turner which resulted undoubtedly from improvements in design. The 1,200-rpm 30,000-kva unit shows still further progress in the matter of reducing losses although increase in size was a contributing factor. The 36,000-kva machine covered by curve 4 is practically a duplicate of the 30,000-kva machine of curve 5, and therefore the effect upon percentage losses of increasing the gas pressure to 15 pounds per square inch is clearly shown.

In general, it is found that friction and windage losses increase slightly at the higher speeds and that the stator loss decreases. Field losses and open-circuit core losses remain approximately constant and load loss decreases with increased speed for the salient-pole units. However, in the case of the 3,600-rpm unit, the adaptation of an existing turbogenerator design for condenser service, with the necessary provision of features for decreasing the high starting currents resulted in a percentage load loss, considerably greater than obtained on the lower-speed salient-pole machines. It is hoped on future designs at this speed and with larger capacity units, that losses occasioned by load and other factors can be considerably decreased with the result that the 3,600-rpm machine will have percentage total losses as low or lower than those of the slower-speed units.

VIII. Operation at Higher Gas Pressures

A. General

The maximum benefits from hydrogen cooling are not obtained until gas pressures considerably higher than atmospheric, and of more of the order of 15 pounds per square inch above atmospheric, or even higher, are employed. Increasing the hydrogen pressure enables the gas more efficiently to remove the heat from the windings. The rate of surface heat dissipation by forced convection increases almost directly with the absolute gas pressure, thus more readily transferring the heat to the coolers. As a result, for the same cooling-water temperature, lower field and stator temperatures are obtained as the gas pressure is increased. This is true up to some limit not as yet definitely determined. At the time of the purchase of the Turner machine, the advantage of high-pressure operation was fully realized, and test runs were made at 15 pounds per square inch. However, the gas leakage at this pressure was excessive, primarily because the condenser was not particularly designed for

Table V. Temperature Runs on Turner 20,000-Kva Synchronous Condenser

Test Number Cooling Medium Gas Pressure in Lbs. Per Sq In.	1 Air Atmos.	2 Air Atmos.	3 Hydrogen 0.3	4 Hydrogen 0.3	5 Hydrogen 15
Stator					
Kva	15,000	19,000	20,000	24,000	25,000
Volts	12,000	12,200	11,500	11,500	11,500
Amperes	720	900	1,000	1,200	1,250
Temperature (by temperature detector)	56	76	60	73	64
Temperature rise	33	44	39	50	39
Rotor					
Volts	86.5	118	108	140	137
Amperes	590	710	730	860	890
Kilowatts	51	83.7	78.8	120.3	122.0
Temperature by resistance	71	111	73.5	103.5	86
Temperature rise	48	79	52.5	80.5	61
Bearing temperature					
Collector end	36	45	33	39	38
Opposite end	33	43	31	32	33
Rise					
Collector end	13	13	12	16	13
Opposite end	10	11	10	9	8
Gas temperature					
From coolers	23	32	21	23	25
Into coolers	38.5	50	37	41.5	37.5
Drop through coolers	15.5	18	16	18.5	12.5
Water temperature					
Into coolers	21	28	20	21	22.5
From coolers	26.5	34	26	28	30.5
Rise	5.5	6	6	7	8
Gallons water per minute	175	200	180	175	175
Using spray	No	No	Yes	Yes	Yes
Outside temperature	10	9.5	12	19	13
Length of run (hours)	4.5	4.5+3	5	4.5	4.5+3.5
Hydrogen purity			0.980	0.980	0.986

Above temperatures in degrees centigrade.

Tests made at Turner station in December 1928.

operation at pressures above two pounds per square inch. In planning and building the Fostoria unit, high-pressure operation was definitely taken into consideration, and its rating of 36,000 kva is based upon a gas pressure of 15 pounds per square inch. Experience and special tests on this machine and others at high pressure are given below.

B. TESTS ON TURNER 20,000-KVA SYNCHRONOUS CONDENSER

The special tests made on the 20,000-kva synchronous condenser at the Turner station were described in section VII and the results of temperature runs at 0.3 and 15 pounds per square inch gas pressure are included in table V.

The effect of high-pressure operation can be readily seen by comparing the results of tests numbers 4 and 5 since the load carried in each case was approximately the same. The temperature rise of the stator is 11 degrees less, and that of the rotor 19$^1/_2$ degrees less than at the lower pressure. The bearing temperature rises were also several degrees less. This indicates the ability of the gas at higher pressures more effectively to carry away the heat from the bearings and windings and transfer it to the cooling water. Note that the gas temperature drop through the coolers was 18.5 degrees for operation at a pressure of 0.3 pound per square inch and only 12.5 degrees at 15 pounds per square inch or a reduction of 33 per cent.

It will be noted that the machine carried 25,000 kva at 15 pounds per square inch pressure or 25 per cent above its rating at 0.3 pound per square inch gas pressure with the same stator temperature rise and with a somewhat higher temperature rise of the rotor. It is evident that approximately 24,000 kva could have been carried at the higher pressure with a field temperature rise corresponding to that of test number 3 or 20,000 kva at low gas pressure, or in other words a 20 per cent increase.

After approximately seven years' operation, gas-leakage tests were made on the Turner machine at pressures up to 15 pounds per square inch. The results obtained were as follows:

Average Pressure (Pounds Per Square Inch)	Leakage (Cubic Feet) Per Hour	Per Week
14.6	110	18,480
9.5	32	5,376
4.7	6	1,008
0.5 (average over 11 years)	0.2	31

These tests indicated that the excessive gas leakage at the higher pressures was occurring primarily through welded joints as well as through the main shell joints of which there are four on this machine. It indicated, in general, that a given machine must have been designed and tested for high gas-pressure operation in order that satisfactory results may be obtained.

Table VI. Temperature Runs on New Carlisle 30,000-Kva Synchronous Condenser

Test Number Cooling Medium Gas Pressure in Lbs. Per Sq In.	1 Air Atmos.	2 Hydrogen 2.0	3 Hydrogen 14.95
Stator			
Kva	25,000	30,000	30,000
Volts	11,500	11,500	11,500
Amperes	1,255	1,510	1,510
Temperature—by resistance temperature detector	72.5	68.5	43.5
Temperature rise	48.5	52.0	25.3
Rotor			
Volts	180	199	181.6
Amperes	450	500	500
Temperature by resistance	108.0	90.5	62.3
Temperature rise	84.0	74.0	44.1
Gas temperature			
Entering coolers	55	40.2	31
Leaving coolers	24	16.5	18.2
Drop through coolers	31	23.7	12.8
Water temperature			
Entering front cooler	8	7.5	7.5
Leaving front cooler	17.4	15.0	15.0
Entering rear cooler	8	7.5	7.5
Leaving rear cooler	17.5	15.3	15.5
Rise through coolers (average)	8.5	7.8	8.0
Total gallons water per minute	181	184	184
Room temperature	24	27	24
Length of run (hours)	9.5	6.25	7
Hydrogen purity		0.95	0.95

Above temperatures in degrees centigrade.

Tests made at East Pittsburgh in March 1939.

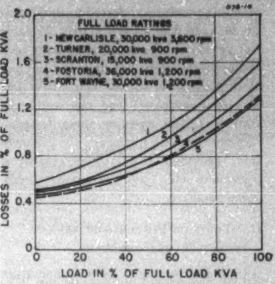

Figure 10. Total losses of five typical hydrogen-cooled synchronous condensers (at normal voltage and frequency and at leading power factor)

Curves 1, 2, 3, and 5 based on gas pressure of $^1/_2$ pound per square inch, curve 4 on 15 pounds per square inch

C. OPERATION OF SCARBRO CONDENSER AT HIGHER GAS PRESSURES

The design of the 15,000-kva synchronous condenser at Scarbro was based on operation at gas pressures not exceeding two pounds per square inch. For a period of two months during the latter part of 1938, it became desirable to be able to carry 20,000 kva on this machine in order to maintain voltage for carrying additional load in an adjacent area. This increased capacity was required for only a few hours each day. The pressure was boosted to a maximum of 12 pounds per square inch and maintained through a range of 5 to 12 pounds per square inch for the entire period with satisfactory results.

The temperature of the gas from the coolers was not allowed to exceed 40 degrees centigrade and the field temperature was not allowed to exceed 115 degrees centigrade. (The upper limit being 120 degrees.) Although on several occasions temperatures were reached very close to these limits, it was possible to carry the increased loads required for the necessary periods and maintain properly the system voltage. The chief difficulty was to obtain adequate cooling with the increased losses involved; although the machine rating is based on ingoing cooling water of a temperature not exceeding 85 degrees Fahrenheit, this entering water actually reached a maximum temperature of 93 degrees Fahrenheit.

The gas leakage on this machine at an average pressure of $^1/_2$ pound per square inch has been averaging approximately 7.0 cubic feet per day. During the run at higher pressures, referred to above, this gas leakage was approximately 25 cubic feet per day at 5 pounds per square inch and approximately 100 cubic feet per day at 12 pounds per square inch. It is thus seen that increased load can readily be carried in some cases on these low-pressure machines by increasing the gas pressure, provided the exciters, switching equipment, etc., have the necessary additional capacity.

Table VII. Temperature Runs on 11.5-Kv 36,000-Kva Synchronous Condenser at Fostoria, Ohio, September 4–5, 1937

H_2 Gas Pressure (Lbs. Per Sq In.)	1		5		10		15		20	
Stator										
Kva—actual	28,800		30,500		32,800		34,400		37,300	
Corrected for normal voltage	30,500		32,500		34,500		36,300		39,000	
Voltage	12,080		12,210		12,170		12,150		12,350	
Amperes	1,379		1,445		1,556		1,621		1,742	
Temperature, resistance temperature detector (deg C)	57		57		57.5		60		61	
Temperature rise (deg C)	35		32.5		33.0		31.5		33.5	
Rotor										
Volts	134		142		150		158		174	
Amperes	650		685		720		750		807	
Temperature by resistance (deg C)	100		101.5		104		106		115	
Temperature rise (deg C)	78.0		77.0		79.5		77.5		87.5	
	CE*	OE*	CE	OE	CE	OE	CE	OE	CE	OE
Bearing temperature (deg C)	56.5	57.0	57.0	57.0	57.5	57.5	58.0	58.0	62.0	61.5
Temperature rise (deg C)	34.5	35.0	32.5	32.5	33.0	33.0	29.5	29.5	34.5	34.0
Gas temperature (deg C):										
From coolers	22.0		24.5		24.5		28.5		27.5	
To coolers	38.0		38.0		37.0		39.0		39.0	
Temperature drop	16.0		13.5		12.5		10.5		11.5	
Water temperature (deg C):										
To coolers	20.5		21.0		21.7		24.5		24.2	
From coolers	25.0		26.3		27.5		30.5		30.8	
Temperature rise	4.5		5.3		5.8		6.0		6.6	
Outside air temperature (deg C)	15.5		15.3		15.0		16.0		15.8	
Temperature cooling water to field thyratron tubes	34.0		36.0		37.0		39.5		40.0	

* CE—collector end; OE—opposite end.

D. TESTS ON FOSTORIA 36,000-KVA SYNCHRONOUS CONDENSER

In September 1937, five temperature runs were made on the Fostoria synchronous condenser at the following average hydrogen gas pressures: 1, 5, 10, 15, and 20 pounds per square inch. The purpose was to determine the maximum loading which could be carried at each pressure without exceeding the guaranteed field temperature rise of 80 degrees centigrade. The results of these tests are presented in table VII which includes kilovolt-ampere load carried, field current and voltage, temperature rises of various parts, and other pertinent data. Several uncontrollable factors made it impossible to secure results such as could ordinarily be obtained in similar tests at the factory. Most important of these was the fact that an overvoltage of from five to eight per cent was applied to the terminals of the machine which increased the core loss above that which would have occurred at normal voltage, and at the same time made necessary an increase in the field current to obtain the same reactive kilovolt-ampere load. The kilovolt-ampere loads, which would have been obtained at rated voltage corresponding to the field currents actually applied, were calculated and are also included in the table. The total losses under these assumed loadings would have been approximately the same as those obtained during the corresponding temperature runs and these values give a true picture of machine capacity. It is seen that for each five pounds per square inch increase in gas pressure, it is possible to obtain approximately 2,000 kva additional capacity. Obviously the limiting feature is the ability of the coolers to remove the increased heat losses occasioned by the additional loading. The Fostoria unit is not designed for continuous operation above 36,000-kva load, but for short periods will safely carry a considerable amount above its normal rating.

Figure 11. View of 50,000-kva 3,600-rpm hydrogen-cooled turbogenerator from collector end—Logan plant of the Appalachian Electric Power Company

The effect of forced heat convection at higher pressures is indicated by the fact that the gas-temperature drop through the coolers steadily drops off with increase in gas pressure as well as increase in losses so that at 15 pounds per square inch this drop is 10.5 degrees compared to 16 degrees at 1 pound per square inch, although the losses have increased about 33 per cent. These corresponding temperature data at 20 pounds per square inch are not representative, because of the field-temperature rise of 87.5 degrees, which occurred because of changing system conditions. It will be seen that the temperature rises of the bearings were consistently lower with increase in pressure, when the temperature rise of the rotor is taken into consideration. The percentage increases in temperature rise for the cooling water, agree very closely with the percentage increases of calculated losses, as is to be expected since the quantity of cooling water circulated was approximately the same for all tests.

E. OPERATING RESULTS WITH FOSTORIA CONDENSER AT A GAS PRESSURE OF 15 POUNDS PER SQUARE INCH

As mentioned in section III, the Fostoria unit is the first synchronous condenser designed for operation at 15 pounds per square inch pressure. The changes in construction of the shell, joints, and other parts, to make it explosionproof and gastight for the higher operating pressure have already been mentioned. This operating experience is included in section IV, where it is stated that the relatively low availability factor was occasioned by a long outage because of rotor trouble, and by the fact that this unit has been in service for slightly less than three years. During the period from January 1, 1938, to September 30, 1939, this machine was out of service 379 hours, making its availability factor for this period 97.53 per cent which compares very favorably with the results obtained for the other units, which are operated at low gas pressure.

There has been no difficulty in carrying loads of 36,000 kva at all times through the year, and swings as high as 70,000 kva have apparently caused no difficulties. The loading of this machine is controlled primarily by the requirements of the 132-kv system, the voltage of which at this point is maintained largely by means of this machine with its electronic regulator and exciter. In addition a field-temperature controller is provided to step back partially the field current in case the field temperature reaches approximately 100 degrees centigrade. This prevents overloading of the unit which would otherwise make necessary a complete shutdown to allow removal of excess heat losses.

The miscellaneous equipment necessary for maintaining the gas pressure at 15 pounds per square inch has worked out satisfactorily. The fine experience with the collector compartment seal has already been mentioned. Gas leakage data is included in section IV. However, it may be well to point out that the leakage figures given, include the excessive loss of gas which occurred during the first few months of operation. The normal average leakage in 1938 amounted to 185 cubic feet per week and the total average gas consumption to 272 cubic feet per week. Experience in nine months of 1939 has resulted in a normal average leakage of only 12 cubic feet per week, so that apparently less gas will be used on this high-pressure unit than on any of the machines running at low pressure.

F. Tests on New Carlisle 30,000-Kva Synchronous Condenser

The results of temperature runs made on this 30,000-kva 3,600-rpm machine at 2 and 15 pounds per square inch gas pressure are included in table VI. The increased cooling effect at the higher pressure is clearly shown and the possibility of obtaining a capacity of approximately 36,000 kva at 15 pounds per square inch is definitely indicated.

IX. Hydrogen-Cooled Turbogenerators

A. The Logan 50,000-Kva Turbogenerator

1. *General*

Early in 1935 studies indicated the desirability of installing at Logan, W. Va., a 40,000-kw 3,600-rpm high-pressure superposition turbine, in order most economically to develop and increase the capacity of the existing power plant at this location. At that time the cooling of the rotor for a generator of this size and speed could be accomplished only by using hydrogen ventilation. Following the extensive and successful experience with the seven hydrogen-cooled synchronous condensers then in service, the authors unhesitatingly accepted hydrogen cooling for this new generating unit. This machine being the first of this type to be placed on order, initiated a new era of hydrogen-ventilated turbogenerators as indicated by the fact that a great many such units aggregating several million kilowatts are now in service or under construction. The only possible doubtful point, as far as operation in hydrogen was concerned, was the problem of shaft sealing, but extensive tests[11] and factory operating experience with the oil seal at Schenectady over a period of years, provided a basis for anticipating satisfactory performance from this sealing arrangement in power-station service.

2. *General Description of Installation*

The Logan unit has a normal rating of 50,000 kva at 0.8 power factor when operating in hydrogen at a pressure of approximately one-half pound per square inch and with the temperature of raw cooling water for the pad liquid heat exchangers, not exceeding 85 degrees Fahrenheit. A view of the complete turbine generator with direct-connected exciters is given in figure 11. The control panels for the turbine, generator, shaft-sealing system, and hydrogen equipment are located adjacent to the machine as shown, in the main turbine room with necessary pumps, tanks, heat exchangers, etc., placed below the unit on two operating floor levels. The main oil tank is directly under the generator, with the twin oil pumps driven by vertical shafts from the reduction gearing above. In conformance with the latest fire-prevention design for turbine lubricating systems, all oil feed pipes are enclosed in the return piping from the various parts of the unit.

A two-piece explosionproof shell split horizontally provides the outer covering for the generator using a construction somewhat like that of the previous synchronous condensers. The bearings are supported by the end bells, which are also split horizontally to facilitate assembly and inspection of the bearings and interior. Metal-to-metal joints are used on the outer shell with grooves filled with a plastic sealing compound to prevent gas leakage. Instead of the conventional type of internal surface coolers, numerous hollow Monel-metal cooling pads are installed between the groups of stator laminations and distilled water is circulated through the pads to absorb heat from the core by direct contact as previously described.[3] Finned pads are placed in the central portion of the core, which in addition to cooling the stator iron, also remove heat from the hydrogen, as the gas is circulated through them by the action of the rotor fans. Thus, the cooling of the rotor and of the stator end turns is effected by means of the gas. The pad liquid is cooled by circulation through one or two heat exchangers, as determined by the temperature of the raw cooling water.

3. *Shaft-Sealing System—Description*

The problem of shell construction to eliminate gas leakage is similar to that which obtains for synchronous condensers, with the important exception that a shaft seal of the oil-film type is provided in each generator bearing to prevent escape of hydrogen where the shaft passes through the end shields. This system has been described previously in detail.[3,11,12] The Logan installation is designed so that the generator may be operated at gas pressures as high as 15 pounds per square inch. Although at first glance this system appears unnecessarily complicated, it is almost completely automatic in operation, and is provided with necessary alarms to guard against abnormal conditions. In

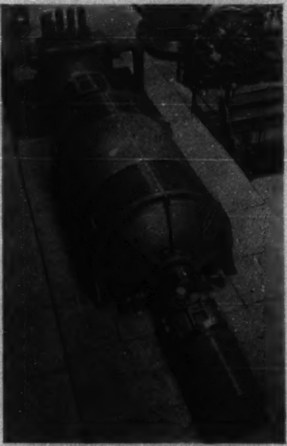

Figure 12. View of the 66,667-kva 3,600-rpm hydrogen-cooled turbogenerator from collector end—Windsor plant of The Ohio Power Company

the event of failure of the vacuum-treating system, a solenoid-operated valve operates automatically to supply oil to the seals from the bearing header of the lubrication system. Such operation results in air being liberated inside the generator which causes a gradual drop in purity, thus necessitating scavenging with pure hydrogen to maintain the gas purity above 90 per cent.

4. *Hydrogen Control and Accessory Equipment—Description*

The equipment for automatically maintaining gas pressure in the generator and for obtaining indications of purity, pressure, and temperature is substantially the same as that used on the synchronous-condenser installations. In the case of a generator the purity indicator becomes an extremely important item, since it is possible for the purity to decrease because of insufficient vacuum treatment of bearing-seal oil, or because of operation of the seals with untreated oil from the bearing header. The purity indicator is of the same type as that used on other hydrogen-cooled machines with the exception that alarm contacts are included to warn of low purity. A fan pressure gauge provides a reliable check on purity when the machine is running.

The hydrogen bottles are located on the turbine floor adjacent to the gas control panel as shown in figure 11, primarily to facilitate scavenging operations with the machine in service. The carbon-dioxide bottles and accessories are located on the operating floor below the generator.

5. *Operating Experience*

Operating experience with this machine has been highly satisfactory. Because of lack of space it is impossible to include this information at this point but it is hoped to present it elsewhere before the Institute.

B. The Windsor Hydrogen-Cooled 66,667-Kva Turbogenerator

1. *General*

This unit located in the Windsor plant of The Ohio Power Company, except for type of coolers, is similar to the Logan machine. A view of this turbogenerator taken from the exciter end is given in figure 12. The outlets for two of the four horizontal coolers can be readily seen. These coolers are similar to the vertical ones previously utilized on synchronous condensers. The accessory equipment is, in practically all respects, the same as at Logan, and the installation is likewise arranged for operation at gas pressures up to 15 pounds per square inch.

2. *Operating Experience*

Operating experience with the Windsor unit has likewise been very satisfactory but for lack of space this information has been deleted.

X. Conclusions and Acknowledgments

A. Conclusions

Operating experience with the eight synchronous condensers described, dating as far back as 1928, and with two hydrogen-cooled alternators, permits the drawing of some rather definite conclusions:

1. Construction for operation in hydrogen is one of the minor factors adversely affecting machine availability. The fact that two machines have each been operating for over two years with no direct supervision indicates that the reliability is as great or greater than that of air-cooled units.

2. From an economic standpoint gas consumption is an almost negligible item. The cost of hydrogen used is generally less than five per cent of the savings obtained by reduction in windage losses.

3. Accessories required for operation in hydrogen have introduced no complications of any significance, and the reliability of this equipment has been fully demonstrated.

4. From a safety standpoint there has appeared no reason for concern when the machine is normally operating in hydrogen. As to the necessary handling of inflammable gas, if reasonable precautions are employed there is considerably less danger involved than is ordinarily taken for granted in connection with operating and maintaining electrical equipment.

5. More than 45 machine years of outdoor operation involving five machines during all possible weather conditions, demonstrate the adaptability of this apparatus for such service.

6. General inspections every five years have been found to be sufficient. Internal difficulties occurring between regular inspections have been reliably and quickly indicated by the various protective devices.

7. Field tests have demonstrated that in general, the air-cooled capacity of a given synchronous condenser can be increased 25 per cent by the utilization of hydrogen, at slightly above atmospheric pressure, and still further augmented an additional 20 per cent by raising the gas pressure to 15 pounds per square inch.

8. When designed for gas pressures up to 15 pounds per square inch, reliable performance can be obtained at such higher pressures, and advantage taken of the additional cooling effects of a hydrogen atmosphere.

9. The utilization of hydrogen, particularly at pressures above atmospheric, permits not only the carrying of additional continuous loads, but also allows heavy swings of short duration with entire safety because of the ability of hydrogen rapidly and efficiently to remove heat from possible hot spots in the windings.

10. The condition of the windings, particularly in the machines which have been in service for over seven years, affords every reason for anticipating insulation life considerably greater than is now obtained from units exposed to the injurious effects of oxidation and corona.

11. It seems clear, to the authors at least, that the hydrogen-ventilated synchronous condenser is preferable and more economical for units above 10,000 kva to the equivalent air-cooled machines. The future offers possibilities of higher voltage and further development at 1,800 or 3,600 rpm to take maximum advantage of hydrogen cooling.

12. Although tests seem to indicate that increases above 15 pounds per square inch do not produce correspondingly proportional gains in cooling or capacity, operation at gas pressures above this level should, and undoubtedly will be explored.

13. The comparatively more limited experience with the hydrogen-ventilated turbogenerators at the Logan and Windsor plants is insufficient safely to draw any conclusions. There is definite reason to believe results which have been obtained with the condensers will be duplicated in the operation of generators which are substantially the same in construction. The only new feature which has been added is the shaft-sealing equipment. This arrangement is still somewhat complicated, but operation on these two units during a combined total of almost two years has demonstrated that these installations as a whole as well as in their component parts, are entirely reliable. As in condensers, the feasibility of using higher gas pressures to obtain greater capacity or to limit maximum permissible heating of the windings, when available cooling water approaches extremely high temperatures, has been demonstrated.

14. Hydrogen cooling permits the construction of large high-speed turbogenerators, which could not be properly cooled by air at all, or in some sizes only by utilizing external fans. By going to hydrogen a more compact design results, which combined with savings from reduced windage losses, more than balances the cost of explosionproof construction and accessories necessary for operation in hydrogen.

15. Judging by the action of the power industry and its engineers, in the past three years, it is increasingly apparent that hydrogen cooling has opened up a new field for the development and design of large capacity and high-speed machines. Hydrogen gas has definitely been accepted by most of the profession associated with power machinery as being the ideal cooling medium for obtaining greater capacity, greater efficiency, and longer life for synchronous condensers, turbogenerators, and for similar large rotative equipment. Hydrogen has become accepted as a cooling medium as matter of factly as its presence in, and as, water.

B. Acknowledgments

The authors wish to acknowledge the fine spirit of co-operation and the vast amount of work carried out by the engineering staffs of both the General Electric Company and the Westinghouse

Electric and Manufacturing Company who between them designed and constructed all the machines discussed in this paper, and their indebtedness to the organizations of the Indiana & Michigan Electric Company, The Ohio Power Company, Appalachian Electric Power Company, The Scranton Electric Company, and the Atlantic City Electric Company, upon whose systems these machines were installed. Had it not been for their patient and sympathetic co-operation in solving all the problems incident to the operation of hydrogen-cooled machines at the time when hydrogen cooling was new and apparently seemed to offer more hazard than benefit, it is doubtful whether the progress made in the last decade in developing hydrogen cooling to the successful point that it has reached today, would have been made as rapidly, if at all.

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Discussion

For discussion, see page 1187.

Theory of Hysteresis-Motor Torque

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THE object of this study is the development of a quantitative theory of hysteresis-motor torque, especially in relation to magnetic properties and dimensions. The theory is interpreted physically in an idealized case, and applied to a particular motor to verify the result experimentally.

Steinmetz,[1] in 1908, discussed the production of torque by magnetic hysteresis and gave a brief analysis of motor performance. Unpublished experimental studies relating to motor design have been made more recently by other engineers of the General Electric Company, including W. J. Morrill, C. A. Nickle, and R. F. Edgar. The present work was begun while the writer was associated with the last two.

The hysteresis motor consists of a wound stator similar to that of an induction motor; and a rotor composed of laminations of a magnetically hard steel assembled on a nonmagnetic arbor. These elements are shown in figure 1. Stator currents establish a revolving field in the rotor, where hysteresis causes the flux density to lag behind the magnetic intensity. The space phase angle between the stator magnetomotive force and the resultant flux gives rise to an impelling torque, produced without the aid of rotor currents or salient poles. Self-starting, as well as synchronous, the hysteresis motor has been used in phonographs and traffic-signal controls, and, with modifications, in clocks. As Steinmetz[2] pointed out, the torque, efficiency, and power factor compare unfavorably with those of an induction motor; thus the hysteresis motor is not a general competitor, but appears to better advantage as the size decreases.

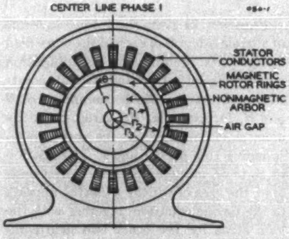

Figure 1. Cross section of hysteresis motor

Derivation of General Torque Equation

A general expression for instantaneous torque in terms of the field within the rotor is obtained from the energy exchange accompanying a virtual angular displacement of the rotor. This method is discussed in many textbooks; for example, the one by Bitter,[3] but expressions directly applicable to the present case are not given.

The stator field and induced rotor magnetization at a given instant are treated as a stationary configuration, the impelling torque due to the field being balanced by an externally applied opposing torque. Each element of volume of the rotor is situated in a magnetic field of strength $\boldsymbol{B}$, which is the sum of $\boldsymbol{B}'$, attributable to the stator alone, and $\boldsymbol{B}''$, to the induced rotor magnetization. The impelling torque may be considered to have its origin in the interaction of $\boldsymbol{B}'$ with the magnetized rotor. To simplify the derivation, the stator is replaced by permanent magnets of appropriate strength and distribution to give an equivalent field $\boldsymbol{B}'$ within the rotor, this substitution being justified by the equivalence between a current circuit and a magnetic shell.[4]

If, by a slight decrease of the opposing torque, the rotor is given a small forward angular displacement $\delta\phi$, the intensity of magnetization $\boldsymbol{M}$ at each point being held constant, mechanical work of amount $T\delta\phi$ is done at the expense of the magnetic energy of the system, thus,

$$T\delta\theta = -\delta W \tag{1}$$

In this and succeeding equations, centimeter-gram-second electromagnetic units are employed. Since $\boldsymbol{M}$ is constant, the

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1. For all numbered references, see list at end of paper.

only portion of the energy which changes is that associated with the interaction of the rotor magnetization and the stator field. On the basis of Ampere's theory of magnetism,[4] this energy for any volume element dV is given in terms of components by

$$dW=-(M_rB_r'+M_\theta B_\theta'+M_zB_z')dV \qquad (2)$$

where r, θ, and z are cylindrical co-ordinates, fixed with respect to the stator, and shown in figure 1. The displacement moves the volume element to a position where the field components are $B_r'+(\partial B_r'/\partial\theta)\delta\phi$, $B_\theta'+(\partial B_\theta'/\partial\theta)\delta\phi$, etc.; thus the energy of the element decreases by the amount:

$$-\delta(dW)=[M_r(\partial B_r'/\partial\theta)+M_\theta(\partial B_\theta'/\partial\theta)+M_z(\partial B_z'/\partial\theta)]\delta\phi dV \qquad (3)$$

Integration over the volume V of the rotor gives:

$$-\delta W=\delta\phi\int\int\int_V[M_r(\partial B_r'/\partial\theta)+M_\theta(\partial B_\theta'/\partial\theta)+M_z(\partial B_z'/\partial\theta)]dV \qquad (4)$$

On substituting this expression in equation 1, dividing both members by $\delta\phi$, and writing dV in terms of the co-ordinates, there results:

$$T=\int_0^L\int_{r_1}^{r_2}\int_0^{2\pi}[M_r(\partial B_r'/\partial\theta)+M_\theta(\partial B_\theta'/\partial\theta)+M_z(\partial B_z'/\partial\theta)]rd\theta drdz \qquad (5)$$

where L is the length of the rotor.

Equation 5 can also be obtained by an alternative procedure which makes use of a general expression[5] for the torque on a magnetized body, and does not require a virtual displacement, or the substitution of permanent magnets for the stator.

An equation giving torque in terms of the total field $\boldsymbol{B}$, instead of $\boldsymbol{B}'$, is more convenient for application. The other part of the field, $\boldsymbol{B}''$, which arises from the magnetization of the rotor itself, produces only pairs of equal and opposite torques, the actions and reactions between magnetic elements. Thus, if an external field identical with $\boldsymbol{B}''$ were impressed on the rotor, the torque would be zero, or

$$0=\int^L\int_{r_1}^{r_2}\int_0^{2\pi}[M_r(\partial B_r''/\partial\theta)+M_\theta(\partial B_\theta''/\partial\theta)+M_z(\partial B_z''/\partial\theta)]rd\theta drdz$$

If this equation is added to 5, there results:

$$T=\int_0^L\int_{r_1}^{r_2}\int_0^{2\pi}[M_r(\partial B_r/\partial\theta)+M_\theta(\partial B_\theta/\partial\theta)+M_z(\partial B_z/\partial\theta)]rd\theta drdz \qquad (6)$$

In each term the component of $\boldsymbol{B}$ may be used as the variable of integration in place of θ, and since $\boldsymbol{B}$ and $\boldsymbol{M}$ go through a cycle of values in twice the pole pitch of the stator winding, the integral around the rotor may be expressed in terms of the cyclic integral, thus,

$$T=p/2\int_0^L\int^{r_2}\oint(M_rdB_r+M_\theta dB_\theta+M_zdB_z)rdrdz \qquad (7)$$

in which p is the number of poles. Each integration of MdB is to be performed in the direction of positive θ. The torque may be expressed in terms of magnetic intensity $\boldsymbol{H}$ instead of $\boldsymbol{M}$ by using the relation

$$\boldsymbol{M}=(\boldsymbol{B}-\boldsymbol{H})/4\pi \qquad (8)$$

For the r term,

$$\oint M_rdB_r=1/4\pi\oint B_rdB_r-1/4\pi\oint H_rdB_r$$
$$=-1/4\pi\oint H_rdB_r \qquad (9)$$

since $\oint B_rdB_r$ vanishes for integration over a cycle.

If the last member of equation 9 is integrated by parts

$$\oint M_rdB_r=-1/4\pi H_rB_r+1/4\pi\oint B_rdH_r$$
$$=1/4\pi\oint B_rdH_r$$

since H_rB_r has the same value at the beginning and end of one cycle. After a similar treatment of the other terms, equation 7 becomes

$$T=p/8\pi\times$$
$$\int_0^L\int^{r_2}\oint(B_rdH_r+B_\theta dH_\theta+B_zdH_z)rdrdz \qquad (10)$$

Equation 10 gives the torque in terms of a triple integral of each of the three field components, requiring for its application a knowledge of both $\boldsymbol{B}$ and $\boldsymbol{H}$ at all points within the rotor. Each component of $\boldsymbol{B}$ and $\boldsymbol{H}$, for example, B_θ, varies cyclically along a circular arc of the rotor corresponding to a fixed r and z. The relation of B_θ to H_θ is represented by a hysteresis loop, the area of which, $\oint B_\theta dH_\theta$, appears in the torque equation, together with the loop areas for the other two field components. In general all three loops vary with r and z.

For some applications equation 10 may be simplified. Thus, if the axial component of the field is negligible, the last term of the integrand may be dropped, the other components are independent of z, and

$$T=(pL/8\pi)\int_{r_1}^{r_2}\oint r(B_rdH_r+B_\theta dH_\theta)dr \qquad (11)$$

Also, it can be shown, from the expression for the magnetic field, that as the radial thickness of the rings is decreased; that is, as r_1 approaches r_2, the radial component of the field decreases, and the tangential field becomes independent of r. For sufficiently thin rings the radial component may be dropped from the equation, and the remaining integral written as the product of two factors; thus, after multiplication and division by 2π,

$$T=[p/(16\pi^2)](L\int_{r_1}^{r_2}2\pi rdr)\oint B_\theta dH_\theta$$
$$=pV/(16\pi^2)\oint B_\theta dH_\theta$$
$$=K\oint B_\theta dH_\theta \qquad (12)$$

where V is the volume of the magnetic material in the rotor, and K, a constant for a particular motor.

The motor used in the experimental verification of the theory has dimensions r_1 and r_2 such that the tangential component varies but little with r, as shown in figure 6, but the radial component is large enough to contribute up to 10 per cent of the total torque. The contribution from the former is calculated from equation 12 in which B_θ is replaced by its average $\overline{B}_\theta$, taken with respect to r. Since the relatively small torque T_r, from the radial component, need not be determined accurately, r is replaced by its mean value, $^1/_2(r_1+r_2)$, and

$$T_r=[pL/(16\pi^2)][2\pi(r_1+r_2)/2]\times\int_{r_1}^{r_2}\oint B_rdH_rdr \qquad (13)$$

Here the integral is the average value of $\oint B_rdH_r$ with respect to r, multiplied by the radial thickness (r_2-r_1); hence,

$$T=[pL/(16\pi^2)][\pi(r_2+r_1)(r_2-r_1)]\overline{\oint B_rdH_r}$$
$$=K\overline{\oint B_rdH_r} \qquad (14)$$

where the bar denotes the average of the whole integral. Thus the total torque of the experimental motor is given by:

$$T=K[\oint\overline{B_\theta}d\overline{H_\theta}+\oint\overline{B_rdH_r}] \qquad (15)$$

Physical Interpretation of the Torque Equation

For illustration the theory will be applied to a simple type of motor having such relative dimensions that equation 12 is appropriate. It is assumed that the revolving stator magnetomotive force is sinusoidal, and that the air gap reluctance is large compared to that of the rotor. Thus the rotor flux density will be tangential, varying sinusoidally with θ, and independent of r.

When the rotor turns at less than synchronous speed, any element of volume moves with respect to the field and experiences a sinusoidal variation of B in time, eventually reaching a cyclic state in which corresponding values of B and H are given by an alternating hysteresis loop, such as shown in figure 2. Other elements of volume go through exactly similar magnetic cycles, displaced in time phase; thus the loop gives either the time variation of the magnetic state at a fixed point on the rotor, or the space variation around the rotor at a particular instant of time. According to the latter interpretation, the area, A, of the loop is the integral $\oint B_\theta dH_\theta$ required in equation 12, and

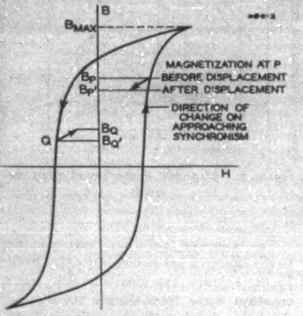

Figure 2. Instantaneous B-H relation around rotor

$$T = KA \qquad (16)$$

The torque is the same from zero to synchronous speed, a fact noted by Steinmetz.[1]

Torque at Synchronous Speed

When the motor is accelerating to synchronous speed, the rotor slips backward with respect to the rotating field at a diminishing rate. The field at a given point P goes through its magnetic cycle, figure 2, more and more slowly, until at synchronism, the flux density reaches the value B_P and momentarily stops changing. Depending upon the location of P with respect to the flux density wave, the last change of B may have been an increase or a decrease; if the former, B_P lies on the ascending branch of the hysteresis loop. At another point Q, where the last change of B was a decrease, B_Q lies on the descending branch. The relation of B to H around the rotor, at the instant when it has synchronous speed, is still given by figure 2, and the impelling torque is the same as at subsynchronous speeds. If, at this instant, the load torque is just equal to the impelling torque, the motor speed will not change any further, and the motor will operate steadily at synchronous speed.

However, if the load torque is less than the impelling torque, there will be a further increase of speed and a displacement of the rotor forward with respect to the field. Such a relative movement is in the opposite direction to that last occurring, hence the field at P, reached by an increase, now decreases to B_P', the change of state occurring on a minor loop. Thus, after a forward displacement, the B-H relation from point to point around the rotor is given by a closed curve which is similar to the original hysteresis loop and derived from it by a displacement of all its points inward along their respective minor loops. The new loop is smaller and the torque less than before. Such B-H loops are shown in figure 3. These were obtained graphically, the minor loops being taken as straight lines, all having the slope of the descending branch of the major loop at its tip. On this basis the torque decreases linearly with displacement angle, but if the true minor loops are used, the relationship is nonlinear and multivalued. Thus as the forward displacement increases, the impelling torque decreases and at some point is in equilibrium with the load. However, the rotor speed is greater than synchronous, and so the displacement continues to increase, the net torque becoming negative, or retarding. The relation between net torque and displacement is such that the rotor oscillates about the equilibrium point, with decreasing amplitude because of losses. When the oscillations have decreased to zero the rotor turns uniformly at synchronous speed, and has a position with respect to the field that makes the impelling torque just equal to the load. At no load the position is such that the area of the B-H loop is zero. If an external driving torque is applied the further displacement interchanges the ascending and descending branches, giving a negative area integral, which corresponds to generator action.

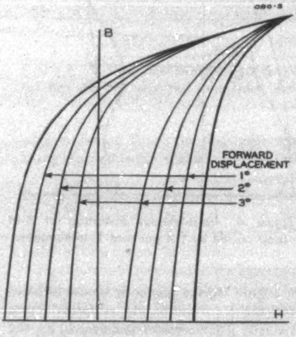

Figure 3. B-H loops for decreased loads. Angular displacement referred to B

The speed-torque characteristic is similar to that of a synchronous motor whose field-pole structure can slip on the shaft when the torque reaches a sufficiently high value. For the purposes of analogy, the friction torque during slipping is constant, is equal to the maximum static friction, and both are less than the magnetic pull-out torque. When the shaft slips, turning at less than synchronous speed, the output torque is that of constant friction; but at synchronous speed the torque has any positive or negative value up to this magnitude, depending upon the angle between the poles and the rotating magnetomotive force, and hunting can occur.

Effect of Harmonics

The effect of field harmonics on torque may be determined from their modification of the B-H loop. The effect depends upon whether the harmonics move at the same speed as the fundamental, or at different speeds; hence two types of field must be considered.

The first, in which the resultant field has a constant wave form, may be produced by the magnetic nonlinearity of the rotor, or by mechanically turning the stator, it being excited by direct current and having a nonsinusoidal magnetomotive force. If the harmonic amplitudes are so small that they do not introduce additional maxima and minima in the flux-density wave, the B-H relation is still a hysteresis loop, the same as the one for the fundamental alone (figure 2), except for a possible change in tip value depending upon the phases and amplitudes of the harmonics. Hence, in this case the harmonics have little or no effect on the torque.

The second type of field, in which the harmonics move relatively to the fundamental, is produced by polyphase a-c excitation, the stator windings being distributed in slots. When the rotor turns at synchronous speed, the field at any volume element P consists of the periodically varying resultant of harmonics superposed on the constant local value of the fundamental. The modification of the B-H relation is illustrated in figure 4, where point p gives the flux density B_P at P, in the absence of harmonics. Let the harmonic variation be introduced in such a way that at P it is initially zero, then increases to a maximum, B_h, decreases to $-B_h$, etc., periodically. The first change from zero to B_h occurs on line pq, and that from $+B_h$ to $-B_h$ on line qs. The latter is also the locus of all subsequent variations if the width of the minor loop and its change of position with cyclic repetition are neglected. No matter how the harmonics are introduced, the final cyclic variation of B with respect to H occurs on the minor loop which intersects the major loop at the sum of the local value of the fundamental and the maximum of the harmonics. The major loop is that one whose tip flux density is the maximum of the total field.

At volume elements other than P there are similar variations, except for phase. Hence at any instant the harmonics cause a wave-like indentation of the major loop, shown for a single harmonic, the 11th, in the figure. In this case the indentations have a maximum of twice the harmonic amplitude and travel around the loop in time. The corresponding instantaneous torque at synchronous speed is given by equation 16 in which A is the area of the indented loop. The average torque can be found by averaging the loop area with respect to time, or by using the loop drawn through the centers of the minor loops, such as point r in figure 4. Such a loop is similar, but not identical, to the ones of figure 3. The reduction of area depends upon the amplitude of the harmonic and the slope of the minor loop, or incremental permeability.

When more than one harmonic is present the modification due to the resultant must be determined. In general the effect of a number of harmonics is not the sum of their individual effects.

The torque at subsynchronous speeds can be found from a similar analysis, but in this case the variation at each volume element due to the fundamental must also be considered.

Application of Theory to Experimental Motor

The theory is applied to the four-pole motor shown in figure 1, and for which data are given in table I. There are four different sets of rotor rings, one of 36 per cent cobalt steel, and three of 3 per cent chrome steel with different air gaps. The motor differs from commercial shaded-pole types in having a distributed stator winding, which simplifies the calculation.

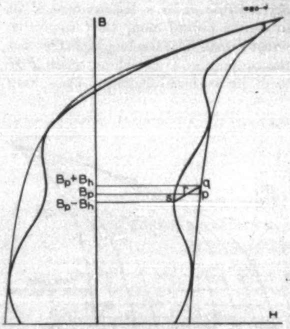

Figure 4. Instantaneous distortion of B-H loop caused by 1.2 per cent 11th harmonic

Figure 5 gives magnetic characteristics for the rotor materials, obtained from ballistic galvanometer tests made on the rings. The characteristics include the normal and incremental permeabilities, and the hysteresis loss per unit volume per cycle, the last being $^1/_{4\pi}$ times the loop area. The incremental permeability is that for small changes at the tip of the major loop. Separate curves are given for the largest chrome-steel rotor since its heat treatment is different.

Table I

Stator Data

Stacked length—1.250 inches
Inside diameter—2.2494 inches
Slot opening—0.074 inch
Number of slots—24
Number of phases—2
Number of poles—4
Frequency of stator current 25 cycles

Rotor Data

Material	3 Per Cent Chrome	3 Per Cent Chrome	3 Per Cent Chrome	36 Per Cent Cobalt
Outer radius, inches	1.1119	1.0089	1.0829	1.1115
Inner radius, inches	0.8125	0.8125	0.8125	0.8125
Air gap, inches	0.0128	0.0258	0.0418	0.0132
Stacked length, inches	1.200	1.190	1.190	1.109
Heat treatment	Hardened Drawn Rehardened	Hardened Drawn, 200 deg C.	Hardened Drawn, 200 deg C.	Hardened Drawn, 550 deg C

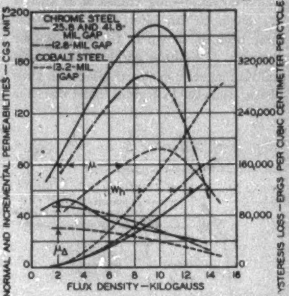

Figure 5. Magnetic properties of rotor materials

Two kinds of operation, corresponding to the effects of harmonics, are considered. In the simpler, the magnetic field has a constant wave form during its motion, this being simulated by exciting one winding with direct current, and driving the rotor with an external motor. The harmonics are small enough so that they have a negligible effect on maximum flux density and torque.

The distribution of the magnetic field and its strength in relation to current are first determined, and then the corresponding torque. For the sake of brevity only an outline of the procedure is given.

The following assumptions are made to permit and facilitate analytical treatment:

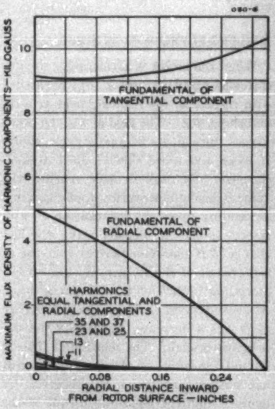

Figure 6. Calculated amplitudes of field components

Chrome-steel rotor, 12.8-mil gap, $\mu = 150$, $\mu_\Delta = 27$

1. The field is two dimensional; the axial component, due to fringing at the ends, being neglected.

2. There are no magnetomotive-force drops in the stator laminations.

3. There are no eddy currents in the laminated rotor.

4. The tangential component of H at the stator boundary is a rectangular wave. This is expressed as a Fourier series, and in the experimental motor the winding is so distributed that the only harmonics present are those of order 1, 11, 13, 23, 25, etc.

5. The field in the arbor supporting the rotor rings is negligible, due to magnetic shielding by the more permeable rotor steel.

6. In calculating the fundamental components of B and H in the rotor, the hysteresis loop is replaced by an inclined ellipse, which has the same maximum values of B and H, and the same area as the alternating loop. The geometric shape factors of the ellipse, the ratio of B_{max} to H_{max} and the phase angle between B and H, are determined to correspond to the tangential flux density averaged over the section, and then treated as if independent of the variations of the field with r.

7. In calculating the harmonics at synchronous speed, the incremental permeability, μ_Δ, the slope of the minor loops, is taken as constant; and for simplicity the slope of the descending branch of the major loop at its tip is used.

The procedure of determining the magnetic field consists of finding solutions of the field equations,

$$\nabla \cdot \boldsymbol{B} = 0 \qquad (17)$$

$$\nabla \times \boldsymbol{H} = 0 \qquad (18)$$

separately for the regions of air gap and rotor rings, and then assigning values to the undetermined constants to satisfy assumptions 4 and 5, and the conditions of continuity for $\boldsymbol{B}$ and $\boldsymbol{H}$ at the common boundary. The fundamental component of the field in the air gap and, by virtue of assumption 7, the harmonics in both regions are solutions of Laplace's equation, the method and results being given, for example, by Doherty and Nickle.[6] A modification of this method is used to determine the fundamental within the rotor rings, to conform to assumption 6; but the result is similar, except for a phase angle, to that obtained when B is proportional to H.

At any point within the rotor, the radial and tangential components of the density are expressible as the sum of harmonic terms, the amplitudes of which vary with r, and can be computed from the dimensions, current, and magnetic data. Typical values, as functions of r, are shown in figure 6. While the radial and tangential components of the fundamentals differ markedly, those for any harmonic are equal.

In the case of the fundamental, the field calculations agree reasonably well with measurements, as shown for three different air gaps in figure 7. The measured values are obtained from the electromotive force generated in an exploring turn of fine wire enclosing the rotor rings, the rotor being turned with respect to the field. The agreement is improved if, to allow for the decreased permeability of the stator laminations caused by punching, an effective air gap two mils larger than the nominal one is used in the calculation.

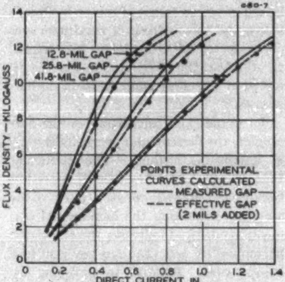

Figure 7. Fundamental component of tangential flux density averaged over radial depth

In the case of harmonics, the agreement is not nearly so good. The measured amplitude of the 11th is about twice that calculated; and the 13th, which should be nearly equal to the 11th, is scarcely present. The resultant of the two, averaged with respect to r and θ, which is required in the second torque determination, is approximately 25 per cent higher than the calculated values.

The flux density, which can be related to current, determines the area of the B-H loop, and, by equation 15, the torque. In the first case, when the field has a constant wave form, harmonics are neglected, since they increase the average tangential flux density by less than two per cent. (They contribute more to the radial flux density, as shown in figure 6, but the latter gives rise to only ten per cent, or less, of the total torque, and hence need not be calculated as accurately.) The results of the torque calculation are given in figure 8, also the measured torques, obtained from the force required to balance the stator in a ball-bearing dynamometer mounting.

In the second case, with a-c excitation and operation at synchronous speed, the torque attributable to the tangential field is calculated from the area of the alternating loop as reduced by harmonic displacements along minor loops. Only the two most important harmonics, the 11th and 13th, are included and the resultant of these is averaged with respect to r, θ, and time to obtain an equivalent constant uniform indentation of the major loop. The torque attributable to the radial field is reduced in greater proportion since harmonics of the same amplitude are superposed upon a smaller fundamental, hence this part of the torque, small in the absence of harmonics, is now neglected. Calculated and measured values are shown in figure 9.

Discussion of Results

The difficulties in applying the theory arise chiefly in the inherently nonlinear characteristic of the rotor material. Hence the calculations are more accurate for the larger air gaps, where the rotor characteristic has less effect, and for weak fields, in which the nonlinearity is less pronounced.

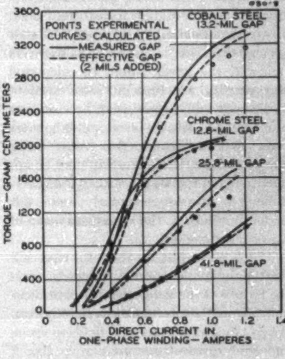

Figure 8. Maximum torque, field of constant wave form

The assumptions and approximations are to simplify the calculations, and to supply information not otherwise available. On the basis of experimental and theoretical evidence, assumptions 1, 3, 4, and 5 are believed to introduce little error. The discrepancy between measured and calculated flux density (figure 7) is attributed to the inaccuracy of assumption 2 arising from the decrease of permeability near the edge of the laminations by the mechanical strain of punching. Motor designers sometimes make allowance for this by increasing the measured gap by about

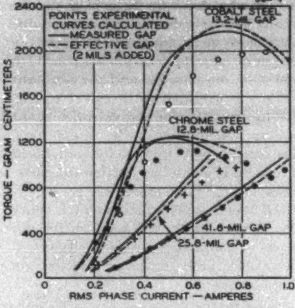

Figure 9. Maximum torque at synchronous speed, balanced two-phase excitation

four mils for the purpose of calculations. Similar effects have been reported by H. M. Turner[7] for a butt joint between sections of a laminated transformer core, where the apparent air gap was four mils greater than that measured. As shown in figure 7, the lower values of calculated flux density are brought into excellent agreement with experiment by using an effective gap which is two mils greater than that measured, this value being chosen because the correction need be applied only to the stator. Also, the gap is difficult to measure accurately, and is not perfectly uniform, the variations being of the order of one mil.

The *B-H* relation for the rotor material, treated in assumption 6, depends upon the nature of the field, and differs at most points from the alternating hysteresis loop. When two equal sinusoidal fields in space and time quadrature combine to give a rotating field of constant magnitude, the *B-H* relation for each component is an ellipse,[8] the area of which at first increases with flux density, then reaches a maximum, and finally decreases to zero at saturation. In the hysteresis motor the fundamental of the field is predominantly alternating; but, because of the radial component, also has a rotating constituent, which varies from zero at the inner rotor boundary to equality with the alternating constituent at the outer one. For a field that has both constituents, the *B-H* relation is not exactly known. At synchronous speed, moreover, it will be modified by the varying harmonics. Spooner[9] has shown that the effect of harmonics in the same direction as the fundamental can be explained in terms of minor hysteresis loops, and this analysis of maximum synchronous torque is similar to his method. In addition, the *B-H* relation may be affected by the cross components of the harmonics.[10]

To permit the calculation of the harmonics, the incremental permeability, μ_Δ, is assumed constant. The particular value chosen has little effect on the result, because of the predominating effect of the air gap; increasing the incremental permeability tenfold increases the calculated harmonics by less than ten per cent in a typical case. Relative to assuming the same incremental permeability for all the minor loops on a given major loop, measurements show the same proportionate error in the harmonics at high flux densities, where the variation of μ_Δ is seven to one, as at low ones, where it is only two to one. However, the use of linear approximations in treating the nonlinear motor must be the ultimate source of the discrepancy between the calculated and the experimental values of the harmonics, since there is agreement when a nonmagnetic material is substituted. The use of averages introduces no new approximations after linearity has been assumed.

Conclusions

This study provides a general relationship between hysteresis torque and magnetic field which can be used to predetermine hysteresis motor performance. It is shown that the torque depends not only on the area of the hysteresis loop, but also when harmonics are present, on the incremental permeability, a rotor material with high incremental permeability being most desirable. It is important to design the stator windings to minimize harmonics as far as possible.

The application of the theory gives a test of its accuracy, an indication of its applicability, and the relative numerical magnitudes of the quantities involved. The greatest discrepancy between predicted and measured torques was found to be 20 per cent, and this is believed to have its principal source in the approximations made to deal with nonlinear characteristics, especially for calculating the harmonics. In the case of shaded-pole motors, which have relatively larger harmonics, the difficulties would be greater and the calculations correspondingly less accurate. The application also indicates the need for more complete data than are available, particularly the *B-H* relations when rotating harmonics are superposed on a constant field.

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Discussion

For discussion, see page 1208.

Starting Performance of Salient-Pole Synchronous Motors

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IN recent years, two papers on the subject of salient-pole synchronous motors have been published in which practical methods for calculating the starting performance are derived on the basis of considerations that take into account all the unsymmetrics of this type of machine; the authors of these two papers are respectively H. V. Putman and T. M. Linville. Application of the Putman method gives at small values of the slip torque values that are somewhat too high; on the other hand, application of the Linville method is rather laborious, especially when the damper cage has a relatively large number of bars per pole. In view hereof, the author presents in this paper another method of calculating the starting performance of a salient-pole synchronous motor, a method which also takes into consideration all the unsymmetrics of this machine. This new method is, on the one hand, much simpler to apply than the Linville method, while on the other hand, it leads to more accurate results than the Putman method. It has been used for many years in the calculation and design of machines of various ratings and speeds, and has always given satisfactory results.

General Method of Calculation; Equivalent Flux Waves and Circuits

The assumption will be made that magnetic saturation can be neglected. It is then possible to superimpose the five fluxes set up by the five-winding systems of the three-phase machine. In doing this, only the fundamentals of these five fluxes will be taken into consideration.

The five fluxes just referred to will be replaced by two other fluxes which are stationary with respect to the field structure and whose axes coincide respectively with the center lines of the poles and with the center lines of the interpolar gaps. Furthermore, the unsymmetrical damper winding will be replaced by two single-axis windings whose axes also coincide respectively with the center lines of the poles and with the center lines of the interpolar gaps.

An illustration of this procedure is given in figure 1, which relates to a damper winding having six bars per pole; the two pulsating fluxes B_d (direct-axis flux) and B_q (quadrature-axis flux) are shown in figure 1*a*, and the two equivalent single-axis damper windings in figures 1*b* and 1*c*. It follows from figure 1*a* that the direct-axis flux produces in two damper bars that are located symmetrically with respect to the center line of the pole, currents of equal magnitude and of opposite direction, while it produces in two damper bars that are located symmetrically with respect to the center line of the interpolar gap, currents of equal magnitude and of equal direction. It is clear, therefore, that there cannot flow any currents induced by the direct-axis flux in the end-ring segments that connect together bars located in adjacent pole shoes, and that by virtue hereof the cage may be replaced by a winding arranged as per figure 1*b* as long as only the effect of the direct-axis flux is being considered. The quadrature-axis or cross flux, however, induces currents of equal magnitude and opposite direction in two damper bars that are located symmetrically with respect to the center line of the interpolar gap, and currents of equal magnitude and equal direction in two damper bars that are located symmetrically with respect to the center line of the pole. This means that the currents flowing through the damper bars in one-half of a pole shoe cannot take a return path through the damper bars in the other half of the same pole shoe, and that it is possible, therefore, to replace the damper cage by a winding as per figure 1*c* as long as only the effect of the quadrature-axis flux is being studied. Thereupon the resultant currents may be obtained by superposition, by virtue of the fact that the differential equations governing the current components are linear.

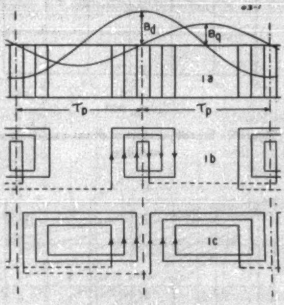

Figure 1. Direct-axis damper winding and quadrature-axis damper winding

The bars of the equivalent damper windings, figures 1*b* and 1*c*, are all connected in series, and this might be construed as being an indication that all these bars carry current of equal magnitude. This, however, is not in accordance with the facts, so that it becomes necessary to introduce appropriate breadth factors in order to take into account the actual conditions of current flow. If the new calculation method, when applied to the special case of an ordinary polyphase induction motor with squirrel-cage rotor, gives correct results, this may be considered as a criterion of the correctness of the method in general. It can be readily proved that in the case just referred to the results will be correct, that is, that the results obtained by calculating said induction motor as a machine having two equivalent rotor windings as per figure 1 will be the same as those obtained by the customary method of calculating the machine.

When the cage bars belonging to one and the same pole have not all the same resistance, they must be replaced by "equivalent" cage bars having all identically the same resistance over the entire pole width, whereupon the two single-axis damper windings as per figures 1*b* and 1*c* must be assumed to be formed by these "equivalent" cage bars.

In calculating the machine upon the introduction of those two single-axis damper windings, it can be taken into account that the winding as per figure 1*c* will have longer end turns than the winding as per figure 1*b*. Usually, however, this difference in end-turn length will be negligible, because in synchronous motors the resistances of the ring-segments are nearly negligible in comparison with the cage-bar resistances.

The equivalent single-axis damper wind-

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ing as per figure 1*b* will be called the direct-axis damper winding and will be designated by D_d, while the winding as per figure 1*c* will be called the quadrature-axis damper winding, designated by D_q.

It is shown in appendix I that the breadth factors of the two equivalent single-axis damper windings are equal to:

$$\xi_{Dd}=\frac{\sin\left(\frac{q_p-1}{4}\alpha\right)\sin\left(\frac{q_p+1}{4}\alpha\right)}{(q_p-1)\sin\frac{\alpha}{2}}\quad \text{for } q_p=\text{odd} \tag{1}$$

$$\xi_{Dd}=\frac{\sin^2\frac{q_p}{4}\alpha}{q_p\sin\frac{\alpha}{2}}\quad \text{for } q_p=\text{even} \tag{2}$$

$$\xi_{Dq}=\frac{\sin\frac{q_p}{2}\alpha}{2q_p\sin\frac{\alpha}{2}}\quad \text{for } q_p=\text{odd and even} \tag{3}$$

if q_p designates the number of damper bars per pole, and α the angle between two adjacent damper bars in electrical degrees. Using further the symbols whose meanings are given in the list of symbols found at the end of this paper, the factors to be introduced in order to reduce the damper windings to terms of the primary are found to be equal to:

$$\frac{m_1}{4p(q_p-1)}\left(\frac{w_1\xi_1}{\xi_{Dd}}\right)^2 \quad \text{for } q_p=\text{odd, or}$$

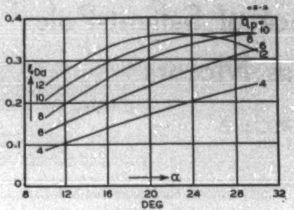
$$\frac{m_1}{4pq_p}\left(\frac{w_1\xi_1}{\xi_{Dd}}\right)^2 \quad \text{for } q_p=\text{even}$$

for one bar of the direct-axis winding, and to

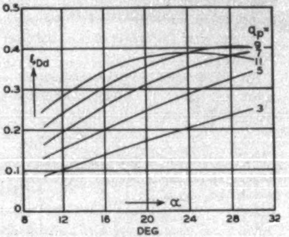
$$\frac{m_1}{4pq_p}\left(\frac{w_1\xi_1}{\xi_{Dq}}\right)^2 \quad \text{for } q_p=\text{odd and even}$$

for one bar of the quadrature-axis winding, p representing in either case the number of pairs of poles.

The breadth-factors ξ_{Dd} and ξ_{Dq} are represented in the figures 2 and 3 as functions of the angle α and for different values of q_p.



(a) q_p EVEN



(b) q_p ODD

Figure 2. Breadth-factors of direct-axis damper winding

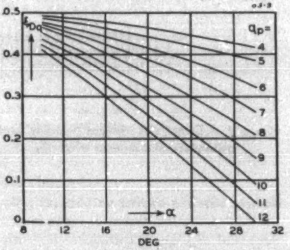


Figure 3. Breadth-factors of quadrature-axis damper winding

Torques and Current Equations

It will be shown in appendix *B* how in the first place the two fluxes B_d and B_q can be determined on the basis of certain simplifying assumptions, how subsequently the currents induced by these fluxes in the two single-axis damper windings can be calculated, and how finally the torque developed by the machine can be found. All quantities appearing in the final equations are per-unit quantities, unit voltage being the normal or rated phase voltage, unit current the current, and unit torque the torque corresponding to the normal or rated three-phase output, unit impedance the impedance through which unit current will be caused to flow by the rated voltage at the rated frequency. Using said per-unit quantities, the following simple expression for the torque is obtained:

$$T=\frac{1}{2}\left[\frac{\frac{r_{2d}}{s}}{\left[r_1+(1+\tau_{1d})\frac{r_{2d}}{s}\right]^2+[x_1+(1+\tau_{1d})x_{2d}]^2}+\frac{\frac{r_{Dq}}{s}}{\left[r_1+(1+\tau_{1q})\frac{r_{Dq}}{s}\right]^2+[x_1+(1+\tau_{1q})x_{Dq}]^2}\right] \tag{4}$$

The symbols appearing in this expression are partly explained in the list of symbols partly given in the equivalent circuit diagrams represented in figure 4 (direct-axis diagram) and figure 5 (quadrature-axis diagram).

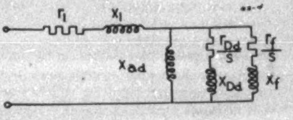


Figure 4. Equivalent circuit for direct axis

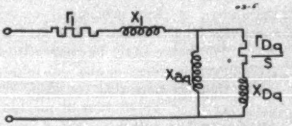


Figure 5. Equivalent circuit for quadrature axis

The expression for the current can be written as follows:

$$I=\frac{1}{2}\left[\sqrt{\frac{f_d^2+h_d^2}{k_d^2+l_d^2}}+\sqrt{\frac{f_q^2+h_q^2}{k_q^2+l_q^2}}\right]\times\cos\phi\times\eta \tag{5}$$

in which:

$f_d=1+\tau_{2d}$ $\qquad$ $f_q=1+\tau_{2q}$

$h_d=-\frac{\frac{r_{2d}}{s}}{x_{ad}}$ $\qquad$ $h_q=-\frac{\frac{r_{Dq}}{s}}{x_{aq}}$

$k_d=r_1+(1+\tau_{1d})\frac{r_{2d}}{s}$ $\qquad$ $k_q=r_1+(1+\tau_{1q})\frac{r_{Dq}}{s}$

$l_d=x_1+(1+\tau_{1d})x_{2d}$ $\qquad$ $l_q=x_1+(1+\tau_{1q})x_{Dq}$

while k_d, l_d, k_q, and l_q are the same quantities which appear in the denominator of T. Since the expression for the current has been multiplied with the product $\cos\phi\times\eta$, it gives the current expressed in the unit input-current.

The calculated and the measured torques for two different machines are represented on a comparative basis in the figures 6 and 7, while for one of these machines the calculated and measured current values are also given. It will be noted that in figure 6, the calculated torque values do not check so very well with the tested values when the slip is small. It should be noted that in many instances the discrepancies between calculated and tested values will be smaller than in figure 6, although occasionally they will be still larger. Those who are intimately acquainted with the subject in question will readily admit that it is

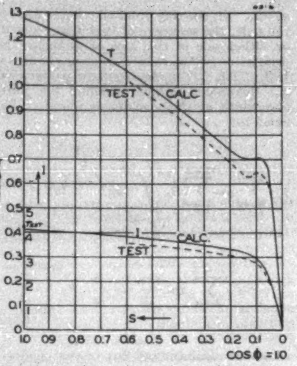


Figure 6. Calculation and test comparison for a synchronous motor, 780 horsepower, 32 poles

very difficult to take properly into account all the factors that affect the starting performance of a synchronous machine with salient poles.

It is equally possible to calculate on the basis of the equivalent circuit diagrams figure 4 and figure 5, the torque and the current by the method followed by Linville. The latter method is based on certain formulas derived by Park, and gives separately the forward current and the backward current [the latter current having the frequency $(2s-1)f_1$]. The forward current is found to be equal to:

$$I_f = \frac{1}{l^2+m^2}\sqrt{f^2+g^2}\cos\phi\times\eta \tag{6}$$

the backward current is found to be equal to:

$$I_b = \frac{1}{l^2+m^2}\sqrt{f'^2+g'^2}\cos\phi\times\eta \tag{7}$$

and for the torque the expression is found:

$$T = \frac{f}{l^2+m^2} - I_f^2 r_1 - I_b^2\frac{r_1}{1+2s} \tag{8}$$

in which:

$$r_{cd} = x_{ad}^2\frac{\dfrac{r_{3d}}{s}}{\left(\dfrac{r_{3d}}{s}\right)^2+[(1+r_{3d})x_{ad}]^2}$$

$$x_{cd} = \frac{x_{3d}x_{ad}^2+x_{ad}\left[\left(\dfrac{r_{3d}}{s}\right)^2+x_{3d}^2\right]}{\left(\dfrac{r_{3d}}{s}\right)^2+[(1+r_{3d})x_{ad}]^2}$$

$r_d = r_{cd}+r_1 \qquad x_d = x_{cd}+x_1$

$$r_d' = r_d+2r_1\frac{1-s}{2s-1}$$

$$r_{cq} = x_{aq}^2\frac{\dfrac{r_{Dq}}{s}}{\left(\dfrac{r_{Dq}}{s}\right)^2+[(1+r_{3q})x_{aq}]^2}$$

$$x_{cq} = \frac{x_{Dq}x_{aq}^2+x_{aq}\left[\left(\dfrac{r_{Dq}}{s}\right)^2+x_{Dq}^2\right]}{\left(\dfrac{r_{Dq}}{s}\right)+[(1+r_{1q})x_{aq}]^2}$$

$r_q = r_{cq}+r_1 \qquad x_q = x_{cq}+x_1$

$r_q' = r_q+2r_1\dfrac{1-s}{2s-1} \qquad a = r_d r_q' - x_d x_q$

$c = r_d' r_q - x_d x_q \qquad l = a+c$

$b = x_d r_q' + x_q r_d \qquad d = x_d r_q + x_q r_d'$

$m = b+d \qquad R = r_q' + r_d'$

$f = Rl + xm \qquad R' = r_q' - r_d'$

$f' = R'l + xm' \qquad x = x_q + x_d$

$g = xl - Rm \qquad x' = x_q - x_d$

$g' = x'l - R'm$

Using the following abbreviations:

$r_t = r_d + r_q \qquad x_t = x_d + x_q$

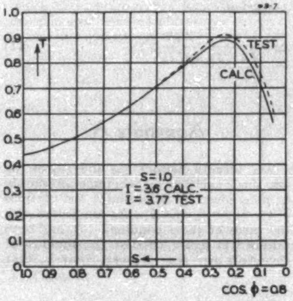


Figure 7. Calculation and test comparison for a synchronous motor, 1,200 horsepower, 56 poles

the following expressions for the currents and for the torque at one-half the synchronous speed ($s=0.5$) are obtained:

$$I_f = 2\frac{1}{\sqrt{r_t^2+x_t^2}}\cos\phi\times\eta$$

$$I_b = 0$$

$$T = \frac{2}{\sqrt{r_t^2+x_t^2}} - I_f^2 r_1$$

Influence of the D-C Excitation

Of considerable interest is the influence of the d-c excitation on the starting performance. The d-c excitation has the effect of producing a negative torque, that is, a torque which seeks to decelerate the rotor during start and which is the cause of the occurrence of "negative damping" in a synchronous machine. It will be shown in appendix *C* that the torque produced by the d-c excitation is given by the equation:

$$T_f = -\left(\frac{E}{U}\right)^2\frac{r_1[r_1^2+(1-s)^2x_q^2](1-s)}{[r_1^2+(1-s)^2x_dx_q]^2} \tag{9}$$

This torque, which is negative over the entire starting period, has a maximum in the neighborhood of the standstill point, and the magnitude which the slope of the curve representing this torque has in the neighborhood of the synchronous point is a measure of the "negative damping torque" of the synchronous machine. This magnitude of the slope of the torque curve in the vicinity of the point $s=0$ is the larger, the higher the resistance of the primary or a-c winding, and this explains why the "negative damping" of synchronous machines having a high primary resistance is relatively high.

In figure 8, which relates to the same synchronous machine as figure 6, the curve representing the torque produced by the d-c excitation is given for two different values of the primary resistance. This diagram clearly reveals the effect which the magnitude of the primary resistance has on the slope of the torque curve. In plotting this diagram, the assumption has been made that the electromotive force E is equal to the terminal voltage U.

It should be noted that the d-c excitation will have the effect of lowering the asynchronous torque developed by the machine at the instant at which it pulls into step, for the reason that the torque developed by the d-c excitation has the negative sign.

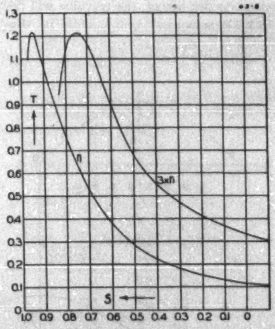


Figure 8. Torque produced by the d-c excitation on the motor, figure 6

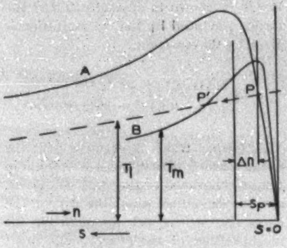


Figure 9. Pulling-into-step conditions

Pulling Into Step

The formula

$$s_p=\frac{621}{n}\sqrt{\frac{N_p}{f\times WR^2}} \tag{10}$$

derived by Edgerton and Zack for the slip at the pull-in point, that is, for the maximum value of the slip at which, under the most unfavorable switching conditions, the machine is still capable of pulling into step, is a very useful one. In fact, this formula has been found to give correct results not only in the case of a synchronized induction motor, but also in the case of a synchronous motor having salient poles. It checks with the formula published by Shoults, Lauder, and Crary in the latest paper devoted to the subject matter in question.

After the machine has reached its highest speed as an induction machine, as given by the torque curve that applies when the field winding of the machine is connected to a resistor (point *P* on curve *A* of figure 9), the d-c energizing voltage is applied to the field winding and the resistor is removed from the circuit. As a consequence hereof, the machine will decelerate somewhat and increase its slip, until it reaches a running point on the new torque curve (point *P'* on curve *B* of figure 9), that is, on the torque curve that is determined jointly by the squirrel-cage effect and by the effect of the d-c excitation. If on this occasion the slip increases to such an extent that it exceeds the value of the slip s_p, the machine will not be capable to pull into step. Tests have proved, however, that it is not necessary to consider the torque curve *B* (resistor removed from the field circuit) as an exclusive criterion of the pull-in conditions. In fact, it can be stated that the machine will pull into step without fail as long as the time interval of deceleration from the speed at point *P* down to the speed corresponding to the value of the slip s_p is of much longer duration than the time constant of the field circuit of the machine (determined for the case that the primary winding is short-circuited). In other words, it is merely necessary to satisfy the condition:

$$\Delta t=\frac{\Delta n}{\frac{T_m}{T_r}-\frac{T_l}{T_r}}\,\frac{WR^2}{307T_r}>T_f \tag{11}$$

Appendix A

The breadth factors ξ_{D_d} and ξ_{D_q} of the two single-axis damper windings can simply be determined by determining the portions of the fluxes B_d and B_q that are linked with the turns of these windings. It has been assumed in figure 10 that the number of cage bars per pole is even, that is, that $q_p=2n$. If, starting from the center line of the pole, the sequence of cage bars located on either side of this axis of symmetry is designated by the natural sequence of digits 1, 2, ... *k*, ... *n*, the resultant coil flux of the direct-axis damper winding per unit core-width of the machine is found to be equal to:

$$\psi=\sum_{k=1}^{k=n}\phi=\sum_{k=1}^{k=n}B_d\int_{\frac{\tau_p-(2k-1)a}{2}}^{\tau_p-\frac{\tau_p-(2k-1)a}{2}}\sin\left(\frac{\pi_p}{\tau}\xi\right)d\xi$$

$$=\frac{2}{\pi}\tau_pB_d\sum_{k=1}^{k=n}\sin\frac{2k-1}{2}\alpha=\frac{2}{\pi}\tau_pB_d\frac{\sin^2\frac{q_p}{4}\alpha}{\sin\frac{\alpha}{2}}$$

while the resultant coil-flux of the quadrature-axis damper winding (figure 6*b*) is found to be equal to:

$$\psi=\sum_{k=1}^{k=n}B_q\int_{-\frac{\tau_p-(2k-1)a}{2}}^{\frac{\tau_p-(2k-1)a}{2}}\cos\left(\frac{\pi}{\tau_p}\xi\right)d\xi$$

$$=\frac{2}{\pi}\tau_pB_q\sum_{k=1}^{k=n}\cos\frac{2k-1}{2}\alpha=\frac{2}{\pi}\tau_pB_q\frac{\sin\frac{q_p}{2}\alpha}{2\sin\frac{\alpha}{2}}$$

The breadth factors of the two windings are then obtained by dividing the expressions just given by $\frac{2}{\pi}\tau_pB_dq_p$ resp. $\frac{2}{\pi}\tau_pB_qq_p$.

If q_p is odd, $q_p=2n+1$, the resultant coil flux of the direct-axis damper winding

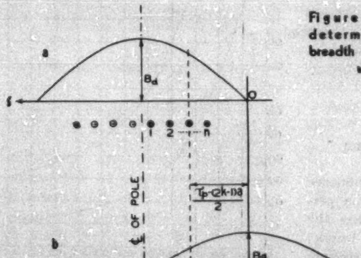


Figure 10. For determination of breadth factors, figures 2 and 3

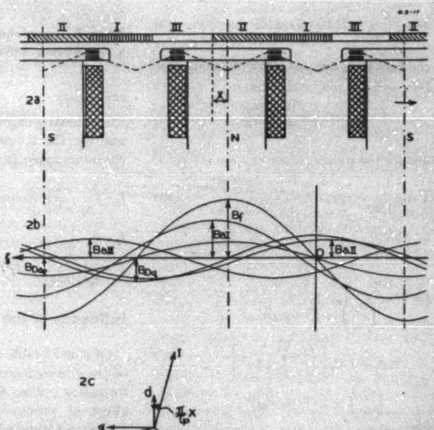


Figure 11. For determination of torque

is found to be equal to:

$$\psi=\frac{2}{\pi}\tau_p B_d\sum_{k=1}^{k=n}\sin(n\alpha)$$

$$=\frac{2}{\pi}\tau_p B_d\frac{\sin\frac{q_p-1}{4}\alpha\sin\frac{q_p+1}{4}\alpha}{\sin\frac{\alpha}{2}}$$

if the bar placed on the center-line of the pole is designated by the numeral zero. In analyzing the quadrature-axis damper winding, the cage bar designated by zero belongs simultaneously to two different winding elements. In view hereof, the resultant coil flux is determined in the first place with the 0th cage bar included in the winding, and in the second place without the 0th cage bar, whereupon the average of the two values thus obtained is computed. The result is:

$$\psi=\frac{1}{2}\frac{2}{\pi}\tau_p B_q\left[\sum_{k=0}^{k=n}\cos(n\alpha)+\sum_{k=1}^{k=n}\cos(n\alpha)\right]$$

$$=\frac{2}{\pi}\tau_p B_q\frac{\sin\frac{q_p}{2}\alpha}{2\sin\frac{\alpha}{2}}$$

The breadth factor of the direct-axis damper winding is obtained in this case by dividing the expression for the resultant coil-flux by $2/\pi\tau_p B_d(q_p-1)$, while the breadth factor of the quadrature-axis damper winding is again obtained by dividing the expression for the resultant coil flux by $2/\pi\tau_p B_q q_p$.

Appendix B

In figure 11*a* are shown the relative positions which the field system and the phase belts of the primary winding of a three-phase synchronous machine have with respect to one another at a given instant t. At this instant, the center of the north pole N is shifted toward the right over a distance x with respect to the center line of the winding phase I. In figure 11*b* are given the three flux-distribution curves for the three winding phases of the primary winding, the flux-distribution curve for the direct-axis damper winding, that for the quadrature-axis damper winding, and that for the field winding. These six fluxes may be replaced in an arbitrary manner by two other fluxes whose axes are perpendicular to each other. We shall choose these two fluxes in such a manner that one of them has its amplitude along the center line of the pole, while the other one has its amplitude along the center line of the interpolar gap; the former flux is then the resultant direct-axis flux B_d, and the latter is the resultant quadrature-axis flux B_q of the machine. It is then readily found (by projection) that the amplitudes B_{I}, B_{II}, B_{III} of the resultant fluxes along the axes of the primary winding phases are expressed by the following formulas (see figure 11*c*):

$$\left.\begin{aligned}B_{\mathrm{I}}&=B_d\cos\frac{\pi}{\tau_p}x-B_q\sin\frac{\pi}{\tau_p}x\\B_{\mathrm{II}}&=-B_d\sin\left(\frac{\pi}{6}-\frac{\pi}{\tau_p}x\right)+\\&\qquad B_q\cos\left(\frac{\pi}{6}-\frac{\pi}{\tau_p}x\right)\\B_{\mathrm{III}}&=-B_d\sin\left(\frac{\pi}{6}+\frac{\pi}{\tau_p}x\right)-\\&\qquad B_q\cos\left(\frac{\pi}{6}+\frac{\pi}{\tau_p}x\right)\end{aligned}\right\}\quad(12)$$

These amplitudes B_{I}, B_{II}, B_{III} can be expressed in a very simple way in the terminal voltage of the primary if the primary resistance and the primary reactance are neglected. We shall do this in the remaining part of this appendix, and show afterward, in appendix *C*, what the result will be when the primary resistance and the primary reactance are taken into account. But already in this appendix our knowledge of the theory of the induction machine will enable us to predict how r_1 and x_1 will affect the simplified formula that will now be derived.

It follows from the equations by means of which the electromotive forces induced in the primary winding-phases are expressed in the flux pulsations that:

$$\left.\begin{aligned}B_{\mathrm{I}}&=-\frac{\sqrt{2}U}{Z\omega}\cos\omega t\\B_{\mathrm{II}}&=-\frac{\sqrt{2}U}{Z\omega}\cos\left(\omega t-\frac{2\pi}{3}\right)\\B_{\mathrm{III}}&=-\frac{\sqrt{2}U}{Z\omega}\cos\left(\omega t-\frac{4\pi}{3}\right)\end{aligned}\right\}\quad(13)$$

in which

$$Z=\frac{2}{\pi}\tau_p l w_1\,\xi_1\,10^{-8}\quad(14)$$

Upon elimination of B_{I}, B_{II}, B_{III} between (12) and (13), we can solve for B_d and B_q, and obtain:

$$\left.\begin{aligned}B_d&=-\frac{\sqrt{2}U}{Z\omega}\cos\left(\frac{\pi}{\tau_p}x-\omega t\right)\\B_q&=\frac{\sqrt{2}U}{Z\omega}\sin\left(\frac{\pi}{\pi_p}x-\omega t\right)\end{aligned}\right\}\quad(15)$$

But

$$\frac{\pi}{\tau_p}x=\omega_r t$$

and

$$\frac{\pi}{\tau_p}x-\omega t=(\omega_r-\omega)t=-s\omega t\quad(16)$$

so that

$$\left.\begin{aligned}B_d&=-\frac{\sqrt{2}U}{Z\omega}\cos(s\omega t)\\B_q&=-\frac{\sqrt{2}U}{Z\omega}\sin(s\omega t)\end{aligned}\right\}\quad(17)$$

Thereupon we can find the currents in the rotor windings by expressing them in the known fluxes B_d and B_q. In the direct axis of the machine there are two rotor windings, the direct-axis damper winding and the field winding, which are connected in parallel (see figure 4). We shall introduce for this axis the resultant resistance and the resultant reactance, which are given by:

$$r_{2d}=\frac{(r_{Dd}x_{fm}^2+r_f x_{Dm}^2)s^2+r_{Dd}r_f r_{t2}}{r_{t2}^2+s^2x_{t2}^2}\quad(18)$$

$$x_{2d}=\frac{x_{Dd}(r_f^2+s^2x_f^2)+x_f(r_{Dd}^2+s^2x_{Dd}^2)+2x_m(r_{Dd}r_f-s^2x_{Dd}x_f)-s^2x_m^2x_{t2}}{r_{t2}^2+s^2x_{t2}^2}\quad(19)$$

in which:

$$\left.\begin{aligned}x_{Dm}&=x_{Dd}-x_m\qquad &r_{t2}&=r_{Dd}+r_f\\x_{fm}&=x_f-x_m\qquad &x_{t2}&=x_{Dm}+x_{fm}\end{aligned}\right\}\quad(20)$$

x_m designating the reactance corresponding to the mutual inductance due to the leakage fluxes.

The rotor currents for the two axes are then found to be equal to:

$$\left.\begin{aligned}i_{2d}&=-\sqrt{2}U\frac{s}{\sqrt{r_{2d}^2+s^2x_{2d}^2}}\times\\&\qquad\sin\left(s\omega t-tg^{-1}\frac{sx_{2d}}{r_{2d}}\right)\\i_{2q}&=+\sqrt{2}U\frac{s}{\sqrt{r_{Dq}^2+s^2x_{Dq}^2}}\times\\&\qquad\cos\left(s\omega t-tg^{-1}\frac{sx_{Dq}}{r_{Dq}}\right)\end{aligned}\right\}\quad(21)$$

The torque exerted on the stator in the direction opposite to the direction of rotation is equal to (R being the radius of the stator bore):

$$\begin{aligned}T=-2w_1\xi_1 lR\Big[&i_{\mathrm{I}}B_d\cos\left(\frac{\pi}{2}-\frac{\pi}{\tau_p}x\right)-\\&i_{\mathrm{II}}B_d\cos\left(\frac{\pi}{6}-\frac{\pi}{\tau_p}x\right)+i_{\mathrm{III}}B_d\cos\left(\frac{\pi}{6}+\frac{\pi}{\tau_p}x\right)+\\&i_{\mathrm{I}}B_q\cos\frac{\pi}{\tau_p}x-i_{\mathrm{II}}B_q\sin\left(\frac{\pi}{6}-\frac{\pi}{\tau_p}x\right)-\\&i_{\mathrm{III}}B_q\sin\left(\frac{\pi}{6}+\frac{\pi}{\tau_p}x\right)\Big]\end{aligned}\quad(22)$$

The currents i_{I}, i_{II}, i_{III} appearing in this equation can be expressed in the rotor currents i_{2d} and i_{2q}. This requires, however, that the ampere turns produced by the winding phases of the primary shall be expressed mathematically with reference to a co-ordinate ξ (see figure 7*b*) that rotates together with the rotor. The corresponding expressions are:

$$\left.\begin{aligned}\theta_{\mathrm{I}}&=\Theta_{\mathrm{I}}\sin\left(\frac{\pi}{\tau_p}\xi-\frac{\pi}{\tau_p}x\right)\\\theta_{\mathrm{II}}&=\Theta_{\mathrm{II}}\cos\left(\frac{\pi}{\tau_p}\xi-\frac{\pi}{\tau_p}x+\frac{\pi}{6}\right)\\\theta_{\mathrm{III}}&=-\Theta_{\mathrm{III}}\sin\left(\frac{\pi}{\tau_p}\xi-\frac{\pi}{\tau_p}x+\frac{\pi}{3}\right)\end{aligned}\right\}\quad(23)$$

and apply for any value of the time t, while the resultant magnitude of all the primary ampere-turns is equal to:

$$\Theta_{\mathrm{I}}+\Theta_{\mathrm{II}}+\Theta_{\mathrm{III}}=\Theta_{1d}\sin\frac{\pi}{\tau_p}\xi+\Theta_{1q}\cos\frac{\pi}{\tau_p}\xi\quad(24)$$

in which:

$$\left.\begin{aligned}\Theta_{1d}&=\theta_{\mathrm{I}}\cos\frac{\pi}{\tau_p}x-\Theta_{\mathrm{II}}\sin\left(\frac{\pi}{6}-\frac{\pi}{\tau_p}x\right)-\\&\qquad\Theta_{\mathrm{III}}\cos\left(\frac{\pi}{3}-\frac{\pi}{\tau_p}x\right)\\\Theta_{1q}&=-\Theta_{\mathrm{I}}\sin\frac{\pi}{\tau_p}x+\Theta_{\mathrm{II}}\cos\left(\frac{\pi}{6}-\frac{\pi}{\tau_p}x\right)-\\&\qquad\Theta_{\mathrm{III}}\sin\left(\frac{\pi}{3}-\frac{\pi}{\tau_p}x\right)\end{aligned}\right\}\quad(25)$$

Equation 25 indicates how the three primary ampere-turn distributions can be replaced by two ampere-turn distributions that rotate together with the rotor, one of the latter ampere-turn distributions having its amplitude (Θ_{1d}) on the center line of the pole, while the other one has its amplitude (Θ_{1q}) on the center line of the interpolar gap. The ampere-turn distribution with amplitude Θ_{1d} is the direct axis, and the one with amplitude Θ_{1q} is the quadrature-axis distribution produced by the primary winding.

It then follows that the resultant direct-axis distribution is equal to:

$$\Theta_d=\Theta_{1d}+\Theta_{2d} \tag{26}$$

while the resultant quadrature-axis distribution is equal to:

$$\Theta_q=\Theta_{1q}+\Theta_{2q} \tag{27}$$

These equations give, in connection with (25), for the amplitude of the resultant direct-axis flux:

$$B_d=c_d\left[i_{\mathrm{I}}\cos\frac{\pi}{\tau_p}x-i_{\mathrm{II}}\sin\left(\frac{\pi}{6}-\frac{\pi}{\tau_p}x\right)-i_{\mathrm{III}}\cos\left(\frac{\pi}{3}-\frac{\pi}{\tau_p}x\right)\right]+c_di_{2d} \tag{28}$$

and for the amplitude of the resultant quadrature-axis flux:

$$B_q=c_q\left[-i_{\mathrm{I}}\sin\frac{\pi}{\tau_p}x+i_{\mathrm{II}}\cos\left(\frac{\pi}{6}-\frac{\pi}{\tau_p}x\right)-i_{\mathrm{III}}\sin\left(\frac{\pi}{3}-\frac{\pi}{\tau_p}x\right)\right]+c_qi_{2q} \tag{29}$$

When the expressions for B_d and B_q given by (28) and (29) are introduced into the torque equation 22, this equation becomes:

$$T=Zp\left(i_{2d}B_q-i_{2q}B_d+\frac{c_d-c_q}{c_dc_q}B_dB_q\right) \tag{30}$$

and if subsequently the values for B_d and B_q as given by (17) and the values for i_{2d} and i_{2q} as given by (21) are introduced into (30), the equation expressing the starting torque as a per-unit quantity is obtained in the form:

$$T=\frac{1}{2}\left[\frac{r_{2d}}{\left(\frac{r_{2d}}{s}\right)^2+x_{2d}^2}+\frac{r_{Dq}}{\left(\frac{r_{Dq}}{s}\right)^2+x_{Dq}^2}-\frac{1}{\sqrt{\left(\frac{r_{2d}}{s}\right)^2+x_{2d}^2}}\cos\left(2s\omega t-t_g^{-1}\frac{sx_{2d}}{r_{2d}}\right)+\frac{1}{\sqrt{\left(\frac{r_{Dq}}{s}\right)^2+x_{Dq}^2}}\cos\left(2s\omega t-t_g^{-1}\frac{sx_{Dq}}{r_{Dq}}\right)-\frac{c_d-c_q}{c_dc_q}\frac{1}{\omega Z}\sin(2s\omega t)\right] \tag{31}$$

The first two terms of the expression between square brackets appearing in the second member of this equation represent the constant torque components produced respectively by the direct-axis damper winding and by the quadrature-axis damper winding. The third, fourth, and fifth terms represent torque components that pulsate with twice the slip frequency, the third and the fourth terms being due to the unsymmetry of the damper winding, and the fifth term to the difference in the values of the magnetic reluctance in the direct axis and in the quadrature axis.

On the basis of our knowledge of the theory of induction machines, we can indicate at once the more general form which the constant component of the torque will have when the primary resistance and the primary reactance of the machine are taken into account:

$$T=\frac{1}{2}\left[\frac{\frac{r_{2d}}{s}}{\left[r_1+(1+\tau_{1d})\frac{r_{2d}}{s}\right]^2+[x_1+(1+\tau_{1d})x_{2d}]^2}+\frac{\frac{r_{Dq}}{s}}{\left[r_1+(1+\tau_{1q})\frac{r_{Dq}}{s}\right]^2+[x_1+(1+\tau_{1q})x_{Dq}]^2}\right]$$

Appendix C

An investigation of the influence of the d-c excitation and of the primary resistance and reactance on the starting performance will not be dependent on the manner in which the rotor windings are arranged on the field system. In other words, we are free to assume that the machine has no damper winding, its field system being equipped only with a magnetizing winding. The investigation can further be simplified by assuming that the stator of the machine is equipped with a two-phase winding. The set of equations 12 must then be replaced by the set:

$$\left.\begin{aligned}B_{\mathrm{I}}&=B_d\cos\frac{\pi}{\tau_p}x-B_q\sin\frac{\pi}{\tau_p}x\\B_{\mathrm{II}}&=B_d\sin\frac{\pi}{\tau_p}x+B_q\cos\frac{\pi}{\tau_p}x\end{aligned}\right\} \tag{32}$$

and the voltage equations for the three circuits appear in the form:

$$\left.\begin{aligned}\sqrt{2}u\sin\omega t&=Z\frac{dB_{\mathrm{I}}}{dt}+L_1\frac{di_{\mathrm{I}}}{dt}+r_1i_{\mathrm{I}}\\\sqrt{2}u\cos\omega t&=Z\frac{dB_{\mathrm{II}}}{dt}+L_1\frac{di_{\mathrm{II}}}{dt}+r_1i_{\mathrm{II}}\\U_f&=Z\frac{dB_d}{dt}+L_f\frac{di_f}{dt}+r_fi_f\end{aligned}\right\} \tag{33}$$

The torque equation 30 can be written in this case more simply:

$$T=Zp\left[i_fB_q+\frac{c_d-c_q}{c_dc_q}B_dB_q\right] \tag{34}$$

The quantities i_f, B_q, and B_d appearing in the latter equation have to be expressed in the voltages applied to the windings of the machine. This requires in the first place that in the voltage equations 33 the quantities B_{I}, B_{II}, i_{I}, i_{II} shall be replaced by B_d, B_q, and i_f. Equation 32 gives us one relation, between B_{I}, B_{II} on the one hand and B_d, B_q on the other hand. Additional relations are obtained by writing down equations analogous to (28) and (29) for the case under consideration:

$$B_d=c_d\left(i_f+i_{\mathrm{I}}\cos\frac{\pi}{\tau_p}x+i_{\mathrm{II}}\sin\frac{\pi}{\tau_p}x\right) \tag{35}$$

$$B_q=c_q\left(-i_{\mathrm{I}}\sin\frac{\pi}{\tau_p}x+i_{\mathrm{II}}\cos\frac{\pi}{\tau_p}x\right) \tag{36}$$

If we now introduce a new variable, defined by:

$$I_d=\frac{B_d}{c_d}-i_f \tag{37}$$

we can bring the equations 35 and 36 into the following form:

$$\left.\begin{aligned}i_{\mathrm{I}}&=I_d\cos\frac{\pi}{\tau_p}x-\frac{B_q}{c_q}\sin\frac{\pi}{\tau_p}x\\i_{\mathrm{II}}&=I_d\sin\frac{\pi}{\tau_p}x+\frac{B_q}{c_q}\cos\frac{\pi}{\tau_p}x\end{aligned}\right\} \tag{38}$$

We can now substitute in the voltage equations 33 for B_{I} and B_{II} the values given by (32), and for i_{I} and i_{II} the values given by (38), whereupon, with the aid of the relations expressed by (16) and of those given in the following:

$$Zc_d=\frac{x_{ad}}{\omega}\qquad Zc_q=\frac{x_{aq}}{\omega}$$

$$\frac{L_1}{Zc_d}=\tau_{1d}\qquad\frac{L_1}{Zc_q}=\tau_{1q}\qquad\frac{L_f}{Zc_d}=\tau_2$$

$$1+\tau_{1d}=k_{1d}\qquad1+\tau_{1q}=k_{1q}\qquad1+\tau_2=k_2$$

the voltage equations can be brought into the form:

$$\left.\begin{aligned}-Z\omega_rB_d-\frac{r_1}{c_q}B_q-L_1\omega_rI_d-Zk_{1q}B_q'&=\sqrt{2}U\cos(s\omega t)\\-Z\omega_rk_{1q}B_q+r_1I_d+ZB_d'+L_1I_d'&=\sqrt{2}U\sin(s\omega t)\\\frac{r_f}{c_d}B_d-r_fI_d+Zk_2B_d'-L_fI_d'&=U_f\end{aligned}\right\} \tag{39}$$

The latter equations immediately give us three linear, nonhomogeneous differential equations of the third order, which, upon solution, yield the following expressions for the three unknown variables:

$$\left.\begin{aligned}B_d&=\frac{C_dG+D_dH}{G^2+H^2}\sin(s\omega t)+\frac{D_dG-C_dH}{G^2+H^2}\cos(s\omega t)+\frac{K_d}{a_8}\\B_q&=\frac{C_qG+D_qH}{G^2+H^2}\sin(s\omega t)+\frac{D_qG-C_qH}{G^2+H^2}\cos(s\omega t)+\frac{K_q}{a_8}\\I_d&=\frac{C_IG+D_IH}{G^2+H^2}\sin(s\omega t)+\frac{D_IG-C_IH}{G^2+H^2}\cos(s\omega t)+\frac{K_I}{a_8}\end{aligned}\right\} \tag{40}$$

in which:

$$K_d=\frac{U_f}{Z}\left[\frac{r_1^2}{Zc_q}+\omega_r^2k_{1q}L_1\right]$$

$$C_d=-\frac{\sqrt{2}U}{Z}k_{1q}s\omega L_f\left[\omega(2s-1)-\frac{r_1r_f}{Zc_qk_{1q}s\omega L_f}\right]$$

$$D_d=\frac{\sqrt{2}U}{Z}k_{1q}r_f\left[\omega(2s-1)+\frac{r_1s\omega L_f}{Zc_qk_{1q}r_f}\right]$$

$$K_q=-\frac{U_f}{Z}r_1\omega_r$$

$$C_q=\frac{\sqrt{2}U}{Z}k_{1d}r_f\left[\omega(2s-1)+\frac{s\omega r_1k_2}{r_fk_{1d}}\right]$$

$$D_q=\frac{\sqrt{2}U}{Z}s\omega(L_fk_{1d}+L_1)\left[\omega(2s-1)-\frac{r_1r_f}{Zc_ds\omega(L_fk_{1d}+L_1)}\right]$$

$$K_1=-\frac{U_f}{Z}\omega_r^2k_{1q}^2$$

$$C_1=-\frac{\sqrt{2}U}{Z}\,s\omega Zk_2k_{1q}\left[\omega(2s-1)-\frac{r_1r_f}{Zc_dZc_qk_2k_{1q}s\omega}\right]$$

$$D_1=\frac{\sqrt{2}U}{Z}k_{1q}\frac{r_f}{c_d}\left[\omega(2s-1)+\frac{r_1s\omega k_2Zc_d}{r_fZc_qk_{1q}}\right]$$

$$G=a_3-(s\omega)^2a_1$$
$$H=-s\omega[a_2-(s\omega)^2a_0]$$

$$a_1=\frac{r_1}{Zc_q}(L_fk_{1d}+L_1)+k_{1q}(r_fk_{1d}+r_1k_2)$$

$$a_2=\frac{r_1r_f}{Zc_d}k_{1q}+\frac{r_1}{Zc_q}(r_fk_{1d}+r_1k_2)+\omega_r^2k_{1q}(L_fk_{1d}+L_1)$$

$$a_3=r_f\left(\frac{r_1^2}{Zc_dZc_q}+\omega_r^2k_{1d}k_{1q}\right)$$

$$a_0=k_{1q}(L_fk_{1d}+L_1)$$

Since the torque equation 34 can be brought into the form:

$$T=ZpB_q\left[\frac{B_d}{c_q}-I_d\right] \tag{41}$$

the torque can be readily calculated with the aid of the equations 40. As in appendix *B*, the torque obtained in this case consists of components of constant magnitude and of components that vary with time. In order to obtain for the torque an expression which clearly reveals these various components, we first simplify the derivation by introducing into the set of equations 40:

$$\left.\begin{aligned}B_q&=\lambda_{1Q}\sin(s\omega t)+\lambda_{2Q}\cos(s\omega t)+\lambda_{3Q}\\ \frac{B_d}{c_q}-I_d&=\lambda_{1D}\sin(s\omega t)+\lambda_{2D}\cos(s\omega t)+\lambda_{3D}\end{aligned}\right\}\tag{42}$$

The expression for the torque can then be written:

$$T=\text{const.}\left\{\frac{\lambda_{1Q}\lambda_{1D}+\lambda_{2Q}\lambda_{2D}}{2}+\left[\frac{\lambda_{2Q}\lambda_{2D}-\lambda_{1Q}\lambda_{1D}}{2}\cos(2s\omega t)+\frac{\lambda_{1Q}\lambda_{2D}+\lambda_{2Q}\lambda_{1D}}{2}\sin(2s\omega t)\right]+[(\lambda_{1Q}\lambda_{3D}+\lambda_{1D}\lambda_{3Q})\sin(s\omega t)+(\lambda_{2Q}\lambda_{3D}+\lambda_{2D}\lambda_{3Q})\times\cos(s\omega t)]+\lambda_{3Q}\lambda_{3D}\right\} \tag{43}$$

The pulsating torque components are produced by the same causes as in appendix *B*. The constant torque components are two in number. The first component

$$\text{const.}\frac{\lambda_{1D}\lambda_{1Q}+\lambda_{2D}\lambda_{2Q}}{2}$$

is produced by the effect of the primary or stator flux on the field winding (which plays in this respect the role of a secondary or induced winding) and actually constitutes the "asynchronous" torque delivered by the machine. This torque component is only of interest to us in the present case in so far as we expect it to reveal the effect which the primary resistance and the primary reactance have on the torque. A complete analysis shows that the primary reactance enters into the equation as an additive term in conjunction with the secondary reactance. The influence of the primary resistance is not as simple as in the case in which equation 4 applies, as a consequence of the fact that the rotor winding of the machine is now a single-axis winding. If it is assumed that $c_d=c_q$, the following quantity appears in numerator of the expression for the torque:

$$\omega(2s-1)Z\left[\omega(2s-1)k_{1d}+\frac{r_1}{r_f}\frac{s\omega}{k_{1d}}\right]$$

and it is seen that this quantity becomes zero for $s=0.5$, making the torque equal to zero at a running speed in the vicinity of one-half the synchronous speed. This is nothing but the well-known phenomenon encountered in the operation of a polyphase induction motor having a single-phase rotor winding. This vanishing of the torque will not occur, however, in the limiting case where $r_1=0$. And this peculiar influence of r_1 on the torque curve will hardly be noticeable when the field system is equipped with a damper winding.

The primary resistance r_1 further affects the second constant component of the torque, the component represented by const. $\lambda_{3Q}\lambda_{3D}$. This torque component is produced by the field current in conjunction with the currents in the primary winding; its magnitude is given by equation 9. It has been shown in the foregoing that this torque component is negative during the entire starting period, that is, from standstill up to synchronism, and that for this reason the starting process may be adversely affected when d-c excitation is applied too soon to the field winding. This torque component has normally its maximum in the neighborhood of the standstill point.

List of Symbols

a = distance between two rotor bars
B_d = field amplitude in the direct axis
B_q = field amplitude in the quadrature axis
c_d = constant given by equation 28, appendix *B*
c_q = constant given by equation 29, appendix *B*
E = electromotive force induced in the stator winding by the rotor flux
f = line frequency
I_f = forward current
I_b = backward current
$K_{1d}=1+\tau_{1d}$
$K_{1q}=1+\tau_{1q}$
l = ideal core width
$L=\frac{x_1}{2\pi f}$ coefficient of the primary leakage inductance
m_1 = phase number of the primary winding
n = revolutions per minute
p = number of pole pairs
q_p = number of damper bars per pole
r_1 = primary resistance
r_f = resistance of the field circuit
r_{Dd} = resistance of the direct-axis damper winding
r_{Dq} = resistance of the quadrature-axis damper winding
r_{2d} = resultant secondary resistance in the direct axis
T = torque
T_f = time constant of the field circuit
U = terminal voltage of the stator
w_1 = number of turns of the primary winding per phase
x_1 = primary leakage reactance
x_f = leakage reactance of the field winding
x_{Dd} = leakage reactance of the direct-axis damper winding
x_{Dq} = leakage reactance of the quadrature-axis damper winding
x_m = mutual-inductance reactance due to the leakage fluxes of direct-axis damper winding and field winding
x_{2d} = resultant secondary leakage in the direct axis
x_{ad} = reactance due to the main flux in the direct axis
x_{aq} = reactance due to the main flux in the quadrature axis
$x_d=x_1+x_{ad}$
$x_q=x_1+x_{aq}$
α = angle between two damper bars
η = efficiency
θ = magnetomotive force
Θ = amplitude of the magnetomotive force distribution
ξ_1 = distribution factor times chord factor of the primary winding
ξ_{Dd} = breadth factor of the direct-axis damper winding
ξ_{Dq} = breadth factor of the quadrature-axis damper winding
$\omega=2\pi f$
$\omega_r=\frac{2\pi pn}{60}$
τ_p = pole pitch
$\tau_{1d}=\frac{x_1}{x_{ad}},\ \tau_{2d}=\frac{x_{2d}}{x_{ad}},\ \tau_{1q}=\frac{x_1}{x_{aq}},\ \tau_{2q}=\frac{x_{Dq}}{x_{aq}}$

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Discussion

For discussion, see page 1215.

Heating of a D-C Armature

CARL J. FECHHEIMER
FELLOW AIEE

I. Purposes

THE purposes of these investigations were: (1) to determine what the maximum temperatures of the copper and iron were, and to compare those maximum temperatures with thermometer readings; (2) to measure the average of the maximum temperatures by resistance and to compare the results with the average secured from the maximum copper temperatures; (3) to measure the surface temperatures by means of a special contact thermocouple, and to compare those readings with those obtained by other means; (4) to measure the maximum commutator temperatures in several axial positions by the contact thermocouple, and to compare the results with thermometer readings; (5) to determine more accurate methods of calculating temperatures; and (6) to determine how to decrease the temperatures.

II. Special Measurements and Methods of Conducting Tests

The tests were made on a 300-kw 1,200-rpm 250-volt armature, the d-c machine being direct connected to a synchronous motor. When the motor generator set was disconnected from the line, dynamic braking was used on the synchronous machine, so that the set was brought to a standstill in very short time, and readings were taken before there was sufficient internal heat flow to modify the maximum temperatures greatly. The maximum temperatures were obtained by plotting temperature-time curves on semi-log paper projected back to the instant of shutdown.

The tests were made several years ago on a machine that was recognized to be unsatisfactory from the standpoint of heating, and the design has since been modified. It is believed that some of the methods of measurement used were novel, and that descriptions of similar methods have not appeared in the technical press.

DESCRIPTION OF APPARATUS

It is recognized that temperature measurements of the bare copper, or of the iron in a rotating armature by detectors are generally unsatisfactory, because when slip rings are used, too many rings are required if a large number of readings is desired, and if thermocouples are employed, there are liable to be inaccuracies. The method adopted was to use thermocouples, and to take a series of readings, which when plotted against time and extrapolated back to zero time, gave the maximum temperatures.

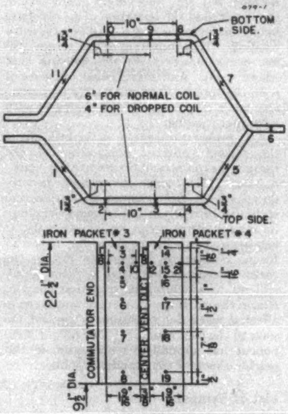

Figure 1. Location of embedded thermocouples

The thermocouple junctions on the bare copper were formed by soldering number 26 constantan wire to the copper conductor (the armature copper being the copper side of the junctions), there being a total of 11 junctions on each of two adjacent armature coils, located as shown in figure 1. One of the armature coils was of the usual type; the other was dropped at the rear for suitable cross connectors. They are designated as "normal" and "dropped" coils. It was necessary to have only one copper wire lead from the armature copper to serve for all 22 thermocouples on the armature copper. The constantan leads were all brought out at the rear at the U bends of the coils.

In two iron packets axially adjacent to the central vent duct, thermocouple junctions were placed, arranged as shown in figure 1. The iron packets selected were circumferentially between the top sides of the two adjacent coils referred to above. The thermocouple metals used were 0.005 inch thick, copper and constantan, and were insulated with mica from the iron. Near the inner periphery, a few laminations were cut back $\frac{1}{2}$ inch; number 26 copper and constantan wires were soldered on, and the wires were brought out axially through a slot cut in one of the spider arms to the rear of the armature. Suitable sleeving was placed over the wires in the slot.

In order to secure readings rapidly after shutdown, three special plug boards were made, each provided with 15 contacts. The construction is shown in figures 2 and 3, and photographs (figures 4 and 5) show the plug boards on the machine. The three boards were spaced 120 degrees apart, and each board was provided with guide pins so that they could be pushed "home" after shutdown without loss of time. In each of the individual contacts, the stationary member had a projecting

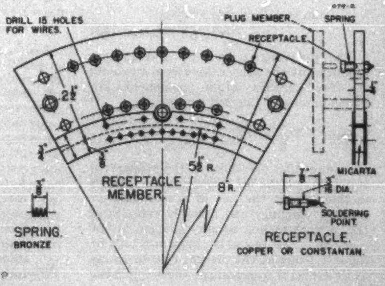

Figure 2. Receptacle side of plug board

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1. For all numbered references, see list at end of paper.

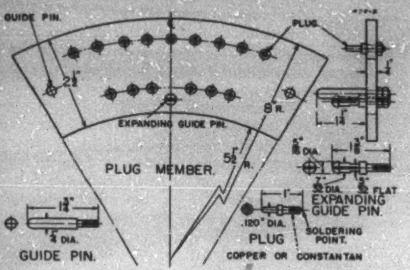

Figure 3. Plug side of plug board

pin which fitted loosely into the hole bored into its mate in the revolving member. The contact was made at the shoulder, suitable springs being provided to insure ample pressure at that surface. The precaution was observed of making the two members of each plug of the same kind of metal as the lead-in and lead-out wires; otherwise there might have been a difference in temperature which would have introduced an error. Each wire leading from the stationary parts of the plugs was joined to a contact button on a dial plate, there being two dial plates in use simultaneously. Readings were taken with potentiometers.

Two men took observations at each dial plate. As soon as the main circuits were opened, one man started a stop watch, and the other started to take readings as soon as the machine was stopped, turned over to the proper position, and the plug boards joined to the receptacle members. From five to seven thermocouples were read on each dial plate in succession, the time and potentiometer reading recorded by the man with the stop watch. After reading the series, they were successively repeated for a total lapse of about 15 minutes. It was possible to obtain an initial reading in about 45 seconds after the main circuits were opened. The temperature test was repeated until all thermocouples were read.

Figure 4. Receptacle and plug members disconnected

The average temperature was obtained by measuring a part of the armature resistance. The positions for measurement were marked on the commutator, the measurement being made on less than the span of one pole, so as not to include the effect of short-circuiting by the brushes. The particular coils in which the thermocouples were placed were included in the group measured. The armature was turned after shutdown so as to occupy the same position as when the cold resistance was measured. The cold resistance was measured at intervals during about six hours, the armature temperature by thermometer being noted during that period of time. All resistances were measured with the same Kelvin bridge.

Figure 5. Receptacle and plug members connected

A special contact thermocouple was also made in which a very thin ribbon junction was laid on cork placed in a recess near the end of a Micarta bar of square cross section. See figures 6 and 7. The cork acted as a spring when the thermocouple junction was pressed against the surface. The cork has low specific thermal capacity, and is a very poor conductor of heat, which made for quick response and accuracy. The contact thermocouple was used for securing iron surface and commutator temperatures. Time readings were taken along with the other readings.

Thermometer readings were taken in the usual manner, and time readings were taken. In this way, a comparison is made between the maximum temperatures and thermometer readings, at the instant that the thermometers reached their maxima, as well as the maxima noted by other means prior to the thermometers reading their maxima.

III. Results

In figure 8, the important parts of the armature are shown.

Only a few of the test time-temperature curves are included. The end points were obtained by plotting on semi-log paper, and although those curves were not quite straight lines, greater accuracy was obtained that way than by projecting back on usual co-ordinate paper.* The curves included herewith are all plotted on usual paper, they having been transcribed from the semi-log plots.

Figures 9 and 10 are typical bare-copper time-temperature curves of various positions on one of the "dropped" coils at 100 per cent load. Figure 9 is for the top, and figure 10 for the bottom side of the same coil. A similar set of curves, but for the iron and for 125 per cent load, for various radial positions, are shown in figure 11. A comparison between contact thermocouple and thermometer readings can be obtained from figure 12. Another typical set of thermometer time-temperature curves is given in figure 13. An armature time-resistance curve is shown in figure 14.

The distribution of initial (end point) temperatures radially in the iron for the various conditions of load is shown in figure 15. In all except the no-load tem-

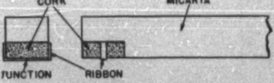

Figure 6. Contact thermocouple

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* When plotting, temperature was plotted on the logarithmic scale and time on the linear scale. This follows from the well-known cooling equation $\theta = \theta_{\max}\epsilon^{-t/T}$.

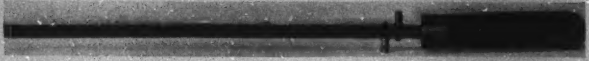


Figure 7. Contact thermocouple

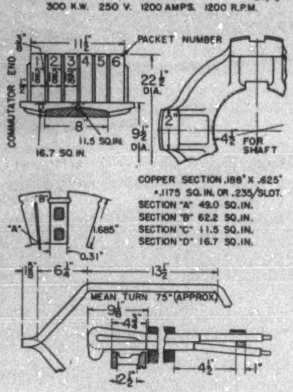


Figure 8. Drawing of parts

perature test (for which they were substantially equal), the temperatures in packet number 3 (near the commutator end) were higher than in packet number 4. This is because the air flow was restricted by the limited section between the internal periphery of the core and the external periphery of the spider hub (see figure 8). With load, the temperatures in packet number 3 were appreciably higher than in number 4. In figure 16 the axial distribution in the iron is shown for 125 per cent load. The axial temperature distribution in an iron packet should theoretically be a parabola with its maximum at or near the middle of the packet; however for packet number 4 the maximum was near the central vent duct, proving that there was insufficient air passing through that vent. The temperature near the central vent for packet number 3 is not far from that in number 4, but it rises toward the middle of the packet, and its average is greater than for number 4. Unfortunately, the thermocouple nearest to the commutator side in packet number 3 open-circuited, so that the complete axial distribution curve in that packet could not be obtained.

Some commutator temperature rises are plotted in figure 17. They are from two tests at 100 per cent load by thermometer, one at 125 per cent load by thermometer and by contact thermocouple, and one at 125 per cent current on short-circuit by contact thermocouple. The results are interesting, because they show how much lower the readings by thermometer were than by contact thermocouple, and because they show the approximate axial distribution of temperatures. In figure 18, time-temperature tests for 125 per cent load by contact thermocouple and thermometer are plotted. It is of interest to note that for such time as was required for the thermometer to reach its maximum, the reading was only a few degrees lower by thermometer than by contact thermocouple, but the end-point temperature by the latter was considerably higher than by the former method.

The maximum test temperatures are recorded in table I, data being given therein as can be read from the curves, but as only a few representative curve sheets are included in this paper, some data in table I cannot be obtained from the curve sheets.

In figure 19 are shown the test and calculated longitudinal temperature distribution along the copper at 100 per cent load. The curves are for the top and bottom coils, and the test points apply to the normal and to the dropped coil. The test points are the maxima, or end points. The calculated curves are symmetrical about the center line, but the test curves are not. This is because, as previously mentioned, the air flow was restricted by the limited section between the inner periphery of the core and the external periphery of the spider hub. Especially did this apply for the location at the central vent, and consequently the embedded copper temperatures were higher at the side nearer to the commutator than those nearer to the rear. The agreement between test and calculation for the top coil is quite close, but there are appreciable differences for the bottom coil: the test temperatures were higher than calculated. This may be explained by the following: (1) there probably was more heat flow from the top to the bottom coils than was allowed for in the calculations; (2) due to higher magnetic densities, the tooth loss adjacent to the bottom coil was considerably higher than for the top part; (3) and that there was some eddy current loss in the bottom coil due to flux threading it because of tooth saturation, an allowance having been made, but was probably insufficient. An interesting point is that the maximum temperature

Figure 9. Bare copper time-temperature curves

Top side of coil, "dropped" coil

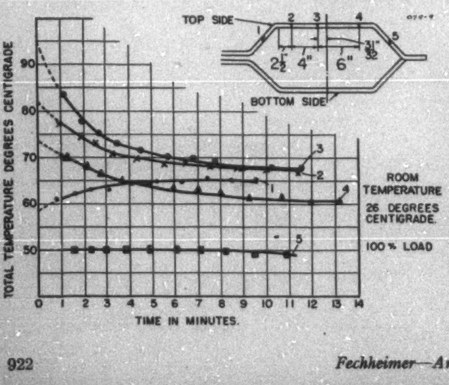
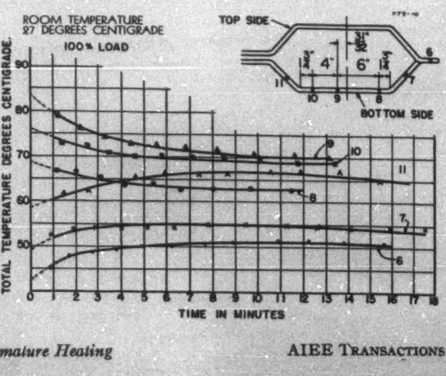


Figure 10. Bare-copper time-temperature curves

Bottom side of coil, "dropped" coil



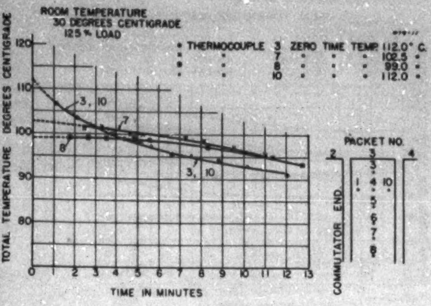


Figure 11. Iron-packet time-temperature curves by embedded thermocouple

at, or near, the center line would be considerably higher were the influence of longitudinal flow in the copper absent. The calculations indicate that it would have been about 16 degrees higher at 100 per cent load, and about 26 degrees higher at 125 per cent load. In longer-core machines the beneficial effect of longitudinal flow would not have been so effective in reducing the copper temperatures.

Without doubt, the considerable difference between the top and bottom coil temperatures is due to the difference in eddy-current loss. By stranding the copper, as has been done in subsequent machines, this loss is greatly reduced.

The curves in figure 19 were calculated by the methods given by Soderberg[1] and the writer.[2] The calculations are long and tedious, and few engineers can find sufficient time for such laborious calculations. Those calculations, therefore, are not included in this paper. A method which appears in another paper,[3] is therefore suggested. That would involve tests on a number of armatures, and from such tests constants can be obtained, from which temperatures in other armatures of comparable proportions can be predicted. A few results of such application to the particular armature tested are given subsequently by way of illustration; such tests, if conducted with care, particularly in regard to the measurement of temperature, should not be expensive.

IV. Comments Upon Results

In order that a clearer mental picture of the temperatures be obtained, more important maximum temperature rises are given in table II.

Although the particular armature on which these investigations were conducted is probably not typical of most d-c armatures, and the differences in temperatures between the various methods of measurement were consequently greater than they would be in most armatures, it will be evident that the usual method of measurement by thermometer does not give the maximum temperature. Thus, the iron rise at normal load was 39 degrees by thermometer, 48 degrees by contact thermocouple, and 71 degrees by embedded thermocouple. Whereas embedded detectors can be employed only in occasional machines, the contact thermocouple can be used readily, is inexpensive, and gives more reliable readings than does a thermometer. Also, the temperature rises of the commutator were appreciably higher by contact thermocouple than by thermometer; and although the armature was not of modern design, which was partly the cause of large differences between embedded and surface measurements, this does not apply to the commutator. The writer has used a contact thermocouple frequently on other machines since these tests were made, and has come to the belief that it is a dependable method for measuring surface temperatures.

The peculiar shape of the contact-thermocouple curve in figure 12 is interesting. The initial rise is readily explained by the thermal capacity of the measuring device. An explanation for the second rise is that the heat flow from the copper to the iron was sufficient to more than counterbalance heat escapement from the external surface. The second maximum was reached at 12.5 minutes; and by comparison with figures 9 and 10, the mean of the top and bottom coil copper near the middle of the armature was about 3.5 degrees higher than the iron by contact thermocouple at the instant, which is a reasonable difference.

Attention is called to average temperature rises. In tables II and III they are given as (1) by resistance, (2) by mean of ordinates of initial temperatures (from figure 19 for 100 per cent load and similar curves, not included with this paper, for 125 per cent load). The calculated average temperature rises in table III were obtained by the method given in two previous papers.[1,2]

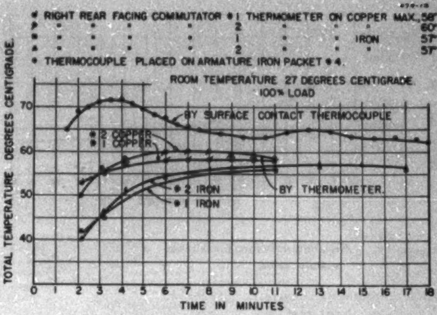


Figure 12. Iron-surface time-temperature curves

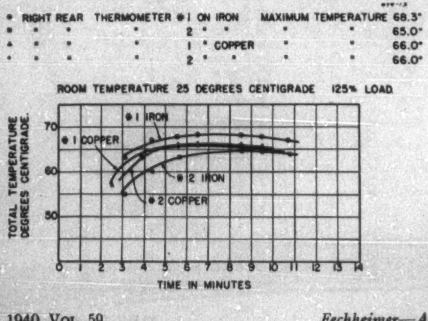


Figure 13. Iron-surface and coil-end time-temperature curves by thermometer

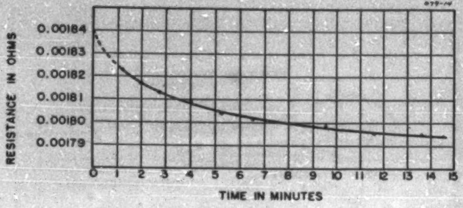


Figure 14. Armature-resistance times temperature curve

Room temperature 25 degrees centigrade; 100 per cent load

Temperature rise by resistance = 35.5 degrees

Resistance at 24 degrees centigrade = 0.001619 ohm

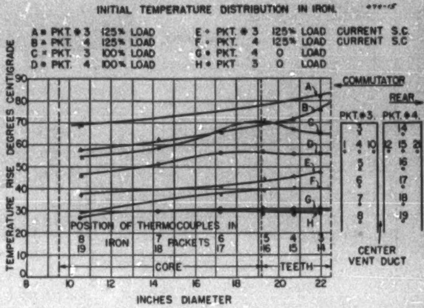


Figure 15. Maximum temperature rise distribution in iron, radially

V. Calculations From Tests

In table III will be found in parallel columns calculations of temperature rise and test results. The calculations are from no-load and short-circuit tests, as described in a previous paper.[3] For the test results, the averages for the bare copper were taken as the arithmetical mean for location numbers 3 and 9 (see figure 1), for both normal and dropped coils. The averages for the iron by embedded thermocouples are the mean of readings at 100 per cent load at positions 3, 4, 5, 14, 15, and 16; and at 125 per cent load the mean for positions 3, 14, 15, and 16. For all thermocouple readings in table III, the extreme values entering into the determination of the averages are also recorded. For all thermocouple readings in table III, the extreme values entering into the determination of the averages are also recorded in table III.

For calculating the detector temperature rises in table III, the means for the

Table I. Maximum Temperature Rises in a D-C Armature

(See Figure 1 for Location of Thermocouples)

Couple Number	Per Cent Load Kind of Coil	100 Dropped	100 Normal	125 Dropped	125 Normal	No-Load Dropped	No-Load Normal	125 Per Cent S.C. Dropped	125 Per Cent S.C. Normal
On bare copper									
By Embedded Thermocouple									
1		32.5	26	47	42.5			41.0	34.5
2		56.2	56.5	91.5	89.0	19.2	14.1	61.0	56.5
3		69.0	67.0	100.5	94.5	24.3	19.5	64.5	61.0
4		46.5	51.8	71.5	71.5	18.0	15.3	52.0	49.0
5		29.0	34.0	40.0	46.5			34.0	37.0
6		15.5	27.5	27.5	36.5			20.7	32.0
7		22.0	27.0	30.0	35.0			27.0	31.5
8		41.5	36.0	55.5	55.5	18.0	13.5	38.0	39.0
9		57.0	53.0	76.0	65.5	23.5	18.5	58.5	56.0
10		49.0	49.0	67.0	59.0	19.0	17.5	48.5	50.5
11		31.5	31.0	41.2	30.0			35.0	37.5
In iron packets									
Packet Number									
3	4	3	4	3	4	3	4	3	4
1	12		57.0		74.0		32.0		41.5
3	14	65.0	55.5	82.0	76.0	29.0	31.0	47.5	40.5
4	15	67.0	55.5		71.0	28.5	31.0	45.5	40.5
5	16	71.0	56.5		70.0	29.5	31.0	45.0	39.5
6	17		56.8		66.0	30.0	31.0	40.5	37.0
7	18	58.5	51.0	72.5	63.0	30.0	30.0	40.0	31.5
8	19	54.5	46.5	69.0	57.5	27.0	27.0	37.5	30.0
10	21	60.0	50.0	82.0	61.5	30.0	28.0	45.0	36.0
By Thermometer									
	Iron—front	39.0		44.5		20.0		31.0	
	Iron—rear	31.0		49.0		17.5		26.5	
	Coil-end front	38.0		47.5		15.5		37.0	
	Coil-end rear	34.5		39.5		14.5		29.5	
	Commutator front	44.5		49.0					
	Commutator middle	44.5		50.0					
	Commutator rear	44.5		49.0					
By Contact Couple									
	Iron packet number 3			54.5				35.0	
	Iron packet number 4	48.0		50.5				31.0	
	Commutator front			69.0				67.0	
	Commutator middle			66.5				70.5	
	Commutator rear			61.5				55.	

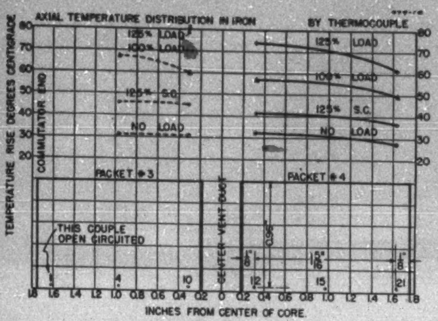


Figure 16. Maximum temperature rise distribution in iron, axially

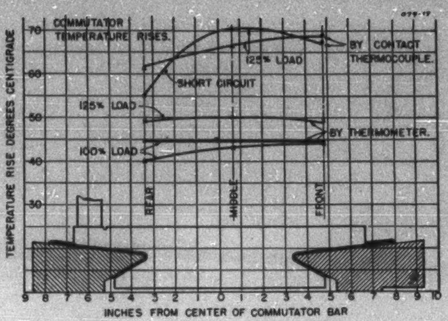


Figure 17. Maximum temperature rise distribution on commutator

copper and iron temperature rises from the no-load and short-circuit tests, for positions as mentioned above, were used for determining the heat resistance factors, R_{11}, R_{12}, and R_{22}, as given in the previous paper.[3] Suitable allowances were made for air rise, θ_{a1} for both heat runs. The load temperature rises were then calculated by equations 20 and 22 in the same paper.[3] Considering the rather wide variation in load test readings, it is believed that the calculated temperature rises check reasonably well with those from test.

The calculated temperature rises by thermometer in table III were obtained by adding the maximum thermometer rises from the no-load and short-circuit temperature tests, without correction for the increase in losses due to incremental increases of copper temperature. The justification for neglecting departures in air temperature is that the tests on open circuit and short circuit were for the same voltage and current for load conditions as obtained in the 125 per cent load test; the sum of the air temperature rises for the two cases is substantially the same as the temperature rise of the air with the same load, since the sum of the losses for the two tests is nearly the same as for 125 per cent load. There would be a slight correction downward for the 100 per cent load test, and had this been done the agreement would probably have been better. The use of the maximum readings from the no-load and short-circuit tests compensates in part for not having allowed for the increase in copper resistance with temperature.

For the contact-thermocouple readings, the readings are all for packet number 4, as the readings for packet number 3 were incomplete. Allowances for increased copper resistance were made on the basis of the sum of the constituent temperature rises.

VI. Conclusions

1. The internal temperatures of a rotating armature can be measured with what is believed to be considerable accuracy with embedded thermocouples and by taking series of time-temperature readings, if care and precautions are observed. The points should be plotted on semi-log paper and projected back to zero time.

2. Surface temperatures of the armature iron, coil ends, and commutator can be measured quicker with a contact thermocouple than with thermometers. It is believed, also, that the results are more accurate with the former than with the latter method, and generally show higher temperatures.

3. Armature average temperature rise by resistance can be measured with considerable accuracy if (*a*) the hot and cold measurements are made between the same commutator bars which do not include any brushes; (*b*) a series of time-temperature readings is taken and plotted on semi-log paper; (*c*) the

Figure 18. Commutator-surface time-temperature curves

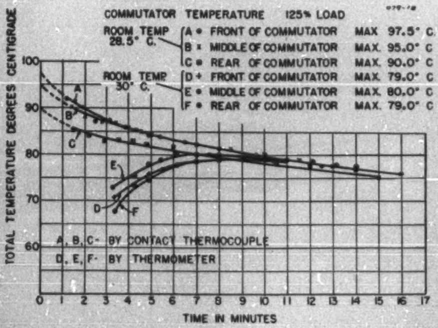


Figure 19. Armature-copper longitudinal temperature rise

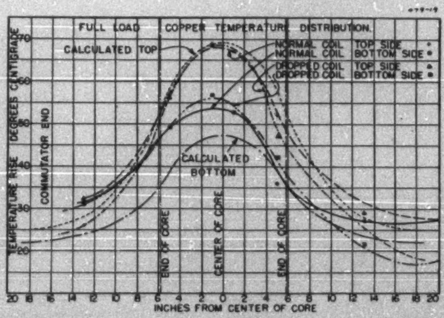


Table II. Comparison of Temperature Rises, Degrees Centigrade

	Iron			Copper				Commutator	
						Average			
	Embedded Detector	Contact Thermocouple	Thermometer	Embedded Detector	Thermometer on Ends	By Resistance	From Maximum Ordinate	Contact Thermocouple	Thermometer
100 per cent load	71.0	48.0	39.0	69.0	38.0	35.5	38		44.5
125 per cent load	82.0	54.5	49.0	100.5	47.5	58.5	55	69.0	50.0

Table III

	Temperature Rises, Degrees Centigrade			
	100 Per Cent Load		125 Per Cent Load	
	Calculated	Test	Calculated	Test
Copper by detector	64.6	53 to 69 Avg. 61.5	90.0	65.5 to 100.5 Avg. 84.2
Iron by detector	63.3	55.5 to 71 Avg. 61.7	79.6	70 to 82 Avg. 74.7
Mean copper { By resistance	36.8	35.5	51.0	58.5
Mean copper { Mean ordinate		38.0		55
Thermometer on iron	40.0	39	54	49.0
Thermometer on coil ends	39.2	38.0	52.5	47.5
Contact thermocouple	40.3	40 to 48 Avg. 44.1	52.5	50.5

same instrument, preferably a Kelvin bridge, is used for the hot and cold readings; and (*d*) cold readings of resistance are taken over a sufficiently long period to insure that the temperature of the armature copper is the same as that of the armature surface, measured by thermometer, or other suitable means.

4. The commutator surface temperatures may be appreciably higher than determined by the usual thermometer method. It is believed that an improvement can be effected with the aid of a contact thermocouple.

5. A suggested method for predicting temperatures is to make open and short-circuit tests on typical machines, and obtain data for typical slotting, from which heat resistance factors can be obtained for future calculations. For securing such data, it may be advisable that the armature be run at no load at a number of speeds and excitations, and on short circuit at perhaps two values of current.

6. General conclusions as to temperature differences between actual copper (or iron) and thermometer (or contact thermocouple) cannot be drawn from tests on a single armature. It is believed that the methods may prove to be helpful to others working in the same, or similar fields.

7. For the particular armature that was used in these investigations, reductions in temperatures could be secured by (*a*) stranding the conductors, so as to reduce the eddy current loss, and (*b*) increasing the section for axial air flow by changing the mechanical design of the spider hub. The particular armature tested has been obsolete for a number of years, and these suggestions have been adopted in more recent machines. Mica insulation has been used for a great many years, and this was especially the case for armatures of older design, thereby safeguarding them against failure.

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Discussion

For discussion, see page 1210.

"Excitron" Mercury-Arc Rectifiers

O. K. MARTI
FELLOW AIEE

FOR some time it was hoped that the present active research in atomic nuclear physics would solve many of the unknown phenomena connected with the electric arc, especially the arc in vacuum, and that this would clarify some of the phenomena of the rectification process in a vacuum tube. However, in spite of the fact that little progress was made in applying the latest theories to the practical application of rectifiers, nevertheless, a considerable step forward was made during the last few years in the design of large electronic glass and metal tubes.

The conventional rectifiers of the multiple-anode type have been perfected during the last decade to such an extent that extremely satisfactory service can be given for any kind of power load. However, they have one drawback, and that is the fact that the arc drop increases with the increase in size of the rectifier tank or tube. It can easily be seen in figure 1 that by increasing the tank diameter D, and therefore the distance of the anodes from the cathode, consequently this increases the arc length, a, which results in higher arc-drop loss and lower efficiency. The arc length could be reduced by providing a cathode pool of very large diameter or a ring cathode. This, however, would complicate the design of the rectifier. In figure 6, there are shown two curves, A and B, which illustrate the arc drop of two 12-anode rectifiers. Curve A applies to a rectifier of larger tank diameter than that to which curve B refers.

In connection with other electrical machines, usually an increase in size or capacity means an increase in efficiency, and it was therefore the desire of rectifier engineers for some time to obtain the same results with rectifiers. We therefore find that during the last few years when industry called for larger and larger rectifier units, rectifier tanks were used in multiple on common rectifier transformers, and that several large rectifier plants were installed with two, four, or even six tanks on one rectifier transformer. However, it was found that even this arrangement of sectionalizing rectifier units did not give the desired results in so far as arc drop or efficiency is concerned. The gain in efficiency did not justify the increase in number of auxiliary devices such as evacuating equipment, excitation and ignition devices, etc., which are necessary with the sectionalized rectifier.

Single-Anode-Tank Mercury-Arc Rectifier

INTERMITTENT IGNITION-EXCITATION

Recently an attempt was made to effect a low arc drop by sectionalizing the rectifier unit into as many tanks as anodes; in other words, each anode having its own individual tank. This design received a great deal of encouragement due to the introduction of a means for intermittent ignition which permitted the starting of the arc at any time during the positive half of each cycle. There are several ways to accomplish this, namely, by immersing the ignition anode (or ignitor as it is sometimes called) directly into the cathode mercury, or by using an insulated electrode (starting band) which may be either next to the cathode mercury or immersed in it but insulated from it. The first electrode arrangement permits initiating a cathode spot by discharging a relatively high current through the electrode and the latter accomplishes the same end by using a high-potential discharge. In one case an electrothermic-dynamic action produces the cathode spot; in the second case, it is a purely electrostatic phenomenon. With these devices it is possible to ignite each tank at the time the anode of that tank becomes more positive in voltage than the anode of any other tank comprising that particular rectifier unit. It was hoped that the use of such a method of ignition would reduce greatly the sensitivity to backfires in that no cathode spot, and therefore little or no ionization, would exist during the time the anode is at a negative potential.

This was accomplished by establishing a cathode spot anew during each cycle by means of current discharges which, however, have to take place over a very short time interval in order to keep losses at a minimum. In figure 2 is illustrated the means necessary for intermittent striking of the arc. Usually starting tubes T_1, T_2, T_3, etc. are used in order to effect the discharge at the proper time, and also concentrate the energy within a very small time interval so as to obtain a cathode spot with greatest intensity.

The sequence of operation may easily be seen in figure 2. As one of the main anodes A_1, A_2, A_3, in sequence, becomes more positive, the grid of one of the associated starting tubes T_1, T_2, T_3, etc. also becomes positive. When the potential on the anode of the starting tube reaches a predetermined value, current I_1, I_2, I_3 will flow to the respective ignition systems. This energy discharge causes an incandescent spot to be formed at the point where the ignition anode is immersed into the cathode mercury, causing electrons to be emitted from the latter. The main anode can then, if at the proper potential, carry current. As soon as the main anode begins to carry current the starting tube will extinguish, as the arc drop in the main arc is not sufficient to maintain a current through the starting tube. Hence the cathode spot initiated through the ignition anode discharge is maintained only as long as the main arc is firing and therefore has to be re-established every cycle.

Such a scheme has, however, shown several drawbacks, namely, the current necessary to establish a cathode spot of sufficient density is relatively large, and unless it is accomplished in a very brief interval, the losses become prohibitively large. If the cathode does not emit sufficient electrons, the cathode spot may not permit the main anodes to pick up current or then only after some delay in time. However, it is extremely important that all tanks pick up load at the correct instant during each cycle. Should any one tank fail to pick up load at the proper time, an unbalance in the transformer may take place which may cause high-frequency surges due to d-c saturation of the transformer core. Furthermore, if an anode does not fire at the proper instant, a very poor current distribution among the anodes may result, causing some of the tanks to carry undue overloads during this time interval. Another disturbance due to such a failure may consist of the generation of abnormal harmonics or excessive wave-shape distortion, see figure 3. During no-load and full-load periods, A and B, respectively, the anodes are firing in their normal sequence while during period C one anode failed to fire, resulting in a considerable dip in voltage.

Besides this, the successful operation of the ignition anode depends upon its im-

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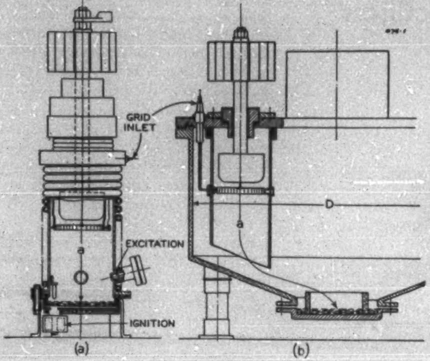

Figure 1. Cross section of a 600-volt Excitron and a conventional-type rectifier, illustrating anode, grid, and cathode assembly

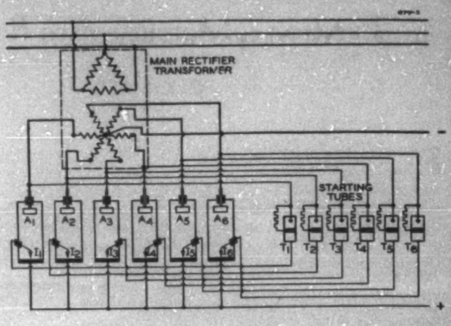

Figure 2. Typical ignitron-rectifier connection diagram showing six single-anode tanks

mersion into the cathode mercury of a very accurately maintained distance so that the heat density is localized in the immediate vicinity of the ignitor. Otherwise, the emission of electrons may not be sufficient for the main anodes to carry current or pick up at the proper instant. Therefore, the mercury level of the cathode in such single-anode rectifiers must be carefully maintained and should not be subjected to violent changes or vibrations which may disturb the depth to which the ignition anode should be immersed in the mercury. It may, therefore, be very difficult to use such a device on ships or locomotives where vibrations or fluctuations may affect the surface of the cathode mercury.

Continuous Ignition-Excitation

The main advantage usually referred to in connection with the intermittent-ignition rectifier is believed to be due to the fact that during the time the anode carries no current, and is subjected to a negative potential, there is no ionization in the tank. Several investigators have even thought this feature would make possible a rectifier design which would be practically backfire-proof, and would still permit locating the anode directly above the cathode, in this way reducing the arc length and, in turn, the arc drop. This deduction was probably based on the assumption that in the conventional multiple-anode rectifier a cathode spot and an arc are maintained during the time some of the anodes are at a negative potential. This arc and cathode spot cause ionization of the mercury vapor in the space under the anodes, which may become negative in potential; this ionization would then be responsible for increasing the backfire sensitivity. Extensive investigations on rectifiers operating on the test floor and also under actual load conditions have, however, indicated that such is not the case. It was found that the backfires in a conventional multiple-anode rectifier occur not only shortly after the anode has been carrying current and has been subjected to the maximum ionization, but also—much later in time. This and other considerations substantiated by extensive tests encouraged the Allis-Chalmers Manufacturing Company to build a six-anode rectifier, consisting of six single tanks using an ignition-excitation system for the main arc with a continuous cathode spot. In other words each tank will have some additional ionization besides the residue during the negative voltage period as a small current is maintained by an auxiliary anode. This type of rectifier has been called the Excitron rectifier.

Construction of the "Excitron" Rectifier

The construction of the anode and grid assembly is very similar to a commercial multiple-anode rectifier, as shown in figure 1. The grids are insulated from anode and tank and can be energized through a bushing, inserted near the anode. It was found advisable to insulate the cathode from the tank and to provide means for easy removability of this plate. The detachable anode plate can be eliminated, as the assembly of anode and grid can be done through the cathode opening.

The grids and shields are so designed and located that they provide adequate deionization and baffling. The ignition and excitation anodes are arranged and shielded in such a way so that little ionization is added to the residuum holding over during the negative-voltage period. It can therefore be seen that the design of the ignition and excitation system had to be based on somewhat different considerations than for the multianode rectifier. It would require a lengthy discussion to establish the theoretical basis for proving why a lower arc drop can be obtained in such a design than in a multianode tank, giving due considerations to the tendency of the rectifier to backfire. The arc drop can be made less in spite of the presence of grids and shields, at the same time the tendency to backfire would not be increased in spite of the direct exposure of the anodes to the cathode spot, and the splashing of mercury droplets from the cathode surface; this will be the subject of a separate future paper.

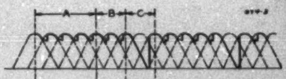

Figure 3. Direct-voltage wave shape with normal firing, with delayed firing, and if one anode fails to fire

The tanks are evacuated with a standard evacuating equipment consisting of a mercury-vapor and rotary oil pump, as shown in figure 4. The 6-tank set shown in this figure is rated 2,000 amperes at 600 volts, and if the number of tanks is doubled, a rating of 4,000 amperes and

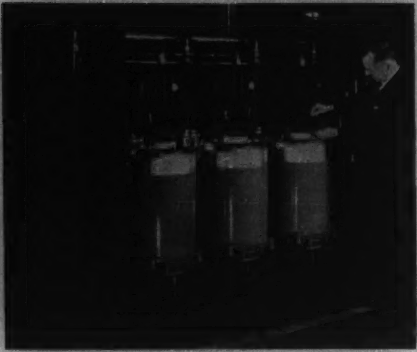

Figure 4. A 600-volt Excitron with control cabinet, evacuating and vacuum-measuring equipment

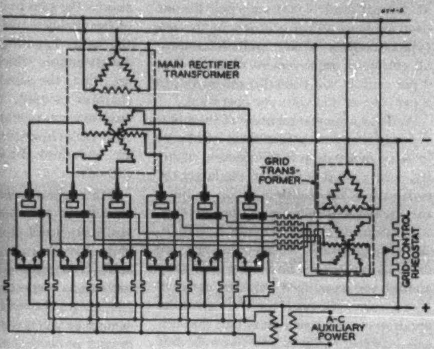

Figure 5. Typical Excitron rectifier connection diagram showing six single-anode tanks

600 volts would be obtained, which rating would correspond to a 12-anode multiple tank rectifier. Due to the fact that this rectifier has a very small thermal capacity because its weight per kilowatt is far less than that of a multiple-anode rectifier of the same rating, a special cooling system was developed which enabled this rectifier to be subjected to very heavy overloads and still maintain a safe operating temperature. The rectifier tanks are equipped with copper cooling coils which are connected to a mixing tank using a water-circulating pump.

Starting of the "Excitron" Mercury-Arc Rectifier

After extensive tests, it was found that a very small current can maintain a cathode spot of sufficient density and start the main arc firing exactly at the right instant of the cycle, even at very low temperatures of the tank. This was accomplished by a suitable selection for the location of the starting anodes. In order to make such a rectifier available for operation, an auxiliary arc is established only once by means of an auxiliary anode or anodes, controlled by automatic equipment. This auxiliary arc can be established with a smaller mercury-jet ignition consisting of a solenoid-operated plunger fitted over a tube and extending down from the cathode. If the coil is energized, a mercury jet is thrown up against an extension from the tank. As the jet collapses, an arc is established starting the auxiliary excitation arc. This arc will then maintain a cathode spot on the mercury and as soon as the main anode becomes energized, the power arc can flow to the individual anode. The establishing of the main arc will not depend on any delicate timing or tube equipment, but is accomplished by a most reliable device—namely, a transformer.

Figure 5 shows the diagram of connections of the auxiliary circuit. It can be seen that any kind of operating condition can be obtained by a proper interlocking of the ignition-excitation. The automatic control can be equipped with the necessary relays and time-delay devices in order to provide proper protection, restarting, and stopping of the cathode spot.

The control of the rectifier voltage can be effected by combining a variable d-c bias, supplied by a battery or obtained by a rheostat in a potentiometer connection from the rectifier bus and with an auxiliary source of alternating current for the grids, see figure 5. This imposes a d-c bias between the rectifier cathode and the neutral point of the grid-transformer secondary, so that the neutral point is at an adjustable d-c potential with respect to the rectifier cathode.

The operating voltage of the rectifier may be set at any desired value, so as to give the rectifier any suitable operating characteristic—drooping, flat-compounded, or overcompounded. This type of grid control was repeatedly described in connection with multiple-anode rectifiers and need therefore be referred to here only briefly.

The ability of a grid to prevent the flow of current from an anode during arbitrary time intervals makes it possible to use grids for completely interrupting the flow of current through the rectifier. When the grids of a rectifier are energized at a suitable negative potential, with respect to the cathode, the anode then carrying current continues to do so until its voltage becomes negative, when it will stop firing as a result of its valve action. The negative potential on the grids of the other anodes then prevents them from picking up the arc, and since the anode first considered will likewise be unable to again pick up the arc, the rectifier becomes blocked in a fraction of a cycle of the alternating voltage. The grids in the Excitron rectifier can therefore also be used for interrupting the current flow during abnormal operation, such as a short circuit, or backfire; for instance, applying a negative potential to the grid by using a relay which may be actuated from the primary or secondary circuit. It was found that the action of the grids is just as reliable in such a rectifier as it is in conventional multiple-anode units.

Further protective features are obtained by interconnecting the excitation circuit with the grid control circuit in such a way that the grids block the anodes from carrying current, and the cathode spot becomes extinguished by other means at the same time, so that the whole tank is made inoperative in the most reliable way, since there will be no ionization next to the anode and cathode.

Comparison of Arc Drop and Efficiency

In figure 6 are indicated the arc drops of two multiple-anode-type rectifiers, in curves *A* and *B*, each having 12 anodes, but having different diameters *D* (see figure 1*b*), and of a 12-tank single-anode rectifier, in curve *C*. It can be seen that at normal load, 4,000 amperes, the arc drop is about 40 per cent less in the new type of rectifier, which results in an overall efficiency improvement of about 3 to 4 per cent at 250 volts d-c and of $1^{1}/_{2}$ to 2 per cent at 600 volts d-c operation.

Another important feature of these new types of rectifiers is that there will be no increase in arc drop with increased capacity. As pointed out above, the larger the capacity of the multiple-anode rectifier, the greater the increase of the arc drop with capacity; see curves *A* and *B*. With single-anode tank units the arc drop stays the same no matter how many anodes are combined on one common transformer. Curve *C* therefore gives the arc drop of a 6-anode unit of half the indicated current rating or of a 24-anode unit of twice the indicated current rating.

Large multiple-anode-type rectifiers are at present being developed which have ring cathodes in order to avoid the above drawback, compare also figure 1, parts *a* and *b*. It will be interesting to see how the arc drop of single-anode tanks of different diameters will compare with each other and whether it will be possible to obtain a design having nearly the theoretical minimum arc drop without unduly increasing the tendency to backfires.

The efficiencies for the conventional and the Excitron type rectifiers at 600 volts are indicated in figure 6, curves *A′*, *B′*, and *C′*, respectively. It can be seen that at such a relatively high voltage the single-anode rectifier does not seem to present any considerable advantage.

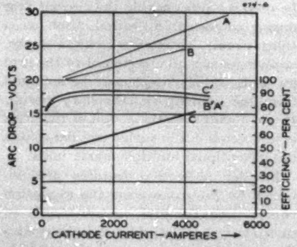

Figure 6. Comparative arc drop and efficiency curves of a double six-tank Excitron rectifier and of a conventional rectifier

Discussion of Relative Advantages and Disadvantages

Some of the advantages of these single-anode tank rectifiers over the conventional type are quite obvious as, for instance, the gain in efficiency and decrease of headroom required, and, furthermore, the easier handling of such units The disadvantages which may be mentioned are, first, the small volume of the arc chamber, second, the large amount of auxiliary equipment due to the fact that each tank requires means for igniting the arc, and, third, the necessity of a multiple cathode.

Other advantages as well as the disadvantages can, however, not be stated positively until a sufficient number of units have been operating on different kinds of loads, and for different types of services for some time. Some of the disadvantages, however, may be overcome or may be taken care of by the use of improved protective equipment. We have here in mind the small volumetric capacity of the individual tanks which may be a tremendous drawback in case a large reverse current has to be carried by the backfiring unit, causing in turn a very high vapor pressure. In such a case the possibility exists of reignition, especially in case of a very highly inductive circuit which may supply enough energy to cause an anode to fire over several cycles. Arrangements could be made, however, to feed this energy back into the a-c system, but it would usually necessitate a very complicated control system.

We would like to stress the fact in this connection that the above reasoning convinced us that it would be advisable to have an insulated cathode. The assumption, therefore, that an intermittent ignition permits the use of a noninsulated cathode (due to the arc being started at the point of the ignition anode and, therefore, unable to travel to the tank walls) may not be as correct as preliminary study may have indicated. However, only time will tell if it is advisable, even when using an intermittent ignition to connect the cathode directly with the tank, or if it would not be of advantage to use an insulated cathode, considering the above observation, during abnormal operation of rectifier plants.

Careful investigations have shown that a small excitation arc, properly arranged, etc., gives the same operating condition, as far as frequency of backfires is concerned, as with an intermittent ignition rectifier, and that, therefore, this rectifier may show in time several advantages over this ignitor rectifier. This type may especially prove to be less sensitive to the condition of the cathode, as, for instance, depth of mercury, disturbance due to vibration of the mercury surface, deposit of impurities on the surface, etc. Furthermore, there is no danger of disturbing the means of carrying the excitation arc and producing the cathode spot as there is when using the intermittent ignition. In the latter case, the ignition anode is continually exposed to the cathode spot produced by the main arc and will, therefore, be subjected to extremely high thermal stresses, especially during abnormal load conditions such as short-circuits, backfires, etc. The replacement of such an ignition anode may require only a short outage time, but it has to be kept in mind that there will be at least 12 such anodes in a unit of large capacity and a replacement, no matter how quickly accomplished, requires some re-forming.

Service Records

The unit as shown in figure 4, and other single-anode rectifiers of different diameters were in operation for several hundred hours at typical full-load and overload conditions similar to those encountered in electrolytic and railway service. The load was absorbed by a 600-volt 12,000-ampere motor generator set, and in case of a backfire, this set furnished reverse currents up to 40,000 amperes. The tests under such conditions can be considered as very severe and it was found that the unit met all requirements which were striven for when designing this rectifier.

During the last few months the unit has given very satisfactory service on a 625-volt railway system, where it is subjected to regular base load and overloads, but always carrying a good base load. In order to accomplish proper parallel operation the unit is practically continuously working with delayed firing, which means severe service for the anodes during the rectification cycle, as the commutating voltage with delayed firing is higher in value than when the anodes are left to fire in a natural way.

Conclusion

Shop and field tests show that a single-anode mercury-arc rectifier having continuous excitation can be just as reliable and free from backfires as a rectifier with intermittent ignition. Furthermore, it can be concluded that with this type of excitation the arc drop is as low as practically obtainable, being nearly equal to the cathode drop alone at light load (although the anode is located somewhat farther from the cathode) and is much lower than in a multiple-anode rectifier. Present experience with both types of single-anode rectifiers has definitely established the fact that reliable service

A New Electronic Multiplier for Power Frequencies

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ASSOCIATE AIEE

THERE are many electrical applications which require alternating current at higher frequencies than those normally obtained from the power lines. These applications include: induction heating, the operation of high-speed motors, and the operation of fog horns and underwater signaling devices. Although motor generators are satisfactory for most of this work, their inherent upper frequency limitations and need for mechanical maintenance have led to the development of several alternative methods of obtaining higher frequencies.

The inverter,[1] using mercury-vapor tubes, has the advantage of high efficiency but has an upper frequency limit in the neighborhood of 1,000 cycles, due to deionization time of the tubes. Even at lower frequencies, occasional failures occur.

The vacuum-tube oscillator[2] is more reliable than the inverter but must operate at very high voltages in order to obtain good efficiency. The inverter and vacuum-tube oscillator both have the additional disadvantage of requiring large energy-storage elements in the output circuit due to the fact that direct power transfer from input to output occupies only a small portion of the output frequency cycle.

Saturable-reactor frequency changers[3] have been built to operate at very good efficiency. The most efficient forms are used to triple the input frequency. Their principal disadvantages are: poor voltage regulation at the output, poor input power factor, and heat dissipation problems brought about by the requirement of high flux densities.

This paper describes a new electronic frequency multiplier which also has a few disadvantages but, at the same time presents certain advantages which the above devices do not have. These are:

1. Although it employs gas-filled tubes, its upper frequency limit and reliability are not influenced by deionization effects.
2. It does not require large energy-storage elements at the output because power is transferred directly from input to output during half of each output cycle.
3. It provides very good regulation of output voltage.
4. It is stable under all conditions, including transient overloads or input voltage variations.

An incidental feature of the circuit is that it may be used to convert multiphase power to single-phase power with balanced loading of the multiphase lines.

Fundamental Circuit

Figure 1 shows the circuit of the simplest form of the new electronic frequency multiplier, a circuit which will convert single-phase power to single-phase power at double frequency. It is similar to a single-phase full-wave rectifier except that the terminals which would normally be the d-c output terminals are connected to the primary of an output transformer. The tubes are of a controllable type such as the Permatron, thyratron, or ignitron. The tubes which are illustrated are permatrons, which appear to be more readily adaptable to the circuit. These tubes have been described in detail elsewhere[4] and all that need be said here is that their behavior is similar to that of thyratrons except that the instant at which they start conducting is controlled by an externally applied magnetic field rather than by potential applied to an internal grid. Magnetic control is indicated in the diagram by the coil adjacent to each tube. Conduction through the Permatron starts at the instant that the magnetic field, produced by this coil, becomes smaller than a certain critical value which depends on the voltage between anode and cathode. A greater field strength, regardless of its polarity, will prevent the starting of conduction.

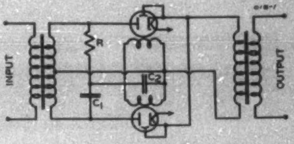

Figure 1. Fundamental circuit-electronic frequency doubler

R and C_1—Phase-shift control circuit
C_2—In resonance with control coils

A mathematical theory of the operation of the electronic frequency multiplier has been developed. This analysis takes into account the voltage drop through the tubes and the losses caused by circulation of direct current through the tubes and output transformer primary. The fundamental idea, however, is best explained by temporarily neglecting these complicating factors.

If the tubes are adjusted to start conduction or "fire" at a delay of nearly 90

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[1] For all numbered references, see list at end of paper.

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and some economy can be expected, especially at voltages below 600 volts, and in installations where, during a backfire, no, or only very little, backfeed current can enter the backfiring tank.

It was also definitely established that in spite of the small size of the tanks, which allow storing of only very little heat energy, considerable overloads can be carried if a sensitive high-speed cooling system is employed.

Furthermore, operation under fluctuating as well as continuous loads has shown that no concern need be felt that failures of excitation may cause undue overloading of the transformer secondary windings.

At present there seems to be a number of applications for which each type of rectifier—the multiple-anode and the single-anode—is best suited. Experience may, however, show in time that the single-anode rectifier may be suitable for applications where the conventional type at present seems to be more favorable. Although the conventional type may always have its place in the electric-power field, yet the development of the single-anode tank may open up new applications in the low-voltage and probably also in the very high-voltage field.

Discussion

For discussion, see page 1168.

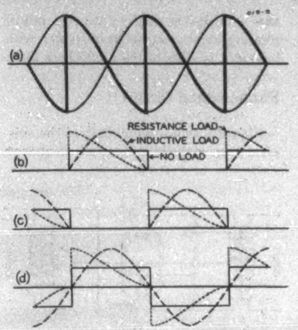

Figure 2. Voltage and current relations in the electronic frequency doubler

(*a*)—Formation of output voltage wave

(*b*) and (*c*)—Plate transformer secondary currents

(*d*)—Input currents

degrees, the voltages throughout the circuit are as shown on axis *a* of figure 2. The plate-transformer secondary voltages each side of center tap, are indicated by two sine waves 180 degrees out of phase. Each tube starts to conduct just before the peak of the positive half cycle of its associated secondary, and, due to the inductance of the output-transformer primary, continues to conduct until the other tube starts. Thus, the voltage across the primary of the output transformer is represented by the heavy line. This wave has a fundamental frequency which is double the line frequency and contains a small d-c component which is required to maintain the d-c flow through the output transformer primary. Each tube supplies alternate cycles of the output voltage.

With no load across the output, the current pulses through the tubes are approximately as shown by the solid waves on axes *b* and *c* of figure 2. These pulses are square topped if the inductance of the output transformer primary is large When a load is connected to the output, it reflects a current component into the primary of the output transformer which produces a resultant wave form as shown by the dotted line for a resistance load and by the dashed line for an inductive load. The maximum load is such that the peak amplitude of the reflected load current just balances the amplitude of the square-wave no-load current. An output load greater than this will cause each tube to stop before the next tube starts by forcing the current to zero. Axis *d* of figure 2 shows the current wave forms as reflected to the primary of the input transformer for no load and for full inductive and resistance load at the output.

Thus, the fundamental principle of operation is that energy is supplied to the load from the input power line during the positive half cycles of output voltage and that the energy in the negative half cycles is supplied from energy stored in the output transformer during the positive half cycles.

For higher-frequency output, similar circuits may be developed from multiphase rectifier systems as shown by figure 3, a circuit used for tripling the line frequency, and which, incidentally, gives a single-phase output with balanced loading of the three-phase line. More complex circuits are described below for obtaining multiphase output and higher frequency output. In general, the number of controllable discharge paths required is equal to the frequency multiplication factor times the number of output phases.

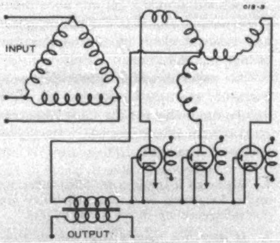

Figure 3. Fundamental circuit of frequency tripler

Output-Transformer Inductance

In a previous paper[3] the writer has described a method of computing the "critical" value of the input choke in a filter system for a rectifier using controllable gas-filled tubes. The critical value of inductance is that which is just sufficient to insure that each tube in the rectifier system will continue to conduct until the next tube starts. With an inductance of this value at the input to the filter, the current through each tube approaches zero at some instant during the period of conduction of each tube.

The output-transformer design in the frequency multiplier is a similar problem, since the primary inductance of this transformer has the same effect in controlling conduction through the tubes. However, under no-load conditions in the frequency multiplier it is required that in addition to remaining continuous, the current must remain practically constant, and never approach zero. Thus, the primary inductance of the output transformer must be much greater than the critical value.

The formula, derived for computing the critical value of inductance, was based on the assumption that the d-c output voltage of the rectifier was constant and free from ripple. In the frequency multiplier, the corresponding direct voltage is the voltage drop due to the d-c circulating current through the output transformer. Since it is required that this current be practically constant, being made up of a succession of square-topped waves from each tube, the d-c output voltage is also constant and the same assumptions as used for computing critical inductance are valid. The formula for critical inductance at large angles of firing delay is:

$$\omega L_c/R=(1+D/E_{dc})(1-\pi/n\ \cot\ \pi/n)\ \tan\ \theta$$

where:

$\omega=2\pi$ times the input line frequency
$L_c=$ value of critical inductance (henries)
$R=$ output load resistance (ohms)
$D=$ drop through tubes (volts)
$E_{dc}=$ d-c output (volts)
$\theta=$ firing delay angle (radians)
$n=$ number of tubes or rectifying phases

Further mathematical work, given in detail in the appendix, has shown that it is also possible to compute the value of inductance required to keep the current through the tubes from dropping below a given fraction, K, of the amplitude of the ideal square-topped wave. This value of inductance is given by the equation:

$$\omega L/R=(\omega L_c/R)/(1-K)$$

A more complicated relation is obtained for small angles of firing delay, but the above equations are of most interest in the frequency multiplier where the firing delay is always of the order of 90 degrees. The accuracy of the second equation becomes greatest as K approaches a value of one.

The impossibility of obtaining the type of operation described above with a firing delay of exactly 90 degrees is illustrated by the fact that this value of θ gives a value of infinity for the required inductance. In multiphase circuits where n is greater than two, the ideal firing delay angle is still 90 degrees. Firing delay in multiphase systems, however, is measured from the normal commutation point of the tubes rather than from the time of zero voltage across the associated secondary of the plate transformer.

Table I

Multiplying Factor	Output Frequency (From 60 Cycles)	Approximate Efficiency (Per Cent) K = 0.9 ωL/R = 100	Output Voltage Analysis $\left(\frac{\text{RMS Values}}{E_{\max}}\right)$			
			Total	Fundamental	Second Harmonic	Third Harmonic
2	120	90.5	0.707	0.600	0.290	0.154
3	180	94.0	0.542	0.439	0.200	0.131
4	240	95.6	0.427	0.342	0.162	0.107
6	360	97.2	0.294	0.232	0.113	0.075
10	600	98.2	0.179	0.141	0.070	0.046
20	1,200	99.3	0.095	0.071	0.035	0.024

In all cases, when each tube continues conduction until the next tube starts, the d-c output voltage is given by the equation:

$$E_{dc}=\frac{nE_{\max}}{\pi(1+D/E_{dc})}\sin\pi/n\cos\theta$$

Thus, a firing delay approximating 90 degrees is ideal in the frequency multiplier because it results in a small d-c component of voltage across the output-transformer primary. The principal design problem is to determine the closest approximation to a 90-degree delay which may be obtained with finite values of inductance of the output transformer.

Since the load resistance, R, is the d-c resistance of the output transformer primary, it is possible to determine immediately the maximum firing delay which may be obtained because the value of $\omega L/R$ is fixed by the wire size and core dimensions of the output transformer. In practice, $\omega L/R$ may be as high as 200. Note that it is the d-c resistance rather than the a-c resistance which affects the present analysis. The added resistance due to core losses has its effect mainly in reducing the available power output. To obtain maximum values of inductance with the least amount of material, the transformer design must follow the same principles as used in designing rectifier filter chokes. The presence of d-c magnetizing current necessitates an air gap in the core. General principles of this type of design have been given by C. R. Hanna and R. F. Edgar.[6,7]

Efficiency

The limitation of $\omega L/R$ of the output transformer defines the maximum firing delay and thus determines the direct-voltage component across the primary winding. The d-c circulating current is determined directly from the output current, and the voltage ratio of the output transformer, since it must balance the reflected peak amplitude of output current. The losses due to this circulating current are equal to its product with the direct-voltage component across the output-transformer primary, plus the voltage drop through the tubes. These losses are the only ones which are essentially due to the method of frequency conversion. Copper losses and core losses in the two transformers are of the same order of magnitude as is encountered in all transformers, and have not been taken into account in computing theoretical efficiency.

On this basis, the equation obtained for efficiency is:

$$\text{Per cent efficiency}=100/(1+N)$$

where N is given by the equation:

$$N=3n/2\pi\div\sqrt{1+\left(\frac{(1-K)\omega L/R}{(1+D/E_{dc})(1-\pi/n\cot\pi/n}\right)^2}$$

A detailed derivation of this equation is also given in the appendix.

Estimated values of efficiency, obtained from these equations, are given in table I for various values of n and for $\omega L/R=100$. For convenience, the resulting values of output frequency for the different values of n are given for a 60-cycle line frequency. The table indicates extremely high efficiency at the higher output frequencies. This should not, however, be misunderstood. It is doubtful that such high values would be possible in practice because, as shown later, the input power factor becomes so poor at the higher output frequencies that losses due to circulatory currents would, in an economical design, reduce efficiency to a very large extent.

Output Characteristics

One of the most favorable qualities of the electronic frequency multiplier is its regulation to load. As long as continuous conduction is maintained through the tubes, the output voltage wave must follow the wave form of the plate-transformer secondaries. Any drop in output voltage from no load to full load is entirely due to resistance and leakage inductance of the transformers. The voltage drop through the tubes remains practically constant at all loads.

The output wave form remains constant from zero to full output load and its characteristics may be easily determined. Table I includes values of rms alternating voltage across the output transformer primary, rms values of the fundamental component, and rms values of harmonic components, all as a function of the frequency-multiplying factor. The presence of harmonics may require filtering of the output for some applications, but in others it is not detrimental. For example, in driving induction motors the presence of third harmonics in the applied voltage up to 30 per cent of the fundamental increases the losses by only 2 per cent of the motor input.[8]

Input Characteristics

One point which is often overlooked in applications of gas-filled control tubes is the fact that delayed firing produces a low input power factor and distortion of the current wave form makes it difficult to correct for bad power factor. In the electronic frequency multiplier, this situation is partially overcome by the fact that conduction is maintained through each tube for as long a period as possible. However, due to the abnormal firing delay, poor input power factor is one of the principal drawbacks of the circuit. At low output frequencies, the input power factor, uncorrected, is better than at high output frequencies. On the other hand, the input-current wave form is better at the higher output frequencies. These two factors, working in opposite directions, result in the fact that, at all output frequencies, it is possible to correct power factor to better than 80 per cent. The higher output frequencies, however, require more correction. For example, at 960 cycles output, power factor correction requires seven kva per kilowatt output.

Experimental Results

The fundamental idea and theoretical performance of the electronic frequency multiplier were obtained entirely through mathematical work, previous to any experimental work. Thus the initial tests were made principally to establish finer points in the design and to determine the behavior of the circuit under unusual conditions such as transient variations in input voltage and load.

The tests were made on the single-phase frequency-doubling circuit of figure 1. Adjustment of the circuit was accomplished by setting the firing delay of the tubes at an angle less than 90 degrees and with a variable resistor in series with the output transformer primary. The firing delay was increased by steps while the variable resistor was reduced sufficiently to keep the direct current up. Eventually, the variable resistor was reduced to zero and taken out of the circuit. A small final adjustment of firing delay was made in order to set the direct current at the required value. The adjustment process was found to be simple if the direct current is kept under observation and if an oscilloscope is connected across the primary of the output transformer. The voltage wave form across this transformer is as indicated in figure 2 except when either tube fails to continue conduction until the other starts. In that case, the wave takes a short cut across the lower corner as shown in figure 4. Having reached the final adjustment at no load, an output load was applied in steps to obtain data on regulation and efficiency.

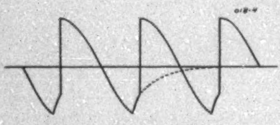
Figure 4. Effect of overload on output waveform of frequency doubler

During the course of the tests, it was found that the direct current showed a tendency to drift. This is due to the fact that an extremely small shift in firing angle, such as might be caused by shifting of tube characteristics or control-magnet characteristics by temperature changes, will cause a relatively large shift in direct current. This tendency was corrected by adding an auxiliary winding to each control magnet and allowing the direct current to flow through these coils in such a direction as to cause a shift in firing delay to counteract any shift in current. The two auxiliary windings were connected in opposition so that the alternating voltages induced by the a-c control flux were balanced out. This automatic correction is very effective regardless of load conditions and is the principal reason why the use of Permatrons is preferred. If thyratrons are used, a similar action may be obtained by adding a resistor in series with the common cathode connection and returning all the grid circuits to the end of the resistor which is connected to the output-transformer primary. This method adds appreciably to the losses. Theoretically, the regulation obtained by such circuits is best when the feedback of d-c control is greatest. However, due to the time constant of the circuit elements, instability results unless the a-c control predominates.

Previous to the test, the value of $\omega L/R$ of the output transformer was estimated as 183 from its inductance at 60 cycles and its d-c resistance. Computing backward from the results obtained, it was found that its effective value was 232. The discrepancy is due to the fact that its operating conditions with a large d-c magnetizing component and a 120-cycle voltage of unusual wave form are difficult to reproduce.

Data obtained by varying the load from zero to full load are plotted in the curves of figure 5. Efficiency values are not as high as might be obtained because the tubes were operating at a low voltage level in comparison to their rating. This is illustrated in figure 5 by the large proportion of the total losses which are due to tube loss. These curves are not affected by the addition of the d-c regulating coils, because the direct current remains practically constant throughout the entire load range.

At overloads, the output voltage, due to the alteration of wave form, illustrated in figure 4, drops rapidly because the tubes fail to conduct for a full half cycle. The dotted line in figure 4 is placed there to indicate the fact that the "kink" in output voltage under overloads follows the natural decay curve of the output-transformer inductance and the load resistance. An extended regulation curve is shown in figure 6. This curve was made with the d-c regulating coils connected so that the drop in voltage is more rapid than it would be without them. In most cases, this is an advantage because it protects the circuit from damage due to momentary overloads. The direct current rises during overloads because failure of the tubes to conduct continuously produces an increase in the d-c component of voltage across the output-transformer primary.

Several transient effects were investigated. When the power is switched off and on at the input to the plate transformer, the output voltage responds rapidly. When the switch is first closed, the output voltage rises to practically full value in less than one cycle. The first four cycles of output voltage indicate that conduction through the tubes is not quite continuous. By the fifth cycle, operation is substantially at steady-state conditions. Switching the load on and off at the output has no apparent effect if the load is at full value or less. Overloads up to a short circuit produce a transient drop in output voltage which recovers almost immediately when the overload is removed. If one tube fails to conduct, the output voltage drops to half its normal value but recovers as soon as conduction is resumed.

The final conclusion reached from ob-

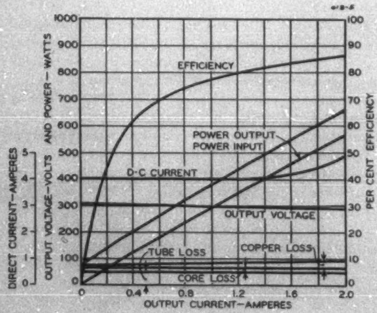

Figure 5 (left). Performance of experimental frequency doubler

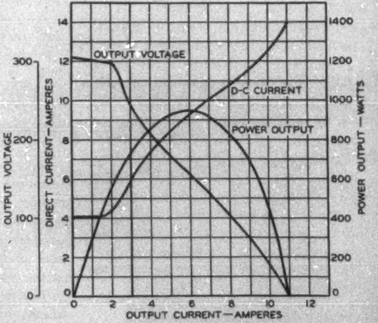

Figure 6 (right). Overload performance of experimental frequency doubler

servations of such unusual conditions was that the circuit is thoroughly stable and has none of the characteristics observed in vacuum-tube oscillators or gas-tube inverters where it is possible to destroy oscillations under transient conditions and which must always be placed in operation according to a fixed schedule. Its general behavior is as good as that of any rectifier system. Transient regulation to load is far better than may be obtained with either motor generators or filtered rectifiers because, under normal load conditions, the output is electrically connected to the input at every instant.

During the tests, it was also realized that the high efficiency at full load may be extended to lighter loads by making a slight adjustment in firing angle so that, when the load is reduced, the direct current is reduced simultaneously. In a large unit, it would be feasible to make this adjustment automatic through a regulator system actuated by the output current. The simplest method is to rectify the voltage from the secondary of a current transformer in series with the output and to apply the resulting direct current to a third winding on each control magnet. The output of this auxiliary rectifier should be shunted by a large capacitor in order that the automatic regulation may depend on the peak value of output current rather than the average value. The current wave form varies with the type of load while the maximum output of the frequency multiplier is dependent on peak output current.

Applications

The most immediate application for the frequency multiplier is for operation of high-speed motors. Practically all high-speed motors are of the three-phase induction type. Figure 7 shows a circuit which has been developed for producing a triple-frequency three-phase output. There are three groups of three tubes, each group being phased 40 degrees apart from the others in terms of the input frequency. At the output frequency, the voltages are 120 degrees apart. The plate transformer converts the three-phase input to nine phases and its secondary windings are distributed to give the optimum utilization of windings and to avoid d-c magnetization of the core.

One possible advantage of this combination of frequency multiplier and motor is in its application in industries where the product is made in a continuous strip such as is the case with rayon or paper. In both cases, the thread or paper passes through stages where its tensile strength is not fully developed and slight variations in speed may cause trouble. In operating such a factory, the use of a synchronous motor generator driving synchronous motors represents a two-stage problem in hunting, prevents the installation of separate units at different points, and often requires mechanical linkage between several machines. If electronic frequency multipliers are used, each remains in accurate synchronism with the power line. The hunting, represented by small shifts in firing delay of the tubes, is less than 0.1 degrees of the low-frequency cycle.

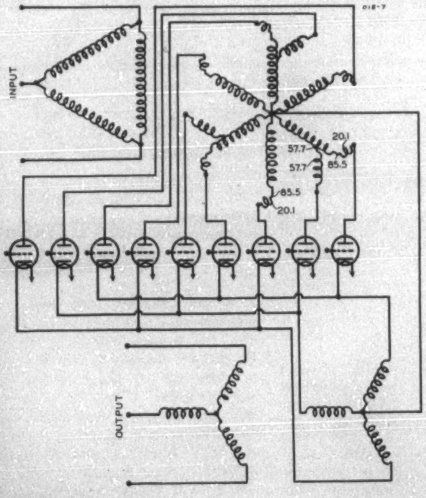

Figure 7. Electronic frequency tripler with three-phase output

One application to which the frequency multiplier appears to be especially adaptable is the operation of equipment using motors designed for 50–60 cycle operation in 25-cycle districts. There are several such cases in which it has been found better to add an auxiliary frequency converter than to redesign the equipment to accommodate an occasional 25-cycle motor installation.

A second field of application is that of induction heating. Preliminary cost estimates indicate that, at frequencies in the region of 1,000 to 2,000 cycles, it is doubtful that the electronic frequency multiplier would cost less than a motor generator, especially when the cost of power-factor correction is included. An experimental setup of 16 small tubes indicated, however, that reliable operation may be readily obtained at an output frequency of 960 cycles. At lower frequencies, an advantage in cost may be shown. Applications for low-frequency induction heating include those such as the baking of enamel on steel automobile parts.[9]

Conclusions

The experimental work, at its present stage, is sufficient to show the validity of the mathematical analysis represented by the first few sections of this paper. In adapting the circuit to applications, such as those mentioned above, some simplification should be possible. For example, the windings of a high-speed motor could be so designed and connected as to avoid the need of the output transformer of the frequency multiplier.

Appendix

Inductance Formulas

The formula for critical inductance is derived from an expression for the current in each tube of an n-phase rectifier. During the period of conduction, this current is given by:

$$i = E_{\max}/\omega L\left[\sin(\omega t - \pi/n) - \frac{n\omega t}{\pi}\cos\theta\sin\pi/n + C\right] \quad (1)$$

where:

$E_{\max}$ = peak secondary phase voltage (volts)
$\omega = 2\pi$ times the line frequency (cycles per second)
n = number of anodes or rectifying phases
t = time (seconds) and is equal to zero at the normal time of firing without delaying control
θ = firing delay angle (radians)
C = a constant of integration

In evaluating the critical inductance, the constants, C, is determined by setting i equal to zero at the instant at which the voltage across the choke passes through zero. When the inductance is greater than the critical value, this instant no longer corresponds to zero current but to minimum current. If the minimum current is assumed to be a certain fraction, K, of the average direct current, and A is the value of ωt at which the choke voltage passes through zero, then:

$$KI_{dc}=E_{\max}/\omega L\left[\sin(A-\pi/n)-\frac{nA}{\pi}\cos\theta\sin\pi/n+C\right]$$

and

$$C=\frac{nA}{\pi}\cos\theta\sin\pi/n-\sin(A-\pi/n)+\omega LKI_{dc}/E_{\max} \qquad (2)$$

L is now, by definition, limited to the value which produces a minimum current equal to KI_{dc} and is determined by integrating equation 1 over the period of conduction and setting its average value equal to the direct current. This process leads to the equation:

$$\omega L/R=\frac{(1+D/E_{dc})}{(1-K)}\times\left[\tan\theta-\theta-\pi/n+A-\frac{\pi\sin(A-\pi/n)}{n\cos\theta\sin\pi/n}\right] \qquad (3)$$

where:

D = drop through tubes (volts)
E_{dc} = d-c output voltage

This form of the equation requires the substitutions:

$$E_{dc}+D=\frac{nE_{\max}}{\pi}\cos\theta\sin\pi/n \qquad (4)$$

and

$$R=E_{dc}/I_{dc}$$

For large firing delay angles, such as required in the frequency multiplier, A equals θ so that:

$$\omega L/R=\frac{(1+D/E_{dc})}{(1-K)}\tan\theta(1-\pi/n\cot\pi/n) \qquad (5)$$

Efficiency Formulas

Since the limiting factor in equation 5 above is the value of $\omega L/R$, the firing angle is given by:

$$\tan\theta=\frac{(1-K)\omega L/R}{(1+D/E_{dc})(1-\pi/n\cot\pi/n)} \qquad (6)$$

At this firing angle, we have, from equation 4:

$$E_{\max}=\frac{\pi E_{dc}(1+D/E_{dc})}{n\cos\theta\sin\pi/n} \qquad (7)$$

The peak output voltage of the frequency changer, referred to the primary winding, is $E_{\max}\sin\pi/n$. Assuming the output voltage to be a saw-tooth wave, the power output into a pure resistance load is:

$$\left.\begin{aligned}&W_o=\frac{2}{3}E_{\max}I_{dc}\sin\pi/n\\&\text{or}\\&\frac{2\pi E_{dc}I_{dc}(1+D/E_{dc})}{3n\cos\theta}\end{aligned}\right\} \qquad (8)$$

The saw-tooth-wave assumption is favorable to a frequency doubler but becomes increasingly accurate as the output frequency increases. Substituting from equation 6 for $\cos\theta$:

$$W_o=\frac{2\pi E_{dc}I_{dc}}{3n}\times\sqrt{(1+D/E_{dc})^2+\left[\frac{(1-K)\omega L/R}{1-\pi/n\cot\pi/n}\right]^2} \qquad (9)$$

The d-c losses are given by:

$$\left.\begin{aligned}&W_1=(E_{dc}+D)I_{dc}\\&\text{or}\\&E_{dc}I_{dc}(1+D/E_{dc})\end{aligned}\right\} \qquad (10)$$

Thus, the fractional losses in terms of power output are given by:

$$N=\frac{W_1}{W_o}=\frac{3n}{2\pi}\div\sqrt{1+\left[\frac{(1-K)\omega L/R}{(1+D/E_{dc})(1-\pi/n\cot\pi/n)}\right]^2} \qquad (11)$$

And the efficiency is given by:

$$\text{Per cent efficiency}=1/(1+N) \qquad (12)$$

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Dynamoelectric Amplifier for Power Control

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THE USE of amplifiers has become common knowledge in radio, but on the other hand, the term amplification has seldom been applied to processes in power engineering. Strictly speaking, we may say that a radio amplifier is only a form of control because we always tap a new source of power and the function of the amplifier is to control this power so as to reproduce the changes of energy flow at a higher power level. On this ground we might say that an ordinary d-c generator is an amplifier because we control the power output by the current in the field winding. Such a terminology would, however, be rather misleading because when we say amplification, we imply something more specific than when we say control. An amplifier should give accurate reproduction both of intensity and time intervals, whereas, controlled power circuits used so far have not met these requirements. In the radio-frequency amplifier we are dealing with a time element of less than a millionth of a second. In an audio amplifier the time element is about $^1/_{1,000}$ of a second. The only device we know of that will respond in such a short time is the high-vacuum electron tube. In power circuits, on the other hand, the time element is seldom less than one-tenth of a second.

When it became apparent that amplifiers were needed in power engineering we naturally turned to the vacuum tube. It was found, however, that the high-vacuum tube used in radio did not lend itself so well to high power circuits as the mercury-vapor and gas-filled tube with grid control known as the thyratron. The thyratron amplifier has found many successful uses in power applications and it was in fact this demand that led us to consider what other means may be available to solve similar problems. In the thyratron amplifier we have a device with an extremely high ratio of amplification so that for most practical purposes only a single stage is needed.

A thyratron amplifier may be controlled from a small regulator and may be used to furnish excitation of large synchronous machines. Installations of this type have proved very successful in service and are found superior to the conventional exciter plant in giving quick response to sudden changes of load. From an engineering standpoint, this type of installation is highly successful. Unfortunately, in the present stage of its development, the costs are such that it is not justified economically.

The type of dynamoelectric amplifier which will be described was developed to meet new control functions in industry where a high rate of amplification must be combined with a quick and accurate response. As an approach to this problem we were again guided by the history of radio amplification. It was found in the design of radio amplifiers that the ratio of amplification or the sensitivity can be increased within certain limits by sacrifice of the quickness of response. Thus an improvement of ten to one in sensitivity might be gained by permitting the process to take ten times as long. The same general principle applies to the dynamoelectric amplifier. In radio we have found, however, that by using amplification in several stages we can gain in sensitivity in geometric progression without sacrifice of speed of response. The dynamoelectric amplifier which for brevity has been called "amplidyne" generator, has been developed on this principle. It is a two-stage amplifier incorporated in one dynamoelectric machine. In its physical structure it resembles the Winter-Eichberg motor, the Rosenberg generator, and the Pestarini metadyne, characterized by a pair of short-circuited brushes at right angles to the power brushes. In its functions, it is quite different. The first stage of amplification is from the control field to the short-circuited brushes and the second stage from the short-circuited brushes to the power brushes.

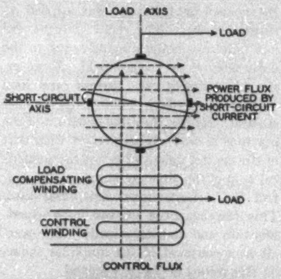
LOAD AXIS
LOAD
SHORT-CIRCUIT AXIS
POWER FLUX PRODUCED BY SHORT-CIRCUIT CURRENT
LOAD COMPENSATING WINDING
LOAD
CONTROL WINDING
CONTROL FLUX

Figure 1. Functional diagram of the dynamoelectric amplifier (amplidyne generator)

Figure 1 shows a functional diagram of the dynamoelectric amplifier (amplidyne generator). The principal elements are:

A control field

A short-circuit axis on the armature

An output circuit from the armature including a compensating winding

A high ratio of amplification of the order of 10,000 to 1 can be realized by this system. The over-all amplification is the product of an amplification of about 100 to 1 between the input winding and the short-circuit axis and another of 100 to 1 between the short-circuit axis and the output axis. The process of amplification in two stages can be further explained by assuming that the magnetic flux which generates the voltage in the short-circuit axis may be made only one-tenth as strong as flux in the power axis. Therefore the volt-amperes required to produce the control flux need be only $^1/_{100}$ as great as if the power field were excited directly from the control circuit. The control circuit may therefore contain enough resistance in proportion to its reactance to realize the desired short time constant.

The diagram also shows that the output circuit is on the same magnetic axis as the control circuit. There may be thus a direct magnetic back coupling from the output circuit to the control circuit with the tendency to regeneration and oscillations with which we are familiar in radio amplifiers. The object of the compensating winding is to neutralize this back coupling. That this is a rather delicate adjustment may be realized when it is remembered that the volt-amperes in the output circuit may be more than 10,000 times as great as the volt-amperes in the control circuit.

We also find that a compensation which is correct for direct current in the steady state may not be right for transients or alternating current. In the design of an amplifier system we must therefore take into account not only the characteristics of the machine itself but also the circuits

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Figure 2. Correctly compensated amplidyne generator shows no back coupling when the load circuit is closed

A—Control-field volts, 60-cycle timing wave
B—Load volts
C—Load current

with which it is associated and we must analyze the performance of the system from the point of view of a-c and transient stability as well as d-c stability.

The reaction of the output circuit upon the input circuit with alternating current is under certain circumstances opposite to the reaction from direct current. This statement may seem surprising and need some further explanation. Let us assume that the machine is so designed that the back-coupling action for direct current is negative which produces stable operation for the steady state. Let us now assume that we use this same machine for amplification of alternating current and that we deliver the power to an inductive circuit like the field of a synchronous machine. Under those circumstances the back-coupling effect for alternating current may be positive, and cause regenerative oscillations at a critical frequency. The reason for this is that the alternating current in the output circuit is displaced more than 90 degrees from the current in the control circuit, thus reversing the back coupling from negative to positive. This positive reaction is regenerative and may result in continuous oscillations, as shown on oscillogram figure 4.

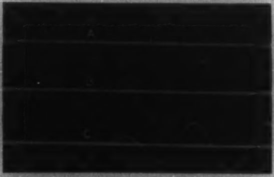

Figure 3. Effect of back coupling in undercompensated amplidyne generator when closing load circuit

A—Control-field volts, 60-cycle timing wave
B—Load volts
C—Load current

Figure 2 shows a correctly compensated machine feeding an inductive load. No back coupling between the load circuit and the control circuit is noticed when the load circuit is closed. The operation is perfectly stable and there is no tendency to oscillate.

In contrast to this, figure 3 shows the behavior of the same machine when undercompensated. The action of the load current upon the control flux is shown by a substantial increase of the output voltage due to regeneration. This is in contrast to a reduction in output voltage when a steady d-c load is applied. When the control field is open-circuited the machine passes from forced oscillations at four cycles per second to free oscillations at seven cycles, as shown in figure 4.

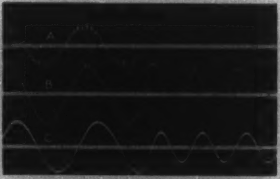

Figure 4. Free oscillations in undercompensated amplidyne generator when opening control circuit

A—Control field volts, 60-cycle timing wave
B—Load volts
C—Load current

Such tests with alternating current are convenient for analyzing and defining the amplifier characteristics. In most cases of practical application we are however dealing with rapid control transients in a generally unidirectional flow of current. Figure 5 shows an oscillogram taken of a typical machine. In this test the control field voltage was applied instantaneously. The field current did not build up instantaneously due to the inductance of the control field. However, the short-circuit-axis current and load voltage are nearly in phase with the field current. Hence, the time constant of the machine is only slightly larger than that of the control field and is not the numerical sum of the time constants of the control field, short-circuit axis, and load. This fact has been proved both experimentally and mathematically. The overall time constant of the amplifier shown on this record is $^1/_{30}$ of a second.

The tendency toward regenerative oscillations is present when the dynamoelectric amplifier feeds a highly inductive load such as the field winding of a large machine. If the desired combination of d-c stability and a-c stability has not been realized in the design of the machine it is possible to overcome the difficulty by adding an external back coupling which functions only at transient and alternating current. In most cases, however, this expedient is not needed. No case has however been found where it was not possible to adjust the system for stable operation in one way or another.

A theory of oscillations and the methods for avoiding them to be complete should take into account the effect of capacity as well as inductance in the output circuit. Electrostatic capacity in low-voltage circuits is so small that it may be neglected, but effects equivalent to capacity are apt to be present to an extent that becomes important in the functioning of the system. The mechanical inertia of a d-c armature reacts upon the circuit like an electrostatic capacity with the only difference that it is usually so much greater that it may be measured in farads instead of microfarads. Electrostatic capacity in conjunction with electromagnetic inductance produces tuning effects at a natural period in accordance with the well-known formula

$$\text{frequency} = \frac{1}{2\pi\sqrt{LC}}$$

When we use a dynamolectric amplifier to deliver power to a d-c motor we have a practical case of that sort. The inductance of the armature winding of the generator plus the inductance of the motor winding create a natural period of resonance in conjunction with the electromechanical capacity which is determined by the inertia attached to the motor shaft. In practical systems this natural period is of a magnitude of a few

Figure 5. Response of an amplidyne generator to control transients

A—Control-field current, 60-cycle timing wave
B—Short-circuit axis current
C—Load volts

The Design Characteristics of Amplidyne Generators

ALEC FISHER
ASSOCIATE AIEE

COMPANION papers present some of the inherent possibilities and the useful applications for dynamoelectric amplifiers.[1,2] They have also clearly pointed out that the inherent value of such an amplifier is determined by three characteristics: (1) ratio of response or sensitivity; (2) speed of response; and (3) stability.

This paper describes the essential design features of two-stage dynamoelectric amplifiers, known as "amplidyne" generators, which enable satisfactory values for these three characteristics to be obtained. Typical performance curves and oscillograms are shown, indicating the type of performance that has been found satisfactory in service.

Preliminary Analysis

The amplidyne generator is fundamentally an armature-excited machine, in that the armature is essentially the source of main excitation, and it is also the source of main power output. The first stage of amplification is from the control field to the short-circuit axis and the second stage from the short-circuit axis to the power-output axis. The total amplification is equal to the product of first and second stage amplification.

Figures 1*a* and 1*b* show a two-pole d-c armature with two pairs of brushes in quadrature. In order to simplify the figure, the frame is not shown and the assumption is made that the brushes have ideal commutation characteristics. Location of windings is shown for the pure amplidyne generator with (figure 1*b*) and without (figure 1*a*) a quadrature shunt and series winding. Figure 1*b* also illustrates the position of a few of the auxiliary windings used in amplidyne generators.

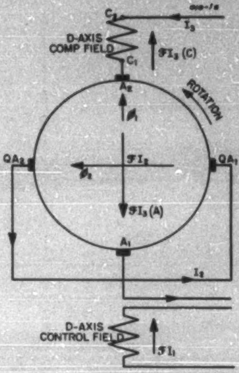

Figure 1a. Location and phase relation of windings of a pure amplidyne generator (two poles)

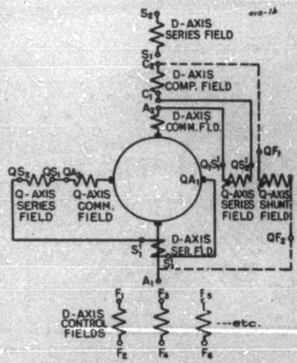

Figure 1b. Location and nomenclature of additional windings for a two-pole amplidyne generator

A flow of current I_1, produced by voltage E_1 in the direct-axis field F_1F_2 creates a direct-axis flux ϕ_1 in the first stage of amplification. The rotation of the armature develops a speed voltage E_2 and current I_2 in the short-circuit axis, QA_1, QA_2. As a result, the flow of armature current I_2 develops a distributed magnetomotive force $\mathcal{F}I_2$ and flux ϕ_2 in the quadrature axis. Rotation of the armature in this field then develops a speed voltage E_3 and load current I_3 in the output circuit.

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[1] For all numbered references, see list at end of paper.

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cycles per second. This is likely to be a frequency which is representative of the control function for which the system is to be used, and it has been found that in such cases the electromechanical resonance is apt to be a very real problem, which must be foreseen in the design. In such cases it is found to be desirable to make the inductance of both generator and motor as low as possible by distributed compensating windings at the same time as the mechanical inertia is kept as low as possible. Both these factors tend to place the electromechanical resonance frequency above the frequency of the control functions, thus avoiding any tendency to mechanical oscillations.

When a control system is desired which demands a still higher ratio of amplification than is attainable in a single dynamoelectric amplifier it is possible to introduce a first stage of electron tubes of the type used in radio sets. Such tubes are adapted to an output sufficient to control an amplidyne whereas the primary control energy of such tubes is measured in microwatts. We have thus established simple means for industrial purposes to amplify a minute energy such as a beam of light into a quick and accurate control of power flow of many horsepowers. It is hoped that this will open up many new possibilities in the control of industrial processes.

Other aspects of the dynamoelectric amplifier are discussed in a paper by A. Fisher, dealing with machine design (pages 939-44) and a paper by D. R. Shoults, M. A. Edwards, and F. E. Crever, dealing with practical applications (pages 944-9).

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Discussion

For discussion, see page 1136.

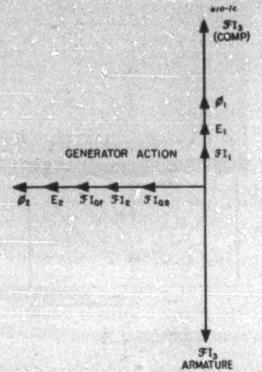

Figure 1c. Space vector relation of magnetomotive forces for a two-pole amplidyne generator

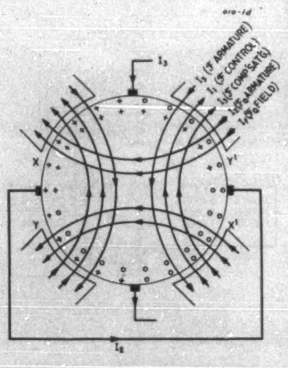

Figure 1d. Magnetomotive-force paths and armature-current distribution—two-pole amplidyne generator

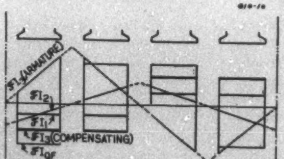

Figure 1e. Air-gap magnetomotive force distribution—two-pole amplidyne generator (not to scale)

The compensating-winding (C_1C_2) direct-axis magnetomotive force $\mathfrak{F}I_3(C)$ is in direct opposition to the effective armature magnetomotive force $\mathfrak{F}I_3(A)$ both in value and in direction. The complete neutralization of armature magnetomotive forces in the direct axis allows independent action of control magnetomotive force created by current I_1 in the first stage of amplification. The value of control flux ϕ_1 therefore need be only sufficient to overcome the electrical resistance drop in the short-circuit armature axis and as will be shown later, the reactance voltage drop during transients. The magnetomotive force $\mathfrak{F}I_1$ is equal to the ampere turns required to force the small flux ϕ_1 across the air gap and through the magnetic structure of the machine and is therefore relatively quite low.

In the second stage of amplification the magnetomotive force I_2 necessary to establish secondary flux ϕ_2 is equal to the ampere turns required to force ϕ_2 across the air gap and quadrature magnetic structure. The action in the second stage is similar to a salient-pole d-c dynamo with these exceptions: (1) the armature reaction is completely compensated preventing distortion; (2) air gaps are relatively smaller; (3) large magnetic sections prevent saturation; and (4) the armature winding, as a source of excitation, has low losses. As a result, power amplification in the second stage is higher than in a conventional machine. The addition of a self-excited quadrature winding QF_1, QF_2 (figure 1*b*) further reduces the quadrature current I_2 and increases the amplification considerably.

Magnetomotive forces are shown in space vector relation for both axes in figure 1*c* and the distribution for both the pole structure and air gap is shown in figures 1*d* and 1*e*. Current distribution for the armature circuit under load is shown in figure 1*d* where the outside conductors represent load current I_3 and the inside conductors quadrature current I_2. An unequal distribution of current in the armature is a result of current additions in XX^1 and subtractions in YY^1. This condition of unequal current distribution is improved by using quadrature shunt or series windings; however for most amplidyne generators, the essential action is not seriously affected by unequal current distribution.

There are no mutual linkages between the quadrature-axis windings and control windings in the direct axis. This essential feature plus others described previously, is responsible for the high speed of response. A change in control voltage E_1 produces a corresponding voltage change E_3 in the output circuit. A preliminary analysis has thus far shown an amplifier (1) with high amplification, (2) with a low mutual coupling, and (3) with a faithful pattern reproduction.

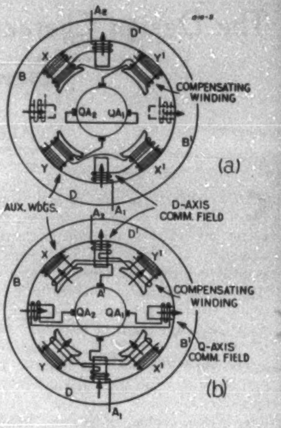

Figure 2. Schematic diagrams of two-pole amplidyne generators with block or concentrated compensation

The compensating windings are wound about each segmental pole in *a* and about each pair of poles in *b*

Practical Amplidynes

Figures 2*a* and 2*b* are schematic diagrams of practical amplidyne generators using block or concentrated compensation and excitation. The field structure is a four-segmental pole design with interpolar spaces sufficient for good commutation and low leakage. Paths are drawn for the direct, quadrature, and commutating pole fluxes. Field-frame section BB^1 carrying direct flux ϕ_1 may have less section than DD^1 which carries the higher quadrature main flux ϕ_2. In order to improve transient performance, reduce residual magnetism, and insure greater uniformity in design, a laminated frame is used.

In figure 2 the compensating winding is concentrated around each segmental pole. The ampere turns on the commutating pole are equal to the armature reaction plus sufficient overcompensation to reverse the armature coils undergoing commutation.

In figure 2*a* the compensating winding C_1C_2 is wound about a pair of segmental poles. The ampere turns on the compensating winding now add directly to the commutating-pole ampere turns, thus reducing the number of turns required on the commutating-pole body. Commutating poles, when used in the quadrature axis are connected as shown in figure 2*b*. The actual design connection used depends on the size of machine, available room for commutating poles, and required output characteristics.

The distribution of magnetomotive forces is shown in figure 1*c* for the essential direct and quadrature fields. In practice, the resultant distribution depends on the relative value of each component and is further influenced by commutation, commutating pole, and leakage magnetomotive forces. A plot of magnetomotive force around the armature circumference determines the value, location, and effectiveness of both first- and second-stage windings.

Steady-State Characteristics

In figure 3 the saturation curve of the direct and quadrature axis is shown. When the small direct-axis flux ϕ_1 is applied to the quadrature axis saturation curve at its operating point ϕ_2 and $\mathfrak{F}_2$ its magnetomotive force aids in one pole and subtracts unequally in the other as shown by X and Y, requiring more ampere turns than indicated by $\mathfrak{F}_1$. The value of $\mathfrak{F}_1$ is actually greater than the calculated value because of (1) local distortion in the pole face and armature slots, (2) reactive ampere turns of coils undergoing commutation, (3) imperfect compensation, and (4) increased effective armature resistance due to changes in brush contact drop. These secondary effects, however, are relatively small in the normal range of operation.

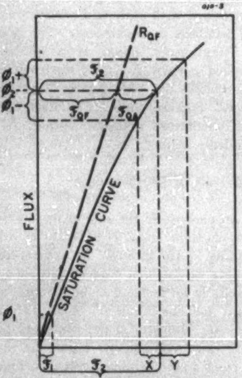

Figure 3. Saturation curves of first and second stages

The second-stage saturation curve is practically identical with first stage. The quadrature field resistance line is R_{QF} and its magnetomotive force is $\mathfrak{F}_{QF}$

Since ϕ_1 is so much smaller than ϕ_2 there is practically no distortion introduced by ϕ_1. An interesting and important feature of design is that all of the armature leakage flux connected with ϕ_2 is useful in producing output voltage E_3. This leakage flux, being air borne, relieves the iron magnetic circuits, effectively reduces the size of the machine, increases amplification, and, having zero coupling with other windings, aids in speeding up the response.

Figure 3 also illustrates the advantage of using a shunt quadrature field QF_1, QF_2. The strength of this quadrature field is adjusted so that its resistance line R_{QF} does not intersect the quadrature-axis saturation curve. The amplification is increased approximately, as follows: (where A_F is the new amplification and A_A the original amplification):

$$A_F=\frac{(\mathfrak{F}_2)^2}{(\mathfrak{F}_{QA})^2}\times A_A$$

Quadrature armature current is reduced in the ratio of $\mathfrak{F}_2$ to $\mathfrak{F}_{QA}$ relieving the armature circuit and resulting in a more uniform balance of armature currents. In order to reduce the effect of mutual coupling between the quadrature-armature and shunt-field windings, a high inductance, entirely separate from the machine, is normally connected in series with the shunt winding, effectively reducing interference during transients.

Commutation

Good commutation is essential for high amplification and good stability. Well-defined neutrals and linear commutation reduce the magnetizing and demagnetizing magnetomotive forces, due to over or undercompensated armature coils, undergoing commutation. It is sometimes desirable to use additional commutating poles in the quadrature axis QA_1, QA_2, even at the expense of increased resistance and inductance, on account of the more perfectly defined neutrals and decreased cross magnetization obtainable.

Amplidyne generators have been built with and without commutating poles depending on commutation factors and sensitivity requirements of design. Interpolar commutating zones are designed as in conventional machines; secondary current I_2 and output currents I_3 are commutated independently by either brush resistance drop or with the aid of commutating-pole flux. As a coil in the direct-axis undergoes commutation, and changes its direction of I_3 current flow, quadrature current I_2 flows through the same coil, without a change of direction. As a result there is a slight distortion of flux across the commutating-pole face but otherwise commutation is not affected. When commutating poles are not used, a slight distortion of current distribution is found across the brush face. Similar effects appear in the quadrature axis as I_2 undergoes commutation. Experience shows that each current can be commutated independently.

Output Characteristics

The general characteristics of a four-kw amplidyne generator are shown in figure 4. Although the transient response with a quadrature shunt field is slower than without such a field, its speed approaches the pure amplidyne generator when external reactance is added to the quadrature field. It has the decided advantage

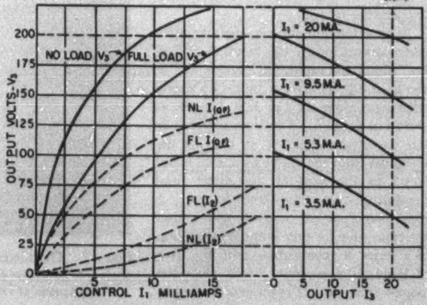

Figure 4. Characteristics of a four-kw amplidyne generator with a quadrature shunt field QF. (Actual field and short-circuit current values are omitted)

of low excitation and high amplification and is used extensively. At full rating the amplification for a given field setting is 18,000 and at three-fourths voltage and current 100,000, increasing to higher values at lower voltages.

Transient Performance

Under conditions of transient performance the current build-up in the first stage with a sudden applied voltage E_1 is:

$$i_1=\frac{E_1}{R_1}\left(1-\epsilon^{\frac{-R_1t_1}{L_1}}\right)$$

neglecting eddy current losses and saturation i_1 and ϕ_1 are proportional to each other and are in time phase. Inductance L_1 varies with the degree of saturation and when appreciable saturation is obtained, a step-by-step method of solution can be used.

The expression for current build-up in the second stage with a sudden application of load is:

$$i_2=\frac{E_2}{R_2}\left(1-\epsilon^{\frac{-R_2t_2}{L_2}}\right)$$

Voltage e_2 in the second stage begins its build-up at the same instant as i_1, in the first stage and the equation for voltage and current in this stage is:

$$e_2=ki_1=R_2i_2+L_2\frac{di_2}{dt_2}$$

A solution of equations of this type is:

$$i_2=\frac{E_1K}{R_1R_2}\left[1-\frac{\epsilon^{\frac{-R_1t_1}{L_1}}}{\left(1-\frac{L_2R_1}{L_1R_2}\right)}+\frac{\epsilon^{\frac{-R_2t_2}{L_2}}}{\left(\frac{L_1R_2}{L_2R_1}-1\right)}\right]$$

or in more general terms:

$$\text{load volts}=\left(E_3-B\epsilon^{\frac{-R_1t_1}{L_1}}+C\epsilon^{\frac{-R_2t_2}{L_2}}\right)$$

where B and C are constants depending on L and R.

Figure 5 is a graphical picture of these exponential terms.

In an actual amplidyne generator, with eddy-current losses, the fluxes ϕ_1 and ϕ_2 lag behind the excitation current and so the load voltage always builds up slowly with zero slope at the first instant as shown in figure 5. The time constant in the two-stage amplifier, as may be determined from the above formulas, is not equal to the sum of the direct- and quadrature-axes time components. A rigid mathematical analysis shows that the expression for time is equal to the sum of a series of exponentials with the time of build-up somewhat longer than the longest time lag. It is therefore essential in design to minimize the component having the longest time lag.

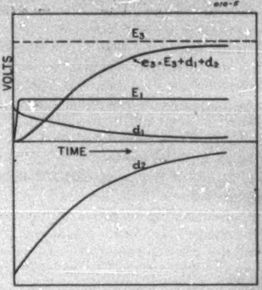

Figure 5. Transient response with sudden application of voltage E_1, on first stage

E_3 is output voltage

Test Performance

The oscillograms shown in figures 6 and 7 show the transient response of a four-kw amplidyne generator when control current or voltage is suddenly applied. Under the test conditions no quadrature shunt field and no overexcitation were used and amplification was approximately 6,000.

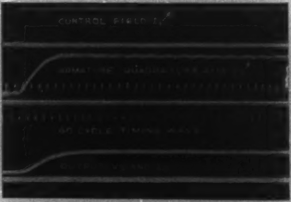

Figure 6. Oscillogram of transient response of a four-kw amplidyne generator with a sudden application of current I_1 in first stage

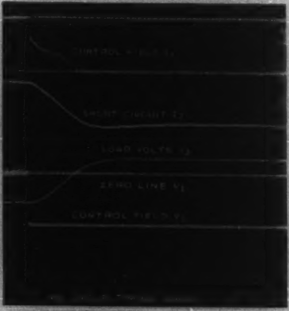

Figure 7. Oscillogram of transient response of a four-kw amplidyne generator with a sudden application of voltage in first stage

Figure 8. Oscillogram in transformer response with a sudden application of current in first stage of a four-kw amplidyne generator

Showing effect of eddy current and iron losses on a blocked armature with brushes raised

Figure 8 is an oscillogram of the same amplidyne with a blocked armature and all windings open-circuited. The delay in transformer action of a suddenly applied current I_1 shows the time lag introduced by eddy-current and iron losses. Similar results are obtained when the quadrature armature current I_2 is suddenly applied.

Analysis of Performance

An analysis of the rate of response or amplification alone, is likely to be misleading. An increase in control-field copper weight or a reduction in air gap results in an increase of both time constant and amplification. A longer time constant indicates slower response, but with equal volt-amperes input, the time to reach a predetermined voltage is much less for a given amplidyne generator with

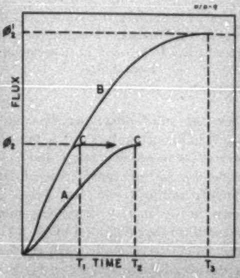

Figure 9. Transient performance showing effect of time constant and amplification

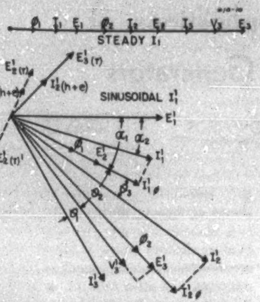

Figure 10. Vector diagram of a pure amplidyne generator with constant and sinusoidal excitation

Speed voltages are in phase with fluxes $I_1^1\phi$, $I_2^1\phi$, $I_1(h+e)$ $I_2(h+e)$ are vector components of I_1^1 and I_2^1

the higher time constant and amplification. The time T_1 to reach ϕ_2 in figure 9 is less for characteristic *B* than T_2 is for *A*, although the time T_3 to reach the ultimate flux level is longer on characteristic *B* than for *A*.

The amplidyne generator lends itself to field forcing on account of low control watts. Field forcing on curve *B* with the control circuit cutting off at point C^1 is good practice. In judging oscillograms as shown in this paper and in comparing operation with other amplifiers, comparison must be made on the basis of volt-amperes input. A long time for build-up usually indicates a high amplification.

Amplification

Amplification has been defined as the ratio of volt-amperes output to volt-amperes input. With steady d-c control current I_1 the vector diagram is a straight line as shown in figure 10, and

$$A=\frac{V_3I_3}{E_2I_2}\times\frac{E_2I_2}{E_1I_1}=\frac{V_3I_3}{E_1I_1}$$

for an amplidyne generator without a quadrature shunt field. Amplification characteristics of a four-kw machine less quadrature shunt field are shown in figure 11. Amplifications from 2,000 to 10,000 and higher at full load are practical depending on the rating obtained from a given frame size. First-stage amplification is much higher than second stage and for example, at full load and normal saturation, second-stage amplification is about 40 for a four-kw machine.

Performance characteristics of an amplidyne generator are judged by the ability to follow sinusoidal voltage of low frequency applied to the control field. There is a reduction in amplification caused by the power-factor angle of the load and armature quadrature axis and a further reduction caused by the angular lag between magnetomotive force and flux, a result of eddy current, hysteresis, and secondary losses of commutation. The new low-frequency amplification factor is:

$$A'=\frac{V_3'I_3'}{E_1'I_1'}$$

The vector diagram is shown in figure 10 for sinusoidal field excitation and the angular lags in both stages are shown for both inductance lag and flux losses. Angular displacements have been exaggerated for clarity. Total amplification is:

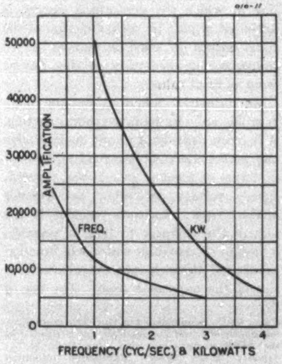

Figure 11. Relation of amplification with load and first-stage frequency on a four-kw amplidyne generator

Frequency curve plotted for partial load initial setting of $A=30{,}000$

$$A_{31}=\frac{V_3'I_3'}{E_1'I_1'}=\frac{V_3I_3\cos\theta_1\cos^2\theta_2\cos^2\alpha_1}{E_1I_1\cos\alpha_2}$$

A change in ϕ_1 in the first stage induces transformer voltage E_T' across A_1A_2 which is completely cancelled by voltage E_T' produced by compensating winding C_1C_2 with 100 per cent coupling. Speed voltage E_2' in phase with ϕ_1 across brushes QA_1, QA_2 generates short-circuit current I_2', which magnetomotive force creates quadrature flux ϕ_2. Output speed voltage E_3' in phase with ϕ_2 is generated across main brushes A_1A_2. The transformer effect produced by ϕ_2 appears as additional eddy current and hysteresis losses $I_2'(h+e)$ but no transformer voltage is induced across the output circuit.

A lag between excitation and flux in both stages is shown on the vector diagram, as a result of hysteresis and eddy-current losses. The sine-wave resultant of $I(h+e)$ is used on the diagram and illustrates the reduction in amplification as shown by an increase in angular lag. The curve in figure 11 indicates how amplification changes with frequency for this particular four-kw amplidyne generator. The change with frequency depends on the change in ratio of resistance to inductance and on the change of hysteresis and eddy-current losses with frequency. Inductance is the controlling factor since its value is usually greater than resistance.

General Discussion

The diagrams and formulas indicate the design features necessary for the construction of amplidyne generators for good transient performance. Amplification is increased by reducing the angular lag between the various vectors. The use of low-reactance armatures and more complete compensation of armature reaction effectively reduces the angular lag. Effective compensation also prevents regenerative back coupling of transformer voltages and over- and under-compensation during steady-state conditions. Amplification is further increased by reducing the transient losses produced by commutation currents, and variable brush-contact drops.

Normal requirements of design such as low iron losses, low magnetic distortions, and low magnetic densities favor the use of small air gaps, and therefore high amplifications. The output of an amplidyne generator is somewhat less than a conventional generator of the same frame size on account of the lower den-

Figure 12. Oscillogram of angular lag between stages of a four-kw amplidyne generator with approximately two-cycle excitation

sities used. However the efficiency is usually higher than a conventional machine, since the losses are less.

Many additional windings are used to obtain different characteristics. A series winding QS_1, QS_2, in the short-circuit axis creates a more uniform distribution of magnetomotive force across the pole faces, results in lower short-circuit current I_2, and less current distortion in the armature but has the disadvantage of increased impedance and ineffective use of leakage flux. Increased amplification is also obtained by using this field, reducing the total losses in the second stage. A series winding $S_1{}^1S_2{}^1$ is sometimes used to compensate for changes in brush-contact resistance and results in higher amplification.

Greatly increased amplification is obtained by overcompensating winding C_1C_2 slightly. The result is in general similar to the addition of a series winding in a conventional d-c generator and the amplidyne generator will tend to self-excite. This higher steady-state amplification is obtained at the expense of transient or low-frequency amplification, in which the back-coupling effect is increased. There are conditions where over- or undercompensation is desirable but the use of such windings for obtaining particular characteristics depends on the application. In general the performance is judged by response to transient or low-frequency excitations, and the standards for amplification should be based at a representative frequency.

Conclusions

The design features and performance curves that have been discussed indicate the remarkable range and fidelity of amplification that can be secured in the practical design of this new type of d-c machine designated as the amplidyne generator. The transient performance, which is the real test of an amplidyne generator, indicates that the problems met with in designs giving high amplification, speed of response, and stability have been solved successfully. In view of these results, it seems probable that this type of machine will have many useful applications in the automatic control of industrial operations.

Terms used:

I_1, I_2, I_3—Currents, first, second, and output stages

i_1, i_2, i_3—Instantaneous currents, first, second, and output stages

E_1, E_2, E_3—Voltages, first, second, and output stages

e_1, e_2, e_3—Instantaneous voltages, first, second, and output stages

E_1', E_2', E_3'—Rms voltages, first, second, and output stages

I_1', I_2', I_3'—Rms currents, first, second, and output stages

$\mathfrak{F}I_1$, $\mathfrak{F}I_2$, $\mathfrak{F}I_3$—Magnetomotive forces, first, second, and output stages

R_1, R_2, R_3—Resistances, first, second, and output stages

L_1, L_2, L_3—Inductances, first, second, and output stages

L_1/R_1; L_2/R_2—Time constants first and second stages

A_1, A_2—Amplification, first and second stages

ϕ_1, ϕ_2 —Flux, first and second stages

$E_{2(T)}$, $E_{3(T)}$—Transformer voltage, first and second stages

$I_1(h+e)$, $I_3(h+e)$—Sine-wave current component $(h+e)$ losses

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1. Amplifiers for Power Control, E. F. W. Alexanderson, M. A. Edwards, and K. K. Bowman. AIEE Transactions, volume 59, 1940, pages 937–9.

2. Industrial Applications of Amplidyne Generators, D. R. Shoults, M. A. Edwards and F. E. Crever. AIEE Transactions, volume 59, 1940, pages 944–9.

Discussion

For discussion, see page 1135.

Industrial Applications of Amplidyne Generators

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A CONSIDERABLE number of "amplidyne"* generators[1,2] have been applied in conjunction with other electrical apparatus to industrial processes within the past few months. A summary of these applications indicates that the amplidyne generators are of particular value in conjunction with so-called "closed-cycle" control systems. A closed-cycle system may be defined as one in which the controlling agency is actuated in part by some function of the final output in such a manner as to minimize any deviation of this output from an ideal value.

As industrial functions become more complex and requirements more exacting, it is often desirable to limit or determine the performance of electrical equipment in terms of torque, horsepower, current, speed, voltage, or other performance characteristic. In such cases it is the aim of the engineer to find, if possible, a system of machines which will function as desired through inherent characteristics of the apparatus used. The use of the three-field d-c generator in electric-shovel equipment illustrates the utilization of basic machine characteristics to produce definite forces or torques which are more or less independent of the load reactions. In other difficult applications it is often more feasible to apply closed-cycle controllers, making use of more standard power apparatus and so modifying the power flow to the apparatus as to produce the required results.

Closed-Cycle Controllers

In designing a closed-cycle controller of this sort, there are four factors which must be considered:

First, there must be some standard of performance established and a mechanical or electrical counterpart determined. This may be, for example, the torque or force of a spring, a voltage, a current, or a combination of these.

Second, the functioning of the complete equipment must be expressed in a similar physical manner. Thus the speed of a motor may be expressed by means of the frequency or voltage of a tachometer generator driven by the motor. The torque of a separately excited d-c motor may be determined by the current flowing through its armature. Such indication of performance is then "fed back" to the indication of ideal performance in such a manner as to obtain as a physical quantity, the difference between the ideal and actual indicators so as to determine the departure of the actual from the ideal operation.

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[1] For all numbered references, see list at end of paper.

* A convenient name selected to designate a dynamoelectric amplifier, which it is hoped will be generally adopted.

The third requirement is then that this difference, representing a deviation from the desired value, shall be used to modify or control the operation to reduce the deviation. With power apparatus of usual size, it is necessary to amplify this physical quantity in order that it may be used to produce the required correction.

The fourth factor, that of stability, is one which must be examined very carefully in any closed-cycle controller. A complete discussion of this problem of stability of a closed-cycle system such as a servo mechanism, electrical regulator, or similar controller is beyond the scope of this paper, but a discussion of the important factors is desirable here. The cause of most instability of such systems is the time lag which occurs in various parts of the system between the indication of error and the output. Some time lag is inevitable in any amplifying device of which we have knowledge, as even radio-type amplifiers have limiting frequencies above which they cannot function effectively.

The speed of response of a control system is usually determined by the element having the longest time delay and the stability problem becomes most serious where two or more sequential parts of the system have time-delay factors of similar magnitude. In the usual industrial power apparatus, the response time of the final controlled element is appreciable. This may be the time delay of a large generator field, the acceleration of a motor with a large connected inertia, or similar action. In this case, then, it is important in the closed-cycle system of control to shorten the response times of all the elements of the system prior to the final one, that is, any primary or secondary amplifier should function with much less time lag than that of the final power device.

Amplifier Requirements

An amplifier used in a closed-cycle control or regulating system should, therefore, function with a minimum time lag between input signals and output. In most industrial systems, the threshold value of time lag above which difficulty may be expected is in the neighborhood of one-tenth to one-quarter second. Contradictory examples can, of course, be found but the foregoing is a reasonable general value.

The next factor to be considered is the magnitude of the amplification. If the final unit to be controlled is the field of a large generator or motor, the power output required may be from a fraction of a kilowatt to many kilowatts. The differential power obtained from the feedback comparison is usually limited, being at the most a few watts and at times materially less than this where high accuracy is a factor. The amplifier must then cover its full output range with this excitation change.

A uniform or linear relationship between output and input signals may be an important factor in obtaining sensitive yet stable operation. There are exceptions here also where, for instance, an equal percentage response should be obtained at all times, or where some other relationship is desirable. In the usual case it is found expedient to provide any required nonlinear variations by special means used in conjunction with linear amplifiers.

The last, but by no means the least important consideration in designing or selecting the amplifier, is that it have a high degree of reliability and that it be sufficiently elemental in operation that it may be understood by nontechnical maintenance personnel. Shutdown or loss of production time in practically any industrial process is a costly thing, but in some processes this is vitally so. Such continuous processes as steel or paper making if unexpectedly interrupted may incur a loss in product alone of greater value than the first cost of the device causing the failure. In processes such as glass making, stoppage of some parts may require weeks or even months to restart.

In any case, any failure to function successfully should only be the result of the failure of some part or device which will be readily and quickly apparent to the maintenance electrician. Furthermore, the approach of such a failure should be determinable by routine inspection of the apparatus involved.

The Amplidyne Generator

The amplidyne generator meets the foregoing requirements in most instances to a very complete degree. The response time as determined previously,[3] is sufficiently short so that the over-all time lag of typical control systems is not increased materially by the use of this amplifier. The amplification constant is sufficiently high that in many cases the amplidyne generator can be used directly without supplementary amplifiers. Where the requirements of high accuracy dictate, it is readily controllable by sensitive high-accuracy amplifiers of low power output. The required input signal for the amplidyne generator is usually of the magnitude of a watt or so, which may be easily obtained from even a vacuum-tube amplifier using commercial radio-receiving tubes.

The amplidyne generator, while of somewhat unusual arrangement, utilizes the physical parts and method of construction of the familiar d-c machinery and consequently, may be readily tested for grounds, short circuits, or other causes of misoperation by readily available testing apparatus.

Methods of Use

There are three more or less basic manners in which the amplidyne generators may be employed in closed-cycle controllers. These differ in the means by which the excitation power or control signal is obtained and applied to the amplidyne generator field.

In the first and simplest method as illustrated in figure 1, the amplidyne exciter is equipped with two balanced fields on the control axis. One of these fields is excited from a source which provides the primary standard of performance. The excitation power supplied from this source is many times that required to obtain full output from the amplidyne generator. The second field is excited from the output function of the apparatus, which in figure 1 is the line current of a d-c generator, but may be alternatively speed, voltage, position, or other characteristic such that the control-field current will be proportional to this final function. This excitation,

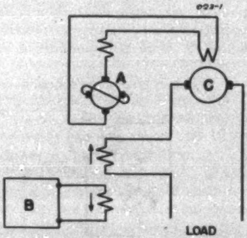


Figure 1. Circuit arrangement of an amplidyne exciter, used in a closed control system, excited by balanced field action

A—Amplidyne exciter with compensating field and opposed control fields

B—Any suitable primary current source

C—Separately excited d-c generator, controlled to deliver current, proportional to that of primary source differential with respect to the primary excitation, is also many times that required to excite the amplidyne generator. The net difference of these two exciting components then produces such excitation as may be required for the control range required. Inasmuch as the amplidyne generators operate at low flux densities on the control axis, there is adequate room in interpolar space for opposing field circuits of the magnitude described. Additional fields may be placed on this axis to assist in stabilization or to provide secondary control influences.

The limit of sensitivity of the amplidyne control systems using this arrangement is determined by the maximum values of opposed excitation which may be obtained from the two sources and placed upon the amplidyne control axis. This accuracy, in general, is in the neighborhood of a few per cent and is suitable for a great number of uses where speed of response and reliability are of more concern than accuracy.

The second method of obtaining the amplidyne generator excitation is from differential voltage circuits as illustrated in figure 2. Here the standard of comparison and the indication of output performance are expressed as opposed voltages with their circuits interconnected through the field circuit of the amplidyne generator. Any deviation of output from the ideal results in a difference in voltage, with a current flow of equivalent direction and magnitude through the amplidyne generator field so as to produce a change in output to reduce the deviation. Where sufficient control power is available so that the sources of control voltage are influenced to only a minor degree by the amplidyne field current, this is an effective means of control. Its accuracy is limited by the power available from the two voltage sources only and in some cases may be made extremely high. The disadvantage of this circuit arrangement is that it requires an electrical interconnection between the two sources of comparison. This, at times, may be impractical.

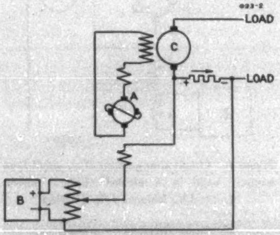


Figure 2. Circuit arrangement of an amplidyne exciter, used in a closed control system, excited from a differential voltage source

A—Amplidyne exciter with compensating and control fields

B—Any suitable primary source of direct voltage

C—Separately excited d-c generator controlled to deliver current proportional to the voltage of the primary source

A third method of application of the amplidyne equipment is through the use of supplementary composing amplifiers as illustrated in figure 3. The composing amplifier output used to excite the amplidyne generator is arranged in accordance with any desired relation of control functions. With such an amplifier, it is possible, for example, to control voltage of a generator accurately within certain load limits and at these load limits to transfer from voltage to current limit control or vice versa, depending upon the operating conditions. The amplidyne generator in this case functions as an intermediate power amplifier and as such, its characteristics of low time lag and linearity of characteristics are usually most important.

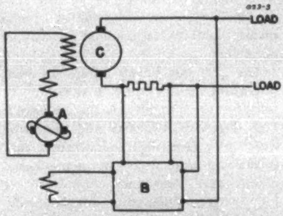


Figure 3. Circuit arrangement of an amplidyne exciter used in a closed control system, excited by composing amplifier

A—Amplidyne exciter with compensating and control fields

B—Composing amplifier combining current and voltage of d-c generator in required relationship with suitable standards to provide the amplidyne excitation

C—Separately excited d-c generator controlled to operate with a specified current-voltage relationship

Some of the typical applications of amplidyne generators which have been made recently will be briefly described. A complete discussion of all of the engineering features of each application is impractical and, accordingly, this summary will be indicative mainly of the variety of uses to which these machines may be put.

Machine-Tool Control

A simple yet interesting application of amplidyne equipment is in connection with a multiple-head boring mill in the Schenectady works of the General Electric Company. The two motions on each head of the mill (figure 4) are controlled by suitable motor-operated lead screws. The control motor for each head is arranged with a double-throw mechanical clutch mechanism so that it may operate either lead screw.

The operator may preset the next cut to be made by means of dials registering feet, inches, and thousandths of inches. When one cut is finished it is only necessary to clutch in the motor to the proper motion, press a button, and the equipment will automatically move the cutting tool to the exact position for the next cut.

This positioning control is obtained by a series of limit switches and accurate mechanical means of operating these from the motion of the heads. Each d-c control motor is separately excited and

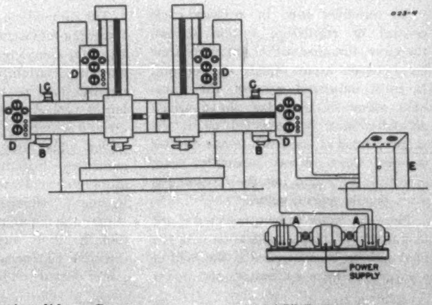


Figure 4. Schematic arrangement of amplidyne control equipment for automatic positioning of cutting heads on a boring mill

A—Amplidyne generators

B—Control motors

C—Pilot generators

D—Dial indicators and limit switches

E—Main control cabinet

receives its armature power from an amplidyne generator. The field excitation of the amplidyne generator is applied through circuits controlled by the precision limit switches. The amplidyne equipment by its low control power requirement permits these switches to be small in size and hence accurate in performance. One other important point is that the speed of the control motor is definitely held at any set value by means of the amplidyne equipment which is excited from a voltage-matching circuit. This circuit consists of a pilot generator, driven from the control motor, and a potentiometer circuit across the excitation source. When the slowdown limit switch is reached the motor is slowed down to $^1/_{50}$ rated speed by a change in potentiometer connection. This slow speed is held accurately regardless of the friction conditions which are encountered, thus permitting of accurate positioning on the final limit switch. Figure 5 illustrates schematically the control circuits for one of the control motors. Switches *A*, *B*, and *C* are the limit switches operated respectively by the foot, inch, and thousandths limit switches. After movement is started by pressing one of the directional push-button stations, switches *A* and *B* are opened as the final position nears, and the equipment slows down to an accurately held approach speed. At the final point, switch *C* opens which deenergizes the equipment, bringing about a quick and accurate stop.

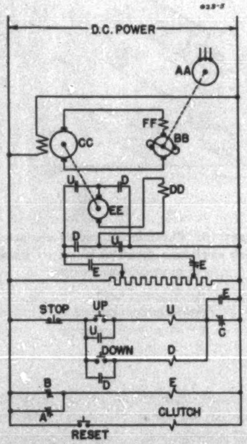


Figure 5. Elementary control diagram for amplidyne positioning control on boring mill

AA—A-c motor driving amplidyne generator
BB—Amplidyne generator
CC—Control motor
DD—Amplidyne generator control field
EE—Pilot generator

Reel Control

The problem of reeling a continuous strip of material is common to many industrial processes. The material may be delivered at constant speed or at times the speed of the material may be varied through wide limits. In many cases the drive requirements of the main part of the machine may be such that adjustable generator-voltage control is used for acceleration and to control the speed. In this case the speed of the strip may be directly proportional to the voltage applied to the driving system. In such cases, the requirements of constant reeling tension may be met by controlling an adjustable-speed d-c motor driving the reel core in such a manner that it will draw constant armature current from the adjustable-voltage power supply. The use of an amplidyne exciter for the reel motor, arranged so as to vary the excitation to cause the motor to draw a constant adjusted value of current has been successful in the reeling of steel strip as it is delivered from a tandem cold-strip mill.

In some installations other factors, besides the steady-state torque delivered to supply the strip tension, may become important. On drives requiring threading at low speed with subsequent rapid acceleration to operating speed, the acceleration torque of the motor and reel may be a substantial proportion of the total torque delivered and consequently must be taken into account. It is also sometimes desirable to provide for accurate speed control of the unloaded reel motor prior to the time the strip reaches the reel and to provide an automatic transition from speed control to tension control as the strip is threaded into the reel and the reel diameter begins to build up. Such operations as these may be obtained through the use of amplidyne equipment together with a supplementary composing amplifier or circuit. The reel shown in figure 6 was controlled by amplidyne equipment using one unit as a composing amplifier to combine the speed and acceleration control functions with a second and larger amplidyne exciter combining these functions with the main one of strip tension control in exciting the reel motor.

Steel-Strip Polisher

Several installations of a somewhat similar nature to the one just described have been made to control strip-polishing machines. These strip-polishing machines consist essentially of unwinding and winding reels, arranged in such a manner that the steel strip may be wound from one to the other and back again. The strip is buffed or polished by suitable means as it passes between the two reels. The drive must maintain substantially constant tension in the strip between the two reels and should also maintain uniform strip speed as the diameter of one reel decreases and the other increases. Amplidyne excitation control has been successfully applied to several of these machines to provide the desired characteristics of tension and speed. Figure 7 shows the general setup of such a machine with the two reels and the polishing mechanism. The motor generator set supplying power to the two reel motors, with the amplidyne generator used for excitation control, is shown in the left background.

Figure 6. Hot-strip-mill installation using amplidyne exciter for constant tension control of strip tension at reel

Figure 7. Steel-strip polishing apparatus using amplidyne exciter for reel motors to control speed and tension

Single-Motor Paper-Machine Drive

Accurate control of the speed of a paper-making machine is highly desirable to assure a uniform product and a minimum of waste. A wide range of operating speeds is also desirable so that both light and heavy grades of paper may be made on the same machine. Figure 8 shows the schematic arrangement of an amplidyne exciter applied to such a machine where the main drive motor was controlled by adjustable generator voltage. The amplidyne exciter controls the main-generator field excitation in response to a signal indication obtained from a tube-type composing amplifier. The indication of speed is supplied by a pilot generator of the permanent-magnet type which is driven from the line shaft of the machine. The voltage of this pilot generator, or a portion of it, is balanced against a voltage standard in the composing amplifier. The difference controls the amplidyne excitation to maintain the proper speed.

In addition to the main requirement of speed control, the current is held within definite limits during acceleration or retardation, figure 9. In starting the drive, the line contactor is closed and the current rises immediately to the limiting value slightly above full load. The drive accelerates at this current limit until the set speed is reached where the current drops to the normal steady-state value required by the load. When a change in speed is required, the operator moves the pilot-generator calibrating rheostat. Should this require an increase in speed, the current will be held at the upper current limit until the new speed is reached. Should the change be a decrease, the current will decrease to the point where the reverse-current limit control becomes effective. The drive will then slow down until the new speed is reached where the current will again stabilize at that required for the load.

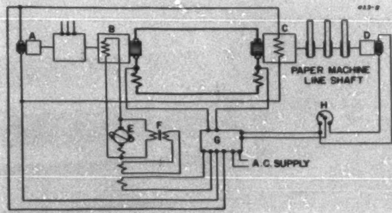
The over-all amplification of the control system is very high and the speed is accurately held to a fraction of one per cent. The current limits are not critical and are held within a few per cent.

Such a system of control is applicable in many places, particularly where the inertia of the mechanical system is large and a considerable time is required to accelerate from one speed to another.

Flying-Shear Control

The problem of acceleration control is also encountered in the application of motors to a flying shear at the end of a hot strip mill. Two successful amplidyne exciter applications have been made for this service. The shear is driven by a d-c motor with adjustable generator voltage supply. Synchronization of speed of shear with that of the last mill stand is provided by a supplementary mechanical interconnection including an overrunning clutch. The clutch permits the shear to be stopped while the mill is in operation.

Figure 8. Schematic diagram of paper-machine drive using electronic composing amplifier and ampidyne exciter for control of accelerating current and regulation of speed



A—Constant-voltage exciter
B—Adjustable-voltage generator
C—Main drive motor
D—Pilot generator
E—Amplidyne exciter
F—Stabilizing transformer
G—Composing amplifier
H—Speed-setting rheostat

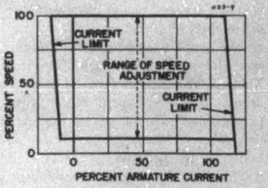


Figure 9. Speed-adjustment and current-limit diagram of paper-machine drive shown in figure 8

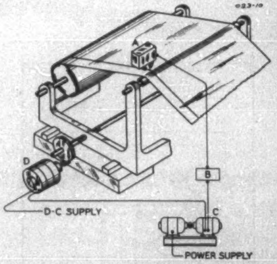


Figure 10. Photoelectric register equipment using amplidyne generator for power supply to servo correcting motor

A—Scanning head
B—Electronic amplifier
C—Amplidyne generator
D—Control motor

As the leading end of the strip approaches the shear, the shear is accelerated rapidly until the clutch engages and thereafter the motor exerts just sufficient torque to maintain the clutch in engagement under all normal load conditions. The shear must be quickly and accurately accelerated in such a manner that the first cut will be made just as the end of the strip comes to the shear. A high rate of acceleration is required and is limited only by the commutating capacity of the electrical equipment. As the shear reaches the speed corresponding to the mill speed, at which time the speed is limited by the overrunning clutch, the torque must be reduced to a much lower value just sufficient to maintain contact in the

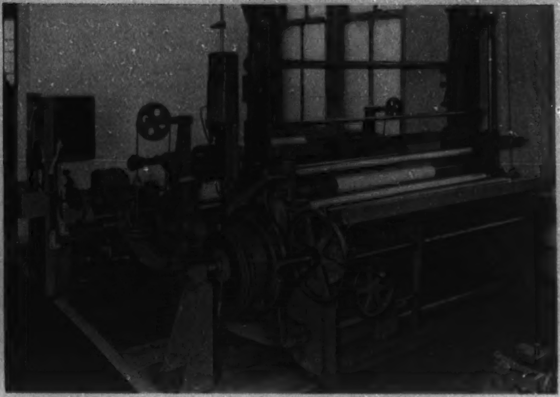


Figure 11. Installation of amplidyne unit on photoelectrically controlled winder and slitter

clutch. Thus, we have the problem of two degrees of current control, the transition from one to the other occurring at a certain definite speed relationship as determined by the relative voltages of two tachometer generators, one driven from the last mill stand and the other from the shear. The amplidyne exciter is particularly useful in this arrangement as the speed of response of the entire system must be high to prevent lagging or overshooting in torque on the speedy acceleration, with rapid transition to a lower torque as the clutch engages.

Photoelectric Side Register Control

A number of amplidyne generators have been built to deliver power to motors holding registration of a web of paper being rewound and slit as illustrated in figure 10. A photoelectric scanning head and amplifier obtains an indication from the edge of the web or from a line printed on the web of paper to determine when the web deviates from its proper position. The indication is amplified and applied to the field of the amplidyne generator. This generator applies power to a separate-excited d-c motor which is arranged to shift the main roll of paper sideways to bring about a correction in registration. Arrangements are made so that the paper is moved beneath the scanning point simultaneous with the moving of the main paper roll in order to eliminate any tendency to hunt which would be caused by a lag in the movement of the paper in front of the scanning head after the main paper roll has been moved.

An accuracy of approximately 0.01 inch is required in controlling the edge of the paper or line on the paper. When winding at high speed, the paper may be found to deviate rapidly from a set position so that a correcting speed of from 30 to 60 inches per minute is required to keep up with the errors introduced in the paper. Thus, at times the control line may move the full width of the accuracy zone in only $^1/_{100}$ of a second. A high response rate of all the equipment involved is necessary to keep up with the errors, and particularly necessary in order to obtain stable operation without hunting with the combination of high accuracy and high speed of correction.

Synchronous-Motor Excitation Control

In a recent installation of a 5,000-horsepower synchronous-motor drive for a structural-steel mill the motor excitation was supplied by an amplidyne exciter. This was controlled in such a manner that the motor operated with substantially constant leading reactive kilovolt-amperes at all loads up to 150 per cent of normal. This control assists the synchronous motor to develop high pull-out torque when peak loads are encountered. The 50-kw amplidyne-exciter-control excitation was obtained as a function of motor current and line voltage. As this excitation requirement was of the order of a few watts it was obtained from potential and current transformer circuits without further amplification.

Conclusions

Amplidyne generators have been successfully applied in a considerable number of industrial processes. In addition to the uses previously mentioned they are controlling load division between large parallel-operating d-c motors; controlling reel tension in a wire-drawing machine; and maintaining close speed regulation of tandem cold strip mills. The field of application has expanded rapidly but there are still many other uses to which these machines may be profitably put.

Systems requiring a quickly reversible source of armature power or field excitation are particularly benefited by the excellent amplidyne performance. In fields where thyratron grid-controlled rectifiers have been used to obtain a quick response characteristic of power proportions, the amplidyne generator offers an alternative method of control which some users will prefer because of the physical nature of the equipment.

Thus the amplidyne generator offers considerable promise in industrial power applications where the use of dynamo-electric machines is desirable but where other types of power amplifiers have been required in order to obtain proper responsiveness of control.

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Discussion

For discussion, see page 1136.

Generalized Bridge Network for Dielectric Measurements

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I. Problem

RECENT experience with bridges for the determination of power factor and dielectric constant of small oil samples over a range of frequency has indicated the desirability of improving their characteristics. These improvements or developments have to do principally with changes in circuit arrangements, a generalized representation of the bridge network in terms of controllable impedances, an increased sensitivity inherent in the bridge itself, and an effective means for controlling the influence of inherent impedances extraneous to the bridge measuring arms on the accuracy, precision, and dependency of the bridge measurements and balances.

The foregoing has been developed in connection with a joint research project of the Utilities Co-ordinated Research, Inc. (Association of Edison Illuminating Companies) and the Massachusetts Institute of Technology on the electrical and chemical studies of the deterioration of electrical insulating oils. This work involves the determination of the change in power factor and dielectric constant of the oil as a function of

(*a*). Deterioration brought about by

1. Oxygen absorbed during oxidation
2. Heat with an inert gas
3. Materials in contact with the oil under service conditions in combination with (1) or (2)
4. Processes (1), (2), and (3) carried on with or without the presence of the volatile products of deterioration

(*b*). Chemical products formed under (*a*)

(*c*). Initial physical, chemical, electrical, and optical data characterizing the oil

The oils used in these studies include standard insulating oils, narrow cuts from different crudes prepared by distillation and solvent refining, the purpose being to obtain a complementary set of samples with wide variation in initial physical and chemical properties, and synthetic samples of known composition.

The foregoing studies require that power factor and dielectric constants be measured with relatively small samples since, for example, during oxidation it is necessary to make a number of electrical tests during the run in addition to a large group of chemical tests, and in the use of specially prepared samples of oil only limited quantities of the oil are practically available. Some work done on the oxidation of oils in which the volatile products are retained in part in the oil has indicated that more satisfactory electrical measurements may be made at relatively low voltage gradients. Furthermore, it was desired to make the electrical measurements over a range of frequency, for the present 10–20,000 cycles per second, to permit a more complete study of the nature of the loss. This again limits the applied bridge voltage to relatively low values. Thus we have a combination of factors, small measuring cell capacitances, low applied bridge voltages, and low frequencies each of which makes more difficult precise determinations of the power-factor and dielectric-constant measurements.

It is logical to inquire as to what would be considered a desirable precision of these measurements. The precision of the measurements is determined principally by (*a*) the precision of the bridge measurements, (*b*) the precision of the temperature measurement of the oil in the measuring cell, (*c*) the effect of cleaning procedure used in cleaning the measuring cell before the introduction of the oil, the nature of the measuring cell, and the method of introducing the sample into the measuring cell, and (*d*) the effect of time and change of temperature of the oil in the cell. The accuracy of any particular measurement is probably principally determined by (*c*) and the precision of the measurements for a change in frequency is principally determined by (*a*) and (*b*). The effect of temperature measurement of the sample under test may be determined by first considering an oil which doubles its power factor for any change in temperature of 15 degrees centigrade. To obtain an electrical measurement to plus or minus one per cent of the measurement requires the temperature measurement of the sample to ±0.21 degrees centigrade. Similarly for a value of 10 degrees instead of 15 degrees centigrade, a precision of electrical measurement of plus or minus one per cent of the measurement requires a temperature measurement of ± 0.14 degrees centigrade. In view of the foregoing it is believed that a precision of measurement of power factor to plus or minus one per cent of the power factor represents satisfactory measurements. Thus for an oil sample power factor of 10^{-4} (an approximate range for standard insulating oils at 25 degrees centigrade) the precision of power-factor measurement by the bridge must be $\pm 10^{-6}$ and for an oil sample power factor of 10^{-2} a measurement precision of $\pm 10^{-4}$.

For the case of capacitance or dielectric-constant measurements an accuracy of measurement of plus or minus one per cent is readily obtainable, this being principally determined by the accuracy of the measuring-arm resistances and the calibration of the reference capacitor. However, in the studies referred to in the foregoing, it is desirable to have a greater precision in change of capacitance measurements to permit studies to be made of the changes as a function of chemical data obtained during a deterioration. The change in dielectric constant of an oil during a reasonable deterioration may be of the order of a few per cent and it would be desirable to determine such a change to a precision of plus or minus one per cent. It is believed that these values may be obtained.

Initially the conventional (Thomas)-Schering bridge (source-symmetrical bridge since symmetry obtains with respect to source terminals) was used for these measurements. However for the foregoing conditions, this type of bridge is quite unsatisfactory from the point of view of inherent sensitivity at low frequencies due to the large difference in impedance of the measuring cell and the measuring arms. Consequently the conventional conjugate (Thomas)-Schering bridge (detector-symmetrical bridge since symmetry obtains with respect to detector terminals) was used because it has a greater inherent bridge sensitivity under the foregoing conditions. However, it was found that with measuring cells of low

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capacitance that this type of bridge is practically impossible to operate, due to the fact that the bridge and guard or shield (Wagner[1] ground) balances are so closely dependent.

Further study of a generalized bridge network showed that the precision and dependency of balances of a general five-terminal bridge network is a function of three dependent balances and a dependency factor which is a function of the ratios of certain impedances in the bridge network. This paper evaluates these factors and shows that for certain values of bridge constants and frequency, the use of three balances is a practical necessity and in general permits simplified bridge construction and operation.

II. General Bridge Network

In general, accurate dielectric measurements of small insulating-oil samples with low power factors require a three-terminal measuring cell and reference capacitor. Thus a five-terminal bridge network is required. Since a general five-terminal impedance network will consist of a minimum of ten impedances, of which two will be impedances across diagonally opposite terminals, the impedances of the source and of the detector, a minimum of eight impedances must be considered in the balance relations. All of the impedances must be considered in connection with the bridge sensitivity. Such a generalized bridge network is shown in figure 1*a*.

Since there are eight impedances to be considered in the balance relations it is advisable to consider the nature of these impedances. Four of these impedances will be in the bridge arms *AM*, *AN*, *BM*, and *BN* in figure 1*a* and the remaining four will be from the bridge terminals to shield or guard (ground in figure 1*a*) *AG*, *BG*, *MG*, and *NG*. The bridge arms consist of two measuring arms made up of resistors and capacitors in parallel arrangement and two arms containing a reference capacitor (or capacitors when a substitution method is used) and a measuring cell. Now since each of these bridge arms is shielded, each arm individually forms a three-terminal network, thereby introducing three individual impedances. For example, consider a three-terminal reference capacitor in bridge arm *AN* of figure 1*a*, the impedance from the source electrode to the bridge electrode will be the impedance Z_2, the impedance from the bridge electrode to guard electrode and the impedance from the bridge lead to its shield as introduced by mechanical supports or by other means will be in part the impedance Z_5 (the remaining part arises similarly from arm *BN*), the impedance from source electrode to guard and from the source lead to its shield as introduced by mechanical supports or by other means will be in part the impedance Z_3 (the remaining part arises similarly from *AM*).

The extraneous bridge arms *AG*, *BG*, *MG*, and *NG* of figure 1*a*, may contain in addition to the inherent impedances as outlined in the foregoing and arising from the bridge construction, definite controllable impedances (such as resistors, capacitors, and inductors) for varying the impedances of these arms as may be required for desired bridge operation and precision. The generalized bridge network will contain such controllable impedances in each of these arms.

III. General Balance Relations

For the generalized bridge network figure 1*a* Balsbaugh and Moon[2] have shown that the detector points *M* and *N* will have equal potentials for the condition of

$$\begin{aligned}&Z_1Z_2Z_5Z_6Z_7+Z_2Z_3Z_4Z_7Z_8+Z_2Z_4Z_5Z_6Z_7+\\&\quad Z_2Z_3Z_5Z_6Z_7+Z_2Z_3Z_4Z_6Z_7+Z_2Z_3Z_4Z_5Z_7-\\&\quad Z_1Z_2Z_4Z_6Z_8-Z_1Z_3Z_4Z_5Z_8-Z_1Z_4Z_5Z_6Z_8-\\&Z_1Z_3Z_5Z_6Z_8-Z_1Z_3Z_4Z_6Z_8-Z_1Z_3Z_5Z_7Z_8=0\end{aligned} \tag{1}$$

The balances necessary to satisfv (3) may be obtained by rewriting (1) as follows:

$$\begin{aligned}&(Z_2Z_7-Z_1Z_8)(Z_4Z_5Z_6+Z_3Z_5Z_6+Z_3Z_4Z_6+\\&\quad Z_3Z_4Z_5+Z_2Z_4Z_6)+Z_7(Z_1Z_5-Z_2Z_4)(Z_2Z_6-\\&\qquad Z_3Z_8)=0\end{aligned} \tag{2}$$

Thus (2) shows that to permit the use of the bridge balance relation

$$Z_2Z_7=Z_1Z_8 \tag{3}$$

there must simultaneously be satisfied in addition to (3) either of the relations

$$Z_1Z_5=Z_2Z_4 \tag{4}$$

or

$$Z_2Z_6=Z_3Z_8 \tag{5}$$

With (3) satisfied (4) and (5) may also be expressed as (6) and (7) respectively

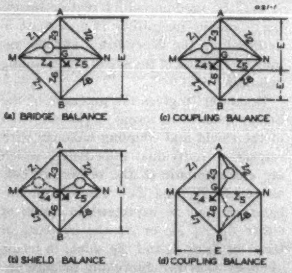
$$Z_5Z_7=Z_4Z_8 \tag{6}$$

$$Z_1Z_6=Z_3Z_7 \tag{7}$$

Thus in general there are three dependent balances in establishing relations between the arms of the general bridge network. The relation $Z_2Z_7=Z_1Z_8$ is termed a bridge balance, the relation, $Z_2Z_6=Z_3Z_8$ or $Z_1Z_6=Z_3Z_7$, is termed a shield balance, and the relation, $Z_1Z_5=Z_2Z_4$ or $Z_5Z_7=Z_4Z_8$, is termed a coupling balance. Note that there are two unique impedances with respect to each of the coupling and shield balances namely, Z_4, Z_5 and Z_3, Z_6 respectively. Accordingly the impedances Z_4 and Z_5, the impedances from the detector terminals to shield, are termed the coupling impedances in view of the fact that they give a coupling from the bridge balance points to shield, and the impedances Z_3 and Z_6, the impedances from the source terminals to shield, are termed the shield impedances.

It is important to note that the coupling and shield balances are fundamentally different. In the shield or guard balance the shields and guards are balanced to the same potential as the bridge detector terminals (bridge sections of the measuring cell and reference capacitor) whereas in the coupling balance the shields and guards must be at a different potential than the bridge detector terminals. It is furthermore important to note that in (*a*) a five-terminal bridge network using a three terminal measuring cell and reference capacitor the guard and shield balances are made simultaneously and that in (*b*) a five-terminal bridge network consisting of a shielded our-arm bridge using two terminal measuring and reference capacitors, the guards are absent and only a shield balance may be made. The use of three balances and the theory developed applies to both (*a*) and (*b*) and since the shield balance is common to both, the term shield balance is used. Thus in the present case shield and guard balances are synonomous.

The methods used in obtaining the three balances are shown in figures 1*a*, 1*b*, 1*c*, and 1*d*. Figure 1*a* shows the detector in position for bridge balancing $Z_1Z_8=Z_2Z_7$. Figure 1*b* shows the detector in either of two positions for shield balancing, $Z_2Z_6=Z_3Z_8$ or $Z_1Z_6=Z_3Z_7$, the preferred position depending upon the



(a) BRIDGE BALANCE (c) COUPLING BALANCE

(b) SHIELD BALANCE (d) COUPLING BALANCE

Figure 1. Balancing arrangements

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1. For all numbered references, see list at end of paper.

relative magnitudes of the impedances in the arms involved in either balance and thus affecting the resulting sensitivity. Figure 1*c* shows the detector in position for coupling circuit balancing $Z_1Z_5=Z_2Z_4$ or $Z_6Z_7=Z_4Z_8$, the detector being in the normal bridge balancing position and with the source voltage being applied either between *A-G*, *B-G*, or between *G* and *A* connected to *B*. Figure 1*d* also shows an optional arrangement for coupling circuit balancing with the detector circuit in either of two positions. Note that in the latter case the source and detector terminals are interchanged so that the shield and coupling arms are interchanged with balance obtained similarly to the normal shield balance. The preferred arrangements for coupling circuit balances depend upon the facility and practicability of the switching operations and the resulting sensitivity obtainable.

Since it has been shown that there are three balances determining the bridge operation and measurement it is desirable to define and determine the dependency of the balances. The interrelation between any two balances or the dependency may be definitely defined by considering figure 1*a* in which the detector is in position for bridge balancing. This dependency may be obtained either for the general case assuming any degree of unbalance in each of the three balances or for the specific case in which all three balances are close to balance. Taking the latter case since it is of greatest importance, assume that the circuit impedances in figure 1*a* are such that three balances are satisfied to some desirable precision and that the shield balance is unbalanced by changing Z_6, this change being either in the real component (capacitance-factor unbalance) or imaginary component (power-factor unbalance). These changes may be made individually or simultaneously. Now, in general, a deflection will appear in the detector indicating the bridge circuit is out of balance, the magnitude of this will depend upon the degree of unbalance in the shield balance, the degree of unbalance of the coupling balance and the bridge constants and frequency. The apparent unbalance in the bridge may now be corrected by changing any one of the bridge impedances, say Z_7, either real or imaginary components or both as required, to give again a bridge balance. The ratio of the per-unit change from balance in Z_7 (the change in Z_7 expressed as a ratio of Z_7) to the per-unit change in Z_6 (the change in Z_6 expressed as a ratio of Z_6) gives a quantitative measure of the dependency between the bridge and shield balances. This quantity may be complex, indicating a power-factor unbalance in one circuit gives a capacitance factor unbalance in the other circuit or vice versa. This same procedure may be used to obtain quantitatively the dependency between any two of the three dependent balances.

Since in general for precise measurements the dielectric constant and power factor of the sample must be obtained from (3) it is advisable to investigate the precision of the coupling balance as given by (4) or (6) and the shield balance as given by (5) or (7) on the validity of (3). Also since there are three dependent balances it is advisable to investigate the conditions determining the degree of dependency of these balances on each other. From the point of view of the mechanics of the balancing it is desirable to have these three balances relatively independent. The foregoing are analyzed in IV.

Kouwenhoven and Banos[3] have shown that "in a completely guarded and shielded bridge it is advisable to keep the cross capacitances existing between the measuring circuits and the guard circuits low" and that "in such a bridge it is of prime importance that the conductances of the cross capacitances be maintained at zero". Balsbaugh and Moon[2] showed the significance of the difference of the coupling impedances in the bridge balance and Balsbaugh and Herzenberg[4,5] showed that there were three balances in a five-terminal network and gave a procedure for obtaining a capacitance factor balance for the coupling circuit.

IV. Bridge Balance as Affected by Shield and Coupling Balances

1. GENERAL RELATIONS

To determine quantitatively the dependency of the balances and the precision of the shield and coupling balances as affecting the precision or validity of the bridge balance assume that (3), (4), and (5), the three balance relations, are not satisfied and that

$$Z_2Z_7-Z_1Z_8-Z_8\Delta Z_1=0 \tag{8}$$

$$Z_1Z_5-Z_2Z_4-Z_2\Delta Z_4=0 \tag{9}$$

$$Z_3Z_8-Z_9Z_6-Z_9\Delta Z_6=0 \tag{10}$$

in which ΔZ_1, ΔZ_4, and ΔZ_6 are the changes in impedances Z_1, Z_4, and Z_6 respectively to effect the bridge, coupling and shield balances respectively. Substituting (8), (9), and (10) in (2) gives

$$\frac{\Delta Z_1}{Z_1}=\frac{\Delta Z_4}{Z_4}\,\frac{\Delta Z_6}{Z_6}D \tag{11}$$

in which D is the dependency factor and

$$\frac{1}{D}=\left(\frac{Z_1Z_8}{Z_2Z_7}\right)\left(\frac{Z_5}{Z_2}+\frac{Z_6Z_3}{Z_9Z_4}+\frac{Z_8}{Z_2}+\frac{Z_8Z_6}{Z_9Z_4}+1\right) \tag{12}$$

and $\Delta Z_1/Z_1$, $\Delta Z_4/Z_4$, and $\Delta Z_6/Z_6$ are the per-unit complex changes required in Z_1, Z_4, and Z_6 to effect the three balances. These complex per-unit changes may be expressed in terms of a real per-unit change, q (capacitance-factor unbalance) and an imaginary per-unit change, p (power-factor unbalance). Thus

$$\frac{\Delta Z_1}{Z_1}=q_1+jp_1 \tag{13}$$

$$\frac{\Delta Z_4}{Z_4}=q_4+jp_4 \tag{14}$$

$$\frac{\Delta Z_6}{Z_6}=q_6+jp_6 \tag{15}$$

Thus, this procedure permits the effects of shield and coupling circuit unbalances to be directly evaluated in terms of precision of power-factor and capacitance-factor measurements in the bridge circuit or sample. A discussion of these per unit capacitance factor and power factor quantities and their determination in terms of the resistance and capacitances in any arm is given in IV-2. Substituting (13), (14), and (15) in (11) gives

$$(q_1+jp_1)=(q_4+jp_4)(q_6+jp_6)D \tag{16}$$

Note that (16) is a perfectly general relation and is independent of the closeness of balance of either of the three balances, of the nature of the inherent impedances in the bridge network, and of the impedances in the bridge arms.

The dependency factor D for the condition of all three balances close to balance may be simplified from (12) by using (3), (4), and (5). This gives

$$\frac{1}{D}=\frac{Z_5}{Z_2}+\frac{Z_3}{Z_1}+\frac{Z_8}{Z_2}+\frac{Z_9}{Z_6}+1 \tag{17}$$

The dependency factor D is a function of ratios of impedances appearing in the different balances, coupling and shield to bridge. It is in general complex although it may be predominantly real or imaginary, and its magnitude is important in determining the dependency and the required precision of the shield and coupling balances.

Equation 16 gives the precision of the bridge balance in terms of the precision of the shield and coupling balances with respect to individual impedances which are characteristic of the respective balances, Z_1, Z_4, and Z_6. The bridge unbalances may be expressed in terms of either Z_1, Z_2, Z_7, or Z_8, the coupling in terms of Z_4 or Z_5 and the shield in terms of either Z_3 or Z_6. Since any balance determines the relation between four impedances it is possible to express a per-unit change of one of the impedances in terms of the equivalent change from balance in one of the other impedances. Consider figure 1*a* and diagonally opposite impedances Z_1 and Z_8 changed from balance by ΔZ_1 and ΔZ_8 respectively. Then

$$Z_2Z_7-(Z_1+\Delta Z_1)(Z_8+\Delta Z_8)=0 \qquad (18)$$

and neglecting second order terms

$$\frac{\Delta Z_1}{Z_1}=-\frac{\Delta Z_8}{Z_8} \qquad (19)$$

or

$$(q_1+jp_1)=-(q_8+jp_8) \qquad (20)$$

Similarly for any two adjacent impedances in the bridge for example Z_7 and Z_8 and a change from balance of ΔZ_7 and ΔZ_8 respectively, gives

$$Z_2(Z_7+\Delta Z_7)=Z_1(Z_8+\Delta Z_8) \qquad (21)$$

$$\frac{\Delta Z_7}{Z_7}=\frac{\Delta Z_8}{Z_8} \qquad (22)$$

$$q_7+jp_7=q_8+jp_8 \qquad (23)$$

Thus using relations similar to (20) and (23), (16) may be expressed for the bridge, coupling, and shield unbalances in terms of any impedance which is unique with respect to that balance.

2. Per-Unit Changes in Impedances

A per-unit change in an impedance is defined as the change in the impedance from an initial value to a final value expressed as a ratio of the initial value. Since in general the impedance will consist of a real and an imaginary component, the per-unit change in impedance will consist of a real and an imaginary component, the real part of the per-unit change being expressed as a per-unit capacitance-factor change q, and the imaginary part of the per-unit change being expressed as a power-factor change p. The arms of the bridge network may be considered conveniently to be essentially resistive (measuring arm with resistance in parallel with capacitance) or capacitive (reference capacitor or measuring cell represented by resistance in series with a capacitance). Thus for a capacitance C_p in parallel with a resistance R_p

$$Z_p=\frac{R_p-j\omega R_p^2C_p}{1+\omega^2R_p^2C_p^2} \qquad (24)$$

$$Z_p+\Delta Z_p=\frac{(R_p+\Delta R_p)-j\omega(R_p+\Delta R_p)^2(C_p+\Delta C_p)}{1+\omega^2(R_p+\Delta R_p)^2(C_p+\Delta C_p)^2} \qquad (25)$$

and from (24) and (25) $\Delta Z_p/Z_p$ may be determined and

$$\frac{\Delta Z_p}{Z_p}=(q_p+jp_p) \qquad (26)$$

For the common condition of $\omega R_pC_p<0.1$, and for relatively small values of q_p and p_p (26) becomes

$$\frac{\Delta Z_p}{Z_p}=q_p+jp_p\cong\frac{\Delta R_p}{R_p}-j\omega R_p\Delta C_p \qquad (27)$$

For example with a resistance, $R_p=10^5$ ohms, in parallel with a capacitance, $C_p=100$ micromicrofarads and $f=60$ cycles, a change in R_p to 100, 100 ohms and in C_p to 126.5 micromicrofarads gives $\Delta R_p=100$ ohms and $\Delta C_p=26.5$ micromicrofarads. Then from (27) $\Delta Z_p+Z_p=0.001-j0.001$. This gives a positive per-unit capacitance-factor change and a negative per-unit power-factor change in Z_p of 0.001.

For a capacitance C_s in series with a resistance R_s

$$Z_s=R_s-j\frac{1}{\omega C_s} \qquad (28)$$

$$Z_s+\Delta Z_s=R_s+\Delta R_s-j\frac{1}{\omega(C_s+\Delta C_s)} \qquad (29)$$

and from (28) and (29) $\Delta Z_s/Z_s$ may be determined and

$$\frac{\Delta Z_s}{Z_s}=q_s+jp_s \qquad (30)$$

For the common condition of $\omega R_sC_s<0.1$, and for relatively small values of q_s and p_s (30) becomes

$$\frac{\Delta Z_s}{Z_s}=q_s+jp_s\cong-\frac{\Delta C_s}{C_s}+j\omega\Delta R_sC_s \qquad (31)$$

For example, with a resistance $R_s=10^6$ ohms in series with a capacitance $C_s=10$ micromicrofarads and $f=60$ cycles, a change in R_s to 1,265,000 ohms and in C_s to 10.01 micromicrofarads gives $\Delta R_s=265{,}000$ ohms and $\Delta C_s=0.01$ micromicrofarad. Then from (31) $\Delta Z_s/Z_s=-0.001+j0.001$. This gives a negative per-unit capacitance-factor change and a positive per-unit power-factor change of 0.001.

3. Application to Detector-Symmetrical Bridge

Consider the detector-symmetrical bridge in figure 2*a* with impedance designation corresponding to figure 1*a* for similar positions in the network. It is now desired to determine the effect of the precision of shield and coupling balances on the precision of the bridge balance for (*a*) condition of all balances close to balance and (*b*) for condition of coupling circuit far out of balance as would be the case when no provision is made for a coupling balance (arms *qr*, *sg*, and *vu*, *tg* open figure 2*a*) and with the shield and bridge balances close to balance.

(*a*). For the condition of all three balances close to balance the effect of coupling and shield unbalances on the bridge balance is given in general by (16) with the dependency factor D given by (12) or (17). The per-unit changes in impedances Z_1, Z_4, and Z_6 may also be expressed in terms of their resistance and capacitance components,

$$\frac{\Delta Z_1}{Z_1},\ \frac{\Delta Z_6}{Z_6},\ \text{and}\ \frac{\Delta Z_4}{Z_4}\ \text{by (27)}$$

(for this type of bridge Z_4 must be essentially resistive and Z_6 essentially capacitive). Substituting these into (16)

$$\left(\frac{\Delta R_1}{R_1}-j\omega R_1\Delta C_1\right)=\left(\frac{\Delta R_4}{R_4}-j\omega R_4\Delta C_4\right)\times\left(\frac{\Delta R_6}{R_6}-j\omega R_6\Delta C_6\right)D \qquad (32)$$

Values of the dependency factor D for certain typical bridge-arm constants and for different frequencies are given by (*a*) to (*f*) in table I. It may be noted that the values are relatively large and vary from $0.49+j0.00$ at 60 cycles to $0.18-j0.06$ at 10,000 cycles and the assumed constants. Thus taking the constants as given by (*a*) table I in which $D=0.49+j0.00$, (32) and (16) indicate that to give $q_1=p_1=\pm10^{-6}$ requires, for the shield and coupling circuits balanced to the same precision in capacitance factor and power factor, that $q_4=q_6=p_4=p_6=\pm10^{-3}$. This shows that for this particular case of bridge operation (with the resistances in the shield and coupling arms set to within one part in a thousand and with the capacitances set to within an ωRC of 0.001 in these same arms) the measured bridge power factor as affected by the extraneous

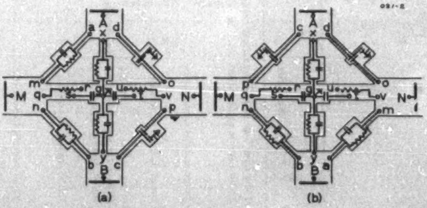

Figure 2. Generalized bridge network

(*a*)—Conjugate Schering or detector - symmetrical connection

(*b*)—Schering or source - symmetrical connection inherent bridge impedances or by the shield and coupling balances will be within $\pm 10^{-6}$. Similarly if it is desired to have $q_1 = p_1 = \pm 10^{-4}$, it is necessary to have $q_4 = q_5 = p_4 = p_5 = \pm 10^{-2}$. The foregoing indicates quite satisfactory bridge operation in that close balancing of the shield and coupling circuits is not required.

(*b*). However, now consider the case in which a coupling balance is not used and the coupling impedances Z_4 and Z_5 are determined entirely by the bridge construction. This case is represented by (*g*) in table I in which the coupling impedances Z_4 and Z_5 correspond to 400 micromicrofarads at 60 cycles, $D = 0.95 - j0.00$ and $q_4 + jp_4 = -1.00 + j0.00$. Thus (16) becomes

$$(q_1 + jp_1) = -0.95(q_4 + jp_4) \qquad (33)$$

This indicates that the bridge and shield balances are almost directly coupled (unit coupling) and that the shield must be balanced as precisely as the bridge. For low power factors and high-precision measurements this is a practical impossibility in view of the nature of the inherent impedances comprising the shield balance. Of equal importance is the fact that balance of the shield and coupling circuits could not be made since the two balances cannot be separated in the balancing operation.

4. APPLICATION TO SOURCE-SYMMETRICAL BRIDGE

Consider the source-symmetrical bridge in figure 2*b* with impedance designation corresponding to figure 1*a* for similar positions in the network. It is now desired to determine the effect of the precision of the shield and coupling balances on the precision of the bridge balances for (*a*) condition of all balances close to balance and (*b*) for the condition of shield and bridge circuits close to balance and the coupling circuit far out of balance, as would obtain when no provision is made for this balance and (1) the inherent impedances Z_4 and Z_5 are such that these values are far out of balance due to bridge construction or (2) even if these impedances are normally reasonably close to balance they are thrown far out of balance as a result of high humidity changing their loss component.

(*a*). For the condition of all three balances close to balance, the effect of coupling and shield unbalances on the bridge balance is given in general by (16) with the dependency factor D given by (12) or (17). The per-unit changes in impedances Z_1, Z_4, and Z_5 may also be

Table I. Values of the Dependency Factory D for Typical Cases

Remarks		Z_1	Z_2	Z_3	Z_4	Z_5	Z_6	Z_7	Z_8	$\frac{\Delta Z_4}{Z_4}$	Coupling Circuit	f	D	
Values for detector-symmetrical bridge, Z_2 is sample, Z_8 is reference capacitor														
Small sample capacitance	R in ohms	10^6	0	10^6	2,500	0	10^6	10^6	0			60	$0.49-j0.00$	*a*
	C in $\mu\mu f$	100	10	100	4,000	400	100	100	10	$0+j0$	Balanced	1,000	$0.49-j0.00$	*b*
	Connection	Parallel	Series	Parallel	Parallel	Series	Parallel	Parallel	Series			10,000	$0.48-j0.01$	*c*
Large sample capacitance	R in ohms	10^6	0	10^6	10^6	0	10^6	10^6	0			60	$0.25-j0.00$	*d*
	C in $\mu\mu f$	100	400	100	100	400	100	100	400	$0+j0$	Balanced	1,000	$0.25-j0.02$	*e*
	Connection	Parallel	Series	Parallel	Parallel	Series	Parallel	Parallel	Series			10,000	$0.18-j0.06$	*f*
Small sample capacitance	R in ohms	10^6	0	10^6	0	0	10^6	10^6	0	$-1.00+j0.00$		60	$0.95-j0.00$	*g*
	C in $\mu\mu f$	100	10	100	400	400	100	100	10	$-1.00+j0.01$	Unbalanced	1,000	$0.95-j0.01$	*h*
	Connection	Parallel	Series	Parallel	Series	Series	Parallel	Parallel	Series	$-0.96+j0.07$		10,000	$0.90-j0.07$	*i*
Values for source-symmetrical bridge, Z_1 is sample, Z_2 is reference capacitor														
Small sample capacitance	R in ohms	0	0	0	0	0	250	10^4	10^4			60	$0.0000+j0.0015$	*j*
	C in $\mu\mu f$	10	10	400	400	400	4,000	100	100	$0+j0$	Balanced	1,000	$0.0008+0.j0251$	*k*
	Connection	Series	Series	Series	Series	Series	Parallel	Parallel	Parallel			10,000	$0.076+j0.226$	*l*
Large sample capacitance	R in ohms	0	0	0	0	0	10^4	10^4	10^4			60	$0.0000+j0.0015$	*m*
	C in $\mu\mu f$	400	400	400	400	400	100	100	100	$0+j0$	Balanced	1,000	$0.0027+j0.0248$	*n*
	Connection	Series	Series	Series	Series	Series	Parallel	Parallel	Parallel			10,000	$0.125+j0.117$	*o*
Small sample capacitance	R in ohms	0	0	0	0	0	10^4	10^4	10^4			60	$0.0000+j0.0015$	*p*
	C in $\mu\mu f$	400	400	400	100	400	100	100	100	$-0.75+j0.00$	Unbalanced	1,000	$0.0022+j0.0249$	*q*
	Connection	Series	Series	Series	Series	Series	Parallel	Parallel	Parallel			10,000	$0.125+j0.142$	*r*
Coupling arms in parallel instead of in series	R in ohms	0	0	0	10^6	10^6	250	10^4	10^4			60	$0.0905+j0.0000$	*s*
	C in $\mu\mu f$	10	10	400	400	400	4,000	100	100	$0+j0$	Balanced	1,000	$0.0905-j0.0001$	*t*
	Connection	Series	Series	Series	Parallel	Parallel	Parallel	Parallel	Parallel			10,000	$0.0902-j0.0004$	*u*

In all cases the bridge and shield balances are made, the coupling balance is or is not made as indicated. The power factor of the sample, the reference condenser, and one of the inherent impedances is taken as zero. This makes the impedances for balance independent of frequency and it has only a slight effect on *D*.

expressed in terms of their resistance and capacitance components, $\Delta Z_1/Z_1$ by (31), $\Delta Z_6/Z_6$ by (27), and $\Delta Z_4/Z_4$ by either (27) or (31), depending upon whether Z_4 and Z_6 are both essentially resistive or capacitive respectively (arms *qr*, *sg* and *vu* and *tg* in series or parallel, respectively, figure 2*b*). Equation 16 now gives

$$\left(-\frac{\Delta C_1}{C_1}+j\omega\Delta R_1C_1\right)=\left[\left(-\frac{\Delta C_4}{C_4}+j\omega\Delta R_4C_4\right)\right.$$
$$\left.\text{or}\left(\frac{\Delta R_4}{R_4}-j\omega R_4\Delta C_4\right)\right]\left(\frac{\Delta R_6}{R_6}-j\omega R_6\Delta C_6\right)D \tag{34}$$

Values of the dependency factor *D* for this type of bridge and for certain bridge constants and for different frequencies are given by *j* to *o*, table I, for the coupling impedances being essentially capacitive and by *s* to *u*, table I, for the coupling impedances being essentially resistive. These values of *D*, for the cases given, vary from $0.0000+j0.0015$ to $0.125+j0.117$. Thus they show quite a range in magnitude and also in the angle of *D*.

(*b*). Now consider the condition of the shield and bridge circuits balanced and the coupling circuit out of balance due to no provision for this balance and resulting from bridge construction or operating conditions. This condition is represented by *p* to *r*, table I, and gives values of *D* from $0.0000+j0.0015$ to $0.125+j0.142$ with $q_4+jp_4=-0.75+j0.00$ for the assumed bridge constants. The inherent coupling impedances in the source-symmetrical bridge are normally capacitive although they may have substantial loss components. Thus even though the capacitance components are approximately equal in the bridge construction the loss components may be unequal giving a significant value of p_4. Furthermore, when no provision is made for coupling balance q_4 or p_4 may have relatively large values resulting from (*a*) high humidity, (*b*) use of different measuring cells or reference capacitors with different inherent coupling impedances, and (*c*) use of the bridge with other than equal-ratio arms.

5. Precision of Bridge Balance in Terms of Precision of Shield and Coupling Balances and Dependency Factor

The precision of the bridge balance in terms of the precision of the shield and coupling balances and the dependency factor may be readily obtained from (16). There are two conditions of particular interest: (*a*) all three balances made and with the shield and coupling balances of equal precision in both capacitance factor and power factor, and (*b*) only two balances made, bridge and shield, with various values of q_4 and p_4.

(*a*). For this condition (16) may be expressed giving the assumed equal precision of coupling and shield balances, q_4, p_4, and q_6, p_6 respectively in terms of the desired precision of the bridge measurements q_1 and p_1 and the real and imaginary components D_r and D_i respectively of the dependency factor. This gives

$$\pm q_4=\pm q_6=\pm p_4=\pm p_6=\sqrt{\frac{\pm q_1=\pm p_1}{2(D_r+D_i)}} \tag{35}$$

Equation 35 gives the greatest precision required of any of q_4, q_6, p_4, and p_6. In (35) D_r and D_i are always positive and independent of the algebraic sign as determined from *D*.

Representative values from (35) are given in table II.

Table II

D_r	D_i	$\pm q_4=\pm q_6=\pm p_4=\pm p_6$ for $\pm q_1=\pm p_1=\pm 10^{-6}$	 for $\pm q_1=\pm p_1=\pm 10^{-4}$
1	0	$0.71(10^{-3})$	$0.71(10^{-2})$
0	1	$0.71(10^{-3})$	$0.71(10^{-2})$
1	1	$0.5\ (10^{-3})$	$(0.5)10^{-2}$
0.01	0	$0.71(10^{-2})$	$0.71(10^{-1})$
0	0.01	$0.71(10^{-2})$	$0.71(10^{-1})$
0.01	0.01	$(0.5)10^{-2}$	$0.5(10^{-1})$
0.1	0.01	$2.13(10^{-3})$	$2.13(10^{-2})$
0.01	0.1	$2.13(10^{-3})$	$2.13(10^{-2})$

q_4, q_6 (capacitance factors) and p_4, p_6 (power factors) are precisions of shield and coupling balances—q_1, p_1 are precisions of bridge balance—D_r, D_i are real and imaginary components of dependency factor *D*.

Thus (35) and table II show that for the limiting practical case the required precision of the shield and coupling balances ($\pm q_4=\pm q_6=\pm p_4=\pm p_6$) is equal to 0.71 times the square root of the desired precision of the bridge balance ($\pm q_1=\pm p_1$). As the value of the dependency factor gets smaller the required precision of the shield and coupling balances under the foregoing conditions gets less for a given precision of bridge balance. Even for high-precision bridge balance the required guard and coupling balance precisions are very readily obtainable.

(*b*). For this condition (16) may be expressed giving the required shield precision $\pm q_6$ and $\pm p_6$ in terms of desired bridge precision $\pm q_1$ and $\pm p_1$ for specific values of q_4, p_4, D_r, and D_i (since in this case no coupling balance is made and q_4 and p_4 depend on the bridge construction or operating conditions). This gives

$$\pm q_6=\pm\frac{q_1(q_4D_r-p_4D_i)-p_1(p_4D_r+q_4D_i)}{(q_4D_r-p_4D_i)^2+(p_4D_r+q_4D_i)^2} \tag{36a}$$

$$\pm p_6=\pm\frac{p_1(q_4D_r-p_4D_i)-q_1(p_4D_r+q_4D_i)}{(q_4D_r-p_4D_i)^2+(p_4D_r+q_4D_i)^2} \tag{36b}$$

In (36*a*) and (36*b*), q_4, p_4, D_r, and D_i must have their proper algebraic sign.

Representative values from (36*a*) and (36*b*) are given in table III.

Thus (36*a*), (36*b*), and table III show that for the limiting practical condition the required shield balance precision ($\pm q_6$ and $\pm p_6$) may approximate either of the desired bridge balance precision ($\pm q_1$ and $\pm p_1$) depending upon the relative magnitudes of the D_r, D_i, q_4, and p_4.

V. Advantages to Be Gained From the Use of Three Balances and a Generalized Bridge Network

It is not to be inferred from the foregoing that precise measurements cannot be obtained with bridges not utilizing more than two balances. However it is maintained that the use of three balances and a generalized bridge network

(*a*). Permits more rapid balancing since the dependency of the balances is maintained at a minimum as the circuits are progressively balanced to a higher precision in the balancing operation.

(*b*). Permits simplified shield circuits in that less precise balances are required when the coupling circuit is balanced.

(*c*). Permits greater flexibility in design of bridges, reference capacitors, and measuring cells in that complete control of all inherent impedances is obtained.

(*d*). Is definitely required for certain types of bridges, namely, the detector-symmetrical

Table III

D_r	D_i	q_4	p_4	$\pm q_6$	$\pm p_6$
1	0	± 1	0	$\pm q_1$	$\pm p_1$
0	± 1	0	± 1	$\pm q_1$	$\pm p_1$
± 1	0	0	± 1	$\pm p_1$	$\pm q_1$
0	± 1	± 1	0	$\pm p_1$	$\pm q_1$
1	1	1	1	$\pm 0.5p_1$	$\pm 0.5q_1$
± 0.01	0	± 0.5	0	$\pm 0.2(10^3)q_1$	$\pm 0.2(10^3)p_1$
0	± 0.01	0	± 0.5	$\pm 0.2(10^3)q_1$	$\pm 0.2(10^3)p_1$
± 0.01	0	0	± 0.5	$\pm 0.2(10^3)p_1$	$\pm 0.2(10^3)q_1$
0	± 0.01	± 0.5	0	$\pm 0.2(10^3)p_1$	$\pm 0.2(10^2)q_1$

See notes at bottom of table II.

bridge utilizing measuring cells having low capacitances.

(*e*). Prevents difficulties with bridge measurements due to a change in nature of the inherent impedances. For example with the source-symmetrical bridge, high humidity may sufficiently change the real components of the coupling impedances so that there is difficulty in balancing and that sufficiently precise measurements cannot be obtained.

VI. Relative Bridge Sensitivity

It is of interest to determine the relative bridge sensitivity with the bridge operated with detector symmetry as compared to source symmetry. For the bridge considered by itself (for the present neglecting shield and coupling circuits) the voltage across the detector E_{9s} with applied bridge voltage E for the condition of source symmetry is (impedance designations, figure 1*a*, bridge circuits figure 2*b*)

$$E_{9s}=\frac{(Z_2Z_7-Z_1Z_8)Z_9E}{Z_1Z_2(Z_7+Z_8+Z_9)+Z_7Z_8(Z_1+Z_2+Z_9)+Z_9(Z_2Z_7+Z_1Z_8)} \tag{37}$$

and E_{9s} is zero for $Z_2Z_7=Z_1Z_8$. The magnitude of this voltage for a given per unit capacitance or power-factor unbalance of the bridge may be determined by considering the bridge unbalanced by ΔZ_1 in Z_1. Then

$$Z_2Z_7-Z_1Z_8-\Delta Z_1Z_8=0 \tag{38}$$

$$\Delta Z_1Z_8=Z_2Z_7-Z_1Z_8 \tag{39}$$

the latter may be written

$$Z_2Z_7-Z_1Z_8=\frac{\Delta Z_1}{Z_1}Z_1Z_8=(q_1+jp_1)(Z_1Z_8) \tag{40}$$

Substituting (40) in (37) will give E_{9s} for given values of per unit or absolute power factor change p_1 and per unit capacitance factor change q_1 of impedance Z_1.

Now under similar conditions with the bridge having detector symmetry the value of E_{9d} may be obtained from (37) by interchanging impedances Z_1 and Z_8 (then impedance designation is that of figure 1*a* with impedances Z_1 and Z_8 interchanged, bridge circuit is that of figure 2*a*). This gives

$$E_{9d}=\frac{(Z_2Z_7-Z_1Z_8)Z_9E}{Z_1Z_2(Z_7+Z_8)+Z_7Z_8(Z_1+Z_2)+Z_9(Z_2Z_7+Z_1Z_8)+Z_9(Z_1Z_7+Z_2Z_8)} \tag{41}$$

and E_{9d} may be expressed in terms of per-unit p_1 or q_1 unbalances in Z_1, by substituting (40) in (41).

For equal per-unit unbalances in Z_1, the relative bridge sensitivity of the bridge with detector symmetry as compared to the case with source symmetry may be obtained by taking the ratio of (41) to (37) with equal numerators for each equation. This gives

$$\frac{E_{9d}}{E_{9s}}=\frac{Z_1Z_2(Z_7+Z_8+Z_9)+Z_7Z_8(Z_1+Z_2+Z_9)+Z_9(Z_2Z_7+Z_1Z_8)}{Z_1Z_2(Z_7+Z_8)+Z_7Z_8(Z_1+Z_2)+Z_9(Z_2Z_7+Z_1Z_8)+Z_9(Z_1Z_7+Z_2Z_8)} \tag{42}$$

Two cases are of particular interest; (*a*) the impedance of the oil cell Z_1, the impedance of the reference capacitor Z_2, and the input impedance of the detector Z_9 are of the same order of magnitude and large as compared to the measuring arms Z_7 and Z_8, and (*b*) same as for (*a*) except Z_9 is the order of magnitude of Z_7 and Z_8.

For (*a*),

$$\frac{E_{9d}}{E_{9s}}\cong\frac{Z_9}{Z_7+Z_8} \tag{43}$$

For (*b*),

$$\frac{E_{9d}}{E_{9s}}\cong\frac{Z_7+Z_8+Z_9}{Z_7+Z_8} \tag{44}$$

Thus for these conditions the detector-symmetrical bridge will give greater bridge sensitivity than the source-symmetrical bridge. The effect of the shield and coupling circuits on bridge sensitivity may be approximately determined by considering the detector input impedance Z_9' to be

$$Z_9'=Z_9\frac{(Z_4+Z_5)}{Z_9+Z_4+Z_5} \tag{45}$$

This indicates that for maximum inherent bridge sensitivity it is desirable to keep the coupling impedances of the bridge as large as possible.

VII. Conclusions

1. The balancing of any general five-terminal network is a function of three dependent balances, namely bridge, shield, and coupling.

2. The per-unit complex precision of the bridge balance of a five-terminal bridge network is equal to the product of the complex precisions of the coupling and shield balances and a dependency factor.

3. The dependency factor is in general complex and may vary between wide limits depending upon the circuit arrangement, magnitudes, and angles of the circuit impedances and frequency.

4. The use of three balances in the bridge balancing is a practical necessity under certain conditions of frequency, type of bridge, and impedances in the arms of the bridge network.

5. The use of three balances permits obtaining the minimum required precision of the coupling and shield balances to give the desired precision in the bridge balance, and therefore permits the greatest simplification and minimum range for the extraneous bridge arms.

6. The use of three balances permits the minimum time required for the bridge balancing operation in that the dependency of the balances is maintained at a minimum, thus giving the smallest number of individual balancing operations to satisfy simultaneously all the variables involved.

7. Bridge networks should be designed with the minimum number of required terminals to give the minimum number of dependent balances.

8. The theory developed is applicable to any five-terminal bridge network and is independent of the nature of the arms constituting the network.

9. When three balances are used the precision of the bridge balance can be made independent of the nature of the inherent impedances in the bridge network, the only requirement being that the inherent impedances have sufficient magnitude to separate the balances.

10. The theory developed is applicable to a shielded four terminal bridge network for measuring two terminal capacitors.

11. The shield and coupling balances are fundamentally different in that in the shield balance the shield and guard and the bridge detector terminals are balanced to the same potential whereas in the coupling balance the shield and guard and the bridge detector terminals must be at different potentials.

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Discussion

For discussion, see page 1105.

The "Speedomax" Power-Level Recorder

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POWER-LEVEL and attenuation measurements are increasing in importance and a larger number of these measurements are being made each day. They are of particular importance in the radio and telephone industries where it is essential to know the power-level variations in electrical circuits and acoustic devices, the gain of amplifiers, and the attenuation of filters or communication lines at various frequencies.

Since both power-level and attenuation are measured in decibels, methods for measuring them are similar. Manual methods of obtaining these measurements have been fairly well standardized but, while they have proved to be satisfactory for some laboratory experiments, they are not applicable for many production tests. Even in some laboratory cases the time allowable for making the measurements does not permit manual methods to be used and automatic recording is necessary.

An automatic recorder for recording power level or attenuation should faithfully record rapid or slow variations in power level with time. It should be independent of frequency variations within the range of interest and record only changes in power level. In general, the faster the recorder, the more faithful the recording, provided it does not overshoot. The recorder should not move in steps, but it should draw a smooth curve on rectangular co-ordinate paper in order to eliminate the need for interpolation at any point on the record.

As a result of the need for an automatic recording device of this type, the "Speedomax" power-level recorder was developed.

In the standard manual method of making precise power-level or attenuation measurements, the instrument under test is supplied with power of the desired frequency or frequencies and its output terminals are connected to the input terminals of an attenuator which is manually adjusted so that the deflection of an indicating device across the output terminals of the attenuator remains constant for variations in input power. This indicating device, which usually consists of an a-c amplifier, a rectifier, and a meter, operates at a constant power level. Its requirements, therefore, are simple, and it is comparatively easy to design. Since precision attenuators are available, this method permits measurements to be made over wide decibel ranges with very accurate results. It eliminates two difficult problems, the designing of an a-c amplifier having reproducible characteristics over a large range of input voltage and the designing of a dynamic element with a logarithmic response.

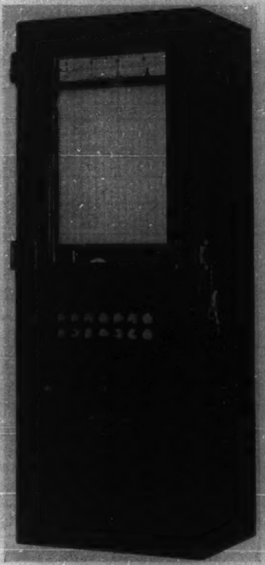

Figure 1 (above). The Speedomax power-level recorder (closed view)

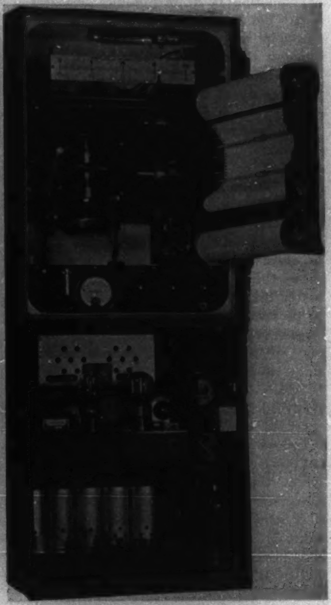

Figure 2 (right). The Speedomax power-level recorder (open view)

The basic idea of the Speedomax power-level recorder was that an automatic method should be used to make power-level and attenuation measurements that would be similar to the standard manual method. The problem was to devise an electrical circuit that would operate a recording mechanism to do this. The electrical circuit must be insensitive to frequency variations and only respond to changes in input power. The recording mechanism is similar to that of the high-speed recorder described in an article by A. J. Williams, Jr., in the 1938 AIEE TRANSACTIONS[1] and which is used to record small direct voltages.

The first instrument was completed in 1935, but the recorder described in this

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This paper was presented to the faculty of the Moore School of Electrical Engineering of the University of Pennsylvania in partial fulfillment of the requirements for the degree of doctor of science in electrical engineering.

The author appreciates the suggestions of A. J. Williams, Jr., of the Leeds and Northrup Company in developing this instrument.

[1] For all numbered references, see list at end of paper.

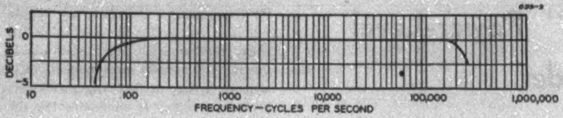


Figure 3. Frequency characteristic of the recorder

paper is a later model which has an extended frequency range (figures 1 and 2). The scale is linear in decibels (logarithmic voltage scale). It has a range of 40 decibels above a zero level of 0.01 milliwatt with an input impedance of 135 ohms. It is designed to operate between 150 and 150,000 cycles (figure 3) and over this frequency range the recorder is accurate to one-half per cent of full scale (0.2 decibel). The speed of response of the recorder is such that the pen will traverse any part of the ten-inch chart and arrive at complete balance without overshooting in approximately one second. The paper is driven by a synchronous motor at speeds of either six inches a minute or twelve inches an hour. The recorder draws a smooth curve and operates direct from a 115-volt 60-cycle supply. No batteries are used.

A sample record made by the recorder is shown in figure 4. It was obtained by applying a 1,000-cycle voltage to the input terminals of a decade attenuator which was connected to the power-level recorder. By changing the settings of the attenuator the recorder pen was made to move across the scale. This record shows the accuracy of the recorder to be better than one-half per cent, the speed to be approximately one second for the largest unbalance, the dead range to be very small, and that the recorder draws a smooth curve.

General Description

In the operation of the recorder an unknown alternating voltage is impressed across the input terminals of a continuously adjustable ladder-type slide-wire attenuator (see the block diagram, figure 5). The output of the attenuator is amplified by an a-c amplifier and rectified by the detector tube. The d-c output voltage of the detector is amplified by the d-c amplifier. The output voltage of the d-c amplifier is introduced into the converter where it is opposed by a standard direct voltage. The resultant of the two voltages is impressed across the grids of the double-triode vacuum tube of the converter. If the two voltages do not balance each other, the resultant voltage will operate one of the triode units of the converter, and the direction of unbalance will determine which unit operates. Each triode unit of the converter controls a thyratron, which in turn controls the direction of rotation of the slide-wire motor. Consequently, when one-half of the double triode operates one thyratron is turned on, the motor revolves and moves the slide-wire attenuator and the pen which are mechanically coupled to it. When the alternating voltage applied to the input terminals of the recorder changes, the motor revolves and moves the attenuator to a position that will restore the original voltage level of the a-c amplifier and balance the recorder without overshooting. Since it is desirable to avoid the use of batteries in this instrument, the constant voltages necessary for operation of the amplifiers and detectors are obtained from a regulated power supply.

In this recorder the balance point is not approached at constant speed, but the speed of rebalancing increases in proportion to the amount of unbalance. Therefore, large changes in power levels

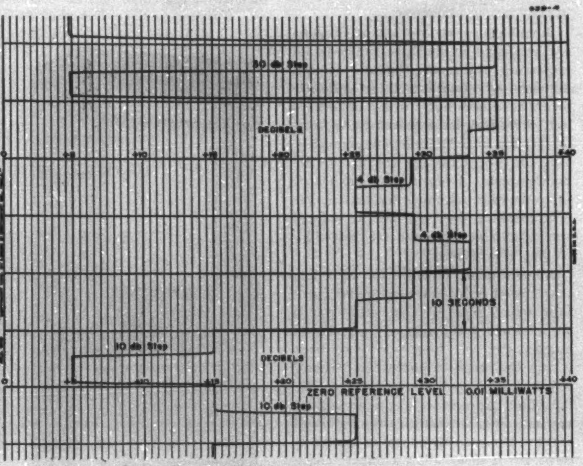


Figure 4. Chart made by recorder

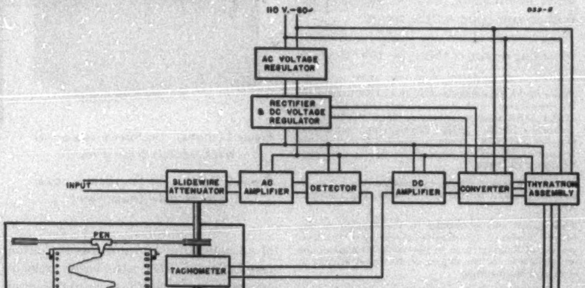


Figure 5. Block diagram of recorder

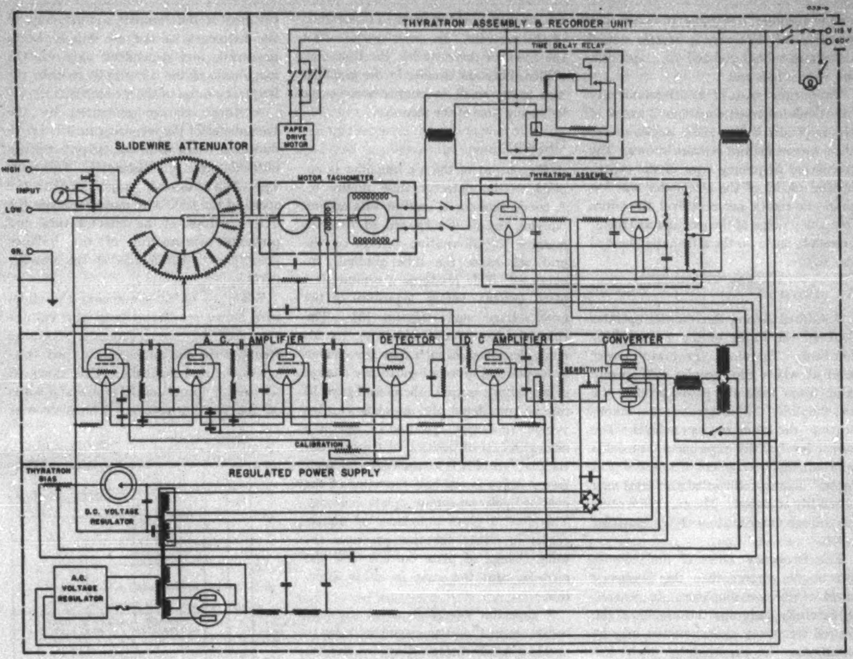


Figure 6. Schematic wiring diagram of the recorder

will be recorded approximately as fast as small changes.

Detailed Description of Component Parts

Slide-Wire Attenuator

. The decibel range and the characteristic impedance of the recorder are determined by the slide-wire attenuator. This type of attenuator is preferable to a step-by-step attenuator because its attenuation is continuously adjustable and, therefore, enables more accurate measurements to be obtained. The attenuator in this recorder has a range of 40 decibels with a characteristic impedance of 135 ohms. Slide-wire attenuators have been constructed with other decibel ranges, and with various other characteristic impedances.

The accuracy of the recorder depends on the linearity and stability of this attenuator. The linear decibel scale is obtained by using a ladder-type attenuator which is a continuously variable unbalanced π-section slide-wire attenuator (figures 6 and 7). The output of the attenuator operates into the grid circuit of a vacuum tube, which, because of its high impedance, introduces no attenuation error. Since the moving contacts of the attenuator are in this high-impedance circuit, they cause no trouble in the operation of the recorder.

The series coils are in the form of a uniform tapped slide-wire which is divided into equally spaced sections having the same resistance per section. A shunt coil is connected to each tap on the slide-wire.

In designing a slide-wire attenuator, which has a specified attenuation, the number of sections is determined by the allowable attenuation error and is independent of the characteristic impedance (see the development in the appendix). The values of the attenuation in decibels at successive shunt coils lie on a curve which is linear, but the variation in the attenuation between shunt coils is not linear (figure 8). This error, which can be made negligible by using a sufficient number of sections, can be readily calculated as is shown in the appendix. In the graphs of figure 9, which were obtained from equations 9 and 10 of the appendix, the location of the point in each attenuator section at which the attenuation error is greatest (n_{max}) and the value of this error (D_{max}) are plotted against sections per decibel (S/A). These graphs show that for values of S/A greater than $^1/_4$ the maximum error is small and occurs very close to the mid-point of each section. In this particular recorder, the range was 40 decibels and by using a 15-section slide-wire attenuator, it was made linear in decibels to one-eighth per cent (see figure 8).

These graphs apply to all unbalanced slide-wire attenuators and if the allowable error and the total attenuation is known, then the number of sections can readily be obtained. The slide-wire resistance and the shunt coils can then be calculated, to obtain any characteristic impedance.

The slide-wire attenuation must be constant over the entire frequency range of the recorder. In order to obtain this feature, the slide-wire is reverse wound on a hard-rubber rod and the shunt coils are woven resistors.

Frequency errors of the attenuator may be calculated from equations 3 and 6 of the appendix if the phase angles of the slide-wire and shunt coils are known. The calculated frequency error of the experimental model of the attenuator was less than one-eighth per cent over the entire frequency range of the recorder and measurements made on the attenuator checked this figure.

A-C AMPLIFIER

As stated before, the recorder operates to keep the input of the a-c amplifier constant. Therefore, the lowest power level at which the recorder will operate is no lower than the power level of the a-c amplifier. The greater the amplification, the lower the power level. The lowest level of the experimental model is approximately ten microwatts and a gain control is provided so that this level may be readily adjusted. The maximum overall voltage amplification of the amplifier is 300.

The frequency range of the recorder can be no greater than the frequency range of the a-c amplifier. In general, by carefully designing this amplifier, the desired frequency characteristics may be obtained. The experimental model has a three-stage resistance-capacitance coupled amplifier (figure 6) which uses screen-grid tubes. By reducing unwanted feedback to a negligible amount and using broadly tuned circuits in addition to the resistance-capacitance coupling, the frequency characteristic of the a-c amplifier is made flat to within 0.1 decibel from 150 cycles to 150 kilocycles (figure 3).

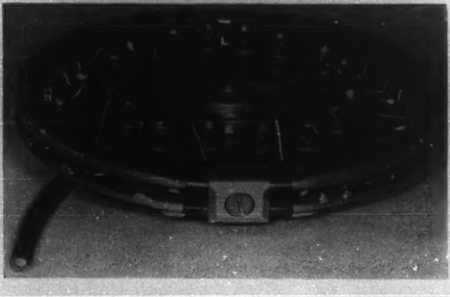


Figure 7. The slide-wire attenuator

Precautions are taken to keep any stray alternating voltages out of the a-c amplifier, as they cause an error in the reading of the recorder. In order not to affect the speed of the recorder, the time constants of certain circuits in the amplifier had to be small in comparison to the balancing time of the recorder.

DETECTOR

The output of the a-c amplifier is applied to the detector tube (figure 6). A triode detector is used and its accompanying circuit is adjusted so that increasing the alternating voltage on the grid will cause the tube gradually to overload. This produces a symmetrical plate current versus logarithm of the grid voltage curve (figure 10). The characteristic of the slide-wire, as already described, is such that any change in its position gives a logarithmic change in its voltage output, therefore figure 10 can be considered also as plate current versus slide-wire position. For the normal balanced position of the recorder the plate current is at the center of symmetry which is one-half the value of the overload or maximum plate current. Therefore, a given unbalance of the recorder in either direction produces the same change of plate current, and the recorder runs the same in either direction.

A capacitor is placed across the load resistance in the plate circuit in order to obtain a steady direct voltage by smoothing out the pulsating direct plate current flowing in the resistor. The capacity of this capacitor is limited to a value which makes the time constant of this resistance-capacitor combination low enough not to affect the speed of the recorder. The low value of this time constant affects the response of the recorder to frequencies lower than 100 cycles.

Converting the alternating voltage of the a-c amplifier to a direct voltage makes the rest of the recorder independent of the frequency of the a-c supply being measured, and dependent only on the magnitude of the a-c supply (within the frequency range of the a-c amplifier).

A direct voltage generated by the tachometer of the recording unit is introduced in series with the output voltage of the detector tube (figure 6). This voltage which is directly proportional to the speed of the slide-wire motor opposes the output voltage of the detector tube and prevents overshooting of the balance point due to the inertia of the moving parts.

When an unbalance occurs the slide-wire motor speeds up until the voltage generated by the tachometer becomes greater than the unbalance output voltage of the detector tube. This shuts off the driving torque on the motor and turns on the braking torque. The slide-wire motor slows down until the tachometer voltage becomes less than the unbalance output voltage of the detector tube, then the driving torque is again applied. This process is repeated until the recorder is brought to the balanced position which it attains without overshooting. During the balancing period the torque on the motor reverses, but the direction of rotation does not change.[1]

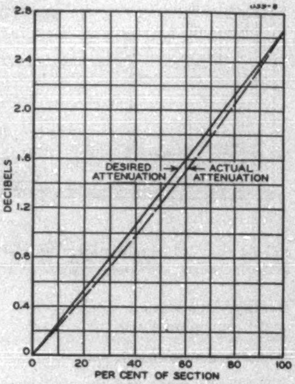


Figure 8. Accuracy graph of an attenuator section

The unbalanced voltage applied to the d-c amplifier is, therefore, alternately positive and negative during the balancing cycle, but is zero when the recorder is balanced.

For a given per cent unbalance of the

recorder, the magnitude of the d-c output voltage of the detector is greater for a large decibel range than for a small one. The magnitude of the braking voltage generated by the tachometer should approach the magnitude of the d-c output voltage of the detector. Too much tachometer voltage will make the recorder slow, while not enough voltage will allow the recorder to overshoot. If the speed of the recorder is to be independent of the decibel range, then the amount of tachometer voltage necessary to prevent overshooting will depend on the decibel range of the recorder; the larger the range, the larger the voltage required.

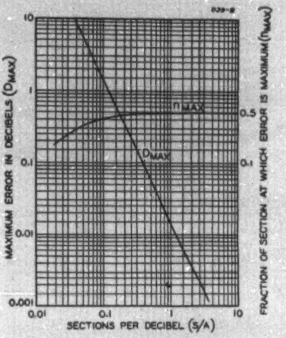


Figure 9. Maximum attenuation error and its location versus sections per decibel

The kinetic energy of the moving system is proportional to the square of the speed, but the energy which can be absorbed by the braking of the motor is proportional directly to the remaining distance to the balance point. In order to obtain maximum recorder speed without overshooting, these two energies should be equal throughout the entire decelerating period. If this is to be the case, then either the damping voltage of the tachometer should be proportional to the square of the speed or else the output voltage of the detector tube should be proportional to the square root of the unbalance.[1]

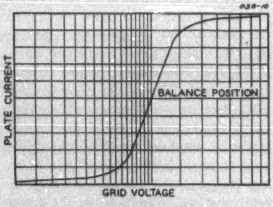


Figure 10. Plate current versus grid voltage curve of the detector

In this recorder the tachometer voltage is a linear function of the speed, but each half of the detector plate current versus logarithm of the grid voltage curve (figure 10) besides being symmetrical is approximately a square-root relationship. Thus the above requirement is satisfactorily fulfilled and the greatest speed is obtained.

D-C Amplifier

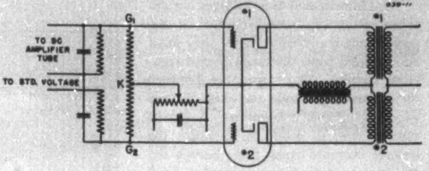
In order to obtain the desired sensitivity for the recorder, it is necessary to have a d-c amplifier since, for a small unbalance of the recorder, the voltage swing across the load resistance of the detector tube is not of sufficient magnitude to operate the converter. A single-stage screen-grid amplifier (figure 6) which has a voltage amplification of five is sufficient for recorder ranges as low as ten decibels. Lower ranges require additional amplification. A capacitor is placed across the load resistance in the plate circuit in order to smooth out any pulses which may appear in the voltage across this resistance. The time constant of this resistance-capacitor combination must also be kept low.

The Converter

The next problem is to utilize this d-c output voltage to operate the thyratrons and drive the slide-wire motor. It is not a simple one, because the d-c output of the d-c amplifier is 90 volts above ground, while the thyratrons are at ground potential. Insulating transformers must be used, but in order to use insulating transformers, the output voltage of the d-c amplifier must be transformed into an alternating voltage or used to control an alternating voltage. The alternating voltage must operate one thyratron when the output of the d-c amplifier increases, and must operate the other thyratron when the output of the d-c amplifier decreases. The converter does all of this.

The output voltage of the d-c amplifier goes to the converter (figures 6 and 11) where it is opposed by a standard direct voltage obtained from the regulated power supply. The difference between these two voltages is applied across the two resistances in the grid circuits of a double triode.

Figure 11. The converter



The grids of the double triode are biased so that when the two voltages are balanced neither section of the double triode will pass plate current.

Each plate of the double triode is connected through its own plate transformer to the same 60-cycle voltage supply. Therefore, the alternating voltage between the plate and cathode of each triode section is of the same magnitude and phase.

Referring to figure 11, if the output voltage of the d-c amplifier becomes less than the standard voltage, then G_1 will become positive with respect to K and triode number 1 will pass plate current, but since G_2 becomes negative with respect to K, triode number 2 will not pass plate current. If the output voltage of the d-c amplifier becomes greater than the standard voltage, then G_2 will become positive with respect to K, and triode number 2 will pass plate current, but since G_1 becomes negative with respect to K, triode number 1 will not pass plate current. When either triode passes plate current, a half-wave 60-cycle current flows through the primary of its plate transformer and produces a peak voltage wave across the transformer secondary. This voltage wave is, of course, the derivative of the current wave (figure 12) and is positive only one-fourth of a cycle. This is a desirable feature of the circuit, since the phasing between this voltage, which is applied to the grid of the thyratron, and the 60-cycle sine-wave plate voltage does not have to be adjusted as accurately as is generally necessary when sine-wave voltages are used on the grid as well as on the plate of the thyratron. It also insures that the thyratron will be turned on at the beginning of the positive half of its plate voltage cycle, and thus obtains maximum power output for each cycle during operation.

Since only one section of the double triode can operate at one time, only one thyratron can be turned on at one time. In this way the power, which drives the slide-wire motor, is controlled.

The filament and bias voltages are obtained from separate windings on the power-pack transformer, since they must be insulated from the main filtered regulated supply. The impedance between these windings and the high-voltage windings of this transformer should be considerable, in order to minimize the leakage currents which flow through the grid resistances of the converter tube. In order further to reduce the effect of those currents which get into the grid resistances of the tube, the impedance from G_2 to ground was made the same as the impedance from G_1 to ground (figure 11). Now the leakage currents divide equally and their resulting effect has been reduced to zero.

Up to this point in the recorder the power supply has been obtained from regulated sources. The plate voltage for the converter tube is, however, not regulated, but it is obtained from the same source as that from which the plate voltage for the thyratrons is obtained. This insures correct phasing between the voltage applied to the thyratron grids and that used on their plates. Normal line-voltage variations at this point in the recorder will cause no error in its operation.

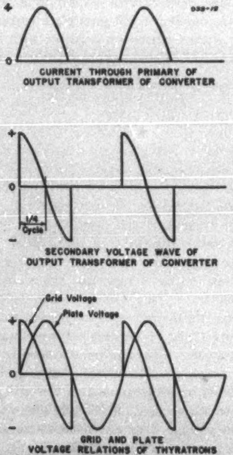

039-12
+
0
CURRENT THROUGH PRIMARY OF OUTPUT TRANSFORMER OF CONVERTER
+
0
1/4 Cycle
−
SECONDARY VOLTAGE WAVE OF OUTPUT TRANSFORMER OF CONVERTER
Grid Voltage
Plate Voltage
+
0
−
GRID AND PLATE VOLTAGE RELATIONS OF THYRATRONS

Figure 12. Wave shapes in converter and thyratron assembly circuits

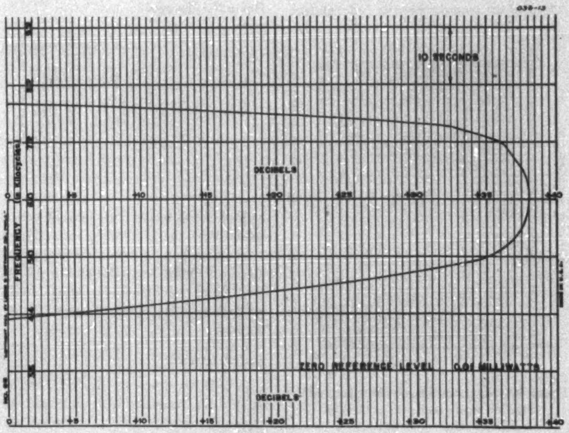


Figure 13. Frequency characteristic of a band-pass filter

Thyratron Assembly and Recording Unit

The output of the converter, as stated before, is applied to the grids of the thyratrons (figure 6) which drive the three-lead split-field series reversing motor that is mechanically coupled to the slide-wire attenuator, the tachometer, and the pen.[1] The thyratrons in this recorder perform a control function and as in all forms of control, there is a tendency to hunt. This can be eliminated by temporarily reducing the sensitivity of the control as soon as it starts operating. The sensitivity is temporarily reduced in this instrument by raising the cathode potential of the thyratrons every time they operate, and by allowing them to return to normal only gradually over a period of several cycles. The raising of the cathode potential is equivalent to depressing the grid potential and gives less sensitivity. In the common plate lead is a resistance which develops a drop of several volts upon the operation of either thyratron. This voltage drop charges a capacitor through copper-oxide rectifiers. After the thyratrons are shut off and the plate current goes to zero, the rectifiers discharge the capacitor but only gradually and in the meantime the cathodes are held more or less up from their normal zero value as is desired.

The record is made by a pen with ink on a chart ten inches wide and as long as desirable (up to 40 yards). A 12-inch length of chart is exposed. There are two paper speeds in the experimental model, 6 inches per minute or 12 inches per hour. Other paper speeds as high as $^3/_4$ inch per second have been used.

Power Supply

The power supply for the recorder, exclusive of the plate voltages for the converter and the thyratron assembly, must be very well regulated (figure 6). This is particularly true of the power supply for the detector, the d-c amplifier, and the standard voltage supply, since any variation in their power supply enters directly into the operation of the recorder and will affect its performance. Somewhat greater variations in the power supply of the a-c amplifier would be permissible since these variations do not greatly affect its gain, which is all that need be held constant. Variations in the power supply of the converter will affect only the sensitivity of the recorder.

In order to obtain a power supply which was good enough for satisfactory operation of the recorder, two separate voltage regulators were used. One regulator was of the magnetic type and operated on alternating current. It regulated the 60-cycle power supplied to the power pack-transformer. The other was a gaseous tube regulator which regulated the d-c output of the power pack. The d-c output controlled by these two regulators was constant to within 0.1 per cent for a period of 12 hours during which time the 60-cycle supply varied 10 per cent. Calculations and tests indicated that for a recorder accuracy of $^1/_2$ per cent the d-c power supply should be constant to 0.2 per cent, so the regulation obtained by this method is sufficient.

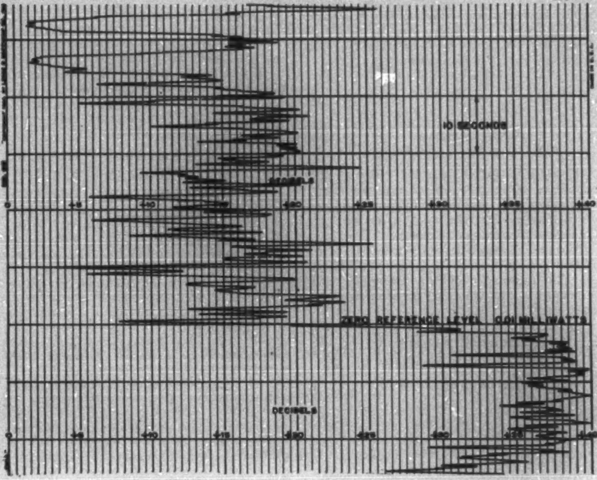


Figure 14. The fluctuating power level of a radio program

Ease of Operation

While the recorder itself is somewhat complicated, the operation of it is very simple. There are only three adjustments to be made, the "thyratron bias", the "sensitivity", and the "calibration" of the recorder (figure 6). These adjustments do not have to be made every time the recorder is used, but they should be checked occasionally.

In the calibration of the recorder, a high-resistance vacuum thermocouple voltmeter is connected across the input terminals (see figure 6). After the input voltage to the recorder has been adjusted to a definite amount as indicated on the voltmeter, the recorder is adjusted to indicate the corresponding power level by means of the "calibration" dial, which controls the gain of the a-c amplifier.

Uses of the Recorder

The uses of this recorder are very varied as can be judged from the following abbreviated list of applications. It may be used for obtaining:

1. Frequency-attenuation characteristics of filters. An example of this is shown in figure 13 which is the characteristic of a band-pass filter.
2. Frequency-attenuation characteristics of an entire radio set.
3. Frequency-attenuation characteristics of voice channels.
4. Frequency-attenuation characteristics of carrier channels.
5. Frequency-attenuation characteristics of transformers, loud speakers, amplifiers, etc.
6. Measurements of noise levels.
7. Power-level measurements of voice levels occurring in the output of a phonograph record, a radio program, etc. (figure 14).

Since the recorder slide-wire and pen are driven by a fair-size motor, control disks may be mounted on the slide-wire shaft and used for signaling or control purposes (that is, introducing additional attenuation into a filter or a channel to keep the total attenuation or power output within restricted limits).

The slide-wire motor may be used to drive the transmitter of a remote indicator or recorder and thus obtain two records at widely separated points.

Appendix

The Slide-Wire Attenuator

The equations developed here are useful in designing slide-wire attenuators.

If the total attenuation, the characteristic impedance, and the desired accuracy of the attenuator are known, the values of the shunt and series coils can be readily computed. Referring to figures 15*A* and 15*B*, let:

A = total attenuation in decibels
S = number of sections
Z_k = characteristic impedance of the attenuator
Z_b = impedance of each series coil
Z_a = impedance of first shunt coil
Z_t = terminating impedance
E_o = voltage output of one section
E_o' = voltage output along section
E_i = voltage input
n = fraction of a section; ratio of distance between adjacent shunt coil tap and contact to distance between shunt coils. Distances are measured in the output-input direction
n_{max} = value of n for which the attenuation error is maximum
D = attenuation error in decibels
D_{max} = maximum attenuation error in decibels

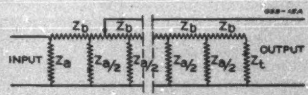


Figure 15A. Schematic diagram of the slide-wire attenuator

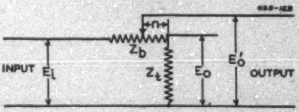


Figure 15B. One section of the slide-wire attenuator

Now the attenuation per section of a slide-wire attenuator is,[3]

$$-\frac{A}{S}=20\log\frac{E_o}{E_i} \tag{1}$$

(all logarithms in this paper are to the base 10).

But

$$\frac{E_o}{E_i}=\frac{Z_t}{Z_t+Z_b}$$

where

$$Z_t=\frac{Z_aZ_k}{Z_a+Z_k} \tag{2}$$

The characteristic impedance of a symmetrical π section attenuator is

$$Z_k=\sqrt{\frac{Z_a^2Z_b}{2Z_a+Z_b}} \tag{3}$$

Hence

$$Z_b = \frac{2Z_a Z_k^2}{Z_a^2 - Z_k^2} \tag{4}$$

therefore

$$\frac{E_o}{E_i} = \frac{Z_a - Z_k}{Z_a + Z_k} \tag{5}$$

and the attenuation per section can be written,

$$-\frac{A}{S} = 20 \log \frac{Z_a - Z_k}{Z_a + Z_k} \tag{6}$$

the total slide-wire resistance (R_s) is of course

$$R_s = SZ_b \tag{7}$$

If the impedances of the shunt and series coils of the attenuator are assumed to be pure resistances, their values may be obtained from equations 4 and 6 when the total attenuation, the characteristic impedance, and the number of sections are known. The first two are generally known, but the number of sections is not and must be determined from the accuracy requirements in accordance with the following method.

Since the values of the attenuation in decibels at successive shunt coils lie on a curve which is linear, but the variation in attenuation between shunt coils is not linear, the number of sections is determined by the allowable attenuation error of the attenuator. This error can be readily calculated and by using a sufficient number of sections, it can be made negligible.

Referring to figure 15*B*, the actual attenuation at a fractional position n is:

$$-\frac{A}{S}(\text{actual}) = 20 \log \frac{E_o'}{E_i} = 20 \log \left(\frac{Z_i + nZ_b}{Z_i + Z_b}\right)$$

$$= 20 \log \left(\frac{Z_a - Z_k + 2nZ_k}{Z_a + Z_k}\right)$$

(in terms of Z_a and Z_k)

but from (5)

$$Z_a = Z_k \left[\frac{1 + E_o/E_i}{1 - E_o/E_i}\right]$$

Which gives,

$$-\frac{A}{S}(\text{actual}) = 20 \log \left[n + (1-n)\frac{E_o}{E_i}\right]$$

or

$$-\frac{A}{S}(\text{actual}) = 20 \log \times \left[n + (1-n) \operatorname{antilog}\left(-\frac{A}{20S}\right)\right] \tag{8}$$

The desired attenuation is $(1-n)$ 20 log (E_o/E_i) or $-(1-n)A/S$.

The attenuation error in one section for a fractional position n is in decibels,

$$D = -(1-n)\frac{A}{S} - 20 \log \times \left[n + (1-n) \operatorname{antilog}\left(-\frac{A}{20S}\right)\right] \tag{9}$$

Therefore, the attenuation error of the slide-wire is independent of the characteristic impedance and dependent only on the decibel range and the number of sections (exclusive of frequency errors).

The point in each section of the slide-wire attenuator at which the error is greatest may be found by differentiating D with respect to n and equating the derivative to zero.

$$\frac{dD}{dn} = +\frac{A}{S} - \frac{20(\log \epsilon)\left[1 - \operatorname{antilog}\left(-\frac{A}{20S}\right)\right]}{n + (1-n) \operatorname{antilog}\left(-\frac{A}{20S}\right)\Big]}$$

hence

$$n_{\max} = +\frac{20S}{A} \log \epsilon - \frac{\operatorname{antilog}\left(-\frac{A}{20S}\right)}{1 - \operatorname{antilog}\left(-\frac{A}{20S}\right)} \tag{10}$$

When this value of $n_{\max}$ is substituted in equation 9, the maximum error ($D_{\max}$) is obtained.

The graphs of figure 9 show the location of the point in each attenuator section at which the attenuation error is greatest ($n_{\max}$) and the value of this error ($D_{\max}$) at every point in each section. If the value of S/A is greater than 1/4, the maximum error is small and occurs very close to the midpoint of each section.

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2. High-Frequency Alternating Currents (a book), McIlwain and Brainerd.

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Synchronizing Transients and Synchronizers for Large Machines

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Synopsis: This paper reports an investigation of the transients accompanying synchronizing operations performed under practical conditions which include some departures from the ideal conditions as to voltage, phase position, and frequency of the incoming machine. A typical long-distance transmission system is selected for the study of the transients arising from the synchronizing of one of several similar units connected to the same sending-end bus. This problem reduces to that of a three-machine stability problem which is carried out with the aid of the a-c network calculator. The body of the paper shows the variations in initial and maximum "swing" currents, in voltage, in frequency, and in angular relations which may be produced by synchronizing under unfavorable conditions. The results of this study are then examined from the standpoint of automatic synchronizers and criteria for defining their performances are developed. The latter part of the paper includes a description of a new electronic type of automatic synchronizer. The operating features of this synchronizer, which have been installed on the more recent units of the Boulder Dam installation, are discussed.

Synchronizing Transients

IT IS a curious fact that the phenomena encountered in synchronizing operations have received scant attention in the technical literature even though the phenomena have been encountered ever since two synchronous machines were first operated in parallel, and even though a large number of synchronizing operations are performed every day. Possibly the explanation is due to two facts; first, that the ideal conditions introduce no disturbance, and, second, that the synchronizing transients are usually not large but for analysis require consideration as a stability problem of considerable complexity. A review of the synchronizing problem has become desirable because of the development of new types of automatic synchronizers and because of the progress in the analysis of system transients in connection with system stability. This study includes only those synchronizing operations for which the voltage, phase, and frequency conditions are matched before the connection is made; it does not include self-synchronizing methods.

The early synchronizing operations were performed manually and the large majority of them continue to be accomplished in this way even to the present day. The exacting requirements for a perfect synchronizing operation were, of course, not met initially. The first apparatus used to facilitate synchronizing consisted of lamps operated from the differential voltages of the machines to be synchronized. Lamps could indicate only the approximate relative phase position and frequency, and were incapable of showing whether the speed of the incoming machine should be raised or lowered. The introduction of the synchroscope by P. M. Lincoln marked an important step in the development of synchronizing equipment. The performance varied widely from installation to installation, being dependent upon the skill of the operator and upon the apparatus characteristics which permit accurate matching of the frequencies and the phase positions.

It was early recognized that ideal conditions for synchronizing produced no disturbance but that unfavorable conditions could produce a more severe disturbance than a dead short circuit at the machine terminals, particularly if the incoming machine were connected in parallel on the same bus with other units of much larger aggregate capacity. A number of schemes were proposed to minimize the bad effects of imperfect synchronizing. Most of these required the introduction of reactance in the circuit between the incoming machine and the remainder of the system. This introduction of reactance was also favorable from the standpoint of reducing the duty on circuit breakers but unfavorable from the standpoint of maintaining the machines in synchronism at times of disturbances. For that reason some of the schemes contemplated the introduction of reactance into the circuit during the synchronizing operation but with its removal after the operation was completed. One method occasionally used for accomplishing this result was to synchronize through a remotely connected unloaded transmission line.

The trend toward automatic equipment in recent years has expedited the development of automatic synchronizers. The advantages to be obtained from automatic synchronizing are: (1) the reduction of stress in the incoming machine, (2) the reduction of disturbance to the system, (3) the elimination of a requirement for special skill on the part of the operator or avoidance of delay in performing the operation, and (4) the ability to make consistent repetitive operations. The first automatic synchronizer in America was developed in 1906.

The synchronizing problem became of increasing importance and complexity as long-distance transmission became more extensive. This was due in part to the increased size of the machines and in part to the reduced reactance, a feature introduced to improve the transient stability of the longer transmission installations. Developments in stability also introduced the need for faster circuit breakers for disconnecting the faults encountered on such systems and thus to minimize the transient disturbances resulting therefrom. This process of speeding up circuit breakers has carried with it the speeding up of circuit closing. The shorter and more definite time for circuit closing is, of course, advantageous from the standpoint of synchronizing operations, whether performed manually or with the automatic equipment. However, some changes in the adjustment of automatic synchronizers may be required to take full advantage of higher circuit-closing speeds. The Boulder Dam installation is remarkable for the size of the units and the relatively low reactances which are employed. As a consequence, it became desirable to develop a new synchronizer especially designed to meet these exacting requirements of low stress under synchronizing operations, and to take advantage of faster circuit-closing speeds. This development provided the occasion for an analytical review of synchronizing transients taking advantage of the advances which have been made in recent years in the understanding of stability and other electromechanical transients on power systems.

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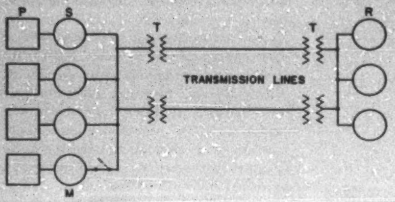


Figure 1. Schematic diagram illustrating a typical synchronizing problem on a transmission system

P—Prime movers
S—Sending-end generators
R—Receiving-end synchronous motors
M—Incoming machine
T—Transformers

Synchronizing conditions may be analyzed in terms of three factors, namely, voltage magnitude, phase relation, and frequency of the incoming machine in comparison with the corresponding system quantities. Imperfect synchronizing will produce currents which exert momentary torques in connected machines. These synchronizing currents may conveniently be classified as (1) "initial" currents which are based on subtransient reactances and the corresponding internal voltages, and (2) "swing" currents which are based on transient reactances and corresponding internal voltages and which do not include the very quickly decaying components. A broad picture of the synchronizing phenomena may be obtained by calculation of these currents for a variety of synchronizing conditions on a typical long-distance transmission system with low-reactance generators.

Typical System Selected for Analysis

A typical transmission system, illustrated in figure 1, was selected for the study of synchronizing transients. In this figure the machines can conveniently be considered in three groups as follows:

1. Generator group, consisting of three units at the sending end.
2. Motor group, consisting of three motors of the same capacity, at the receiving end.
3. The incoming machine.

The transmission system is assumed to be carrying a motor load equal to the rating of the three generators. The incoming machine is assumed to be identical in characteristics to the other generators but of one-third the capacity of the generator or motor group. The system is assumed to be without losses and the voltages are to be maintained at equal values on the low-voltage sides of the transformers at the sending and receiving ends. Each machine is assumed to have an inertia constant[1] of five kilowatt seconds per kilovolt-ampere. This represents a somewhat higher than normal value for hydroelectric units but an average value for turbogenerator units.

The generators and motors are each assumed to have 20 per cent transient reactance and 15 per cent subtransient reactance. The equivalent reactance diagram of the system shown schematically in figure 1 is given in figure 2 using transient reactances which are required for swing calculations. The equivalent star diagram for the calculations of the initial symmetrical subtransient currents would be obtained by reducing the generator and motor branches in figure 2*a* from 20 to 15 per cent respectively, and reducing the incoming machine branch from 60 to 45 per cent.

A vector voltage diagram for the initial conditions of operation, that is, just prior to synchronizing, is shown in figure 3. The voltage of the incoming machine is plotted in this figure for a condition in which the incoming machine voltage, E_M, leads by a small angle the voltage E_{TS} of the system at the point at which the machine will be connected. The voltage E_{TS} is used as the reference voltage for defining the phase of the incoming machine. Different synchronizing conditions with respect to the phase of the incoming machine voltages are shown diagrammatically by the locus of E_M in figure 3. The case shown is that in which the incoming machine voltage is exactly equal in magnitude to the system voltage. The reference voltage will normally have a phase position closer to that of the generators on the same bus than to that of the receiver machines. It will also be noted from figure 2 that the incoming machine can interchange very much larger amounts of power with one group of machines than with the other, the interchange being greater with the generators on the same bus.

The *initial symmetrical currents* caused by synchronizing depend upon the subtransient reactances and internal voltages of the system, and upon the relative phase position of the incoming machine. The convenient reference for that voltage is the voltage of the system at the point to which the incoming machine is to be connected. The calculated values for these instantaneous synchronizing currents are given in figure 4, for three values of incoming machine voltage equal to 92, 100, and 108 per cent of the system voltage at the point of connection. It will be noted that the initial currents are affected more by the phase position of the incoming machine than by the normal variations in the magnitude of incoming machine voltage. When synchronizing with zero difference in phase, the magnitude of the internal voltage is of some importance, and the currents are of zero magnitude only when the incoming voltage is exactly equal to the system voltage. For the range of

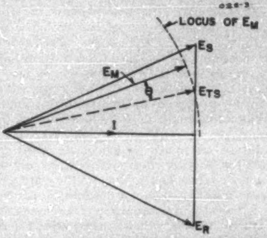
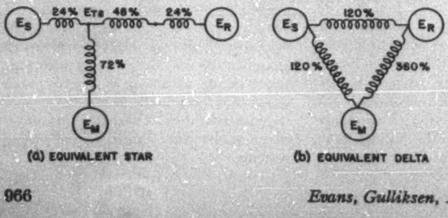


Figure 3. Vector diagram for transmission system of figure 1, assumed to be carrying rated load, prior to synchronizing

E_S—Internal voltage of sending machines
E_R—Internal voltage of receiving machines
E_M—Internal voltage of incoming machines
E_{TS}—Terminal voltage of sending machines
I—Line current
θ—Angle between E_M and E_{TS} before synchronizing



(a) EQUIVALENT STAR

(b) EQUIVALENT DELTA

Figure 2. Equivalent reactance diagram of the transmission system shown in figure 1. See also figure 3

Table I. Summary of Transient Conditions for Synchronizing the System of Figure 1

	Initial Conditions For Incoming Machine				
Item	Voltage (Per Cent)	Frequency (Per Cent)	Angle (Deg)	Sending-End Terminal Voltage Minimum (Per Cent)	Receiving-End Frequency, Maximum or Minimum (Per Cent)
1	100	100	+30	96	99.8
2	100	100	0	100	100.0
3	100	100	−30	92	100.16
4	100	101	+30	92	99.75
5	100	101	0	96.5	99.77
6	100	101	−30	95	100.03
7	100	99	+30	95	99.98
8	100	99	0	94	100.3
9	100	99	−30	92	100.4

conditions, there is relatively little difference between these currents for these three values of incoming machine voltage, the maximum difference being less than 40 per cent of rated current. For this reason it is considered adequate to study the influence of the other variables in the problem only for the conditions in which the incoming machine voltage equals the system voltage.

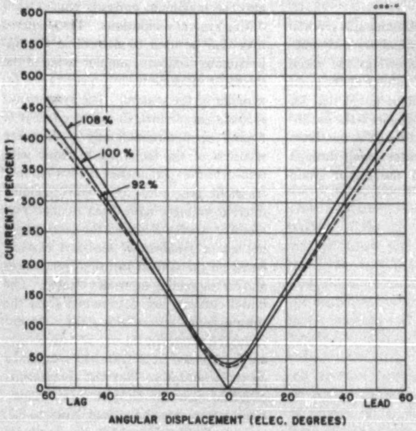


Figure 4. Instantaneous symmetrical synchronizing currents in the incoming machine, plotted for three ratios of incoming voltage to system voltage—based on subtransient reactances

Transient conditions may be more severe than the initial conditions in regard to torque and current stresses and thus it becomes necessary to examine "swing" curves to determine the maximum conditions. For example, for the condition of synchronizing with the voltage magnitude and phase position of the incoming machine equal to the corresponding system quantities there is no disturbance initially but there will be one at a subsequent time if the frequency of the incoming machine is different from that of the system. A greater disturbance will be experienced if the connection is made when a difference in phase also exists. In order to provide a general picture of the phenomena, it has been found convenient to analyze the transient conditions as outlined in table I, including three frequencies of the incoming machine of 101, 100, and 99 per cent of the system frequency and including three phase positions of 30 degrees leading, 0 degrees, and 30 degrees lagging. These deviations of one per cent in frequency and 30 degrees in phase position, while representing relatively large departures from the standpoint of proper synchronizing, do assist in giving a broad picture of the phenomena and do provide a basis for general curves passing through points representing small deviations. Leaving out item 2 of table I for the ideal synchronizing condition, there remain eight conditions which may be grouped in four pairs as follows: items 1 and 3, 4 and 9, 5 and 8, 6 and 7. For each pair of conditions the magnitudes of the transient disturbances are the same or substantially the same, but the initial directions of movement are opposite. This observation greatly simplifies the analysis of the many conditions to be examined. It is to be noted that the conditions outlined in table I do not include variation in voltage of the incoming machine with respect to system voltage. The incoming machine voltage has been taken as equal to the system voltage for the reasons previously discussed.

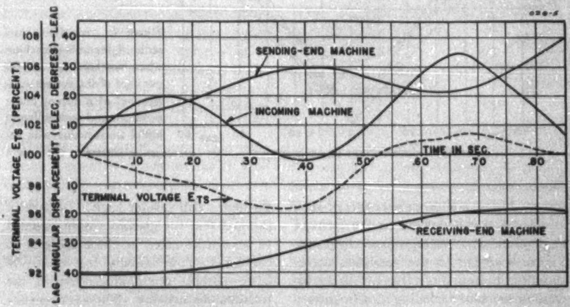


Figure 5. Time-angle relations for the system of figure 1 with the incoming machine synchronized at zero phase position and at 101 per cent frequency. The variations in the terminal voltage at the sending end are also shown

The preferred method of examining the transient disturbances due to imperfect synchronizing is the step-by-step method using the a-c network calculator as employed in the analysis of three-machine stability problems.[1] By this method the problem is considered as one in dynamic equilibrium and the position of each rotor is computed on the basis of its inertia and its mechanical and its electrical inputs or outputs, the electrical conditions being computed on the basis of constant internal voltages acting through known system reactances. By using the equivalent circuit of figure 2*b*, the three-machine stability problem may be analyzed as three problems each involving one reactance and two sources of electromotive force. Figure 5 shows the results of such step-by-step calculations in the form of angle-time and voltage-time diagrams. Figure 5 applies to the condition corresponding to item 5 of table I, with the incoming machine traveling at a frequency one per cent above that of the system but synchronized with the phase of the incoming-machine voltage identical with that of the system voltage at the point of connection. In this discussion of transients the phase position of synchronizing is given in terms of the angle by which the incoming-machine voltage leads or lags the initial position of the system voltage at the point of connection. It will be noted that for the condition shown in figure 5 the incoming machine swings through a relatively large angle, the generator group through a smaller angle, and the motor group through a still smaller angle. Because of the fact that the machines were assumed to be synchronized in phase there is no current to flow initially but relatively large currents are produced during the transient disturbance. These transient or "swing" currents may be estimated from the relative phase positions of the different machines as shown in figure 5 and the internal voltages and reactances of the system as discussed subsequently. It may be pointed out that if the synchronizing operation were performed with the incoming machine operating at 99 per cent of system frequency the transient disturbance would be similar except that the initial movement of all machines would be in directions opposite to those shown in figure 5, but the amplitudes would be substantially the same. If the incoming machine were synchronized out of phase but with the same differential frequency the oscillations would be of increased amplitude as shown in figure 6 which covers the conditions of synchronizing at 30 degrees lagging and a frequency of 99 per cent.

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[1] For all numbered references, see list at end of paper.

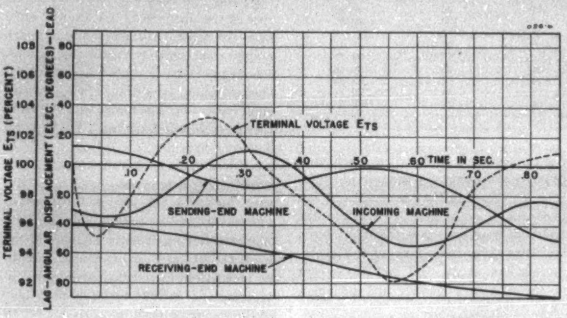


Figure 6. Time-angle relations for the system of figure 1 with the incoming machine synchronized at 30 degrees lagging and at 99 per cent frequency. The variations in the terminal voltage at the sending end are also shown

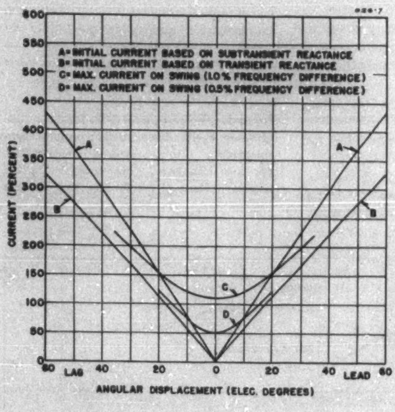


Figure 7. Maximum currents of incoming machine when synchronized at 100 per cent of the system voltage but at different phase positions and with different initial frequencies with respect to the system

A very useful diagram for studying transient conditions is shown in figure 7. This figure shows the initial currents and also the maximum currents encountered during system oscillations. These curves may also be used to determine the approximate maximum angular swing of the incoming machine with respect to the remainder of the system. The *symmetrical subtransient currents*, shown by curve *A* of figure 7, are determined only by the phase relations of the incoming machine with respect to the system since the system subtransient reactances and corresponding internal voltages have been defined previously. Curve *B* shows the corresponding set of *symmetrical transient currents* based on the system transient reactances and corresponding internal voltages. The initial currents are determined from the subtransient reactances; the current values encountered during system swings are determined from transient reactances on the assumption that the components of initial currents, subject to the short-time decrements, have had time to become negligible. These transient currents may, therefore, be related to the angular displacement curves if the internal voltages, are maintained constant by voltage regulators. The omission of voltage regulators would, however, not affect the phenomena in any important way. In the calculations the effects of losses have been neglected so that the system tends to oscillate equally in both directions about its mean point.

Consider now in detail the explanation of the use of figure 7 to determine the initial and maximum swing currents and angular displacements for different initial conditions of synchronizing. Consider first the case in which the system is synchronized at an angle of 30 degrees leading (or lagging). The initial symmetrical subtransient current is seen to be approximately 225 per cent of the rated current for the machine as read on curve *A* for the angle of 30 degrees. If the incoming machine were operating at a frequency equal to that of the system, the incoming machine would oscillate from 30 degrees lead to approximately 30 degrees lag and at that point it would draw a swing current equal to 170 per cent of its own rating as read from curve *B* at 30 degrees lag.

Consider next the case in which the system is synchronized with zero difference in phase but with a frequency difference of one per cent as given by figure 5. In this case the initial currents are zero as shown by curve *A* of figure 7 for the angle of 0 degrees. There will, however, be a transient oscillation which quickly produces a maximum angular displacement of 20 degrees as shown in figure 5. Curve *C* has been plotted for the purpose of giving these maximum swing currents and maximum angular displacements for synchronizing under one condition of frequency difference. The maximum swing current for the case considered in figure 5 is obtained by entering the frequency-difference curve *C* at the initial synchronizing angle, zero in this case. This gives a maximum swing current of 120 per cent. This value of current is identical with that shown by curve *B* based on transient reactances with an angular displacement of approximately 20 degrees. Curve *D*, calculated by an approximate method, has been added to indicate the nature of the family of curves similar to curve *C*. Thus, curves *C* and *D* may be used to give the maximum swing currents and the maximum angular displacements for the initial outward swing and return. Subsequent swings are not considered because of the damping due to the system losses.

Considering now the more complicated case of figure 6 for which the incoming machine is synchronized at an angle of 30 degrees lagging with the machine operating at a frequency of 99 per cent of the system. In this case the initial current is approximately 220 per cent as obtained from curve *A* at the angle of 30 degrees. The maximum swing current is given by curve *C* at the angle of 30 degrees and the value is 200 per cent. This value of transient current corresponds to a maximum displacement of 36 degrees which is the value on curve *B* which gives the same current of 200 per cent. This maximum angular displacement of 36 degrees is shown on figure 6 as occurring very shortly after synchronizing. Approximately the same angular displacement would be encountered if the system were synchronized at 30 degrees leading, with a frequency difference of 99 per cent. In this case the effect of phase position and initial frequency difference would be in such a direction as to cumulate the oscillations and thus would overshoot from the initial angle of 30 degrees on one side of the reference angle and swing to a maximum of about 36 degrees on the other side. Similar swings would be encountered for the other pair of conditions with the same angular displacements and the same frequency difference but with a different sign of the angle or sign of the frequency difference.

There is another effect that requires consideration and a certain amount of explanation. Synchronizing action may produce a permanent change in the phase positions of the system with respect to their original positions. If the incoming machine were synchronized under ideal conditions there would be no tendency for the phase position of the entire system to change. If, however, the incoming machine were synchronized at 0 degrees but with a frequency of 101 per cent of that of the system, the incoming machine would give up energy in order to synchronize and the system would correspondingly absorb energy and shift the absolute phase position in a forward direction as shown in figure 5. A similar effect is produced when machines traveling at the same frequency are synchronized out of phase. If the incoming machine has a voltage whose phase position is leading that of the system at the point of connection, the synchronizing action will cause the incoming machine to drop back in phase position and the system to move forward in phase position. If the machines were synchronized with the incoming machine lagging that of the system, the shift in absolute phase position would, of course, be in the opposite direction. Both effects are present when the system is synchronized under conditions including both angular difference and phase difference. These effects may tend to augment each other as shown in figure 6 or to oppose each other as would occur if the frequency of the incoming machine were one per cent above normal instead of one per cent below normal.

The change of the absolute phase position as a result of synchronizing is, generally, of little importance from an operating standpoint. This is due to the fact that usually the various machines connected to a system can follow it. However, in some cases, because of a relatively high product of inertia and reactance of the tie connection, some of the smaller machines may not be capable of quickly following a system disturbance. Under these conditions, synchronizing at a leading angle when the frequency is less than normal or at a lagging angle when the frequency is above normal may be advantageous. These phenomena explain certain practical operating conditions that have been observed in which two disturbances, produced by synchronizing, appear to be of equal magnitude at some parts of the system but of radically different magnitude at other parts. However, it is not considered advantageous to modify the usual synchronizing procedure since that would increase the disturbances for the main machines, although reducing them for the others.

The effects of various synchronizing conditions on the sending-end bus voltages are listed in table I. The variations in voltage at the receiving bus will be only a fraction of the minimum values tabulated. In some special applications the rapid variation in velocity or frequency in cycles per second is of importance. For this reason, the variations in frequency of the receiving-end machines are also given in table I. In connection with the variation in voltage and in frequency, it is to be recognized that these synchronizing conditions include wide departures from usual or proper synchronizing.

Synchronizing under conditions producing transient currents with a d-c component will cause pulsating rotor currents and pulsating torques. These torques are responsible for the low-frequency vibrations of machines during synchronizing causing these machines to "grunt". This circumstance provides another reason for securing accurate control of voltage difference and particularly of phase difference. If a machine rotor has a natural frequency close to fundamental frequency of the system, there will be a tendency to augment the oscillation due to pulsating rotor currents. Ordinarily, the natural frequency of machine rotors will be well below the generated frequency, particularly for hydro machines.

Requirements for Good Synchronizing

The relative importance of the variations in voltage difference, frequency difference, and phase difference, that are likely to be encountered in commercial installations will now be considered.[3,4] The voltages of the incoming and running machines are usually under the control of voltage regulators and the voltage difference for normal conditions will be less than five per cent which can readily be reduced by manual adjustments. The frequency of the incoming machine is under the control of a governor with speed matcher and the frequency difference may range from 0.025 to 0.10 cycles per second for large machines. For smaller machines this range should be increased to 0.3 cycle per second with the reservation that some small machines are too erratic for good synchronizing. The range of frequency difference may also be expressed in terms of the revolutions of the synchroscope and the corresponding figures are 40 to 10 seconds per revolution for large machines and the range increased to 3 seconds per revolution for small machines. A phase difference of five degrees represents a limit which is as difficult to obtain as the limits given for the other factors.

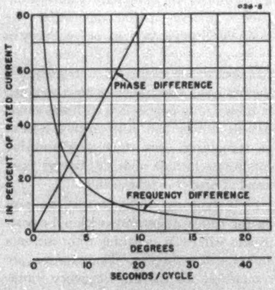


Figure 8. Comparison of the effects of frequency difference and phase difference on magnitude of inrush currents

The magnitude of current in the incoming machine may be taken as a measure of the disturbance produced by synchronizing action. Using this criterion, it is an easy matter to compare the relative importance of the differences due to voltage, frequency, and phase angle. From figure 4 it will be seen that for the system of figure 1, an eight per cent difference in voltage will cause a current in the incoming machine equal to 40 per cent of its rating when synchronized under ideal conditions as to frequency and phase difference. This figure also shows that the effect of voltage difference upon the synchronizing current is relatively more important for small than for large angular differences. In view of the fact that voltage regulators keep the voltage difference within five per cent, it is apparent that the effect of voltage difference under practical operating conditions is relatively unimportant in comparison with the other factors.

In considering the relative importance of frequency difference and phase difference, it is convenient to use the data of figure 7 but replotted in the form shown in figure 8. This curve shows that synchronizing at zero phase difference but with a frequency difference of three seconds per cycle will produce a current in the incoming machine of about 50 per cent of its rating. This value of current would be produced by synchronizing with zero difference in frequency but with a six-degree difference in phase. From this, it becomes evident that, within the normal range of control, the effect of frequency difference is less important than that of phase-angle error. The effects of losses, including induction-motor action, will further minimize the oscillations and swing-current magnitudes. The variation of phase difference arises principally from the change in angle which takes place during the time that is required for the synchronizing breaker to close. For example, on the system which has been considered, the frequency difference of 0.16 cycle per second or 6.0 seconds per revolution of the synchroscope, the system will shift 30 degrees in phase position during the time of 0.5 second, which is not an unreasonably long closing time for a synchronizing breaker. It is apparent, therefore, that quite accurate compensation for frequency difference is required. In other words, if the synchronizing breaker is actually closed at zero phase position the frequency difference normally encountered under synchronizing conditions on commercial systems will not introduce disturbances of important magnitude.

The discussion which has been given in the paper applies principally to the typical system selected for study. It should, however, be pointed out that with lower-reactance machines or with greater capacity on the bus, the relative importance of phase difference in synchronizing is increased. Similarly, if the inertia of the incoming machine is increased, the effect of frequency difference in synchronizing is increased. The system selected for study is believed to be representative of the more severe conditions from the standpoint of synchronizing currents and disturbance to the incoming machines.

The essential features for automatic synchronizers may be stated as requiring the following:

1. **A phase advance element which compensates for the time required to close the synchronizing circuit breaker, this element being properly responsive to change in frequency difference.**

2. **A beat-frequency characteristic which prevents operation of the synchronizer if the frequency of the incoming machine differs too widely from that of the system.**

3. **A voltage-difference element which prevents operation of the synchronizer if the voltage difference is excessive.**

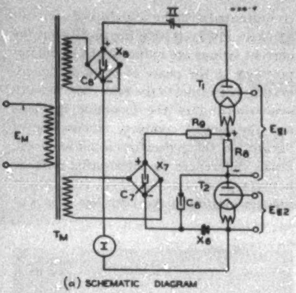
The beat-frequency characteristic and the voltage difference element prevent operation of the synchronizer under conditions which might produce doubtful synchronizing or which might be beyond the range of the compensating features provided in the phase-advance element. It is, of course, necessary that the various elements perform their functions correctly for the range of voltage and frequency variations that are encountered in practice. The phase-advance element must be adjustable for the time range required for closing the synchronizing breakers. The beat-frequency characteristic must also be adjustable to suit the range required on the particular system.

Description of Electronic Synchronizer

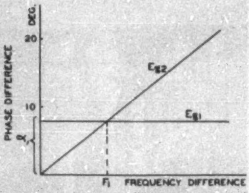
Automatic synchronizers have been on the market for several years.[2,3,4] Previous types have employed electrically interlocked relays which operate each time the phase angle between the machine and line voltages becomes zero. These automatic synchronizers have given excellent service in a variety of applications. However, it has become evident that faster and more accurate synchronizing could be obtained if the interlocking relays were eliminated and their functions performed by electronic means. In order to eliminate all moving parts except the first breaker-closing relay, an "all-electronic" synchronizer was developed.

The internal circuit of this synchronizer comprises two thyratron tubes, transformers, and several rectifiers, capacitors, resistors, and a breaker-closing relay. Each rectifier is provided with a smoothing capacitor and resistor. Voltage-checking relays, apart from the automatic synchronizer, prevent synchronizing operations until contact I is closed to denote that the machine and line voltages are approximately equal.

Figure 9*a* illustrates the two thyratron tubes, T_1 and T_2, which are electrically interlocked so that relay I will become energized to close the circuit breaker only when both tubes pass current in series from rectifier X_8. The tube-interlocking circuit is arranged to prevent operation of tube T_1 if tube T_2 has already started to pass current. Current flowing through resistor R_8 from rectifier X_7 causes the voltage across resistor R_8 to become suffi-



(a) SCHEMATIC DIAGRAM

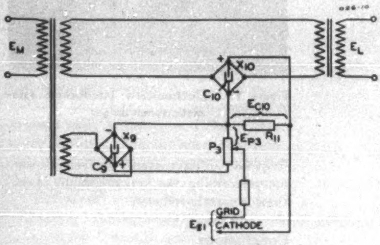


(b) GRID CONTROL VOLTAGE REQUIREMENTS

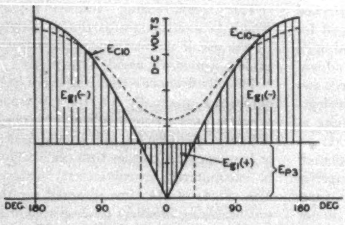
Figure 9. Electronic-type interlocking circuit

ciently negative as to bias tube T_1 and prevent its operation.

The operation of these tubes will respond to the grid-control voltages E_{g1} and E_{g2}. If these grid-control voltages have frequency-difference and phase-angle characteristics as shown in figure 9*b*, tubes T_1 and T_2 will operate to close relay I only when the frequency difference is less than F_1. When the frequency difference exceeds F_1, tube T_2 operates before the phase angle α_1 is reduced to that for which tube T_1 is set to operate. Consequently, relay I cannot operate.



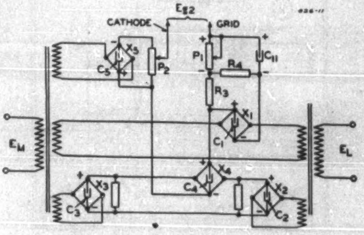
(a) SCHEMATIC DIAGRAM



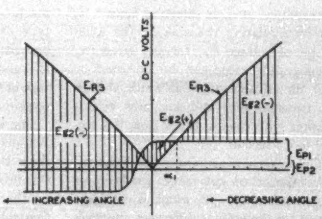
(b) GRID VOLTAGE PHASE DIFFERENCE CHARACTERISTICS

Figure 10 (left). Grid-control circuit for tube T_1

Figure 10*a* illustrates the method for obtaining grid-control voltage E_{g1} for tube T_1. A constant d-c potential E_{P3} is obtained from rectifier X_9. A variable d-c potential E_{C10} is obtained from the "beat-frequency" voltage rectifier X_{10}. By varying the adjustment of rheostat P_3, the grid voltage E_{g1} can be made to become positive at a definite angle of advance, as indicated by the curves in figure 10*b*. This angle of advance is practically independent of the frequency difference so long as it does not exceed the usual synchronizing limits of one-fourth to one-third cycle. If the frequency difference is high, capacitor C_{10} introduces a delaying action so that voltage E_{C10} does not become zero when the phase angle is zero but follows the dotted line shown in figure 10*b*. This characteristic prevents synchronizing when the frequency difference is too high. However, it does not affect the accuracy of synchronizing at the normal synchronizing frequency differences.



(a) SCHEMATIC DIAGRAM



(b) GRID VOLTAGE PHASE DIFFERENCE CHARACTERISTICS

Figure 11 (right). Grid-control circuit for tube T_2

Figure 11*a* illustrates the method for obtaining grid-control voltage E_{g2} for tube T_2. Voltage E_{g2} consists of four components, E_{P2}, E_{R3}, E_{C4}, and E_{P1}. Voltage E_{P2} is a constant value taken from rheostat P_2. Voltage E_{R3} is obtained from the "beat-frequency" voltage rectifier X_1. This is a variable voltage that is high for large phase angles, low for small phase angles, and zero only when the machine and line voltages are exactly equal and exactly in phase. Voltage E_{C4} is taken from rectifier X_2 and X_3 through rectifier X_4 to obtain compensation against differences between the machine and line alternating-voltage magnitudes, within the range set by an external over-all voltage difference checking relay.

Rheostat P_1 and capacitor C_{11} are connected across resistor R_4 which receives voltage from the "beat-frequency" voltage rectifier X_1. When the phase angle decreases from 180 degrees toward zero, capacitor C_{11} discharges through rheostat P_1. This produces a voltage drop E_{P1} which is approximately proportional to the frequency difference and which has a polarity as indicated in figure 11, with the result that the grid of tube T_2 tends to become more positive if the frequency difference is high.

The curves in figure 11*b* show the various component voltages E_{P2}, E_{R3}, and E_{P1}. Voltage E_{C4} is not shown for it is assumed that the machine and line voltages are equal in magnitude. E_{B3} is highly negative at 90 degrees phase angle and decreases to zero at zero phase angle. Voltage E_{P2} is adjusted so that when E_{P1} is zero (that is, when the frequency difference is zero), the grid voltage E_{g2} becomes zero at approximately two degrees angle. As the frequency difference is increased, voltage E_{P1} increases proportionally so that the grid voltage E_{g2} becomes zero at an advanced phase angle, for example, at α_1 as shown in figure 11*b*. By manipulating rheostat P_1 the magnitude of voltage E_{P1}, for any given frequency difference, can be adjusted so that the advance angle at which voltage E_{g2} becomes zero will suit any circuit breaker with closing times from 0.3 second to 0.8 second.

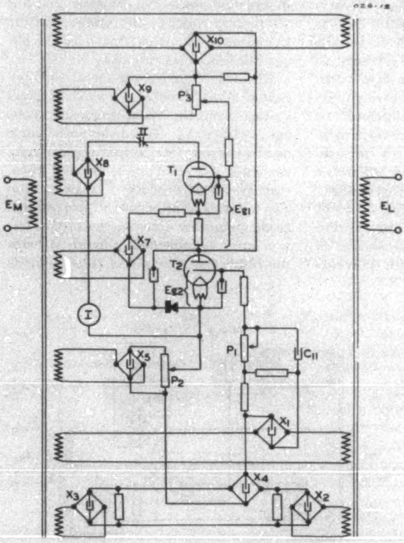


Figure 12. Schematic diagram for complete electronic synchronizer

The internal diagram for the electronically-interlocked automatic synchronizer is shown in figure 12, which is complete except for the voltage difference checking and other auxiliary relays. A photograph of this synchronizer is shown in figure 13. The phase advance and lockout frequency-difference features are all obtained by interlocking of the vacuum-tube grid control and plate circuits. The breaker-closing relay which responds to the interlocked tube action is mounted in the synchronizer case.

Operating Experience

The generators at Boulder power plant are all synchronized by automatic means. This installation now includes a total of 11 units of which 8 are rated 82,500 kva, one 40,000 kva, and 2 station service units 3,500 kva each. The interlocked-relay type of synchronizer[3] was included with the original installation and has given good service in synchronizing these machines. The electronically-interlocked type of synchronizer was developed for use with the more recently installed units *A*-6 and *A*-7 in order to take full advantage of the fast breaker closing speeds and to provide more accurate synchronizing.

For synchronizing the larger units at Boulder power plant, operating results show that the electronically interlocked synchronizer performance is far superior to that of the interlocked-relay type. Control-battery voltage variations do not affect accuracy of the synchronizer because the plate and grid control circuits are all taken from the incoming generator potential which is under control of the voltage regulator, and reaches a constant normal value before the synchronizer becomes operative. The synchronizing performance is more consistently accurate because the electronic interlocking feature is co-ordinated with the actual speed of circuit breakers involved with the result that the current swings are reduced. Of equal importance to the peace of mind of the operating personnel is the relatively smooth synchronizing of the incoming machine and the almost complete elimination of "grunt" as the generator circuit is closed. Small differences in alternating voltage between generator and bus do not prevent breaker closure at the very low values of frequency difference and the over-all time for performing the synchronizing operations is greatly reduced.

Figure 13. Electronically interlocked automatic synchronizer

Conclusions

The synchronizing transients may readily be analyzed for particular systems by the use of the methods applied to the typical system discussed in the paper.

The disturbances resulting from synchronizing manually or by automatic synchronizers are more affected by phase difference than by the voltage differences or frequency differences that are likely to be encountered on commercial systems. The effects of the time required for the synchronizing breakers to close appear as differences in phase and may be compensated for by adjustments responsive to the frequency difference.

The electronic synchronizer described in the paper permits taking advantage of faster circuit-breaker closing speeds and provides more accurate synchronizing than the previous equipment with mechanical interlocking relays.

Analysis of Transient Voltages in Networks

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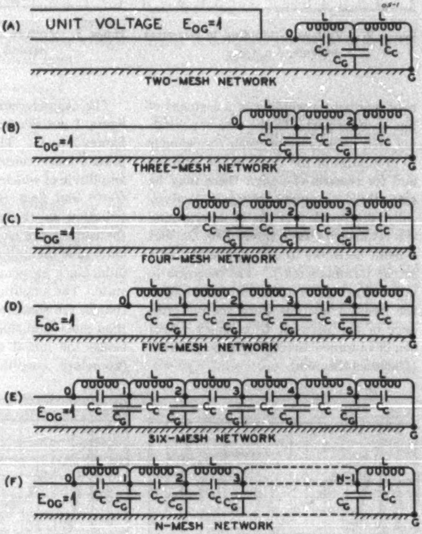
IMPORTANT contributions on transient phenomena have appeared in the technical literature during the past two decades. Circuit analysis has contributed materially to the present-day knowledge resulting from studies of the following major engineering problems:[1–6] transient voltages in transformer, generator, and other apparatus windings due to surges originating from lightning; traveling waves and switching phenomena on lines and cables; recovery voltages across terminals of circuit breakers and protective devices as affected by the circuit; response of devices and circuits in measuring voltages and currents applied in impulse and short-circuit testing; and others.

These and other contributions on transient phenomena were made possible in a large measure through the development and application of the operational calculus.[7–13] The experimental aid afforded by the cathode-ray oscillograph and from other suitable means of measurement and testing has been invaluable in itself and, furthermore, it has in turn stimulated markedly the analytical work. Even within the field here considered, new engineering problems or the extending of the past studies into greater detail will require further analytical work. It is apparent that the need is arising to unify the various analyses and corresponding circuits into a more closely correlated whole. Should this process be carried out to its logical end, this branch of the engineering art would become better integrated; at the same time, it is expected that it would be extended in scope and also simplified.

Recognizing this need and bearing in mind the application to engineering, the writers have undertaken the analytical work in this paper. Mathematical processes are used, yet the physical approach is followed as much as possible. The practical advantages in guiding the mathematical solution in terms of physical concepts and of the underlying physical phenomena will become more apparent from the analysis and the accompanying figures. Likewise, efforts were directed to securing results with a minimum of mathematical encumbrance. To this end, the operational calculus was employed. The paper presents rational processes that have been developed and effectively applied in the analysis. These methods indicate the possibilities toward simplification and a more comprehensive approach to the problems requiring circuit analysis.

Figure 1. Symmetrical lumped networks



Symmetrical Lumped Networks

Many engineering problems requiring analysis are effectively approached through the simplification of the circuit into an equivalent network of finite meshes, each mesh generally consisting of two or more lumped parameters. Figure 1 shows a type of networks commonly encountered. These consist of series inductances (L), capacitance to ground (C_G) from each of the common points joining the inductances, and self-capacitance (C_C) across each inductance. These symmetrical lumped networks

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1. For all numbered references, see list at end of paper.

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4. COMPENSATED AUTOMATIC SYNCHRONIZER, H. T. Seeley. ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), volume 53, 1934, pages 960–8.

Discussion

For discussion, see page 1104.

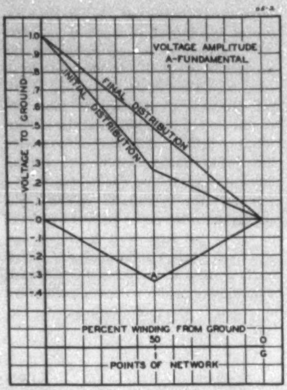

Figure 2. Voltage distribution in two-mesh network, $C_G = 4C_C$

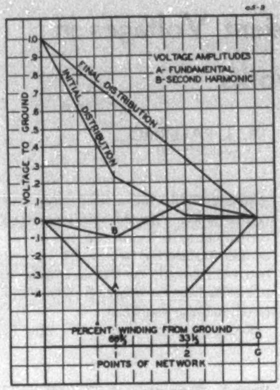

Figure 3. Voltage distribution in three-mesh network, $C_G = 4C_C$

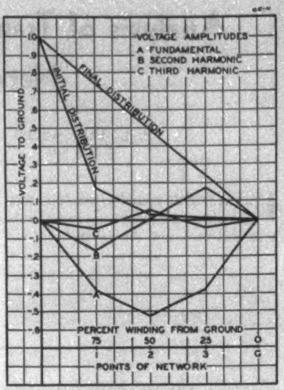

Figure 4. Voltage distribution in four-mesh network, $C_G = 4C_C$

may represent a winding or a segment of line or other part. In the case of a winding each inductance or mesh corresponds to groupings of the turns of the winding and for reasons of design there may be two, three, or more groups. Analytically, the problem is one of determining the response of two, three, four. . . and n-mesh networks to unit applied voltage at the terminals (OG). The response to unit voltage (Heaviside unit function) is the basis of analysis since the performance of the network to complex waves can be obtained directly by superposition (Duhamel theorem).

The characteristics of the networks in figure 1 are illustrated in the curves of figures 2 to 11. The response to unit applied voltage consists of oscillations, the amplitude of which depends on the initial ($t=0$) and final ($t \rightarrow \infty$) voltage distribution in the network. Physically, the capacitance elements of the network determine the initial distribution and the inductance elements the final or steady state. The amplitudes of the oscillations, that is, the space waves shown for $t=0$, thus must sum up to the difference between the initial and final distribution (boundary conditions). As illustrated in the figures, the greater this difference is, the greater become the amplitudes. In the analysis, the amplitudes of the oscillations are essentially determined from the boundary conditions through the Fourier process which in operational calculus is inherently performed in the derivation of the solution (appendix I).

For symmetrical networks each space wave is distributed symmetrically between ends, from line to ground, the amplitude being highest for the fundamental and correspondingly smaller for the "second", "third", and higher harmonics. The periods (time constants) of the oscilla-

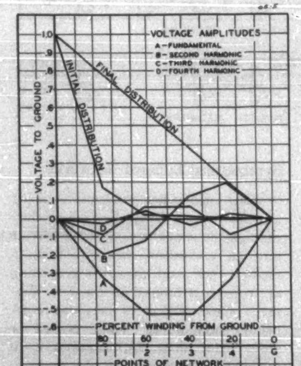

Figure 5. Voltage distribution in five-mesh network, $C_G = 4C_C$

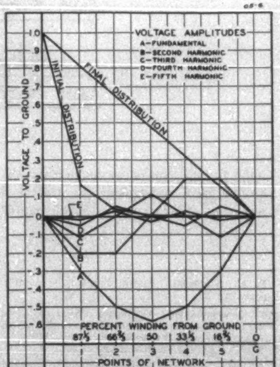

Figure 6. Voltage distribution in six-mesh network, $C_G = 4C_C$

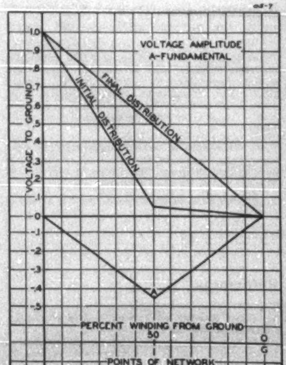

Figure 7. Voltage distribution in two-mesh network, $C_G = 16C_C$

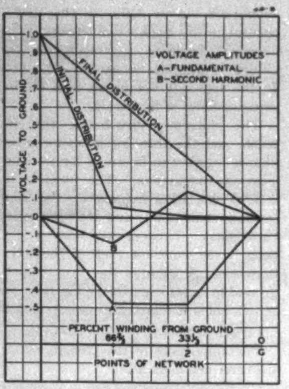

Figure 8. Voltage distribution in three-mesh network, $C_G=16C_C$

tions naturally depend solely on the circuit parameters L, C_G, and C_C, the higher the order of the harmonic, the shorter the period but not in simple arithmetical relation. The total number of oscillations is $N-1$ where N is the number of meshes of the network. Thus each of the space waves oscillates at its natural period about the zero voltage axis, and, to determine the voltage distribution in the network at any time, the space waves at that instant are combined with the steady state.

A simplification in the analysis became apparent from physical considerations of the symmetrical progression of the network (figure 1) from two to three meshes, three to four, etc., which suggested a pattern of the operational expressions. This pattern can be set down completely for a network of any number of meshes when the operator for the two-mesh network is known; as follows:

(1) 1
(2) $\alpha+2$
(3) $\alpha^2+4\alpha+3$
(4) $\alpha^3+6\alpha^2+10\alpha+4$
(5) $\alpha^4+8\alpha^3+21\alpha^2+20\alpha+5$
(6) $\alpha^5+10\alpha^4+36\alpha^3+56\alpha^2+35\alpha+6$
(7) etc.

where the operator alpha (α) is the ratio of the operational impedances of the series (Z_S) and the shunt (Z_G) elements of the mesh, that is, $Z_S/Z_G=C_GLp^2+(C_CLp^2+1)$ (appendix I).

To illustrate the analysis, for the six-mesh network, the voltages are written immediately in operational form:

$$e_{1G}=\frac{\alpha^4+8\alpha^3+21\alpha^2+20\alpha+5}{\alpha^5+10\alpha^4+36\alpha^3+56\alpha^2+35\alpha+6}$$

$$e_{2G}=\frac{\text{operator }4}{\text{operator }6}$$

etc.

These operational expressions are expanded into partial fractions of alpha (α). The operational terms then are in a form recognizable for conversion and the solution is effected. As an example:

$$e_{1G}=\frac{1/12}{\alpha+2-\sqrt{3}}+\frac{1/4}{\alpha+1}+\frac{1/3}{\alpha+2}+\frac{1/4}{\alpha+3}+\frac{1/12}{\alpha+2+\sqrt{3}}$$

$$e_{1G}=A_0-A_1\cos w_1t-A_2\cos w_2t-A_3\times\cos w_3t-A_4\cos w_4t-A_5\cos w_5t$$

where the specific values for the coefficients and the periods ($T=2\pi/w$) are given in appendix I.

The solution for e_{2G} and e_{3G} are similar expressions, which are readily obtained. The voltages e_{4G} and e_{5G} are apparent from the symmetry of the space waves (see figures 6 and 11).

The periods of oscillation inherently are established from the space wave distribution in relation to the elements of the network. A few examples will illustrate this physically fundamental relationship. For the fundamental of the two-mesh network, the "second" harmonic of the four-mesh, the "third" harmonic of the six-mesh, etc., the space wave rises from zero to maximum as a straight line and recedes back to zero in the same way. It is apparent from the space-wave distribution that the parallel elements L, C_C of the first leg oscillate with the three elements C_G, L, C_C of the second part. Thus, the effective inductance is $^1/_2L$ while the capacitances add together in determining the period, that is, $T=2\pi\sqrt{\frac{L}{2}(C_G+2C_C)}$. In the case of the fundamental of the three-mesh network, the "second" harmonic of the six-mesh, etc., the voltage of the space wave between points 1 and 2 of the network is zero and therefore the period in this case is determined simply from L, C_C in series with C_G, that is, $T=2\pi\sqrt{L(C_G+C_C)}$. For the "second"

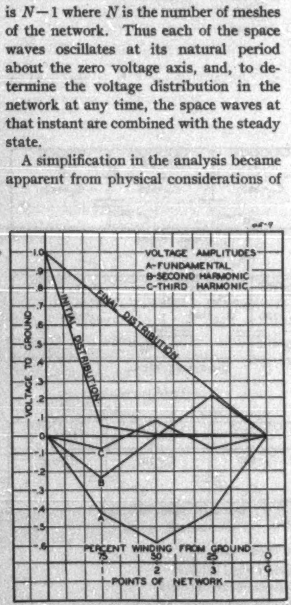

Figure 9. Voltage distribution in four-mesh network, $C_G=16C_C$

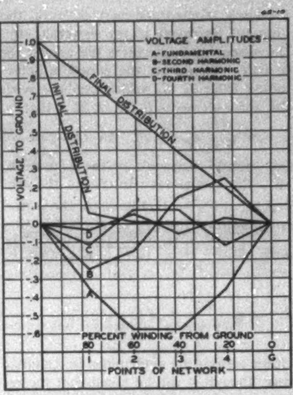

Figure 10. Voltage distribution in five-mesh network, $C_G=16C_C$

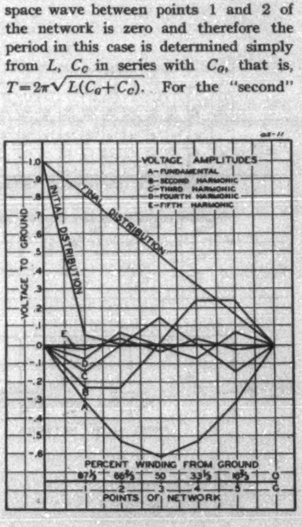

Figure 11. Voltage distribution in six-mesh network, $C_G=16C_C$

harmonic of the three-mesh network, the "fourth" harmonic of the six-mesh, etc., the space wave intersects the second leg in half. Therefore, the period is determined from L, C_C which oscillate with the three elements C_G, $L/2$, $2C_C$, that is, $T=2\pi\sqrt{\frac{L}{3}}(C_G+3C_C)$. In the case of the "third" harmonic of the four-mesh, etc., the space wave intersects the second leg at $(\sqrt{2}-1)$, therefore, the period is determined from L, C_C in combination with C_G, $\frac{L}{1+\sqrt{2}}$, $(1+\sqrt{2})C_C$, that is,

$$T=2\pi\sqrt{\frac{L}{2+\sqrt{2}}(C_G+[2+\sqrt{2}]C_C)}$$

Other periods are determined in a similar way, though it will be noted that the relation becomes complex in the measure that the space waves assume more complex geometrical forms.

Comparison of Uniformly Distributed and Lumped Networks

As the meshes increase in number, the network approaches uniform distribution. The solution for the uniformly distributed or infinite-mesh network is readily obtained from Heaviside expansion theorem (appendix II). The characteristics of this network are illustrated in figure 12. These consist of sinusoidal space waves.

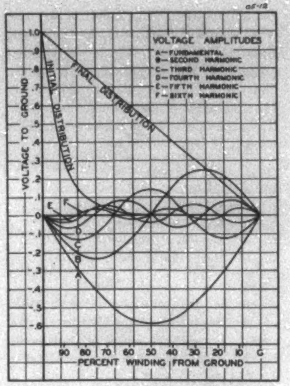

Figure 12. Voltage distribution in uniformly distributed network, $N\sqrt{\frac{C_1}{C_2}}=12$

A comparison of figures 2 to 11 with figure 12 shows the extent that a finite-mesh network approaches the uniformly distributed network. For example, the space wave of the fundamental for the six-mesh network attains a form and amplitude closely alike to the fundamental for the distributed network. The "second" harmonic is trapezoidal, the "third" harmonic triangular, etc., that is, the approach diminishes with the increasing order of the period.

The uniformly distributed, the six-mesh, and the three-mesh networks are shown together in figure 13 for the purpose of comparison. The basis is as follows: the total inductance, the total ground capacitance, and the total series capacitance (between the two ends), each is the same for each of the three networks. A value of the ratio of total ground to series capacitance has been selected which is representative in practical application, that is, $\sqrt{(C_1/C_2)N^2}=12$.

The amplitudes of the oscillations for the three networks are:

	Distributed Network (Figure 12)	Six-Mesh Network (Figure 6)	Three-Mesh Network (Figure 8)
Fundamental	0.59	0.58	0.47
Second harmonic	0.24	0.20	0.14
Third harmonic	0.13	0.11	
Fourth harmonic	0.07	0.05	
Fifth harmonic	0.04	0.02	
...	...		

In comparing the periods, let $T_k=\frac{2\pi}{\delta}\times\sqrt{(L_1C_1)N^2}$; where L_1N is the total in-

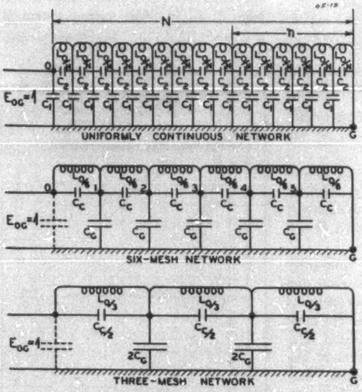

Figure 13 (left). Distributed and lumped networks compared

Total inductance $=L_0$
Total ground capacitance $=C_1N=6C_G$
Total series capacitance $=C_{2/N}=C_{C/6}$

$$\sqrt{\frac{\text{total ground capacitance}}{\text{total series capacitance}}}=\sqrt{\frac{C_1N}{C_{2/N}}}=\sqrt{\frac{6C_G}{C_{C/6}}}=12$$

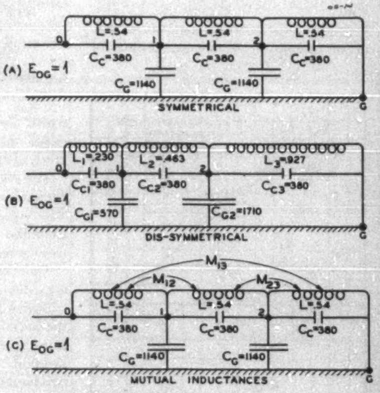

Figure 14 (right). Symmetrical and dissymmetrical three-mesh networks compared

Basis of comparison for:
(*A*)—Symmetrical network
(*B*)—Dissymmetrical network
(*C*)—Mutual inductances network
Capacitances are in micromicrofarads and inductances in henrys

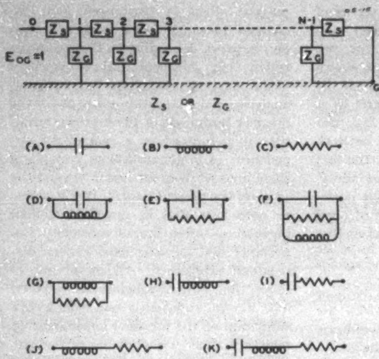

Figure 15 (left). Symmetrical lumped networks—generalized

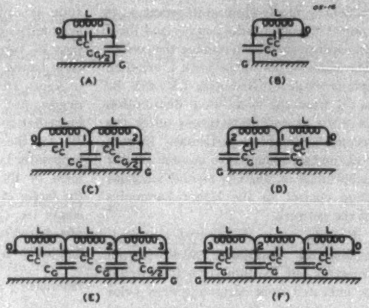

Figure 16. Ungrounded symmetrical lumped networks

ductance, C_1N the total ground capacitance, δ a numerical factor, while subscript k denotes the harmonic. The values of δ corresponding to the periods for the three networks are tabulated as follows:

	Distributed Network	Six-Mesh Network	Three-Mesh Network
Fundamental	3.04	3	2.91
Second harmonic	5.56	5.36	4.76
Third harmonic	7.40	6.93	
Fourth harmonic	8.65	7.85	
Fifth harmonic	9.52	8.33	
Sixth harmonic	10.12		
...	...		

The amplitudes for the distributed and the six-mesh networks are in generally good agreement. For practical purposes, for these two networks, it will be noted that the corresponding periods are also in quite close agreement. The comparison points out that the finite-mesh networks can be considered in practical analyses as uniformly distributed networks with adequately good approximation. The simplicity in analyzing a multielement network in terms of an "equivalent" uniformly-distributed network is borne out by a comparison of the solutions in appendix II with appendix I.

However, in lumped networks—and most apparatus is best represented by lumped networks—the characteristics of the higher harmonics differ from those determined from the uniformly distributed network. On the other hand, the higher the harmonic the less in general is the importance it assumes relative to the lower harmonics.

Effect of Dissymmetry in Networks

Dissymmetry in networks has a very marked effect in complicating the mathematical solution. Appendix III gives analyses for dissymmetrical three-mesh networks and illustrates the extent the equations become complex and cumbersome.

A comparison between symmetrical and dissymmetrical networks is best approached from specific consideration of the response, as in the three-mesh networks of figure 14. The comparison of the three networks is based on a total self-inductance, a total ground capacitance, and a series capacitance, each of which is equal for the three networks. The following are the equations of the voltages, where time t is in microseconds:

Symmetrical network, figure 14*A:*

$$E_{1G}=0.667-0.375\cos 0.035t-0.075\cos 0.049t$$

$$E_{2G}=0.333-0.375\cos 0.035t+0.075\cos 0.049t$$

Dissymmetrical network, figure 14*B:*

$$E_{1G}=0.86-0.19\cos 0.0324t-0.37\cos 0.070t$$

$$E_{2G}=0.57-0.54\cos 0.0324t+0.01\cos 0.070t$$

Network with mutual inductances, figure 14*C:*

$$E_{1G}=0.68-0.375\cos 0.039t-0.095\cos 0.064t$$

$$E_{2G}=0.32-0.375\cos 0.039t+0.095\cos 0.064t$$

where $M_{12}=M_{23}=0.4L$ and $M_{13}=0.2L$.

The principles discussed for symmetrical networks apply equally to the dissymmetrical networks. The amplitudes and periods are likewise determined from the initial distribution and the steady state, and from the distribution of the space waves (see equations above) in relation to the network (figure 14). Thus the fundamental periods for the dissymmetrical and the symmetrical networks are found closely alike for the reason that the product of the inductance and capacitance elements, which determine the period, remains substantially unchanged. The dissymmetry of the network B is accompanied by a corresponding distortion or skewing in the distribution of the fundamental space wave, as it readily can be noted from the equations above. The space wave of the second harmonic is concentrated largely across the two meshes at the line end; naturally the period is practically established from the circuit elements in these two meshes.

Another arrangement of the dissymmetrical network B is obtained grounding O and applying the voltage at the G end. In this case, the fundamental wave predominates throughout the network and furthermore its amplitude is reduced in amount. There are other intermediate combinations of the circuit elements. In general, a departure from symmetry in a network does not modify the fundamental to the same relative extent that it affects the amplitude, period, and distribution of the higher harmonics.

The effect of mutual inductances in an otherwise symmetrical network (figure 14C) is apparent from the equations given above and can readily be demonstrated also from a physical consideration of the space waves in relation to the elements of the network. Referring to the space waves of a symmetrical network (figure 3), it is clear that equal mutual inductances M_{12} and M_{23} do not affect the fundamental, for their effects on the center inductance mutually cancel. The mutual inductance M_{13} naturally influences the fundamental period as it couples the outer inductances—and these are elements which determine the period. The space waves for network *C* are then symmetrically distributed. It will be noted from the space-wave distribution that the mutual inductances affect the second harmonic in particular. In general, mutual inductances assume greater importance in relation to and influence more control on the higher harmonics of the network.

For the sake of brevity, the comparison has been based on three-mesh networks but the same principles would be indicated from comparing more complex networks. It must be recognized that the mathematical solution and numerical evaluation of multimesh dissymmetrical networks becomes prohibitive in practice. These difficulties can be overcome in part through the physical approach and interpretation of the particular complex network under consideration; in general, through this process coupled with a basic understanding of circuits, the fundamental and the harmonic oscillations can be estimated to a degree usually satisfactory for practical purposes. Experience in network analysis and in its application indicates that in a number of the cases encountered, the degree of dissymmetry while theoretically appreciable can, for practical purposes, be disregarded and accordingly, in such cases, the solution is attained by arranging the circuit into the symmetrical form and treating it as such. Occasionally, dissymmetry in networks is more apparent than real and in such cases the network is readily conducive to marked simplification through a consideration of the equivalent symmetrical network. Whether an approximate solution is permissible or a rigorous analysis is required, in all cases, the advantages of the physical approach can hardly be overemphasized.

Generalization of Analysis

UNGROUNDED NETWORKS

The analysis of networks grounded at one end can readily be extended to determine the response of ungrounded networks (figure 16). Consider the grounded end of the networks in figure 1 open and unit voltage applied simultaneously at the two ends *O* and *G*. Then from superposition of the space waves and the distribution curves in figures 2, 3, 4, etc., the response is established. It consists simply of the fundamental, third, and other odd harmonics doubled in amplitude and oscillating about a steady-state axis of unity (1.0). By virtue of symmetry, each network in effect consists of two equal parts from the two ends to the middle; one part is shown in figure 16. Thus the response of the ungrounded network *E* in figure 16 is immediately given from E_{G1}, E_{3G}, and E_{5G} for the grounded six-mesh network (appendix I), where in all cases, the first term of the equations becomes unity, the factor of two is applied to the coefficients for the fundamental, third, and fifth harmonic, and where the even harmonics are not present.

IMPEDANCE Z_S AND Z_G GENERALIZED

In the foregoing, the networks are representative of apparatus windings, segments of line or cable, etc. The analysis however is general and is applicable to other engineering problems. As an example, the inductive elements in figure 1 would be replaced by resistance to study the response of a resistance voltage-divider or for determining the voltage across the capacitor banks of an impulse generator during the charging process. Consider a six-mesh network, the solution is given in appendix IV. The pattern of the solution is the same and the same physical principles developed previously apply equally here. Naturally, the modes of the response become exponential terms.

Figure 15 gives the generalized network and it shows also typical circuit elements for the Z_S and Z_G impedances. These can assume other combinations—for instance, Z_G may consist of separate elements mutually coupled thus physically separating the meshes of the network. The more complex the Z_S and Z_G impedances are, the more complex form the operator takes on and correspondingly more unwieldy and lengthy become the equations of the solution. Simplification of the network proper may be possible. For instance, the dissipative characteristics (resistance elements) in inherently oscillatory networks frequently can, for practical reasons, be disregarded in transient voltage analysis, and where this approach is permissible, the solution is much simplified.

TERMINAL IMPEDANCES

Networks are almost invariably interdependent to a greater or lesser degree, that is, apparatus connects to lines or the neutral may be grounded through an impedance; separate windings of a transformer are mutually coupled in more than one way; lines and cables terminate at stations; an impulse generator is charged from a circuit suitable for the purpose, then is rearranged and discharged through either a simple or a complex network depending on the apparatus tested; etc. Mathematical completeness would suggest extending the foregoing analysis for the condition where one or the other or both ends of the network terminate in a lumped or a generalized impedance. A mathematical analysis along these lines while of interest is beyond the scope of the present paper. It will suffice to note here that in practice terminal impedances often are of secondary importance and in some cases can be disregarded altogether. When of importance, their effect frequently can be taken into account sufficiently well from a consideration of the terminal impedances in relation to the characteristics of the network proper or from a simplified network of the combination. Again, through the physical approach well-nigh insuperable mathematical difficulties can be overcome and thus the task of attaining a solution adequate for engineering purposes simplified.

VOLTAGE APPLIED

The unit function is of basic importance in analysis although in practice voltages of this form seldom occur. The response of networks to other forms is obtained from superposition. As a specific case, voltages on electrical systems originating from lightning disturbances usually are considered for purposes of design and in testing apparatus, as exponential. In appendix V is derived the response of a network to a voltage of exponential wave form.

It is apparent from the principle of superposition that the response of a network depends on the duration and the wave form of the voltage applied relative to the characteristics of the network. For instance, a voltage of steep front and short total duration develops more fully, and even can accentuate, the oscillations of shorter duration; whereas a voltage of relatively long front and sustained tail generates largely the fundamental at the exclusion practically of the higher harmonics. In fact, the analytical work often can be simplified from a consideration of the voltage applied in relation to the time constants of the network.

The treatment has dealt hereto with voltages applied. Transient phenomena in networks are produced also from circuit interruption and from other conditions. On a-c interruption usually takes place at current zero and the circuit voltage is re-established in a relatively short time after. The recovery voltages across *OG* in figure 1 for the networks proper, analytically consist in applying a current equal and opposite to the current interrupted,[8] that is, $i=wI_s t=wI_s/p$. Analysis of the networks based on applied currents likewise would bring forth guiding principles, simplifications, practical methods of approach similar to those which already have been discussed for voltages applied to the networks.

Applications

The analysis and the methods outlined in the paper have been applied to determine transients in windings,[2] to the development of impulse testing and of suitable measurement,[1] and to other problems. These methods and the same simplified approach should be helpful in corresponding applications whether these arise from electrical, acoustical, or heat-flow problems.[8]

An important step in the application work is the calculation of circuit elements from the dimensions of the apparatus or the parts which physically make up the network. Another important step is the building up of the equivalent network. Application problems usually require simplification to adapt the problem in a form conducive to calculation. In both these steps sound judgment, based on experience and on a fundamental grasp of network characteristics, is essential in arriving at practical results. To confirm the degree of approach in the calculated results recourse is had naturally to check tests.

In this connection, the cathode-ray oscillograph has served as an indispensable tool.[15] In addition, present progress hardly would have been possible without the impulse generators and other testing equipment which generate and subject apparatus and circuits to the transient phenomena these may experience in service. In recent years various means have been devised to facilitate establishing rapidly by test the transient characteristics of circuits and apparatus for the particular conditions desired.[16] For the larger electrical systems where the physical outlay is so extensive that full-scale testing is impractical or not economical and also because of the general complexity of the system and the phenomena involved, recourse is had to equivalent miniature networks. In this case the response of the system is determined from test on the equivalent built-up network.[17-19]

Analytical results and simplified methods of attack as the paper presents not only are a useful tool in design but likewise can be applied as an effective guide where the experimental approach is essential. From the satisfactory experience and good results obtained in the fields of application mentioned, it would appear that these methods can be extended to be useful in other connections.

Appendix I

This appendix deals with the networks in figure 1. The solutions are for "unit" voltage impressed. The subscripts are the points of the network between which the voltage or the impedance is taken.

Two-Mesh Network, Figure 1A

$E_{1G}=\frac{Z_{1G}}{Z_{OG}}\mathbf{1}$, where $\mathbf{1}$ is the symbol for "unit" voltage.

(For brevity, the symbol **1**, will be omitted hereafter.)

$$E_{1G}=\frac{1}{\frac{Z_S}{Z_G}+2}=\frac{1}{\alpha+2}$$

where $\alpha=\frac{Z_S}{Z_G}=\frac{p^2C_GL}{p^2C_CL+1}$

The solution for the operator, $\frac{A}{\alpha+B}$, where A and B are constants, is:

$$\frac{A}{\alpha+B}=\frac{A}{B}-\frac{AC_G}{B(C_G+BC_C)}\times \cos t\sqrt{\frac{B}{L(C_G+BC_C)}}$$

for the two-mesh network, the response is:

$$E_{1G}=\frac{1}{2}-\frac{C_G}{2(C_G+2C_C)}\cos wt$$

where $w^2=\frac{2}{L(C_G+2C_C)}$

Three-Mesh Network, Figure 1B

$$E_{1G}=\frac{\alpha+2}{\alpha^2+4\alpha+3}=\frac{1}{2}\left[\frac{1}{\alpha+1}+\frac{1}{\alpha+3}\right]$$

$$=\frac{2}{3}-\frac{C_G}{2(C_G+C_C)}\cos w_1t-\frac{C_G}{6(C_G+3C_C)}\cos w_2t$$

where

$$w_1^2=\frac{1}{L(C_G+C_C)} \text{ and } w_2^2=\frac{3}{L(C_G+3C_C)}$$

$$E_{2G}=\frac{1}{\alpha^2+4\alpha+3}=\frac{1}{2}\left[\frac{1}{\alpha+1}-\frac{1}{\alpha+3}\right]$$

$$=\frac{1}{3}-\frac{C_G}{2(C_G+C_C)}\cos w_1t+\frac{C_G}{6(C_G+3C_C)}\cos w_2t$$

Four-Mesh Network, Figure 1C

$$E_{1G}=\frac{\alpha^2+4\alpha+3}{\alpha^3+6\alpha^2+10\alpha+4}=\frac{1/4}{\alpha+(2-\sqrt{2})}+\frac{1/2}{\alpha+2}+\frac{1/4}{\alpha+(2+\sqrt{2})}$$

$$=\frac{3}{4}-\frac{C_G\cos w_1t}{4(2-\sqrt{2})[C_G+(2-\sqrt{2})C_C]}-\frac{C_G\cos w_2t}{4(C_G+2C_C)}-\frac{C_G\cos w_3t}{4(2+\sqrt{2})[C_G+(2-\sqrt{2})C_C]}$$

where

$$w_1^2=\frac{2-\sqrt{2}}{L[C_G+(2-\sqrt{2})C_C]},\; w_2^2=\frac{2}{L(C_G+2C_C)}$$

$$w_3^2=\frac{2+\sqrt{2}}{L[C_G+(2+\sqrt{2})C_C]}$$

$$E_{2G}=\frac{\alpha+2}{\alpha^3+6\alpha^2+10\alpha+4}=\frac{1}{\alpha^2+4\alpha+2}$$

$$=\frac{\frac{1}{2\sqrt{2}}}{\alpha+(2-\sqrt{2})}-\frac{\frac{1}{2\sqrt{2}}}{\alpha+(2+\sqrt{2})}$$

$$=\frac{2}{4}-\frac{C_G\cos w_1t}{2\sqrt{2}(2-\sqrt{2})[C_G+(2-\sqrt{2})C_C]}+\frac{C_G\cos w_3t}{2\sqrt{2}(2+\sqrt{2})[C_G+(2+\sqrt{2})C_C]}$$

$$E_{3G}=\frac{1}{\alpha^3+6\alpha^2+10\alpha+4}=\frac{1/4}{\alpha+(2-\sqrt{2})}-\frac{1/2}{\alpha+2}+\frac{1/4}{\alpha+(2+\sqrt{2})}$$

$$=\frac{1}{4}-\frac{C_G\cos w_1t}{4(2-\sqrt{2})[C_G+(2-\sqrt{2})C_C]}+\frac{C_G\cos w_2t}{4(C_G+2C_C)}-\frac{C_G\cos w_3t}{4(2+\sqrt{2})[C_G+(2+\sqrt{2})C_C]}$$

PATTERN OF THE α OPERATORS

The operational solutions of the networks thus far considered suggest a definite pattern of polynomials in α. Consider the following:

(1) 1
(2) $\alpha+2$
(3) $\alpha^2+4\alpha+3$
(4) $\alpha^3+6\alpha^2+10\alpha+4$
(5) $\alpha^4+8\alpha^3+21\alpha^2+20\alpha+5$
(6) $\alpha^5+10\alpha^4+36\alpha^3+56\alpha^2+35\alpha+6$
(7) $\alpha^6+12\alpha^5+55\alpha^4+120\alpha^3+126\alpha^2+56\alpha+7$

This pattern can be extended to cover any network containing a definite number of meshes. The pattern of polynomials is extended as follows:

(1) 1
(2) $\alpha+2$
(3) $(\alpha+2)[\alpha+2]-1$
(4) $(\alpha+2)[(\alpha+2)(\alpha+2)-1]-(\alpha+2)$
(5) $(\alpha+2)[(4)]-(3)$
(6) $(\alpha+2)[(5)]-(4)$
(7) $(\alpha+2)[(6)]-(5)$
(8) $(\alpha+2)[(7)]-(6)$

Six-Mesh Network

From the pattern set forth above the operational solutions for the six-mesh network shown in figure 1*E* are:

$$E_{1G}=\frac{\alpha^4+8\alpha^3+21\alpha^2+20\alpha+5}{\alpha^5+10\alpha^4+36\alpha^3+56\alpha^2+35\alpha+6}=\frac{\text{polynomial (5)}}{\text{polynomial (6)}}$$

$$E_{2G}=\frac{\alpha^3+6\alpha^2+10\alpha+4}{\alpha^5+10\alpha^4+36\alpha^3+56\alpha^2+35\alpha+6}=\frac{\text{polynomial (4)}}{\text{polynomial (6)}}$$

$$E_{3G}=\frac{\alpha^2+4\alpha+3}{\alpha^5+10\alpha^4+36\alpha^3+56\alpha^2+35\alpha+6}=\frac{\text{polynomial (3)}}{\text{polynomial (6)}}$$

$$E_{4G}=\frac{\alpha+2}{\alpha^5+10\alpha^4+36\alpha^3+56\alpha^2+35\alpha+6}=\frac{\text{polynomial (2)}}{\text{polynomial (6)}}$$

$$E_{5G}=\frac{1}{\alpha^5+10\alpha^4+36\alpha^3+56\alpha^2+35\alpha+6}=\frac{\text{polynomial (1)}}{\text{polynomial (6)}}$$

The final solutions are:

$$E_{1G}=\frac{5}{6}-\frac{C_G\cos w_1t}{12(2-\sqrt{3})[C_G+(2-\sqrt{3})C_C]}-\frac{C_G\cos w_2t}{4(C_G+C_C)}-\frac{C_G\cos w_3t}{6(C_G+2C_C)}-\frac{C_G\cos w_4t}{12(C_G+3C_C)}-\frac{C_G\cos w_5t}{12(2+\sqrt{3})[C_G+(2+\sqrt{3})C_C]}$$

$$E_{2G}=\frac{4}{6}-\frac{C_G\cos w_1t}{4\sqrt{3}(2-\sqrt{3})[C_G+(2-\sqrt{3})C_C]}-\frac{C_G\cos w_2t}{4(C_G+C_C)}+\frac{C_G\cos w_4t}{12(C_G+3C_C)}-\frac{C_G\cos w_5t}{4\sqrt{3}(2+\sqrt{3})[C_G+(2+\sqrt{3})C_C]}$$

$$E_{3G}=\frac{3}{6}-\frac{C_G\cos w_1t}{6(2-\sqrt{3})[C_G+(2-\sqrt{3})C_C]}+\frac{C_G\cos w_3t}{6(C_G+2C_C)}-\frac{C_G\cos w_5t}{6(2+\sqrt{3})[C_G+(2+\sqrt{3})C_C]}$$

$$E_{4G}=\frac{2}{6}-\frac{C_G\cos w_1t}{4\sqrt{3}(2-\sqrt{3})[C_G+(2-\sqrt{3})C_G]}+\frac{C_G\cos w_2t}{4(C_G+C_C)}-\frac{C_G\cos w_4t}{12[C_G+3C_C]}+\frac{C_G\cos w_5t}{4\sqrt{3}(2+\sqrt{3})[C_G+(2+\sqrt{3})C_C]}$$

$$E_{5G}=\frac{1}{6}-\frac{C_G\cos w_1t}{12(2-\sqrt{3})[C_G+(2-\sqrt{3})C_C]}+\frac{C_G\cos w_2t}{4(C_G+C_C)}-\frac{C_G\cos w_3t}{6(C_G+2C_C)}+\frac{C_G\cos w_4t}{12(C_G+3C_C)}-\frac{C_G\cos w_5t}{12(2+\sqrt{3})[C_G+(2+\sqrt{3})C_C]}$$

where

$$w_1^2=\frac{2-\sqrt{3}}{L[C_G+(2-\sqrt{3})C_C]}\qquad w_2^2=\frac{1}{L(C_G+C_C)}$$

$$w_3^2=\frac{2}{L(C_G+2C_C)}\qquad w_4^2=\frac{3}{L(C_G+3C_C)}$$

$$w_5^2=\frac{2+\sqrt{3}}{L[C_G+(2+\sqrt{3})C_C]}$$

Appendix II

The solution[14] for the uniformly distributed network, figure 13, is:

$$e=\frac{n}{N}+\sum_{k=1}^{k=\infty}\frac{\dfrac{2\sin\dfrac{k\pi n}{N}}{K\pi}}{\cos k\pi\left(1+\dfrac{k^2\pi^2}{N^2}\dfrac{C_2}{C_1}\right)}\times\cos\frac{t}{\sqrt{\dfrac{L_0NC_1}{k^2\pi^2}+LC_2}}$$

where $k=1, 2, 3$, etc.

Appendix III

Dissymmetrical Lumped Networks

(a). DISSYMMETRY—L'S, C_C'S, AND C_G'S ALL DIFFERENT

In the dissymmetrical three-mesh network, figure 14*B*, again let the subscripts denote the points of the network between which the impedances and the voltages are taken.

$$E_{1G}=\frac{Z_{1G}}{Z_{0G}}=\frac{ap^4+bp^2+c}{dp^4+ep^2+f}$$

where a, b, c, d, e and f are:

$a=L_1L_2L_3C_1(C_2+C_3)$
$b=L_2L_3(C_2+C_3)+L_1C_1(L_2+L_3)$
$c=L_2+L_3$
$d=L_1L_2L_3[(C_1+C_4)(C_2+C_3)+C_2C_3]$
$e=L_1(C_1+C_4)(L_2+L_3)+L_2L_3(C_2+C_3)+L_1L_2C_2+L_1L_3C_3$
$f=L_1+L_2+L_3$

$$E_{1G}=\frac{a}{d}+\frac{\left(\dfrac{bd-ae}{d^2}\right)p^2+\left(\dfrac{cd-af}{d^2}\right)}{p^4+\dfrac{e}{d}p^2+\dfrac{f}{d}}$$

and from

$$\frac{1}{p^4+\dfrac{e}{d}p^2+\dfrac{f}{d}}=\frac{\dfrac{d}{\sqrt{e^2-4fd}}}{p^2+\dfrac{e-\sqrt{e^2-4fd}}{2d}}-\frac{\dfrac{d}{\sqrt{e^2-4fd}}}{p^2+\dfrac{e+\sqrt{e^2-4fd}}{2d}}$$

it follows:

$$E_{1G}=\frac{c}{f}+\left[\frac{bd-ae}{d\sqrt{e^2-4fd}}-\frac{cd-af}{w_1^2d\sqrt{e^2-4fd}}\right]\times\cos w_1t-\left[\frac{bd-ae}{d\sqrt{e^2-4fd}}-\frac{cd-af}{w_2^2d\sqrt{e^2-4fd}}\right]\times\cos w_2t$$

where

$$w_1^2=\frac{e-\sqrt{e^2-4fd}}{2d}$$

and

$$w_2^2=\frac{e+\sqrt{e^2-4fd}}{2d}$$

$$E_{2G}=\frac{Z_{2G}}{Z_{2G}+Z_{12}}\times E_{1G}=\frac{a_1p^4+b_1p^2+c_1}{dp^4+ep^2+f}$$

where

$a_1=L_1L_2L_3C_1C_3$
$b_1=L_2L_3C_3+L_3L_1C_1$
$c_1=L_3$

$$E_{2G}=\frac{c_1}{f}+\left[\frac{b_1d-a_1e}{d\sqrt{e^2-4fd}}-\frac{c_1d-a_1f}{w_1^2d\sqrt{e^2-4fd}}\right]\times\cos w_1t-\left[\frac{b_1d-a_1e}{d\sqrt{e^2-4fd}}-\frac{c_1d-a_1f}{w_2^2d\sqrt{e^2-4fd}}\right]\times\cos w_2t$$

(b). DISSYMMETRY DUE TO MUTUAL INDUCTANCES, FIGURE 14C

Let $M_{12}=M_{23}=M_1=$ mutual inductance between adjacent meshes; and let $M_{13}=M_3$ = mutual inductance between first and third mesh.

The following are six independent equations for the network:

$e_{01}=i_1pL+i_2pM_1+i_3pM_3$
$e_{12}=i_1pM_1+i_2pL+i_3pM_1$
$e_{2G}=i_1pM_3+i_2pM_1+i_3pL$
$1=e_{01}+e_{12}+e_{2G}$
$i_2+pC_Ce_{12}=i_3+p(C_C+C_G)e_{2G}$
$i_1+pC_Ce_{01}=pC_G(e_{12}+e_{2G})+i_2+pC_Ce_{12}$

where the currents are those in the inductance elements. From these relationships the solution is obtained, as follows:

$$e_{1G}=\frac{1}{2}+\frac{1}{2}\,\frac{L^2+LM_3+2LM_1+2M_1M_3}{L^2+LM_3-2M_1^2+2(L+M_1+M_3)^2}-\frac{1}{2}\,\frac{C_G}{C_C+C_G}\cos w_1t+\frac{1}{2}\left[\frac{C_C}{C_G+3C_C}-\frac{L_2+LM_3+2LM_1+2M_1M_3}{L^2+LM_3-2M_1^2+2(L+M_3+M_1)^2}\right]\cos w_2t$$

$$e_{2G}=\frac{(L+M_3)(L+M_3+M_1)}{L^2+LM_3-2M_1^2+2(L+M_3+M_1)^2}-\frac{1}{2}\,\frac{C_G}{C_C+C_G}\cos w_1t-\frac{1}{2}\left[\frac{C_C}{C_G+3C_C}-\frac{L_2+LM_3+2LM_1+2M_1M_3}{L^2+LM_3-2M_1^2+2(L+M_3+M_1)^2}\right]\cos w_2t$$

where

$$w_1^2=\frac{1}{(L-M_3)(C_C+C_G)}$$

$$w_2^2=\frac{1}{(L+M_3)(C_G+3C_C)}\times\left[1+\frac{2(L+M_3+M_1)^2}{L^2+LM_3-2M_1^2}\right]$$

Appendix IV

The foregoing analyses are generally applicable for other combinations of impedances (figure 15). As an example, replace the inductances of figure 1*E* by resistance (R). From appendix I, the operational solution is written immediately and expanded into the same partial fractions, where now $\alpha = pC_GR/pC_CR+1$. Thus the solution in final form is:

$$E_{1G}=$$

$$\frac{5}{6}-\frac{1}{12(2-\sqrt{3})}\frac{C_G}{[C_G+C_C(2-\sqrt{3})]}\epsilon^{-b_1t}-\frac{C_G}{4(C_G+C_C)}\epsilon^{-b_2t}-\frac{C_G}{6(C_G+2C_C)}\epsilon^{-b_3t}-\frac{1}{12}\frac{C_G}{C_G+3C_C}\epsilon^{-b_4t}-\frac{1}{12(2+\sqrt{3})}\times\frac{C_G}{[C_G+C_C(2+\sqrt{3})]}\epsilon^{-b_5t}$$

where

$$b_1=\frac{2-\sqrt{3}}{R[C_G+(2-\sqrt{3})C_C]}$$

$$b_2=\frac{1}{R(C_G+C_C)}$$

$$b_3=\frac{2}{R(C_G+2C_C)}$$

$$b_4=\frac{3}{R(C_G+3C_C)}$$

$$b_5=\frac{2+\sqrt{3}}{R[C_G+C_C(2+\sqrt{3})]}$$

$$E_{2G}=$$

$$\frac{4}{6}-\frac{C_G}{4\sqrt{3}(2-\sqrt{3})[C_G+(2-\sqrt{3})C_C]}\epsilon^{-b_1t}-\frac{C_G}{4(C_G+C_C)}\epsilon^{-b_2t}+\frac{C_G}{12(C_G+3C_C)}\epsilon^{-b_4t}+\frac{C_G}{4\sqrt{3}(2+\sqrt{3})[C_G+(2+\sqrt{3})C_C]}\epsilon^{-b_5t}$$

etc.

Appendix V

Let $e(t)=A+B\cos w_1t+C\cos w_2t$, be the response to unit voltage impressed. It is desired to obtain the response to an impressed voltage having a steep-front and an exponential tail. This can be effected by applying Duhamel's integral, that is, by superposition:

$$\frac{d}{dt}\int_0^t e(t-\lambda)\epsilon^{-a\lambda}d\lambda$$

The response to steep-front exponential tail voltage impressed is then:

$$E_{1G}=\left[A+\frac{a^2B}{a^2+w_1^2}+\frac{a^2C}{a^2+w_2^2}\right]\epsilon^{-at}+\frac{w_1B}{\sqrt{a^2+w_1^2}}\cos(w_1t-\theta_1)+\frac{w_2C}{\sqrt{a^2+w_2^2}}\times\cos(w_2t-\theta_2)$$

where $\tan\theta_1=\dfrac{a}{w_1}$ and $\tan\theta_2=\dfrac{a}{w_2}$.

The above example illustrates the general application of superposition.

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Engineering Features of the 230-Kv Boulder-Chino Transmission Line of the Southern California Edison Co., Ltd.

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THIS paper deals with the problem of transmitting Boulder Dam power to the Southern California Edison Company's system. Preliminary studies were made of this project on the assumption that it should be a high-capacity peak plant with a high degree of reliability of service.[1] It was soon recognized, however, that this power was to be delivered to a system already well equipped for peak demands. This led to a reconsideration of the problem of disposing of this power and the fitting of it into the existing system of the Southern California Edison Company as a base-load-supply project. To justify this base-load operation, it was necessary to keep as low as possible the annual cost of a kilowatt-hour delivered from Boulder Dam to the company's system. However, since this power, 165,000 kw, was about 25 per cent of the present peak load on the system, service reliability of the transmitting facilities was still a major factor. In addition, it was desired to include those features of the peak-plant design which could not be added economically at a later date should the project be converted to a peak-load plant. High inertia constants and low reactance were retained in the design of terminal equipment for such an eventuality.

Electrical Characteristics of Transmission Line

The economic restrictions imposed by the designated base-load method of operation dictated a single-circuit line from Boulder Dam to Chino, Calif., a substation on the eastern boundaries of the company's subtransmission system, 234 miles from the dam, and a single-circuit line from Chino to the Barre substation, near Stanton, Calif., 26 miles from Chino, as shown schematically on figure 1.

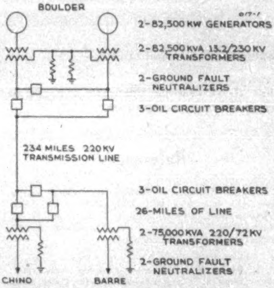
BOULDER
2-82,500 KW GENERATORS
2-82,500 KVA 13.2/230 KV TRANSFORMERS
2-GROUND FAULT NEUTRALIZERS
3-OIL CIRCUIT BREAKERS
234 MILES 220 KV TRANSMISSION LINE
3-OIL CIRCUIT BREAKERS
26-MILES OF LINE
2-75,000 KVA 220/72 KV TRANSFORMERS
2-GROUND FAULT NEUTRALIZERS
CHINO
BARRE

Figure 1. Schematic single-line diagram of the Boulder-Chino-Barre 230-kv transmission line

Two 82,500-kva 13,800-volt waterwheel generators furnish power to this line through two 82,500-kva 13.2/230-kv banks of transformers consisting of six single-phase transformers, each rated 27,500 kva and located in the Arizona wing of the dam. A bank of transformers rated 75,000 kva, 220/72 kv consisting of three single-phase units each rated 25,000 kva is located in each of the receiving-end substations at Chino and Barre.

The 230-kv operating voltage of the line was established from a consideration of steady-state stability limits, line losses, and voltage regulation. The electrical characteristics of the final design of the transmission line are summarized in table I.

Table I. Electrical Design Characteristics of Transmission Line

1.	Steady-state power limit........	180,000 kw
2.	Operating voltage at Boulder....	230 kv
3.	Operating voltage at Chino......	216–220 kv
4.	Total line loss at 165,000 kw.....	19,000 kw
5.	Charging current...............	48,000 kva
6.	Charging current loss...........	600 kw
7.	Corona loss not over............	500 kw
8.	Line efficiency at 165,000 kw (per cent)......................	89
9.	Annaul efficiency at 900,000,000 kw-hr (per cent)...............	92
10.	Corona loss with one wire grounded—not over............	25,000 kw

The size of the line conductor was determined from economic studies of annual cost of load losses, charging current losses, corona losses, and annual fixed charges against cost of conductors and steel towers designed for various sizes and types of conductors. The annual loss was computed by an empirical formula determined from studies of load curves and corresponding losses, this formula showing the following relation between load factor of loss (y) and load factor of load (x):

$$3y = 4x^2 - 2x + 1$$

Charging-current loss and corona loss are constant, and produce an annual loss of 8,760 kilowatt-hours per kilowatt of loss. The value of a kilowatt-hour of loss and the rate of annual fixed charges on investment are of course parameters for each specific problem of transmission-line design. The over-all lowest annual cost for the Boulder-Chino transmission line was given by a 605,000-circular-mil aluminum-conductor steel reinforced conductor. The remarkable result of the study was the fact that this conductor gave the lowest annual cost for the operation of the contemplated installation of 165,000 kw at any desired annual load factor. This is due chiefly to the rapid increase in corona loss on conductors of smaller diameter or the higher cost of supporting structures when reduced carrying capacity is obtained without reducing the diameter of the conductor. A 605,000-circular-mil aluminum conductor, steel reinforced, cable composed of 30 strands of 0.1420 aluminum conductors and 19 strands of 0.0852 steel was selected for the line.

The transmission line has a horizontal configuration with a spacing of 23 feet and an average height of 45 feet. Two one-half-inch seven-strand steel overhead ground wires are located midway between and 20 feet above the line conductor plane as shown on figure 2.

Tests were made at the Chino substation on May 14, 1939, to check the no-load characteristics of the 234 miles of line from Chino to Boulder Dam. During these tests the Boulder Dam transformer banks were disconnected, likewise the Chino-Barre line. The high-side terminal voltages were metered by means of six capacitor-type potential devices connected directly to the terminal of each line conductor. The line currents at Chino were measured by current transformers

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[1] For all numbered references, see list at end of paper.

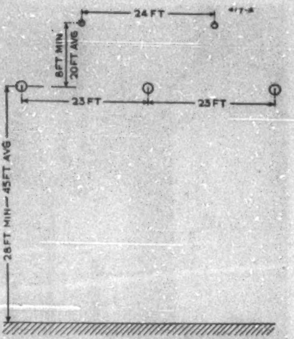

Figure 2. Configuration of transmission-line conductors, Boulder-Chino-Barre 230-kv transmission line

and ammeters located in each high-voltage transformer winding near the neutral end. All other measurements were taken on the 72-kv side of the transformer bank at Chino, and include the losses of the Chino transformers. Four line excitation tests were made at voltages both above and below the operating voltage, the results of which are shown on table II.

Table II. Excitation Characteristics of Boulder-Chino Line and Chino Transformer on 72/220-Kv Ratio—Frequency 50 Cycles, 234 Miles of Overhead Line

Item	1	2	3	4
Line-to-neutral kv at Chino	86	103	123	143
Line-to-neutral kv at Boulder	94	113	135	156
66-kv bus kv at Chino	46	55	65	75
Line amperes at Chino	89	110	132	150
Low-voltage amperes at Chino	285	345	400	450
Loss in kilowatts	600	830	1,470	4,650

Service Protection Features

Reliability of service requirements on this line were met (1) by the installation of two overhead ground wires, (2) by the installation of counterpoises at towers where measurements indicated tower-footing resistances in excess of 100 ohms, and (3) by the installation of ground-fault neutralizers at the terminals of the line.

The installation of two overhead ground wires was based on a study of the performance of the 132-kv transmission line of the Nevada-California Power Corporation from Boulder Dam to Victorville, Calif. From the five-year record of this line, and with the help of the excellent data and literature accumulated by the AIEE committee on power transmission and distribution, it was possible to calculate the lightning performance of the proposed transmission line with varying degrees of lightning protection. It was estimated that less than one flashover a year could be expected with the line equipped with two overhead ground wires. This flashover would be limited to a single line-to-ground type of flashover if tower-footing resistances were reduced to 100 ohms or less.

Measurements of tower-footing resistance were taken at four-mile intervals to determine the general levels of tower-footing resistance along the line, and subsequently tower-footing resistances were measured at all towers in the high-resistance regions. Counterpoises of 200 feet, parallel to the line in both directions, were installed at all towers exceeding 100 ohms in tower-footing resistance. As a result, the maximum tower-footing resistance measured after 54 counterpoises had been installed did not exceed 70 ohms. The average tower-footing resistance is in the order of 18 ohms. It is estimated that the installation of two overhead ground wires and the lower tower-footing resistances will limit lightning flashovers to the single conductor-to-ground type.[2]

Operating experiences on this company's other 230-kv lines indicate that faults from causes other than lightning may also be expected from time to time. The majority of these other troubles will probably involve only one conductor and ground and the remaining problem of design was to provide means to mitigate the disturbances resulting from single conductor-to-ground faults. The ground-fault neutralizer (Petersen coil) was a service protective device which was available for dealing with the single line-to-ground type of fault. The application of ground-fault neutralizers to a 230-kv 260-mile, transmission line designed to carry load near its rated steady-state stability limit and at the lowest annual cost obtainable per kilowatt-hour is a unique feature of this transmission project which will be discussed in detail.

General Application Requirements for Ground-Fault Neutralizers

The Boulder-Chino 230-kv transmission line was not to be metallically interconnected with the other 230-kv lines on this system. The Chino and Barre substations were on the Southern California Edison Company's subtransmission system, so step-down transformers were required at these locations. Thus, the new line was a complete electrical unit unto itself which eliminated the necessity of purchasing additional isolating transformers, a contributing factor to make the application of neutralizers feasible economically.

All new electrical apparatus was to be purchased for the line so it could be specified to have the necessary insulation levels required on a ground-fault-neutralizer-equipped system. A ground fault on such a system causes the two ungrounded phases to shift to approximately the normal system line-to-line voltage above ground and the system neutral to shift to approximately leg voltage above ground. Accordingly, the station-type lightning arresters at Boulder Dam were specified for full line-to-line voltage and have a maximum permissible line-to-ground voltage rating of 242 kv. The power-transformer banks at Boulder, Chino, and Barre were specified to have neutral insulation in the 138,000-volt class for operation with ground-fault neutralizers connected from their neutrals to ground.

Application and Design

The neutralizer current ratings were derived from calculations made of the electrical characteristics of the 230-kv transmission line.[3] A study was made which covered the (1) determination of the charging current in the fault when one conductor is grounded, (2) determination of the need for line transpositions, and (3) determination of the in-phase component of current in the fault.

The ground-fault neutralizers were designed to pass during a line-to-ground fault on the line sufficient lagging current to compensate the leading charging current in the fault. The charging current in a ground fault was estimated using the principles of symmetrical components and Maxwell's potential coefficients modified to include the effects of corona applied to the configuration and profile of the Boulder-Chino line shown on figures 2 and 3. For 234 miles of 230-kv line, the estimated charging current was 301 amperes, 50 cycles. This figure included the effect of the formation of corona on the conduc-

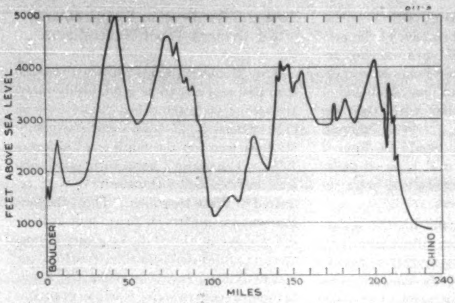

Figure 3. Profile of Boulder - Chino-Barre 230-kv transmission line

tors, which in this case, increased the current by approximately 3.5 per cent. These calculations showed that the charging current in the ground fault would be of the order of 2.17 times the normal three-phase charging current.

It was expected from experiences available on lower-voltage neutralizer circuits that the measured current would be 5 to 10 per cent higher than the current calculated for the transmission line alone. This increase in the current is derived from the capacitance in the towers, line insulators, transformers, and other connected electrical apparatus. Computations made from the results of the tuning tests of May 14, 1939, indicate the current to be 314 amperes, 50 cycles, for 234 miles of line.

The neutralizers in tune with the system would form a series resonant circuit for any normal unbalance in the system neutral-to-ground voltage. In a circuit like this, the normal neutral-to-ground voltage present with the system neutrals ungrounded would be amplified probably at least 15 times, and calculations were made to determine if, with the neutralizers in service, the displacement in the neutral voltage would be greater than could be reasonably tolerated. Due to the dissymmetry of the capacitance of the three-phase conductors to ground, it was found advisable to transpose the line. Transposition of conductors is also required in California to avoid interference with telephone communication circuits, and three transposition barrels were incorporated in the design of the line. The tests showed that the line was well balanced as the neutral-to-ground voltage with the neutralizers in service was less than 1,000 volts.

It was expected that the uncompensated fault current on this system resulting from corona loss would be higher than on any other existing neutralizer-equipped system in this country to date. The corona loss, under conditions of one wire at ground potential and the other two at line potential to ground, was computed to be 15,800 kw by means of Peek's formulas[4] for 234 miles of line of the conductor size selected and the lines transposed. The unsuppressed fault current corresponding to this corona loss was 59.4 amperes, a current which it was believed would not maintain the arc with the arcing distances involved. The arcing tests to be described later justified this contention. The measured fault current on solid ground faults was between 60 and 70 amperes on the 234-mile section of line.

It was planned to construct the Boulder-Chino line first and later extend it to the Barre substation when additional power became available at the dam. Thus, the neutralizers were to be suitable for compensating 234 miles of line when first installed and later 260 miles of line.

Ground-fault neutralizers were planned for the Boulder Dam, Chino, and Barre transformer neutrals. The Chino and Barre neutralizers were designed to compensate, each for one-third of the total length of line. The Boulder neutralizer has a double winding mounted on a single magnetic core, each winding designed for operation either singularly or in multiple with the other and to compensate for a third of the total line length. The double winding feature is to permit retuning by the station operator at Boulder Dam in the event either the Chino or Barre transformer bank is out of service by the simple expedient of switching the second winding into service. Disconnecting switches, mounted on the cover of the Boulder neutralizer are available for this purpose. Installation photographs of the Boulder, Chino, and Barre neutralizers are shown on figures 4, 5, and 6.

These neutralizers were specified to have a current range of 100 to 135 amperes at 50 cycles with 20 per cent additional current capacity at 60 cycles. Tap

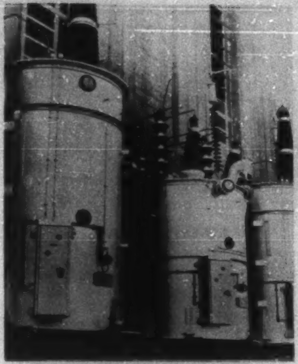

Figure 4. Ground-fault neutralizer, 230 kv, 100/135 or 200/270 amperes, 50 cycles (center) on line of Southern California Edison Company, Ltd., at Boulder Dam

changers were provided for manual operation to adjust the impedance to give approximately ten-per-cent increments from minimum to maximum of current rating. Tap-changer handles were located on the side of the tank convenient for making adjustments with provision for locking. The neutralizers were suitable for carrying rated current on any tap for ten minutes without exceeding the temperature limits established by the AIEE.

Each neutralizer was equipped with a 138-kv bushing on the transformer end of the winding and a 15-kv bushing on the

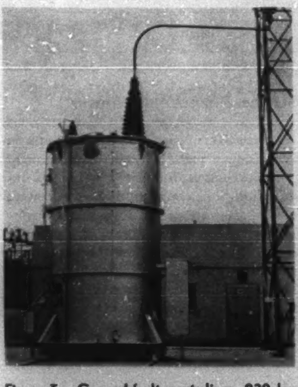

Figure 5. Ground-fault neutralizer, 230 kv, 100/135 amperes, 50 cycles, in Chino substation of the Southern California Edison Company, Ltd.

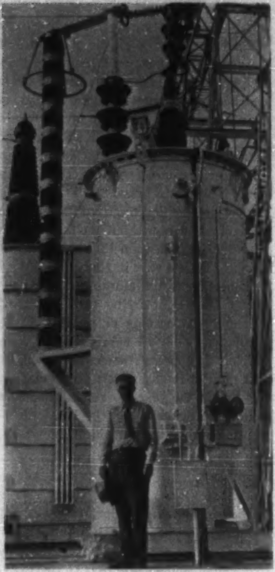

Figure 6. Ground-fault neutralizer, 230 kv, 100/135 amperes, 50 cycles, in Barre substation of the Southern California Edison Company, Ltd.

ground end and given an induced voltage test with the ground end solidly grounded of 277 kv at 420 cycles for 18 cycles.

The Boulder and Chino neutralizers were equipped with Thyrite by-pass resistors to protect them from abnormal voltages which might reach them through the transformers in the neutrals of which they were connected. The Barre neutralizer, furnished at a later date, has a Thyrite station-type lightning arrester mounted on the side of the tank.

The Boulder Dam neutralizer is one of the largest reactors ever built. An interior view of it showing the built-in resistor is shown on figure 7. Some idea of its physical size may be judged from the single-phase 27,500-kva 230-kv transformers adjacent to it on figure 3.

The current and ohmic characteristics of these neutralizers are given in table III.

Table III. Characteristics of the Ground-Fault Neutralizers for Use in the Neutrals of the 230,000-Volt Boulder-Chino-Barre Line

Ohms Reactance	Amperes for Ten Minutes	Tap Position
50-cycle connections		
930	139	3
1,022	127	4
1,080	120	5
1,137	114	6
1,215	107	7
1,300	100	8
1,370	95	9
1,520	86	10
60-cycle connections		
787	162	1
835	153	2
890	144	3
935	139	4
995	129	5
1,045	123	6
1,100	117	7

Auxiliary Equipment

The auxiliary equipment on each of these neutralizers consists of an instantaneous overcurrent relay, a timing relay, and an operation-recording device which at Boulder is an automatic oscillograph. No automatic switch or oil circuit breaker is furnished for short-circuiting the neutralizers and solidly grounding the transformer neutrals in case of permanent ground faults. Since there is a single line from Boulder to Chino, the operating procedure during permanent ground faults will be to keep the line energized up to possibly the full time rating of the neutralizers so that the power which is to be lost upon opening this line can be picked up on the other generating stations on the system. It is expected that this added feature of being able to operate with this line grounded for several minutes will greatly reduce the number of disturbances to the system which would otherwise occur if the line were tripped out immediately upon the occurrence of a ground fault.

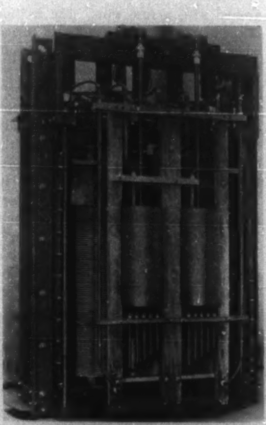

Figure 7. Interior view showing assembled core and coils of ground-fault neutralizer shown in figure 4

Tuning Tests

The Boulder and Chino neutralizers were tuned to the system on May 14, 1939. At the time of these tuning tests, construction work on the 26-mile line from Chino to the Barre substation was not completed so all of the testing was done on the Boulder Dam-Chino 234-mile line. Thus, for these tuning tests, both windings of the Boulder Dam neutralizer were in service.

The transmission line was energized from the Chino end and with this system setup, it was necessary to tune both neutralizers as a unit with the line. The Boulder Dam neutralizer tap positions were predetermined by calculation and tuning curves taken on the Chino neutralizer in two different ways as described in detail in the technical literature.[5]

First, with the system normal: Each winding of the Boulder neutralizer was set on tap number 7 and the amperes through the Chino neutralizer recorded for successive tap positions. The neutralizers and the system are in tune when the current is the maximum. This was tap number 8 on the Chino neutralizer with a current of 0.7 ampere. This current is produced by the slight unbalance in the phase voltages to ground and the relatively low magnitude of the current shows that the transmission line is well transposed.

Second, with one line solidly grounded: The Boulder Dam neutralizer was set on tap number 7 as on the previous test and the current in the ground fault was recorded for different tap positions on the Chino neutralizer. The tuned position was given by minimum fault current, which was tap number 8 on the Chino neutralizer. The minimum current in the fault varied between 60 and 70 amperes rms and results mostly from corona loss, although resistance and leakage loss, and harmonics, also contribute to it.

The Barre neutralizer was tuned to the system on September 19, 1939, by which time the Barre line extension from Chino and the second 82,500-kva generator at Boulder Dam were ready for service. The transmission line was energized from Boulder Dam and carrying the full output of one generator during the tests. For these tests, only one of the Boulder neutralizer windings was in service and with the Boulder neutralizer set on tap number 7 and Chino neutralizer set on tap number 8, tuning curves were made on the Barre unit. The tap position on the Barre unit giving the maximum current for normal tuning conditions was tap number 3 and likewise this same tap position gave the minimum current on fault tuning. The uncompensated fault current during these tests was of the order of 80 amperes.

It is to be noted that the two methods of tuning gave the same tap position. This indicates that corona did not increase the line capacitance sufficiently so that it could be detected within the tuning limits set by the spacing of the taps on the neutralizers.

Arcing Tests

After the tuning tests with system normal were completed, arcing faults were put on the system to check and demonstrate the performance of the neutralizers. For the May 14 tests an automatic magnetic oscillograph was installed at Chino, connected to record the system line-to-ground voltages, neutralizer, and fault currents. Arcing faults to ground were made by short-circuiting a 14-unit insulator string suspended from a spur line in the Chino substation yard. A 0.5-ampere fuse wire and also a cotton string soaked in a salt solution were used to initiate the arc. These were suspended from a weighted cotton cord looped over the transmission line by which means the fuse was drawn into position to span the insulator string.

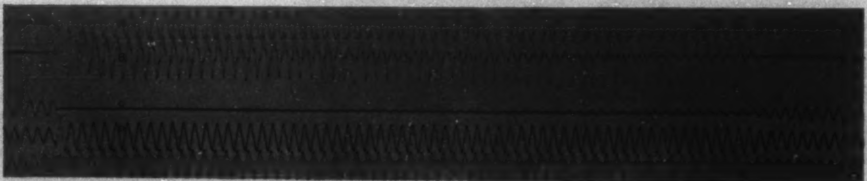

Figure 8. Oscillogram of ground-fault-neutralizer operation during staged arc to ground in Chino substation of Southern California Edison Company, Ltd., on May 14, 1939

A—Ground-fault-neutralizer current, 100 amperes rms

B—Fault current, 60 amperes rms

C—Line-to-ground voltage, phase *A*, 132 kv rms

D—Line-to-ground voltage, phase *B*, 132 kv rms

E—Line-to-ground voltage, phase *C*, 132 kv rms

A series of eight tests were made and in each case, the arc to ground was suppressed without any system disturbance or oil-circuit-breaker operations. The arcing time varied from the minimum of 0.86 second to the maximum of 3.76 seconds. These results are summarized in table IV.

Table IV. Arcing Tests

Arc to Ground Over 14-Disk Insulator String With Ground-Fault Neutralizers in Service

Boulder Neutralizer Tap Position Winding 1	Boulder Neutralizer Tap Position Winding 2	Chino Neutralizer Tap Position	Barre Neutralizer Tap Position	Arcing Time (Seconds)
Chino tests—May 14, 1939; 234 miles of line in service				
7	7	8	Not in service	3.76
7	7	8		1.20
7	7	8		1.78
7	7	8		0.86
7	7	8		1.46
7	7	8		1.36
7	7	8		1.44
7	7	8		1.28
Boulder tests—September 19, 1939; 260 miles of line in service				
7	Not in service	8	3	2.45
7		8	3	1.70

For the September 19 tests, the arcing faults were initiated by closing in with one of the 230-kv oil circuit breakers on a test setup located in the Boulder Dam switch yard consisting of a 14-unit string of insulators which was short-circuited to ground with a fuse wire. The melting of the fuse wire started the arc to ground which was extinguished by the ground-fault neutralizers. The various system electrical quantities during the tests were recorded by the automatic oscillograph which was part of the regular auxiliary equipment of the Boulder neutralizer.

Two arcing faults were put on the system in this manner and were quenched in 2.45 seconds and 1.70 seconds respectively. These data are included in table IV.

At the time of both tests, the weather was fair with little or no wind. Visual observations of the arcs indicated that they were very mild in character, showing little of the fire, noise, and elastic properties of the usual arcing fault on a grounded-neutral system. It is probable that if there had been any appreciable movement of the air in the vicinity of the arc, it would have been quenched in even shorter time than was recorded.

An oscillogram of one of the arcing faults is shown in figure 8. The smooth recovery of the line-to-ground voltage of the faulted phase without oscillations is a very definite indication that the neutralizers and system were in tune.

Inspection of the aluminum line conductor after eight arcing tests disclosed absolutely no burning or pitting of the conductor. Only a few bright spots were visible. It appears reasonable to expect that most of the flashovers to ground on this line in the years to come will leave insufficient evidence for the line patrolmen to discover the fault location.

Summary

The economic restrictions of base-load operation plus the necessity for a high degree of reliable service owing to the relatively large amount of power to be transmitted, has led to the design of a single-circuit transmission line with two overhead ground wires, counterpoises, and ground-fault neutralizers with an expected service continuity record closely approaching that which heretofore was obtainable only by two transmission lines located on separate rights-of-way with crossovers and paralleling switching stations.

With this new design, flashovers caused by lightning are expected to be reduced to single line-to-ground faults by the two overhead ground wires and counterpoises and suppressed by the ground-fault neutralizers. Permanent ground faults are not to be removed immediately, but are to be allowed to remain on the line until the load can be backed off the Boulder Dam generators. This alleviates the shock to the system which might otherwise occur if the load was rejected immediately. Operating in this manner is

Surge Characteristics of a Buried Bare Wire

E. D. SUNDE
ASSOCIATE AIEE

BURIED bare wires are frequently used as grounds or counterpoises for transmission-line towers and also as shield wires for underground rubber-covered telephone wires. Experience has verified the effectiveness of such wires in reducing insulator flashovers for direct strokes to transmission towers and breakdowns of underground rubber-covered wires due to surges. Quantitative relations between surge characteristics of the wires and various fundamental constants are, however, incomplete. Theoretical considerations of surge characteristics appear to have been confined to empirical approximations of test results[1,2,3] and do not reveal the relation to fundamental constants, such as the earth resistivity and dielectric constant of the earth.

In the following theoretical investigation, a previous study of the propagation of alternating currents along conductors with large leakage to ground[4] is extended to include the propagation of transient impulses, and the results are applied to the calculation of certain cases of practical interest. The paper is divided in four parts, the first dealing with propagation of alternating and square-front (unit step) current waves in wires of infinite length, the second with surge impedances under the same conditions. The third part considers current propagation and surge impedances for unit step current and wires of finite length and the fourth part, impedances for surge currents of shape similar to that encountered in case of lightning.

In obtaining working formulas it has been expedient to make certain approximations not impairing the fundamental aspects of the problem. These formulas show that when unit step current enters the mid-point of the wire, the surge impedance rises within a small fraction of a microsecond from zero to a maximum value which depends on the dielectric constant and the resistivity of the earth. From this maximum value the surge impedance decreases nearly inversely with the square root of the time until current reflected at the end of the wire returns to its mid-point. From then on the surge impedance approaches the leakage resistance of a wire in perfect contact with the earth, which is somewhat smaller than the leakage resistance as measured with direct or low-frequency current. For currents produced by lightning the surge impedance is of a similar form and in satisfactory agreement with measured impedances.

I. Current Propagation

Sinusoidal Currents

The wire is assumed to be half buried, that is, with its axis in the plane of the earth's surface, and to be in perfect contact with the earth. It is assumed to have infinite length and negligible internal impedance. For the steep wave fronts usually encountered, the assumption of negligible internal impedance is permissible and simplifies the problem considerably. Displacement currents in the air are neglected in comparison with those in the ground, a simplification which is permissible on account of the comparatively large dielectric constant of the ground.

As shown in the paper referred to above,[4] the propagation of sinusoidal currents and potentials along the conductor when energized at its mid-point is nearly exponential, except for distances from the origin so large that the current is inappreciable. For a conductor of negligible internal impedance in perfect contact with the ground, the current at the distance x from the mid-point of the counterpoise is

$$I(x,i\omega)/I(0,i\omega)=e^{-\Gamma x} \qquad (1)$$

where $2I(0,i\omega)$ is the current entering the counterpoise, and

$$\Gamma=\frac{1}{v}\left[i\omega(1/\rho\kappa+i\omega)\right]^{1/2} \text{ per meter}$$

ν = earth inductivity = $\mu\cdot 1.256\cdot 10^{-6}$ henries per meter
κ = earth capacitivity = $\epsilon\cdot 8.858\cdot 10^{-12}$ farads per meter
ρ = earth resistivity in meter-ohms
v = velocity of propagation = $(2/\nu\kappa)^{1/2}$ meters per second
$\epsilon \gg 1$ = dielectric constant of earth
$\mu = 1$ = permeability of earth

The notation $I(x,i\omega)$ is employed to indicate dependence on frequency, in order later to extend the application to nonsinusoidal conditions.

The above formula applies when the dielectric constant of the earth is much greater than that of the air, as is usually the case. The dielectric constant of the earth varies between wide limits, and is usually about 4 for dry ground, while for wet ground it may vary from 10 to 40.[5] Correspondingly, the velocities of propagation vary between 0.7 and 0.2 times the velocity in free space. Observations on counterpoises have indicated velocities within these limits.

The above expression for the propagation constant is obtained by solving the following equation (reference 4, page 1340, approximate formula)

$$\Gamma=[G(\Gamma)Z(\Gamma)]^{1/2}$$

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[1] For all numbered references, see list at end of paper.

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possible with ground-fault neutralizers in the power-transformer neutrals.

The installed cost of the ground-fault neutralizers which permits the single-circuit line to approach the double-circuit line in performance, is less than five per cent of the cost of the second line.

The successful suppression of the line-to-ground arcing faults demonstrates conclusively that ground-fault neutralizers will function on 230-kv circuits with in-phase components of current of the order of 80 amperes.

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Discussion

For discussion, see page 1176.

where, in case of negligible internal impedance and perfect contact between wire and ground:

$$G(\Gamma)^{-1}=\int_{-\infty}^{\infty} Q(r)e^{i\Gamma v}dv,$$

$$Z(\Gamma)=i\omega\int_{-\infty}^{\infty} P(r)e^{i\Gamma v}dv$$

where $r=(v^2+a^2)^{1/2}$, a being the radius of the conductor. The functions $P(r)$ and $Q(r)$ depend on the earth resistivity, the dielectric constant of the earth and the air, and on the depth of the conductor below the surface of the earth. When the conductor is at the surface of the earth and displacement currents are neglected in the air, corresponding to zero dielectric constant of the air, the following approximate expressions are obtained:*

$$\left.\begin{aligned} G(\Gamma)^{-1}&=\frac{i\omega\nu}{\pi\gamma^2}\log_e\frac{1.12}{a\Gamma} \\ Z(\Gamma)&=\frac{i\omega\nu}{2\pi}\log_e\frac{1.85}{a(\Gamma^2+\gamma^2)^{1/2}} \end{aligned}\right\}\qquad(2)$$

where $\gamma=(i\omega\nu/\rho=\omega^2\nu\kappa)^{1/2}$ is the propagation constant of a plane wave in the earth.

Therefore:

$$\Gamma^2=\frac{1}{2}\gamma^2\frac{\log 1.85/a(\Gamma^2+\gamma^2)^{1/2}}{\log 1.12/a\Gamma}\qquad(3)$$

By inspection it is seen that $\Gamma^2\cong\gamma^2/2$; inserting this value of Γ in the logarithmic terms the expression becomes:

$$\Gamma^2=\frac{1}{2}\gamma^2\frac{\log 1.52/a\gamma}{\log 1.58/a\gamma}\qquad(4)$$

The ratio of the logarithmic terms is practically unity for all frequencies up to several million cycles, so that for all practical purposes $\Gamma=\gamma/\sqrt{2}$. For comparison it may be noted that for a conductor imbedded at infinite depth the propagation of current is strictly exponential and that $\Gamma=\gamma$ (reference 4, page 1343). In the latter case the velocity of propagation is $v=(1/\nu\kappa)^{1/2}$ as compared with $v=(2/\nu\kappa)^{1/2}$ for a conductor at the surface of the earth.

Unit Step Current

The propagation of currents of arbitrary wave shape entering the wire may be formulated in terms of the propagation of a unit step current equal to 0 for $t<0$ and unity for $t>0$. Consideration of a current of this form, which may be regarded as an ideal surge current, also reveals certain fundamental characteristics of wave propagation along buried wires. For a unit step current the operational expression for the current at x is obtained by replacing $i\omega$ by p in (1). The solution of the operational expression thus obtained is:[6]

For $vt<x$: $i(x,t)/i(0,t)=0$

For $vt>x$:

$$\begin{aligned} i(x,t)/i(0,t)&=e^{-\alpha x}+\alpha x\int_x^{vt}\frac{e^{-\alpha u}}{w}I_1(\alpha w)du \\ &=1-\alpha x\int_{vt}^{\infty}\frac{e^{-\alpha u}}{w}I_1(\alpha w)du \qquad(5)\end{aligned}$$

where $\alpha=\rho\kappa v/2=(2\nu/\kappa)^{1/2}/4\rho$, $w=(u^2-x^2)^{1/2}$, t the time in seconds and I_1 the Bessel function of the first kind and order for imaginary arguments. The second expression is obtained from the first by using the condition $i(x,t)/i(0,t)=1$ for $tv=\infty$.

When $\alpha vt-\alpha x<1$ the following formula may be used:

$$i(x,t)/i(0,t)=e^{-\alpha x}+\frac{\alpha x}{2}(e^{-\alpha x}-e^{-\alpha vt})\qquad(6)$$

When $\alpha vt-\alpha x>1$ and $vt\gg x$ the following formula is sufficiently accurate:

$$\begin{aligned} i(x,t)/i(0,t)&=erfc[\alpha x(2\alpha vt)^{-1/2}] \\ &=erfc\ [x(v/8\rho t)^{1/2}] \qquad(7)\end{aligned}$$

where

$$erfc(z)=1-erf(z)=\frac{2}{\sqrt{\pi}}\int_z^{\infty}e^{-s^2}dz$$

is the error function complement.

Expression (6) is obtained by noting that when $\alpha w<1$, $I_1(\alpha w)\sim\alpha w/2$; while (7) is derived as follows:

When $\alpha vt-\alpha x>1$, $I_1(\alpha w)\sim(2\pi\alpha w)^{-1/2}e^{\alpha w}$

and when

$vt\gg x$, $I_1(\alpha w)\sim(2\pi\alpha u)^{-1/2}e^{\alpha u-\alpha x^2/2u}$

Hence:

$$\begin{aligned} i(x,t)/i(0,t)&=1-\alpha x(2\pi\alpha)^{-1/2}\times \\ &\qquad\int_{vt}^{\infty}u^{-3/2}e^{-\alpha x^2/2u}du \\ &=1-\frac{2}{\sqrt{\pi}}\int_0^{Z}e^{-s^2}dz \\ &=erfc[\alpha x(2\alpha vt)^{-1/2}]\end{aligned}$$

where $Z=\alpha x(2\alpha vt)^{-1/2}$.

Curves of the ratio $i(x,t)/i(0,t)$ are shown in figure 1. When $vt>2x$, the current ratio as given by (7) is quite accurate.†

II. Infinite-Length Impedances

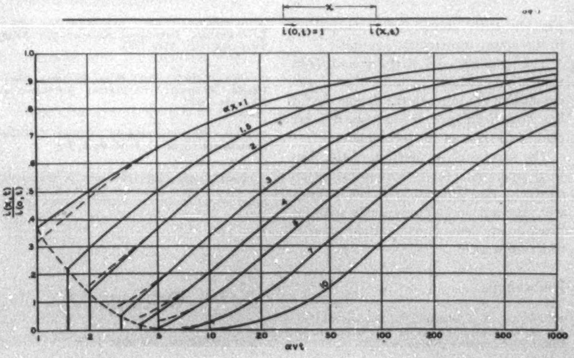
Sinusoidal Current

The impedance to ground of the counterpoise, as measured between its mid-point and a remote point, is in the following denoted by $Z(a,i\omega)$. The mutual impedance of counterpoise-to-ground and transmission-line-to-ground circuits is

Figure 1. Propagation of unit step current in a buried wire of infinite length

Solid curves represent equation 5, dashed curves equation 7

t = time in seconds
$\alpha=(2\rho\kappa v)^{-1}$; numeric per meter
ρ = earth resistivity, meter-ohms
κ = earth capacitivity
$=\epsilon\cdot 8.856\times10^{-12}$ farads per meter
ϵ = dielectric constant of earth
v = velocity of propagation
$=(2/\nu\kappa)^{1/2}$ meter per second
ν = earth inductivity
$=1.256\times10^{-6}$ henries per meter



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* In the paper referred to displacement currents were neglected also in the earth, and the formula for $G(\Gamma)$ written in a different form, which is identical with that below when $\gamma=(i\omega\nu/\rho)^{1/2}$.

The present formulas are based on expressions for $P(r)$ and $Q(r)$ with displacement currents neglected in the air, obtained from: "Mutual Impedance of Grounded Wires for Stratified Two-Layer Earth" *Bell System Technical Journal*, volume 12, April 1933, pages 162–7.

† It may be of interest in connection with the propagation of surges along transmission lines to observe that the same formulas and curves apply to the voltage ratio $e(x,t)/e(0,t)$ for an infinitely long line on which a unit step voltage $e(0,t)$ is impressed at some point. In that case $\alpha=(R/2)(C/L)^{1/2}$ and R, L, C the unit length resistance, inductance, and capacity of the line, which is assumed to have negligible leakance.

denoted by $Z(h,i\omega)$ and equals the ratio of transmission line voltage to current entering the counterpoise, the voltage being taken between a point on the transmission line opposite the counterpoise mid-point and a remote point. Assuming exponential current propagation, both impedances are obtained from the following expression by taking y equal to the radius of the counterpoise (a) and the height of the transmission line (h), respectively:

$$Z(y,i\omega)=\frac{i\omega\nu}{4\pi\Gamma}\int_0^\infty\frac{e^{-\Gamma x}dx}{(x^2+y^2)^{1/2}} \qquad (8)$$

$$=\frac{i\omega\nu}{8\Gamma}\left[H_0(\Gamma y)-Y_0(\Gamma y)\right]$$

where the distance y is in meters and

H_0 = Struve's associated Bessel function of first kind, zero order

Y_0 = Bessel function of second kind, zero order

The above integral represents the ratio of earth potential at the perpendicular distance y from the mid-point of the counterpoise to the current entering the latter. This earth potential arises from currents $-I'(x,i\omega)=\Gamma e^{-\Gamma x}$ leaving the counterpoise along its length, the earth potential at y due to unit current leaving the counterpoise at x being $i\omega\nu/2\pi\gamma^2(x^2+y^2)^{1/2}=i\omega\nu/4\pi\Gamma^2(x^2+y^2)^{1/2}$. Since the counterpoise is in intimate contact with the earth, the earth potential at $y=a$ equals the counterpoise potential. In obtaining $Z(a,i\omega)$ from the above expression, the internal impedance of the counterpoise is neglected and in obtaining $Z(h,i\omega)$ a negligible error is committed by assuming the mutual impedance the same for a horizontal separation $y=h$ as for an equal vertical separation. The latter condition is true only when displacement currents are neglected in the air, as assumed here, the equipotential lines in that case being circles about the conductor.

Unit Step Current

For unit step current the operational expression for the self- and mutual impedances defined by (8) is obtained by replacing $i\omega$ by p. For large values of time displacement currents may be neglected and the solution for the surge impedance is:

$$S(y,t)=\frac{1}{4\pi}\left(\frac{\nu\rho}{2\pi t}\right)^{1/2}\exp\,(y^2\nu/16\rho t)\times K_0(y^2\nu/16\rho t) \qquad (9)$$

where K_0 is a Bessel function of the second kind for imaginary arguments, zero order. For $z\cong y^2\nu/16\rho t<0.10$, which is usually the case in applications, $K_0(z)\cong\log_e(1.12/z)$. For larger values of z tables of this function are available.[7]

For small values of time displacement currents must be included and the surge impedance is obtained from the expression:

$$S(y,t)=\frac{(2\nu/\kappa)^{1/2}}{4\pi}e^{-\alpha vt}\left\{\left[1+\left(\frac{\alpha vt}{2}\right)^2+\frac{1}{2}\left(\frac{\alpha y}{2}\right)^2\right]\log_e\frac{tv+(t^2v^2+y^2)^{1/2}}{y}-\frac{1}{2}\left(\frac{\alpha vt}{2}\right)^2(1+y^2/t^2v^2)^{1/2}\right\} \qquad (10)$$

where $\alpha=(2\nu/\kappa)^{1/2}/4\rho$. Expression (9) may be used when $\alpha vt>1$ and (10) when $\alpha vt<1$.

The above results are obtained by observing that:

$$\frac{dZ(y,p)}{dx}=\frac{\nu}{4\pi}(x^2+y^2)^{-1/2}\frac{p}{\Gamma}e^{-\Gamma x} \qquad (11)$$

The solution of this operational expression is:[8]

$$\frac{dS(y,t)}{dx}=0 \text{ when } t<x/v$$

$$\frac{dS(y,t)}{dx}=\frac{(2\nu/\kappa)^{1/2}}{4\pi(x^2+y^2)^{1/2}}e^{-\alpha vt}\times I_0[\alpha v(t^2-x^2/v^2)^{1/2}] \text{ when } t>x/v \qquad (12)$$

where I_0 is the Bessel function of the first kind for imaginary arguments, zero order. Thus:

$$S(y,t)=\frac{1}{4\pi}(2\nu/\kappa)^{1/2}e^{-\alpha vt}\times\int_0^{vt}\frac{I_0[\alpha v(t^2-x^2/v^2)^{1/2}]}{(x^2+y^2)^{1/2}}dx \qquad (13)$$

For $\alpha vt<1$,

$$I_0[\alpha v(t^2-x^2/v^2)^{1/2}]\cong 1+\frac{\alpha^2v^2(t^2-x^2/v^2)}{4}$$

and inserting this value for I_0 expression (10) is obtained by integrating (13).

For $\alpha vt\gg 1$ expression (9) is obtained by observing that

$$I_0[\alpha v(t^2-x^2/v^2)^{1/2}]\rightarrow(2\pi\alpha vt)^{-1/2}\times\exp\alpha vt\,(1-x^2/2vt^2)$$

so that:

$$S(y,t)=\frac{1}{2\pi}(\nu\rho/2\pi t)^{1/2}\int_0^\infty\frac{e^{-x^2\nu/8\rho t}}{(x^2+y^2)^{1/2}}dx \qquad (14)$$

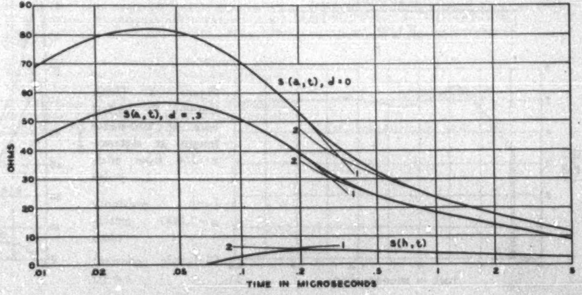
Inserting the value of the integral[9] expression (9) is obtained. The same result is obtained in a simpler way by neglecting displacement currents in operational expression (11), in which case (14) is obtained directly.[10]

As stated before, displacement currents in the air have been neglected throughout. This has a negligible effect for large values of t or for $y=a$ but results in the condition $S(h,t)=0$ for $t=0$, rather than $S(h,t)=0$ for $t<h/c$, c being the velocity of propagation in free space. It would appear that the effect of displacement currents in the air is obtained, in a first approximation, by taking t as the time in excess of h/c. The surge impedance for small values of time is of secondary importance in application to actual surge currents, since these are always small during the interval in which displacement currents have appreciable effect.

In the above formulas the wire is assumed to be half buried in the earth, that is, with its axis in the plane of the earth's surface. For wires at finite depths below the surface, exact formulas would be considerably more complicated. As a first approximation for practical purposes, the surge impedance of a wire at a small depth d is in the following taken as half the sum of $S(a,t)$ and $S(b,t)$, where $b=(a^2+4d^2)^{1/2}$. This is suggested by a corresponding relationship (implicit in equation 16) for the leakage resistance to ground of a buried

Figure 2. Surge impedances for unit step current for buried wires of infinite length

Radius of wire $a=0.5$ centimeter
Depth of wire $d=0$ and 0.3 meter
Height $h=20$ meters
Earth resistivity $\rho=1{,}000$ meter-ohms
Dielectric constant $\epsilon=10$



wire of finite length. The surge impedance calculated in this way will be somewhat too small, since the propagation constant increases with depth, but for depths in the order of one foot, as actually encountered, the error is likely to be small.

The variation in $S(y,t)$ with t is shown in figure 2, for a representative case as indicated in the figure. Near the intersection of curves 1 and 2 calculated from (10) and (9), respectively, the actual surge impedance is obtained by drawing a transition curve as shown in the figure.

III. Wires of Finite Length

Propagation of Unit Step Current

For a wire of length l the current at the distance x from the mid-point where energization is effected, is obtained from the following formulas:

$$\left.\begin{array}{ll} \textbf{Condition} & \mathbf{i(x,t)/i(0,t):} \\ x<vt<l-x & \mathit{erfc}\,x\beta \\ l-x<vt<l+x & \mathit{erfc}\,x\beta-\mathit{erfc}(l-x)\beta \\ l+x<vt<2l-x & \mathit{erfc}\,x\beta-\mathit{erfc}(l-x)\beta+ \\ & \quad \mathit{erfc}(l+x)\beta \\ 2l-x<vt<2l+x & \mathit{erfc}\,x\beta-\mathit{erfc}(l-x)\beta+ \\ & \quad \mathit{erfc}(l+x)\beta-\mathit{erfc} \\ & \quad (2l-x)\beta \end{array}\right\} \quad (15)$$

and so on, where $\beta=(\nu/8\rho t)^{1/2}$ and *erfc* is the error function complement, as in expression (7). The first term in each expression is the current for a wire of infinite length, the second term the current reflected at the end of the wire and reaching x at the time $(l-x)/v$, the third term the current reflected again at the mid-point and reaching x at the time $t=(l+x)/v$, etc. The currents are in each case assumed to be given by expression (7), which is sufficiently accurate for practical purposes.

At the mid-point of the counterpoise $i(x,t)/i(0,t)=1$ and at the end point $i(x,t)/i(0,t)=0$. At intermediate points the current varies with time. In figure 3 this variation with time is shown for a point midway between the mid-point and the end point of a counterpoise 100 meters long for an earth resistivity $\rho=1{,}000$ meter-ohms. From this figure it is seen that the condition $i(x,t/i(0,t)={}^1/_2$ for $x=l/4$, corresponding to uniform leakage along the wire, is practically reached within one or two microseconds.

Surge Impedance for Unit Step Current

On account of the many difficulties of a formal solution for the surge impedance of a wire of finite length, this section is confined to a demonstration of a graphical method which appears to be sufficiently accurate for practical purposes.

The surge impedances shown in figure 2 have been plotted in figure 4 along a linear time scale, together with leakage resistances of wires of 100-meter length, buried at depths $d=0$ and $d=0.3$ meter, and being in perfect contact with the ground. For reasons to be explained, the surge impedances are shown dotted for $t>0.74$, and the leakage resistances are the horizontal lines on the right-hand side of the figure. The latter are obtained from the formula:[11]

$$R=\frac{\rho}{2\pi l}[\log_e(4l^2/ab)-2], \qquad (16)$$

where

ρ = earth resistivity in meter-ohms
l = length of counterpoise, meters
$b=[a^2+(2d)^2]^{1/2}$
a = radius of counterpoise, meters
d = depth to center of counterpoise ($d \gg l$)

For a wire 100 meters long the time required for current reflected at the ends to return to the mid-point is $l/v=0.74$ microsecond, when $v=1.35\cdot10^8$ meters per second for $\epsilon=10$, as used in calculation of the surge impedances.

Between this time and the time $t\cong1.5$ microseconds required to obtain uniform leakage along wire, reflected currents increase the surge impedance until it equals the leakage resistance. Since the surge impedance variation in this short time interval is rather small, it is sufficiently accurate to assume a transition as shown in the figure. In the same time interval $0.74<t<1.5$ microseconds, the impedance $S(h,t)$ decreases to zero, as shown in the figure.

The leakage resistance used above is that of a wire in perfect contact with the earth. Usually there is some contact resistance between the wire and the surrounding soil, but the capacity of the wire to the soil is so large, that for surges of short duration this contact resistance may be neglected, and the wire regarded as being in perfect contact with the ground. When d-c or low-frequency measurements are made of the leakage resistance, the latter will include the contact resistance and therefore be somewhat larger than that used above. For wires a few hundred feet long the impulse impedance after a couple of microseconds is therefore somewhat smaller than the leakage resistance as measured with direct current. This condition has been observed in surge impedance measurements on various types of grounds,[12] but has generally been ascribed to the much larger currents used in case of surge-impedance measurements than in case of leakage-resistance measurements.

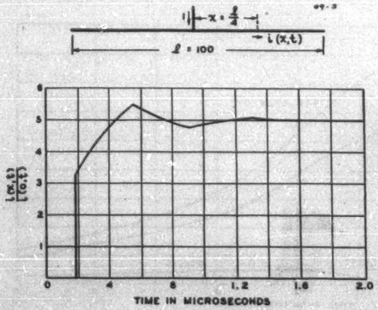


Figure 3 (left). Current in buried wire of 100-meter length at distance x=l/4 from mid-point

Earth resistivity ρ=1,000 meter-ohms

Dielectric constant ϵ=10

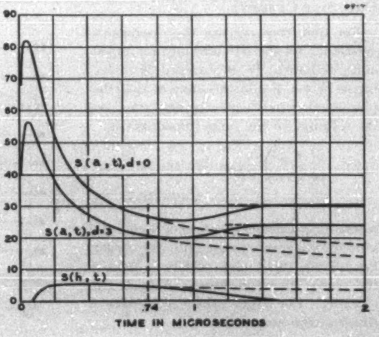


Figure 4. Surge impedances for unit step current for a buried wire of 100-meter length

Radius of wire a=0.5 centimeter
Depth of wire d=0 and 0.3 meter
Height h=20 meters
Earth resistivity ρ=1,000 meter-ohms
Dielectric constant ϵ=10

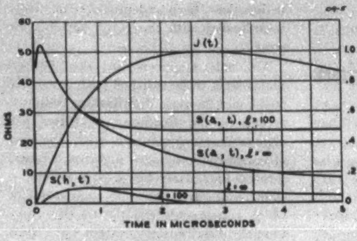


Figure 5 (left). Surge impedances for lightning currents

Length of wire $l = 100$ meters and infinite
Radius of wire $a = 0.5$ centimeter
Depth of wire $d = 0.3$ meter
Height $h = 20$ meters
Earth resistivity $\rho = 1{,}000$ meter-ohms
Dielectric constant $\epsilon = 10$
Surge current $J(t) = 1.44(e^{-0.1t} - e^{-t})$

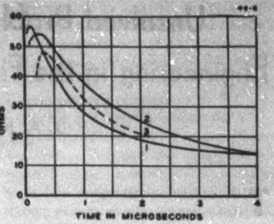


Figure 6. Comparison of calculated and measured surge impedances of a long wire

Radius of wire $a = 0.318$ centimeter
Depth of wire $d = 0.3$ meter
Earth resistivity $\rho = 2{,}000$ meter-ohms
Dielectric constant $\epsilon = 15$

Curve 1—Mid-point surge impedance calculated for unit step current

Curve 2—Mid-point surge impedance calculated for $J(t) = 1.44(e^{-0.1t} - e^{-t})$

Curve 3—One-half of end-point surge impedance observed for condition similar to those assumed in calculations

IV. Surge Impedances for Lightning Currents

Calculation of Surge Impedances

When a surge current $J(t)$ enters the mid-point of the wire the corresponding surge voltage may be obtained from the superposition theorem:[13]

$$V(y,t) = \int_0^t J'(t-\tau)S(y,\tau)d\tau \qquad (17)$$

where $J'(t) = \dfrac{d}{dt}J(t)$ and $S(y,t)$ is the surge impedance for unit step current. It is usually necessary to perform the integration numerically, but the process is not difficult and does not require much time for the accuracy needed here. The surge impedance is defined as the ratio $V(y,t) \div J(t)$.

Lightning current entering counterpoises may usually be approximated by an expression of the form:

$$J(t) = J(e^{-\gamma_1 t} - e^{-\gamma_2 t}) \qquad (18)$$

where γ_1 and γ_2 may be in the order of 0.1 and 1, respectively, when t is taken in microseconds. With these values of γ_1 and γ_2 and with $J = 1.44$, the above expression represents a current rising from zero to a maximum value of 1 ampere within 2.5 microseconds and diminishing to 0.01 ampere within 50 microseconds. Actual lightning current may rise to a maximum value of 100,000 amperes or more.

With a current of the above form and with unit step surge impedances shown in figure 4 for a counterpoise at a depth of 0.3 meter, the surge impedances shown in figure 5 are obtained.

Comparison With Measured Surge Impedances

Although several tests have been made to investigate surge characteristics of counterpoises, it appears that in only a single case were the tests performed in such a way as to obtain reliable results for small values of time.[3] In this case a surge current was impressed at one end of a counterpoise 925 feet long, of 0.25-inch diameter, buried at a depth of 12 inches. Measured leakage resistances of counterpoises of various lengths indicate an earth resistivity at the test location varying between 1,500 and 2,000 meter-ohms. The observed velocity of propagation along the counterpoise indicates a dielectric constant of about 15.

In figure 6 are shown surge impedances calculated for these conditions for unit step current and for a current as shown in figure 5. Since the surge impedance at the end point of a long counterpoise is nearly twice as large as at the mid-point[9] the calculated values are compared with one-half the observed end-point impedance. The shape of the current used in the measurement is not apparent in the publication referred to,[3] but in other tests[1] on the same counterpoise a current with somewhat steeper wave front was used than that upon which curve 2 of figure 6 is based.

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Discussion

For discussion, see page 1180.

Unsolved Problems of Electrical Engineering in the Field of Transportation

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THE ability to determine and state what comprises the outstanding problems in any field of activity implies very extensive knowledge of the developments in that field and to state the unsolved problems implies even more than this. When we begin to consider what ought to be done or attempted, the assumption is that there is an accepted standard or goal toward which we are striving. The author knows of no clear and comprehensive statement of the goals or ends of modern transportation. However, competition is making it increasingly clear that if public transportation is to survive, the industry must strive toward higher levels of convenience, speed, comfort, efficiency, and attractiveness. Today, the public assumes that safety has been given adequate consideration. This, then, is taken as the major premise of the paper; that any development that offers hope for improving these factors, through the effort of electrical engineers, is a proper problem for consideration.

Obviously, a paper such as this can only scratch the surface of the potential electrical problems in the transportation field, for, after all, this subject is one that would be more appropriate for a large research or scientific organization. It is suggested later in the paper that there is a great need for such an organization in the field of transportation. Lacking such an organization, we have attempted to state some of the problems facing electrical engineers of the transportation industry, particularly those in the railroad and urban transportation field.

For the purpose of grouping related problems, the paper is divided into five parts:

Power distribution to electric railways
Power utilization by transportation units
Possible electrical solutions of operating problems
Improved research instruments
Conclusion

Power Distribution to Electric Railways

With the development of superpower networks and relay systems furnishing power of utmost reliability, it is taken for granted today that the great power systems which supply our industrial and domestic power needs will likewise furnish power for the electrification of transit systems. Problems involved in the generation and transmission of this power are beyond the scope of this paper. However, vital unsolved problems are involved in the purchase and supply of this power.

The power contract itself, which involves the determination of an equitable method for allocating demand charges is one such problem. A problem of more vital concern is the use of commercial voltages and frequencies for heavy railroad electrifications.

The economic advantage of using central-station power for railroad electrification is due chiefly to two factors:

1. The energy component of the power rate is lower because the central station can operate larger and consequently more efficient generating units.
2. The demand component of the power rate is lower due to the diversification of railway, commercial, and domestic loads with resulting high load factor, and because extensive joint use of generating and transmission facilities is possible.

It is obvious that the maximum economic advantage of using central-station power is obtainable only if the catenary is operated at commercial voltage and frequency. Ten years' experience with an urban-railway power-distribution system wherein all power is furnished to railway substations over regular commercial high-voltage lines, has convinced the author that joint use of transmission lines and distribution facilities is dependable and economical, and that with suitable railway and power-system design, such commercial feeders in no respect jeopardize the transportation service.

A review of the existing power-distribution facilities of various heavy transportation systems (table I) raises the question whether the transportation power system design has been adequately or fully co-ordinated with the power-supply systems.

Although engineers in the United States seem to be satisfied with a variety of d-c and 25-cycle distribution systems, several of the European countries are vigorously experimenting with the use of commercial voltages and frequencies.

Since 1932, Italy has had in service a 10,000-volt three-phase 45-cycle system, which corresponds exactly to the commercial distribution system of that country.

In 1934, the Hungarian State Railways instituted service on the main line section of the intercontinental line, Paris-Istanbul. This electrified line, 120 miles long, is divided into three separate sections. The catenary of each section is fed by means of step-down transformers with 16,000-volt 50-cycle single-phase current, taken from one phase respectively of the 110-kv three-phase public network. The 50-cycle single-phase energy is converted on the locomotive into three-phase current.

An electrification 35 miles long using 20,000-volt 50-cycle current was completed by the German State Railways in 1937 between Frieberg, Baden, and the heights of the Black Forest. This line is divided into two separate sections fed from the 110-kv three-phase industrial network, by means of step-down transformers in Scott connection, statically changing the three-phase into two-phase current. Four different-type locomotives have been provided by four different companies. Two locomotives involve the use of steel-tank mercury-arc rectifiers. One locomotive uses four sets of twin motors capable of three synchronous speeds. The other locomotive uses a tap transformer and four sets of single-phase 50-cycle motors, each set consisting of two 225-volt motors connected permanently in series.

A Swiss locomotive, which has been described by O. K. Marti in a paper before the AIEE, has been designed to operate from a 15,000-volt 50-cycle catenary using commutatorless single-phase motors. The usual commutators, tap switches, starting resistors, and reversing switches have all been replaced by a mercury-arc rectifier with control grids.

It would seem from the above that there are distinct possibilities for the adoption and use of commercial types of power supply for heavy railroad electrifications which should result in reduction of the large investment required for main-line electrification. The solution of the

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problem involved in making such equipment commercially available offers a challenge to the skill and ingenuity of electrical engineers.

Closely related to this problem of power supply is the problem of constructing a catenary and distribution system at a lower cost than those now available. One of the great difficulties in connection with the electrification of main-line railroads is that the catenary and distribution system must be installed complete over fairly large areas in order to secure economies in operation, and it must be installed on the basis of handling the maximum traffic expected within the near future. This requires a heavy investment involving large fixed charges that continue even if the traffic does not come up to expectations. The present trend of main-line motive power to self-contained units is largely due to an effort to avoid these fixed charges. Should a solution be found that would permit a drastic reduction in the cost of catenary and distribution systems, this trend might easily be reversed.

Table I. Power Distribution Characteristics of Principal United States Railroad Electrifications*

	System of Electrical Distribution		
	Current	Voltage	Third Rail or Catenary
Butte, Anaconda & Pacific	D-c	2,400	Cateuary
Chicago, Milwaukee & St. Paul	D-c	3,000	Catenary
Cleveland Union Terminal	D-c	3,000	Catenary
Delaware, Lackawanna & Western	D-c	3,000	Catenary
Fort Dodge, Des Moines & Southern	D-c	1,200	Catenary
Great Northern (Motor generator—d-c)	A-c	11,000	Catenary
Illinois Central	D-c	1,500	Catenary
Illinois Terminal Co.	D-c	650	Trolley
Long Island	D-c	650	Third rail
New York Central	D-c	750	Third rail
New York Connecting Railroad	A-c	11,000	Catenary
New York, New Haven & Hartford	A-c	11,000	Catenary
Norfolk & Western (phase converter)	A-c	11,000	Catenary
Pennsylvania	A-c	11,000	Catenary
Reading	A-c	12,000	Catenary
Southern Pacific Co.	D-c	1,200	Catenary
Virginian (phase converter)	A-c	11,000	Catenary

Note: All a-c electrifications are single-phase 25-cycle.

* Data taken from National Power Survey, 1936.

Considering further the various distribution systems of the electrified rail lines of the United States, it is clear that another major unsolved problem in electrical engineering is involved in the standardization of these systems.

In 1931, Sidney Withington, electrical Engineer of the New York, New Haven and Hartford Railroad, reviewing 25 years of experience with heavy electric traction, at a meeting of the New York Railroad Club, said that the most important problem which now requires solution is, "What electric system is the best for general use for heavy main-line electrification in the United States?"

Inasmuch as this problem is still with us and since his remarks are as pertinent today as they were in 1931 we quote Mr. Withington.

"It is sincerely to be hoped that this important question will be answered before long. With the growth of railroad electrification, the necessity will arise for the interchange of equipment among railroads, and unless a system of power distribution can be agreed to as standard by the railroads of the country, the diversity of electrical characteristics among railroads is going to be a major calamity, comparable, perhaps to the diversity in track gauge which caused so much trouble and loss in the early days of railroading.

"It is perhaps, true that until recently there has not been sufficiently reliable information available to justify standardization of this kind, but this is no longer the case. The dogfights which took place from time to time years ago as to the relative value of the various "systems" were not based on quantitative analysis and led nowhere. If, however, the data now existing can be properly correlated, a conclusion, based on definitely established premises should be reached. It is not pertinent to say that one type of electric power distribution is most satisfactory for terminal or yard switching, another for heavy freight operation on steep grades, or for high-speed passenger service and another for suburban electrification, for such tenets defeat the basic principle of standardization. A single system, one which is by and large most suitable for all electrification requirements should be authoritatively agreed upon. Until this is accomplished, the extension of railroad electrification will be seriously handicapped."

When we consider problems of this kind from the standpoint of national defense, they take on an even greater significance. Transportation units must be capable of mobilization in various parts of the country. England, France, and Germany, realizing this, have adopted national standards.

Standardization of motive-power units is not necessary to obtain the essential requirements for mobility as outlined above, but standardization is desirable in this respect if the railroads are to secure the tremendous economic advantage of mass production.

For the welfare of the electric transportation system as a whole and everyone individually involved in it, standardization is imperative. Major competition today is with transportation units produced by mass-production technic. This is true, not only of private cars, but also of motor and air transportation units. We need not fear that standardization will impair initiative and new development, because the driving force of competition is present today in the transportation field in greater degree than ever before.

Perhaps if some degree of standardization can be attained, the suggestion contained in the National Power Survey of 1936 for an increased electrification mileage of 12,000 miles of track on 20 steam railroads involving an annual energy consumption of 5,000,000,000 kilowatt-hours, can be made a reality or even substantially increased.

Power Utilization by Transportation Units

The elimination or the substantial reduction of the losses involved in starting d-c traction motors is a problem that has long concerned transportation engineers. Early in the history of the industry, these losses were reduced by approximately one-half with the adoption of series-parallel control, which has the further advantage of providing an efficient half-speed running point.

The past few years have seen rapid developments in the control equipment of urban transportation units. Competition with rubber-tired vehicles makes imperative the use of accelerating rates which closely approach the limits determined by rail adhesion. In order to maintain smooth acceleration from the starting point until the full-voltage motor curve is reached, it has been found desirable to eliminate transition points. The reduction in first cost and maintenance occasioned by the simplified control of a single motor, is further reason for modern trolley coaches and street cars to use rheostatic control.

Recent tests show that 40 per cent of the total energy supplied to the modern trolley coach is dissipated in accelerating resistor losses when the coach is operated over a heavy urban route. This is due to the fact that operation is constantly on resistance when the coach is operating in heavy traffic. On lighter urban runs where more free running is possible, this loss probably averages about 30 per cent. Similar conditions prevail on modern PCC cars.

The compound-regenerative motor used

on some recent trolley-coach installations, provides for more efficient medium-speed running, but the major portion of the starting-resistor loss still remains.

It is frequently pointed out that these losses are salvaged during the winter months for heating the coach. This is true, but dynamic braking, now so successfully installed on both trolley coaches and rail equipment, for the primary purpose of reducing brake maintenance, makes the energy available for heating. More energy is being dissipated on modern transportation units of this type than can be comfortably used for heating purposes.

The ideal system of control for transportation equipment would provide for the total elimination of control-resistor losses and make available an infinite number of efficient low-speed running points.

The need for efficient d-c control seems to be appreciated to a greater extent in Europe than in America. This is evident from the fact that development along these lines has progressed farther, and that the foreign technical press carries more articles relating to this problem than the American technical press.

The "metadyne" control, recently furnished on 73 new multiple-unit equipments for the London Passenger Transport Board, is one answer to this problem, although the equipment involved at the present state of development is rather complicated and heavy for general application to small urban transportation units.

The name "metadyne" has been given to a type of d-c machine in which special use is made of armature reaction to control machine characteristics. Metadynes have been built to act as rotary transformers of d-c power and applied to control railway equipments. This control is arranged to convert power from constant voltage to variable voltage so as to give any desired speed-torque characteristics to the traction motors from standstill to full speed and from full speed to stop. For example, for the comfort of passengers when using a high rate of acceleration, it is desirable to start with low acceleration, to rapidly increase the acceleration at a steady rate until a maximum is reached and then to decrease the acceleration steadily so that when the power is cut off, the passengers will not be inconvenienced. Similarly when braking regeneratively, it is desirable to start with a low rate of retardation, increase it to a maximum and finally to ease off the rate, so that just before stopping the rate of retardation is low. These features can be obtained with the metadyne in a very smooth manner by varying the natural characteristics.

The metadyne control thus takes the place of starting resistors, accelerating relays, switches, and cam groups commonly used for traction-motor control, and in addition, provides means for the regenerative control of the machine to standstill. The power wastage ordinarily involved in resistance losses is thereby avoided, while the smooth control has obvious advantage over the use of stepped resistances.

Experiments have been made with inverters and d-c transformers of the commutator type, in which only light rotating parts are used and the actual transformation of power is accomplished by highly efficient static transformers. A device along such lines would seem to offer possibilities for producing smaller and lighter units than the metadyne does at present.

There seems to be no fundamental reason why an efficient d-c control cannot be evolved. Perhaps, the problem needs only to be emphatically brought to the attention of research organizations. Certainly, it should be listed as one of the unsolved problems of electrical engineering in the transportation field in the United States.

Another problem is the definite need for some simple efficient equipment that will convert 600-volts direct current to a low-voltage alternating current. On 600-volt multiple-unit cars, especially those operated from third rail, the only method now commercially available for converting from third-rail voltage to a low-voltage direct current or alternating current for providing low-voltage lighting, is by means of a motor generator set with a 600-volt d-c motor and a low-voltage d-c generator. Such motor generator sets are usually required to have an input of approximately two kilowatts but because they operate over a range of approximately 450 to 750 volts direct current, the motors have to be large in order to maintain the generator output at 450 volts. Also the motor is subjected to terrific abuse whenever a train runs off the end of a stretch of third rail with the traction motors connected to the rail, as then the potential applied to the motor reverses instantaneously from plus 600 to minus 800 or 900 volts due to the inductive kick from opening the traction-motor circuits. The motors of such motor generator sets usually have to be compound wound and the above requirement imposes a very difficult commutation problem because of the tendency of the motor to generate under such a condition and to dissipate its kinetic energy as an electrical short circuit. Motor generator sets of this type have been built but are expensive and require high maintenance on account of commutator troubles.

The ideal solution of this and allied problems would be a device of the nature of a vibrating inverter that would convert from 600-volts direct current to low-voltage alternating current which could be used directly on the lamps, or rectified for battery charging, or both. Such a device should have very lightweight moving parts so that there would be no appreciable kinetic energy to dissipate at times of sudden voltage reversals due to third-rail gaps. Means should be incorporated in the device for maintaining constant a-c output voltage with varying d-c input voltage. Inverter tubes would be excellent for this purpose if they could be developed to withstand the high inverse voltages involved and to deliver the necessary power outputs. The problem of rectifying alternating current from such a source to low-voltage direct current can, of course, be solved in a number of well-known ways.

Still another problem is the need, on certain classes of cars and by certain fixed equipment at remote locations, for a small amount of power for lighting and signal lights without using the conventional systems of generation. The conventional systems of power supply such as straight storage batteries, storage batteries and axle generators, or self-contained gasoline engines all seem to be too expensive in first cost and too high in maintenance for the small amount of power required.

A possible solution is the use of primary batteries with some type of fluorescent lamp requiring very little power, yet giving considerable light. Together with these would be required an efficient inverter for the transformation of power to secure alternating current for the operation of the fluorescent lights. The inverter presents a problem on account of the high efficiency required with a low power input.

Other problems of power utilization on vehicles would be immensely simplified if storage batteries of greatly improved characteristics for transportation purposes could be developed. While great improvements in storage batteries, both of the lead-acid and alkaline types, have been made in the last 20 years, they have been along the lines of refinements rather than fundamental changes. What is needed is a battery of radically new characteristics, such as much lighter weight, higher voltage per cell, greater capacity, and the ability to be charged from commercial a-c sources of supply. While this may sound revolutionary it would be no more remarkable than other developments that have occurred in recent years. The Drumm battery developed in Ireland several years ago is an attempt at a solution of this unsolved problem.

Suitable thermostatic control of heat on transportation units is still an unsolved problem. The use of a single thermostat on a multiple-unit car is far from satisfactory, particularly on runs where doors are opened frequently because of stops being close together on one portion of the run, and closed continuously on other portions where there are no stops. A solution of this problem is desired without resorting to the use of forced ventilation.

There is also a great need for a better d-c lightning arrester. At present, the most effective arrester for d-c traction vehicles is the aluminum-cell arrester. No case is known of damage to equipment where the vehicle has been equipped with one of these devices and it was in working order.

However, these arresters are expensive to maintain, require careful maintenance, and in many parts of the country where they have to be kept operating 12 months of the year they must be protected against freezing in the winter and overheating in summer, the film being very critical at high temperatures. On 3,000-volt d-c cars and locomotives, 12 of these jars have to be used in series and high short-circuit currents can be caused by jar failures.

For a-c circuits, the Thyrite resistance arrester has been developed and has superseded the aluminum cell. For various reasons, this material cannot yet be used on direct current. It would seem as though a similar material could be developed for use on d-c circuits.

Possible Electrical Solutions of Operating Problems

There is no doubt that electrical engineering can play a considerable part in solving some of the more difficult operating problems of transportation. Consider, for instance, the problem of communication between the vehicle and the dispatcher. With the present state of development of ultrashort-wave radio such communication is entirely feasible, yet out of reach economically. It is obvious that such a development would greatly expedite the handling of emergency traffic problems. Another communication problem, which, however, seems about solved is that of communication between the ends of long freight trains.

The electric fare box is a splendid development. But if an electrical device could be developed that would record and collect the amount of the individual fare in proportion to the length of the individual ride, a veritable revolution in urban transportation might result. At present, the major part of short-haul rides are lost because the cost of these rides is, as a rule, out of all proportion to the value to the individual patron.

Notwithstanding the use of many clever mechanical devices, the formation of sleet on trolley wire, and on third rails of the over-running type, is in many parts of the country a serious operating problem. Various schemes have been proposed for preventing sleet from forming or, at least, to keep it from sticking tightly to the wire or rail. However, no satisfactory solution has yet been found.

It might be possible to develop some sort of material that would serve as a coating and would prevent sleet from forming. The problem of preventing the current collectors from removing such material would of course be a considerable difficulty.

On many transportation lines, particularly on subways, the problem of announcing the approaching station is receiving considerable attention. To date, no infallible scheme has been developed that is economically feasible.

Possibly some sort of device, such as motion-picture film projecting the name of the station or announcing the name of the station audibly, could be worked out, which would enable the problem of various routes and changes in destinations to be taken care of without the apparatus becoming too unwieldy.

The same problem is also present to a certain extent on mainline trains, and it is only reasonable to suppose that the train of the future will include some provision to supersede the brakeman's "grunt" which has become over the years synonymous for practically any station on the line.

In connection with signaling and train control, there seems to be a problem of the conduction and detection of very small currents at low voltage through wheels of the free-wheeling type. In general, however, modern systems of train control and cab signaling have been perfected to such an extent that the major problem would seem to be that of financing their installation. Automatic train control is of course a unique advantage from the safety standpoint that rail transportation has over all of its competitors. Unsolved problems in this field certainly merit serious consideration.

Another safety problem that should receive attention is that of providing timely and adequate warning in the event of fractured or broken axles.

The problem of recording headways between rail units or trolley coaches at a central dispatcher's office on urban transportation systems has been solved. Such a system, complete with automatic alarms, operating when pre-established headways are exceeded can be installed today at a reasonable cost. The satisfactory recording of motor-coach headways, however, still remains an unsolved problem. Such a recorder, functioning without street treadles or attention from the coach operator, is urgently needed today.

In general, it seems to the author that the advantages of centralized control and supervision of operation cannot be overemphasized. Such systems can be developed economically and extended over every phase of the power supply and distribution system with great advantage to the transportation service.

Improved Research Instruments

Despite the phenomenal development of scientific instruments, the use made of them by public transportation agencies is not significant. Something is lacking here. No one questions that the essential factors making for convenience and comfort and therefore for public acceptance of transportation units are speed, smoothness of acceleration and retardation, absence of noise and vibration, illumination and ventilation, yet the transportation unit is not periodically checked by suitable instruments with respect to these factors.

The performance of new equipment is not even specified and checked with regard to all of these factors. We still rely on our intuition and feelings after a demonstration ride to determine the most comfortable equipment. All of the factors mentioned are physical and therefore susceptible to quantitative measurement. There is no reason why recording instruments presenting a graphic picture of these factors as the transportation unit proceeds along its route should not be developed and used. The significance of these factors will only be appreciated after they have been recorded in some such manner.

There is seldom much progress made in improving a given situation until objective measures of the results are obtained, for the reason that there is no agreement on the basic facts until we have visual pictures of them.

One of the important physical factors which we have not yet succeeded in measuring is the presence of objectionable odors or fumes. If someone should develop an objective method of determining and graphically measuring these, we would undoubtedly solve the problem of their elimination.

Notwithstanding the need it is unlikely that instrument or meter manufacturers will of their own initiative, develop extraordinary tools for furthering research and development work on transit systems for the reason that the market for such equipment would be so limited that the development would be unprofitable.

Conclusion

The multiplicity of problems facing the industry leads naturally to the vital unsolved problem of stimulating technical developments in the transportation field. Experience has demonstrated the relative ineffectiveness of individual operating companies in the fields of research and progressive achievement. Railway association committees are likewise relatively ineffective. They do excellent work, but the tempo of their achievement due to the very nature of the volunteer type of organization is inadequate to meet present-day competition. Individual manufacturing companies have made noble efforts to keep the public-transportation industry abreast of the times. The money they can afford to spend on new development, however, is strictly limited to potential profits they can make from the sale of new equipment. When the prospects for the sale of new equipment vanish, the development program slackens at the very time when it should be accelerated. Somehow, the problem of maintaining an adequate research and development program must be solved.

Illustrative of what might be accomplished by an adequate approach is the result of several years' activity of the Presidents Conference Committee, the new PCC car—a revolution in urban transportation.

The development of equipment and improved standards for railways, as in practically all of the older industries, has been a sort of evolutionary process, that is, improvements have come through the elimination of the cause of faults and complaints. With the limited funds available for research and development, no other technic was possible. Even with the maximum effort and co-operation only moderate progress is possible by using such a technic. The activity of various technical committees, through interchange of experience and ideas, stimulates and accelerates the evolutionary method, but the results are naturally the refinements of existing equipment instead of the development of revolutionary new methods and equipment.

If the necessary funds, time, and organization were available it would be possible to attack this problem in an entirely different way. The broad general objective could be established a priori or by means of a consumer or patron survey of results that should be accomplished. Research men and scientists could then set about obtaining all factual data relevant to the accomplishment of these results. After the physical factors entering into the solution had been isolated, performance specifications could be set up. Up to this time, no consideration need necessarily be given to past experience or to equipment available at present. The newly evolved specifications would provide the necessary standards by which to judge, not only the performance of existing equipment but also of all proposed equipment.

Probably the best example of the use of this scientific research method in the transportation field is the activity of the Presidents Conference Committee in the development of the PCC car.

The late Doctor C. F. Hirshfeld speaking of this activity said:

> "The important thing to note is that this research enabled us to set fairly exact specifications for those things that would have to be achieved to produce a completely modernized and satisfactory street car. I shall speak of these specifications as performance specifications because that is what they are in essence.
>
> "Frankly, the performances indicated were in many cases so far beyond what the industry had theretofore considered necessary that our demands were at least startling to manufacturers who were asked to produce equipment to give such results. My impression is that they were equally startling to many operating men as well. I confess that in some cases we announced these specifications with some fear of ridicule, because, after all, we were in the position of advocating rather revolutionary changes on the basis of experimental work and without the reassurance that comes from practical use."

It is vital to observe that there is nothing final or absolutely conclusive about performance specifications developed as outlined above. The whole technic and development is based on a premise which is constantly shifting with public sentiment. If noise was disregarded by the public yesterday but considered vital today, our performance specifications must be changed accordingly.

An organization designed to cope with problems in such a manner, must, of course, be permanent and its activity continuous. If its financing were a charge against operating expense as is the case with the development work of the American Telephone and Telegraph Company, development would be greatly stimulated, and furthermore the acceptance and actual use of new developments would be expedited for the following reasons:

1. Operating companies are in direct competition with other forms of transportation and therefore have a keen interest in improving their competitive position. They are able to sense more quickly and accurately public demands in transportation than the more remote manufacturers of equipment. They would therefore initiate more quickly development work actually needed to meet public demands.

2. In an effort to realize on their contributions to the joint development fund operating companies would do everything possible to put new developments into actual service.

3. Due to the fact that new developments would not be loaded with manufacturers development costs, the initial cost of new equipment would be greatly reduced with consequent savings in fixed charges such as interest, depreciation, and taxes.

Such an organization supported by the operating companies and functioning with adequate resources would inevitably present to the manufacturers of this country unsolved and worthwhile problems of the industry which we cannot even comprehend today.

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Discussion

For discussion, see page 1173.

Resistance of the Spot Weld

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Synopsis: This paper covers an investigation of the electrical resistance across a spot weld and offers both calculated and measured data for comparison.

THE OUTPUT of resistance-welding machines is generally determined by Joule's law

$W = RI^2$

where R is the resistance of the heated metal being welded and I is the current flowing during the welding operation. With the majority of machines, welding current is determined by the internal impedance of the machine, the resistance of the weld being but a small part of the impedance of the entire circuit.[1] Therefore the value of R is the only variable which can change the output at the electrodes of a given machine for a given internal impedance. Designers of welding machines are familiar with the value of R for different welding jobs,[2] however, there has been a lack of systematic study of this resistance. This paper indicates one method of evaluating spot weld resistance. Investigations were restricted to the most important case, the welding of the ordinary mild-steel sheets.

Flow of current through the electrodes and sheets is shown in figure 1. The resistance which effects the weld consists of:

1. Two contact resistances between the electrodes and the sheets.
2. One contact resistance between the two sheets.
3. Resistance of the sheets.

Investigations of the contact resistance were based on the studies of R. Holm and his collaborators.[3–7] According to Holm's theory of "sieve-resistance" (*siebwiderstand*), two clean metallic surfaces never touch in a plane, but contact in a larger or smaller number of microscopic projections depending upon surface conditions. The number of these projections in contact increases with the pressure exerted on these surfaces. The resistance of the two surfaces to current flow is in fact but a metallic resistance of the very narrow current fibers, contracting into the contact sieve. The contact area will be best filled with elementary contact areas when the pressure exceeds the compressive strength of the material. With this condition, both surfaces fit into each other nonelastically and the resistance across the contacting surfaces becomes,

$$R_c = \frac{\rho}{d} \tag{1}$$

ρ = resistivity of the metal (with two different metals in contact, the mean value of both should be used)
d = diameter of the contact area

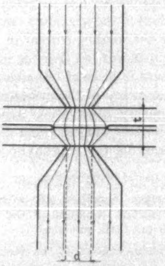

Figure 1. Flow of current through the electrodes and sheets in the average spot weld

Since contact resistance is metallic in nature, its value increases with rising temperature as determined by the thermal coefficient of resistivity.

The current fibers between the electrodes form a space conductor. It can be assumed that the surface contact between the two welding parts is of infinitely large diameter. This assumption is essentially correct since further investigations indicate that most of the current fibers run very close to the axis of the current flow. This is illustrated in figure 3. The potential field of this current can be determined according to Lehman's rules[8,9] by tracing the form of the equipotential and force fields in the axial section of this space. Lines of force and equal potential cross orthogonally and divide this space into ring elements of equal permeance. Referring to figure 2,

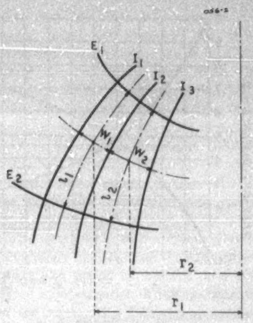

Figure 2. Relation of current fibers with respect to the axis of current flow

$$\mathcal{P} = 2\pi r_1 \frac{w_1}{l_1} = 2\pi r_2 \frac{w_2}{l_2} = \text{etc.}$$

Total permeance of this space is determined by the number of ring elements connected in parallel, divided by the number of elements in series. For the space conductor of figure 3, current path was determined for contact diameter, $d = 0.4$ centimeter (0.164 inch) and for the total sheet thickness $t = 0.8$ centimeter (0.328 inch). Permeance of one ring element becomes 0.95 centimeter, and total permeance of the space occupied by the current 0.534 centimeter. If the current fibers were straight between the circular contacts, permeance of the space conductor would be that of the cylinder 0.4 centimeter in diameter and 0.8 centimeter in height and may be expressed—

$$\mathcal{P}_c = \frac{\pi d^2}{4 \cdot t} = 0.157 \text{ centimeter}$$

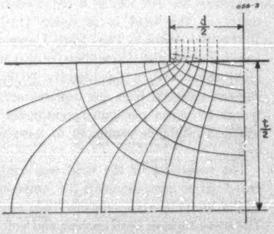

Figure 3. Equipotential and force fields in space conductor

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[1] For all numbered references, see list at end of paper.

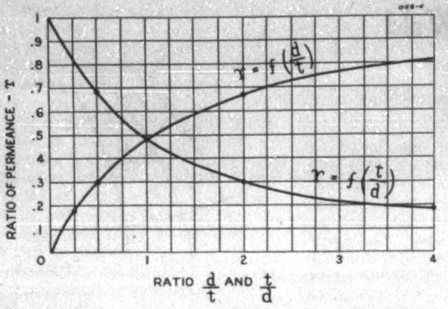


Figure 4. Curves showing ratio of permeances Υ as a function of electrode diameter to sheet thickness and sheet thickness to electrode diameter

Ratio of both permeances

$$\gamma = \frac{\mathcal{P}_c}{\mathcal{P}}$$

will be constant for a given ratio t/d. For the above constants $\Upsilon = 0.295$. Values of Υ were obtained from the field images, traced for $t/d = 4, 2, 1, {}^1/_2$ and plotted against the ratio t/d, d/t respectively as shown in figure 4. Reciprocal curves permit extrapolation by making use of both limits: $\Upsilon = 1$ for $t/d = 0$ and $\Upsilon = 0$ for $d/t = 0$. In the following calculations, resistance of the space conductor between the two circular contacts was computed from the equation

$$R_m = \Upsilon\rho\frac{4 \cdot t}{\pi d^2} \tag{2}$$

The shape and resistance of the space conductor is affected by the strong magnetic field produced by the welding current. Therefore, in obtaining data this effect was minimized by locating the weld near the edge of the sheets, so that only a relatively small flux could affect the space conductor. Eddy currents also affect the resistance value. Current densities in the weld amount to about 10,000 amperes per square centimeter. The resistivity of the hot steel is approximately 10^{-4} ohm per centimeter cube. Therefore the I^2R loss in a cubic centimeter of the weld is of order $(10^4)^2 10^{-4} = 10^4$ watts. The eddy current loss in a very thick (10 millimeter or 0.39 inch) sheet with a flux density 20,000 gausses (129,000 lines per square inch) is but 10 watts per cubic centimeter[10] which is negligible compared to the I^2R loss.

The resistance of the weld was computed from measurements of amperes and of the volts across the electrode tips. The wave form of both current and voltage was found to be practically sinusoidal with sheet thicknesses from 0.5 to 10 millimeter and with currents to 8,000 amperes. A slight deformation of the voltage wave, due probably to the partial discharge in the contact, was found but this deformation never exceeded the degree of accuracy of the method.

Measurements of the resistance were limited to voltmeter readings only, using the volt-ampere characteristic curves of the welding machine.[11] The volt-ampere curve for a given primary tap of the transformer and for given physical dimensions of the secondary circuit gives directly the welding current in terms of voltage. It was felt that the use of one instrument in place of two added greatly to the accuracy of the measurements.

Preliminary observations under various conditions disclosed that the resistance across the weld depends upon:

(*a*). Material being welded.

(*b*). Surface condition of metal being welded (surface condition of the electrode produces but little influence).

(*c*). Pressure exerted by the electrodes.

(*d*). Diameter of the contact.

(*e*). Temperature of the metal.

The relation between pressure, contact area, and temperature is complicated. In general, however, as the heated metal becomes softer, the contact area increases, causing a decrease in resistance. At the same time the rising temperature causes an increase in resistance due to the greater resistivity. Thus the effects tend to balance each other.

In order to analyze the problem it was necessary to eliminate as many variables as possible. With this in view mild-steel sheets, with ground surfaces, were used.

Resistance at Ambient Temperatures

To eliminate the contact resistance between two sheets, measurements were made with only one sheet between the electrodes of the welding machine. The sheet was held at ambient temperature by low-current and high-pressure values. By decreasing the primary voltage of the welding machine, current values from 1,000 to 2,000 amperes were obtained. A fixed pressure of 450 kilograms (990 pounds) was used.

From the experiments of Hertz,[12] it was found that when the compressive strength of the metal is exceeded, the increase in contact area is nearly proportional to the increase in pressure. This is always the case with the ordinary pressure settings of spot welders. Therefore, the contact diameter follows the relationship

$$\frac{\pi d^2}{4} = \frac{P}{p} \tag{3}$$

P = pressure exerted on the contact surfaces
p = compressive strength of the contact metal

For the average hard-drawn copper electrode in contact with a ground mild-steel surface, it was found that $p = 84.5$ kilograms per square millimeter. Pressures from 30 to 700 kg, were used covering all pressures commonly used in spot welding.

Voltages were measured across mild-steel sheets 0.5, 1, 2, and 4 millimeters in thickness with contact diameter of 0.26 centimeter and pressure of 450 kilograms. These readings are plotted against amperes in figure 5. Voltages obtained for each sheet thickness indicate that the resistance is constant. In table I the resistance of a single sheet at ambient temperature as obtained by measurement, is compared with the same values computed from the equations 1 and 2.

The resistance of copper to steel at ambient temperature in equation 1 was taken as $\rho = {}^1/_2(0.13 + 0.0175)10^{-4} = 0.074 \times 10^{-4}$

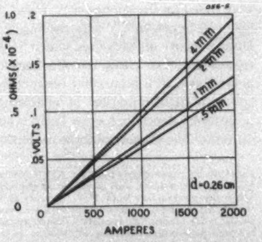


Figure 5. Voltage measurements across single thicknesses of mild-steel sheets for different current values and constant contact diameter of 0.26 centimeter

Table I

Sheet Thickness t (Mm)	0.5	1	2	4
$2R_s$ (equation 1)—10^{-4} ohm	0.57	0.57	0.57	0.57
R_m (equation 2)—10^{-4} ohm	0.1	0.18	0.26	0.34
Total $R_w = 2R_s + R_m$—10^{-4} ohm	0.67	0.75	0.83	0.91
R_w (from figure 5)—10^{-4} ohm	0.61	0.67	0.94	0.98

Table II

Sheet Thickness t(Mm)	2×0.5	2×1	2×2	2×4
$2R_s$ (equation 1)—10^{-4} ohm	0.57	0.57	0.57	0.57
R_m (equation 2)—10^{-4} ohm	0.18	0.27	0.34	0.41
Total $R_w = 2R_s + R_m$—10^{-4} ohm	0.75	0.83	0.91	0.98
R_w (from figure 6)—10^{-4} ohm	0.57	0.65	1.18	1.04

ohm-centimeters. For mild steel in equation $2\rho = 0.13 \times 10^{-4}$ ohm-centimeters.

With the same values of current and pressure, voltages were read across two ground mild-steel sheets of the same thickness. These readings are plotted against amperes in figure 6. A study of the resistance values in figures 5 and 6, corresponding to the same total thickness indicates that the contact between these ground sheets does not affect the resistance value materially. Therefore, in table II values obtained from figure 6 are compared with the values calculated from equations 1 and 2 assuming that the resistance is the same whether one sheet or two sheets of half the thickness, is used.

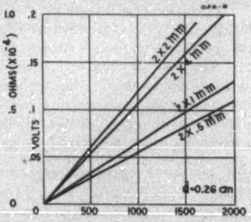


Figure 6. Voltage measurements across two thicknesses of ground mild-steel sheets for the same current values and electrode diameters used in figure 5

The differences between the computed and measured values in table I and table II, are within the accuracy of the method.

With smaller pressure values, the resistance of the surface contact between sheets adds to the total resistance. This higher resistance affects the weld for only a very small part of the welding time and therefore it did not seem necessary to follow the matter further.

Resistance at Elevated Temperatures

During the investigation, the resistivity of the steel was carefully controlled. Figure 7 shows the relation of the resistivity to the temperature. Only two values were carefully measured. These were: $\rho = 0.13 \times 10^{-4}$ ohms per centimeter cube at ambient temperature and ρ max$= 0.96 \times 10^{-4}$ ohms per centimeter cube, the latter being constant above 1,100 degrees centigrade. The curve which was taken from test data falls between the curves found by Stablein[13] for steel containing 0.05 per cent carbon, and by Holm[1] for chemically pure iron. In the measurement of voltages of the "hot" resistance, use was made of the fact that the resistivity of the steel was constant in the temperature range through which the welding operation is performed. By checking the heat color of the metal, the temperature was easily determined for each reading.

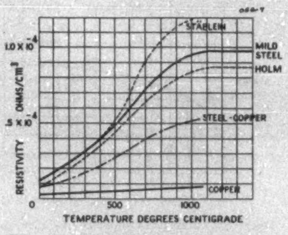
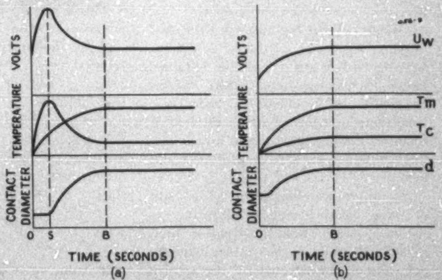


Figure 7. Resistance of copper-steel contacts and of steel and copper at elevated temperatures

The relationship of the different variables which influence the weld resistance during the period of heating, was obtained by observation of voltage changes on the screen of a cathode-ray oscillograph. In order to observe these effects more closely, the heating period was lengthened by decreasing the output of the welding machine until a heat equilibrium was established. The equilibrium was obtained when the total heat developed was dissipated through the electrodes into the cooling system. The voltage, temperature, and contact diameter during heating was found to be dependent upon the magnitude of the pressure.

Measurements at Low Pressure

Figure 8*a* shows these relationships for a pressure of 30 kilograms exerted on the electrodes. It will be seen that the voltage drop at ambient temperature rises immediately to a high value. This is due to the increasing temperature of the contact while the contact diameter remains unchanged. However, the heated metal softens rapidly and after the inertia of the movable electrode has been overcome at *S*, the contact area increases thus decreasing the resistance. This causes a corresponding decrease in voltage. The voltage, temperatures, and contact diameter become constant at "hot" values which maintain a balance of heat flow at point *B*. The temperatures of both the



(a)

(b)

Figures 8a and 8b. Values of different variables during the weld cycles for low and high pressures respectively contact and of the sheets show that the heating originates in the contact and progresses into the sheets. With lower pressure and improper cooling of the electrodes, the metal melted at the contact surfaces before the welding operation was begun which resulted in unsatisfactory welds. In the balanced final conditions when the heat flow equilibrium is reached, the electrode contacts are much colder than the inside of the weld, forcing the heat to flow through the electrodes into the cooling system.

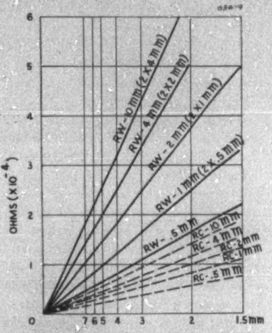


Figure 9. Resistance across one- and two-sheet combinations of mild steel at welding temperatures for electrode diameters of 1.5 to 7 millimeters

Measurements at High Pressure

With pressure at 300 kilograms or greater, the characteristic curves of voltage, temperature, and contact diameter are as shown in figure 8*b*. The voltage across the electrodes rises steadily to a constant value. Curves for temperature and contact diameter also show this same characteristic. The temperature of the weld is always greater than the contacts, due to the greater effect of the resistance of the sheet on the total resistance. With high pressure the heat develops first inside of the weld, and the welding temperature can be obtained before the contact temperature reaches the melting point. However, under these conditions, a short welding interval is required to make a weld and a high current value necessary to develop sufficient heat.

An important observation as a result of these measurements is that the resistance of the weld at welding temperature depends on the contact diameter only and is not affected by the applied pressure. Based on this fact, measurements were made as follows: A steel sheet was placed between the electrodes and heated. When the heat color indicated that a temperature of 1,100 degrees centigrade had been reached the flow of current was interrupted. The voltmeter reading was used to determine the resistance of the hot metal and the contact diameter was obtained from the mean value of the diameters of the electrode impressions on each side of the sheet.

Here again, complete measurements were made with only one sheet between the electrodes to eliminate the effect of the center-contact resistance. Weld resistance, R_w, plotted against the contact diameter, d, reveals a hyperbolic relationship. Where $d=0$, $R_w=\infty$ for any thickness of the sheet, but when $d=\infty$, $R_w=0$. Since reciprocal co-ordinates reduce hyperbolic functions to straight lines a reciprocal scale was used for diameter in figure 9.

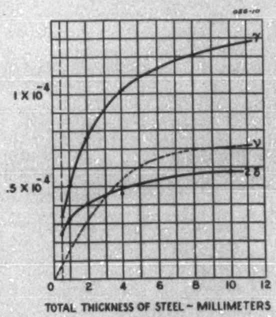


Figure 10. Resistance in weld area of mild-steel sheet

2δ = resistance of both contacts
ν = resistance of metal
γ = total resistance

This diagram illustrates the resistance across a clean, mild-steel sheet 0.5, 1, 2, 4, and 10 millimeters thick at welding temperature. Measurements cover the range of contact diameters commonly used, that is, between 1.5 and 7 millimeters ($^1/_{16}$–$^1/_4$ inch). Extreme extrapolation beyond these limits should be avoided. Straight lines through the measured points indicate the following relationship

$$R_w=\frac{\gamma}{d} \tag{4}$$

The value of γ is determined by the specific resistance and the relationship follows Holm's formula (1). However value of γ changes for various sheet thicknesses even though the temperature of the weld remains constant. Although the data points are scattered, this variation does not exceed the accuracy of the measuring method. The differences are due largely to the difficulties encountered in accurate measurement of the hot contact diameter.

Values of γ plotted against the sheet thickness are shown in figure 10. This curve indicates a probable constant maximum value, similar to the curve $\rho=f(T)$ in figure 7. It seems logical to assume that the resistance of the space conductor in the metal will not vary greatly under different conditions of cooling, since the welding temperatures are in the range of ρ max. Conversely, the temperature of the contacts depends largely upon the method of cooling of the welding tips and the contact resistance varies with the change in temperature. It is advisable to divide the total weld resistance into contact resistance and sheet resistance to permit estimation of the weld resistance under other conditions.

Measurement of Contact Resistance

The voltage drop caused by the contact resistance was measured by use of the special electrode illustrated in figure 11. The hole through the center was made 1 millimeter in diameter and a copper wire 0.5 millimeter was inserted and insulated with asbestos. Voltage between the contact wire and the outer edge of the contact face of the electrode includes all equipotential planes. Before each reading was taken, the contact

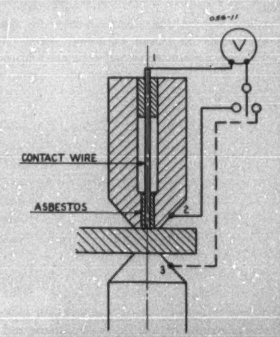


Figure 11. Special electrode and setup used in measuring voltage drop in weld area

face was filed to form a plane surface. The only measurements used were those showing a clear impression of the contact wire in the center of the electrode after contact with the sheet steel. Half of the readings were taken between points 1 and 2, and the remainder between the contact wire and the opposite electrode, that is, points 1 and 3. By subtracting the latter from the reading of the voltage across the weld (points 2–3), the error in measurement of these small voltages was reduced.

In figure 9 the resistance of one contact, computed from the voltage and the current across the contact, is plotted against the contact diameter using reciprocal co-ordinates. This was done for various sheet thickness as noted, the metal being in the range of welding temperatures as in the previous measurements. This relationship may be written as follows

$$R_c = \frac{\delta}{d} \tag{5}$$

This equation is also similar to Holm's formula, equation 1. There is however, a great difference between the meaning of ρ in equation 1 and of δ in equation 5. In equation 1 ρ is a function of the contact temperature, while δ in equation 5 is a function of sheet thickness. Both equations would be comparable, if the contact temperature were equal with all contact diameters for a given sheet thickness and if the contact temperature increased with the sheet thickness. This is impossible of course, since contact of a given diameter reaches equal temperature for all sheet thicknesses because of equal metal temperature and of equal heat conductivity of the electrodes.

Following Holm's theory, a temperature-resistance curve for copper-steel contacts was plotted as in figure 7 based on $\rho = f(T)$ for steel and for copper. In this curve, the following temperatures correspond to the values as shown in figure 9.

Sheet thickness t (millimeters)	0.5	1	2	4	10
Theoretical contact temperature (degrees centigrade)	150	280	390	440	530

The actual contact temperature was checked by the heat flow at equilibrium. This balance was obtained by suitable current adjustment of the welding machine. For the contact diameter of 0.3 centimeter, the heat loss through one electrode was found to be practically constant for all sheet thicknesses. A loss of 735 watts with a given heat conductivity of the electrode gives an actual contact temperature of 550 degrees centigrade. (In this instance the cooling was very inefficient.) With contact diameter of 0.4 centimeter, the loss was 1,000 watts giving an actual temperature of the contact of 650 degrees centigrade.

Thus the theoretical temperature approaches the actual temperature with an increase of the sheet thickness and with a decrease of the contact diameter. The apparent variance also might be a result of the different heating conditions due to two contacts rather than one contact. Holm's theory is based on the assumption, that the heat generated in the contact is equally dissipated in both directions. In the case of the weld, heat is generated in both of the contacts and the metal and flows unidirectionally from the metal toward the contact.

Referring to figure 10, the curve 2δ shows the resistance of both contacts. The curve ν, which is the difference of curve γ and curve 2δ, gives the apparent resistivity of the metal space conductor for the equation

$$R_m = \frac{\nu}{d}$$

The curve ν indicates a maximum value, indicating the limit of the heat absorbed by the metal of the weld. This may explain the difficulties encountered in welding of the heavy sheet gauges.

To check values of ν given by figure 10, the resistance of the space conductor was computed from equation 2 for the various contact diameters and sheet thicknesses, using the maximum resistivity ρ max $= 0.96 \times 10^{-4}$ ohms per centimeter cube. With contact diameters of 0.5 centimeter and larger, close agreement was found between the computed values and the curve ν. With contact diameters of less than 0.5 centimeter the resistance exceeded the values of the curve ν. This is as expected in that as the diameter decreases more of the current fibers pass through the outer, colder zones of the metal and results in a lower value of resistivity of the space conductor.

All of the preceding investigations with steel at welding temperatures were based on only one sheet between the electrodes. However actual welding is never performed under this condition. To simulate actual welding conditions two ground mild-steel sheets of equal thickness were placed between the electrodes of the welding machine and the resistance of the weld at welding temperature was measured by the same methods. The resulting values of R_w for steel sheets 2×0.5, 2×1, 2×2, and 2×4 millimeters were plotted against the contact diameter and are shown in figure 9. These values compare closely with the measurements taken with one sheet of twice the thickness. This proves that the resistance of the contact between the sheets is negligible compared to the total weld resistance. This appears at variance with equation 1 which shows that with the relatively high resistivity of hot steel the contact resistance should attain a considerable value even with large contact diameter. This may be explained as follows: Two plane surfaces of the same metal under pressure and at red heat do not represent the contact sieve of Holm's theory, but fuse together offering resistance to the current flux. This occurs prior to the consummation of the weld, possibly when the creep strength of the heated metal is exceeded.

Summary

This paper presents a study of the resistance of spot welds in ground mild-steel sheets. Surfaces of the sheets must be free from visible oxide coating. The resistance at ambient temperature which determines the power input into the weld at the beginning of the welding cycle consists of two contact resistances and the resistance of the space conductor in the metal. A ratio was found between the resistance of the space conductor and of the cylinder conductor as shown in figure 4. With heavy pressure and ground and polished sheets, the contact resistance between the sheets was found to be insignificant. Contact diameters were determined from equation 3, using the compressive strength of the hard-drawn copper tip against the ground, mild steel as 84.5 kilograms per square millimeter.

Resistance of the weld at the welding temperature, which determines the current required for the greater part of the welding cycle, depends only on the contact diameter and on the total sheet thickness. Values of γ for equation 4 are plotted against the total sheet thickness in figure 10. The same result was obtained with one or two sheets and whether or not a weld was made. Relative importance of both components of the weld resistance is illustrated in figure 10 by the separation of γ into contact resistance 2δ and sheet resistance ν. It was proved that the resistance of the contact at elevated temperatures does not follow Holm's theory with regard to temperature coefficient. This may be explained in that variable heating conditions are found in the contact since the majority of the weld resistance with thin sheets is incorporated in the contacts,

while with thicker gauges a larger proportion of the resistance is through the sheet. The value of ν cannot be affected materially by more efficient cooling of the electrodes as this resistance is determined by the welding temperature. However, the temperature of the contacts depends upon the efficiency of the cooling method, the contact resistance being reduced by greater heat dissipation from the contact. It should be borne in mind that there is no direct relation between the contact temperature and resistance; therefore, for thin gauges only a small difference can be expected from the values given above.

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Power-Factor Correction of Resistance-Welding Machines by Series Capacitors

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Synopsis: Resistance-welding machines usually are characterized by a large intermittent kilovolt-ampere demand at low power factor which is sometimes difficult to handle on the available power circuits due to regulation problems. Frequently the situation is so acute that power companies are unable to serve large resistance-welding machines, because the cost of reinforcing the system to render the desired service is disproportionate to the revenue received from such an installation.

This paper explains briefly the reasons why the usual method of power-factor correction by means of shunt capacitors is not satisfactory, and presents a system of power-factor correction using series capacitors which avoids the undesirable characteristics of shunt capacitors for this service. Where synchronous control is used, it is shown that by establishing a precharge on the capacitor, the welding transformer can be energized without transients and with complete power-factor correction, thereby greatly reducing the kilovolt-ampere demand. With this system of operation, a given feeder can handle more or larger welders and regulation problems are greatly minimized. Large welders, heretofore considered impractical because of excessive kilovolt-ampere demands, can be served by this means.

The operating characteristics of the power-factor-corrected welding machine, and several methods of current control which may be used with it are described. Oscillograms of an actual welding machine taken under various operating conditions are also included. They show a resistance-welding machine which normally demands 434 kva at 41.5 per cent lagging power factor being served with a line demand of 181 kva at unity power factor. The paper also demonstrates that this system of power factor correction is applicable both to synchronous and nonsynchronous controls and that many benefits accrue for both cases.

THE problem of power-factor correction of resistance-welding machines is of current interest because of their widespread and rapidly growing application. In addition, new and improved methods of capacitor production have diminished the cost of capacitors to a point where it now becomes economically feasible to apply them in many cases where previously the cost was prohibitive.

One of the limitations of resistance-welding machines, both in size and number, is the large intermittent kilovolt-ampere demand at low power factor, which imposes a difficult load on the power system both from the standpoint of feeder regulation and capacity. In many locations, the power company cannot serve large resistance-welding machines because of the small revenue and high installed capacity required to serve them, or because of the "flicker" problem. Under these conditions, the manufacturer either must avoid fabrication of large parts by this admittedly good and usually inexpensive method; or else install large expensive transformers and feeders, or even more expensive rotating machinery to balance the single-phase load on a three-phase system and to smooth out the intermittent load on the system by means of its inertia. Very often the problem of supplying a large number of relatively small welders becomes acute because of the limitations of the power supply, and the interference between welders due to regulation drop.

A large part of the power companies' difficulty in handling the load would be eliminated if it were feasible to correct the power factor to approximately unity so that the power system would have to supply only the energy component of the load.

Regulation problems, interference between given welding machines on the same feeder, flicker, and other problems would all be greatly minimized by such correction. Furthermore, a given feeder could handle many more or larger welders if these machines were power-factor corrected. In many cases, penalties are imposed for peak kilovolt-ampere demand of welding machines. In such cases both the power company and the plant operator will welcome any practical system of power-factor correction, as both parties will benefit thereby.

This paper demonstrates that power-factor correction of resistance-welding machines by means of series capacitors is feasible and that it avoids many of the difficulties encountered with the application of shunt capacitors for this purpose. Since shunt capacitors are so universally used for power-factor correction on other apparatus, it is desirable to discuss the problems arising from their use for correction of the power factor of resistance-welding machines.

Shunt Capacitors

The merits of power-factor correction have been recognized for a long time.

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Shunt capacitors have been used very successfully on many more or less constant-load applications for many years. The problem of power-factor correction of resistance-welding machines is unusual as compared with ordinary power-factor correction problems in that the load is of a highly intermittent nature. For instance, a spot welder may operate 100 times per minute, and seam welders may be energized 900 or more times per minute. The amount of inrush to a shunt capacitor each time the welder is energized may be several times the normal welder inrush, depending upon the phase angle of closure of the circuit. Thus, shunt capacitors frequently aggravate rather than help the power-supply problem on a highly intermittent load. It is theoretically possible to eliminate the charging inrush current to the shunt capacitors by precharging them to the correct voltage and connecting them in parallel with the welder primary synchronously and simultaneously with the closure of the primary circuit to the power-supply feeder. This can be accomplished with synchronous control (such as thyratron control). However, such an arrangement requires two sets of power tubes. One pair of tubes permits the precharge of the capacitor by disconnecting it from the welder primary (the precharge would immediately leak off through the welder primary if the capacitor was not disconnected from it) and the other pair of power tubes would act as a synchronous switch on the supply circuit to control the welder in the conventional manner (see figure 1). The controlled power tubes symbolically represented in these circuits may be of any type. Usually they are hot-cathode thyratrons or pool-type ignitrons. All timing and control circuits have been omitted from the diagrams for simplicity.

Series Capacitors

The extra pair of power tubes encountered in the use of shunt capacitors, as mentioned above, may be eliminated by making use of another form of circuit tuning or power-factor correction known as "series resonance". The phenomenon of series resonance is well known and frequently used in radio circuits. There are certain characteristics of the series-resonance circuits which make them ideally suited to highly intermittent low-power-factor loads, many of which exist in industry today. For the sake of brevity this paper is concerned primarily with the application of series capacitors to power-factor correction of resistance-welding machines.

The fundamental circuit is shown in figure 2. It will be noted that in this circuit only one pair of power tubes is required, and that these tubes effectively block the capacitor from discharging during the "off" time, thereby permitting the capacitor to be precharged to the proper value as required for transient-free operation.

The theory of operation of this circuit can best be demonstrated by reference to the vector diagrams shown in figure 3. These vector diagrams represent the conditions in a series circuit (see figure 2) operating on an essentially sine-wave voltage supply, and in a circuit which is linear (no saturation). These conditions are approximated in a properly designed welding-transformer circuit, operating on full wave. (The conditions in a phase-controlled welder cannot be represented by a simple vector diagram but can be readily calculated by mathematical analysis.)

Figure 3*a* shows the conditions existing when the welding-transformer load is corrected to unity power factor. Figure 3*b* represents the same welding transformer partially corrected so that the line is supplying a somewhat lagging load. Figure 3*c* represents the same welding transformer overcorrected so that the line is supplying a leading load.

Figure 8 shows the actual voltage and current relations corresponding essentially to the unity power factor case. It represents part of an interrupted spot weld made on two pieces of iron. Note that the supply-line current (trace *B*) is essentially in phase with the supply-line voltage (trace *A*). Note also that the current (trace *B*) lags the welder voltage (trace *C*). That is, this oscillogram demonstrates the current and voltage relations in both the power-factor-corrected welder (traces *A* and *B*) and the conventional welder (traces *B* and *C*). The calibration of the voltage traces on this oscillogram are all approximately equal so that their magnitudes and the correction ratio may be determined by comparing their relative size.

For purposes of comparison, let the correction ratio be defined as follows:

$$\text{Correction ratio} = \frac{\text{peak kva demand of welder}}{\text{peak kva supplied by line}}$$

The correction ratio, as thus defined, is not a complete measure of the beneficial effects of power-factor correction since, in addition to the lower kilovolt-ampere demand on the line required to make a given weld, the line is supplying this smaller demand at essentially unity power factor so that the voltage drop in the supply system is further reduced.

In the particular case shown in figure 8 the welder was used as an interrupted spot welder welding two pieces of iron each one-quarter inch thick. The welder power factor was 41.5 per cent lagging and the welder demand was 434 kva. The line power factor was essentially unity (99.5 per cent leading) and the line demand 181 kva or a correction ratio of 2.4, with a power factor improvement from 41.5 per cent lagging to essentially unity.

It should be observed that the current of figure 8 contains no transient and the capacitor retains its charge during the off interval. Further, it should be noted that the magnitude and polarity of the residual charge on the capacitor is left exactly correct so that the next spot begins with no transient. This is due to the fact that the control is arranged for unipolar starting, and also for an even number of half cycles or an integral number of

Figure 1. Shunt-capacitor power-factor-correction circuit

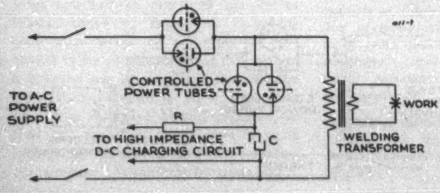
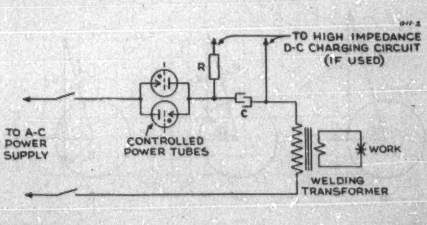


Figure 2. Series-capacitor power-factor-correction circuit



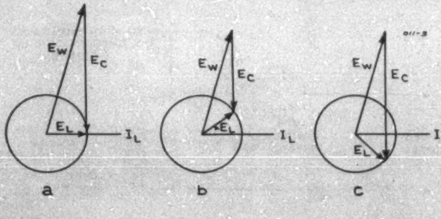
cycles per spot. No precharging circuit was used in this case, the correct capacitor precharge having been established by the preceding spot. Consequently, where a precharging circuit is used, it would have no function after the first spot other than to supply a small leakage current to the capacitor. When it is desired to use an odd number of half cycles per spot, antipolar starting can be used with perfect results, with no precharging circuit.

Figure 9 shows a case of overcorrection where the circuit was corrected to a leading power factor. In this case the welder power factor was 36.6 per cent lagging and the corrected power factor at the supply line was 85.7 per cent leading. It is of particular interest to note that in this case the line voltage shows a slight rise when the welder load comes on. This is due to the fact that when a leading current is drawn from an inductive feeder or supply, there is a rise in terminal voltage. It can be readily seen that if the current is made sufficiently leading to overcome the *IR* drop in an inductive feeder there will be a voltage rise under load. This is a very significant result in that it indicates that the troublesome "flicker" problem and regulation problems caused by resistance-welding machines can be greatly reduced or in some cases eliminated entirely.

Figure 10 shows an interrupted spot weld where no precharge was applied to the capacitor. It will be observed that the initial charging transient current is small, rather than large as would be the case with shunt capacitors. After one cycle the current is substantially up to normal. In a long spot or interrupted spot weld this smaller current during the first cycle makes no significant difference in the total heat delivered to the weld, and consequently for such welds no precharging circuit need be used. The reason for the lack of inrush current on the discharged capacitor is due to the fact that the welder impedance is in series with the capacitor. Furthermore, the driving voltage available on the welding transformer is small until the capacitor charge has built up to its correct value.

Figure 3. Vector voltage relations in series-capacitor power-factor-corrected circuit

I_L—Line current
E_L—Line voltage
E_W—Welder primary voltage
E_C—Capacitor voltage



For short welds, such as from one to ten cycles duration, it is recommended that either a precharging circuit be used or that the first weld be made on scrap stock to obtain the proper charge on the capacitor. Once this charge is established, the welder will immediately give uniform welds, and the charge will automatically be left on the capacitor of the correct polarity and magnitude for the next spot as previously explained.

Figure 10 also demonstrates the use of the phase-control method of reducing current on a power-factor-corrected welding machine, as indicated by the gaps in the current and voltage traces. This method of current control will be discussed under "Methods of Current Control".

Methods of Current Control

There are a variety of methods of current control applicable to power-factor-corrected welding machines. Current control is of utmost importance in resistance welding, since it governs the rate of generation of heat at the weld. The methods may be classified as follows:

1. Phase control
2. Variable-voltage supply
   - (*a*). Tapped autotransformer on supply circuit
   - (*b*). Induction regulator on supply circuit
   - (*c*). Field control on supply generator (if used)
3. Variable reflected resistance at resonance
   - (*a*). Simultaneous switching of taps on welding transformer and capacitor bank
4. Dissonance regulation
   - (*a*). Taps on welding transformer
   - (*b*). Taps on capacitor bank
   - (*c*). Frequency control
5. Variable primary resistor (practical only on small welders)

These methods will be discussed in the order listed above.

1. Phase Control Method of Current Regulation

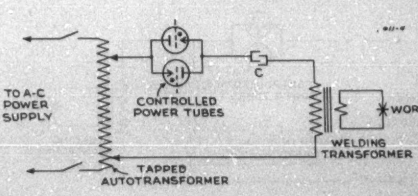
This method is applicable to power-factor-corrected resistance-welding machines in the same manner as it is applied to the uncorrected thyratron-controlled machines. It differs slightly in that for complete control, the phase-shift network must be adjusted to shift over 180 electrical degrees of the supply voltage, whereas in the conventional welder control, complete control is obtained by shifting over a span of 180-β electrical degrees where β is the power-factor angle of the circuit.

It should be pointed out that "power factor" and "kilovolt-amperes" no longer have the same significance for chopped waves that they carry for full waves. To obtain the fullest possible benefit from the capacitors, it is preferable to operate at full wave, although the correction ratio, as previously defined, holds up well for phase-controlled waves. For very low-phase-controlled currents where the correction ratio diminishes, the current is so small that the regulation problem is not important.

Figure 11 shows the results of phase control on a power-factor-corrected welding machine. It should again be observed that the capacitor automatically arrives at the proper precharge for this phase-controlled condition in the same manner as it did for the full-wave case.

In the case of an inductive feeder, the reactance drop, Ldi/dt, is caused by the rate of change of current. In the case of a full-wave welder corrected to unity power factor, the maximum rate of change of current occurs when the supply line voltage is zero, and the rate of change of current is zero when the voltage is at its crest, consequently the feeder regulation drop is small. If, however, in this same circuit, the starting point of each current wave is delayed 90 electrical degrees by means of phase control, the maximum rate of change of current will occur at the crest of the voltage wave and consequently a

Figure 4. Variable-voltage supply circuit for series-capacitor power-factor-corrected resistance-welding-machine circuit



reactance regulation drop will appear. Ordinarily this is not troublesome, however, since the kilovolt-amperes are greatly reduced at the lower currents and further reduced by the correction ratio. Delay of the point of ignition by phase control (reduction of welding current) may be considered as a reduction of power factor, since it introduces higher harmonics in the current wave. For these higher harmonics the circuit is no longer tuned to resonance, as the inductive reactance $(2\pi fL)$ increases while the capacitative reactance $(1/2\pi fC)$ decreases.

When any welder is operating with phase control, the peak current demand on the line is greater than for a full-wave welder making the same weld. However, in the full-wave condition where the demand is maximum, the peak current required is the same, since it is full wave for both cases. Consequently, where it is desired to minimize peak current load on the supply for the maximum current setting of the machine, there is no disadvantage in using phase control. However, in cases where it is desired to minimize the peak current on the supply system at all times, and especially under reduced current conditions, it is necessary to use full wave. Where many welders are operating simultaneously on a high production schedule, at currents considerably reduced from the maximum possible per machine, substantial saving in peak current demand can be effected by using full wave on all machines.

It should be emphasized that the simple vector diagram applicable to the full-wave case is not applicable to the phase-controlled case, except for determining maximum voltages and currents which can exist in the full-wave condition.

2. Variable-Voltage-Supply Method of Current Regulation

The variable-voltage supply in the form of a tapped autotransformer located ahead of the power tubes, as shown in figure 4, makes an ideal method of control for a power-factor-corrected welder. If desired, phase control may be used in addition for finer adjustment between coarse taps. Such an autotransformer is relatively small because it handles only from one-half to one-third the welder kilovolt-amperes, depending upon the correction ratio. In addition, the fact that it is an autotransformer rather than a transformer again diminishes its size by a factor of two or more, depending upon the range of control required, so that the resulting transformer is small indeed as compared with the welding transformer. It would be from one-half to one-third the size of the autotransformer used with a conventional welding transformer where they are furnished. This method is advantageous in that the welding machine may be made to operate at unity power factor (as far as the supply line is concerned) and with full wave at all currents, thereby minimizing regulation and peak currents on the supply line, and making fullest use of the capacitors at all times. The capacitor precharging circuit, not shown in figure 4, may be used if desired.

Other sources of variable-voltage supply mentioned operate in essentially the same manner. Where a rotating machine is used, a much smaller size machine can be used for a given welder; or a given rotating machine will supply a much larger welding machine where power factor correction is applied.

3. Variable Reflected Resistance at Resonance Method of Current Regulation

The resistance of the work, referred to the primary of the welding transformer, is proportional to the square of the turn ratio. For the full-wave resonant condition, the current is limited only by the resistance of the circuit. Assuming a constant line voltage, and that the circuit is tuned to resonance in each case, it is possible to vary the current to the work by changing the turn ratio and switching the capacitor to maintain resonance. Consider the full-wave case (without phase control). If for a given piece of work the turn ratio is doubled, and the capacitor changed by switching to again tune the circuit to resonance, the primary current will be approximately one-fourth of its former value, and the secondary current will be approximately one-half its former value since the turn ratio is doubled. The primary voltage will remain essentially constant since the current has decreased to one-fourth and the impedance has increased by a factor of four.

The effect of variable reflected resistance on the current in a series-capacitor power-factor-corrected resistance-welding machine of any power factor is the same as for the case of $\phi = 1.00$ on figures 6 and 7. The disadvantage of this method of current control is that it requires switching of both the capacitor and welding transformer.

4. Dissonance Regulation of Current

The maximum response of a series tuned circuit occurs at resonance. If the circuit is detuned either by changing the inductance, capacity, or applied frequency, the response can be markedly changed as indicated by the curves shown in figure 5, which illustrate the effect of change of inductance in a simple series circuit operating on full wave. Similar curves may be made for detuning by taps on the capacitor or by frequency control. Analogous conditions exist in an actual welding-transformer circuit with power-factor correction by series capacitors, except that the reflected impedance changes with the tap setting rather than remaining constant, as in the simple *RLC* circuit characteristics shown in figure 5. It should be observed that the resonance peaks become sharper at the lower power factors and the tuning is therefore more critical. For power factors of 20 per cent to 40 per cent, which are typical of welding machines, the resonance peaks are sufficiently broad so that tuning is not difficult.

Figures 6 and 7 show the effect of changing taps (turn ratio) in a welding-transformer circuit in which the capacitor has been adjusted for unity power factor on the highest current tap. The effects of primary resistance, magnetizing reactance, and leakage reactance have been omitted from these curves for simplicity. Their effect will be to modify the curves slightly from the values shown for an ideal transformer. These curves are drawn for

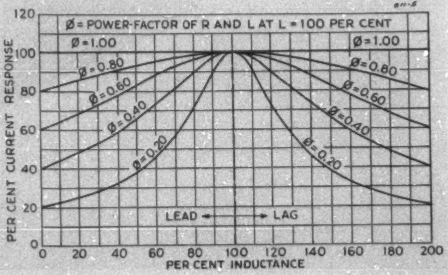


Figure 5. Current response as a function of inductance for a series circuit containing resistance, inductance, and capacitance

Resistance, applied voltage, and frequency maintained constant

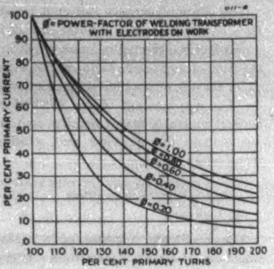


Figure 6. Primary current variation with change of turn ratio

Capacitor adjusted for unity line power factor at 100 per cent turns

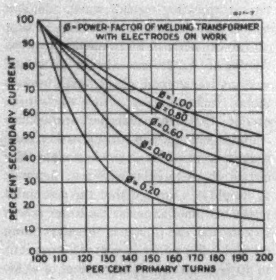


Figure 7. Secondary-current variation with change of turn ratio

Capacitor adjusted for unity power factor with 100 per cent turns

the full-wave condition. It should be noted that the current response variation is greater at lower power factors, as would be expected from the greater sharpness of the resonance curves. It is interesting to note that the curves labeled $\phi = 1.00$ on figures 6 and 7 also represent approximately the current response in an uncorrected welding machine of any power factor, as the primary winding is changed by taps.

In this dissonance method of current control the feeder is not operating at unity power factor, except at one point; consequently the capacitors are not used to best advantage. Furthermore, taps would have to be considerably smaller increments than on the conventional welder to give the same degree of control, particularly on low-power-factor welders—see figures 6 and 7. Where taps on the welding transformer alone are used, the capacitor should be adjusted to give unity power factor, and consequently minimum kilovolt-amperes, on the highest heat tap, where it is most needed.

5. Variable Primary Resistor

The current in a unity-power-factor-corrected welding machine is limited only by the resistance of the circuit, which includes the primary resistance and also the secondary resistance and resistance of the work referred to the primary side. Therefore, by adjusting an auxiliary series variable resistor placed in the primary circuit, control of the current can be readily obtained. This has the further advantage that the additional resistance acts as ballast and consequently the current is less affected by changes in resistance of the work. Because of the additional power loss in the auxiliary resistor, this method of current adjustment is recommended only for small machines or for large welders operating on very low duty cycles.

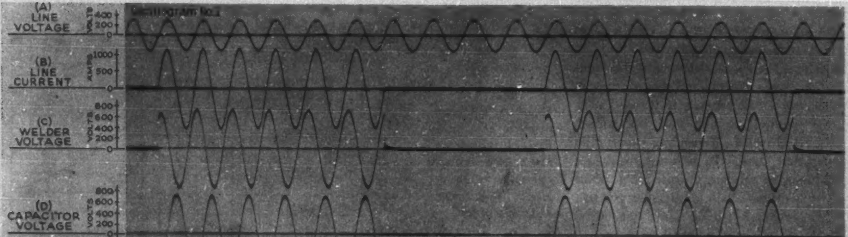
Use With Nonsynchronous Control

Series-capacitor power-factor correction of resistance-welding machines is not limited to use with synchronous control by any means. In fact, this type of power-factor correction has another outstanding advantage when used with nonsynchronous control, such as magnetic contactor control of a resistance-welding machine.

Figure 12 shows portions of an oscillogram taken on a high-speed nonsynchronous contactor making an interrupted spot weld on a conventional resistance-welding machine. Note the excessive primary transients due to unsymmetrical magnetization of the core and cumulative d-c components. It is interesting to note that the high peaks due to magnetizing transient primary current do not appear in the secondary. The line voltage is shown reversed in phase from its normal position on this oscillogram.

Figure 13 presents portions of an oscillogram taken on this same nonsynchronous control and welding transformer, operating at approximately the same welding-transformer voltage, but with series-capacitor power-factor correction. It shows the absence of large transients because of the suppression of any d-c component by means of the capacitor which acts as a blocking capacitor for any d-c component which may be caused by random starting, or inaccurate timing, or both. On this oscillogram, trace *A* is the line voltage, trace *B* the line or welding-transformer current, trace *C* the welding-transformer voltage, and trace *D* the capacitor voltage. Note that the first half cycle of current may be smaller or equal to the steady-state current but is never larger. Thus, the series capacitor not only permits use of a nonsynchronous control without excessive transients when used as an interrupted spot welder, but it also provides power-factor correction, diminishing the feeder kilovolt-ampere requirements, regulation drop, and all of the other attendant advantages of lower kilovolt-ampere demand, high-power-factor operation. It also results in much less severe duty on the welding trans-

Figure 8



former and contactor, thereby augmenting the life of both.

The fact that the current may be smaller than normal for the first cycle may not be a serious disadvantage on relatively long spots on heavy work, because this possibility may be allowed for by increasing the spot length by one cycle longer than that otherwise required. In addition to the excessive primary transients with nonsynchronous control and without power-factor correction, the secondary current may be an offset wave increasing the heat abnormally on the first few cycles, possibly resulting in burned work, splashing, high electrode maintenance, etc.

Series capacitors are applicable, therefore, for power-factor correction and surge suppression on many highly intermittent low-power-factor loads. It is expected that they will find many applications. A few of the possibilities are as follows:

Resistance spot, interrupted spot, seam welders, etc.
Flash, butt, and projection welders
Electric arc furnaces

Characteristics of the Unity-Power-Factor Welder Using Series Capacitors

There are several points of difference in characteristics of a resistance-welding machine operating with a series tuned capacitor for power-factor correction, as compared with a resistance welder operating from a constant-potential bus. Some of these differences are advantageous, others are merely points of difference which may or may not be advantageous, depending upon the type of work involved. It is not claimed that this method is applicable to all resistance-welding jobs, although our experience indicates that it is applicable to many of them.

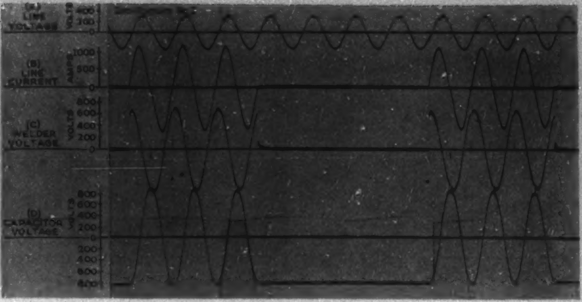


Figure 9

1. Requires less kilovolt-amperes from feeder and at higher power factor, thus improving regulation, diminishing peak demand, and permitting use of more or larger welders from a given supply. The above reasons are the principle justifications for use of this type of power-factor correction. In many instances they are vital factors in determining whether or not resistance welding should be used.

2. Current to welder is essentially resistance limited. Generally speaking, therefore, this type of machine will be subject to greater current variations with changes in resistance of the work than is the conventional low-power-factor machine which is reactance limited. As a consequence of this, low-resistance alloys and materials such as aluminum automatically draw more current than the same thickness of iron for the same voltage. However, more current is required for aluminum, hence a smaller change in circuit adjustment is required to change from welding iron to welding aluminum than would be required for a conventional machine.

3. Small changes of inductance such as might be caused by the presence of iron in the throat of the machine have less effect on the current in a power-factor-corrected welder than a conventional welder. This is because the conventional welder is reactance limited, while the power-factor-corrected welder is resistance limited. This condition exists only as long as the changes of inductance are small enough so that the circuit remains substantially in resonance.

4. Where a precharge circuit is not used and it is desired to make short, accurate spots, one spot on scrap stock should be made to establish the correct capacitor precharge.

5. The open-circuit voltage of the welding transformer cannot be measured by applying power through the series capacitor with the secondary open because the circuit is so far detuned that the current and voltage will be much smaller than exist under load conditions.

6. A phase-controlled power-factor-corrected welder using narrow-peak firing and not equipped with a precharge circuit will not fire the first spot with the capacitors discharged if the phase-control dial is set in the full-wave position. This is because there is no voltage on the starters of the tubes in the full-wave setting, since the peaks occur at the zero point of the voltage wave for full heat at unity power factor.

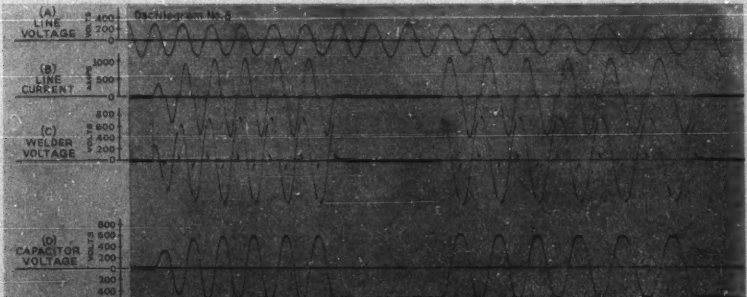


Figure 10

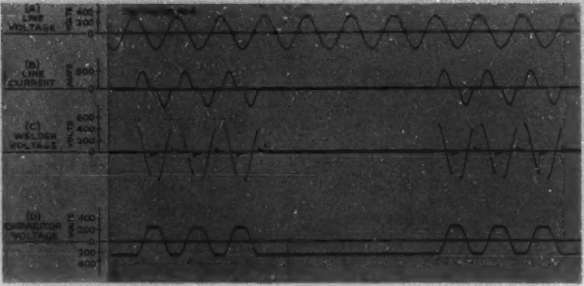


Figure 11

Where a precharging circuit is used, the capacitor voltage is adequate to start the ignitrons. Where such a circuit is not used, an initial charge is readily established by setting the phase control to a lower current and making one spot on scrap stock. The phase control can then be returned to the full-wave position and a second spot made to establish full charge if required. Where sine-wave turn-on voltage is used on the control grids this condition does not arise, since the first tube is maintained open until the voltage reaches the ignition value.

7. The capacitor bank should be equipped with a suitable discharge resistor and contactor which closes on de-energization of the line, discharging the capacitors within a few seconds to a low voltage. This discharge time may be made as short as desired by properly selecting the discharge resistor and contactor. The discharge circuit and interlocking relay, as well as timing circuit details, are not shown on the simplified diagrams.

8. Since the power-factor-corrected welder is essentially a tuned filter when operating on full wave, any distortion in the supply-voltage wave does not result in a corresponding distortion of the current wave. Instead it remains essentially sinusoidal, thereby tending to improve the current wave shape on the system.

9. When used with nonsynchronous control on resistance-welding machines, series-capacitor power-factor correction eliminates destructive high-current transients and makes practical interrupted spot welding with nonsynchronous control.

10. For small or moderate changes in throat of the resistance-welding machine no change in capacitor bank need be made, the change in reactance merely resulting in a change in the line power factor. A compromise capacitor is selected so that the power factor remains near unity. For large changes in throat depth, exceeding $1^1/_2$ to one, the capacitor or welding-transformer primary may be switched as required to maintain good power-factor correction.

11. Machines having extremely deep throats may be built for power-factor correction and such machines will require only slightly more energy from the line than a shallow-throat machine making the same weld. The reactance is entirely cancelled out by the capacitors, hence the increase in power required is due only to the increased resistance of the long throat.

12. The change of power factor occasioned by welding materials of different resistivity and the same permeability does not require any changes in the capacitor tuning, since the inductance is fixed by the geometry of the welder and the permeability of the surrounding media. Changes in resistance of the work result only in changes of current. The line power factor is not appreciably affected. Where the permeability changes, slight changes in tuning will result. Usually the changes are not great enough to require any change in circuit constants.

13. On all machines it is recommended that the transformer and capacitor be designed to withstand the highest voltage which can occur if the operator closes the jaws directly together without having any work inserted between them. In extremely low-power-factor welding machines where this does not result in an economical design, it is recommended that means be provided for limiting the voltage to safe values. The circuit may be detuned leading enough to limit excessive voltages on direct short circuit. However, this limits the maximum response under load. Detuning caused by saturation of the welding transformer may also be made to limit extreme values. Overvoltage or overcurrent trips may in some instances provide ample protection. Where overvoltage trips are provided, they should be located across the welding transformer rather than the capacitor, to avoid leakage of charge from the capacitor. Limitation of voltage on the welding transformer will also limit the voltage on the capacitor.

14. Series-capacitor power-factor correction has the following advantages as compared with rotating machinery:

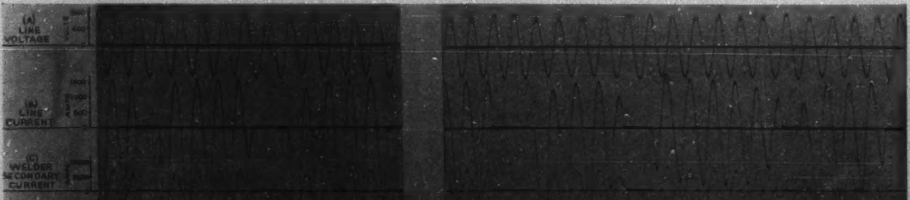
1. Lower initial cost
2. Quietness of operation
3. Low standby loss
4. Less maintenance
5. Less floor space required

15. When phase control is used as a means of current regulation on a series-capacitor power-factor-corrected resistance-welding machine, it is preferable to correct the circuit to unity power factor. Correction to a leading power factor usually results first in a slight increase of rms current as the phase control is retarded from the full-wave position until the zero point of the voltage wave is reached. Beyond this point the rms current decreases in the usual manner as the phase control is further retarded. For this reason there is usually no advantage in correcting beyond unity power factor (to a leading power factor) when phase control is to be used.

Disadvantages of Series-Capacitor Power-Factor Correction as Applied to Resistance-Welding Machines

The advantages and disadvantages of any system depend upon the job it has to do and what it is being compared with. In some cases it will be more economical to install a heavier feeder than to apply power factor correction. In other cases the power factor correction can be justi-

Figure 12



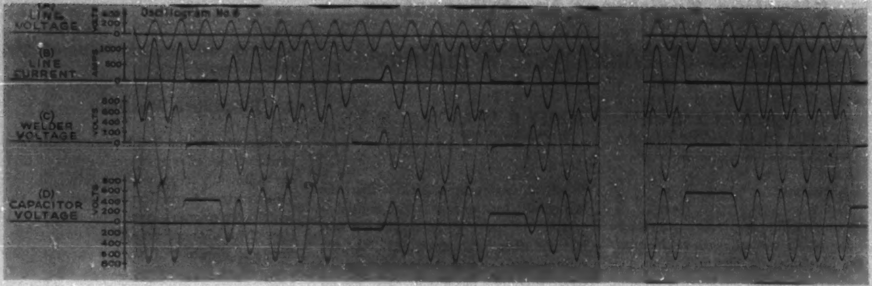


Figure 13

fied economically. The cost trend of capacitors is downward and even at the present level is considerably lower than motor generator sets. The relative sensitivity to resistance changes of this welder as compared with the conventional welder may be disadvantageous on some classes of work.

Installation on Existing Welders and Control

Where it is desired to install series power-factor correction on existing welders and control, it is necessary to lower the feeder voltage or increase the welding-transformer voltage as required by the particular job. Generally speaking, it is preferable to step down the supply voltage since this will require less change in the control, except in cases where the feeder voltage would be reduced below a practical value for synchronous control, in which case it would be preferable to increase the welding-transformer voltage. Where synchronous (electronic) control is used, modification of the phase-shift network, and in some cases modification of the tube bias voltages, may be required.

The greatest correction ratio is obtained on the lowest-power-factor machine. Machines having a power factor of 50 per cent or less, which are in the majority, will show a correction ratio of two or more in kilovolt-ampere demand, and thus will show large demand kilovolt-ampere savings. If the power factor is high to begin with, the savings are less and power factor correction may not be justified.

The application to nonsynchronous control is relatively simple, requiring only the installation of the capacitor together with a contactor and discharge resistor, and a suitable lower-voltage feeder. In any case, however, the capacitor bank and feeder voltages must be properly tailored for the job, otherwise dangerous voltages and damage to the equipment may result.

The floor space occupied by the capacitor is usually less than that required by the welding machine. Frequently floor space is not available, in which case the capacitor may be wall mounted or placed outdoors by using an outdoor type capacitor. It is not essential that it be located adjacent to the control or welder, but the closer it is the less wiring expense will be involved.

Conclusions

This investigation demonstrates the feasibility of series-capacitor power-factor correction of highly intermittent low-power-factor loads, such as resistance-welding machines with either synchronous or nonsynchronous control. By correcting the power factor, the load on the power-supply system is greatly reduced in magnitude as well as being corrected in power factor. This will minimize regulation and "flicker" problems and thereby reduce the opposition of power companies to the installation of such machines.

This method of power factor correction will promote the use of large, deep-throat welding machines previously considered impractical because of the excessive kilovolt-ampere demand. With power-factor correction, deep-throat machines will require only slightly more demand from the system than shallow-throat machines making the same weld.

The method is applicable to many resistance-welding jobs. It will benefit the user by lowering his peak kilovolt-ampere demand in cases where he is paying a penalty for high peak demand, and will benefit the power companies by minimizing disturbances on their lines caused by resistance-welding-machine loads. It will therefore broaden the field of resistance welding with both synchronous and nonsynchronous control.

Discussion

For discussion, see page 1102.

Effective Resistance to Alternating Currents of Multilayer Windings

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1. Purpose

In any multilayer winding carrying an alternating current, such as the windings illustrated in figures 1, 2, and 3, each layer of copper lies in the alternating magnetic field set up by the current in all the other layers. Eddy currents are set up in each layer in a direction to partly neutralize the magnetic intensities in the interior of the copper wire in each layer. As a result of the eddy-current losses in the copper, the *effective* resistance of the winding to the alternating current it carries may be many times its resistance to continuous currents.

In a general consideration of these eddy-current losses, it is enlightening to think of the losses in any layer as the result of the superposition of two distributions of current densities. The first distribution of current densities in any layer, n, is that which is to be attributed to the magnetic flux set up within the copper of layer n by the current in layer n itself. It is the only distribution in single-layer windings, except as noted in the footnote,* and may be called the *skin effect* distribution.

The second superposed distribution in layer n is that distribution of eddy currents which is to be attributed to the magnetic flux set up within the copper of layer n by the alternating current in all the other layers of the winding. It may be called the *multilayer effect* distribution. In windings having many layers, the loss caused by this second distribution is many times greater than the loss caused by the first. The contribution of this paper is the derivation and interpretation of expressions for the current densities and the losses in multilayer windings in which this second effect occurs.

Since the submission of the manuscript, the attention of the authors has been called to the papers[1–4] dealing with losses in multilayer a-c slot-wound armature coils by A. B. Field, Waldo V. Lyon, A. E. Gilman, and H. W. Taylor. The arrangement treated in these papers is illustrated in figure 10, which shows the cross section of the multilayered copper winding in an armature slot. If by suitable connection the layers each carry the same current, the distribution of the magnetic intensities in the space occupied by the coil will be illustrated by the heights from the zero line MM to the broken line marked H. The lines of magnetic intensity extending from face to face of the slot are substantially straight, and the effective permeability of the space occupied by the coil is substantially equal to $\mu w/s$. The differential equations taken to apply to the system illustrated in figure 10 are identical with the equations used by the authors as applicable to figures 1, 2, and 3.

The end formulas of this paper and of the papers (by the first three writers) dealing with the slot-wound conductors, that is, the formulas for computing the loss in the nth layer, can be transformed into identical forms.

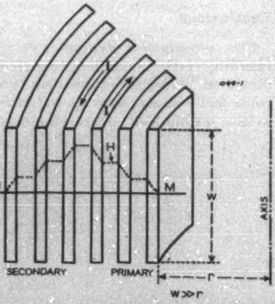

Figure 1. The distribution of the magnetic intensities in the multilayer primary and secondary of a transformer

2. Illustrative Windings

Figures 1, 2, and 3 illustrate windings in which these effects occur and to which the following treatment applies. Figure 1 illustrates an axial cross section of the multilayer primary and secondary windings of an air-core or an iron-core transformer. Figure 2 illustrates the outgoing and return conductors of bus bars each built up of transposed copper laminae (the transpositions of the laminae are now shown). It may also be taken to represent a multilayer solenoid. Figure 3 represents an outgoing bus bar built up of transposed laminations, which bus bar is remote from the return bus bar. (Neither the transpositions of the laminae nor the return bus bar are shown.)

On each of these figures the heights of the ordinates from the zero line MM to the broken line marked H_0 indicate the relative values of the magnetic intensities which would exist at and normal to the central plane MM if the current density, C, in the conductors were uniform over their entire cross section.

3. Notation

In the appendix will be found the derivation of the expressions for the current densities and the power losses in windings or structures which may be represented by figure 2. This figure may be thought of as illustrating the outgoing conductor A and the return conductor B of a bus bar, each conductor consisting of three thin wide copper *laminae* (one end of the structure and the transpositions of the laminae relative to each other are not shown). Or the figure may be thought of as representing two sides of a three-layer solenoid.

In the following discussion, the terms *winding*, *layer*, and *lamina* have the following meanings. The term *winding* designates the entire electrically conducting path between two specified terminals. The *winding* is in the form of *layers* of round, rectangular, strap, or sheet conductors. The *layers* are designated 1, 2, 3, 4, etc. The numbering starts with the layer in the region of lowest magnetic flux density and ends with the layer in the region of highest flux density.

A multilayered winding is regarded as made up of two or more *laminae* which in some cases are connected in series and

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[1] For all numbered references, see list at end of paper.

*Superposed upon the two eddy-current distributions described above, there is a third distribution to be attributed to the nonaxial component of the flux density in the copper near the edges of the layers. This causes the loss at and near the edges of the layers to exceed the loss over their mid portions. It may be called the *end effect* distribution. This effect is not treated in this paper. The equations herein derived do not apply close to the ends of the layers.

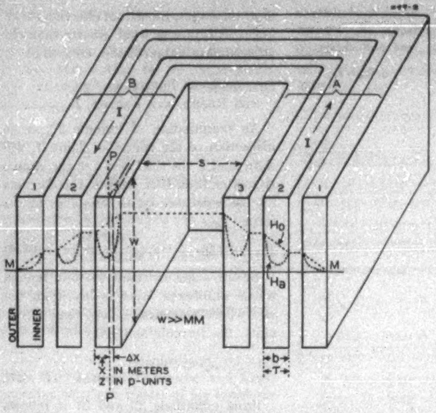


Figure 2. The distribution of the magnetic intensities in two adjacent multilayer bus bars, or in a solenoid

in other cases in parallel between the two terminals. If the *laminae* are to be connected only in series, then *lamina a* may be taken as occupying *layer* 1, *lamina b* as occupying *layer* 2, etc. But if the *laminae* of the winding are to be connected in parallel between the terminals, and if the total alternating current through the winding is to divide equally among the laminae, then the laminae must be so transposed relative to each other that each of the k laminae occupies $1/k$th of the length of each and every layer of the winding.

The equations are derived for the idealized case in which the width w of the layers or laminae is so large in comparison with the distance (MM) that the magnetic intensity normal to the mid plane MM may be taken to be zero on the outer face of layer 1, and nI/w on the inner face of layer n. Under these conditions, the distribution of the magnetic intensities normal to the plane MM will be shown by the line H_0 (figure 2) if the winding carries a continuous current, and will be typified by the line H_a if the winding carries an alternating current.

All symbols represent the values of the symbolized quantities when expressed in units of the meter–kilogram–second system, except where otherwise noted. Capital letters in bold-face type are complex quantities representing the rms value and the phase position of the sinusoidal variable symbolized.

Definitions of Symbols

IMPRESSED QUANTITIES

$\mathbf{E}_1$—rms value (in volts) of the electromotive force impressed upon the loop containing a given lamina
I—rms value of the current (in amperes) in each lamina
f—frequency of the sinusoidally varying quantities in cycles per second

INDEPENDENT VARIABLES

x—distance (in meters) from the outer face (that is, the face at the lower flux density) of any lamina to the plane PP, which is any plane in the same lamina lying parallel to its outer face
z—same distance as above but expressed in p-units (see below)

DEPENDENT VARIABLES

$\mathbf{B}$—rms value of the flux density (webers per square meter) at the plane PP
$\mathbf{C}$—rms value of the current density (amperes per square meter) at the plane PP
$\mathbf{E}$—rms value of the electromotive force of inductance in the loop containing the filaments of the plane PP
$\mathbf{H}$—rms value of the magnetic intensity (in ampere turns per meter) at the plane PP

CONSTANTS OF THE LAMINAE

γ—conductivity of the conducting lamina (in mho-meters)
μ—magnetic permeability of the lamina, $\mu = 4\pi\ 10^{-7}$ for nonferromagnetic materials
w—width of a lamina (in meters)
l—length of a lamina (in meters)
b—thickness of a lamina (in meters)
τ—thickness of a lamina when expressed in p-units. $\tau = b/p$
n—number of the layer and the lamina occupying it. Number 1 is in the region of lowest flux density
R_{nf}—effective resistance of lamina n to the frequency f
P_n—power loss in lamina n
$F_{n\tau}$—*resistance factor*, that is, ratio of the effective a-c resistance of lamina n with a thickness of τ p-units to the d-c resistance of a sheet of the same length, width, and conductivity but having a thickness of 1 p-unit
p—$1/\sqrt{\pi f \gamma \mu}$ meters (see below)

THE PENETRATION UNIT (OR p-UNIT) OF LENGTH

It is shown in section 8 of the appendix that the expressions for the distribution of current density and for the effective resistance of the laminae may be thrown into more elegant and significant forms if *the distance from the face of the lamina to any plane in its interior* is expressed, not in meters, but in terms of a unit of length which may be called the "*penetration unit* (or p-unit) of length of the material at the frequency f." The length of this unit in meters is the value of a constant $p = 1/\sqrt{\pi f \gamma \mu}$ which appears in the differential equations. The values of the p-unit for copper of conductivity $\gamma = 5.8 \times 10^7$ mho-meters for a few frequencies are:

Cycles Per Second	p-Unit (Cm)
60	0.86
1,000	0.200
5,000	0.004
1,000,000	0.0066

4. Effective Resistance of the Lamina

In section 10 of the appendix it is shown that the effective resistance to an alternating current of lamina n of the multilayer structure or winding of figure 2 may be obtained most conveniently from the formula,

$$R_{nf}(\text{ohms}) = F_{n\tau} \frac{l}{wp\gamma} \tag{28}$$

The effective resistance of all the lamina connected in series or in parallel may be

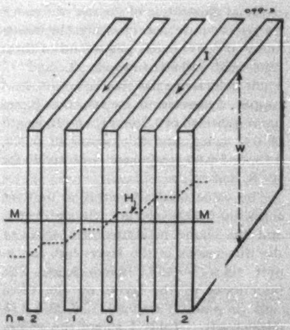


Figure 3. The distribution of the magnetic intensities in a single multilayer bus bar

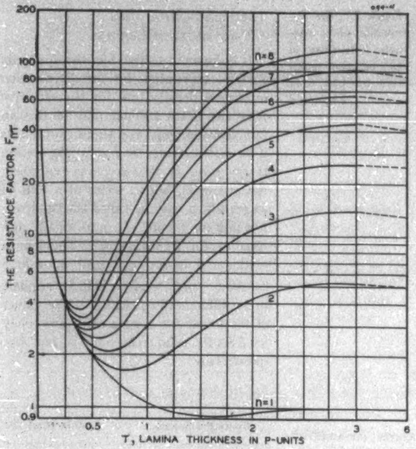


Figure 4. Resistance factor, $F_{n\tau}$ versus lamina thickness, τ in $R_{nl}=F_{n\tau}\frac{l}{wp\gamma}$

obtained by the summations (30) or (31) of the appendix.

In formula 28, the factor $l/(wp\gamma)$ is the readily computed ohmic resistance of a sheet of the lamina material having the same length and width as the lamina but a thickness equal to the penetration unit p of the material. The $F_{n\tau}$ *resistance factor* in (28) is a function of two variables, namely, the numbered position, n, and the thickness, τ, of the lamina. The value of the $F_{n\tau}$ factor may be computed from formula 29 or it may be read off from the curves of figure 4.

$$F_{n\tau}=\left[(2n^2-2n+1)\frac{\sinh 2\tau+\sin 2\tau}{\cosh 2\tau-\cos 2\tau}-(4n^2-4n)\frac{\cos\tau\sinh\tau+\sin\tau\cosh\tau}{\cosh 2\tau-\cos 2\tau}\right] \quad (29)$$

As an illustration of the use of figure 4 let us compute and compare the resistances at 5,000 cycles per second of three solenoidal inductances, A, B, and C. Each inductance has five layers of sheet copper connected in series, the layers having a width of 0.8 meter, a mean length of 0.6 meter, and thicknesses of 0.065, 0.13, and 0.26 centimeter respectively for A, B, and C.

The value of the penetration unit for copper at 5,000 cycles is 0.094 centimeter, and the ohmic resistance of a sheet of the dimensions of the layers but one p-unit thick is 13.75 micro-ohms. The thicknesses of the layers on the three coils (in p-units) are 0.69, 1.38, and 2.76 respectively. Table I shows the values of the $F_{n\tau}$ factors read from the curves of figure 4 for these three values of τ.

Table I. Values of $F_{n\tau}$ for Three Windings

Layer	Winding A $\tau=0.69$	B 1.38	C 2.76
1	1.5	0.92	1
2	1.7	2.5	5.2
3	2.1	5.6	15
4	2.7	10	26
5	3.7	16	44
$\Sigma F_{n\tau}=$	11.7	35.0	91.2
Resistances (micro-ohms)			
A-c	160	475	1,250
D-c	100	50	25
Ratio a-c to d-c	1.6	9.5	50

The high losses in the layers of the heavier copper lying in the strong magnetic field are strikingly shown in the last column of this table and by the curves of figure 4.

If the above coils, instead of being wound with sheet copper giving one wide turn per layer, had been wound with rectangular strap copper of the same thicknesses as above but yielding N turns in a layer of the same width, all the resistances for the strap-wound coils would be N^2 times the resistances in table I. (This makes no allowance for the space required for insulation between turns.)

THE EDGE EFFECT

As explained in the footnote to section 1, the current densities are higher near the edges of the layers than over the wide mid portion. Since the formulas and curves for the $F_{n\tau}$ resistance factor assume no increase in current density near the edges, the actual effective resistances will be somewhat greater than the approximate values herein computed.

APPROXIMATE FORMULAS FOR THE RESISTANCE FACTOR, $F_{n\tau}$

An examination of formula 29, or an inspection of the curves of figure 4, will show that if the thickness τ of the lamina is greater than four p-units, the formula for the resistance factor simplifies to the form,

$$F_{n\tau}=n^2+(n-1)^2 \text{ (approximate for } \tau>4) \quad (36)$$

At the other extreme, for any lamina whose numbered layer is less than ten and whose thickness is less than 0.2 p-units, the formula simplifies to,

$$F_{n\tau}=1/\tau \text{ (approximate) for } n<10 \text{ and } \tau<0.2 \quad (37)$$

From equations 33 and 37 it follows that for $n<10$ and $\tau<0.2$, the a-c resistance of the lamina is equal to its d-c resistance.

OPTIMUM THICKNESSES OF THE LAMINA

An inspection of the curves for the resistance factors figure 4 shows that the effective resistances of the laminae reach minimum values for specific lamina thicknesses. The thicknesses required to obtain minimum loss become smaller as the numbered position of the lamina becomes larger, that is, as the lamina is taken in stronger and stronger magnetic fields.

Let the thickness for which the effective resistance of lamina n is a minimum be called its *optimum thickness*, and let it be represented by τ_{no}. The formula for computing the value of τ_{no} may be

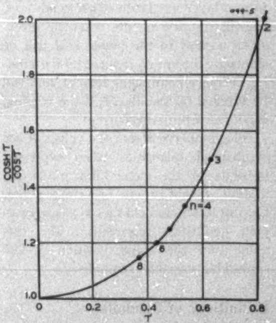


Figure 5. Curve for optimum lamina thickness

$$\frac{\cosh\tau_{no}}{\cos\tau_{no}}=\frac{n}{n-1}$$

found by taking the derivative with respect to τ of the expression for $F_{n\tau}$, (equation 29), equating this derivative to zero and solving for τ. This yields the following expression for optimum thickness,

$$\frac{\cosh \tau_{no}}{\cos \tau_{no}} = \frac{n}{n-1} \tag{38}$$

This may be solved graphically for the values of τ_{no} corresponding to different values of n by plotting in rectangular coordinates the curve of cosh τ/cos τ against τ, as in figure 5. Table II gives the optimum thicknesses for laminae in layers from 1 to 10 and the corresponding minimum values of the resistance factors, as read from figures 4 and 5.

Table II. Optimum Thicknesses and Corresponding Minimum Values of the Resistance Factors

Position of Layer (n)	$\frac{\cosh \tau}{\cos \tau}$ [n/(n−1)]	Optimum Thickness in p-Units (τno)	Minimum Resistance Factor [$F_{n\tau}$ (Min)]
1	1/0	π/2	0.918
2	2/1	0.823	1.62
3	3/2	0.634	2.1
4	4/3	0.536	2.5
5	5/4	0.473	2.85
6	6/5	0.43	3.2
7	7/6	0.393	3.45
8	8/7	0.368	3.7

5. Distribution of the Current Densities and the Magnetic Flux Densities

A clear insight into the cause of the large losses in thick lamina in the strong magnetic fields of other lamina is obtained from curves showing the distribution of current density and of magnetic flux density over the cross section of the laminae. The equations for these curves, as derived in sections 8 and 9 of the appendix, are:

C (amperes per square meter in lamina n) =

$$\frac{(1+j)I}{pw} \times \left[\frac{n-(n-1)(\exp \tau)\underline{/\tau}}{(\exp \tau)\underline{/\tau}-(\exp -\tau)\underline{/-\tau}}(\exp -z)\underline{/-z}+\frac{n-(n-1)(\exp -\tau)\underline{/-\tau}}{(\exp \tau)\underline{/\tau}-(\exp -\tau)\underline{/-\tau}}(\exp z)\underline{/z}\right] \tag{23}$$

B (webers per square meter in lamina n) =

$$-\frac{\mu I}{w} \times \left[\frac{n-(n-1)(\exp \tau)\underline{/\tau}}{(\exp \tau)\underline{/\tau}-(\exp -\tau)\underline{/-\tau}}(\exp -z)\underline{/-z}-\frac{n-(n-1)(\exp -\tau)\underline{/-\tau}}{(\exp \tau)\underline{/\tau}-(\exp -\tau)\underline{/-\tau}}(\exp z)\underline{/z}\right] \tag{19}$$

Sets of curves for ***C*** and ***B*** have been plotted for laminae of two different thicknesses, namely, $\tau=\pi/2$ p-units (the optimum thickness for lamina 1) and $\tau=2\pi$ p-units. Each curve sheet contains four curves applying to lamina 1, 2, 3, and 4 respectively. In these curves, current densities (or flux densities) in the plane PP have been plotted as ordinates against the distance z from the outer face to the plane PP as abscissas. z is expressed in p-units.

In the scale for current density (which is plotted in rms amperes per square meter) appears the factor $I/(w\tau p)$. This factor is the value of the uniform current density which would exist if the current I were a continuous current. In the scale for flux density (which is plotted in rms webers per square meter) appears the factor $\mu I/w$. This factor is the increase which occurs in the magnetic flux density from the outer to the inner face of each lamina. The phase-angles shown are the angles of lead (+) or lag (−) relative to the phase of the current I through each lamina.

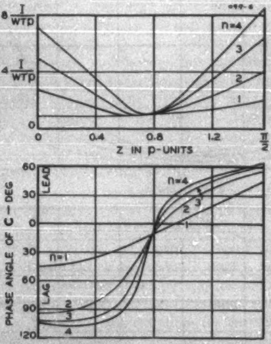


Figure 6. Distribution of current densities

Lamina thickness, $\tau=\pi/2$ p-units; $I/(w\tau p)$ is the density at zero frequency

Refer now to figures 6 and 7 showing the current density and flux density respectively for laminae of thickness $\tau=\pi/2$ p-units. The current density on the inside faces is seen to rise from 2.04 times the d-c density for lamina 1 to 8.57 for lamina 4. In lamina 1, the current density changes from an angle of lag of 45 degrees in the outside face to an angle of lead of 45 degrees in the inside face. In lamina 4, the current density changes from an angle of lag of 105 degrees in the outside face to an angle of lead of 63 degrees in the inside face. By reference to figure 4, it will be seen that the eddy currents disclosed by figure 6 cause the $F_{n\tau}$ resistance factors of these lamina to increase from 0.913 for lamina 1 to 2.9, 7.9, and 15, for laminae 2 to 4.

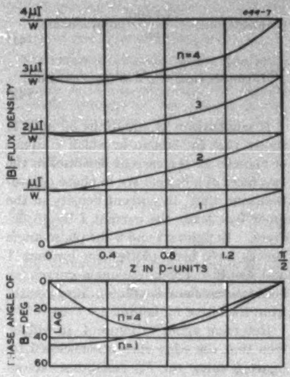


Figure 7. Distribution of flux densities

Lamina thickness, $\tau=\pi/2$ p-units; $\mu I/w$ is the increment in flux density across each lamina

By reference to the flux-density curves of figure 7, it will be seen that the eddy currents disclosed by figure 6, while sufficient to cause large losses, are not sufficient to screen very effectively the central portions of the inner lamina from the magnetic intensities of the outer lamina. This is shown by the fact that the departure from a straight line of the curves showing the increase of the flux density from face to face, while appreciable, is not striking. At the face of each lamina the flux density is necessarily in phase with the current I save in the outer face of lamina 1. As this face is approached, the flux density approaches zero with an angle of lag of 45 degrees. At the mid planes of the lamina the flux densities lag the current I by approximately 34 degrees.

The above relations are to be contrasted with those disclosed in figures 8 and 9 for laminae having a thickness $\tau=2\pi$ p-units. For laminae whose thickness equals or exceeds 2π p-units, equation 23 yields the following simple expressions for the current densities at the faces of the laminae, and for the ratio of the facial current density to the density under an equal direct current.

$$\boldsymbol{C} \text{ (outside face)} = -\frac{(1+j)(n-1)I}{pw} \quad \text{(approximate)} \tag{39}$$

$$\frac{|C_{ac}|}{|C_{dc}|} \text{ (outside face)} = \sqrt{2}\,(n-1)\tau \quad \text{(approximate)} \tag{40}$$

$$C\text{ (inside face)} = \frac{(1+j)nI}{pw}\text{(approximate)} \qquad (41)$$

$$\frac{|C_{ac}|}{|C_{dc}|}\text{ (inside face)} = \sqrt{2}\,n\tau\text{ (approximate)} \qquad (42)$$

A comparison of equations 39 and 41 shows that for lamina in which τ equals or exceeds 2π the current densities in the two faces of a lamina are in phase opposition, and that the current density in the inner face leads the current I by 45 degrees. In figures 8 and 9 the phase-angle curves have been plotted for laminae 1 and 4 only. The phase-angle curves for laminations 2 and 3 will not differ greatly from the curve for lamina 4.

Referring again to figure 4, it will be seen that the eddy currents disclosed by figure 8 cause the F_{nr} resistance factors of these laminae to increase from 1 for laminae 1 to 5.2, 14, and 27 for laminae 2 to 4. The curves of figure 4 show that no matter how much the thickness of the lamina may be increased beyond 2π p-units, there will be no further increase in the F_{nr} resistance factors. This is in agreement with the previous conclusion, namely, that increase in thickness beyond 2π will not cause the surface current densities to increase beyond the values indicated in equations 39 to 42.

This upper limit for the eddy-current densities and for the resistance factors may be accounted for by either of the following statements. It may be said that although doubling the thickness of any lamina doubles the electromotive forces induced in the lamina by the magnetic field of the outer laminae, it also doubles the reactance of the lamina to this system of electromotive forces, and so the current densities are substantially unaffected. (Note: This doubling of the reactance does not occur unless the original thickness is three p-units or greater.) An alternative statement is to say that the system of eddy-current densities which is set up in a lamina 2π p-units thick is adequate to screen the central portion of this lamina from the magnetic intensities of the outer lamina. Therefore the copper added to this screened region by doubling or tripling the thickness of the lamina cannot lead to increased current densities in the system of eddy currents.

The flux density curves of figure 9 show that for all laminae save number 1, the flux densities in the inner and outer faces of the lamina are in phase with the current I. At the two planes each located one p-unit below a surface, the flux density is only $1/\epsilon$th (36.8 per cent) of its value at the nearest surface and lags the current I by one radian. At the two planes each located $\pi/2$ p-units below the surface, the flux density lags the current by 90 degrees. Between these two 90-degree planes and the mid plane of the lamina, the flux density has a component in phase opposition to the current I, and at the mid plane itself the angle of lag reaches 180 degrees.

6. Conclusions

Multilayer windings fall into one of two types:

(*a*). The transformer type (figure 1)

(*b*). The reactor (or field winding) type (figure 2)

In the transformer type of multilayer winding, if the odd-numbered layers constitute the primary winding and the even-numbered layers constitute the secondary (or vice versa), and if the magnetizing current is negligibly small in comparison with the full-load current of the transformer, then the multilayer type of loss associated with the magnetizing current may be neglected in comparison with the loss associated with the full-load current. Under these conditions, the optimum thickness for all the interleaved laminae (except the first and the last) is π p-units. For the first and last lamina it is $\pi/2$ p-units, provided both these laminae belong to the same winding.

The other extreme in the transformer

Figure 8. Distribution of current densities

Lamina thickness, $\tau = 2\pi$ p-units; $I/(w\tau p)$ is the density at zero frequency

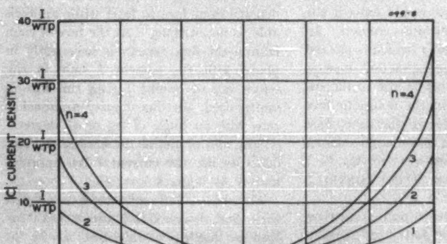
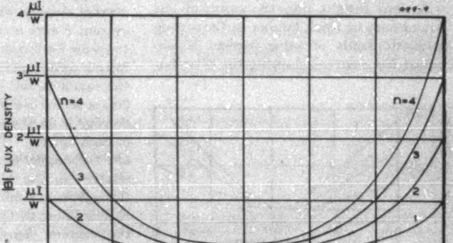


Figure 9. Distribution of flux densities

Lamina thickness, $\tau = 2\pi$ p-units; $\mu I/w$ is the increment in flux density across each lamina



type of multilayer winding would be the case in which the magnetomotive force of the magnetizing current in the primary is quite large in comparison with the magnetomotive force of any load current in the secondary. Under these conditions, little is to be gained by interleaving primary and secondary; the optimum thickness for the primary layers are those of the reactor type.

In the reactor type of winding, if the minimum effective resistance is to be obtained with k layers of a specified width and peripheral length, the thicknesses of the lamina should be proportioned as tabulated in table II. In general, it will not be practicable to vary the thickness from layer to layer, but all layers will be made of the same thickness. The optimum thickness for the laminae of a k-layered winding having all layers of the same thickness can readily be determined by the aid of figure 4.

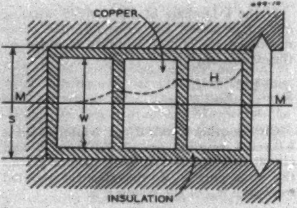


Figure 10. The distribution of magnetic intensities in a multilayer copper coil in an armature slot

If it is feasible to use layers or lamina whose thicknesses do not exceed 0.2 p-units, the *multilayer effect* will not make the effective resistance appreciably exceed the d-c resistance for any number of layers up to 10.

The single multilayered bus bar illustrated in figure 3 (far removed from the return bus bar) falls under the reactor type of figure 2. If the number of layers or laminae is even, all of the equations derived for figure 2 may be applied to the multilayered bus bar.

If, however, the number of laminae in the bus bar is odd, as illustrated in figure 3 then the laminae should be numbered $N=0$, 1, 2, etc., as shown in figure 3. Then the halves of lamina 0, which lie on opposite sides of the plane of zero flux density are each to be viewed as layer $n=1$ of figure 2. The equations for current density, flux density, loss, and resistance factor for layers $N=1$, 2, etc., are to be obtained from the equations derived for figure 2 by substituting $(N+0.5)$ for n, wherever n appears in equations 8, 9, 12, 13, 17, 19, 23, 27, 28, 29, and 34. For example, making this substitution in equation 29 for the resistance factor, it becomes,

$$F_{N\tau}=\left[(2N^2+0.5)\frac{\sinh 2\tau+\sin 2\tau}{\cosh 2\tau-\cos 2\tau}-(4N^2-1)\frac{\cos\tau\sinh\tau+\sin\tau\cosh\tau}{\cosh 2\tau-\cos 2\tau}\right] \qquad (29a)$$

Appendix. The Derivation of Expressions for the Distribution of the Current Densities and for the I²R Losses in Multilayer Windings

7. The Differential Equations of the Winding

For a winding of the type illustrated in figure 2, which is proportioned and idealized as stated in section 3, the differential equations are obtained by writing the following fundamental relations. These relations apply to all loops each of which comprises two corresponding elementary sheets of thickness Δx, width w, and total length l. One of the elementary sheets will be in an outgoing lamina and the other in the corresponding position in the corresponding return lamina.

(*a*). Relation of the Current Density to the Electromotive Force of Inductance

By equating to zero the sum of all the electromotive forces in the loop; namely, the impressed electromotive force, E, (which has the same value for all loops) the electromotive force of inductance, and the electromotive force of resistance, there results,

$$\boldsymbol{E}_1+\boldsymbol{E}+\left(-\frac{\boldsymbol{C}l}{\gamma}\right)=0 \qquad (1)$$

(*b*). Relation of the Rate of Change (With x) of the Electromotive Force of Inductance to the Magnetic Flux Density

The magnetic flux linked with the loop comprised by the inner faces of the elementary sheets of thickness Δx is less than the flux linked with the loop comprised by the outer faces PP by the magnetic flux in the sheet of thickness Δx, namely, $\boldsymbol{B}l\Delta x$. Therefore the electromotive force, $(\boldsymbol{E}+\Delta\boldsymbol{E})$, induced in the inner loop is to be found by subtracting from the electromotive force, $\boldsymbol{E}$, in the outer loop, the electromotive force induced by the flux $\boldsymbol{B}l\Delta x$, namely,

$(-j2\pi fl\boldsymbol{B}\Delta x)$.

Whence,

$$(\boldsymbol{E}+\Delta\boldsymbol{E})=\boldsymbol{E}-(-j2\pi fl\boldsymbol{B}\Delta x)$$

or

$$\frac{d\boldsymbol{E}}{dx}=j2\pi fl\boldsymbol{B} \qquad (2)$$

(*c*). Relation of the Rate of Change (With x) of the Magnetic Flux Density to the Current Density

The magnetic intensity at the inner face $(\boldsymbol{H}+\Delta\boldsymbol{H})$ is greater than the magnetic intensity $\boldsymbol{H}$ at the outer face PP by the magnetic intensity of the current in the sheet of thickness Δx, namely, $\boldsymbol{C}\Delta x$. Therefore,

$$\boldsymbol{H}+\Delta\boldsymbol{H}=\boldsymbol{H}+\boldsymbol{C}\Delta x$$

Whence,

$$\frac{d\boldsymbol{B}}{dx}=\mu\boldsymbol{C} \qquad (3)$$

By eliminating $\boldsymbol{E}$ and $\boldsymbol{C}$ between the three equations 1, 2, and 3, the following second-order linear differential equation in flux density is obtained:

$$\frac{d^2\boldsymbol{B}}{dx^2}=j2\pi f\gamma\mu B \qquad (4)$$

Using p to symbolize $1/\sqrt{\pi f\gamma\mu}$ (5)

equation 4 takes the form,

$$\frac{d^2\boldsymbol{B}}{dx^2}=\frac{j2\boldsymbol{B}}{p^2} \qquad (6)$$

8. Solution for the Magnetic Flux Density

By substituting (7) in (6) it may be verified that the general solution for the flux density may be written in the exponential form:

$$\boldsymbol{B}=\boldsymbol{B}_1\exp\{-(1+j)x/p\}+\boldsymbol{B}_2\exp\{(1+j)x/p\} \qquad (7)$$

The undetermined integration constants, $\boldsymbol{B}_1$ and $\boldsymbol{B}_2$, are evaluated from the following considerations. If equation 7 is to portray the flux densities in lamina n, and if x is measured from the outer face of this lamina, then from physical considerations it follows that:

At $x=0$, $\boldsymbol{B}_{(x=0)}=(n-1)\mu\boldsymbol{I}/w$ (8)

and at $x=b$, $\boldsymbol{B}_{(x=b)}=n\mu\boldsymbol{I}/w$ (9)

in which $\boldsymbol{I}$ represents the current carried by each lamina, and b represents the thickness of lamina n.

But for $x=0$ and $x=b$, equation 7 yields,

$$\boldsymbol{B}_{(x=0)}=\boldsymbol{B}_1+\boldsymbol{B}_2 \qquad (10)$$

$$\boldsymbol{B}_{(x=b)}=\boldsymbol{B}_1\exp\{-(1+j)b/p\}+\boldsymbol{B}_2\exp\{(1+j)b/p\} \qquad (11)$$

By equating the right members of (10) and (8) and also of (11) and (9) and then solving the resulting equations for the integration constants $\boldsymbol{B}_1$ and $\boldsymbol{B}_2$, their values for lamina n are found to be:

$$\boldsymbol{B}_1=-\frac{\mu\boldsymbol{I}}{w}\,\frac{n-(n-1)\exp\{(1+j)b/p\}}{\exp(1+j)b/p-\exp-(1+j)b/p} \qquad (12)$$

$$\boldsymbol{B}_2=+\frac{\mu\boldsymbol{I}}{w}\,\frac{n-(n-1)\exp\{-(1+j)b/p\}}{\exp\{(1+j)b/p\}-\exp\{-(1+j)b/p\}} \qquad (13)$$

The dimensions of the system constant p (defined in equation 5) which first appears in the differential equation of the system (6) are those of a length. Let us name this quantity p (which is expressed in meters) the "*penetration unit* (p-unit) of length of the material for the frequency f." The appropriateness of the name *penetration unit* will become apparent in the application of the solutions.

$$p\text{-unit (meters)}=p=1/\sqrt{\pi f\gamma\mu} \qquad (14)$$

Now an inspection of the solution 7 and of the expressions 12 and 13 for the values of the integrations constants will show that these expressions can all be written in more elegant and significant forms if the distance from the outer face of any lamina to the plane PP is expressed, not in meters, but in p-units. For if, s symbolizes the distance from the outer face to the plane PP expressed in p-units, and τ symbolizes the thickness of lamina n in p-units, then we may write:

$$\left.\begin{aligned}s&=x/p\\ \tau&=b/p\end{aligned}\right\} \qquad (15)$$

Upon substituting these values in (7), (12), and (13) they assume the forms,

$$\boldsymbol{B}\text{ (webers per square meter in lamina } n)=\boldsymbol{B}_1\exp\{-(1+j)s\}+\boldsymbol{B}_2\exp\{(1+j)s\} \qquad (16)$$

$$\boldsymbol{B}_1=-\frac{\mu I}{w}\,\frac{n-(n-1)\exp\{(1+j)\tau\}}{\exp\{(1+j)\tau\}-\exp\{-(1+j)\tau\}} \qquad (17)$$

$$\boldsymbol{B}_2=+\frac{\mu I}{w}\,\frac{n-(n-1)\exp\{-(1+j)\tau\}}{\exp\{(1+j)\tau\}-\exp\{-(1+j)\tau\}} \qquad (18)$$

Solutions written as above in terms of exponential functions are in the form most convenient for the derivation of further equations such as those for the distribution of the current densities and for the effective resistances of the lamina. But for the purpose of plotting curves and of obtaining the most effective physical insight into the relations, the above solution should be thrown into the following trigonometric forms:

$$\boldsymbol{B}\text{ (webers per square meter in lamina } n)=-\frac{\mu I}{w}\times\left[\frac{n-(n-1)(\exp\tau)\underline{/\tau}}{(\exp\tau)\underline{/\tau}-(\exp-\tau)\underline{/-\tau}}(\exp-s)\underline{/-s}-\frac{n-(n-1)(\exp-\tau)\underline{/-\tau}}{(\exp\tau)\underline{/\tau}-(\exp-\tau)\underline{/-\tau}}(\exp s)\underline{/s}\right] \qquad (19)$$

in which

$\underline{/s}$ symbolizes $(\cos s+j\sin s)$ (20)

w is expressed in meters
s and τ are expressed in p-units

9. Solution for the Current Density

Upon substituting the value of $\boldsymbol{B}$ from solution 7 in the fundamental relation 3, the following expression is obtained for the value of the current density at the plane x in lamina n:

$$\boldsymbol{C}=\frac{1+j}{p\mu}\left[-\boldsymbol{B}_1\exp\{-(1+j)x/p\}+\boldsymbol{B}_2\exp\{(1+j)x/p\}\right] \qquad (21)$$

Again expressing distances within the lamina in p-units, this takes the form:

$$\boldsymbol{C}=\frac{1+j}{p\mu}\left[-\boldsymbol{B}_1\exp\{-(1+j)s\}+\boldsymbol{B}_2\exp\{(1+j)s\}\right] \qquad (22)$$

Upon using the values of $\boldsymbol{B}_1$ and $\boldsymbol{B}_2$ and throwing the exponential form into the trigonometric form, the solution for the distribution of the current densities assumes the form:

$$\boldsymbol{C}\text{ (amperes per square meter in lamina } n)=\frac{(1+j)I}{pw}\times\left[\frac{n-(n-1)(\exp\tau)\underline{/\tau}}{(\exp\tau)\underline{/\tau}-(\exp-\tau)\underline{/-\tau}}(\exp-s)\underline{/-s}+\frac{n-(n-1)(\exp-\tau)\underline{/-\tau}}{(\exp\tau)\underline{/\tau}-(\exp-\tau)\underline{/-\tau}}(\exp s)\underline{/s}\right] \qquad (23)$$

10. Equations for the Effective Resistance of the Lamina

Let the effective resistance of lamina n of the multilayer winding to an alternating current $|I|$ of frequency f be defined as the factor, R_{nf} by which the square of the current must be multiplied to obtain the power P_n expended as heat in the lamina. That is,

$$R_{nf}=P_n/I^2 \qquad (24)$$

An expression for R_{nf} may be obtained by deriving an expression for the power loss, P_n, caused by the current I, and substituting this in the defining equation 24. P_n will be the integral of the elementary power losses caused by the known current density distribution in all the elementary sheets of thickness dx which make up the lamina.

In figure 2, the ohmic resistance of the elementary sheet of thickness dx is $R_s=l/(\gamma w dx)$ ohms. The current in the sheet is $I_s=(|C|w dx)$. The power expenditure dP in the sheet is,

$$dP=I_s^2R_s=\frac{|C|^2wl}{\gamma}dx \qquad (25)$$

The power expenditure in the lamina n will be

$$P_n=\int_0^b\frac{wl|C|^2}{\gamma}dx \qquad (26)$$

Upon substituting in (26) the value of $|C|$ as derived from equation 21, carrying out the integration for P_n, and then substituting this value of P_n in equation 24, the following expression is derived for the effective resistance, R_{nf}, of lamina n.

$$R_{nf}=\frac{l}{wp\gamma}\left[(2n^2-2n+1)\frac{\sinh 2\tau+\sin 2\tau}{\cosh 2\tau-\cos 2\tau}-(4n^2-4n)\frac{\cos\tau\sinh\tau+\sin\tau\cosh\tau}{\cosh 2\tau-\cos 2\tau}\right] \qquad (27)$$

Equation 27 expresses the effective resistance of the nth layer as the product of two factors. The factor $l/(wp\gamma)$ is the ohmic resistance of a sheet of the lamina material having a length l, a width w, and a thickness equal to the p-unit for the material. The factor within the brackets is a function of the number n of the lamina and of the thickness τ of the lamina. We may represent this function by the symbol $F_{n\tau}$, call it the *$F_{n\tau}$ resistance factor*, and for each value of n from 1 to 10 or more draw a curve with the values of $F_{n\tau}$ as ordinates and the values of τ as abscissas. See figure 4.

In this symbolism the expression for the effective resistance of lamina n may be written:

$$R_{nf}\text{ (ohms)}=F_{n\tau}\frac{l}{wp\gamma} \qquad (28)$$

in which

$$F_{n\tau}=\left[(2n^2-2n+1)\frac{\sinh 2\tau+\sin 2\tau}{\cosh 2\tau-\cos 2\tau}-(4n^2-4n)\frac{\cos\tau\sinh\tau+\sin\tau\cosh\tau}{\cosh 2\tau-\cos 2\tau}\right] \qquad (29)$$

If the k laminae of a k-layered winding are all in series, the total effective series resistance R_{sf} of the winding is given by the expression,

$$R_{sf}=\sum_{n=1}^{n=k}F_{n\tau}\frac{l_n}{wp\gamma} \qquad (30)$$

On the other hand, if the k laminae of a k-layered winding are all in parallel and are so transposed that each lamina carries the same current I, the effective parallel resistance R_{pf} of the winding is:

$$R_{pf}=\frac{1}{k^2}\sum_{n=1}^{n=k}F_{n\tau}\frac{l_n}{wp\gamma} \qquad (31)$$

11. Ratio of the Effective A-C to the D-C Resistance

If the thickness of a lamina is τ penetration units, its thickness in meters is τp. Therefore its ohmic resistance (or its resistance to direct current) R_{dc} is,

$$R_{dc}\text{ (ohms)}=\frac{1}{\tau}\frac{l}{pw\gamma} \qquad (32)$$

Upon comparing this formula with formula 28 for the effective resistance of the lamina to alternating current, it may be seen that the ratio of the effective to the ohmic resistance of a lamina may be written:

$$\frac{R_{nf}}{R_{dc}}=\tau F_{n\tau} \qquad (33)$$

$$=\tau\left[(2n^2-2n+1)\frac{\sinh 2\tau+\sin 2\tau}{\cosh 2\tau-\cos 2\tau}-(4n^2-4n)\frac{\cos\tau\sinh\tau+\sin\tau\cosh\tau}{\cosh 2\tau-\cos 2\tau}\right] \qquad (34)$$

12. Exponential Versus Bessel Solutions

The linear differential equations with constant coefficients and their exponential solutions as used in this treatment have been derived for multilayer structures in the form of plane sheets. If the plane sheets are bent to form a cylindrical multilayer winding of circular cross-sectional area, as in figure 1, the differential equations take the form of Fourier's equation and the solution is in the form of Bessel functions. However, the exponential or trigonometric solutions apply with great exactness to a

The Output and Optimum Design of Permanent Magnets Subjected to Demagnetizing Forces

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THE recent development of new permanent-magnet alloys having a high available energy has extended considerably the field of permanent-magnet applications. Prominent among these new materials is the group of aluminum-nickel-iron alloys which has been given the name "Alnico". These alloys are characterized by coercive forces of 400 to 700 oersteds, residual inductions of 6,500 to 7,800 gausses, and available energy values ranging from $1.3 \times 10^6/8\pi$ to $1.7 \times 10^6/8\pi$ ergs. Corresponding values of these quantities for three other well-known magnet materials are given in table I, and characteristic curves for these materials are shown in figure 1.

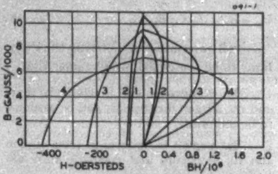

Figure 1. Demagnetization curves and available energy curves for four permanent-magnet materials

1—Chrome magnet steel
2—Tungsten magnet steel
3—Cobalt magnet steel
4—Alnico

Table I. Comparison of Permanent-Magnet Materials

Material	Coercive Force, Oersteds (H_c)	Residual Induction, Gausses (B_r)	Available Energy Ergs × 8π $(BH)_{max}$
Alnico	400–700	6,500–7,800	$1.3–1.7 \times 10^6$
Cobalt magnet steel	240	9,800	0.95×10^6
Tungsten magnet steel	70	10,600	0.32×10^6
Chrome magnet steel	60	9,200	0.23×10^6

In the past, because of the great bulk that would be required if the cheaper chromium or tungsten alloys were used, or because of the prohibitive expense of cobalt steels, the use of permanent magnets was limited to a few special applications where it was inconvenient or impossible to use electromagnets. Instruments, meters, and magnetos represent three of the chief former commercial applications. Today, Alnico is finding its way into an increasing number and variety of electrical and magnetic devices. Relays, contactors, motors and generators, magnetic clutches and chucks, and radio loudspeakers are a few of the many. This permanent-magnet material may be considered as having a possible application wherever a unidirectional magnetic field is required.

Probably because of the rapid advance in the development of these new alloys and their application to new fields of use, the development of design methods has lagged behind the application. The permanent magnet does not always lend itself readily to exact calculation, and each new design involves new problems in determining leakage constants and stabilization factors. Evershed,[1] in a valuable paper, made the first extensive attempt to establish permanent-magnet design as an engineering problem which could be solved by engineering methods. The extension of the use of permanent magnets into some of the applications just mentioned, which involve conditions of varying applied magnetomotive force or varying external reluctance, has raised problems of design not fully treated by Evershed. The object of this paper is to outline graphical and mathematical methods which will be of assistance to the designer in applying permanent magnets to new fields of use. The methods outlined are based on major and minor hysteresis loop data, and are essentially extensions of Evershed's[1] and Watson's[2,3] methods.

Elementary Considerations and Assumptions

Magnetically, a permanent-magnet material is fundamentally the same as any other ferromagnetic material in that all such materials exhibit, to some degree, the property of magnetic hysteresis. The difference between a permanent-magnet material and a soft steel lies in the relative magnitude of the magnetic properties, hysteresis, permeability (μ), coercive force (H_c), and residual induction (B_r). Hysteresis and coercive force, reduced to a minimum in magnetic sheet steels, are developed to a maximum in permanent-magnet materials. The residual induction of magnet materials is usually somewhat less than that of sheet steels, and the permeability, developed to a maximum in sheet steels, may be relatively low. The hysteresis of Alnico is about 400 times as great as that of silicon sheet steel, and its coercive force is about 700 times as great as that of silicon sheet steel.

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1. For all numbered references, see list at end of paper.

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circular cylinder in which the thickness of the cylindrical lamina is not greater than 5 per cent of its radius of curvature. This is the case in most of the applications encountered in practice. The solution in exponential and trigonometric functions lends itself to a simpler and more significant physical interpretation than does the solution in Bessel functions.

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Discussion

For discussion, see page 1262.

The elementary theory of permanent-magnet design has been adequately presented by Evershed,[1] Sanford,[4] and others. Briefly reviewing, let figure 2 represent the closed-circuit hysteresis loop of a magnet material. The point B_r, corresponding to an applied magnetizing force of zero, represents the induction remaining in the circuit after magnetization and is spoken of as the residual induction. The point H_c represents the applied negative magnetizing force necessary to reduce the induction to zero, and is called the coercive force. The portion of the loop between B_r and H_c is referred to as the demagnetization curve. It may be shown that if a magnet has initially been magnetized to a point represented by the tip of the hysteresis loop and then subjected to the insertion of an air gap in the magnetic circuit, some point on the demagnetization curve will represent the magnetizing force which the magnet is applying to the air gap and the induction existing in the magnet. To illustrate, if the point X of figure 2 represents the conditions existing for some particular magnet and air gap, the induction existing in the magnet is given by B_m and the magnetizing force being applied by the magnet to the air gap is H_m. The line from the origin through X is termed the air-gap line.

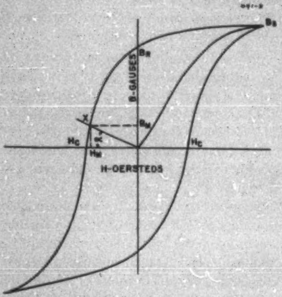


Figure 2. Symmetrical hysteresis loop for a permanent-magnet material

The location of the point X depends upon the relative dimensions of the magnet and air gap, and may be determined as follows. Let A_m represent the area of magnet cross section, A_g the area of air-gap cross section, L_m the length of magnet parallel to the direction of flux lines, L_g the length of the air gap parallel to the direction of the flux lines, and B_g the induction existing in the air gap. Then, since the magnetomotive force applied to the circuit is zero,

$$H_mL_m-B_gL_g=0$$

and

$$H_m=B_gL_g/L_m \qquad (1)$$

If leakage flux is assumed to be negligible the flux in the magnet material is equal to the flux in the air gap.

$$B_mA_m=B_gA_g$$

$$B_m=B_gA_g/A_m \qquad (2)$$

The slope of the air-gap line is defined as B_m/H_m, (tan α in figure 2), and designated by the letter S

$$S=\tan\alpha=L_mA_g/L_gA_m \qquad (3)$$

This is the reciprocal of the so-called demagnetization factor defined by various writers. The volume of magnet material, $V_m=L_mA_m$, required to maintain a magnetic field of density B_g in a gap of length L_g and area A_g is, from equations 1 and 2

$$V_m=B_g^2L_gA_g/B_mH_m \qquad (4)$$

It may be seen that the point X may be located at any desired point over quite a range of the demagnetization curve by properly proportioning the dimensions of the magnet and the air gap. The last equation above indicates that the volume of magnet material required for a given air gap is inversely proportional to the product of B_m and H_m. Hence, the point X should be located where this product is a maximum.

The equations quoted above assume that flux leakage may be neglected so that all the flux is confined definitely to the limits of the magnetic circuit, and that the induction is uniform throughout the magnet. In any magnetic circuit containing an air gap a magnetic leakage flux exists in the space about the working air gap between the ends of the magnet. This leakage flux may be a large part of the total flux, and in some cases can be included in the magnet calculations by a step-by-step method such as described by Evershed, particularly if the magnet is relatively long and of small cross section as is common with magnets of low-coercive-force materials. Fortunately, magnets of Alnico usually are relatively short and of large cross section so that all leakage flux can be treated as coming from the pole pieces or ends of the magnet.

Assuming that leakage flux is a terminal factor makes it a simple matter to modify the elementary equations 1 to 4 to include the effect of leakage flux. Let the quantity which will be called the leakage factor and designated by the symbol K_1 be defined by the following equation.

$$K_1=(P_g+P_1)/P_g$$

where

P_g = permeance of the working air gap
P_1 = permeance of all the leakage flux paths in parallel with the working air gap

The equations 1 to 4 may be modified by this factor as follows:

$$H_mL_m-B_gL_g=0$$

and

$$H_m=B_gL_g/L_m \text{ as before} \qquad (5)$$

$$B_mA_m=B_gA_gK_1$$

and

$$B_m=B_gA_gK_1/A_m \qquad (6)$$

$$S=\tan\alpha=L_mA_gK_1/L_gA_m \qquad (7)$$

$$V_m=B_g^2A_gL_gK_1/B_mH_m \qquad (8)$$

It will be assumed in deriving the equations to be presented in this paper that

1. The working air gap is relatively short and the air-gap flux density can be assumed to be uniform.
2. The magnet is uniform in cross section and its properties and magnetic state are uniform.
3. The magnet has initially been magnetized to saturation so that the discussion applies entirely to the major loop having a tip induction equal to the saturation induction and to minor loops having their origin on this major loop.
4. The quantities involved in the equations are restricted to lie within the limits of the experimental curves.

Empirical Equations and Graphic Construction of Demagnetization Curve

The problem of determining the output of a magnet which is initially magnetized to saturation and not subjected to greater demagnetizing forces than the working air gap is only a special case of the general problem in which variable conditions are involved. However, since this problem can be analyzed more simply as a specified problem, it deserves preliminary consideration.

The demagnetization curve for a permanent-magnet material can be approximated quite closely by Lamont's equation as modified for a material of high coercive force.[3] In terms of the constants of the material this equation results in the following expression:

$$B=\frac{H+H_c}{\frac{H_c}{B_r}+\frac{H}{B_s}} \qquad (9)$$

The quantity B_s is not the true saturation induction, since the curve does not retain its hyperbolic form for positive values of H. For Alnico, B_s is considerably lower than the saturation induction, and for other materials it is closer to it as B_r is larger.

The following equation is approximately true for a sample of Alnico in which $H_c = 430$ oersteds, $B_r = 7{,}400$ gausses, and $B_s = 10{,}000$ gausses.

$$B = (H+430)/(0.0580+0.00010H) \text{ gausses } (H = \text{oersteds})$$

The hyperbolic equation suggests a simple graphic construction for the demagnetization curve based on three constants for the material. This construction was first proposed by Watson[2] as a means of determining whether or not the material had had the proper heat treatments. It has been described also by others.[4]

The hyperbolic equation and the graphic construction have been applied to various compositions of Alnico, as well as to the other magnet materials listed in table I. The constructed curves based on three constants, B_r, H_c, and B_s were found to follow closely the experimental curves. No complete theoretical justification is available for equation 9 at the present time. It is simply an empirical relation which fits the demagnetization curve and may be useful in deriving design equations and in correlating permanent magnet data. It may be shown mathematically and checked experimentally, in so far as the above agreement exists, that if the air-gap line has a slope equal to B_r/H_c it will intersect the demagnetization curve at the point where the product B_mH_m is a maximum. The optimum design of magnet, then, may be determined from the following equations:

$$L_mA_gK_1/L_gA_m = B_r/H_c \text{ (see equation 7)} \quad (10)$$

$$A_m = B_gA_gK_1\sqrt{\frac{H_c}{B_r(BH)_{max}}} \text{ (see equations 5, 6, 8 putting } V_m = L_mA_m) \quad (11)$$

$$L_m = B_gL_g\sqrt{\frac{B_r}{H_c(BH)_{max}}} \quad (12)$$

in which $(BH)_{max}$ = maximum value of the product B_mH_m.

The values $(BH)_{max}$, B_r, and H_c are constants of the material and can be obtained from the demagnetization curve or from test data.

For a sample of Alnico, in which

$$(BH)_{max} = 1.4\times10^6/8\pi \text{ ergs}$$
$$H_c = 430 \text{ oersteds}$$
$$B_r = 7{,}400 \text{ gausses}$$

equations 11 and 12 become

$$A_m = 0.000204B_gA_gK_1 \text{ square centimeters}$$
$$L_m = 0.00351B_gL_g \text{ centimeters}$$

and

$$V_m = 7.14\times10^{-7}\ B_g^2A_gL_gK_1 \text{ cubic centimeters}$$

Equations 10, 11, and 12 apply only to designs involving constant external conditions of air gap and applied magnetomotive force, and, of course, the equations containing the numerical constants apply only to material having the characteristics designated for this sample of Alnico.

The Minor Hysteresis Loop

Many permanent-magnet applications now being made involve either a varying external reluctance or varying applied magnetizing forces, and for such applications the equations so far given are not directly applicable for complete solution of the design problems. Instrument and meter magnets which are stabilized by partial demagnetization, and holding or lifting magnets, are examples. Extension of the above design methods to cover these applications involves consideration of the minor hysteresis loop, as has been pointed out by Evershed[1] and Picou.[5,6]

The relation of the minor hysteresis loop to a condition of varying external reluctance is illustrated in figure 3. The left side of the diagram is an enlarged section of figure 2, showing the demagnetization curve. Suppose that after initial magnetization the maximum reluctance into which the magnet must operate is represented by the air-gap line OX, so that the magnet material is brought to a state represented by X. Suppose, however, that the reluctance into which the magnet operates part of the time is smaller and is represented by the air-gap line OY. The induction and magnetizing force associated with the smaller reluctance will not be represented by the point Y, but by Y', the intersection of OY with the minor hysteresis loop which originates on the demagnetization curve at the point X. If the reluctance be again increased to that represented by the line OX, the induction and magnetizing force will return to the values at X along the upper branch of the minor hysteresis loop which lies slightly above the lower branch and is concave downward as shown. Figure 4 shows a series of minor hysteresis loops in a major hysteresis loop for a sample of Alnico. Obviously if a magnet is expected to operate with a reluctance as represented by OY, figure 3, but must at some part of the operating cycle or preliminary to beginning of operation, be subjected to the reluctance represented by OX, it is a mistake to base the magnet design on the co-ordinates of Y. The co-ordinates of Y' must be used.

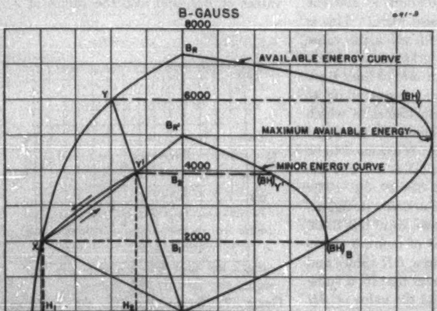
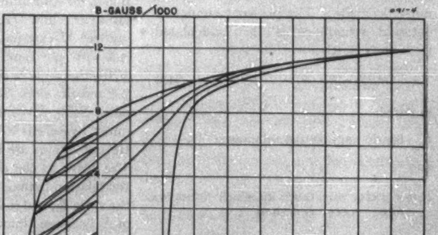


Figure 3. Demagnetization curve and available energy curve for a sample of Alnico showing one minor hysteresis loop and the associated minor energy curve

Figure 4. Major and minor hysteresis loops for a sample of Alnico



The best design of a magnet which must be subjected to changes in external reluctance as illustrated in figure 3 will give the magnet dimensions which make the product of B and H at the point of Y', the intersection of the air-gap line with the appropriate minor loop, a maximum. The significance of the BH product for points on the demagnetization curve of the major hysteresis loop has been discussed by Evershed, Watson, Sanford, and others, but little mention has been made of this quantity as associated with minor hysteresis loops. The outer curve to the right in figure 3 represents values of the BH product for points on the major hysteresis loop, and the curve inside this is a similar curve derived from the lower branch of the minor hysteresis loop.

Magnets operating with constant external reluctance and no applied demagnetizing force, as previously described, operate along the major hysteresis loop, and the values of B and H which give the largest BH product can easily be determined and thus the best point for operation. Magnets operating with varying external reluctance, however, may be designed to operate on any one of an infinite number of minor hysteresis loops, and the particular one giving the best economy of material is not readily apparent. It will be shown, however, that for any specified cycle of varying external reluctance there is a minor hysteresis loop which gives the best economy of magnet material.

The limits of the cycle of varying external reluctance must be determined from the dimensions of the external magnetic circuit. These limits are the maximum external reluctance and the normal working reluctance, including both the working air gap and the leakage paths, and they may be approximated by using the methods described by Evershed.[1] It will probably be found in most cases that they are somewhat dependent on the shape and size of the magnet, and for the first approximation a preliminary assumption of the general shape and size must be made. The per cent change in external reluctance is then calculated from the equation

$$\Delta R = 100(R_1 - R_2)/R_2 \qquad (13)$$

in which

R_1 = maximum external reluctance

and

R_2 = working reluctance at which optimum minor energy is desired

Having determined approximately the external reluctances, the best proportions of the magnet are determined in terms of the slopes of the air-gap lines. They are related as follows (see equation 3):

$$S_1 = L_m/A_mR_1 \qquad (14)$$

$$S_2 = L_m/A_mR_2 \qquad (15)$$

$$S_2 = S_1R_1/R_2 = S_1(\Delta R + 100)/100 \qquad (16)$$

in which

S_1 = slope of air-gap line corresponding to the external reluctance R_1

and

S_2 = slope of air-gap line corresponding to the external reluctance R_2

Referring again to figure 3, the line OX has a slope S_1 corresponding to external reluctance R_1 and the line OY has a slope S_2, corresponding to external reluctance R_2. The point Y' represents the point of operation with the working reluctance R_2, and the product $(BH)_{Y'}$ the energy available for the external circuit. The points X and Y can be raised or lowered along the demagnetization curve by varying the proportions of the magnet, while still maintaining the same values of R_1 and R_2 and ΔR. The point Y' and the value of $(BH)_{Y'}$ will also vary. This is illustrated in figure 5 in which the value of $(BH)_{Y'}$ has been graphically determined for four different sets of lines similar to OX and OY, all corresponding to one set of external reluctances in which $\Delta R = 100$ per cent. Thus, for the point B_1H_1, the slope S_1 of OX is assumed to be 2.5 and the slope S_2 of OY is 5. For the point B_2H_2, $S_1 = 5$ and $S_2 = 10$, etc. The minor hysteresis loops and the demagnetization curves are drawn from test data. The curve at the right of figure 5 shows the relation between these BH values and the slope S_1, and indicates that for a value of S_1 between 10 and 15 the value of BH reaches a maximum. The slope, S, is here expressed as (gausses)/(oersteds).

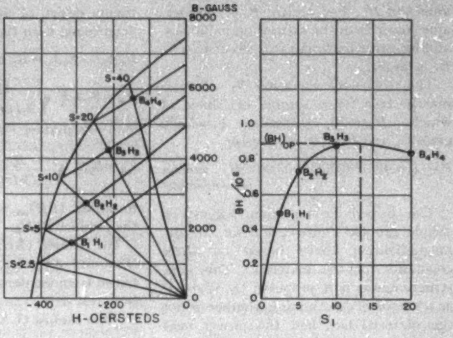


Figure 5. Curves showing values of BH for a sample of Alnico which can be obtained with a single set of external reluctance values by varying the magnet dimensions

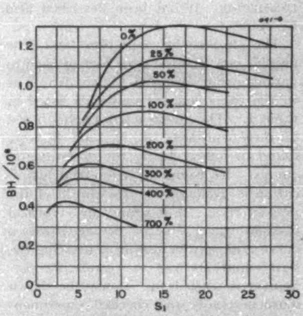


Figure 6. BH curves for a number of values of ΔR plotted as functions of S_1

In figure 6 similar curves are shown for a wide range of ΔR values, ranging from the constant reluctance conditions in which $\Delta R = 0$ to $\Delta R = 700$ per cent. Each of these curves is concave downward having a maximum value within the range plotted. In figure 7 the maximum values of BH, and also the values of S_1

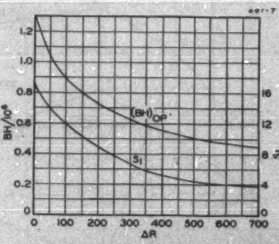


Figure 7. Curves showing the optimum value of S_1 and the optimum value of BH as functions of ΔR for a sample of Alnico

at which they occur, are plotted as functions of ΔR.

The data given by the curves of figure 7 may be used as a basis of design in which a magnet is to be fitted to an external magnetic circuit. The maximum and working reluctances of the external circuit are calculated, or otherwise determined, and ΔR calculated. The value of S_1 corresponding to ΔR in figure 7 will then determine the proportions of length and area of the magnet, and the value of BH will determine its volume.

Application of Minor-Loop Data to Design

For approximate calculation it may be assumed that the minor loop is a straight line in the demagnetization quadrant of the major hysteresis loop. On the basis of this assumption the induction in the permanent magnet may be calculated as follows:

Referring to figure 8 which is similar to figure 3 except for this approximation, the minor hysteresis loop is represented by the line $B_R'Y'X$. This line projected to the left intersects the H axis at some value, H_0, and has a slope $B_R'H_0$ which may be designated as μ_r.

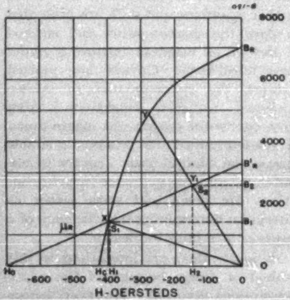


Figure 8. Demagnetization curve for a sample of Alnico showing a minor hysteresis loop replaced by a straight line

Assume that it is desired to determine the output of an Alnico permanent magnet which is to be subjected to open circuit before being assembled with the normal working gap. In figure 8 the slope of the open-circuit air-gap line, S_1, is a function of the permeance of the open-circuit leakage-flux paths, and the slope of the final or working air-gap line S_2, is a function of the permeance of the working gap in addition to the leakage paths. The permeance of the leakage paths may be determined by flux plotting of the stray field or by leakage equations of either an empirical or an analytical basis. Methods for calculating the leakage are given in Evershed's paper.[1] Another method of determining S_1 for certain magnets is given in appendix C.

The equation of the working air-gap line is

$$B_2 = S_2 H_2$$

And for the assumed straight-line minor loop is

$$B_2 = \mu_r(H_0 - H_2)$$

The solution of these two equations yields

$$B_2 = \frac{H_0 \mu_r S_2}{\mu_r + S_2} \tag{18}$$

The physical significance of equation 18 becomes apparent if it is put in slightly different form

$$B_2 = \frac{H_0}{\dfrac{1}{\mu_r} + \dfrac{1}{S_2}} \tag{19}$$

H_0 is the internal magnetomotive force per unit length, $1/\mu_r$ the unit internal reluctance, and $1/S_2$ the unit external reluctance. Hence the equation is simply the expression of Ohm's law for the permanent-magnet circuit and is expressed in a per-unit form.

In equations 18 and 19

B_2 = induction in the magnet during normal operating conditions

H_0 = fictitious "internal magnetomotive force" per unit length

μ_r = slope of assumed straight line "internal magnetization curve" or minor loop

S_2 = slope of working air-gap line

The value H_0 depends upon the location of the point X, figure 8, and hence upon the slope of the open-circuit air-gap line S_1. μ_r also varies somewhat for different values of S_1. These two quantities are plotted as functions of S_1 in figure 9 from test data obtained with a sample of Alnico.

The quantities S_1 and S_2 are calculated for the particular design, and μ_r and H_0 are read from the characteristic curves for the material (figure 9). Then by substitution in equation 18 the induction in the magnet is readily calculated. The density in the air gap is

$$B_g = B_2 \frac{A_m}{A_g K_1} \tag{20}$$

The magnetomotive force per unit length of Alnico for the normal operating air gap is

$$H_2 = \frac{\mu_r H_0}{\mu_r + S_2} \tag{21}$$

Then the available energy is given by

$$B_2 H_2 = \frac{\mu_r^2 H_0^2 S_2}{(\mu_r + S_2)^2} \tag{22}$$

The value of the available BH may be compared with the maximum available given by curve figure 7. The ratio of the available to the maximum available energy may be termed the "utilization" of the material, or symbolically

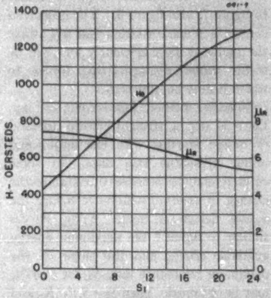


Figure 9. Curves showing values of H_0 and μ_r for assumed straight-line minor hysteresis loops

$$U = \frac{B_2 H_2}{(BH)_{op}} \tag{23}$$

This term is simply the ratio of the total available energy of the actual design to the maximum available energy obtainable for a given variation in external reluctance. It is a measure of how efficiently the material is being utilized.

The over-all efficiency of design is given by

$$E = \frac{B_2 H_2}{(BH)_{op} K_1} \tag{24}$$

This ratio shows the relation of the useful available energy to the maximum available energy obtainable. One hundred per cent efficiency is never obtainable but can only be approached, while 100 per cent utility is possible by correct design. The curve in figure 7 shows the maximum energy available, $(BH)_{op}$, and the optimum slope of the maximum demagnetization as functions of the change in external reluctance from minimum to maximum so that

$$\Delta R = \frac{100(R_1 - R_2)}{R_2} = \frac{100(P_2 - P_1)}{P_1}$$

in which

R_2 = minimum reluctance
R_1 = maximum reluctance
P_1 = minimum permeance
P_2 = maximum permeance

The converse of the problem of determining how efficiently a given magnet design will utilize the material is the problem of calculating a design which will utilize the material to the best advantage. The method is essentially one of cut and try and involves the use of curves similar to figure 7, also figure 9. The object, of course, is to make a design that will utilize the material most efficiently and will be stable under demagnetizing forces to which it may be subjected.

Since the method is one of cut and try, there is not a straightforward method of design starting with the major and minor hysteresis loop data. In some cases it may be possible to estimate the change in external reluctance before the actual design is made. From curves similar to figure 7 the maximum energy available $(BH)_{op}$ and the slope of the maximum reluctance line, S_1, for $(BH)_{op}$ may be obtained. Then $S_2 = S_1R_1/R_2$, and by the use of equations 18 and 20 and curves similar to figure 9, the size of the magnet may be calculated. From the dimensions of the first design the leakage factors may be obtained by calculation or from empirical data, and the actual change in external reluctance determined. The actual output of the magnet may then be calculated from the characteristic curves and compared with the desired output. This method should require but a few tries depending on how complicated the magnetic circuit is and the experience of the designer.

It might be observed that under certain conditions when the leakage is quite appreciable a design giving 100 per cent utility would not yield maximum efficiency, but that maximum efficiency would be obtained at a lower utility. However, the best procedure would probably be to design for maximum utility and then to make check calculations to determine if greater efficiency could be obtained for a design of slightly different dimensions.

Magnet Subjected to Varying Applied Magnetomotive Forces

In some cases the permanent magnet may be subjected to a variable applied magnetomotive force as well as a variable external reluctance. In a generator or motor excited by a permanent magnet such forces would result from armature reaction. Figure 10 illustrates the operation of a permanent magnet subjected to a variation in demagnetizing force from H_{D1} to H_{D2} and a variation in external reluctance ΔR defined by $100(S_2-S_1)/S_1$. Under such conditions the internal magnetomotive force of the magnet is reduced and the equations for magnet output under normal conditions are

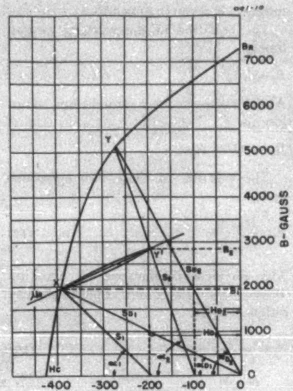


Figure 10. Demagnetization curve and minor hysteresis loop for a condition of combined variation in external reluctance and external demagnetizing force

$$B_2=\frac{\mu_r(H_0-H_{D2})S_2}{S_2+\mu_r} \tag{25}$$

$$H_2=\frac{\mu_r(H_0-H_{D2})}{S_2+\mu_r} \tag{26}$$

and the available energy is

$$(B_2H_2)=S_2\frac{\mu_r^2(H_0-H_{D2})^2}{(S_2+\mu_r)^2} \tag{27}$$

Under such conditions the slope of the maximum demagnetization line will not be S_1 but will be given by

$$S_{D1}=\frac{[\mu_r(H_0-H_{D1})-H_{D1}(\mu_r+S_1)]S_1}{\mu_r(H_0-H_{D1})} \tag{28}$$

Also, the slope of the normal demagnetization line will be given by

$$S_{D2}=\frac{[\mu_r(H_0-H_{D2})-H_{D2}(\mu_r+S_2)]S_2}{\mu_r(H_0-H_{D2})} \tag{29}$$

The ordinate of the curves in figure 7 must be interpreted in terms of the variation of an apparent reluctance which depends on the applied demagnetizing forces as well as the air-gap reluctance.

$$\Delta R'=\frac{100(S_{D2}-S_{D1})}{S_{D1}} \tag{30}$$

Similarly H_0 will be a function of S_{D1} rather than S_1, and the abscissa of figure 9 must be interpreted in terms of this quantity. S_{D1} and S_{D2} are best determined graphicallyas indicated in figure 10.

Summary and Conclusions

This paper presents the elements of permanent-magnet design based on the major and minor loop data and magnetic-circuit theory. For the general case of variable reluctance or applied magnetomotive force it has been shown that the permanent magnet can be treated as a path of almost constant permeability, μ_r, and a variable internal magnetizing force which is a function of maximum external reluctance or demagnetizing force.

Equations and methods for the design of permanent magnets having constant external reluctance or applied magnetomotive force are given based on certain assumptions regarding the shape of the demagnetization curve. Empirical relations of this type are given for a sample of average Alnico.

Since many permanent-magnet problems involve conditions of variable external reluctance or magnetomotive force, further equations and data are presented to show the characteristics and method of design of magnets operating under these conditions. Curves are plotted showing the characteristics of Alnico subjected to such conditions. From these curves the output of a magnet may be predetermined. Maximum available energy and "initial state" curves to obtain maximum energy under varying conditions are given and serve either as criteria for determining the efficiency of a design or as a basis for design.

Obviously there are limitations to this method of design because of the many assumptions involved. However, before attempting too great a refinement in permanent-magnet calculations it must be realized that exact field theory will, in the simplest cases, be too cumbersome to apply, and in general the complexity of the boundary conditions and the multiple-functioned relation of B and H render a mathematical solution hopelessly complicated. Furthermore, variations in magnetic quality of material among magnets and in the same magnet make exact calculations futile from a practical standpoint. The method of permanent-magnet design which has been outlined in this paper has been found sufficiently accurate for a wide variety of problems.

Appendix A

Analytic Determination of Permanent-Magnet Output

In terms of the slope of the air-gap line the equation of the demagnetization curve may be written as follows:

$$B=\frac{-\frac{B}{S}+H_c}{\frac{H_c}{B_r}-\frac{B}{B_g S}} \tag{31}$$

The solution of the above equation for B yields

$$B=\frac{B_g S}{2}\left\{\left(\frac{H_c}{B_r}+\frac{1}{S}\right)-\sqrt{\left(\frac{H_c}{B_r}+\frac{1}{S}\right)^2-\frac{4H_c}{B_g S}}\right\} \tag{32}$$

Since the available energy is defined as BH and since $H=B/S$, the following equation for available energy may be written:

$$BH=\frac{B_g^2 S}{4}\left\{\left(\frac{H_c}{B_r}+\frac{1}{S}\right)-\sqrt{\left(\frac{H_c}{B_r}+\frac{1}{S}\right)^2-\frac{4H_c}{B_g S}}\right\}^2 \tag{33}$$

Equations 32 and 33 are valid only for nonvarying conditions of external reluctance or applied magnetomotive force.

If the permanent magnet is subjected to variable conditions of external reluctance or applied magnetomotive forces, these relations no longer hold. Under such conditions the magnet acts as a path of almost constant permeability and as a source of variable magnetomotive force, both of which are functions of the maximum external reluctance to which the magnet is subjected. Then by equation 32 and the relation $H=B/S$ the equation for the maximum magnetizing force applied to the air gap may be expressed in terms of S_1.

$$H_1=\frac{B_g}{2}\left\{\left(\frac{H_c}{B_r}+\frac{1}{S_1}\right)-\sqrt{\left(\frac{H_c}{B_r}+\frac{1}{S_1}\right)^2-\frac{4H_c}{B_g S_1}}\right\} \tag{34}$$

It is easily shown by means of figure 8 that the internal magnetizing force is

$$H_0=\frac{H_1(S_1+\mu_r)}{\mu_r}$$

so that

$$H_0=\frac{B_g(S_1+\mu_r)}{2\mu_r}\left\{\left(\frac{H_c}{B_r}+\frac{1}{S_1}\right)-\sqrt{\left(\frac{H_c}{B_r}-\frac{1}{S_1}\right)^2-\frac{4H_c}{B_g S_1}}\right\} \tag{35}$$

Then by combining equations 35 and 18

$$B_2=\frac{B_g S_2}{2}\frac{(S_1+\mu_r)}{(S_2+\mu_r)}\left\{\left(\frac{H_c}{B_r}+\frac{1}{S_1}\right)-\sqrt{\left(\frac{Hc}{B_r}+\frac{1}{S_1}\right)^2-\frac{4H_c}{B_g S_1}}\right\} \tag{36}$$

Since $H_2=B_2/S_2$ the available energy under such conditions is given by:

$$B_2H_2=\frac{B_g^2 S_2}{4}\frac{(S_1+\mu_r)^2}{(S_2+\mu_r)^2}\left\{\left(\frac{H_c}{B_r}+\frac{1}{S_1}\right)-\sqrt{\left(\frac{H_c}{B_r}+\frac{1}{S_1}\right)^2-\frac{4H_c}{B_g S_1}}\right\}^2 \tag{37}$$

It will be seen by inspection of (36) and (37) that they reduce to equations 32 and 33 if $S_1=S_2$, which is the condition for a constant air gap.

If the magnet is subjected to a variable applied magnetomotive force as well as a variable air gap, S_1 and S_2 in the preceding equations must be replaced by S_{D1} and S_{D2} which depend both on the external reluctances and the magnitudes of the demagnetizing forces. These quantities are defined by figure 10 or by the equations 28 and 29.

Appendix B

Sample Calculation

The permanent-magnet design shown in figure 11 is used to illustrate the method of calculating the output of a permanent magnet subjected to variable conditions of external reluctance.

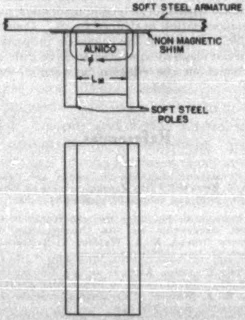


Figure 11. Sample Alnico magnet design. Magnet subjected to maximum reluctance when armature keeper is removed

The use of soft-iron pole pieces is a recommended type of permanent-magnet design for applications where the flux is to be concentrated in an air gap without affecting the flux density in the Alnico. If the pole pieces were of Alnico, the concentration of flux would result in a loss of magnetomotive force in the magnet poles.

The Alnico magnet shown in figure 11 is to be stabilized by being subjected to open circuit before being assembled, with a working air gap of 0.0105 inch. It is desired to find the air-gap flux density and efficiency of design with the normal air gap inserted in the magnetic circuit. The dimensions of the magnet are

$L_M=0.47$ inch $=1.19$ centimeters

$A_M=0.5\times1.75=0.875$ square inch $=5.64$ square centimeters

The dimensions of the working air gap are

$L_g=0.0105\times2=0.021$ inch $=0.0533$ centimeter

$A_g={}^1/_8\times1.75=0.219$ square inch $=1.41$ square centimeters

There is an additional air gap in the magnetic circuit equivalent to approximately 0.004 inch, due to the reluctance of the joints and keeper. This must be added to the working air gap.

Then

$L_g'=0.021+0.004=0.025$ inch $=0.0635$ centimeter

The equivalent working air-gap permeance is

$$P_g=\frac{A_g}{L_g'}=\frac{1.41}{0.0635}=22.2 \text{ centimeters}$$

and the dimensions ratio of the magnet is

$$\frac{L_M}{A_M}=\frac{1.19}{5.64}=0.211$$

By using the conductance formulas given in Evershed's paper[1] the approximate open-circuit and normal operating conductances were calculated:

$P_1=11$ centimeters

$P_2=48$ centimeters

The per cent change in reluctance from minimum to maximum, which is the same as the per cent change in permeance from minimum to maximum, is

$$\text{Per cent } \Delta R=100\frac{(P_2-P_1)}{P_1}=\frac{48-11}{11}=336 \text{ per cent}$$

The slope of the open-circuit line is

$$S_1=\frac{L_M}{A_M}P_1=0.211\times11=2.32$$

and the slope of the normal operating line is

$$S_2=\frac{L_M}{A_M}P_2=0.211\times48=10.13$$

From figure 9, with $S_1=2.32$

and

$\Delta_r=7.3$

$H_0=530$

Then by equations 18 and 21

$$B_2=\frac{7.3\times530\times10.13}{7.3+10.13}=2{,}250 \text{ gausses}$$

and

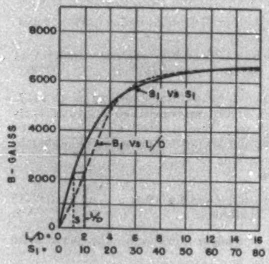
$$H_2=\frac{7.3\times530}{7.3+10.13}=222 \text{ oersteds}$$

Then by equation 20

$$B_g=2{,}250\times\frac{5.64}{1.41}\times\frac{22.2}{48}=4{,}160 \text{ gausses}$$

The available energy (stored in the air gap and leakage fields) per cubic centimeter of Alnico is

$$B_2H_2=2{,}250\times222=5\times10^5 \text{ ergs}\times8\pi$$



which may also be obtained directly from figure 6.

From the curve in figure 7 the maximum possible under the given condition is 5.9×10^6.

Hence by equation 23

$$U=\frac{5}{5.9}=0.847$$

and by equation 24

$$E=0.847\times\frac{22.2}{48}=0.392$$

The significance of U and E are explained in the main body of the paper.

Appendix C

Open-Circuit Demagnetization

If the magnet is of constant cross section and straight axis and has no soft-iron pole pieces when open-circuited, S_1 can be determined by a direct method without resorting to leakage calculations. It has been shown by Scott and others that the remanence or open-circuit residual, B_1, is a function of the dimension ratio, L/D (length/diameter) of the magnet and is very nearly independent of the actual size and shape provided a constant L/D is maintained.

A curve of S_1 as a function of L/D can be plotted from the relation between B_1 and L/D as determined by test, and from the relation between B_1 and S_1 as determined from the demagnetization curve of the material. Figure 12 illustrates this method as applied to a sample of Alnico.

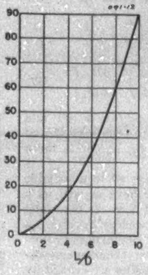


Figure 12. Curve relating slope of open-circuit demagnetization line to dimension ratio, L/D

Since $S_1=\frac{L_M}{A_M}P_1$, it should depend only on the geometry of the magnet and should be independent of the properties of the material. However, further study is necessary to determine if the relation between S_1 and L/D as found by tests on magnets of a certain material can be applied to magnets of other materials. The method has been applied to Alnico magnets of circular and rectangular cross section as a quick and fairly accurate means for determining open-circuit demagnetization as defined by S_1. Considerable variation in the magnetic properties between magnets appeared to have little or no effect on the relation between S_1 and L/D.

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Discussion

For discussion, see page 1256.

An Extension of the Method of Symmetrical Components Using Ladder Networks

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Synopsis: The pictorial quality of equivalent circuits is often of considerable assistance in computing the performance of transformers and that of synchronous and induction machines. The principal object of this paper is to show how equivalent circuits are useful in extending Fortescue's method of symmetrical components to cases where the circuits on both sides of the air gap of either synchronous or induction machines are unsymmetrical. The analysis is developed for steady-state conditions but the method also may be useful in the analysis of transient conditions.

IN his now classical paper on symmetrical components Fortescue analyzed the behavior of synchronous and induction machinesoperating under unbalanced conditions. In this analysis Fortescue assumed that the stator and rotor windings themselves were symmetrical, that the rotor windings were either short-circuited or connected to balanced static circuits, and that the stator windings were connected to unbalanced circuits. Under these conditions there are unbalanced currents of a single frequency in the stator, and balanced currents of two different frequencies in the rotor. It has since been shown how the method of symmetrical components is applied when the rotor windings are connected to unbalanced circuits and the stator windings are connected to balanced circuits. Under these conditions there are unbalanced currents of a single frequency in the rotor, and balanced currents of two different frequencies in the stator.

The object of this paper is to show how the method of symmetrical components can be extended and applied when both the stator and rotor windings are connected to unbalanced circuits. Under these conditions there are unbalanced currents in both stator and rotor, consisting of infinite series of harmonic components. These harmonic components differ in frequency by a multiple of the frequency corresponding to the speed of the rotor. For example, if a synchronous generator is connected to an unbalanced static circuit, the unbalanced stator currents consist of fundamental, third, fifth, and all other odd-harmonic components, and the rotor currents consist of all the even harmonic components. If an induction motor has unbalanced static circuits connected to both stator and rotor windings, the angular velocities of the currents are $\omega \pm kn$; where ω is the angular velocity of the applied potential, n is the angular velocity of the rotor, and k is any even integer for the stator and any odd integer for the rotor. These conditions are well known, and special cases have been analyzed. Doherty and Nickle determined the currents during a single-phase short circuit on a synchronous generator. Wagner has recently determined the conditions existing when a synchronous generator is connected to an unsymmetrical capacitive load.

The proposed method of analysis can be applied only when the machine and its connected load can be represented by an equivalent circuit. This restriction, however, is not at all serious, since an equivalent circuit can always be found for a three-phase machine if the unbalanced static circuits connected to it are symmetrical about *one* phase. Most of the common unsymmetrical conditions are of this type. While it is true that any problem which can be solved with the aid of equivalent circuits can also be solved without using equivalent circuits, nevertheless the pictorial quality possessed by equivalent circuits has proved of considerable assistance in computing the performance of transformers and that of synchronous and induction machines.

The complete solution consists of two parts:

I. First there are the relations between the positive- and negative-sequence currents and terminal potentials of the *same* frequency. These relations depend upon the impedances of the unbalanced circuits connected to the terminals of the stator or rotor windings.

II. Secondly there are the relations between the currents and potentials of *different* frequencies. These relations depend upon the constants of the machine itself and upon its speed.

I. If, for example, Z_0, Z_1, Z_2 are the symmetrical components of three unbalanced impedances connected in Y to the terminals of a synchronous generator, the positive- and negative-sequence components of the potentials at the terminals of the Y are

$$V_1 = I_1 Z_0 + I_2 Z_2$$

$$V_2 = I_2 Z_0 + I_1 Z_1$$

where I_1 and I_2 are the positive- and negative-sequence components of the currents. If Z_1 equals Z_2 these equations are the equations of a coupled circuit which can be represented by figure 1. The positive- and negative-sequence components, Z_1 and Z_2, of the Y impedances are equal when two of the Y impedances are equal; that is, when the Y impedances are symmetrical about one phase. If the Y impedances are Z_a, Z, Z, the symmetrical components of Z_a are

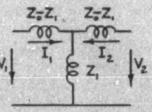

Figure 1

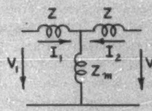

Figure 2

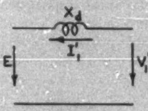

Figure 3

$$Z_0 = \frac{Z_a + 2Z}{3} \qquad Z_1 = Z_2 = \frac{Z_a - Z}{3}$$

Since $Z_0 - Z_1 = Z$, figure 1 can be labeled as in figure 2, in which

$$Z_m = Z_1 = \frac{Z_a - Z}{3}$$

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1. For all numbered references, see list at end of paper.

In each of the following illustrations we shall assume that two of the three impedances connected to the stator or rotor windings are equal, since it is only then that the equivalent circuit of figure 2 can be used. Equivalent circuits like figure 2 show the relations among the positive- and negative-sequence potentials and currents of *each harmonic frequency*. The values of the impedances in each of these equivalent circuits are adjusted for the particular harmonic frequency considered.

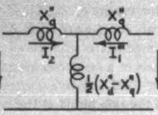

Figure 4

II. In setting down the relations between the currents and potentials of different frequencies, it is best to use a somewhat different procedure for the synchronous machine than for the induction machine. Consider first the synchronous machine. The analysis is somewhat simpler if the resistances of the stator and rotor are neglected, but the resistances can be taken into account if desired.

If the resistances in the machine and in the circuits connected to the machine are neglected, the relation between the excitation electromotive force, E, which is assumed to be sinusoidal, and the positive-sequence component, V_1', of the fundamental terminal potential is determined by the equivalent circuit of figure 3, in which X_d is the synchronous reactance in the direct axis. In all the following equivalent circuits the primes ('), triple primes ('''), and quintuple primes (v) attached to the potentials (V) and the currents (I) indicate the harmonic order of the potential and current. The arrows indicate the assumed positive direction of the current (I) and the fall in potential (V). The relation between the negative-sequence component, V_2', of the fundamental terminal potential of the armature and the reflected positive-sequence triple-frequency component, V_1''', of the terminal potential is determined by the equivalent circuit of figure 4,[1] in which X_q'' is the subtransient reactance in the quadrature axis, and X_d'' is the subtransient reactance in the direct axis. Likewise the relation between the negative-sequence triple-frequency component, V_2''', of the terminal potential and the positive-sequence fifth-harmonic component, V_1^{v}, of the terminal potential is determined by the same equivalent circuit (see figure 5). The equivalent circuits of figure 4 and figure 5 would not be exactly alike if the resistances of the armature and field circuits were taken into account. Note that each harmonic component of terminal potential is divided by the harmonic order.

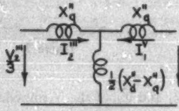

Figure 5

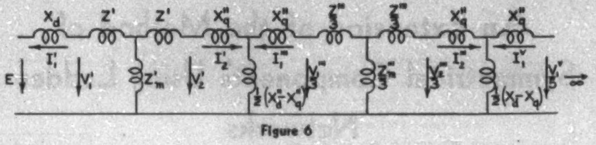

Figure 6

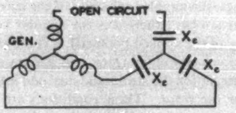

Figure 7a

It is readily seen the alternator and the unbalanced external circuits connected to its armature terminals can be represented by an equivalent circuit composed of units like figure 3, figure 4, figure 5, etc., connected by units like figure 2, as indicated in figure 6. The impedances in the successive units like figure 2 are for fundamental frequency, for third-harmonic frequency, for fifth-harmonic frequency, etc., and in each case the impedance is divided by the harmonic order of the frequency for which it is computed. Since the impedances and the terminal potentials both are divided by the harmonic order, the currents have their correct values. If the resistances, inductances, and capacitances in the external circuit are independent of frequency and are connected in series, dividing the impedances of the external circuit by the harmonic order divides the resistances by the harmonic order, does not alter the inductive reactances, and divides the capacitive reactances by the square of the harmonic order. This interconnection (figure 6) of the component equivalent circuits satisfies the requirement that for each frequency the positive- and negative-sequence potentials and currents at the terminals of the generator equal the corresponding potentials and currents in the external circuit. The network of figure 6 actually extends to infinity but it is usually necessary to include only a few units to attain the desired accuracy in the computation. Not only can the magnitude of the successive harmonic components of the current be determined, but their phase relations also can be computed and hence the wave form of the current. The phase relations of the fundamental and different harmonic components of the current determined from the equivalent circuit (figure 6) equal the phase relations of the rotating vectors representing these currents at the moment the phase of the armature winding for which the analysis is made is directly opposite a field pole; that is, at the moment the excitation electromotive force in that phase is passing through its zero value. At other moments in the cycle the third- and fifth-harmonic vectors have different phase relations with respect to the fundamental vectors, since they spin at different speeds.[1]

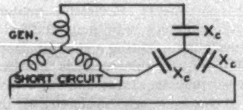

Figure 7b

We shall consider two special cases in which an alternator is delivering current to an unbalanced condenser load, representing a cable or long transmission line whose resistance is neglected. In figure *7a* one circuit breaker opens and in figure *7b* there is a line-to-line single-phase short circuit at the terminals of the alternator. The equivalent circuits are given in figure 8 and figure 9. First consider the conditions of figure *7a*. The impedances connected to the alternator terminals are, at fundamental frequency, $Z_a' = \infty$ and $Z' = -jX_c$. Consequently Z_m (figure 2) is infinitely great. In figure 8 note the capacitor reactances are divided by the *square* of the harmonic order. This is in accord with figure 6, since the actual capacitor reactances vary inversely as the harmonic order. For the conditions of figure *7b* the impedances connected to the

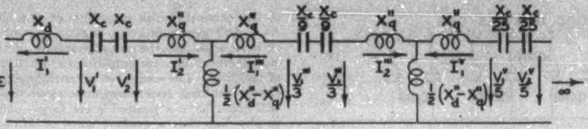

Figure 8

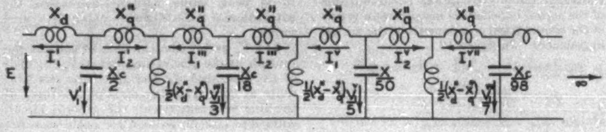

Figure 9

alternator terminals are, at fundamental frequency,

$Z_a'=-j3X_c/2$

and

$Z'=0$

Consequently

$Z_m=-jX_c/2$

In order to determine the currents in these networks (figure 8 and figure 9) the following procedure can be used. Since the capacitive reactances in figure 8 are divided by the squares of the harmonic orders, there will be a point beyond which these reactances can be neglected without introducing much error. If the capacitive reactances are neglected beyond a given point, the reactance of the entire network beyond this point is $\sqrt{X_d''X_q''}$.[1] In figure 9 it is evident there is a point beyond which the capacitive reactances substantially shortcircuit the network. Therefore there is a point in either network where the network may be assumed to terminate if there is connected to the network at this point either an impedance equal to $\sqrt{X_d''X_q''}$ or one equal to zero. The choice of this point depends upon the accuracy desired and the experience of the worker. The following numerical illustration is given of the single-phase short circuit of a 30,000-kva alternator without dampers supplying a capacitive load.

$X_d=1.15\quad X_q''=X_q=0.83\quad X_d''=X_d'=0.33$

from which

$X_2=\sqrt{X_d'X_q}=0.52$

$1/2(X_d''-X_q'')=-0.25$

Reactance of load $X_c=9\times0.52=4.7$ at fundamental frequency. Substituting these values of reactance in the ladder network of figure 9 gives figure 10. All resistances are neglected and the machine reactances are assumed independent of saturation. Applied electromotive force $E=1.0$.

Neglect the part of the ladder to the right of *k*. Apply the rules for computing the impedances and currents in series-parallel circuits. In table I, column 1 gives the reactances at the right and left of the points *j*, *i*, *h*, etc. Column 2 gives the currents in the series branches *ab*, *bc*, *cd*, etc. Column 3 gives the potentials at *a*, *c*, *e*, etc.

From figure 10 and table I, column 3, note that $V_1'=V_a$, $V_1'''/3=V_c$, $V_1^{v}/5=V_e$, $V^{vii}/7=V_g$. Consequently the positive-sequence-harmonic components of the terminal potential are

$V_1'=0.87$

$V_1'''=3\times(-1.74)=-5.22$

$V_1^{v}=5\times(-0.21)=-1.05$

$V_1^{vii}=7\times(-0.011)=-0.077$

The vector diagram of figure 11 can be drawn for the moment the excitation electromotive force, *E*, is passing through zero. The harmonic components are in opposition to the fundamental, and consequently the peak value of the positive-sequence electromotive force at the moment the fundamental is maximum is $0.87+5.22-1.05+0.077=5.12$ times peak value of *E*. With a line-to-line short circuit the peak value of the remaining line potential is three times the peak value of the positive-sequence line-to-neutral potential of the unshort-circuited phase or $3\times5.12=15.4$. This is $15.4/\sqrt{3}$ or 8.9 times normal maximum line potential. The condition chosen is so near resonance for the third harmonic that the resistances should not be neglected, particularly near the point *b* in figure 10, if more accurate results are required.

Table I

	Column 1	Column 2	Column 3
Right of *j*	$0.83-0.019=0.81$	$I=\frac{1}{1.15+7.5}=0.12$	
Left of *j*	$\frac{(0.81)(-0.25)}{0.56}=-0.36$		
Right of *i*	$0.83-0.36=0.47$	$I_{ab}=\frac{0.12(-2.35)}{-0.56}=0.49$	
Left of *i*	$\frac{(0.47)(-0.029)}{0.44}=-0.031$	$I_{bc}=\frac{0.49(-0.25)}{-0.05}=2.38$	
Right of *h*	$0.83-0.031=0.80$		
Left of *h*	$\frac{(0.80)(-0.25)}{0.55}=-0.36$	$I_{cd}=\frac{2.38(-0.261)}{0.19}=-3.27$	
Right of *g*	$0.83-0.36=0.47$	$I_{de}=\frac{-3.27(-0.25)}{0.46}=1.78$	
Left of *g*	$\frac{(0.47)(-0.048)}{0.42}=-0.054$		
Right of *f*	$0.83-0.054=0.78$	$I_{ef}=\frac{1.78(-0.094)}{0.37}=-0.45$	
Left of *f*	$\frac{(0.78)(-0.25)}{0.53}=-0.37$	$I_{fg}=\frac{-0.45(-0.25)}{0.53}=0.21$	
Right of *e*	$0.83-0.37=0.46$		
Left of *e*	$\frac{(0.46)(-0.094)}{0.37}=-0.117$	$I_{gh}=\frac{0.21(-0.048)}{0.42}=-0.024$	
Right of *d*	$0.83-0.117=0.71$	$I_{hi}=\frac{-0.024(-0.25)}{0.55}=0.011$	
Left of *d*	$\frac{(0.71)(-0.25)}{0.46}=-0.38$		
Right of *c*	$0.83-0.38=0.45$	$I_{ij}=\frac{0.011(-0.029)}{0.44}=0.0007$	
Left of *c*	$\frac{(0.45)(-0.261)}{0.19}=-0.63$		
Right of *b*	$0.83-0.63=0.20$		$V_a=(0.12-0.49)(-2.35)=0.87$
Left of *b*	$\frac{(0.20)(-0.25)}{-0.05^*}=0.96$		$V_c=(2.38+3.27)(-0.261)=-1.74$
Right of *a*	$0.83+0.96=1.79$		$V_e=(1.78+0.45)(-0.094)=-0.21$
Left of *a*	$\frac{(1.79)(-2.35)}{-0.56}=7.5$		$V_g=(0.21+0.024)(-0.048)=-0.011$

* This 0.05 is so small that considerable error is made by neglecting resistance.

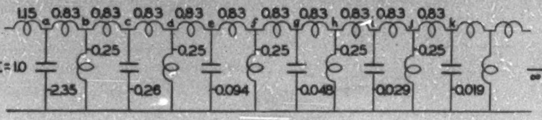

Figure 10

Appendix I

Resistance can be taken into account in the positive-sequence equivalent circuit of the generator, figure 3, by replacing X_d by R_a+jX_q and at the same time multiplying the excitation electromotive force E by

$$\frac{(X+X_q)^2+(R_a+R)^2}{X^2+X(X_d+X_q)+X_dX_q+(R_a+R)^2}$$

where R_a is the armature resistance and $R+jX$ is the complex ratio of fundamental positive-sequence potential V_1' to fundamental positive-sequence current I_1'. See figure 6.

Appendix II

When there is a neutral current so that the line currents have a zero-sequence component, a slightly different procedure is necessary. Let the three self-impedances connected between the lines and neutral be Za, Z, Z, their symmetrical components being $Z_0=(Z_a+2Z)/3$, $Z_1=(Z_a-Z)/3$, $Z_2=(Z_a-Z)/3$. The relations among the symmetrical components of potential, current, and impedance are

$$V_0=I_0Z_0+I_1Z_1+I_2Z_1 \qquad (1)[2]$$

$$V_1=I_0Z_1+I_1Z_0+I_2Z_1 \qquad (2)$$

$$V_2=I_0Z_1+I_1Z_1+I_2Z_0 \qquad (3)$$

Assume that the zero-sequence component of the terminal potential of the generator to which the impedances Z_a, Z, Z, are connected depends only upon the zero-sequence component of the current, or that the positive- and negative-sequence components of current cause no zero-sequence component of terminal potential. Also assume the zero-sequence component of current causes neither positive- nor negative-sequence components of potential. These necessary assumptions are often substantially true but may not be strictly true. Equation 1 can then be extended to include the effect of the impedance, Z_{g0}, of the generator to zero sequence

$$0=I_0(Z_0+Z_{g0})+I_1Z_1+I_2Z_1 \qquad (4)$$

Substitute the value of I_0 determined by equation 4 in equations 2 and 3.

$$V_1=I_1\left(Z_0-\frac{Z_1^2}{Z_0+Z_{g0}}\right)+I_2\left(Z_1-\frac{Z_1^2}{Z_0+Z_{g0}}\right) \qquad (5)$$

$$V_2=I_2\left(Z_0-\frac{Z_1^2}{Z_0+Z_{g0}}\right)+I_1\left(Z_1-\frac{Z_1^2}{Z_0+Z_{g0}}\right) \qquad (6)$$

These equations apply both to the equivalent circuit of figure 12 and to that of figure 13.

The impedances in the branches of these equivalent circuits can be expressed in terms of the actual impedances instead of in terms of the symmetrical components of the actual impedances. In figure 12,

$$Z_0-Z_1=Z$$

$$Z_1-\frac{Z_1^2}{Z_0+Z_{g0}}=\frac{(Z+Z_{g0})(Z_a-Z)}{Z_a+2Z+3Z_{g0}}$$

In figure 13,

$$Z_0+Z_1-\frac{2Z_1^2}{Z_0+Z_{g0}}=\frac{3Z_aZ+Z_{g0}(2Z_a+Z)}{Z_a+2Z+3Z_{g0}}$$

The equivalent circuits of figure 12 or figure 13 can be used to relate the positive- and negative-sequence components of the terminal potential of the generator for every harmonic frequency. It is necessary to know the values of Z_a, Z, and Z_{g0} for the fundamental and each odd harmonic frequency.

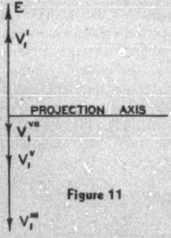

Figure 11

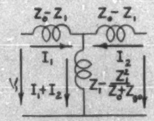

Figure 12

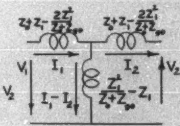
$Z_0+Z_1-\frac{2Z_1^2}{Z_0+Z_{g0}}$
$Z_0+Z_1-\frac{2Z_1^2}{Z_0+Z_{g0}}$
V_1
I_1
I_2
V_2
V_2
I_1-I_2
$\frac{Z_1^2}{Z_0+Z_{g0}}-Z_1$

Figure 13

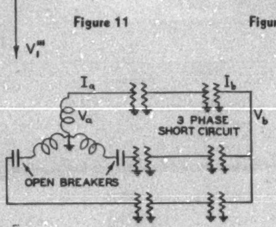

Figure 14

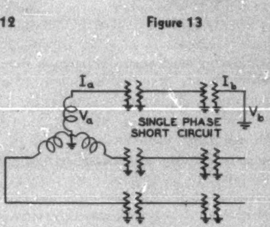

Figure 15

Having determined the positive- and negative-sequence components of current and potential of each desired frequency from the equivalent circuit, the corresponding zero-sequence component of current is determined by equation 4. The zero-sequence component of the terminal potential of the generator is

$$V_{g0}=-I_0Z_{g0} \qquad (7)$$

Appendix III

If a salient-pole generator without dampers is connected to a long transmission line, it is convenient to obtain a solution in terms of the general circuit constants of the line. As a first approximation neglect the effect of saturation on the magnetization of the transformers that are connected at each end of the line. The excessive potentials sometimes resulting from unsymmetrical faults will often have a peaked wave form. Due to the fact that a peaked wave of applied potential results in a flat wave of flux variation in the transformer, the peak value of the applied potential may well be twice the rated peak value without increasing the saturation of the transformer above its rated value.

Let V_a and V_b be the line-to-neutral potentials at the generating and receiving ends of the line, and I_a and I_b be the line currents at corresponding ends of the line. If the line is symmetrical, the general circuit equations can be written for each sequence and for each harmonic frequency independently. The equations for fundamental frequency are:

$$\text{positive sequence}\begin{cases} V_{a1}=AV_{b1}+BI_{b1} & (8)[3] \\ I_{a1}=CV_{b1}+DI_{b1} & (9) \end{cases}$$

$$\text{negative sequence}\begin{cases} V_{a2}=AV_{b2}+BI_{b2} & (10) \\ I_{a2}=CV_{b2}+DI_{b2} & (11) \end{cases}$$

$$\text{zero sequence}\begin{cases} V_{a0}=A_0V_{b0}+B_0I_{b0} & (12) \\ I_{a0}=C_0V_{b0}+D_0I_{b0} & (13) \end{cases}$$

The corresponding constants in the positive- and negative-sequence equations are equal, but are different from the constants in the zero-sequence equations, as indicated by the subscripts. There are corresponding equations for each harmonic frequency in which the general circuit constants are computed for each particular harmonic.

If the transformers at each end of the line are alike, the general circuit constants A and D are equal. Since it simplifies the expressions in the second illustration (figure 15) somewhat if $A=D$, we shall there assume that $A=D$. For simplicity assume the transformers are connected Y-Y with both neutrals grounded. The generator neutral is also grounded. In figure 14 there is a three-phase line-to-line short circuit at the

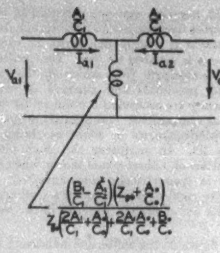
$\frac{A_1}{C_1}$
$\frac{A_1}{C_1}$
I_{a1}
I_{a2}
V_{a1}
V_{a2}
$\frac{\left(\frac{B_1}{C_1}-\frac{A_1^2}{C_1^2}\right)\left(Z_{g0}+\frac{A_0}{C_0}\right)}{Z_{g0}\left(\frac{2A_1}{C_1}+\frac{A_0}{C_0}\right)+\frac{2A_1A_0}{C_1C_0}+\frac{B_0}{C_0}}$

Figure 16

far end which causes two of the breakers at the generator to open. In figure 15 there is a line-to-ground short circuit at the far end. In figure 14,

$$\left.\begin{aligned}&I_{a0}=I_{a1}=I_{a2}\\&V_{a0}+V_{a1}+V_{a2}=V_a\end{aligned}\right\}\text{ at generator}$$

$$\left.\begin{aligned}&I_{b0}=0\\&V_{b1}=0\\&V_{b2}=0\end{aligned}\right\}\text{ at far end}$$

On substituting these relations in the general circuit equations,

$$V_a=I_a\frac{1}{3}\left(\frac{2B_1}{D_1}+\frac{A_0}{C_0}\right)$$

Therefore the unbalanced impedances are

$$Z_a=\frac{1}{3}\left(\frac{2B_1}{D_1}+\frac{A_0}{C_0}\right)\text{, infinity, infinity}$$

Figure 12 should not be used, since each branch is infinitely great. In figure 13, $Z_0+Z_1-2Z_1^2/(Z_0+Z_{g0})$ reduces to $(1/2)\times(Z_{g0}+2B_1/D_1+A_0/C_0)$, so that there is a single through impedance $(Z_{g0}+2B_1/D_1+A_0/C_0)$ between the positive- and negative-sequence potentials V_1 and V_2. In order to preserve the correct phase relation between the harmonic components of different frequencies, the positive direction of the negative-sequence potentials and currents in figure 4 and figure 5 should be reversed and at the same time X_d'' and X_q'' should be interchanged. The component sections are then connected as in figure 6; that is, between V_1' and V_2' there is a single through impedance $(Z_{g0}'+2B_1'/D_1'+A_0'/C_0')$; between $V_1'''/3$ and $V_2'''/3$ there is a single through impedance $1/3(Z_{g0}'''+2B_1'''/D_1'''+A_0'''/C_0''')$; etc. In figure 15:

$$\left.\begin{aligned}&V_{b0}+V_{b1}+V_{b2}=0\\&I_{b0}=I_{b1}=I_{b2}\end{aligned}\right\}\text{ at far end}$$

Substitute these values in the general circuit equations and use the method of obtaining equations 4, 5, and 6. The resulting equivalent circuit relating the positive- and negative-sequence components of the same frequency is in figure 16.

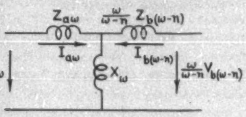
$Z_{a\omega}$
$\frac{\omega}{\omega-n}Z_{b(\omega-n)}$
$I_{a\omega}$
$I_{b(\omega-n)}$
$V_{a\omega}$
X_ω
$\frac{\omega}{\omega-n}V_{b(\omega-n)}$

Figure 17

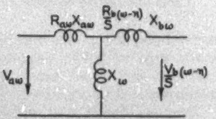

Figure 18

Appendix IV. Induction Machines

Consider a three-phase induction machine having unsymmetrical circuits connected to both the stator and rotor windings, subject to the restriction that two of the branches of each unsymmetrical circuit are alike. The equivalent circuit representing either of these unbalanced circuits is in figure 2. The general equivalent circuit which determines the relation between the corresponding stator and rotor currents is in figure 17. Corresponding stator and rotor currents are balanced systems of currents of the same designated sequence which produce magnetic fields rotating in synchronism. In figure 17 it is assumed that balanced three-phase potentials, $V_{a\omega}$, of angular velocity $\omega=2\pi f$ are applied at the terminals of one winding (either the actual stator or the actual rotor). The leakage impedance of this winding at this angular velocity (ω) is $Z_{a\omega}$. The magnetizing reactance at this angular velocity (ω) is X_ω. The approximate effect of core losses can be included by replacing X_ω by Z_ω, the magnetizing impedance. The balanced currents, $I_{a\omega}$, in this winding produce a rotating magnetic field which has an angular velocity ω with respect to the winding, the direction of rotation depending upon the phase order of the currents. With respect to the winding *a*, the angular velocity of the winding (*b*) on the other side of the air gap is n. If n is a positive number, both the second winding (*b*) and the magnetic field rotate in the *same* direction with respect to the first winding (*a*), and n may be either greater or less than ω. If n is a negative number, the second winding (*b*) and the magnetic field rotate in opposite directions with respect to the first winding (*a*). In any case the relative angular velocity of the second winding with respect to the magnetic field is $\omega-n$. The leakage impedance of the second winding (*b*) at an angular velocity $(\omega-n)$ is $Z_{b(\omega-n)}$. In the equivalent circuit this impedance becomes $\omega\div(\omega-n)Z_{b(\omega-n)}$ and the terminal potential on this side of the equivalent circuit is $\omega/(\omega-n)V_{b(\omega-n)}$, where $V_{b(\omega-n)}$ is the actual terminal potential of the second winding. The subscript $\omega-n$ attached to V_b merely indicates the angular velocity of this terminal potential. These potentials are balanced and are said to have the same phase order as the potentials $V_{a\omega}$. The currents, $I_{b(\omega-n)}$ in the second winding also are balanced and of the same phase order as the potentials. They produce a magnetic field in the air gap rotating in synchronism with the field due to the currents $I_{a\omega}$.

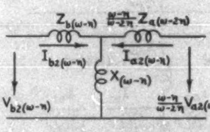
$Z_{b(\omega-n)}$
$\frac{\omega-n}{\omega-2n}Z_{a(\omega-2n)}$
$I_{b2(\omega-n)}$
$I_{a2(\omega-n)}$
$X_{(\omega-n)}$
$V_{b2(\omega-n)}$
$\frac{\omega-n}{\omega-2n}V_{a2(\omega-2n)}$

Figure 19

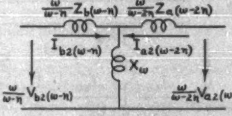
$\frac{\omega}{\omega-n}Z_{b(\omega-n)}$
$\frac{\omega}{\omega-2n}Z_{a(\omega-2n)}$
$I_{b2(\omega-n)}$
$I_{a2(\omega-2n)}$
X_ω
$\frac{\omega}{\omega-n}V_{b2(\omega-n)}$
$\frac{\omega}{\omega-2n}V_{a2(\omega-2n)}$

Figure 20

The usual definition of the slip, s, is $s=(\omega-n)/\omega$. If n is greater than ω, the slip is a negative number.

The more familiar form of the equivalent circuit is in figure 18. The leakage reactances $X_{a\omega}$ and $X_{b\omega}$ are both for the same angular velocity, ω. This assumes leakage inductance is independent of frequency. Since it is sometimes best to recognize the effect of frequency on resistance, the subscripts ω and $\omega-n$ are attached to the resistances to indicate the frequencies of the currents in them.

If the circuits connected to the second winding (*b*) are unsymmetrical, the currents in the second winding (*b*) are unbalanced and may be resolved into their positive- and negative-sequence components. The positive-sequence currents in the second winding (*b*) produce a magnetic field, rotating positively at an angular velocity $(\omega-n)$ with respect to the second winding and with an angular velocity ω with respect to the first winding. The relations of the positive-sequence currents in the two windings are determined by the equivalent circuit of figure 17. The negative-sequence currents in the second winding produce a magnetic field rotating at an angular velocity $(\omega-n)$ in the opposite direction with respect to the second winding and hence at an angular velocity $(\omega-2n)$ with respect to the first winding. This field induces a negative-sequence current of angular velocity $(\omega-2n)$ in the first winding. The relations of the negative-sequence currents in the two windings are determined by the equivalent circuit of figure 19.

If the terminal potentials and the impedances of figure 19 are multiplied by $\omega/(\omega-n)$ as in figure 20, the currents are unchanged.

If the terminal potentials and the impedances of figure 2 are multiplied by $\omega/(\omega-n)$, this circuit can be used to connect the equivalent circuits of figure 17 and figure 20 as in figure 21. Note, for example, that

$$\frac{\omega}{\omega-n}Z_{b(\omega-n)}=\frac{\omega}{\omega-n}r_{b(\omega-n)}+jX_b$$

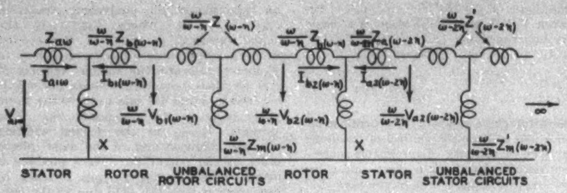
Zaω
ω/(ω−n) Zb(ω−n)
ω/(ω−n) Z (ω−n)
ω/(ω−n) Zb(ω−n)
ω/(ω−2n) Za(ω−2n)
ω/(ω−2n) Z' (ω−2n)
Ia1ω
Ib1(ω−n)
Ib2(ω−n)
Ia2(ω−2n)
Va1ω
ω/(ω−n) Vb1(ω−n)
ω/(ω−n) Vb2(ω−n)
ω/(ω−2n) Va2(ω−2n)
X
ω/(ω−n) Zm(ω−n)
X
ω/(ω−2n) Z'm(ω−2n)
∞
STATOR
ROTOR
UNBALANCED ROTOR CIRCUITS
ROTOR
STATOR
UNBALANCED STATOR CIRCUITS

Figure 21

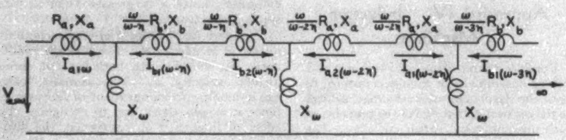
Ra, Xa
ω/(ω−n) Rb, Xb
ω/(ω−n) Rb, Xb
ω/(ω−2n) Ra, Xa
ω/(ω−2n) Ra, Xa
ω/(ω−3n) Rb, Xb
Ia1ω
Ib1(ω−n)
Ib2(ω−n)
Ia2(ω−2n)
Ia1(ω−2n)
Ib1(ω−3n)
Va1ω
Xω
Xω
Xω
∞

Figure 22

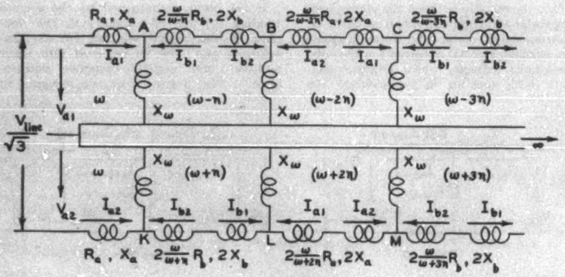
Ra, Xa
2 ω/(ω−n) Rb, 2Xb
2 ω/(ω−2n) Ra, 2Xa
2 ω/(ω−3n) Rb, 2Xb
A
B
C
Ia1
Ib1
Ib2
Ia2
Ia1
Ib1
Ib2
Va1
ω
Xω
(ω−n)
Xω
(ω−2n)
Xω
(ω−3n)
Vline/√3
∞
Xω
ω
(ω+n)
Xω
(ω+2n)
Xω
(ω+3n)
Va2
Ia2
Ib2
Ib1
Ia1
Ia2
Ib2
Ib1
K
L
M
Ra, Xa
2 ω/(ω+n) Rb, 2Xb
2 ω/(ω+2n) Ra, 2Xa
2 ω/(ω+3n) Rb, 2Xb

Figure 23

Consequently each inductive reactance has the same value wherever it appears in the network, provided each phase of the circuits connected to the stator and rotor consists of a series combination of resistance and inductance. The resistance *at a frequency* $(\omega-kn)$ is multiplied by $\omega/(\omega-kn)$, where $(\omega-kn)$ is the angular velocity of the particular component current in the resistance being considered.

Consider the case in which a three-phase motor is operated from an infinite bus with one stator terminal free and only two of the three rotor terminals short-circuited. The unbalanced impedances connected to both stator and rotor terminals are then infinity, zero, zero, and the impedances of figure 2 are $Z=0$, $Z_m=\infty$. The positive-sequence equivalent circuit of figure 21 becomes the equivalent circuit of figure 22. The equivalent circuit showing the relations among the potentials and currents due to the application of negative-sequence potential of fundamental frequency (ω) at the terminals of the stator is exactly like figure 22 except that $-n$ is replaced by $+n$. The resultant equivalent circuit from which all the component potentials and currents of every frequency are computed is obtained by connecting the positive- and negative-sequence equivalent circuits in series as shown in figure 23. In analyzing the single-phase operation of a three-phase motor when the rotor is symmetrically short-circuited, the positive- and negative-sequence equivalent circuits are connected in series in the same way.[4]

In figure 23 the subscripts indicating the frequencies are omitted from the currents to simplify the notation, but the frequencies are indicated in the loops in which the currents flow. The total average torque developed is

$$\frac{550}{746}\frac{3p}{2\omega}\left\{[I_{b1}{}^2{}_{(\omega-n)}R_A+I_{a2}{}^2{}_{(\omega-2n)}R_B+\ldots\ldots]-[I_{b2}{}^2{}_{(\omega+n)}R_K+I^2{}_{a1(\omega+2n)}R_L+\ldots\ldots]\right\}$$

pound-feet

where R_A is the resistance of the entire positive-sequence equivalent circuit to the right of A, R_B is the resistance of the entire positive-sequence equivalent circuit to the right of B, R_L is the resistance of the entire negative-sequence equivalent circuit to the right of L; where p is the number of poles for which the motor is wound; and $\omega=2\pi f$ is the fundamental angular velocity.

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1. Applications of the Method of Symmetrical Components (a book), W. V. Lyon. Article 143.

2. Symmetrical Components (a book), Wagner and Evans. Chapter 18.

Applications of the Method of Symmetrical Components (a book), W. V. Lyon. Chapter 2.

3. Electric Circuits (a book), G. C. Dahl. Volume 1, chapter 10.

4. Wagner and Evans, chapter 17; W. V. Lyon, chapter 7.

Discussion

For discussion, see page 1263.

Performance of Traveling Waves in Coils and Windings

REINHOLD RUDENBERG
MEMBER AIEE

THE reaction of coils and windings to traveling waves which impinge upon their terminals is an ever-important problem in electric power systems from both the practical and the scientific point of view. Transformers as well as alternators are used at increasingly higher voltages and their insulation is thus subjected to switching and lightning surges of ever-increasing severity. A simplified but nevertheless rigorous conception of the wave propagation within such windings may therefore be advantageous. It may be used for coils in communication circuits as well.

Previous investigations[1–5] show that in addition to the self-inductance and ground capacitance of the windings, the internal capacitance between turns or coils plays an important part in their behavior to surges. The various investigators differ, however, in defining the self- and mutual inductances of coils and windings under the conditions of rapid transients. By adopting in this paper a special method of deriving the differential equations of the system, the inductance can be related to well-known data of the winding.

Formerly, the effect of an incident steep wave front has been analyzed from the point of view of standing waves within the winding. This involves the numerical computation of many terms of an infinite series of waves, the series having slow convergence. It will be shown that the conception of traveling waves leads to a much simpler result as the problem can then be solved by a mathematical summation of the series of waves in a compact form. The results developed from the analysis given in this paper are verified by many experimental data, some of which have already been published while others are given here for the first time.

Inductive and Capacitive Coupling of Turns and Coils

Consider first a coil which consists, as in figure 1, of but one layer, the turns being wound fairly close together. A single turn has capacitance to ground and, in addition, to both adjacent turns. The capacitance with respect to any other turn, however, is always small compared with either of the capacitances just mentioned. Figures 1*b* and 1*c* represent the distribution of interturn and ground capacitances over the entire coil, both capacitances being uniform over all the elements. At the ends of the coil, however, the ground capacitance c increases and the turn capacitance k decreases because the external flat side of the last turn affects the ground capacitance, but not the turn capacitance. However, this can be taken into account when the boundary conditions of the coil enter into consideration.

Consider winding elements or turns of a length

$$\Delta x = w \tag{1}$$

x being the length co-ordinate of the conductor. The charging current in the ground capacitance of the nth turn is

$$i_{cn} = cw\frac{\partial e_n}{\partial t} \tag{2}$$

where e_n is the mean voltage of the nth turn, c its ground capacitance per unit length of the coil conductor, and t the time. Correspondingly, the charging current of the nth turn to both its adjacent turns is

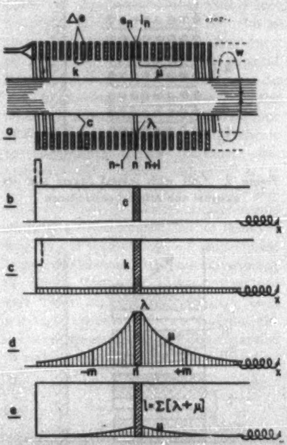

Figure 1. Cylindrical single-layer coil and spatial distribution of its capacitances and inductances

$$i_{kn} = kw\left[\frac{\partial(e_n - e_{n-1})}{\partial t} + \frac{\partial(e_n - e_{n+1})}{\partial t}\right]$$

$$= -kw\frac{\partial \Delta^2 e_n}{\partial t} \tag{3}$$

if we call k the turn capacitance of the unit length between adjacent turns and if the second *difference* of the voltages is abbreviated to

$$\Delta^2 e_n = e_{n+1} - 2e_n + e_{n-1} \tag{4}$$

The sum of both charging currents, as given in equations 2 and 3, is equal to the decrease of the current within the nth turn and the finite spatial alteration of current is thus

$$-\Delta i_n = cw\frac{\partial e_n}{\partial t} - kw\frac{\partial \Delta^2 e_n}{\partial t} \tag{5}$$

If, in addition to the capacitance to ground, there exists any other external capacitance, such as capacitance to conductors connected with the incoming line, its effect can be expressed by terms similar to the first term on the right-hand side of equation 5. This addition, however, does not alter the general character of the relations here derived.

The variation of the voltage along the coil is caused by the inductive effects of its magnetic field. Although in reality there is only one magnetic field, its action can be divided into the self-inductive effect of the turn under consideration and the mutual inductive action of the other turns. If we call λ the self-inductance per unit length of a single turn when separated from the coil, its self-induced voltage is

$$e_{\lambda n} = \lambda w\frac{\partial i_n}{\partial t} \tag{6}$$

where i_n is the mean current within the nth turn. The two immediately adjacent turns with their mutual inductance μ induce a voltage

$$e_{\mu n} = \mu w\frac{\partial(i_{n+1} + i_{n-1})}{\partial t} \tag{7}$$

and a similar voltage is produced by all other turns on both sides of the turn under

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[1] For all numbered references, see list at end of paper.

consideration. However, μ decreases rapidly with the distance between the inducing turns, as shown in figure 1*d*, since for the very rapid variations in time considered here practically no flux can penetrate the laminated iron core of the coil, and mainly the air flux is effective. We exclude, therefore, any common core flux in our considerations in case there exists a closed steel core.

The sum of the currents in equation 7 is built up in a different manner from the sum of the voltages in equation 3. However, we can develop a mathematical expression similar to equation 3 if, within the bracket of equation 7, we add and subtract twice the current i_n of the turn under consideration. Then, for the two adjacent turns, there results

$$e_{\mu n}=2\mu w\frac{\partial i_n}{\partial t}+\mu w\frac{\partial(i_{n+1}-2i_n+i_{n-1})}{\partial t} \qquad (8)$$

and for all turns with any distances *m* from the turn under consideration the sum is

$$\sum_m e_{\mu n}=\sum \mu w\frac{\partial i_n}{\partial t}+\sum \mu w\frac{\partial\Delta^2 i_{n,m}}{\partial t} \qquad (9)$$

In the first term on the right-hand side of equation 9, the change of current is the same for every μ, and can therefore be put before the summation sign. In the second term on the right-hand side the change of current may be different for every μ.

The finite drop of voltage in the turn under consideration is now given by the sum of equations 6 and 9 as

$$-\Delta e_n=(\lambda+\Sigma\mu)w\frac{\partial i_n}{\partial t}+\Sigma\mu w\frac{\partial\Delta^2 i_{n,m}}{\partial t} \qquad (10)$$

if we neglect the small resistance of the conductors. If we consider the first term on the right-hand side of equation 10, we realize that it corresponds to the sum of λ with every μ in figure 1*d*. This, however, is the total self-inductance of the whole coil, related here to the length *w* of one turn. The sum

$$l=\lambda+\Sigma\mu \qquad (11)$$

is therefore the total self-inductance of the winding under consideration per unit length of its conductor.

The coupling effect of all turns with the turn under consideration is expressed mainly by the first term of the right-hand side of equation 10, while the second term, consisting of differences, is merely a remainder. For harmonic oscillations within the coil we can evaluate this remainder (see discussion) and we see then that its elements are mostly acting against one another. If the waves are not extremely short, the total summation will be very small and consequently we make only a small error if we neglect the second term on the right-hand side of equation 10 and confine ourselves to the first term only. If we do not neglect completely the last member of equation 10, but take into account only its largest term, corresponding to the turns immediately adjacent to those under consideration,[6] the result of the calculations would alter only very slightly.

For the change of voltage we now obtain the simple expression

$$-\Delta e_n=lw\frac{\partial i_n}{\partial t} \qquad (12)$$

where *lw* is the contribution of one turn to the total self-inductance of the coil.

Before we proceed further, it is useful to extend the sphere of validity of our derivations. If the coil under consideration has not only one layer, as in figure 1, but any number of layers, as in figure 2, the average voltage and current of every layer may be represented by e_n and i_n. Correspondingly, we denote by *c* the capacitance to ground of every layer and by *k* the capacitance between any two layers, *c* and *k* being related to the unit length of conductor, while the definition of the self-inductance *l* remains the same as before. Then we obtain exactly the same formal expressions 5 and 12, except that now *w* is the length of wire of every layer.

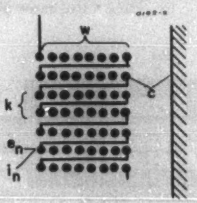

Figure 2. Coil with several layers and its external and internal capacitance

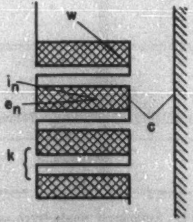

Figure 3. Winding consisting of several coils and its external and internal capacitance

If, finally, the winding is arranged in separate coils as in figure 3, we may relate these characteristic values to any coil, and equations 5 and 12 will now give the mean values of voltage e_n and current i_n of the successive coils.

Not only transformer coils and reactor coils, but also windings of generators or motors can be treated in the same manner. In every case we must take into consideration the wire length *w*, and corresponding capacitances *c* and *k*, over which the mean values of voltage and current are reckoned: the length of a coil for the gross distribution, the length of a layer for the finer distribution, the length of a turn for the very minute change of voltage and current.

Our final equations 5 and 12 are differential equations in time and difference equations in space. If we take the internal capacitance $k=0$, equations 5 and 12 change to the equations of the ordinary choke filter circuit with every turn or coil as an element, although with a different definition of *l*. If we decrease the turn length *w*, with the limit 0, we may write in consequence of equation 1 $w=dx$, and thus equations 5 and 12 change to the simpler differential equations

$$\left.\begin{aligned}-\frac{\partial i}{\partial x}&=c\frac{\partial e}{\partial t}-kw^2\frac{\partial^3 e}{\partial t\,\partial x^2}\\ -\frac{\partial e}{\partial x}&=l\frac{\partial i}{\partial t}\end{aligned}\right\} \qquad (13)$$

These are valid to a good degree of approximation if the number of turns, or layers, or coils is relatively large. As this is often the case with machines and transformers, we shall use this formulation (13) in most of our calculations. Then the term with *k* in equation 13 forms the sole difference in comparison with the well-known differential equations for smooth transmission lines.

Distribution of Standing and Traveling Waves

If we eliminate the current *i* from the two equations 13, we obtain

$$\frac{\partial^2 e}{\partial x^2}-lc\frac{\partial^2 e}{\partial t^2}+lkw^2\frac{\partial^4 e}{\partial t^2\partial x^2}=0 \qquad (14)$$

The same differential equation of the fourth order holds good also for the current *i*.

The simplest form of solution is

$$e=E\cdot\epsilon^{j\omega t}\cdot\epsilon^{j\alpha x} \qquad (15)$$

according to which a voltage with the amplitude *E* oscillates sinusoidally in time and also in space along the extension *x* of the coil conductor. ω is the angular frequency in 2π seconds, and α is the wave density along the wire of the coil expressed as the number of wave lengths in 2π cm of the wire. By introducing equation 15 into equation 14, we obtain

$$-\alpha^2+lc\omega^2+lkw^2\omega^2\alpha^2=0 \tag{16}$$

whereby frequency and wave density are correlated.

Instead of solution 15, which represents standing waves within the coil, we can solve the differential equation 14 by

$$e=E\cdot\epsilon^{j\omega\left(t-\frac{x}{v}\right)} \tag{17}$$

which represents traveling waves within the coil. They oscillate in time with the frequency ω, but propagate along the coil conductor with a velocity v. This solution is fully equivalent to the solution by equation 15. By substituting equation 17 in equation 14, we obtain

$$-\left(\frac{\omega}{v}\right)^2+lc\omega^2+lkw^2\omega^2\left(\frac{\omega}{v}\right)^2=0 \tag{18}$$

which connects frequency ω with velocity v of the traveling waves. Which of the two conceptions, standing waves and traveling waves, is simpler or more practicable depends upon the special physical problem under consideration.

We consider first the development of standing waves. From equation 16 we derive

$$\left.\begin{aligned}\alpha&=\sqrt{\frac{cl\omega^2}{1-klw^2\omega^2}}\\ \omega&=\frac{\alpha}{\sqrt{cl+klw^2\alpha^2}}\end{aligned}\right\} \tag{19}$$

For low frequencies the second term of the denominators is very small and therefore wave density and frequency are practically proportional. The second expression 19 shows that for greater wave density α the internal capacitance k forms an increasing additional capacitance to the external or ground capacitance c. The first expression 19 shows that with increasing frequency ω the wave density α increases rapidly and becomes infinitely great at a certain frequency, given by the zero value of the denominator as

$$\omega=\nu=\frac{1}{w\sqrt{kl}} \tag{20}$$

We call this value ν the critical frequency of the coil,[1] because it plays a critical role in the performance of the winding. It prevents the formation of standing waves for any exciting frequency ω which is equal to or larger than this limiting value ν, because α then becomes imaginary, so that the solution 15 is no longer valid.

We discuss first the subcritical range of frequencies. In each finite winding with open or closed ends, there can exist only a series of well-defined spatial waves which are determined by the boundary conditions at the ends. If m is the number of full waves over the total wire length a, only the wave lengths

$$\lambda=\frac{a}{m}\text{, with } m=\begin{cases}{}^1/_4\times 1, 3, 5, 7\ldots\\ {}^1/_4\times 2, 4, 6, 8\ldots\end{cases} \tag{21}$$

are compatible with the stated boundary conditions. Hence the wave density of such standing waves is given by

$$\alpha=\frac{2\pi}{\lambda}=\frac{2\pi}{a}m \tag{22}$$

If we introduce this expression 22 into the second equation 19, we obtain the only frequencies which can occur in natural oscillations:

$$\omega=\frac{2\pi\frac{m}{a}}{\sqrt{lc\left[1+4\pi^2\frac{k}{c}\left(\frac{w}{a}m\right)^2\right]}} \tag{23}$$

While in smooth lines the natural frequency is directly proportional to the number m of wave lengths and inversely proportional to the length a of the line, this relation may hold in windings only for small values of m and relatively large values of wire length a. However, if the quotient m/a increases, the intervals between successive natural frequencies become smaller. The natural frequencies are crowded together at last in a single point[2] at the value of the critical frequency ν, given by equation 20; this is shown in figure 4 for a critical frequency of five times the fundamental natural frequency.

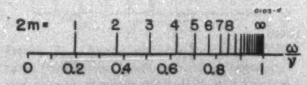

Figure 4. Spectrum of natural frequencies of a coil with internal capacitance

It is interesting to consider how the windings of transformers perform in view of these conclusions. Since it is difficult to co-ordinate natural harmonic frequencies[3] with definite values of m, it appears more conclusive to compare with equation 23 several measurements of fundamental frequencies for different lengths a of the winding. A single-phase power transformer with pancake winding of 16,660 kva with 11-kv low-voltage and 19-kv high-voltage windings, each winding consisting of two separate circuits on the same core for either parallel or series connection, was tested according to figure 5 with four different connections of each winding, the other winding always being grounded at both ends. Table I indicates the results of these experiments* which were carried out by a resonance method. Although the grounding of one terminal of the winding should halve the natural frequency, according to the usual conception, we see that this does not happen either with the high-voltage or low-voltage winding, whether the winding is in parallel or in series. This result is due to the effect of the denominator in equation 23. But if we consider the frequencies with the grounded parallel connection and the insulated series connection, as in figure 5*b* and *c*, we find that the measured frequencies are exactly the same. This is due to the fact that m/a is the same in both cases, thus keeping numerator and denominator of equation 23 constant.

In order to compare these results with equation 23, we use the abbreviation

$$K=2\pi\sqrt{\frac{k}{c}}w \tag{24}$$

and write equation 23 in the form

$$\frac{1}{\omega^2}=\frac{1}{\nu^2}+\left(\frac{a/m}{\nu K}\right)^2 \tag{25}$$

If we plot $1/\omega^2$ against $(a/m)^2$, we should expect that our measured points would lie on a straight line which intersects the axis of ordinates at the critical frequency $1/\nu^2$. We see from figure 6 that the high-voltage frequencies agree very closely with the theoretical relation, while the low-voltage frequencies show minor deviations. The critical frequencies of the two windings as determined from this figure are 90 kilocycles per second for the high-voltage and 130 kilocycles per second for the low-voltage winding.

Before proceeding to supercritical frequencies, we will consider the performance of traveling waves within the winding. From equation 18 we derive the velocity of their propagation

$$v=\sqrt{\frac{1}{cl}-\frac{k}{c}w^2\omega^2} \tag{26}$$

and recognize that this velocity approximates the limiting value

$$v_0=\frac{1}{\sqrt{cl}} \tag{27}$$

only for waves of very low frequency. But the higher the exciting frequency ω is chosen, the smaller the velocity v becomes, and as we approach the critical frequency given by equation 20, the veloc-

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* The measurements have been made by Mr. J. S. Cliff at the Witton factories of The General Electric Company, Ltd., London.

Table I. Natural Frequencies and Wave Velocities of Transformer Windings in Various Connections

	Winding Length a (Kilometers)	Connection and 1/m	Natural Frequency (Kilocycles Per Second)	Wave Velocity v (Meters Per Microsecond)
Low-voltage winding	0.362	○═○ 2	97	70
	0.362	●═○ 4	76	110
	0.724	○—○ 2	75	109
	0.724	●—○ 4	40.5	117
High-voltage winding	0.635	○═○ 2	67	85
	0.635	●═○ 4	43.5	111
	1.270	○—○ 2	43.5	111
	1.270	●—○ 4	24	122

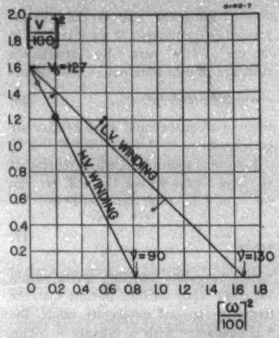


Figure 7. Wave velocity of the tested transformer plotted against natural frequency, both in square scale

ity decreases to zero, so that no propagation of this frequency or of any higher frequency can occur.

In order to determine how our experiments agree with this result, we write equation 26 in the form

$$\left(\frac{v}{v_0}\right)^2+\left(\frac{\omega}{\nu}\right)^2=1 \tag{28}$$

Table I contains the measured velocity of propagation within our transformer windings, determined by the product of natural frequency and actual length of the winding for each test and the numerical factor $1/m$, corresponding to the boundary conditions of the fundamental waves of figure 5 and equation 21. We recognize that the speed of propagation of the traveling waves decreases considerably with increasing natural frequency. In figure 7 v^2 is plotted against ω^2 and we see that the measured points lie on two straight lines in agreement with equation 28, the high-voltage points very closely and the low-voltage points with a fairly good degree of approximation.

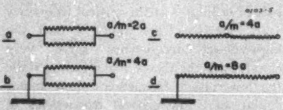


Figure 5. End connections for testing a transformer winding with various natural frequencies

From figure 7 we can determine the value of the limiting velocity of traveling waves for low frequencies as $v_0=127{,}000$ kilometers per second, for both high-voltage and low-voltage windings. If we take the average magnetodielectric constant $\epsilon\mu$ of the winding space as 4.5, the velocity of propagation of free electromagnetic excitations is

$$300{,}000/\sqrt{4.5}=141{,}000 \text{ kilometers per second}$$

independent of the design of the winding. Thus we realize that traveling waves of low frequency are propagated along the transformer wires with very nearly this fundamental velocity. Waves of higher frequency, however, travel substantially slower because of the effect of the internal capacitance.

Table II shows measurements made on transformer windings of different rated voltages. The first two transformers are single-phase with pancake and with concentric windings; the third transformer has three-phase windings in star and in delta connection; and the last apparatus is an air-core, steel-yoke reactor. Figure 8 shows the wave velocity in these differ-

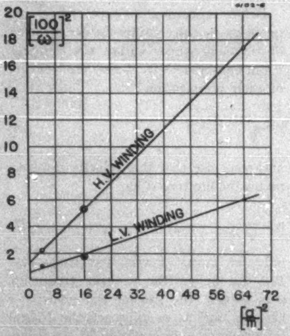
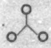


Figure 6. Reciprocal natural frequency of a transformer plotted against the equivalent wire length, both in scale of squares

Table II. Natural Frequencies and Wave Velocities of Transformer Windings of Various Types and Voltages

Power (Kva) Type of Winding	Rated Voltage (Kv)	Phase Length a (Kilometers)	Diagram and 1/m	Natural Frequency (Kilocycles Per Second)	Wave Velocity v (Meters Per Microsecond)
16,660 Pancake	38	1.27	○—○ 2	43.5	111
	22	0.724	○—○ 2	75	109
5,000 Concentric	32	2.15	○—○ 2	32	137
	6.4	0.314	○—○ 2	110	69
12,000 Concentric	33	1.06	 4	31	132
	11	0.485	3	70	102
1,660 Reactor	7.6	1.27	○—○ 2	30	76

ent types of windings, beginning with small velocity at low rated voltages and approaching, at higher rated voltages, the electromagnetic velocity of light in oil transformer insulation.

If the velocity v of the traveling waves is known empirically for various types of transformers, it is easy to predict the natural frequency of any winding, which is simply

$$\omega = 2\pi m \frac{v}{a} \tag{29}$$

where a is the length of the winding and m is the number of wave lengths defined in equation 21. By comparing equation 29 with equation 23 it is evident that the denominator of equation 23 determines the velocity of waves for a given winding

$$v = \frac{v_0}{\sqrt{1 + 4\pi^2 \frac{k}{c}\left(\frac{w}{a} m\right)^2}} \tag{30}$$

If we determine the capacitive ratio k/c from the geometry of the winding, we may then find the velocity and the frequencies by mere calculation.

If the exciting frequency of the winding exceeds the critical frequency of equation 20, the wave density α in equation 19 becomes imaginary and it is better to write this relation as

$$\alpha = j\sqrt{\frac{cl\omega^2}{klw^2\omega^2 - 1}} = j\beta \tag{31}$$

Instead of by equation 15, the voltage is now given by

$$e = E \cdot \epsilon^{j\omega t} \cdot \epsilon^{-\beta x} \tag{32}$$

Thus for supercritical frequencies there no longer exist any standing or traveling waves within the winding, but only an exponential attenuation of the voltage from the terminals toward the interior. The space constant X of this voltage distribution at rest is, with use of equation 20,

$$X = \frac{1}{\beta} = w\sqrt{\frac{k}{c}\left[1 - \left(\frac{\nu}{\omega}\right)^2\right]} \tag{33}$$

Near the critical frequency ν the space constant X is fairly small, but X increases rapidly with higher exciting frequencies, approaching a maximum value

$$X_\infty = w\sqrt{\frac{k}{c}} \tag{34}$$

Figure 9 gives the variation of the space constant with frequency according to the circle equation

$$\left(\frac{X}{X_\infty}\right)^2 + \left(\frac{\nu}{\omega}\right)^2 = 1 \tag{35}$$

The current i in equations 5 and 13 denotes the conductor current within any winding element. At the terminal of the winding, however, there is collected by the external lead the sum of the last-turn current and the internal capacitive current which ends at this point, as it has no exterior adjacent turn in which to flow. The external terminal current is, therefore, using equations 3 and 5 for $n = 0$,

$$i_0 = i_n + i_{kn} = cw \sum \frac{\partial e_n}{\partial t} = c\int \frac{\partial e}{\partial t} dx \tag{36}$$

which is written in difference form as well as in differential form, the latter for large numbers of turns. If we evaluate the last integral, using the voltage of equation 15, we obtain for the terminal current

$$i_0 = c\frac{\omega}{\alpha} \cdot E\epsilon^{j\omega t} \cdot \epsilon^{j\alpha x} \tag{37}$$

an equation valid only for $x = 0$.

The surge impedance of any line is the ratio of the terminal voltage, according to equation 15, to the terminal current, according to equation 37. For our winding this is

$$\frac{e_0}{i_0} = Z = \frac{\alpha}{c\omega} = \sqrt{\frac{l/c}{1 - klw^2\omega^2}} = \frac{Z_0}{\sqrt{1 - \left(\frac{\omega}{\nu}\right)^2}} \tag{38}$$

where

$$Z_0 = \sqrt{\frac{l}{c}} = \sqrt{\frac{L}{C}} \tag{39}$$

is the surge impedance for low frequency, being identical with the expression commonly used for smooth lines. Since we have defined l as the self-inductance L of the whole winding divided by the total length of the winding, and c as the capacitance C of the whole winding to ground divided by the total length of the winding, we realize that Z_0 can be computed easily from the total inductance and total capacitance of the winding, a consideration which is of some value for a simple calculation of practical cases. For increasing frequency the surge impedance of the winding increases according to equation 38 and approaches infinity as the frequency approaches the critical value.

For supercritical frequencies the voltage-current ratio of equation 38 becomes imaginary and we write inverted therefore the ratio

$$\frac{i_0}{e_0} = \frac{\omega c}{\alpha} = j\omega w\sqrt{kc\left[1 - \left(\frac{\nu}{\omega}\right)^2\right]} = j\omega C \tag{40}$$

We see that here the winding acts as pure capacitance C, which is, however, small for frequencies in the neighborhood of the critical value but increases rapidly with higher frequencies up to a maximum

$$C_\infty = w\sqrt{kc} \tag{41}$$

This value is in accordance with the resultant capacitance of a long chain of series capacitances k and ground capacitances c, each extended over a length w.

Figure 9 represents the variation of all characteristic values of the winding which we have derived: traveling velocity v and surge impedance Z for the subcritical range, and space constant X and terminal capacitance C for the supercritical range. For very low and very high exciting frequencies, the winding

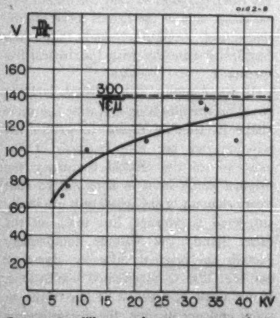


Figure 8. Wave velocities measured in various transformer windings of different rated voltage

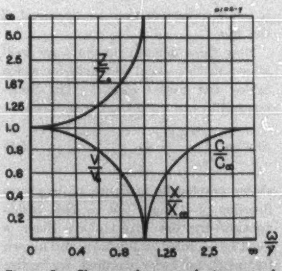


Figure 9. Change of wave velocity v, and surge impedance Z, with subcritical frequency and change of space constant X, and terminal capacitance C, with supercritical frequency, all expressed in relative values

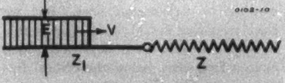


Figure 10. Steep-front wave impinging on a winding from a line

performs regularly like an ordinary smooth line or like an ordinary capacitance chain. In the neighborhood of the critical frequency, however, the characteristic values approach zero or infinity, which means that then the winding is in a kind of resonance state which is due to oscillations of its single elements against one another.

Summation of Subcritical and Supercritical Waves

It is seldom that a transformer, or other machine winding, is excited with a single definite frequency. For the most part traveling waves of any shape, nonsinusoidal in form, impinge from the system lines upon the winding, as shown, for example, in figure 10. However, according to Fourier's theorem, any such wave form can be regarded as a summation of sinusoidal oscillations with every frequency from zero to infinity, each frequency having a definite amplitude. The rectangular wave shown in figure 10, consisting of an abrupt jump at its front and a constant value thereafter, is expressed by the Fourier integral:[7]

$$e=\frac{E}{2}+\frac{E}{\pi}\int_0^{\infty}\frac{\sin\omega t}{\omega}\,d\omega \tag{42}$$

It contains a constant term $E/2$, which makes the wave shape dissymmetrical to the zero line, and an integral which expresses the summation over all frequencies of oscillation with amplitudes inversely proportional to the frequency. The spectral distribution of amplitudes is shown in figure 11.

If such a wave form were to impinge from one smooth line on another smooth line, it would penetrate as a whole into the second line without changing its shape and undergo only refraction according to well-known laws. But we have seen that a coil or winding is able to propagate only those oscillations which have a frequency below the critical frequency ν. Therefore, only those partial waves, shaded horizontally in figure 11, can penetrate the transition point between the two lines and travel on through the winding. All partial oscillations above the critical frequency ν, shaded vertically in figure 11, cannot penetrate deeply into the winding. They excite only standing exponential distributions within the first elements of the winding and are therefore reflected back to the original line.[1]

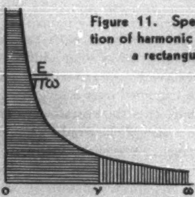


Figure 11. Spectral distribution of harmonic amplitudes of a rectangular-front wave

This splitting of incident waves into two distinct parts is the main difference between the performance of windings and of smooth lines with respect to traveling waves. We recognize therefore that of the entire expression 42 only the first term and the part of the integral up to the frequency limit ν

$$e'=\frac{E}{2}+\frac{E}{\pi}\int_0^{\nu}\frac{\sin\omega t}{\omega}\,d\omega \tag{43}$$

is traveling through the winding, while the second part of the integral with the higher frequencies above ν

$$e''=\frac{E}{\pi}\int_{\nu}^{\infty}\frac{\sin\omega t}{\omega}\,d\omega \tag{44}$$

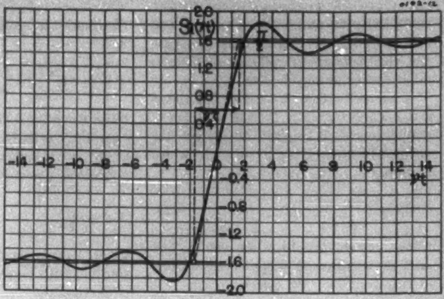
is totally reflected, as at an open end of the line.

Now these definite integrals are well-known mathematical functions for which tables and curves exist.[16] The "sine-integral"

$$Si(\nu t)=\int_0^{\nu}\frac{\sin\omega t}{\omega}\,d\omega \tag{45}$$

shown graphically in figure 12, is a pure function of νt, since the variable ω disappears by the integration. Since ν must be regarded as a definite and known frequency for every winding, we realize that this function remains for every case dependent solely on time t. Thus we see that from the original abrupt jump on the line there remains within the winding only a less steep ascent, as in figure 12 with some residual oscillations on either side of this ascent.

Figure 12. Graphical representation of the "sine-integral"



The sine-integral has very simple asymptotic approximations: for small values, up to $\nu t \cong 0.5$,

$$Si(\nu t)=\nu t \tag{46}$$

and for large values, upwards from $\nu t \cong 4$,

$$Si(\nu t)=\frac{\pi}{2}-\frac{\cos\nu t}{\nu t} \tag{47}$$

If we express the steepness of this sine-integral function by the tangent through the origin, we see from figure 12 that the time τ of the ascent of the voltage is given by the intersections of this tangent with the positive and negative final values. We have, therefore, using equations 46 and 47,

$$\nu\tau=2\frac{\pi}{2}=\pi \quad\text{or}\quad \tau=\frac{\pi}{\nu}=\pi w\sqrt{kl} \tag{48}$$

and realize that the time of ascent is determined solely by the value of the critical frequency of the winding. We see from equation 47 that the oscillations on either side of the ascent have rapidly decreasing amplitudes and that their frequency coincides with the critical frequency ν.

If we compare the original incident jump on the incoming line with the wave shape in time of figure 12 within the winding, we see that because of the existence of the critical frequency the original steep front is flattened so that every element of the winding is gradually subjected to voltage, but that, on the other hand, the voltage temporarily exceeds the original value by about 12 per cent due to attenuated oscillations. If we draw the shape of the wave in space and not in time, the discontinuous jump of figure 13*a* splits into the flattened wave of figure 13*b*, which travels through the winding, and the remainder of figure 13*c*, which is reflected at the transition point. Figure 13*b* contains all subcritical oscillations, while figure 13*c* is made up of all supercritical oscillations.

Although the shape of the front of the penetrating wave of figure 13*b* is determined by the value of the critical frequency, the wave as a whole contains, as we see from figure 11, preponderantly lower frequencies. Since these lower frequencies travel with the common speed v_0 of equation 27, the spatial head-length H can be expressed to a good approximation by

$$H_0 = v_0\tau = \pi\frac{v_0}{\nu} = \pi w\sqrt{\frac{k}{c}} \qquad (49)$$

in which the critical frequency of equation 20 is introduced. We see that the length of the flattened front is a multiple of the wire length w of each winding element, if the internal capacitance k is larger than the ground capacitance c. The wave travels with this compact form through the whole winding. We realize, therefore, that the final effect of the interaction of all turns and coils of every winding consists in the flattening of the wave front by cutting off the steep portions of the original wave. The laws that hold for the traveling of waves within the extension of the winding and for their refraction and reflection at the ends remain the same as the well-known laws for smooth lines.

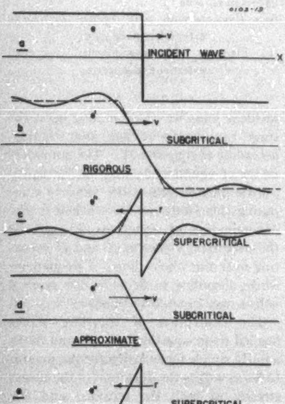


Figure 13. Subcritical and supercritical components of a steep-front wave

To follow the course of the waves graphically, it is not always convenient to draw the exact curved shapes of figures 13*b* and *c*, but it appears expedient to approximate them by the broken straight lines of figures 13*d* and *e*. These indicate that the subcritical penetrating wave contains mainly an inclined front H with a constant tail, and show that the reflected supercritical part consists chiefly of a short duplicate stroke with the original jump in its center.

With these preparations, it is easy to picture the performance at the beginning of a winding if a steep wave impinges upon it. We have only to superimpose at every instant the traveling wave e' within the turns, including the voltage $E/2$ at rest, and the exponential distribution caused by the reflected wave e'' within the end turns. For simplicity, we take as space constant for this reflected distribution the asymptotic value of equation 34, since the preponderant amount of the supercritical oscillations is co-ordinated with this fixed value. The ratio of the head length of the penetrating wave to the space constant of the reflected wave is then, using equations 34 and 49,

$$H_0 = \pi X_\infty \qquad (50)$$

Figure 14*a* represents the propagation of the voltage e' in several steps through the terminal. Figure 14*b* shows the attenuation of the reflected voltage e'' within the end turns for the same times. Corresponding, however, to the simplified straight-line representation of the wave shapes, the spatial attenuation is also linearized to straight lines with the space constant X_∞. In both graphs all states previous to time zero are drawn as dotted lines. Figure 14*c* represents the combined effect of both transmitted and reflected voltages and shows that with entrance of the original jump at time zero the voltage in the end turns suddenly jumps to an attenuated broken distribution, and also shows that the flat wave front then dissolves itself gradually and travels undisturbed from this time on throughout the winding.

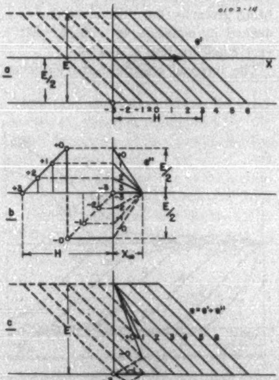


Figure 14. Entrance of a steep-front wave into a winding, in simplified form

We can now complete the picture by using the exact curves of the sine-integral function corresponding to figure 13*b* and *c* rather than figure 13*d* and *e*. Figure 15 shows the result. The small voltages preceding the arrival of the jump wave are now much more rounded. They are dotted also in this representation since they are caused mainly by our approximations, particularly of a constant speed v_0 for the complete wave, a supposition to which we shall return. We recognize from figure 15 that the wave within the winding forms at time zero an attenuated distribution with an initial space constant which is larger than X_∞ by 22 per cent. This voltage appears instantaneously with the arrival of the original jump wave at the terminal and is caused physically by the combined effect of the internal and ground capacitance of the winding. At first slowly and then progressively faster, there emerges from this initial shape a traveling voltage wave of smooth form which always keeps the terminal voltage constant but swings within the winding by 12 per cent below and above its final values. As a result not only the ends but also the interior of the winding are exposed to a considerable excess strain over the steady-state voltage stresses.

It is assumed in these drawings that the refraction factor for the traveling wave and the reflection factor for the reflected wave are equal. For most cases the surge impedance Z of winding is larger than that of the feeding line and Z increases, according to figure 9, even further as the critical frequency is approached. The refraction factor, there-

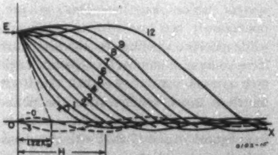


Figure 15. Distortion of a steep-front wave within the winding, in rigorous form

fore, approaches the value 2. The terminal capacitance, on the other hand, which loads the open end of the feeding line for supercritical frequencies, starts with small values, according to figure 9, for the neighborhood of ν and approaches a moderate value for most types of windings. Thus the reflection factor also approaches the value 2. Exact calculations show that the small differences caused by the finite values of Z and C do not distort our general picture. But naturally the final voltage E within the winding compared with that on the feeding line E_1 is always

$$\frac{E}{E_1}=\frac{2Z}{Z+Z_1} \tag{51}$$

Z_1 being the surge impedance of the line.

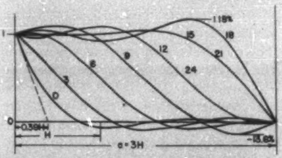


Figure 16. Arrival and reflection of the flattened wave at the grounded end of a winding

If the flat-headed traveling wave approaches the far end of the winding, perhaps the neutral point of a three-phase winding, it is reflected here according to familiar laws. Figure 16 represents several steps of such reflection for a grounded end and figure 17 for an insulated end, if we take arbitrarily the front length of the traveling wave as one-third of the whole phase length. If the winding is subdivided into only a moderate number of coils, the wire length w of each one is a considerable fraction of the phase length. We see then from equation 49 that the head length may become even larger than the phase length, depending entirely on the square root of the capacitances. The reflections which appear concentrated in figures 16 and 17 may thereby become more distributed over several phase lengths. This is preponderantly the case with transformers, while machine windings for the most part have so large a number of coils and such high ground capacitance that the front length is much smaller than the phase length. At all events, we recognize from figures 16 and 17 that with grounded end, the internal strain of the windings near the neutral is doubled, while with insulated end the strain to ground is doubled. By action of the overswinging of the sine-integral curve, as in figure 12, there are created excess voltages of 18 per cent over the final state with both grounded and insulated neutral. In every case small forerunner voltages precede the traveling front of the wave and cause negative stresses of 10–20 per cent at or near the neutral point. The shape and the speed of the traveling-wave front do not remain so uniform in the neighborhood of the neutral point as in the free portions of the winding, since the reflected forerunner voltages cause some distortion of both shape and speed.

If we compare these calculated distributions of voltage with the well-known results[13,14] of cathode-ray oscillograms, we realize that not only the general performance of figures 16 and 17 is in good agreement with the experimental results, but that even refinements, such as the often observed negative voltage forerunners of the penetrating waves, are well given by our representation.

The formulas and figures developed until now represent the voltage stress to ground of every point of the winding. The straining of the internal insulation, however, is determined by the voltage between adjacent turns, layers, or coils. If we reckon these internal voltages over the wire length w of each element, the maximum internal stress caused by the traveling wave with a front length H_0 is

$$\epsilon'=\frac{w}{H_0}E=\frac{E}{\pi}\sqrt{\frac{c}{k}} \tag{52}$$

Every internal element of the winding is successively exposed to such excess voltage, the duration of the exposure being given by the time of ascent τ, as defined by equation 48, τ being only a very small fraction of a second.

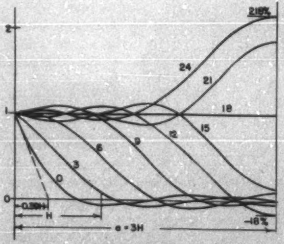


Figure 17. Arrival and reflection of the flattened wave at the insulated end of a winding

The terminal coils or turns, however, are exposed, in addition, to the strain of the standing exponential distribution which has, according to figure 14b and c, a space constant X, and an amplitude $E/2$. Its maximum stress is therefore

$$\epsilon''=\frac{w}{X}\frac{E}{2}=\frac{E}{2}\sqrt{\frac{c}{k}} \tag{53}$$

and thus the combined value of the stress near the terminal is

$$\epsilon=\epsilon'+\epsilon''=\left(\frac{1}{2}+\frac{1}{\pi}\right)E\sqrt{\frac{c}{k}}=0.818E\sqrt{\frac{c}{k}} \tag{54}$$

while its ratio to the internal stress has the fixed value

$$\frac{\epsilon}{\epsilon'}=1+\frac{\pi}{2}=2.57 \tag{55}$$

The duration of the terminal excess strain is only $\tau/2$, since we see from figure 14b and c that it lasts only half the time required by the wave head traveling over the junction.

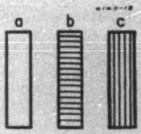


Figure 18. Cross sections of windings

(a)—Given area
(b)—Elements with small internal and large external capacitance
(c)—Elements with large internal and small external capacitance

In every case, we have to remember that E is the final voltage of the refracted wave within the winding which may approach twice the voltage E_1 of the incident jump wave, depending upon the surge impedances of line and winding, according to equation 51. The numerical factor of equation 54 may be in reality a little greater than 0.818, since in computing this factor we have taken in the denominators of equations 52 and 53 the maximum values of X and H occurring over the whole range of frequencies while, according to figure 9, the average values may be slightly smaller.

The conception of traveling waves has led us in equations 48, 52, and 54 to a fairly simple formulation for the magnitudes as well as the durations of the excess stresses of both the terminal and the interior elements. Large internal capacitance reduces the stresses but increases the duration, while small ground capacitance decreases the stresses without influencing the duration. Thus, for extensive self-protection of the winding every turn, every layer, and every coil should extend toward its neighbors with flat sides as wide as possible and toward ground with narrow sides as small as possible. If the winding area is oblong, as in figure 18*a*, it is evidently better to arrange the elements not short sided as in figure 18*b*, but long sided as in figure 18*c*. If by large internal capacitance k the head length H and the space constant X become much larger than the winding length a, the excess voltages decrease towards zero and the problem of the distribution of voltage thus becomes a static one. This is the aim of every surge-proof design of windings.

If an incident wave has not a discontinuous front, as in figure 10, but an already flattened head, its spectral distribution differs somewhat from figure 11; but as long as its front is steeper than the head H which enters the winding, only the higher frequencies above the critical frequency are influenced. The subcritical frequencies retain the same amplitudes as in figure 11 and travel undisturbed through the winding. Thus we recognize that for any incident wave shape those parts which are steeper than corresponds to the critical head length H are cut off and reflected at the terminal.

A capacitance C_p connected to the junction[13] of figure 10 smooths off every steep jump to a gradually ascending front with the well-known time constant

$$T=\frac{C_p}{\dfrac{1}{Z_1}+\dfrac{1}{Z}} \tag{56}$$

We can produce, therefore, protection against the terminal excess stresses e'' by choosing such capacitance that T becomes equal to or larger than the time τ of ascent. Then there remains only the traveling part of the wave with uniform impulse strain over the whole winding. By putting equation 56 equal to equation 48, we obtain as minimum amount of such protective capacitance

$$C_p=\tau\left(1+\frac{Z}{Z_1}\right)w\sqrt{ck} \tag{57}$$

We see that this value is a multiple of the apparent terminal capacitance C_∞ of equation 41.

Flattening of the Propagating Wave Front

The traveling velocity v of every partial oscillation within the winding depends upon its frequency ω, and thus composite waves in coils exhibit the phenomenon of dispersion. While long waves with low frequency, according to equation 26, run with a definite high velocity, the faster oscillating partial waves travel with slower speed and remain therefore behind the longer waves. Although it is possible to formulate mathematically an integral which takes account of the influence of this dispersion on the front shape, it seems impossible to give this a practically useful form. We will follow here, therefore, a geometrical presentation which supplies us, if not with a rigorous, at least with an approximate solution which is simple and easy to survey.

Table III. Flattening of the Wave Front H_x With Traveled Distance x

H_x/H_0	1	1.1	1.2	1.5	2	3	5	8	10	15
x/H_0	1	1.88	2.66	5.8	15	53	250	1,020	2,000	6,750

If the front of our uniformly ascending wave, according to figure 19, travels over a winding length x, it will become distorted, as only the fast-running waves with the speed v_0 carry forward the beginning of the ascending head. In a certain time t the first point of the wave front has moved over a distance

$$x=v_0t \tag{58}$$

During the same time all slower-moving oscillations, however, as for example, one of the frequency ω and the velocity v, have moved only over a distance x_ω which is, using equations 26 and 28,

$$x_\omega=vt=v_0t\sqrt{1-\left(\frac{\omega}{\nu}\right)^2} \tag{59}$$

Therefore the head in front of the distance x_ω will not contain any oscillation of frequency higher than ω. The length of this head, figure 19, is, using the last two equations,

$$H_x=x-x_\omega=x\left[1-\sqrt{1-\left(\frac{\omega}{\nu}\right)^2}\right] \tag{60}$$

Now the head length of our flattened wave is in principle determined by the highest frequency of the partial oscillations contained within the wave. For the critical frequency ν the co-ordination is given in equation 49, where the front H_0 corresponds to all oscillations up to the frequency ν, as shown in figures 11 and 12. But if the head of the wave is built up only by oscillations up to a frequency ω, as in figure 20, the head length is correspondingly

$$H_x=\frac{\pi v_0}{\omega}=\frac{\nu}{\omega}H_0 \tag{61}$$

where the last term expresses the ratio to the head length H_0 at the terminal of the winding. If we insert now the frequency ratio of equation 61 in equation 60, we obtain for the head length

$$H_x=x\left[1-\sqrt{1-\left(\frac{H_0}{H_x}\right)^2}\right] \tag{62}$$

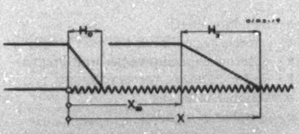


Figure 19. Further flattening of the propagating wave front along the winding

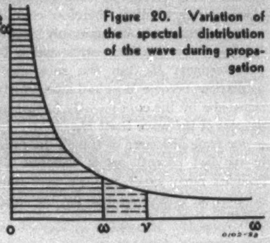


Figure 20. Variation of the spectral distribution of the wave during propagation

This relation contains only the original front length H_0, the distorted front length H_x, and the distance x through which the beginning of the wave has traveled over the winding. The equation is of the fourth degree in H_x and therefore difficult to solve. However, if we determine the distance x in units of the original front length H_0, we obtain the much simpler relation

$$\frac{x}{H_0}=\frac{\dfrac{H_x}{H_0}}{1-\sqrt{1-\left(\dfrac{H_0}{H_x}\right)^2}}$$

$$=\left(\frac{H_x}{H_0}\right)^2\left[1+\sqrt{1-\left(\frac{H_0}{H_x}\right)^2}\right] \tag{63}$$

We see that the original front length $H_x=H_0$ is related in this representation to the traveling distance $x=H_0$ of the fast-traveling waves at the beginning of the head. Table III shows some related values of head length H_x and traveled distance x and we see that the head length increases only very slowly with greater traveled distance. It needs to travel over a winding length of 15 original head lengths in order to flatten out the wave front to twice its original length; and for increasing the front length considerably it needs to travel over some hundred and even some thousand original head lengths.

The initial head length of waves in transformer or machine windings is usually between a large fraction and a small multiple of the winding length. The progressive flattening of the wave front is therefore moderate while traveling to and fro over only one phase length. If, however, we pursue the repeated travel of such a wave over the winding with numerous reflections at its ends, we see that the wave will become flatter and flatter. Free voltage or current oscillations of such windings therefore contain more or less sharp corners only during the first few oscillations corresponding to the steepness H_0, but the subsequent oscillations become more and more nearly sinusoidal by the continuously increasing flatness of the wave front H_x.

For large distances of travel, the ratio in the last parentheses of equation 63 becomes fairly small, and we may thus develop an approximation for the head length

$$\frac{H_x}{H_0}=\sqrt[3]{\frac{1}{2}\frac{x}{H_0}} \qquad (64)$$

which shows that the flattening effect increases as the cube root of the distance. Accordingly, the internal stresses of the insulation between turns and coils, which are inversely proportional to the front length, become approximately

$$\frac{\epsilon_x'}{\epsilon_0'}=\frac{H_0}{H_x}=\sqrt[3]{2\frac{H_0}{x}} \qquad (65)$$

Their exact distribution is shown in figure 21, where over the first head length H_0 there is added the exponential distribution of the reflected wave, according to equation 55. The total curve follows closely the numerous experimental results concerning the internal stresses in uniformly distributed windings of transformers and machines.

If one tries to determine experimentally the traveling speed of waves of any shape, it is necessary to be very cautious because, according to our conclusions, only the beginning of the front has a definite and constant velocity. The center or the end of a wave front, and similarly any other part of a more complex wave has, however, a decreasing speed along the winding, depending on the position of the point in question within the wave. Only sinusoidal waves have a well-defined velocity as determined by equation 26 and they are therefore particularly suited to determine the velocity of propagation.

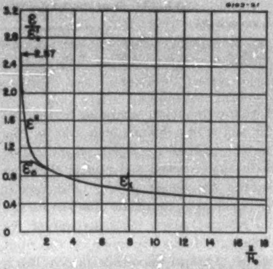


Figure 21. Distribution of internal stresses, caused by an impact, over the length of a winding

Summary

On the basis of a new investigation of the electromagnetic coupling of turns and coils, a differential equation of far-reaching validity is derived in which the inductive and capacitive effects of windings for rapid transients are related to the steady-state values. The spatial behavior of standing and traveling waves is considered, showing the existence of a critical frequency beyond which no harmonic oscillation can penetrate deeply into the winding. This causes a decrease with frequency of the traveling speed of waves which is verified by experiments on transformers.

By action of the critical frequency, an incident steep impact penetrates into the winding with a flattened front while the rest of the wave is reflected, causing a steeper terminal distribution of voltage. The reflection, at the neutral point of windings, of the traveling wave with its oscillating forerunners is derived and is found to be in accordance with many experimental results. Propagation through long uniform coils causes a continuous further flattening of the wave front.

List of Special Symbols

a —winding length
c —ground capacitance per unit length
k —internal capacitance per unit length
l —self-inductance per unit length
m—number of waves
n —number of element
r—reflected wave
v—wave velocity
w—wire length of element
H—head length
K—abbreviation
T—time constant
X—space constant
α—wave density
β—attenuation factor
ϵ—internal voltage
λ—self-inductance of single element
λ—wave length
μ—mutual inductance of elements
ν—critical frequency
τ—time of ascent

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Discussion

For discussion, see page 1257.

The Type CB Impregnated-Paper Cable

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SINCE the importance of low dielectric loss in impregnated-paper cable was first recognized,[1] attention has been constantly directed to securing lower power factor during the life of the cable. Although considerable progress has been made by close co-operation between cable manufacturer and supplier of the basic ingredients, instability of the insulation may result from:

(*a*). Abnormal increases in power factor and dielectric loss.

(*b*). Metallic soaps, due to the catalytic action between the impregnating compound and the metals within the cable.[2–8]

(*c*). "Wax" due to internal ionization.[9–11]

(*d*). Abnormal ionization forming configurations referred to as "tree" discharges.[12]

Some of the means advocated in the past for overcoming insulation instability were the use of:

1. An anti-oxidant within the impregnating compound.[13]
2. A tin coating on the conductor to retard catalysis.[14]
3. Rosin blended with mineral oil.[15]

It is the object of this paper to present a progress report on a new method for attacking the problem of insulation instability by the use of carbon-black protected (or type *CB*) cable.[16]

Theory

The electrical characteristics of an insulating oil can be improved by contact with activated carbon during filtration.[17] Carbon black of the adsorbent type, added to an insulating oil and the mixture agitated also improves the electrical properties by imparting a purifying effect and retarding the formation of deteriorating bodies.[18]

Although initially of high quality, the oil when placed under heat and electrical stress becomes subject to deterioration due to the fact that nearly all antioxidants are solid bodies which may be partially but not completely soluble in oil. When the insulation consists of paper impregnated with oil containing an antioxidant, the paper may act as a partial filter for these solid bodies.

To secure definite chemical and electrical stabilization, the means employed should be effective during the life of the cable and remain in its original position. One means for accomplishing this is to incorporate highly adsorbent carbon black into paper during the pulp stage and to place this carbon-black paper in the cable structure. Standard methods of cable processing are then followed. Inherently, the carbon-black paper tapes shield from the electric field all materials which they may adsorb. In addition, they tend to smooth out the configuration of stranded conductors and thereby reduce the maximum stress. This is the fundamental design of the type *CB* cable.

Experimental Verification of Theory

Test 1. Effect of Carbon-Black Paper on Oil and Impregnated Paper Stability

To determine the protective effect of the presence of carbon-black paper on impregnating oil, three beakers were filled with standard cable impregnating compound. Into one beaker was placed a small strip of carbon-black paper; into the second, standard insulating paper; while the third beaker contained only oil. In this condition, the three beakers were placed in an aging oven and exposed to the oven atmosphere for ten days at 85 degrees centigrade.

Table I. Effect of Presence of Standard Versus Carbon-Black Paper on Stability of Oil and Impregnated Paper

Aged Ten Days at 85 Degrees Centigrade

Aging Condition	Power Factor at 85 Deg C 50 Volts Per Mil (Per Cent)	Insulation Resistivity at 85 Deg C (Megohms $\times 10^6$ Cm)
Oil initially	0.2198	127.0
Oil aged alone	0.6407	29.7
Oil aged with standard cable paper	0.4690	45.8
Oil aged with carbon-black paper	0.3930	50.0
Impregnated paper before aging	0.25	10,000
Impregnated paper after aging alone	0.715	2,270
Impregnated paper after aging in presence of carbon-black paper	0.664	4,600

Similar tests were conducted with strips of five-mil impregnated wood-pulp paper. To one of the beakers was also added a small strip of five-mil carbon-black paper.

The results are given in table I. The data indicate that the presence of carbon-black paper tended to retard the normal rate of deterioration of oil and impregnated paper.

Test 2. Effect of Carbon-Black Paper With Stranded Conductors

Four cable models similar to those used by Whitehead[19] were made with conductors of highly-polished, number 4/0 American wire gauge 19-strand bare copper cables having 14 layers of 5-mil wood-pulp paper. On two models the first two tapes were of five-mil carbon-black paper, thereby these two models were deprived of 10-mils of insulation. However, all models were considered to have 70 mils of insulation for stress and other computations.

Set *A*, consisting of a standard and a carbon-black paper model was aged for ten days at 100 degrees centigrade and 100 volts per mil. Set *B* was aged for 20 days at 85 degrees centigrade to more nearly approach maximum operating temperatures. (For further details on the construction of models and technique of measurements, refer to the appendix.)

Test results are given in table II. The data indicate pronounced stabilizing effect of the two carbon-black tapes next to the conductor. After the severe aging at 100 degrees centigrade (set *A*), the power factor of the *CB* model is only half that of the standard while the insulation resistance is four times as high.

The same stabilizing tendency is evident with 20 days aging at 85 degrees centigrade (set *B*). The power factor of the *CB* model is less than one-third while the insulation resistance is approximately twice that of the standard.

The models were not carried to failure, to permit tape-by-tape examination. Figure 1 illustrates paper tapes taken from set *A*. In the standard model, pronounced stress marks are visible in all of the tapes up to the tenth from the conductor. In the *CB* model, none of the tapes shows overstressing. Tapes from the standard model also show marks parallel to the horizontal axis of the tapes,

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[1] For all numbered references, see list at end of paper.

which correspond to overstressing in the butt spaces between adjacent convolutions.

Tapes from set *B*, showed no stress markings on either model. On the conductor of the standard model there were black helical lines due to overstressing of the oil in the butt spaces, as shown in figure 2.

Test 3. Effect of Carbon-Black Paper Versus Tinning of Conductor

A study was made of the relative protective effects in cable models of carbon-black paper versus conductor tinning. Cable models were prepared with number 4/0 American wire gauge, stranded conductors. The standard model had each strand double tinned; the *CB* model had bare strands.

Both models were aged for ten days at 125 degrees centigrade and 125 volts per mil. The aging conditions were made severe to accelerate the formation of deteriorating bodies due to catalysis, oxidation, electronic bombardment, or other effects.

Figure 3 demonstrates the superiority of the carbon-black tapes when compared to double tinning of the conductor.

A study of the radial power factor after aging was made since this method has been proposed for determining distribution of deterioration through the insulation.[20, 21] The results in figure 4 indicate that double tinning of the conductor afforded very little protection. These results substantiate findings reported in the literature.[22]

Table II. Aging Characteristics of Standard and CB Models With Stranded Conductors

Aging (Days)	Temperature (Deg C)	Standard Model: Power Factor (Per Cent) 50 Volts Per Mil	Standard Model: Power Factor (Per Cent) 100 Volts Per Mil	Standard Model: Insulation Resistance (Megohms ×10⁵ Cm)	CB Model: Power Factor (Per Cent) 50 Volts Per Mil	CB Model: Power Factor (Per Cent) 100 Volts Per Mil	CB Model: Insulation Resistance (Megohms ×10⁵ Cm)
Set A—Aged 10 days at 100 degrees centigrade and 100 volts per mil							
Initial	22.0	0.369	0.387	6,000	0.307	0.316	6,721
	100.8	0.600	0.715	3,120	0.549	0.587	4,270
After 5 days	14.0	1.501	1.709	310	0.480	0.522	5,110
	100.0	23.800	21.200	20	8.690	8.530	96
After 10 days	15.5	1.679	1.866	218	0.480	0.536	4,717
	85.0	15.310	14.890	49	8.640	8.370	101
	100.0	23.700	21.300	24	10.500	10.470	88
Set B—Aged 20 days at 85 degrees centigrade and 100 volts per mil							
Initial	23.7	0.283	0.284	7,800	0.283	0.291	6,975
	84.8	0.589	0.561	4,000	0.536	0.502	4,380
After 10 days	21.5	0.290	0.291	6,875	0.293	0.298	6,800
	85.5	0.766	0.688	2,050	0.524	0.484	4,171
After 20 days	20.8	0.299	0.301	6,640	0.298	0.298	6,700
	85.5	1.309	1.338	352	0.600	0.525	640
	100.0	2.210	2.211	171	0.834	0.703	310

Test 4. Effect of Carbon-Black Paper With Smooth Cylindrical Conductors

Since all of the preceding tests had been conducted on models made with stranded conductors, it was considered desirable to conduct similar tests on highly-polished, bare solid copper conductors. This serves to eliminate strand stress considerations and also reduces copper-oil interface area.

Accordingly, standard and *CB* models were prepared with highly-polished number 4/0 American wire gauge, bare solid conductors. These models were subjected to aging at 100 degrees centigrade and 100 volts per mil for 41 days. The results are plotted in figure 5.

Figure 5 shows that at 100 degrees centigrade there is a definite superiority in the *CB* construction. At room temperature the power factor remained about the same.

Test 5. Effect of Adsorptive Index on the Protective Powers of Carbon-Black Tapes

Colloidal carbon, or "carbon black" is the result of impinging a natural-gas flame against a relatively cool surface.[23] The colloidal size can be varied over a considerable range by control of several factors such as the nature of the gas, size and shape of flame, distance from impinging surface, etc. Changing the colloidal size and surface complex of the carbon-black particle will also change a number of its properties such as, conductivity, adsorptive index, and pH values. As an illustration, the pH values of carbon black can be varied from 2 to 11.[24]

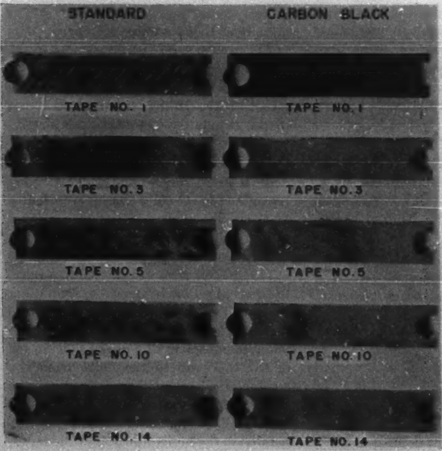


Figure 1. Ionization marks on tapes after aging

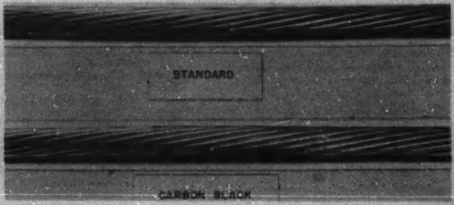
Stranded conductors

Standard versus *CB* models

Figure 2. Ionization marks on conductors after aging

Stranded conductors

Standard versus *CB* models



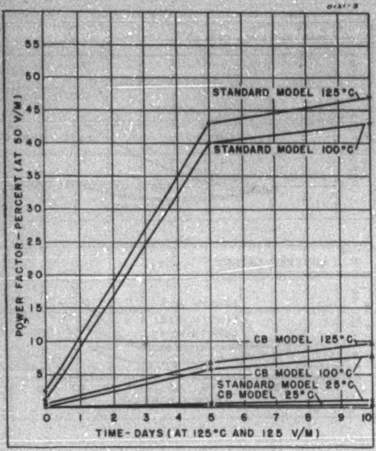


Figure 3. Power factor—aging characteristics

Stranded conductors—tinned versus *CB* protected

Standard versus *CB* models

Table III. Construction Details of Standard and CB Cables

Single Conductor, 350,000 Circular Mils, 19/64 Inch Paper, 7/64 Inch Lead

		Number of Carbon-Black Tapes**	
Length No.	**Shielding Tape***	**Adjacent to Conductor**	**Adjacent to Sheath**
1	None	None	None
2	None	3	None
3	None	None	2
4	None	3	2
5	Copper tape	None	None
6	Copper tape	3	None
7	Copper tape	3	1

* The shielding tape consisted of a 0.003-inch bare perforated copper tape intercalated with a 0.005-inch high-density manila rope paper tape on lengths numbers 1 to 6 and with a 0.005-inch carbon-black paper tape on length number 7.

** The over-all diameter of each insulated conductor was the same. Each carbon-black paper tape therefore displaced a similar number of insulating tapes.

To determine the relative effectiveness of various grades, carbon-black papers were produced with a range of adsorption index and particle size. Models were made having these different types of carbon-black tapes applied in two layers directly over the conductors. Model *E*-1 was made with standard insulating tapes only. The models were aged for ten days at 100 degrees centigrade and 100 volts per mil.

Figure 6 represents the before and after power factor values of the various models plotted against the adsorption indices of the carbon blacks used. This figure not only brings out the extent of the protection afforded by the various carbon-black tapes in contrast with the unprotected *E*-1 model, but also illustrates the remarkable stabilizing effect afforded by the higher-adsorptive-index carbon-black tapes used in models *E*-2 and *E*-5.

Reduction of Theory of Practice

Test 6. Effect of Positioning of Carbon-Black Paper on Dielectric Properties of Single-Conductor Cable

A study was made of the effect on the dielectric properties of a series of cables containing carbon black at different portions of the cable structure. Table III shows the construction details. Table IV shows the results of the high-voltage time tests.

Table IV indicates that the constructions which had carbon-black tapes adjacent to the conductor, namely, lengths numbers 2, 4, 6, and 7, gave the highest voltage life. Length number 4, which had no shielding tape but had three *CB* tapes adjacent to the conductor and two adjacent to the sheath, yielded the highest equivalent one-hour dielectric strength ("life") of all the constructions tested. This is of interest when it is considered that it contained five carbon-black tapes which displaced five insulating tapes, so that compared to lengths numbers 1 and 5, it had only 91 per cent effective insulation thickness.

Figure 7 shows the initial power factor-temperature characteristics. Figure 8 gives the radial power-factor measurements made on samples from these cables *after* they had been subjected to the high-voltage time tests.

In figure 7, it is noted that the cables which had carbon-black tapes adjacent to the conductor show the lowest power factor at elevated temperatures.

In figure 8, a steep rise in power factor is noted adjacent to the *conductor* of lengths numbers 1, 3, and 5, which had no carbon-black tapes applied over the con-

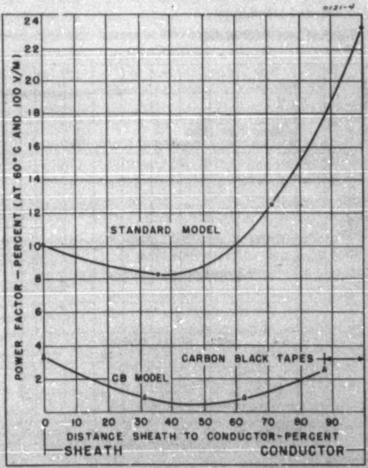


Figure 4. Radial power factor after aging

Stranded conductors—tinned versus *CB* protected

Standard versus *CB* models

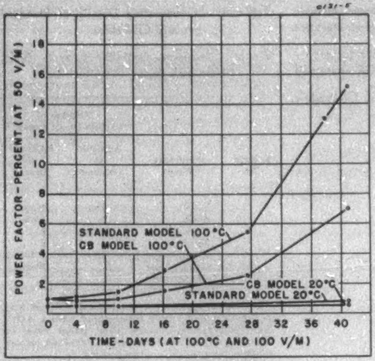


Figure 5. Power factor—aging characteristics

Smooth cylindrical conductors

Standard versus *CB* models

ductor. Lengths numbers 2, 4 and 7 showed practically no rise. Adjacent to the *sheath* where carbon-black tapes were present, no rise in radial power factor is noted. Lengths numbers 4 and 7 with the maximum number of carbon-black tapes exhibited the most uniform radial power-factor characteristics.

Test 7. Effect of Carbon-Black Paper in Three-Conductor 28-Kv Shielded Cable

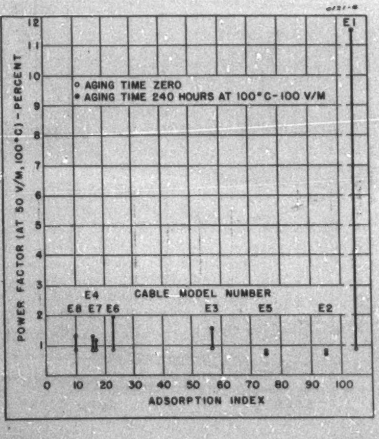
To study the effect of carbon-black paper on the electrical characteristics of three-conductor cable, two lengths of three-conductor 350,000-circular-mil sector shielded-type cable were made, each conductor insulated with 20/64-inch total wall of paper. Length *A* was of standard construction. Length *B* had the three tapes adjacent to each conductor replaced by carbon-black tapes. The intercalation of the shielding and binder tapes of length *B* was carbon-black paper; length *A*, manila-rope paper. Lengths *A* and *B* were impregnated in the same tank after which they were covered with an 8.5/64-inch wall of lead.

Figure 9 shows typical power factor versus temperature and ionization curves.

Figure 6. Power factor aging versus absorption index

Stranded conductors

Standard versus *CB* models



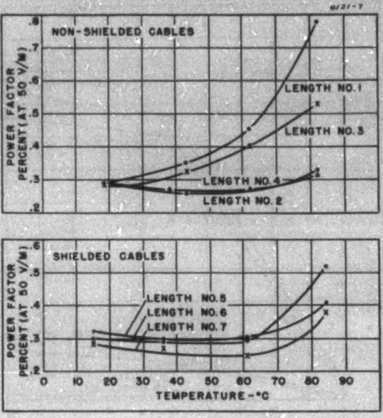


Figure 7. Power factor versus temperature—unaged

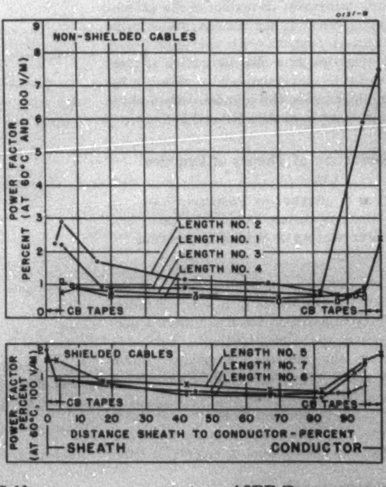
Single conductor, shielded and non-shielded

Standard versus *CB* cables

Figure 8. Radial power factor—aged

Single conductor, shielded and non-shielded

Standard versus *CB* cables



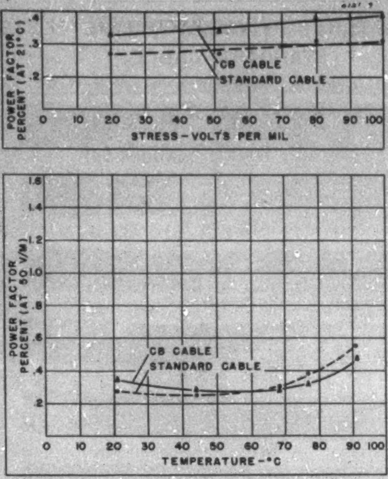


Figure 9. Ionization and power factor characteristics—unaged

Three-conductor, 350,000-circular-mil, sector, 28-kv shielded

Standard versus *CB* cables

Both constructions have excellent initial power factor values.

Figure 10 shows the appearance of some of the tapes removed from both cables after the high-voltage time test of 350 volts per mil applied until breakdown. The standard cable failed under the lead after 155 minutes and the *CB* cable had a terminal failure after 490 minutes.

Figure 11 shows radial power-factor measurements on the cables *after* they had been subjected to the high-voltage time test. In the standard cable, a rise in power factor is noted adjacent to the conductor. In spite of the considerably longer time at which the *CB* cable was subjected to the 350 volts per mil stress (490 versus 155 minutes), the tapes taken from the same region show uniformly low values with no upward trend. The reasons for these results are demonstrated in figure 10.

Test 8. Cyclic Aging Data on Three-Conductor, 28-Kv Shielded Cables

To further study the effectiveness of carbon-black tapes in maintaining insulation stability, two 45-foot lengths of three-conductor cable described in test 7 were subjected to a cyclic aging test.

After the ends were fitted with oiltight terminals, the two lengths were laid in separate wooden troughs supported in a horizontal position about three feet above a concrete floor. Combination vacuum and pressure gauges were attached to the respective sheaths at the center and at each end below the terminal. About one foot of each cable protruded from the trough at each end in a drooping position. The ends of each trough were sealed by tightly packing with glass wool. Thermocouples were attached at five points on the sheath of each cable and connected to a Leeds and Northrup Micromax temperature-recording instrument. The conductors of each cable were connected in series so that the loading current would be the same for both cables. Power-factor readings were made on each cable in three-minute intervals by a Leeds and Northrup recording power-factor bridge having a double scale with ranges in power factor of 0 to 5 per cent and 0 to 20 per cent. The shift from the low to the high scale is automatic.

Table IV. High-Voltage Time Tests

Single-Conductor 27-Kv Standard and *CB* Cables

Length Number	Time (Minutes) at Stresses of: 350 Volts Per Mil	400 Volts Per Mil	450 Volts Per Mil	500 Volts Per Mil	Equivalent** One-Hour "Life" (Volts Per Mil)
1	174				418
2	570	8			512
3	222				434
4	570	914	60	100	697
5	103				383*
6	570	690			633
7	570	660			629

* Tape found to be wrinkled at point of failure.

** Based upon the sixth-power law and total paper thickness.

Table V. Ionization During Cyclic-Aging Test

Three-Conductor 28-Kv Type *CB* Shielded Cable

Day From Start of Test	Power Factor (Per Cent) 100 Volts Per Mil	20 Volts Per Mil	Ionization Factor (Per Cent)
318	0.55	0.55	0
324	0.50	0.50	0
332	0.55	0.55	0
338	0.55	0.55	0
346	1.20	0.57	0.63
352	1.15	0.57	0.58
359	1.20	0.53	0.67
366	1.05	0.52	0.53
373	1.10	0.75	0.35
380	1.00	0.70	0.30
387	1.15	0.83	0.32
394	0.80	0.65	0.15
401	0.95	0.77	0.18

The procedure consisted in the continuous application of 31.3 kv between each conductor and sheath and a simultaneous current loading which was maintained for 8 hours during each 24-hour cycle. The test voltage corresponded to an average stress of 100 volts per mil on the basis of total paper thickness.

The test results are given in figure 12. The cables under test are shown in figure 13 and the recording instruments in figure 14.

The cyclic-loading test was started on February 20, 1939. The standard cable failed during the 202d day which occurred on Saturday, September 9, 1939. The insert in the upper right-hand corner of figure 12, shows the rise in power factor and sheath temperature just prior to failure as recorded by the recording instruments. At the time of writing this paper, the type *CB* cable was still on test.

The published literature on cyclic-loading tests of cable indicates that the maximum sheath temperature generally used was 65 degrees centigrade, with many of the tests between 40 and 50 degrees centigrade. It is, therefore, apparent that, since the average sheath temperature was about 75 degrees centigrade with some

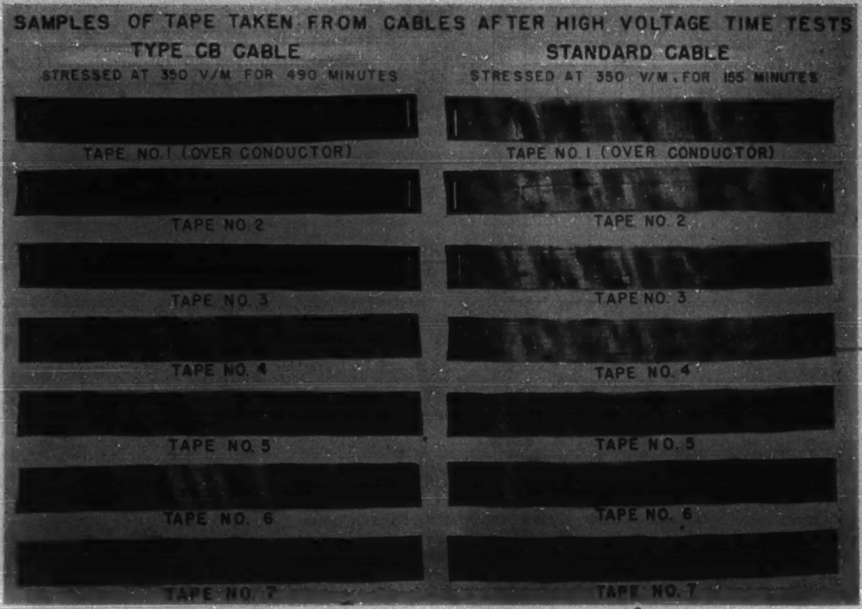

SAMPLES OF TAPE TAKEN FROM CABLES AFTER HIGH VOLTAGE TIME TESTS
TYPE CB CABLE
STRESSED AT 350 V/M FOR 490 MINUTES
STANDARD CABLE
STRESSED AT 350 V/M FOR 165 MINUTES
TAPE NO. 1 (OVER CONDUCTOR)
TAPE NO. 2
TAPE NO. 3
TAPE NO. 4
TAPE NO. 5
TAPE NO. 6
TAPE NO. 7

Figure 10. Ionization marks on tapes after aging

Three-conductor, 350,000-circular-mil, sector, 28-kv,

Shielded *CB* versus standard cable

days as high as 90 degrees centigrade, this cyclic-aging-test was very severe. A conservative estimate would place the average conductor temperature of both cables at about 90 degrees, with high points of about 105 degrees during the days when the sheath temperature was between 85 and 90 degrees centigrade.

In general, correlation can be noticed between the rise and dip of the sheath temperature, pressure, and power factor curves of the standard cable during each cycle. The power factor of the type *CB* cable, both before and after heating, is practically constant up to the 215th day and from then on, there is a period in which the sheath temperatures were maintained at their highest values (between 85 and 90 degrees centigrade). It is at the beginning of this period that the power-factor values seem to follow the rise and dip of the sheath temperatures. However, the "before heating" power-factor values remain substantially constant until the 342d day when the values shifted to a higher level.

Table V shows ionization factors of the type *CB* cable from the 318th day at the end of the cooling cycle.

Some ionization was noted from the 346th day and by examining the corresponding period in figure 12, it will be seen that this coincides with the shift in power factor. From the 359th day, the ionization factor has been continuously decreasing thus indicating that the stability has not been impaired.

The cyclic aging of the type *CB* cable will be continued until evidences of instability have been detected or the test has to be discontinued.

Test 9. Data on Commercial Order of Three-Conductor 15-Kv Type CB Shielded Cable

An overlength of 250 feet was added to an order of 10,000 feet of three-conductor, 600,000-circular-mil sector type *CB* shielded cable for 15-kv grounded neutral. The insulation on each conductor included three carbon-black tapes adjacent to the conductor and 2 adjacent to the shielding tape, the carbon-black tapes being considered part of the total required thickness of 203 mils. The cable was covered with a 125-mil wall of lead.

Figure 15 contains the results of power factor and dielectric loss determinations.

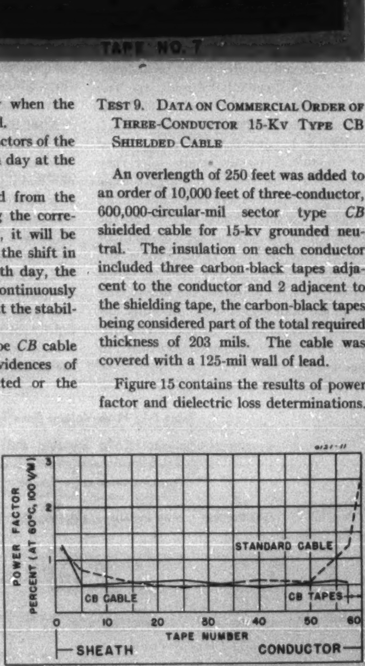


Figure 11. Radial power factor—aged

Three-conductor, 350,000-circular-mil, 28-kv, shielded

Standard versus *CB* cable

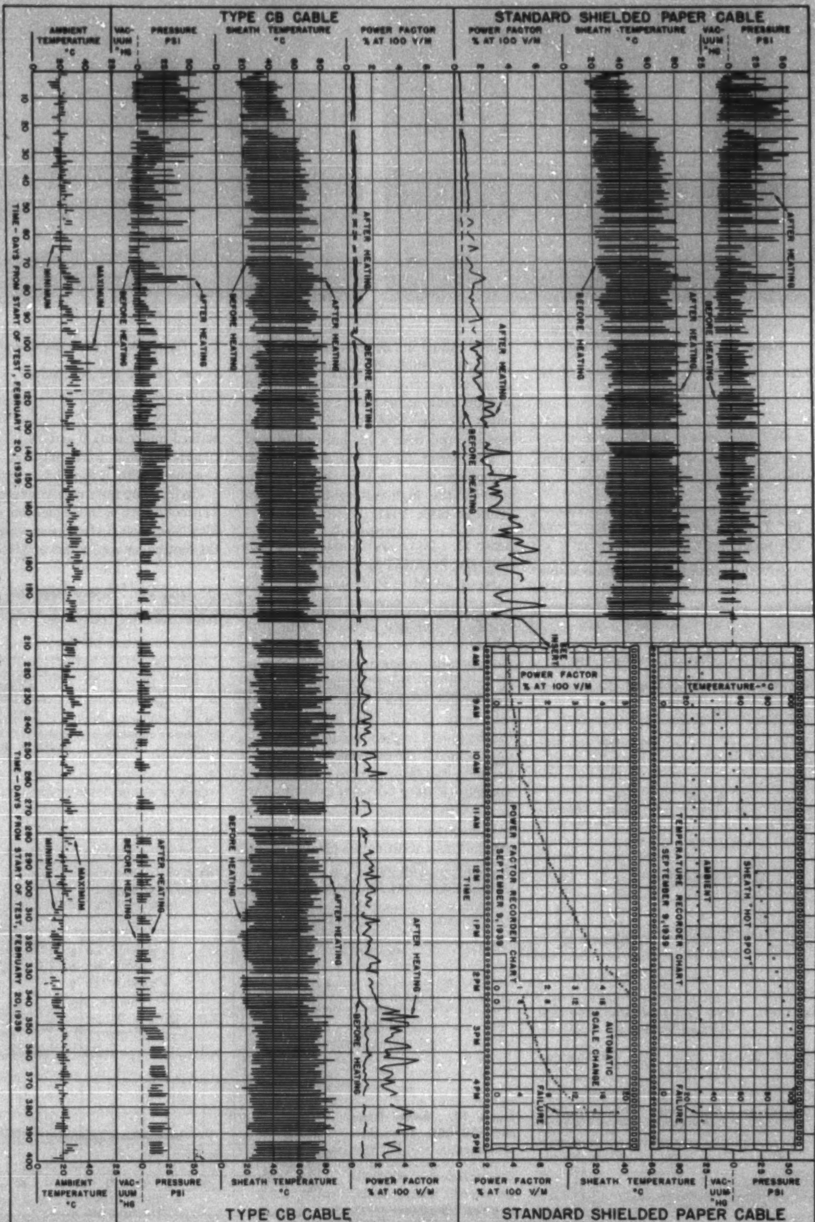


Figure 12. Cyclic aging characteristics

Three-conductor, 350,000-circular-mil, 28-kv, shielded

Standard versus CB cable

Figure 13. Cyclic aging test setup

Three-conductor, 350,000-circular-mil, 28-kv, shielded
Standard versus *CB* cable

Figure 14. Cyclic-aging laboratory

Power factor and temperature recording instruments

Table VI shows the results of the high-voltage time and cold bend tests.

Test 10. Data on Commercial Order of Three-Conductor 35-Kv Type CB Shielded Cable

An overlength of 80 feet for Association of Edison Illuminating Companies sample tests was added to a commercial order of three-conductor 350,000-circular-mil sector type *CB* shielded cable rated at 35-kv grounded neutral. The insulation design was the same as for test 9 except the total paper thickness was 375 mils. The cable was covered with a 125-mil wall of lead.

The power factor and dielectric loss values are given in table VII. The results of the high-voltage time test are given in table VIII.

Table VI. Dielectric-Strength Tests

Three-Conductor 15-Kv Type *CB* Shielded Cable

Sample Number	Nature of Test	Equivalent One-Hour "Life"* (Volts Per Mil) Based on: Total Paper Thickness	Effective Insulation
1	High-voltage time	488	539
4		481+	....531+
7		479	530
Average		483	533
3	Cold bend	511	569
6		456	505
9		479	529
Average		482	534

* Based on sixth-power law.

Table VII. Power Factor and Dielectric Loss at Rated Voltage

Three-Conductor 35-Kv Type *CB* Shielded Cable

Temperature (Deg C)	Power Factor (Per Cent)	Dielectric Loss (Watts Per Foot)
22.3	0.33	0.15
81.1	0.29	0.13
58.7	0.28	0.13
41.5	0.30	0.13
22.4	0.32	0.14

Test 11. Data on Three-Conductor 13-Kv Type CB Shielded Cable With 130 Mils of Insulation

The high dielectric strength and remarkable stability obtained with type *CB* cable have indicated a possibility of a reduction in present insulation thickness. A length of three-conductor 500,000-circular-mil sector type *CB* shielded cable with 130-mil total paper thickness was furnished to the Commonwealth Edison Company for experimental purposes. From the results of tests on this and other thicknesses, data are being secured to determine the proper thickness for 13-kv grounded-neutral operation. The standard thickness of insulation for this voltage is 172 mils. The number of carbon-black tapes applied was the same as that described under tests 9 and 10. The data reported below were obtained in the Commonwealth Edison laboratories.

Cyclic aging tests were conducted on a 111-foot length of this cable from October 9 to November 6, 1939, and consisted in the continuous application of 24 kv, three-

Table VIII. High-Voltage Time Test

Three-Conductor 35-Kv Type *CB* Shielded Cable

Test Voltage to Ground (Kv)	Time of Application (Minutes)	Equivalent One-Hour "Life"* (Volts Per Mil) Based on: Total Paper Thickness	Effective Insulation
75.0	360		
112.5	180		
131.2	180		
150.0	........251**	530	555
Terminal rebuilt			
150.0	243	578	608

*Based on sixth-power law.

**Failure in terminal.

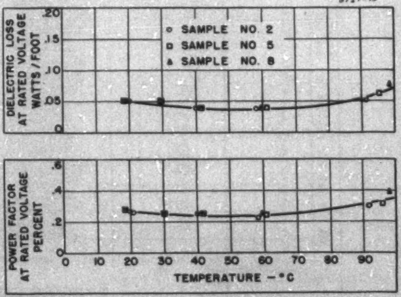


Figure 15. Power factor and dielectric loss characteristics

Three-conductor, 600,000-circular-mil, sector, 15-kv, shielded *CB* cable

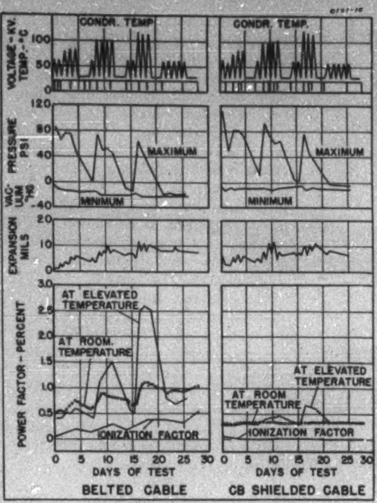
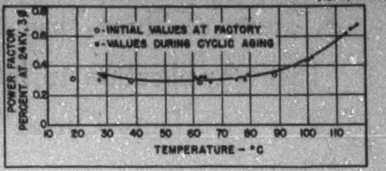


Figure 16 (left). Cyclic-aging characteristics

Three-conductor, 500,000-circular-mil, 13-kv standard belted versus *CB* shielded cables

Figure 17 (right). Power factor versus temperature—aged

Three-conductor, 500,000 - circular-mil, 13-kv, *CB* cable, shielded



phase, for 28 days with superimposed load cycles. Figure 16 shows the data obtained as reported by Herman Halperin. For comparison, similar data obtained on standard belted cable are reproduced from his recent AIEE paper.[25]

The power factor and ionization curves given in figure 16 for the belted cable, both before and after each heating cycle, are typical of excellent quality of cable, especially when consideration is given to the high temperatures to which this cable was subjected. However, when these data are compared to those on the type *CB* shielded cable, the superiority of the latter is readily apparent.

Figure 17 represents the composite power factor-temperature relation taken at all the temperatures during the cyclic aging test including the initial values. This indicates remarkable stability.

Future Work

This paper is a progress report. Additional work is under way to apply the fundamental principle of carbon-black protection to the solution of insulation stability problems of other types of insulated conductors and electrical equipment.

Conclusions

The use of carbon-black paper offers a simple and unique practical means for increasing the useful sphere of solid-type impregnated-paper-insulated cable.

Appendix

Model Accelerated Test Unit. A standardized cable model 16 inches in length having a number 4/0 American wire gauge conductor and a 60 to 75-mil wall thickness of insulation is used as a basis of all constructions. Conductor shape is optionally round or sector and either stranded or solid. Insulation wall is lathe wound, butt lapped with 60/40 overlay. Seven inches precalculated sloped end reinforcements permit maximum operating voltage of 30 kv. The measuring electrode is 5 inches in length provided with $1^{1}/_{2}$-inch overlapping guards.

Treating and Testing Equipment. Monel-metal storage and degasser tanks equipped with vacuum pump, heaters, resistance thermometers, and atomizing device are used for properly storing, degassing, and dehydrating the impregnant. The treated oil is piped to four horizontal monel-metal tanks in which the dry models are mounted. Once the models are installed and sealed in these test tanks all operations of drying, impregnating, and testing are controlled by external means.

Dielectric Measurements. Power-factor and dielectric-constant measurements are made using a Leeds and Northrup modified Schering bridge: power-factor accuracy, 0.00001 ± 0.000005; power-factor sensitivity, 0.000005; capacitance accuracy, ±0.25 per cent. Insulation resistance measurements are made with a guarded high-sensitivity d-c galvanometer circuit readable to 5×10^{6} megohms.

Tape-by-Tape Tests. Tape power-factor measurements are made at 100 volts per mil stress and at 60 degrees centigrade using a balance-pressure type cell with rigid six by three-eighths inch guarded measuring electrode.

Cyclic Aging Measurements. Measuring equipment in the cyclic-aging laboratory is built around the Leeds and Northrup automatic power-factor recorder having two scales: 0 to 5 per cent and 0 to 20 per cent. Low-scale accuracy is ±0.03 per cent with sensitivity at 0.01 per cent. High-scale accuracy is ±0.15 per cent and sensitivity, 0.1 per cent. A Leeds and Northrup 16-point recording thermocouple potentiometer records sheath temperatures to a maximum of 100 degrees centigrade.

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Discussion

For discussion, see page 1235.

A Study of Short-Time Ratings and Their Application to Intermittent Duty Cycles

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VARIOUS American standards for electrical apparatus provide, in addition to continuous ratings, certain short-time ratings, based on 5-, 15-, 30-, and 60-minute runs with the apparatus starting cold. Usually these short-time runs do not correspond to conditions met in service; for example, a motor very seldom starts cold, operates for a limited period of time, and then remains at rest until it has cooled to ambient temperature. Often these short-time ratings are applied to intermittent loads, such as shown in figure 1, in which T represents the total time for each cycle of operation; $T_r = kT$, the running time; and $T_i = (1-k)T$, the time the motor stands still. Since the short-time ratings do not indicate the type of operating cycle with varying values of T, k, and operating current I, for which these ratings are suitable, suggestions have been made from time to time to establish standard intermittent load ratings, especially for motors. Efforts along these lines have so far not led to any definite action because it is difficult to find practical standard provisions applicable to the many variations encountered in operating cycles. Furthermore, the present standard short-time ratings are so simple and convenient for acceptance-test ratings that there naturally is considerable hesitancy in adopting a complicated system of intermittent ratings. This raises the question of the possibility of devising various reasonably simple means for applying short-time ratings to intermittent load conditions without resorting to time-consuming temperature calculations, for instance, those used in the application of railway motors. In many industrial applications the cost of these calculations would be entirely out of proportion to the total cost of the motor and any slight saving in the size and cost of the motor resulting therefrom.

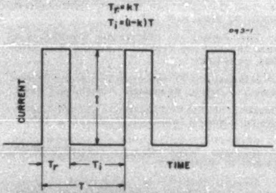


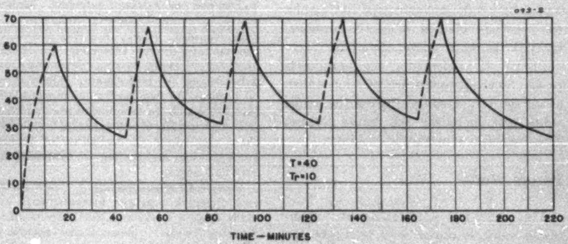
Figure 1. During the operating cycle of total time, T, the running period is $T_r = kT$ and the idle period is $T_i = (1-k)T$

Therefore, this paper discusses a method for applying short-time ratings to intermittent service. It will also very briefly touch upon another application problem, namely, the co-ordination of the control and similar auxiliary apparatus and the wiring for short-time rated apparatus used for intermittent loads. To arrive at a solution of the problem, a few limiting values, somewhat arbitrary but at the same time reasonably logical, will be selected. The first is that the rms current I during the operating period of the cycle should be substantially that for the short-time rating, or at least not exceed it appreciably. With this assumption, the variables are reduced to the total time T of the cycle and the ratio $k = T_r/T$. Also for the purpose of this analysis, applications are divided arbitrarily into three main types of duty cycle.

Time-Temperature Curves

Figure 2 represents an attempt to determine by test an operating cycle giving the same maximum temperature rise as that specified for the short-time rating. A motor susceptible to various ratings between $^3/_4$ and 1.1 horsepower was tested to give 60 degrees centigrade temperature rise by resistance for a 15-minute period. Subsequently the motor was operated at the same current with an idle period of 30 minutes and a running period of 10 minutes. During subsequent cycles the maximum temperature rise gradually increases to about 70 degrees centigrade. The cycle must be changed

Figure 2. The duty cycle for this temperature test at the 15-minute rating current consisted of a 15-minute run followed by a cycle with T = 40 and k = 0.25. The heating sections of the curve are interpolated



Paper 40-93, recommended by the AIEE committees on electrical machinery and standards, and presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940. Manuscript submitted April 23, 1940; made available for preprinting May 20, 1940.

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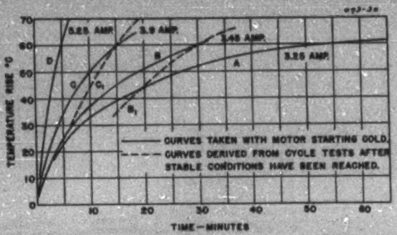


Figure 3a. The heating curves of a motor for 5, 15, 30, and 60-minute ratings starting cold are shown. When the motor parts are heated to stable operating conditions, the dotted curves apply for the same currents

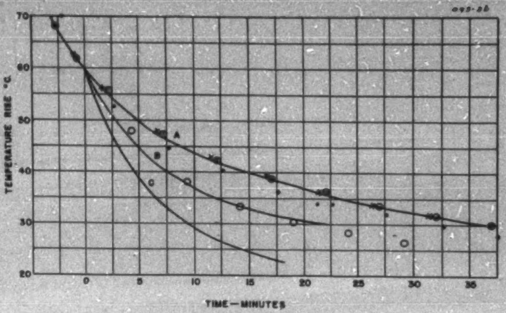


Figure 3b. The cooling curves depend on the time from a cold start, as well, the rate of cooling depending on the running time and on temperature conditions throughout the motor

Crosses—Following cyclic test at 4.3 amperes lasting 95 minutes

⊙—Following continuous test at 3.4 amperes lasting 65 minutes

Dots—Following cyclic test at 3.9 amperes lasting 175 minutes

○—Following continuous test at 3.9 amperes lasting 15 minutes

A—Average cooling curve for 15-minute and longer ratings after conditions stabilize

B—Following cyclic test at 5.25 amperes lasting 85 minutes

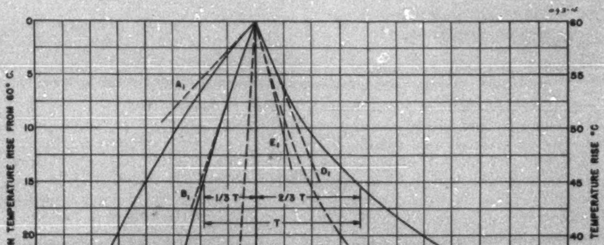
C—Following continuous test at 5.25 amperes lasting 5 minutes

if the limiting temperature rise of 60 degrees is not to be exceeded. An attempt to obtain data for a great many different load conditions by such a "cut and try" method of testing would be very costly and time-consuming and for a comprehensive study, various derivations should be made from suitable time-temperature curves.

Figure 3*a* shows time-temperature curves obtained by test for 5-, 15-, 30 , and 60-minute ratings based on a temperature rise of 60 degrees centigrade by resistance. This temperature rise was chosen because, with an ambient temperature of 40 degrees centigrade and the usual hot-spot allowance, it gives a hot-spot temperature of 105 degrees centigrade, which is the generally adopted limit for class *A* insulation. However, the analytical methods apply equally well to any temperature rise selected. Although tests are required to determine the current values for the various short-time ratings, the full-line curves shown are not suitable for the study of sustained cyclic loads, because, after the short-time run started with cold motor parts, many of the parts (the frame for example) are still relatively cool and absorb considerable heat. This naturally tends to reduce the rate of rise of the winding temperature. To obtain suitable curves, a number of tests as shown in figure 2 must be made with varying time and current values. Values of the rate of temperature rise must be taken from the cycles toward the end of a test when fairly stable temperature change rates in all parts of the machine have been established. The dotted curves B_1 and C_1 in figure 3*a* have been determined in this manner for the currents corresponding to the 30- and 15-minute rating respectively. Figure 3*b* shows a number of time-temperature cooling curves taken after the various runs indicated and with the motor at rest. Here again it is necessary to select for our study a curve taken after fairly stable conditions prevail.

For greater convenience, representative time-temperature curves were replotted in figure 4 to start from the common point of 60 degrees centigrade to show the temperature changes from that point in terms of the time in minutes. If we now wish to find the heating cycle for an "on" period of $T_r = (^1/_3)T$ and an "off" period of $T_f = (^2/_3)T$ at the 15-minute rating current, we locate on curves *B* and *D* of figure 4 a temperature which shows this particular ratio of time. The zigzag curve *A* in figure 5 has been derived in this manner. It is seen that the temperature rise during each load cycle reaches a maximum of 60 degrees, which is the established temperature rise for the 15-minute rating. This, then, represents an ideal application for the motor and corresponds to a total time *T* of 14.55 minutes for each cycle with one-third time on and two-thirds time

Figure 4. The curves of figure 3 have been replotted in terms of the variation from a temperature rise of 60 degrees centigrade



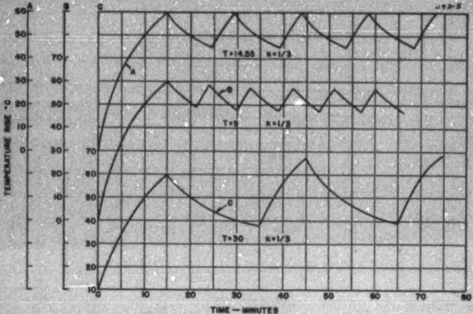


Figure 5. The time-temperature curves for different running cycles may be derived from the curves of figure 4

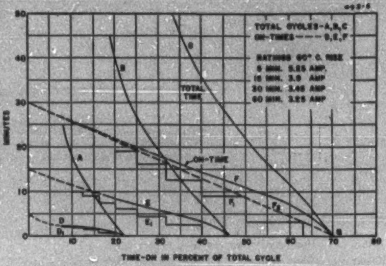


Figure 6. Curves derived from figure 4 show the limiting values of the cycle T and the "on" time T_r for different short-time rating currents to produce a rise of 60 degrees centigrade or less

off. Curve *B* in figure 5 was constructed by using portions of the curves in figure 4, with *T*=nine minutes for each cycle and with the same ratio of time on and time off as before. It will be seen that after uniform conditions have been established, the maximum temperature rise is only 57 degrees, which, although not quite utilizing the full capacity of the motor, is a satisfactory application. Curve *C* of figure 5 is derived for a total time of 30 minutes per cycle, again for the same ratio of the on and off periods; in this case a maximum temperature of 69 degrees is obtained. It follows from these considerations that, for any ratio of on and off time, there is a maximum time per cycle which leads to the limiting temperature rise of 60 degrees, while for longer periods the temperature limit will be exceeded. The plotting of curves as shown in figure 5 is of advantage for illustrative purposes but is not necessary for determining the limiting values.

Class I Applications

For many intermittent loads that can be handled advantageously with motors of short-time ratings, the running time for each cycle is not only relatively short but also definitely limited to a maximum set by the driven device. Examples of this are: hoists, cranes, elevators, door-openers, etc., with which the "on" time is limited by the maximum possible travel of the driven device. Another application is that for gasoline filling stations, where the running time is limited by the largest size of auto tank to be serviced. For these (class I applications), the analysis can be simplified if it is assumed that the maximum running time is limited to a small percentage, say 10 per cent, of the time used as a basis for the rating, giving maximum running times of 0.5, 1.5, and 3 minutes for the 5-, 15-, and 30-minute ratings respectively. For these short intervals the curves of figure 4 can be considered straight lines near the limiting temperature rise of 60 degrees. Curve *A* applying to the 30-minute rating shows a temperature rise from $56^1/_4$ degrees to 60 degrees, that is $3^3/_4$ degrees in a time interval of 3 minutes. Curve *D* shows that after this running period, a cooling time of 1.5 minutes is required to reduce the temperature to $56^1/_4$ degrees. It follows that the 60-degree limit will never be exceeded if the running time is 3 minutes or less and if it is not more than 67 per cent of the total time per cycle. Similarly, it can be shown that a 15-minute rating is suitable for loads with running periods of 1.5 minutes or less, if the running time does not exceed 44 per cent of the total time per cycle. Thus, for this very common type of application, the selection of the proper short-time rating is exceedingly simple if the curves of figure 4 are available.

Class II Applications

For applications where the running time exceeds 10 per cent of the rating time (class II applications), correct results cannot be expected by the method just discussed. However, complete curves as in figure 6 can be derived from figure 4. Tabulations as given in table I, which applies to the 15-minute rating, will be found convenient for deriving the curves of figure 6.

Table I

Change in Temperature (Deg) From 60 Deg	Time on (Curve B)	Time off (Curve D)	Total Time	Time on in Per Cent of Total Time
5	1.6	2.05	3.65	43.8
10	3.2	4.75	7.95	40.2
15	4.65	8.9	13.55	34.3
20	6.0	15.0	21.0	28.6
25	7.25	24.25	31.5	23

Curve *B* in figure 6 shows the values of column 4 against those of column 5. Curve *E*, showing the values of column 2 against those of column 5, gives the maximum permissible running time per cycle. Similarly, curves *A* and *D* apply to the 5-minute rating, and curves *C* and *F* to the 30-minute rating. These curves show the maximum permissible time (minutes) per cycle as well as the permissible running time plotted against the ratio (*k*) of the on time to the total time of the cycle. For instance, with one-third of the time on and a 15-minute rating, the maximum permissible time per cycle is about 14.5 minutes and the permissible running time is 4.8 minutes, corresponding to the case illustrated by curve *A*, figure 5. It is evident from the curves of figure 6 that each of the three ratings is suitable for a certain limited range of the ratio of on time to the total time, and also for certain limited values of running time. Figure 7 gives 30-minute rating data similar to figure 6 for a standard open squirrel-cage motor suitable for ratings between 5 and 7.5 horsepower.

The considerations and results up to this point are theoretically correct. The accuracy is dependent in actual practice only upon the test errors in the time-temperature curves and the selection of the proper curves. However, abnormal rises always can be avoided by using for the derivation of figure 6 a temperature-rise curve slightly steeper than the test curve, and for the cooling curve one slightly flatter than that observed.

It remains now only to put the results into the most convenient form for practical use and to introduce approximations leading to simplicity. Instead of using curves, tabulations as in table II may prove convenient.

Table II

Per Cent of Running Time ($k \times 100$)	Permissible Running Time (T_r) in Minutes for: 5-Minute Rating	15-Minute Rating	30-Minute Rating
8	2		
10	1.8		
12.5	1.6	10	
16.0	1.2	9	
20.0	0.5	7.5	
25.0		6	19
31.5		4.5	16
40		2.5	12.5
50			9
63			3

The values in the first column are chosen from the ten series of preferred numbers, and the values of the other columns are chosen with some margin of safety below the curves of figure 6. The step curves F_1, E_1, and D_1 in figure 6 are from table II. All applications not exceeding these values will have temperature rises below the assumed limit of 60 degrees centigrade.

Another convenient approximation can be made by assuming a straight line F_2 in figure 6 to replace curve F for instance. Here, F_2 simply provides a better margin of safety than F. A straight line from the point (a) to the time value of the short-time rating does not always fall below the corresponding curve for its entire length. This is true for curve D in figure 6, and for curve F in figure 7. To retain a sufficient margin of safety for the higher values of k, the straight line should be drawn to a somewhat smaller value of k, as for F_2 in figure 7; here the value k_{a1}, corresponding to point a_1, has been set at 90 per cent of the calculated value for point a. If the straight line is above the curve for the lower values of k, as shown for F and F_2 in figure 7, applications according to the F_2 values are still satisfactory for the following reason. For low values of k, the temperature peaks are by necessity limited to very small portions of the total time, and rather low temperatures prevail during a large percentage of the time. Normal insulation life can be expected with temperature peaks somewhat in excess of the temperature value which may be considered satisfactory for continuous operation. Therefore, this simplified approximate method will be satisfactory if it is understood that it avoids unsafe operating conditions but does not limit temperature rises to a definite maximum value. Approximations of this kind are essential if reasonably simple methods are to be evolved.

The point a for C and F can be determined by entering the tangents A_1 and D_1 through the limiting temperature rise of 60 degrees for curves A and D of figure 4. The rate of temperature rise (r) and temperature drop (d) for the tangents A_1 and D_1 follow directly. To maintain constant temperature, the drop for each cycle must equal the rise. We thus have $T_i d = T_r r$, or, if we introduce the ratio R of the rate of drop (d) to the rate of rise (r), as $R = d/r$, we have

$$T_r = T_i R \tag{1}$$

Further, since $T_r = kT$ and $T_i = (1-k)T$, we find for the point a the value

$$k_a = \frac{R}{1+R} \tag{2}$$

If, now, T_s is the time basis for the short-time rating, we find the permissible running time from the equation for the line F_2, which is

$$T_r = T_s\left(1 - \frac{k}{k_a}\right) \tag{3}$$

(As indicated before, k_a in equation 3 should be somewhat smaller than calculated from equation 2 to provide a sufficient margin of safety for the higher values of k.)

We thus have a very simple equation (3) to find for any value of k the permissible running time if one characteristic value, namely, the ratio R, has been determined from tests for the particular short-time rating of the machine. The derivation of the curves in figures 6 and 7 can be avoided entirely if this approximate method is used.

The exact conditions indicated in figures 4, 5, and 6 illustrate the method and apply, of course, only to the particular motor tested, which was a three-phase, splash-proof, wound-secondary motor with a small fan at the rear end of the armature. Conditions vary with the size, speed, and type of ventilation of the machine. This is seen by comparison with figure 7.

It is interesting to compare the results of this analysis with the rms current method for applying motors to intermittent loads. This consists of calculating the rms current for the particular load cycle and comparing it with the current at which the motor can operate running continuously. For the smaller motor considered here, the continuous rating current for 60 degrees centigrade rise is 84 per cent of I the 15-minute rating current. For a cyclic load at current I, the rms currents, in per cent of I, for different values of k are given in curve A of figure 8. For $k = 0.25$ the limiting length of the cycle is 27 minutes from figure 6. Yet from figure 8, the rms current for $k = 0.25$ is only 50 per cent of the 15-minute rating current compared to a value of 84 per cent running with continuous load. Curve B in figure 8 shows the rms current permissible with

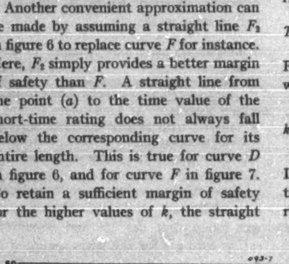
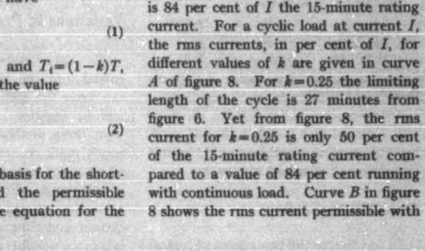


Figure 7 (left). A similar analysis gives the limiting cycles based on the 30-minute rating of a larger motor

Figure 8 (right). The rms values of current for different duty cycles are shown



a continuously running motor, obtained by multiplying curve *A* values by the ratio of the rating currents. Comparing curve *B* and the value *C*, it is seen that, in basing applications on the conventional rms calculations, appreciable allowances must be made to avoid overheating. The causes are the reduction in ventilation during the idle periods and because, with varying temperatures, as in figures 2 and 5, the maximum rise rather than the average rise should be considered.

For standardization purposes, it is of course impractical to use for each size and type of motor the specific value of *R* applying to the motor. An inspection of equation 3 indicates that satisfactory values for T_r will be obtained as long as the ratio *R* in the motor is larger than the value of *R* used in the formulas. With this in mind, two courses are possible for standardization purposes:

1. Groups of motor ratings of a certain type, each group extending over a certain range of ratings and speeds, can be selected. (If the smallest value for *R* applying to any motor in the group is selected for the calculations, safe results will be obtained for all motors of the group.)
2. Certain bands of values for *R* may be selected to establish classes of motors, as in table III.

Table III

Range of Factor R*	Class	Approx. Value of k_s
6.3 to 4	*A*	0.80
4 to 2.5	*B*	0.70
2.5 to 1.6	*C*	0.60
1.6 to 1.0	*D*	0.50
1.0 to 0.63	*E*	0.38
0.63 to 0.40	*F*	0.28
0.40 to 0.25	*G*	0.20

* Figures in column 1 correspond to the five series of preferred numbers.

It is believed that testing a limited number of typical machines will permit classifying all standard motors.

Class III Applications

In addition to the two classes of typical intermittent loads (class I, short-time cycles, and class II, longer-time cycles), there is another class of considerable practical importance. This general type (class III) of application occurs with pumps, compressors, etc. The normal load is intermittent, but there is often a prolonged period of initial operation, to fill an empty tank or to bring a cooling system to the condition of normal operation. The initial periods of operation, which as a rule start with a cold motor, are often more severe than any subsequent intermittent service and therefore may have an important, if not a governing, influence upon the selection of the motor rating. For this purpose the nominal short-time rating may furnish considerable guidance.

With pumps and air compressors the load during the initial period may be the same as or even less than the load during normal operation. Cases are conceivable where the initial period lasts about an hour or more, dictating the choice of a 60-minute or even a continuous rating, where possibly a 30-minute rating would be sufficient for the subsequent intermittent load. For refrigeration and air conditioning the load during the initial stage may be double the operating load, decreasing gradually to $1^1/_2$ times load, and then to full load; the entire period may last from one to eight hours or longer. In these cases the initial operating period determines thermal rating, while the normal operating load is considered for satisfactory efficiency and power factor. Where such initial operating conditions occur very rarely, it will be practical and economical to allow temperature rises in excess of normal values during these periods. Even then this period of operation may govern the selection of the motor rating. (For refrigerator machinery, where the motor is located inside the refrigerator, many of the previous considerations cannot be applied directly because the density of the gases cooling the motor varies during different periods of operation.)

Variations in Practice

The three classes of application discussed and the conclusions reached do not cover the great variety of conditions met in practice. The data obtainable from these methods supply, however, a bench mark or starting point by use of which many variations can be calculated or estimated. Frequently the cycles in operation are not uniform but vary in current and time values; in these cases is is often possible to estimate an average or typical cycle for use with this method of analysis. The current during the "on" period frequently is not uniform, as assumed in figure 1, but these cases can be treated by using the rms current for the "on" period in the calculations; sometimes the appreciable effect of the starting current of squirrel-cage motors can thus be taken into account. Applications where the motor keeps running light between load periods can be made the subject of a similar but modified analysis. The principal difference is that the cooling curves must be obtained with the motor running. Actually, the variations encountered in practice are infinite and these few examples are cited merely to indicate how many of the variations can be treated. In certain special and important cases, more extensive calculations or tests may be the best solution.

Wiring and Control

The wiring used for machines with short-time ratings employed for intermittent loads can be analyzed by the same methods. (Since the ventilation of the wiring does not change with operating conditions like the machine, the rms current can be used as a basis for application if the operating cycles are very short.) Proposed standards for wiring provide a number of continuous ratings for each size of wire and cable, depending upon the grade of insulation and the method of installation of the conductors; as many as 15 different ratings are given for some of the wire sizes. It is obviously impractical to assign short-time ratings and give application data for intermittent ratings to all these variations in wire and methods of installation. Appreciable simplifications obtained through reasonable approximations must be introduced to obtain practical methods.

In order to obtain an idea of the time constants of wire, some tests were made on number 14 wire, which has approximate ratings corresponding to the larger motor tested (curves of figure 7). The time intervals required to reach maximum temperature were:

5 minutes for bare wire installed in open air
10 minutes for insulated wire (50-degree-centigrade Code grade) installed in open air—isolated
40 minutes with four of the same wires in a single metal duct
140 minutes with the motor of figure 7

These relative values seem to indicate a complicated situation. However, it was found that the 15- and 30-minute rating currents for the wires mounted in a conduit, if applied to bare wire or to the same insulated wire mounted in open air, would not give temperature rises exceeding the standard value when operated continuously. This suggests the idea that standards could be set for wires in conduit only, because this is the usual method of installation; operation would, however, be satisfactory if the same wire is used in open air. Many other simplifications could be suggested, but the discussion of these possibilities should be deferred until more basic test data of the kind described here are available, permitting a more comprehensive study of the proper co-ordination of wiring and machines having short-time ratings. The methods outlined also can be used for analyzing the performance of switching and control devices and their co-ordination with the apparatus controlled. In addition, it will be necessary to study in more detail the proper practices for applying thermal protective devices for both the wiring and the machines.

Conclusions

The considerations outlined in this paper seem to indicate that the standardization of a limited number of intermittent ratings for applications with the motor at rest during the off period would be of little assistance. On the other hand, it seems that reasonably simple guiding rules for the application of the present short-time ratings to many such installations can be devised. To just what extent approximations should be introduced to facilitate simplification is, of course, a matter of judgment. Following is an example of a simple type of statement which would lead to satisfactory applications and result in reasonable all-round economy in motor applications:

> "Open induction motors in the range from . . . . to . . . . horsepower inclusive, with a 30-minute short-time rating, do not exceed limiting temperature rises if the rms current during the operating period does not appreciably exceed the rated current and if the running time does not exceed 50 per cent of the total time of the operating cycles. The limit for the "on" time (T_r), in minutes, during the cycle is given in the following equation
>
> $$T_r = 30(1-2k)$$
>
> (This corresponds to equation 3 for $k_a = 0.5$. In both equations k is the ratio of running time to total time per cycle.)
>
> "With operating currents below the rated current, T_r may be increased. If T_r is appreciably smaller than calculated, currents somewhat in excess of the rated current are permissible."

The studies made also indicate that reasonably simple guiding rules can be worked out for the proper co-ordination of machines or apparatus and their control, their protecting devices, and the wiring of the circuits. This paper should not be construed as indicating that intermittent ratings cannot be used to good advantage. In certain specific applications, they may serve a very useful purpose.

Discussion

For discussion, see page 1221.

Some Problems in the Standardization of Temperature Ratings of Fractional-Horsepower Motors

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Synopsis: In this paper statistical data are given on comparisons between different methods of temperature measurement in fractional horsepower motors. For such motors it is shown that surface thermocouple readings tend to give as much, or slightly more temperature rise than resistance measurements. For this, and other reasons, it is proposed, for fractional horsepower motors, that the surface thermocouple method be recognized as a distinct method of temperature measurement, instead of classifying it with the thermometer method, as the present standards do; it is suggested that the rating limits for the surface thermocouple be the same as those for the resistance method. It is pointed out that the surface thermocouple method is often the most convenient one to use in fractional horsepower motor application tests, and that it is used by the Underwriters' Laboratories, particularly in their standards on inherent overheating protective devices, the function of which is briefly discussed. Also, the effect of voltage variation upon fractional horsepower single-phase induction motors is discussed.

At the winter convention of the AIEE in New York in January 1939, there were presented a number of papers on temperature rating of electrical apparatus.[1–4] Since that time AIEE Standards No. 1 has been tentatively revised to favor the resistance method for determining the temperature rise and changing the limits for this method from 55 degrees to 60 degrees centigrade. Little data have been published on how the proposed changes in standardization might affect fractional-horsepower motors. For this reason the study outlined in this paper was undertaken. It is hoped that the information given herein will be of some assistance in standardization problems affecting fractional-horsepower motors. It is further hoped that others who have similar data on fractional-horsepower motors will make them available, regardless of whether they may support or differ with the data in the paper, in order that correct information representative of the industry may be available to those who make the standards, or any future revision of them.

Temperature Rises and Methods for Their Determination

The revised AIEE Standards No. 1 gives three methods for the determination of temperature rises:

1. Thermometer or surface thermocouple
2. Resistance
3. Embedded detector

The resistance method is indicated as the preferred method for most insulated windings because the much greater use of enclosed or protected type of electric motors seems to make a change from the thermometer to the resistance method logical. Although the standard gives certain definite values for temperature rises suitable for the majority of machines and apparatus, the necessity for departures from such values for individual types of apparatus is recognized. It also indicates that since temperature-rise values generally used for electrical apparatus at the present time, are the results of long experience and have proved reasonably satisfactory, major changes in these satisfactory standards should be avoided. From this, it may be concluded that whenever a change is made from the thermometer to the resistance method, values of limits used should be in accord with those found satisfactory in the past. It is obvious, therefore, that if a change in method of temperature-rise measurement is to be made, it is essential to know how the actual test results taken by the two methods compare. Data of this nature are given

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The author wishes to acknowledge with thanks the permission of the Westinghouse Electric and Manufacturing Company to publish the data herein presented. He is further obligated to J. E. Mulheim for assistance in collecting and recording some of these data, and also to R. E. Hellmund for many helpful suggestions in the preparation of the paper.

[1] For all numbered references, see list at end of paper.

in this paper and it is hoped that they will be of assistance in applying these broad principles to fractional-horsepower motors. (Similar data have been given by Summers[8] for motors from 10 to 1,000 horsepower in rating.)

In figures 1 to 6 inclusive are shown the result of 585 temperature tests taken on fractional-horsepower motors by different means of temperature measurement. These temperature runs are all continuous runs and were taken over a period of from seven to ten years on various ratings and different mechanical constructions of fractional-horsepower motors. The results were all taken by standard test procedure by testers generally skilled in the art. The tests shown were selected purely at random from a number of tests taken over this period and were not selected with any particular reference to accuracy. Even some tests of doubtful accuracy have been purposely included to show what magnitudes of variations might be expected in temperature measurements of this sort. In some cases, a special mechanical construction made to suit a particular customer's requirements, makes the problem of locating thermometers difficult.

The surface thermocouples used for these tests were located in the hottest spot of the winding accessible to thermocouples; in single-end ventilated machines, this point will be on the exhaust side. The small size of the thermocouples often makes it possible to put them between strands of wire or between two coils. This procedure was followed in installing "surface" thermocouples; but in all cases, the thermocouples were installed in completed motors, after the windings were impregnated and baked.

To determine the relationship between rise by resistance and rise by thermometer in capacitor-start, split-phase, and d-c motors, the results of many continuous runs were plotted in figures 1 to 4, inclusive. It will be noted that the temperature-rise measurements include a wide range of temperature (because overload temperature runs are included) and that the points are so scattered it would be difficult, with only a few points, to draw a representative curve giving the relationship between rise by resistance and rise by thermometer. For this reason, many points were plotted. It is more or less tacitly assumed, however, and it seems reasonable to suppose that the relationship of rise by resistance to rise by thermometer is a simple proportionality that can be represented by a straight line passing through the origin. (And the data indicate that assumption is probably more reasonable than any other; in other words, the actual differential between the two methods of measurement is proportional to the total temperature rise.) This straight line is so located that there are as many points above as below the line. If each point is considered to represent a ratio of two temperature rises, the slope of the line just constructed represents the median value of the numerous ratios and represents the sought-for relationship between rise by resistance and rise by thermometer. (This median line is the solid line in the figures.) The dotted lines are drawn to show variations that may be expected and include 90 per cent of the points plotted.

In figure 5 the surface-thermocouple method is compared with the thermometer method, and in figure 6, with the resistance method.

The results of these tests are summarized in table I.

Table I. Comparison of Three Methods of Measurement of Temperature Rise

Methods Being Compared	Number of Tests	Ratio of First Method to Second Method: Median Ratio	Maximum Ratio*	Minimum Ratio*
Resistance versus thermometer				
Capacitor-start motors	129	1.16	1.45	0.88
Split-phase motors	102	1.16	1.43	1.00
D-c motor armatures	116	1.61	2.11	1.22
D-c motor fields	116	1.62	2.29	1.08
Surface thermocouple versus thermometer				
A-c motors	66	1.19	1.48	0.94
Surface thermocouple versus resistance				
A-c motors	56	1.04	1.37	0.78

* Excluding 5 per cent of the tests.

From these curves and table I, a number of interesting conclusions may be drawn:

For fractional-horsepower a-c squirrel-cage motors with the primary winding on the stator, the temperature rise by resistance averages 16 per cent higher by resistance than by thermometer. This figure is based upon the results of 231 continuous temperature tests on split-phase and capacitor-start motors. (Figures 1 and 2.) The values of 16 per cent are reasonably consistent with the value of 20 per cent following from the figures given in Standard No. 1. It is to be expected that for small machines such as fractional-horsepower motors, the difference in temperature will be somewhat smaller than that for larger machines. In order to avoid odd values of temperature rise, a value of 50 degrees centigrade by resistance might be substituted for motors previously based on the 40-degree-centigrade standard by thermometer. In fractional horsepower motors such a change would be tantamount to raising the temperature-rise-by-thermometer limits 3 degrees centigrade, certainly a minor and insignificant change, particularly in view of improvements made in insulating materials since the figure of 40 degrees centigrade was initially set.

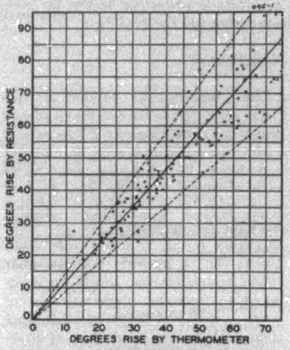

Figure 1. Temperature-rise tests on capacitor-start motors ranging from one-eighth to one horsepower in rating, comparing resistance method with thermometer method (129 tests)

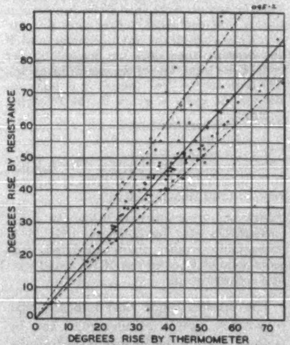

Figure 2. Temperature-rise tests on split-phase motors ranging from 1/30 to 1/2 horsepower in rating, comparing resistance method with thermometer method (102 tests)

For fractional-horsepower d-c motors, the temperature rise of either the armature or of the field is approximately 61 to 62 per cent greater when measured by the resistance method than when measured by thermometer. This difference between figures obtained by the two methods seems to be the same for both armature and field measurements and is based on the results taken separately from both fields and armatures for 116 different d-c motors. (Figures 3 and 4.) In armature temperature measurements, the large difference between the thermometer and resistance readings is likely due to a number of factors: the impossibility of supplying internal ventilation to the armatures makes the temperature rise high; furthermore, the thermometers can only be applied after the machine is shut down and the mass of these armatures is so small that the windings have had time to cool appreciably before thermometers can be installed and reach their final temperature.

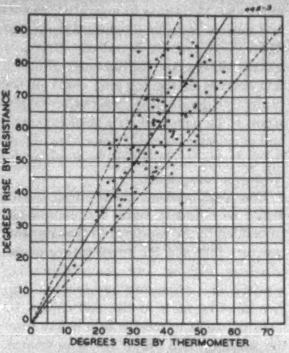

Figure 3. Temperature-rise tests on the armatures of d-c motors ranging from 1/20 to 1 horsepower in rating, comparing resistance method with thermometer method (116 tests)

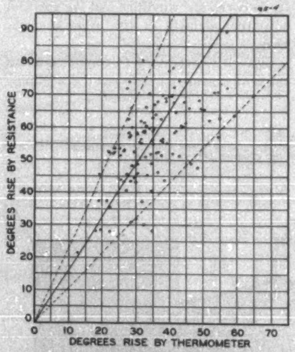

Figure 4. Temperature-rise tests on the field windings of d-c motors ranging from 1/20 to 1 horsepower in rating, comparing temperature rise by resistance with thermometer method (116 tests)

The ratio of rise by resistance to rise by thermometer appears to be subject to greater variations in individual cases of field temperature measurements than in individual cases of armature temperature measurements, although the average of a large number of points is substantially the same. Some fractional horsepower d-c motors have series fields and some do not. When a series field is used it is often wound on top of the shunt coil and the two taped together, making it difficult to determine by which field a thermometer reading is most affected. Different strengths of series fields and different degrees of overload would exaggerate these discrepancies. The resistance to heat flow between the windings and core of an a-c motor is relatively small, because most of the winding is embedded in slots which afford a large area and short length of path for the transfer of heat; also, the cross-sectional area of the coil is limited by the size of a single slot and is, therefore, relatively small. On the other hand, in a d-c motor there is relatively high resistance to heat flow from the field windings to iron parts because the winding for each pole is concentrated in a single coil, of relatively large cross-sectional area, and not in particularly good thermal relationship with the iron parts of the motor. Consequently it is not surprising to find large differences between rise by resistance and rise by thermometer in measurements of d-c fields.

As a result of the foregoing discussion, it is to be concluded that if the standard basis for rating fractional-horsepower d-c motors is to be changed from thermometer to rise by resistance, the 40-degree-centigrade limit should be changed to 60 degrees centigrade. Since figures 3 and 4 show that a rise of 60 degrees centigrade by resistance corresponds to approximately 37 degrees centigrade by thermometer, a rating of 60 degrees by resistance actually is slightly more conservative than the old rating of 40 degrees by thermometer. A figure of 60 degrees by resistance is, therefore, in harmony with the previously expressed principle that there should be no major change in the present time-tried ratings. Actually, a limit of 65 degrees centigrade by resistance would correspond more closely to the present figure of 40 degrees centigrade by thermometer.

Some interesting conclusions regarding the use of surface thermocouples for measurement of temperature rise of fractional-horsepower a-c motors became apparent from a study of figures 5 and 6. The temperature rise by surface thermocouple is seen to be 19 per cent higher than the rise by thermometer and 4 per cent higher than the rise by resistance. This finding is particularly significant in view of the fact that the present AIEE standard recognizes no distinction between surface thermocouple readings and thermometer readings of temperature rise.

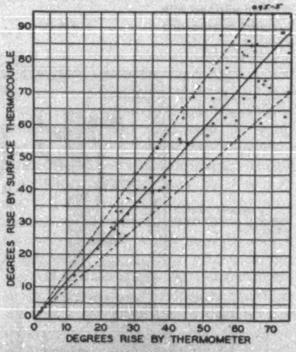

Figure 5. Temperature-rise tests on split-phase and capacitor-start motors ranging from 1/30 to 1 horsepower in rating, comparing rise by surface thermocouple with thermometer (66 tests)

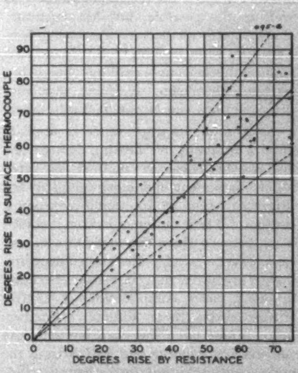

Figure 6. Temperature-rise tests on split-phase and capacitor-start motors ranging from 1/30 to 1 horsepower in rating, comparing rise by surface thermocouple with resistance method (56 tests)

Here is an anomaly which needs correction in any future revision of AIEE Standards No. 1.

As far as fractional-horsepower motors are concerned, the surface-thermocouple method should be considered as a method separate and distinct from the thermometer method, and different limits of rise for the two methods should be established; it would be in accord with the findings in this paper if the limits for the surface thermocouple were made the same as for resistance.

Emphasis has been placed upon the foregoing because of the importance of surface-thermocouple temperature measurements of fractional-horsepower motors. In applying inherent overheating protective devices, temperature rises in the order of 100 to 125 degrees centigrade may be encountered; for such high temperature rises, the use of surface thermocouples is virtually indispensable, and the standards of the Underwriters' Laboratories for these devices are based upon this method of temperature measurement. There are two important reasons why the surface-thermocouple method is best in such cases: (1) the high temperature rises encountered make it essential that an accurate method be chosen, involving as little difference as possible between "hot spot" and observed temperature; this consideration rules out use of the thermometer; (2) moreover, in applying these devices it is most desirable, from an engineering standpoint, to measure temperatures while the motor is running, and part of Underwriters' standards are based on temperatures taken *while the motor is running;* this consideration precludes the use of the resistance method, as well as the possible, but less accurate, thermometer method for measuring temperatures while running. It is found that surface thermocouple readings change but little on shutdown, signifying that running readings can be satisfactorily taken.

Aside from its importance in application of inherent overheating devices, the surface-thermocouple method is often most convenient and most suited in application tests of fractional-horsepower motors. These motors are often installed in appliances where thermometers cannot be applied or read. Resistance measurements are often not convenient because, in many cases, complications of control wiring make them difficult. Furthermore, it may not always be easy to isolate the starting winding and switch from the main winding circuit for resistance readings. Moreover, use of the surface-thermocouple method affords a means of following the motor temperature of various conditions of load imposed by the appliance *without shutting down the machine.*

It is for the reasons given above, that the author believes that the surface-thermocouple method should be given recognition in AIEE standards as a distinct method of temperature-rise measurement.

Inherent Overheating Protection

In earlier days fractional-horsepower motors were protected by external devices which would trip on any continuous current exceeding the "ultimate tripping current" of the thermal relay. Since it was assumed that the *temperature rise* of a motor is a function of the motor current, these external relays really limited the temperature rise of the motors. In recent years, however, there has been a rapidly growing tendency to incorporate the protective device inside the motor to obtain inherent overheating protection. These devices are designed to limit the *total temperature* and not the *temperature rise* of the winding. The standards of the Underwriters' Laboratories for these devices[8] specify that, under no condition of running overload, shall the *total* temperature of the windings, as measured by surface thermocouples, exceed 125 degrees centigrade and that, with a locked rotor, the total temperature of the windings shall not exceed 150 degrees centigrade nor shall there be any excessive smoking indicative of permanent injury to the windings. (Moreover, the device must be able to provide locked-rotor protection for 15 days.) These two figures represent the essential temperature requirements of the Underwriters' standards for these devices.

The figure of 125 degrees centigrade is somewhat above the limiting hot-spot temperature of 105 degrees established for class *A* insulation by AIEE. However, since variations in individual motors and devices must be downward from that figure, and since it is unlikely that any motor will operate continuously within a few degrees of the tripping point of the protective device, the Underwriters' limit seems to be tempered by a good economic and engineering sense. Some idea of how these devices function will be given in the following paragraphs.

The ideal inherent overheating device would be infinitely small and completely embedded in the windings so that its temperature followed the temperature of the windings without any temperature lag whatsoever. However, any practical device must be finite in size and cannot be mounted so as to receive heat solely from the windings and follow the temperature of the latter without an appreciable lag; the greater the current flowing through the windings, the greater will be the lag. In a practical device it is therefore necessary to compensate for the temperature lag between the windings and the thermal actuating element of the protective device.

Since it is necessary to compensate for the temperature lag between the windings

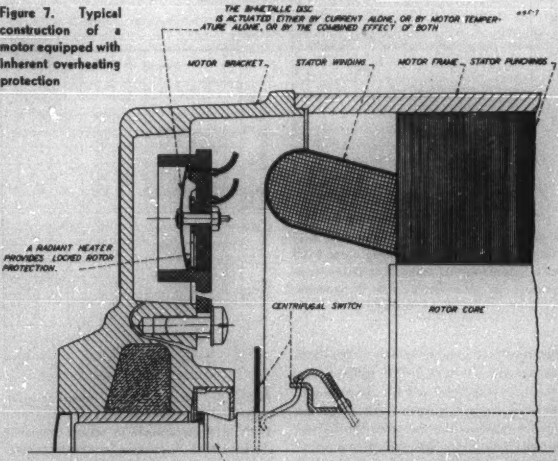
THE BIMETALLIC DISC IS ACTUATED EITHER BY CURRENT ALONE, OR BY MOTOR TEMPERATURE ALONE, OR BY THE COMBINED EFFECT OF BOTH
MOTOR BRACKET
STATOR WINDING
MOTOR FRAME
STATOR PUNCHINGS
A RADIANT HEATER PROVIDES LOCKED ROTOR PROTECTION.
CENTRIFUGAL SWITCH
ROTOR CORE
SHAFT

Figure 7. Typical construction of a motor equipped with inherent overheating protection

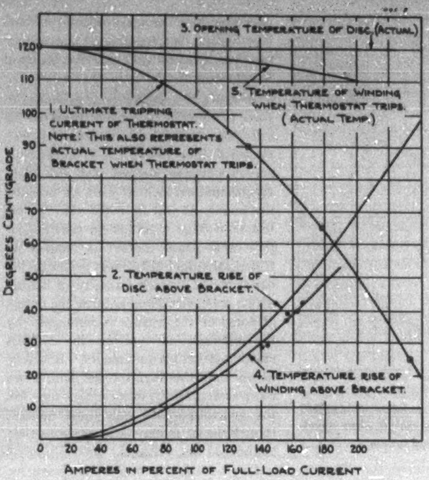

Figure 8. Temperatures in the motor of figure 7

and the device anyway, the latter is often mounted in the end bracket (or end bell) of the motor for the sake of convenience, as shown in figure 7. Here the thermal actuating element is a bimetallic disk set to trip when the temperature of the disk reaches 120 degrees centigrade. (For example, if the entire motor without any current passing through it, were placed in an oven and heated, so slowly that all parts were the same temperature, the disk would trip open when 120 degrees was reached.) In close proximity to the disk is a radiant heater so designed as to compensate for the temperature lag of the bracket temperature behind the winding temperature. The heater is also designed so as to protect the motor against locked-rotor conditions, as will be discussed later.

How the heater provides compensation for the temperature lag of the bracket behind the windings may perhaps be better understood by reference to figure 8. Curve 1 shows the ultimate tripping currents of the thermostat plotted as abscissas against the actual ambient temperature surrounding the thermostat. This curve shows that the higher the temperature surrounding the thermostat, the lower the ultimate tripping current. This same curve also represents the ultimate current necessary to trip the thermostat for any given bracket temperature. The actual temperature of the disk is, of course, higher than that of its surroundings because of the heat generated in the auxiliary heater. The temperature rise of the disk above the bracket is plotted in terms of the current flowing through the heater and is seen to be proportional to the square of the current in the heater. The opening temperature of the disk itself is constant as shown in curve 3. Numerous temperature tests were taken on the motor for which this particular device is used and it was found that the temperature rise of the winding above the bracket, as measured by surface thermocouple, was almost solely a function of the motor current and independent of the bracket temperature. The results are plotted as curve 4 and it is seen that the temperature rise of the windings above the bracket is also proportional to the square of the stator current. By adding curve 4 to curve 1, curve 5 is obtained and this curve represents the actual temperature of the winding when the thermostat trips. Note that for different currents the temperature of the bracket may vary from 120 to 50 degrees centigrade, but for these same conditions, the actual hot-spot temperature of the winding will only vary between 120 and 110 degrees centigrade. To summarize, it may be said that the temperature of the motor which directly affects the inherent overheating device is the temperature of the bracket and the difference in temperature between the windings and bracket is compensated for by the auxiliary heater through which the stator current flows.

From the foregoing it can be seen that by a proper selection of the operating temperature of the device and by using an auxiliary heater of the right proportions, the temperature of the operating element or disk can be made to follow the winding temperature quite closely for various sustained load conditions which do not cause a tripping of the device. For very severe loads of short duration, the thermal device tends to overprotect, since it has less mass and less thermal capacity than the motor as a whole, and hence, reaches its final temperature more quickly than does the entire motor. Actual temperature measurements of the disk and of the windings under various continuous load conditions insufficient to cause tripping, verify this fact that, when properly applied, the disk does actually follow the winding temperature within a few degrees. For such tests, surface thermocouples have been found indispensable because of their convenience and accuracy.

Under locked-rotor conditions, the device operates on current alone, like any current-operated device. If the device is arranged to reset automatically, as does the thermostat in figure 7, it will cycle on and off automatically if the rotor is locked. During the on period, a large amount of heat is generated in the windings, but during the longer off periods, the temperatures tend to equalize partially, so that the temperature of the entire motor, including the end brackets is elevated. As the temperature of the end bracket increases the reclosing time of the thermostat increases, particularly as the reclosing temperature of the thermostat is neared. Thus, after an hour or two, the cycling time of the thermostat, the frame temperature, and the cyclic varia-

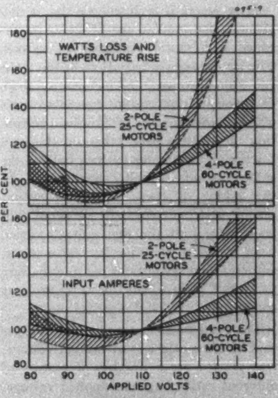

Figure 9. Effects of variation of applied voltage upon capacitor-start motors when developing full-load torque

tions in winding temperature stabilize. The total integrated watts consumed by the motor then becomes equal to the watts that can be dissipated by the motor with no ventilation, which is necessarily small. Inasmuch as the peak winding temperatures reached under locked-rotor conditions will never be maintained continuously, a figure of 150 degrees centigrade for locked-rotor condition, as required by the Underwriters' seems wholly reasonable. In actual practice, this temperature is probably seldom reached.

It is hoped that the foregoing discussion will help to clarify certain aspects of inherent overheating protection with particular reference to temperature measurement considerations. In the author's opinion, it further emphasizes the need for recognition of the surface thermocouple method of temperature measurement.

Effect of Voltage Variations on the Performance of Induction Motors

The effect of a variation of the line voltage upon the operation of fractional-horsepower single-phase induction motors is of considerable interest in setting standards for allowable voltage variations, as well as being of interest to power companies and also users of these motors. There has been a great reluctance on the part of motor manufacturers to make categorical statements as to the general effect of variation in line voltage, because of the enormous number of types and their ramifications which are involved. For these same reasons, it is with some reluctance that the data given in figures 9 and 10 of this paper are presented.

It is generally well known that variations in line voltage are of small importance to a motor operating at no load. The importance of variations in line voltage increases with an increase in load. However, to obtain some measure of this effect a large number of "full-load saturation" tests have been taken on standard fractional-horsepower motors, both of the split-phase and capacitor-start types. In the taking of these tests, the output torque was held constant at rated full-load value and the line voltage varied from 80 volts to 140 volts. The watts, amperes, and revolutions per minute were measured, from which the watts loss was computed.

The total watts loss, expressed as a per cent of the watts loss at normal voltage, was plotted for a number of motors of different ratings against applied voltage. As might be expected, these curves did not all fall directly on top of one another so limiting curves were drawn, between which lay all the curves plotted. The curves for four-pole 60-cycle capacitor-start motors all fell within the limits of the indicated shaded areas. Similar limits for the full-load saturation curves for two-pole 25-cycle capacitor-start motors of the same horsepower ratings were established and also indicated in figure 9. Similarly, on figure 9 is shown the variation in current, expressed as a per cent of full load current at rated voltage, for 60-cycle, and also for 25-cycle, capacitor-start motors.

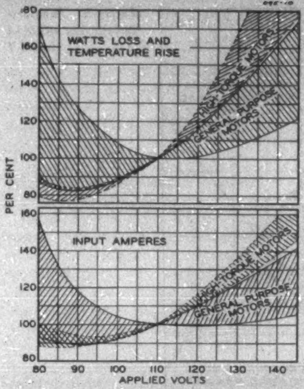

Figure 10. Effects of variation of applied voltage upon split-phase motors when developing full-load torque

The curves of total watts loss versus voltage are of interest, not only from the standpoint of voltage standardization but also from a consideration of temperature rise, since the temperature rise is very closely proportional to the total watts loss. Thus, it can be seen from the curves that if the full-load temperature rise of a capacitor-start motor is 50 degrees centigrade at 110 volts and rated output, the corresponding temperature rise at 125 volts would be from 117 to 121 per cent of this value, that is, from 58 to 61 degrees centigrade. However, if it were a 25-cycle motor under consideration, a 50-degree rise at full load on 110 volts would be increased 40 per cent to 56 per cent, giving a rise of 70 to 78 degrees at 125 volts. Twenty-five-cycle motors, because of higher saturations, are thus seen to be more sensitive to overvoltage than are 60-cycle motors. The curves for 50-cycle motors were not plotted but would probably fall between the 25- and 60-cycle curves.

However, a word of caution must be given in connection with the operation of capacitor-start motors on overvoltage. The limiting feature may be the voltage on the capacitor and not the full-load temperature rise.

In figure 10 are shown curves similar to those of figure 9, except that these are for split-phase motors. This type has been classified into two groups, namely (1) general-purpose motors, and (2) high-torque motors such as used on washing machines. It will be noted that the effect of voltage variation on general-purpose split-phase motors is subject to wider individual differences primarily because of the greater diversity of ratings which are covered and because the design of some of the ratings is influenced by the necessity of meeting the NEMA starting-current requirements. It will be noted that the high-torque motors are more susceptible to overvoltages than are the general-purpose split-phase motors, though the latter may be more susceptible to reduced line voltages.

The variation in current is shown for both types of motors in figures 9 and 10 because it is thought that it may be of some interest in connection with control and in connection with the application of external current-operated thermal overload devices.

No curves are shown for variation in torques as these vary directly as the square of the applied voltage.

It should be pointed out that the curves shown represent the effect of variation of voltage when the motors are operating at full-load torque and are not representative of other load conditions. Qualitatively, it may be stated, the higher the overload, the less relative effect that overvoltage has and the more relative effect that an undervoltage has.

From these curves it does not appear necessary to make any changes in the present standards for permissible voltage variation of plus or minus ten per cent.

A word relative to the much-discussed subject of changing of the present standard voltage rating from 110 to 115 volts may be in order here. One possibility is to design all the motors to give, on 115 volts, the same performance they now have on 110 volts, the curves shown in figures 9 and 10 would all be shifted to the right five volts without an appreciable change in shape. However, a change of this nature means that the torques of the motor would be reduced to $(110/115)^2 =$ 91 per cent of the present torques and the ability of the motor to start the load at reduced voltages would be impaired. The locked-rotor and full-load currents would be reduced in the ratio of 110 to 115.

Another possibility to be considered is

that of simply changing the name-plate stamping of the present motors. In such a case, the curves of figures 9 and 10 would not change, nor would the torques of the motors but the full-load performance of the motors would be changed. Also, the locked-rotor currents would be increased in the ratio of 115 to 110. In any case, however, it would be necessary to redesign some motors, such as two-pole 25-cycle capacitor-start motors and high-torque split-phase motors, to limit the temperature rises, on voltages ten per cent above the new rating and to redesign some capacitor-start motors to limit the voltage on the capacitor to a safe value.

Thus, it can be seen from the two preceding paragraphs, that the problem of changing the voltage rating is not a simple one, involving as it does the ultimate user and the manufacturer, as well as other standards.

Conclusions

The important conclusions of this paper, all of which relate only to fractional-horsepower motors, are:

1. The temperature rise of single-phase induction motors measured by surface thermocouple averages as much or more than the rise by resistance.
2. The surface thermocouple method of temperature measurement should be recognized as a distinct method instead of classifying it with the thermometer method as done in the present AIEE Standard No. 1. For purposes of rating, the limits assigned to this method may be the same as those assigned to rise by resistance.
3. If the method of determining the temperature rise of single-phase induction motors is to be changed, the present limit of 40 degrees centigrade by thermometer could be changed to 50 degrees by resistance or 50 degrees by surface thermocouple, without appreciably affecting actual temperature rises.
4. If the method of determining the temperature rise of d-c motors is to be changed, the present limit of 40 degrees centigrade by thermometer could be changed to 60 or 65 degrees centigrade by resistance without appreciably affecting actual temperature rises.
5. Use of the surface thermocouple is necessary to determine if inherent overheating protective devices comply with Underwriters' standards. Their use in application tests is indispensable.
6. Present standard limits of voltage variation of plus or minus ten per cent are satisfactory from the standpoint of motor performance. But a change in the present standard voltage rating of 110 volts is not a simple problem.

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8. REQUIREMENTS FOR INHERENT OVERHEATING PROTECTIVE DEVICES FOR MOTORS. An appendix to the Standard for Industrial Control Equipment issued by the Underwriters' Laboratories, Chicago, Ill., June 1939.

Discussion

For discussion see page 1218.

New Instruments for Recording Lightning Currents

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IN the early days of lightning investigations surge voltages on transmission lines were measured by a relatively crude device consisting of a number of parallel gaps with different spacings. The maximum gap broken down, which was indicated by markings on a thin piece of paper placed within the gap, was a measure of the voltage. Peters,[1] by applying the phenomenon of Lichtenberg figures to photographic films, then developed the instrument called the klydonograph. Much information concerning the magnitude and frequency of voltage surges on systems was obtained with this instrument. At about this time the cathode-ray oscillograph was just emerging from its laboratory stage of development and several investigating groups adapted it to field work. A number of records were obtained with this device which showed the rate of rise of the voltage wave, its crest, and a limited amount of information regarding its tail. However, the quantity of data secured by this means was quite small, largely because of the initial cost of the instrument and the necessity of constant attendance for operation and maintenance.

Because of the variable nature of lightning it is desirable that many readings of its properties be made so that its statistical nature with regard to magnitude, rate of rise, and duration may be adequately studied. The surge-crest ammeter,[2] because of its simplicity and cheapness, has provided an enormous amount of information relating to the magnitude and polarity of lightning currents. However, there still remains much to learn regarding the wave shape of lightning currents, particularly their duration and initial rate of rise.

The records of wave shape obtained by the cathode-ray oscillograph indicated that the currents in a lightning stroke were essentially dissipated or spent in a time of a hundred microseconds. Lightning arresters and other protective equipment were therefore designed and tested on the basis that they would be called upon to discharge surges of this duration in service.

In parallel with the work being done on transmission lines and the like in the measurement of voltages and currents, photographic records were being made by means of the Boy's camera, a camera in which the lens and film move relative to each other either by movement of the film or by movement of the lens system. Schonland[3,4,5] and his associates in Africa had by this means shown the general nature of propagation of the discharge from the cloud to earth and the subsequent return streamer of high intensity. Their work also indicated the

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Paper 40-90, recommended by the AIEE committee on instruments and measurements, and presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940. Manuscript submitted November 16, 1939; made available for preprinting May 8, 1940.

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[1] For all numbered references, see list at end of paper.

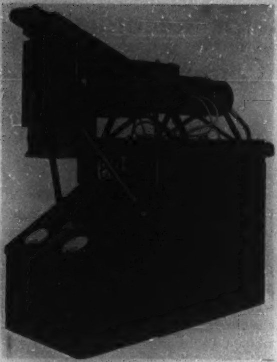


Figure 1. General view of automatic cathode-ray oscillograph

presence under certain conditions of long duration strokes, longer than the 100 microseconds which previously had been regarded as typical. Steknolnikov and Valeev[6] in Russia, Norinder[7] in Sweden, and McEachron[8] in this country with his work on the Empire State Building, likewise supplied fresh evidence that the tail of the current wave might persist at a low value for a long time.

The foregoing evidence therefore suggests the need for additional statistical knowledge of lightning currents, both in the stroke and in stricken equipment, particularly, with respect to the wave shape for periods of time in excess of 50 microseconds. It is the purpose of this paper to describe some new instruments recently developed to accomplish this result.

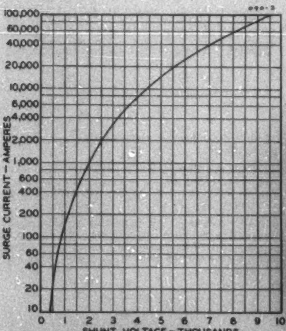
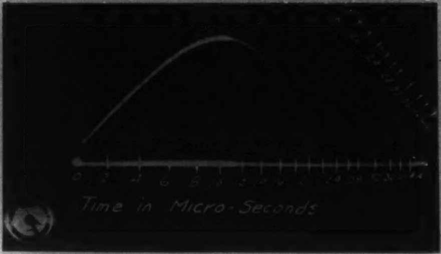


Figure 2. Calibration curve of cathode-ray-oscillograph shunt

Figure 3. Laboratory record obtained with cathode-ray oscillograph under fully automatic conditions



Automatic Cathode-Ray Oscillograph

In the past the cathode-ray oscillographs used in lightning investigations were of the pumped type. A cathode-ray-oscillograph tube of sufficient writing speed which is sealed off and therefore does not require pumping is now available. A laboratory oscillograph utilizing this tube has been described by Kuehni and Ramo.[9] Being sealed off, this tube with suitable auxiliary circuits provides a device which lends itself readily for placement in the field for automatic operation without the constant attendance of an operator. Figure 1 shows a general view of the developed instrument. It is portable and operates from any 110-volt a-c lighting circuit. The tube is of the hot-cathode type and requires a potential of 15,000 volts to accelerate the electron beam which impinges upon a willemite screen. The image thus produced is photographed upon a light-sensitive film through a compound lens system. An auxiliary grid within the tube controls the electron beam so that under normal conditions the beam is not "on". In this manner fogging of the film is prevented. Upon the occurrence of a surge the control grid automatically initiates the beam and permits recording of the phenomenon.

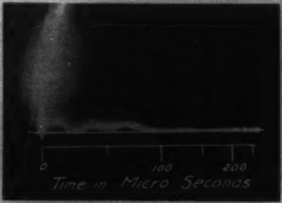


Figure 4. Field record obtained with cathode-ray oscillograph

The photographic film is mounted on a disk which rotates at a speed of five rpm. This is sufficiently slow that during the time of recording of a particular surge the film may be regarded as essentially stationary, but in the event of more than one surge occurring at different times a different surface of the film will be exposed. In order to identify individual records and determine their time of occurrence the face of an electric clock is focused at a point diametrically opposite from the record and is flashed simultaneously with the operation of the control grid of the cathode-ray tube.

The range of currents in which one is interested in this work is enormous as the discharge current of a stroke may vary from a few amperes to more than 150,000 amperes and may persist for a considerable time at small magnitudes. In order to cover this range as well as possible a nonlinear shunt whose resistance is variable and decreases with increasing current is utilized. Figure 2 shows the characteristic of this shunt in the form of a calibration curve plotted as total current in amperes for the ordinate and shunt voltage for the abscissa.

A typical oscillogram obtained in the

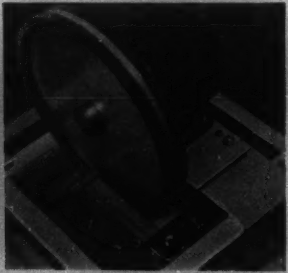


Figure 5. "High-speed" fulchronograph showing wheel

Figure 6. "High-speed" fulchronograph showing coils

laboratory with this instrument under fully automatic conditions is shown in figure 3. A number of oscillograms have also been obtained in the field of which figure 4 is an example.

Fulchronograph

The relatively high cost of the cathode-ray oscillograph and its complexity limits its use for the collection of statistical information regarding the characteristics of lightning currents. The need exists for a simple and less expensive instrument which can be distributed in greater numbers at different locations. A new device to which the name "fulchronograph*" has been given has recently been developed which the authors believe meets these requirements.

The essential part of the fulchronograph is a slotted aluminum wheel which, in one type, the "high speed", rotates at 3,450 rpm and in another type, the "slow speed", at approximately 60 rpm. Figure 5 shows one of the wheels mounted on the high-speed fulchronograph. The slots are filled with three nine-mil permanent-magnet-steel laminations, which project from each side. These projections are made to pass between narrow coils, see figures 6 and 7, through which flows the current to be measured. As a particular set of laminations spans the gap between the coils, the laminations are subjected to a radial magnetizing force proportional to the current at that instant. By measuring the retentivity or residual flux in the laminations it is possible to reconstruct a graph of the current as a function of time. The device functions in a manner similar to the surge-crest ammeter except that the element of time has been introduced by the rotation of the wheel. The two sides of the wheel are designed to cover two ranges of current. On one side the coil has a purely air core except for the magnetizable laminations and covers a current range of from 1,000 to 50,000 amperes; on the other side the magnetic circuit is completed through a Hipernik core with a partial air gap and the sensitivity in this manner is increased to cover a range of from approximately 100 to 6,000 amperes. A single wheel can thus record a range from 100 to 50,000 amperes. The calibration curves for the two sides are shown in figure 8.

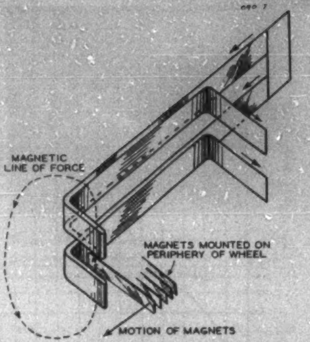


Figure 7. Diagram showing relation between one set of laminations and the coils on one side of the wheel

There are 408 slots in each wheel, so, for the "high speed" fulchronograph, the time element between slots corresponds to approximately 43 microseconds and for the "slow speed" to $^{1}/_{400}$ second. The total time for one revolution of the wheel is, for the "high speed", 0.017

Figure 9. Curves showing the distribution of flux between adjacent slots of fulchronograph wheel

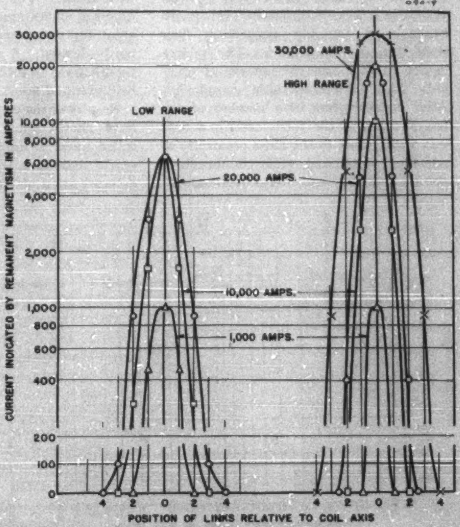
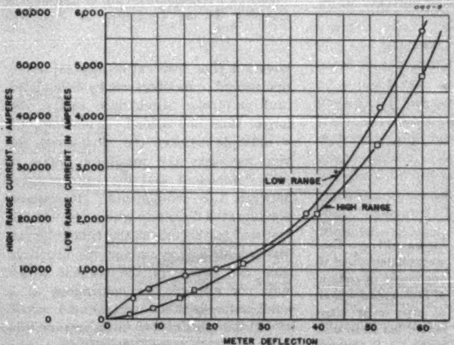


Figure 8. Calibration curves for fulchronograph



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* This name was obtained as a combination of the Latin word *fulmen* meaning *lightning*, and the Greek words *chrono* meaning *time*, and *graph* meaning *to write*.

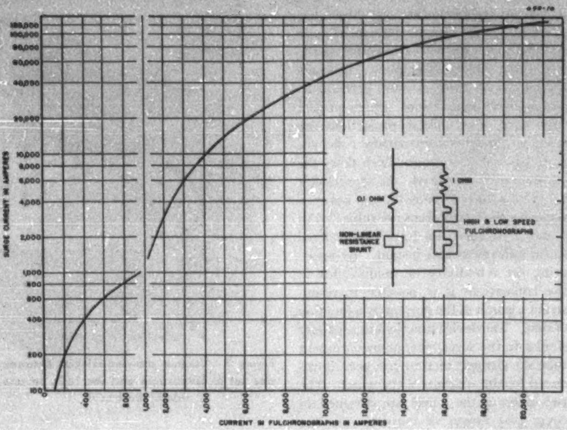


Figure 10. Calibration curve of fulchronograph shunt

second, and, for the "slow speed", 1.0 second.

There are several conceivable methods for utilizing the principle of remanent magnetism. A thin continuous disk of permanent magnetic steel in which the magnetizing force is directed by high-permeability steel to a very narrow region in a lateral direction has the advantage of possibly permitting finer resolution of the record. The pickup device in this construction would probably lead to considerable complexity. After consideration of a number of different schemes the form described here was built. Due to the close proximity of adjacent slots and their laminations, the magnetizing coil has a tendency to spread its effect over several slots. This spread can be determined by surging the coil with the wheel stationary, for various positions of one of the slots relative to the axis of the coil. The resultant current indications as a function of slot pitch and maximum current are shown in figure 9 for both the high-current side and the low-current side. A rate of change of current in excess of the slope of the distribution curves indicated in this figure cannot be determined. For values of current in excess of 30,000 amperes the spread is too great for the degree of resolution ordinarily desired. A specific application of these curves in interpreting a record will be illustrated later.

Results of magnetic-link investigations have shown that arresters are rarely called upon to discharge currents in excess of 10,000 amperes. Therefore, for the determination of arrester currents an upper range of 30,000 amperes is sufficient for this application. For measuring the current in direct strokes which may exceed this value a combination of ordinary resistors and nonlinear resistors is used. Figure 10 shows a calibration curve, for the shunt combination indicated in the insert, in which the maximum current in the fulchronograph is 20,000 amperes for 120,000 amperes of total current.

The purpose of two devices rotating at different speeds is to cover a greater range of time than would be possible with a single high-speed wheel. As stated previously, the high-speed fulchronograph covers a total time of approximately 17,000 microseconds in intervals of 43 microseconds and, of course, runs continuously. The slower speed device covers a time interval of approximately one second. As observed by Schonland, multiple-stroke discharges rarely last longer than one second. Therefore, for a multiple stroke, the slow-speed device measures the maximum value of each component and the time interval between the components. It cannot, however, tell definitely which one was the first and to this extent the time interval, between strokes is ambiguous. Perhaps from the nature of the current which may, for example, be continuous at a small value between successive strokes, it will be possible to differentiate the first from the others. Experience might

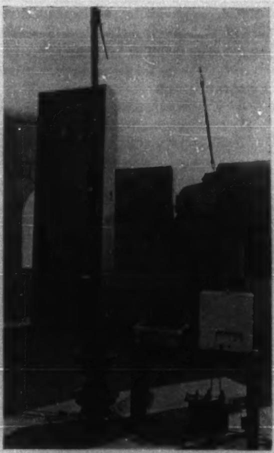


Figure 12. Installation on top of Cathedral of Learning of the University of Pittsburgh

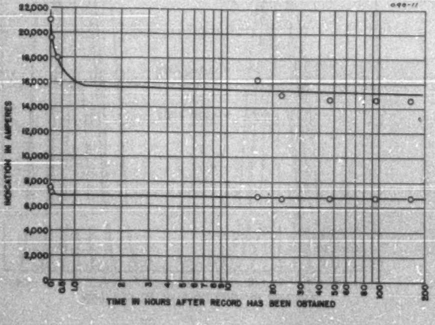


Figure 11. Demagnetizing effect of continued operation of high-speed wheel of high range after record has been obtained

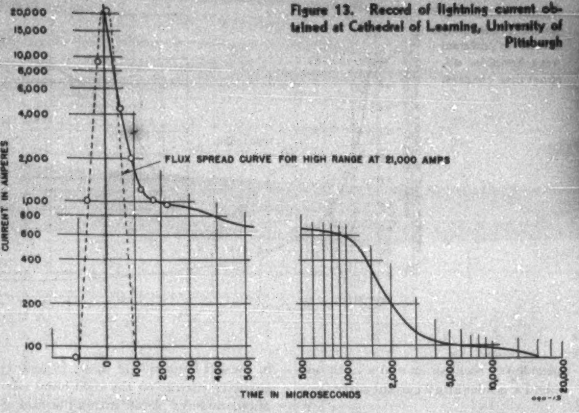


Figure 13. Record of lightning current obtained at Cathedral of Learning, University of Pittsburgh

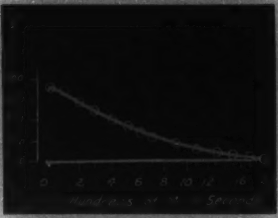


Figure 15. Comparison between fulchronogram and cathode-ray oscillogram for laboratory current surge

indicate the desirability of a different speed for the slow-speed wheels. The high-speed wheel permits of the expansion of the individual waves so that their shapes are discernible. The probability of the high current components of the individual strokes overlapping is not very great. This statement is based on the assumption that the higher-current components have a duration of the order of a few hundred microseconds which covers only a few per cent of the periphery of the wheel.

The presence of the coils for the production of the magnetomotive force naturally raises the question of the impedance of the device and its ability to withstand the voltages that might appear across it when a steep wave front of current is applied. Precautions were taken in its design to limit the inductance by making the distance between the two sides of the loop as small as possible. The coil inductance is equal to 4.0×10^{-7} henry, and its d-c resistance is 0.007 ohm. The device has successfully withstood a current rise of approximately 20,000 amperes per microsecond, and a maximum of 120,000 amperes which represents the limit of test facilities available at the time. The physical forces involved in this kind of structure are too large to be withstood without special bracing. For this reason the windings are substantially supported by blocks of Micarta which completely surround them. These blocks are visible in figure 6.

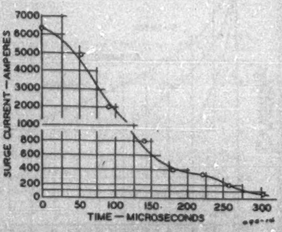


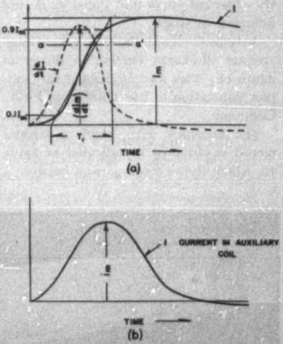
Figure 14. Typical lightning-arrester discharge current at the Gulfport substation of the Mississippi Power Company

The physical arrangement of the magnetic laminations constitutes a very good centrifugal fan which loads the driving motor enormously. To break up the fan effect a strip of paper is cemented to the inner edges of the plates.

Mechanical vibration has a tendency to demagnetize any permanent magnet. Continued operation of the fulchronograph after a record has been obtained has a similar effect. However, the composition and treatment of the magnetic material was selected with this point in mind and it has therefore been kept to a minimum. The curve of figure 11 shows this effect upon a particular wheel for the high range side taken for two degrees of magnetization. Below 10,000 amperes, the effect is relatively unimportant but at 20,000 amperes it is of the order of 20 per cent. However, by insuring that the wheel runs at least one hour before removing, this decrease which becomes constant thereafter can be included in the calibration of the steel.

The current which produced the magnetization is determined by measuring the remanent or residual magnetism in the laminations. At the present time the group of laminations in each slot are removed and measured as a group. The measuring instrument is in turn calibrated against known magnetizing currents. The number of laminations removed is dependent upon the character of the wave. When the rate of change is small it is necessary to remove only every tenth or twentieth group. The region magnetized can be located by means of a small exploring compass. The accuracy of the device is of the order of ten per cent.

A number of fulchronograph installations have been placed in the field for a part of the 1939 lightning season. One of them was placed on the roof of the Cathedral of Learning of the University of Pittsburgh. Atop this 530-foot structure a 50-foot insulated mast was placed and the fulchronographs connected between the mast and the grounded structural iron work. A photograph of this installation is shown in figure 12. One of



(a)

(b)

Figure 16. Typical wave form and its time rate of change

the records of the current in a lightning stroke which have been obtained at this location is shown in figure 13. The enormous range of current and time covered by the device is apparent. It must be kept in mind that the ordinates

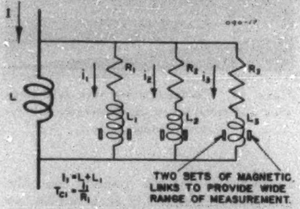

I
L
i_1
R_1
i_2
R_2
i_3
R_3
L_1
L_2
L_3
$I_1 = L + L_1$
$T_{C1} = \frac{L_1}{R_1}$
TWO SETS OF MAGNETIC LINKS TO PROVIDE WIDE RANGE OF MEASUREMENT.

Figure 17. **Schematic diagram of magnetic surge-front recorder**

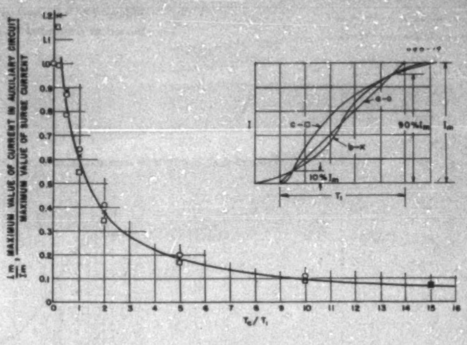
and the abscissas beyond 500 microseconds are logarithmic scales. Below 500 microseconds the abscissa is a Cartesian scale.

In figure 13 the observed points for the first few slots are shown together with the distribution curve from figure 9 corresponding to 21,000 amperes, the crest of the current wave. The zero point of time was determined by fitting the distribution curve for 21,000 amperes to the upward side of the observed readings. A fit indicates a surge front which lies within the envelope of the distribution curve. The observed point at 50 microseconds lies just about on the distribution curve for 21,000 amperes. A curve drawn through the observed points at 90 and 50 microseconds and the current for zero time forms a smooth curve which justifies the original curve drawn. For all other points the distribution curves fall within the observed curve and therefore do not influence its shape.

A typical record showing a lightning-arrester discharge current is shown in figure 14. This was obtained at the Gulfport substation of the Mississippi Power Company.

The accuracy with which a faithful record is obtained is indicated in figure 15, which shows a comparison between a

Figure 19. **Response of auxiliary circuits to different wave fronts for different time constants**



cathode-ray oscillogram and a fulchronogram for a laboratory current surge.

Magnetic Surge-Front Recorder

The principal disadvantage of the fulchronograph is its inability to measure high rates of rise of the front of waves. Devices for measuring the maximum rate of rise have been available for some time. J. F. Peters in the early investigations with the klydonograph first suggested the use of this instrument connected in a loop inductively coupled with the circuit carrying the surge current. Berger[10] later applied the method and published some data. This method gave a reading proportional to the maximum instantaneous rate of rise of current. A quantity probably of greater importance than the maximum rate of rise is that of the average rate of rise such as may be defined by a straight line drawn through the points having values 10 per cent and 90 per cent of the crest value. This is shown in figure 16*a*.

The magnetic surge-front recorder for which a schematic diagram is shown in figure 17 is a device for recording this property of lightning currents. It consists of three circuits containing resistance and inductance having different time constants which are connected across an inductance carrying the main surge current. A photograph of this coil is shown in the left-hand side of figure 18 and its auxiliary circuits on the right-hand side. Magnetic links placed within the field of the inductors of these three circuits serve to record their maximum currents, i_m.

Referring to figure 17 it may be seen that if the resistance of the main coil is negligible, the voltage across it is proportional to the rate of change of the surge current and its inductance L. If the inductances L_1, L_2, and L_3 of the auxiliary circuits are equal to zero the currents in these circuits are all proportional to the voltage across the inductance L and will have the wave shape indicated by the dotted line in figure 16*a*. In this case the maximum currents in all three auxiliary circuits are proportional to the maximum instantaneous rate of rise of the main surge current.

By the introduction of the inductances L_1, L_2, and L_3 the maximum currents in the auxiliary circuits are made to indicate, not the maximum instantaneous values of rate of rise of the surge current, but the average value given by the line *aa′* in figure 16*a*. The current in the auxiliary coils is then of the general form

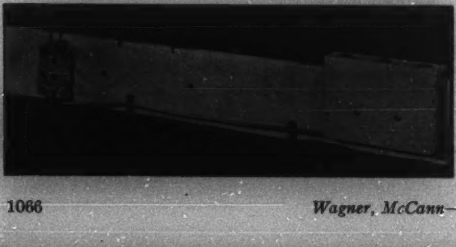


Figure 18. **Magnetic surge-front recorder**

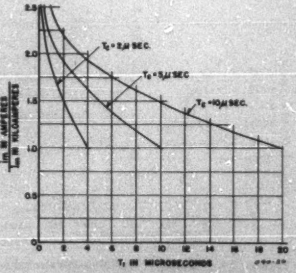


Figure 20. **i_m/I_m as a function of T_1 for fixed values of L, R, and T_c**

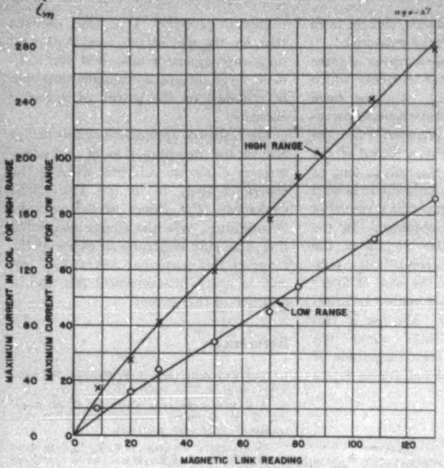


Figure 21. Calibration curves of magnetic links in magnetizing coils

of figure 16*b* varying with the circuit under consideration.

From figure 17 if *I* is the main surge current; i_1, i_2, and i_3 the instantaneous currents in circuits 1, 2, and 3, respectively; and R_1, R_2, and R_3 their respective resistances; then for any of the auxiliary circuits such as circuit 1, a voltage equation such as the following can be written:

$$L\frac{dI}{dt}=R_1i_1+(L+L_1)\frac{di_1}{dt}$$

The solution of this equation in terms of the maximum value of i_1, namely i_{m1}, can be expressed in the form

$$i_{m1}=\frac{L}{R}I_mF\left(\frac{T_{c1}}{T_1}\right)$$

where

I_m = maximum value of I

T_{c1} = time constant of circuit 1 including the inductance L

$= \frac{L+L_1}{R_1}$

T_1 = time to crest of the straight line passing through the 10 per cent and 90 per cent values of current as defined in figure 16

Speaking generally, the extent to which i_m is affected by the nature of the wave front is shown in figure 19. In this figure i_m/I_m is plotted as a function of T_c/T_1 for the three different wave shapes shown in the insert which have the same values of T_1. As T_c/T_1 approaches zero the response i_m/I_m approaches more and more a value corresponding to the maximum instantaneous rate of change of *I* and the spread for the three different waves becomes greater. However, for all practical purposes i_m/I_m is independent of wave shape for values of T_c/T_1 greater than 0.5.

For the specific values of *L*, *R*, and T_c used in the instruments, i_m/I_m can be expressed as the functions of T_1 shown in figure 20 for the range in which T_c/T_1 is greater than 0.5. It can be seen from these curves that if i_m and I_m are known, the duration of the front of the wave, T_1, can be determined. The calibration curves for obtaining i_m from the magnetic-link readings are given in figure 21. It is planned to use this device in conjunction with the fulchronograph which would supply the value of I_m. The purpose of using three circuits instead of one is to increase the accuracy of the instrument. The three curves terminate at values of T_c/T_1 equal to 0.5, because of the inaccuracies mentioned in connection with figure 19 for ratios below 0.5. A similar device can be developed in which the inductance *L* is replaced by a mutually coupled loop.

The foregoing analysis has been based upon neglecting the negative values of dI/dt on the tail of the wave. Since the readings of the magnetic links are dependent to some extent upon reversal of current in the auxiliary coils a limit must be placed upon the permissible negative slope for which sufficient accuracy is attainable. This value is set at a tail slope of about 20 per cent of that of the front slope, a value believed to be greater than that characteristic of most lightning currents.

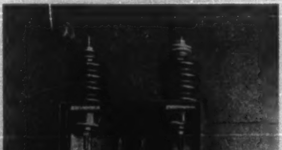


Figure 22. Magnetic surge integrator

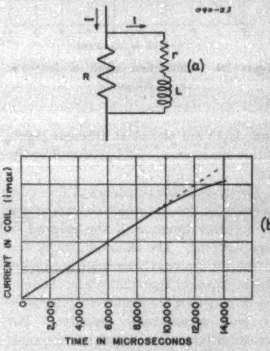


Figure 23. Response of magnetic surge integrator to square-front surge current

Magnetic Surge Integrator

The magnetic surge integrator shown in figure 22 is a relatively simple and inexpensive device for recording the total charge or the integral of the current in a lightning surge. It consists essentially as shown in figure 23*a* of a noninductive shunt which carries the main surge current, across which an inductor is connected.

Neglecting the resistance of the coil the current in it at any instant is equal to the integral of the main surge current and if the surge is nonoscillatory the final maximum value of this coil current is the total integral of the main surge current.

The magnetic field which magnetizes the links is a function of the coil current. Thus from a measurement of the remanent magnetism in the links a record of the total charge in the surge is obtained. To cover a wide range of sensitivity two magnetic links are placed at different points within the magnetic field produced by the coil.

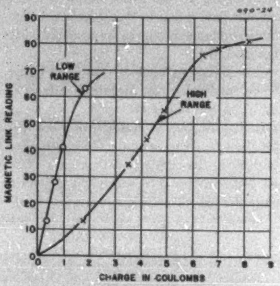


Figure 24. Calibration curve of magnetic surge integrator

The effect of the resistance of the coil is to limit the time for which the response is an accurate measure of the integral of surge current. The extent to which this is true for the constants used in this device is shown in figure 23*b* where the relation is given for the response of the surge to unit square-value impulse. For periods of time up to 10,000 microseconds the response of the coil is essentially linear. This device will therefore accurately integrate unidirectional surges which do not last longer than 10,000 microseconds. In figure 24 are given the calibration curves for one of these instruments having a shunt resistance of 0.05 ohm, designed to be used in transmission and distribution circuits. It is seen that for this value of resistance, charges from 0.1 to 8.0 coulombs can be measured.

In figure 25 are shown curves of three general types of surge currents which might be encountered. On the same curves are given current-response curves of the magnetic surge recorder. Since the magnetic links will give a record of the maximum current in the coils as long as the current is of one polarity it is seen that in the case of figure 25*a* a record of the total integral is obtained. In the case of figure 25*b* the integral of the initial predominant component only will be obtained. In the case of figure 25*c* the record obtained cannot be interpreted and is useless. The use of two surge-crest-ammeter links serves not only to measure the crest magnitude of the main surge current but also to detect change in polarity. It is thus thought that by the use of the combination of surge-crest-ammeter links and the magnetic surge integrator valuable data can be obtained on the nature of lightning currents.

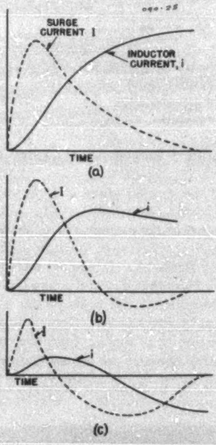


Figure 25. Response of magnetic surge integrator to different wave forms

The principal application of this device lies in the determination of lightning-arrester discharge currents, wherein the charge is likely to be less than eight coulombs. For direct strokes of the "continuing" type the charge associated therewith may be greatly in excess of the range of this particular instrument, not only in charge measurement but also in duration of the current.

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10. Resultats des Mesures Effectures au cours des orages de 1934/1935, **K. Berger.** Association Suisse Des Electriciens *Bulletin*, volume 27, number 6, March 20, 1936, pages 145–63.

Discussion

For discussion, see page 1253.

Discussion Section

¶On the following 195 pages appear all previously unpublished discussions of 1940 Transactions papers.

¶This segregation reflects a mid-year transition in publication policy adopted to simplify and expedite the Institute's publication procedure.

¶All discussions are fully correlated through both the subject index and the author index.

Corona Discharge on Rubber-Insulated Cables

Discussion and authors' closure of paper 39-108 by E. B. Paine and H. A. Brown, presented at the AIEE Middle Eastern District meeting, Springfield, Mass., May 3–5, 1939, and published in AIEE TRANSACTIONS, 1940, pages 709–14.

G. M. L. Sommerman (American Steel and Wire Company, Worcester, Mass.): The writer's discussion of the interesting paper by Professors Paine and Brown is concerned chiefly with the graphs on the right side of figure 2. These graphs depict ozone formation and corona current as functions of the voltage applied to rubber-insulated cables on flat metal test plates for two conditions: (1) the corona produced between the cable and the curved ends of the test plate is included in the results; (2) only the corona produced along the test plate where the insulation is in firm contact with the test plate is included in the results.

For condition (1) ozone formation begins at a voltage which is the same as that at which corona current begins (less than three kilovolts). For condition (2) ozone formation does not begin until the applied voltage is nearly five kilovolts. This voltage is the same as that given in table I for the visible corona forming voltage for this cable (*P*).

This raises the following questions: (1) For cable in firm contact with a metal electrode, is ozone formed only when visible corona is observed? (2) Is visible corona observable in the space between the cable insulation and the curved ends of the test plate when the applied voltage is slightly above the "corona starting voltage" but less than the "visible corona starting voltage" given in table I? (3) If the corona in the space between the cable insulation and the curved ends of the test plate were eliminated by filling this space with solid insulation such as wax, would corona current be measurable at voltages less than that necessary to produce visible corona? These questions apply, of course, only to cables for which no "internal corona" is possible, that is, smooth unbraided cables in which the insulation adheres to the conductor.

While it is true that in practice the cables are not in firm contact with the conduit at all points, it would be desirable to have the answers to the above questions as they are of fundamental importance in the study of ozone formation and corona cutting of insulation.

In previous reports of the work carried out at the University of Illinois (reference 1 in the paper, page 37 and the following), data were presented showing the deterioration caused by corona on rubber-insulated cables. The writer suggests that it would be valuable to extend these studies to cases where the cables are surrounded by an inert gas, such as nitrogen, instead of air. The experimental setup portrayed in figure 1 of the present paper would lend itself admirably to such studies. These tests, in conjunction with tests made in air, would show the importance of pure corona cutting as compared with that of ozone as causes of deterioration of rubber insulation. This question is of interest in connection with the deterioration which might occur in cables in which the rubber does not firmly adhere to the conductor, because, as the authors have found, air does not readily permeate the spaces between the conductor and the rubber.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): It is of interest to compare the conclusions of the authors with Insulated Power Cable Engineers Association standards.

"The commercial two-kilovolt cables with ozone-resisting rubber insulation both with braid and without braid and its saturant are in general, properly rated as to voltage from the corona starting point of view. The five-kilovolt cables are close to the nominal rating."

IPCEA requires no shielding for voltages up to 6,000 volts.

"The 12- and 14-kv cables which were tested appear to be overrated since corona discharge occurs below the normal operating voltage."

IPCEA requires shielding on braided cables for 6,001 volts and over.

The authors' tests confirm the IPCEA standard with respect to the two groups of ratings considered.

A further confirmation of the correctness of their findings is that corona trouble is experienced in some but not all 5,000-volt lines. The authors' tests therefore confirm practical experience in that respect.

I am interested to note that the authors give no data on internal corona. E. J. Merrell of our laboratory has found that an oscilloscope wave of the charging current reveals whether ionization is internal or external or both. If the high-frequency harmonics appear on the peak of one half wave the corona is internal, if on the other, it is external, and if both half waves show high frequency harmonics, there are both internal and external corona. It is easy to find which half wave corresponds with a particular kind of corona by substituting a sample of known corona characteristics. Figure 1 of this discussion shows a typical oscillogram of rather mild unilateral corona discharge. The high-frequency harmonics sometimes appear as distinct surges which may have an amplitude several times that of the fundamental wave, and sometimes as mere fuzziness of the fundamental wave. The former were found when ionization occurred at a metallic surface such as conductor or shield and the latter when no metal or other high emissivity electrode was present.

The oscilloscope test therefore possesses certain advantages over that used by the authors in enabling an analysis of the location and type of ionization to be performed.

We have found that when external corona becomes sufficiently violent it gives rise to internal corona at voltages which will not form internal corona if there is no external corona. This is shown by both the above described wave peculiarities and by the fact that cables with external corona often fail by internal ozone-cutting at voltages which are quite harmless if the external corona is suppressed by shielding. This is due, probably, to the greater potential drop over the internal air films, at the higher harmonic wave frequencies.

It is unfortunate that the authors do not give the specific inductive capacity of all their insulating compounds. With this information, it would have been possible to check the several theoretical formulas which have been developed, connecting ionization point with specific inductive capacity and the geometry of the cable.

The authors make a point that ionization occurs in many cables at a voltage below their normal operating voltage. This sounds alarming but is not really so, as ionization tends to die out and requires a higher voltage to re-establish it. This phenomenon occurs in paper and varnished-cambric insulation as well as rubber. Furthermore, a mild corona such as shown by a fuzzy wave of charging current is absolutely harmless. The authors' method undoubtedly reveals this fuzzy wave as ionization current. It is not until definite vertical lines show on the wave that ionization begins to be destructive.

E. B. Paine and **H. A. Brown:** Replying to the points raised by Doctor Sommerman: (1) this question cannot be definitely answered by the authors at present. Before an answer can be given it would be necessary to eliminate completely the corona at the curving away portions of the test piece outside the glass enclosing tube. The insertion of wax or paraffin probably could not be relied upon to eliminate all possible chance for the existence of minute air spaces

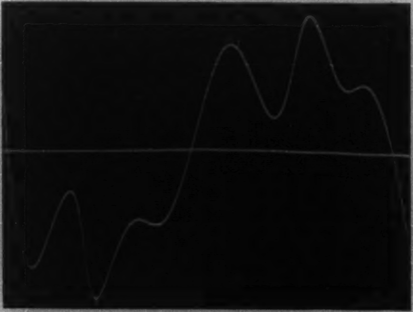

Figure 1

at the critical separation of insulation surface and metal test plate. However, a properly designed oil cup or trough could be cemented to the enclosing tube end cover plates, so that the curving away region could be kept under oil. The authors found that if a metal trough is substituted for the test plate and the test piece submerged in oil, all detectable corona discharge was completely suppressed, even though it was previously accompanied by full visibility. (2) In this investigation "visible corona starting voltage" was actually the point at which the corona became visible along the entire length of the test piece, including the sections of firm contact, as mentioned in the paper. On a number of test pieces definitely audible discharges, giving a small meter reading, were obtained. (3) We have discussed this under point (1); we feel that oil would be better than wax for this purpose.

Regarding the comments by Mr. Del Mar, the authors have previously made tests for internal corona, that is, for ionization discharges which may exist either between the conductor surface and the rubber covering, or in porous spaces which may occur in the insulation material. This was done by immersing the test piece in a salt-water bath, providing suitable pot heads at the points where the test piece entered the water. The authors wonder if the oscilloscope setup used by Mr. Del Mar provides a detector of *high sensitivity*. In the test equipment used by the authors a potential of 3×10^{-6} volts applied to the input of the preliminary amplifier produce a readable deflection of the output meter. In some cases the disturbances due to the corona discharge are minute as compared with the 60-cycle charging current at the starting point of detectable corona, the ratio being less than one to 1,000. Even with the high sensitivity mentioned the output level is sometimes so low at the starting point of corona discharge that the output meter shows no appreciable deflection, the discharges are merely heard in a high-sensitivity headset.

Characteristics of Restricted Ionization and Its Relation to Voids in Insulating Materials

Discussion and authors' closure of paper 39-109 by C. L. Dawes and P. H. Humphries, presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3–5, 1939, and published in AIEE TRANSACTIONS, 1940, pages 701–08.

J. D. Cobine (Harvard Graduate School of Engineering, Cambridge, Mass.): The authors have presented a comprehensive study of the performance of a composite dielectric, under high electrostatic stress, such as may exist, for example, in a cable or the slot insulation in an alternator. The consistency of the experimental results is evidence of the precision with which the over-all characteristics of insulation may be determined by the Dawes-Hoover bridge. In view of the uncertainties always involved in studies of a-c gas discharges, the material presented agrees remarkably well with the results of d-c investigations.

Since one electrode of the air gap is of metal and the other is of glass, the experimental arrangement simulates the conditions for an air film between solid insulation and either the conductor or the sheath of the cable or any void in which conducting material forms one electrode. This arrangement would be expected to exhibit a polarity effect in the current wave. There appears to be no essential difference in the positive and negative half-cycles of current in figure 5. It would be interesting to learn if this is true for all conditions of voltage, current, gap, and pressure. It would seem valuable for the authors to investigate the characteristics of an air gap where both electrodes are of the same insulating material, so as to simulate the conditions where voids occur *within* insulation.

Although the gaps used are greater in thickness than the air films existing in cables, the experimental results can be referred to the shorter gaps if all the requirements of the similarity law are fulfilled. It is evident that the experimental arrangement, of carefully guarded plane parallel electrodes, is ideal from this standpoint. However, extreme caution must be exercised in determining the actual voltage across the air gap when a discharge exists for part of the cycle. The presence of a gas discharge results in space-charge distortion which does not completely disappear immediately after the discharge is extinguished. The gas discharge itself acts as a sort of valve in charging the solid dielectric. When the metal is cathode, electrons collect on the surface of the insulation and remain there during the nonconducting period, if the very small leakage current is neglected. This surface charge on the insulation establishes a high electric field across the gap while the applied voltage is decreasing, and the gap breaks down in the reverse direction while the applied voltage is relatively low, as is shown by the oscillogram. The electron charge is then replaced by a positive-ion surface charge and the process continues. Under such conditions the division of the applied voltage between the two series members of the composite dielectric would be expected to depart from the values determined solely by the capacitance of the two members. An examination of figure 9 reveals that the values plotted, that is, maximum gap voltage (determined by capacitances) divided by the gap length, do not quite satisfy Paschen's law, which states that the spark breakdown voltage is a *function* of the product of gap length and gas pressure. It is reasonable to believe that this discrepancy is due to the distorting effects of space and surface charges. Future work by the authors will undoubtedly be directed toward an experimental investigation of the actual voltage across the air gap and a determination of the effects of space and surface charges.

L. W. Marks (General Electric Company, Pittsfield, Mass.): The authors of this paper are working on a problem which should be of great interest to manufacturers of electrical insulation but which, unfortunately, has been the subject of far too little investigation. They indicate that their work is to be continued and this should prove of real value to a more complete understanding of the mechanism of ionization in narrow, restricted gaps.

I would like to ask the authors if the characteristics of the discharge would be altered if the ionized space were entirely enclosed as in a void in a cable. A continuous stream of air is said to pass through the apparatus shown in figure 4, and it seems possible that this may alter the ionized gas or sweep away the space charge. Would it not be necessary to make measurements in a completely enclosed space in order to obtain conditions more nearly comparable with those occurring in the gas pockets of cable or other insulation?

I have found from radio noise measurements on gas pockets in organic fabricated insulation samples blistered for test purposes by excessive heat and vacuum, that when the completely enclosed space is ionized, the effects of this ionization may exist for minutes, and in some cases even hours, after the voltage has been removed. Residual ionization was evidenced by a much lower critical gradient following the initial ionization. Moreover, if the void was vented mechanically, the critical gradient was increased to its original value.

Measurements at radio frequencies completely eliminated the 60-cycle component, and in most cases gave a sudden and distinct starting value, indicating the occurrence of cumulative ionization. Once ionization was precipitated, it was possible to reduce the voltage to as low as one-eighth the original starting value before secondary ionization ceased. It appeared that space charge played a prominent role in this action and may have become bound on the insulating surfaces of the void after the removal of the initiating voltage.

As shown by the authors, the critical gradient-gap length curve rises at small gap spacings in accordance with the law of Townsend, Peek, and others. In my radio-frequency measurements, the smallest gas-pocket thickness was eight mils or 0.0203 centimeter. The critical starting gradient for ionization was found experimentally to be 92.5 kv crest per centimeter. Substituting in equation 1, page 5 of the paper, we obtain

$$G = 30 + \frac{1.35}{0.0203} = 95.4 \text{ kv crest per centimeter}$$

which is in close agreement with experiment. On the other hand, substitution in equation 3 results in the unreasonably high value of 273 kv per centimeter and indicates that this equation does not hold in the region of small gap lengths below the range studied in this paper.

G. C. Nonken (General Electric Company, Pittsfield, Mass.): The authors are to be commended on their methods of test and the manner in which they have presented their findings on this complex phenomena of restricted ionization. They chose to restrict their measurements to the 60-cycle or fundamental-frequency characteristics of the corona or ionization. The ionization current is, however, made up of a series of random pulses which contain very high frequencies. The authors' oscillogram of corona current, figure 5, indicates the presence of high-frequency current.

The writer has conducted some tests on restricted corona in a 0.05-inch-thick air gap in series with a 0.2-inch-thick glass plate. The electrode against the glass plate was conducting material painted on the glass. The electrode next to the air gap was a 10-centimeter-diameter flat brass disk. These tests were made in dry air at atmospheric pressure.

Figure 1 of this discussion shows oscillograms taken of the corona current with a cathode-ray oscillograph. This oscillograph with attached amplifier is capable of giving true reproduction of frequencies up to 90,000 cycles, although the photography was not capable of reproducing such high-frequency traces. The preionization charging current was balanced out and does not appear in the oscillogram. The writer's experience does not check the authors' figure 5 as to the phase of the corona current with respect to the voltage. It has been observed that the corona current in voids always is in phase or leads the crest of the voltage. It would appear from past experience that the current and voltage in figure 5 are 180 degrees out of phase with their true phase relation.

Figure 1. Corona current in a 0.05-inch air gap in series with a 0.2-inch-thick glass plate

(*A*)—Current at 4.5 kv root mean square, 60 cycles

(*B*)—Current at 6.75 kv root mean square, 60 cycles

(*C*)—Current at 9 kv root mean square, 60 cycles

High-frequency measurements were made on the corona current of the above test piece with 60-cycle voltage applied. The tests were made with a tuned high-frequency meter across a resistance in series with the sample. The results showed that the corona current contains frequencies from at least as high as 20 megacycles down to the audio frequencies.

For the particular exterior power circuit used, the magnitude of the high-frequency current was greatest at 1.5 megacycles and diminished to about half the maximum value at 20 megacycles and at 0.16 megacycles. As the total voltage across the test piece raised from ten per cent above the corona starting voltage to twice that value, the high-frequency component of the corona current increased eight to ten times. The 60-cycle component of the corona current is nearly proportional to the first power total voltage 60-cycle voltage across the sample.

The writer believes, that although the authors have shown the 60-cycle component of the ionization current follows definite simple curves when plotted against different variables, the restricted ionization is more definitely a high-frequency phenomena. High-frequency measurements would add much to the study which the authors have so ably conducted at power frequencies.

G. M. L. Sommerman (American Steel and Wire Company, Worcester, Mass.): The paper by Professors Dawes and Humphries is a valuable contribution to the knowledge of ionization in its relation to voids in dielectrics.

In figure 6 of the paper, data are presented showing the voltage gradients across the air space as a function of the current density in the air space. Above a critical gradient G_0, the current increases more and more rapidly, and the gradient passes through a maximum G_m and then decreases with further increase in current. The authors liken this behavior to that reported by Engel and Steenbeck. For uniform fields and atmospheric pressure, however, it would seem that the ionizing current should be extremely small until the gradient reaches the breakdown strength of the air space, at which point the whole air space should suddenly discharge, resulting in a sudden increase in current corresponding to the electrostatic energy stored in the air gap, $^1/_2CV^2$. This would occur four times in each cycle of the applied wave resulting in a current increase of $4fCV$.

Actually with the experimental setup used by the authors, the field is not entirely uniform because the space between the guard and active low voltage electrodes ($^3/_{32}$ inch) is large compared with the thickness of the air space (0.08 to 0.4 centimeter), and stress at the edges of the low-voltage electrode is greater than the average stress. When the stress at the electrode edges reaches the ionizing value, local discharges begin, and the current would be expected to increase only gradually with further increase in stress. Thus, this might explain the shape of the voltage-current curves obtained, and could even account for the variations in G_m-G_0 with variations in spacing and pressure noted by the authors.

The data given by S. Whitehead (reference 13 in the paper) most nearly corresponding to the range of gap lengths investigated by the authors would seem to be that given in diagram III, page 43. For this, the expression for sparking voltage is

$$G=31.2+1.6/S$$

This is greater than that given by the authors in equations 15 to 17 although less than that indicated by equation 14 for the shortest gap. Incidentally, the writer would like to know if he is correct in assuming that equations 10 to 17 were derived on the basis that the coefficients of the last terms are obtained from G_m-G_0, in accordance with the suggestion made by the authors earlier in the paper.

C. L. Dawes: Both Doctor Cobine and Mr. Marks bring out the fact that these experiments were conducted for a gap bounded by an insulating surface and a conducting electrode, and that the results might be different if both boundaries of the gap consisted of insulating surfaces, such as ordinarily occurs within insulation. The authors agree that with both boundaries of the gap consisting of insulating surfaces the results might be somewhat different and also that conditions within insulation are more nearly simulated. However, the results given in this paper are merely the beginning of an extended research and it seemed rational to commence with the more simple arrangement. Also, it was felt that with one conducting electrode, conditions would more nearly simulate the conditions of breakdown tests between flat parallel electrodes, particularly in the ionization and glow discharge regions, figure 2 of the paper. The close agreement of our results with the breakdown values of Townsend and others indicates, we believe, that we have simulated such conditions quite closely.

Further investigation is still being conducted and it is planned to extend the research to the measurement of gaps of very short lengths and to gaps which are bounded on both sides by insulating surfaces. Both of these investigations are suggested by Doctor Cobine and Mr. Marks as well as by several of the reviewers. Also, Mr. Marks asks if the characteristics would be altered if the ionized space were entirely enclosed as in a void in a cable. We have not conducted experiments with insulation on both sides of the gap, but we have conducted preliminary tests with the metal electrodes and glass in which the gap was sealed so that the products of ionization could not escape. We found that the characteristics of the gap not only varied with time but that tests did not repeat themselves very well. There has not been time to carry this particular investigation further but it is planned to do so in the near future.

Also, for the investigation of very short gaps small electrodes capable of precise adjustment of the air gap, and a bridge designed specifically to measure the characteristics of such short gaps are now under construction.

Doctor Cobine mentions the valve effect of the electrons and the positive ions which collect on the insulating surface and remain over into the next cycle. There is no doubt that this effect exists, as is shown by the dissymetrical current waves shown in figure 2 of this discussion. For example, we have applied high-voltage direct current to such a gap and at the instant of application of voltage the gap is momentarily ionized, which usually may be seen by a flash of corona. This is due to the fact that with instantaneous application of voltage to the system, the voltages across the glass and gap are distributed inversely as their capacitances, and the gradient across the gap is sufficiently great to produce ionization. However, the free charge on the glass surface is immediately attracted to the metal electrode because the sign of the charge on the electrode is opposite that on the free charge. The charges on opposite sides of the gap then become of the same sign and the gradient is reduced to a value far below the ionization gradient. This condition persists for two or three minutes during which time the bound charge on the glass leaks to the electrode to which the glass is attached. This gradually raises the gradient across the gap until ionization again occurs momentarily and the cycle repeats itself. This shows that charge does remain on the insulating surface. With alternating current, however, this charge is removed as soon as the potential of opposite sign on the metal electrode becomes sufficiently great to attract the charge to itself. There is, however, some slight rectifying action as Doctor Cobine suggests. This is illustrated by figure 2, which shows an oscillogram in which the positive and the negative current waves are different in shape. The authors realize that space charges do have some effect on the gradient within the gap and state this fact in the paper. This undoubtedly has some effect on the breakdown gradient, for our breakdown values show some departure from Paschen's Law when the product of pressure and gap length are considered, as pointed out by Doctor Cobine. This effect cannot, however, be large because of the close correlation of our maximum gradients with breakdown values that have been obtained by others.

Figure 2

Volts, 11,600 root mean square; 45 cycles; gap, 1.59 millimeters

Upper trace—voltage; lower trace—current

Doctor Cobine points out that the gap breaks down at a relatively low voltage as shown by the oscillogram and this is due to the residual charge which remains on the surface of the glass in the manner just described for direct current. When the voltage reverses, the sign of this residual charge is such as to raise the gradient across the gap, causing ionization to begin at a lowered value of impressed voltage. The residual charge remains until the potential on the metal electrode is sufficiently great to attract it across the gap. It is difficult to evaluate this effect but again it cannot be large since the values of maximum gradient obtained by us are in such close accord with experimental breakdown values.

Mr. Nonken is correct in pointing out that the voltage wave in figure 5 of the paper is 180 degrees out of phase. An oscillograph vibrator was inadvertently reversed and the error was not noticed until too late to make correction. Mr. Nonken's investigations of the very high-frequency components of the corona current, made under conditions quite similar to ours, are very interesting and add to the knowledge of ionization phenomena. We have taken a large number of oscillograms of the corona current under different conditions and with any substantial ionization they showed frequencies from the fundamental up to the limit of the vibrator response as may be seen from figure 2. Accordingly, we realized that the frequencies must extend well up into the radio-frequency range. We did not know, however, that the maximum high-frequency current occurred at such a high value as 1.5 megacycles. We agree with Mr. Nonken that much could be learned in the high-frequency range, but measurements under the pulsating conditions of the high-frequency current will not be easy. In fact, these 60-cycle measurements are not simple. Also, the power-frequency voltage across the gap can be sinusoidal such as a 60-cycle wave and the harmonic frequencies in the current contribute no power with respect to this voltage. This, again, would make difficult the measurement of high-frequency power.

Doctor Sommerman, in his discussion, suggests that with uniform gaps the ionizing current is small between the gradients G_0 and G_m, figure 6, and when the gradient reaches G_m there is a sudden increase in current due to the electrostatic energy stored in the air-gap, ${}^1/{}_2CV^2$. Our results show that the ionization current is small but appreciable between the gradients G_m and G_0 but its rate of increase is increasing rapidly due to the increasing rate of ionization. We have investigated the literature and discussed with physicists the nature of the phenomena occurring in this region and there appears to be no general agreement. A number of proposed theories is given in the paper. If the voltage across the gap is maintained constant, that is, if the gap is connected across an "infinite bus" the energy stored in the capacitance of the gap ${}^1/{}_2CV^2$ would have no effect, for like the capacitance, the "infinite bus" can deliver an infinite current, but it can sustain this current for any time whatsoever, which the capacitance cannot do.

Under the conditions of these experiments, the energy that can be delivered when discharge occurs is limited by the series impedance of the gap. It is probably true that with rapid increase of current at breakdown with no series impedance, the current-voltage characteristic will not be identical to the characteristic when the current flow is limited and held at definite values as in these experiments.

The space between the guard and ring as used by us could probably be reduced. The edges of the plate and guard were slightly rounded and no local discharges at these edges have been noted. The variation of breakdown with electrode spacing which Doctor Sommerman attributes to this space, is a well-established fact and therefore the authors do not attribute the variation of G_m with spacing to this space between the plate and guard.

The coefficients of the last terms in equations 10 to 17 were obtained from a considerable amount of data and curves, all of which could not be included in the paper because of space limitations. The value of G_0 corresponds to the point where the current-voltage gradient curve departs from the straight-line relation. However, a more sensitive method for obtaining this point is to take the value of gradient in the calculations, at which an energy component of voltage first appears. Doctor Sommerman is correct in pointing out that diagram III, page 43 in Whitehead is more in accordance with our gaps than diagram II. The gradient equation which he gives is more nearly in accordance with our equations 14 to 17 than our equation 3.

The Rectifier Calculus

Discussion and author's closure of paper 39-101 by W. Melvin Goodhue, presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3–5, 1939, and published in AIEE TRANSACTIONS, 1940, pages 687–91.

F. O. Stebbins (General Electric Company, Schenectady, N. Y.): The rectifier calculus as described in this paper makes available to the designing engineer a greatly simplified method for computing many of the characteristics of mercury-arc rectifiers such as output direct voltage, regulation, etc. We have checked some of the formulas developed in this paper and find them to be entirely correct for balanced supply-system voltages. Also they present a considerable saving in time and effort over conventional mathematics. In addition these formulas are no more complicated when the supply-system voltages are unbalanced whereas the solution of unbalanced rectifier circuits with conventional mathematics is somewhat laborious.

We note that the author does not describe any simplification in the method used for calculating rectifier wave shapes and power factor and would like to know if he has attempted to develop his mathematics to include these characteristics.

The author points out that the rectifier calculus has not been completely developed for all the different possible rectifier applications, and also that it can be used in other fields such as for example differential and transcendental equations.

C. C. Herskind (General Electric Company, Schenectady, N. Y.): The operational method proposed by Mr. Goodhue in his paper "The Rectifier Calculus" provides a new tool for calculating rectifier characteristics and behavior. While the methods used at present for making calculations on rectifier circuits are usually quite simple and adequate, the increasing growth of rectifier applications will undoubtedly bring complex problems where the operational method will prove useful.

The calculations commonly made on all rectifiers cover voltage regulation, power factor, efficiency, and wave form. These calculations usually involve a very simple formula derived from an analysis of the physical action in the rectifier and its circuit. Inasmuch as such an analysis of the physical action is quite valuable in studying rectifier behavior, the operational method will probably prove useful only in cases which are so complex that the physical action cannot be readily analyzed.

In order that a rectifier circuit be suitable for commercial application, it must have the proper characteristics not only over the normal load range but also over the full range from absolute zero load to dead short circuit. In the determination of rectifier behavior out of the normal range, unique characteristics, such as, rise in voltage at light load, pulsating current on noninductive load, prestarting due to excessive overlap, unbalance due to mutual reactance efforts and so forth, are frequently observed.

Perhaps the operational method may be a useful tool in handling some of these problems. I would like to ask Mr. Goodhue if he has applied his operational method to any of these problems.

A. Schmidt, Jr. (General Electric Company, Schenectady, N. Y.): Mr. Goodhue has given us a novel treatment of repeated transient problems yielding a useful form of solution, particularly for unbalanced conditions. The treatment of the effect of commutating reactance especially has been simplified. The assumptions which have been made to apply the solutions of full-wave cases to half-wave circuits should not affect the validity of the results in the linear range. It will be interesting to see the method extended to cover the nonlinear range of rectifier regulation, the calculation of peak currents during arc-back and like problems.

While the present physical concepts and associated mathematics will continue to be used for many problems, it is quite possible that Mr. Goodhue's rectifier calculus will find considerable use in due time.

I. K. Dortort (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): Mr. Goodhue has brought to the attention of rectifier engineers a relationship which should prove very useful in the analysis of rectifier performance under various conditions of unbalance. The fact that this relation is very apparent once it has been seen adds considerably to the value of this contribution.

The entire "calculus" is built around an operator S, which, I believe, evaluates the rectified value of the voltage difference between successively firing anodes. It is difficult to differentiate between operators and shorthand notations. It might almost be stated that operators are shorthand symbols for involved operations, that lend themselves readily to mathematical manipulations and transformations. I would prefer to consider S in the category of convenient descriptive symbols.

It appears that considerable simplicity and usefulness is sacrificed in attempting to apply S analytically. The use of operators is really justified only where the mathematical treatment of the subject becomes so involved that the human mind has difficulty in visualizing the physical concept of the mathematics or cannot readily follow the various quantities through all the steps. Most engineers will agree, and probably mathematicians too, that operators, while producing results quickly and mechanically, tend to destroy the physical concept which is so important in avoiding errors.

I think the paper itself will bear me out in this. Compare the proof of the transformation theorem given in the appendix to the beautiful and simple explanation afforded by figure 2a. The area beneath the positive envelope of the unbalanced anode voltages, divided by time, is the average direct voltage of the rectifier. It is quite obvious that this area is equal to the area between the negative envelope of these voltages and neutral. The direct voltage in the case of a three-phase three-anode rectifier is therefore equal to one-half the total area between the positive and negative envelopes divided by time. Using the three-phase connection shown in figure 5, the direct voltage will, of course, be twice as great or equal to the total area between the two envelopes divided by time.

The cross-hatched areas show very clearly that the total area can be obtained by summing the areas between e_a and e_b between limits 6 and 3; the area between e_b and e_c between limits 2 and 5, and between e_c and e_a between limits 4 and 1. At each of these limits the voltage difference between two anodes becomes zero, and, as pointed out in the paper, the contribution of each of these areas to the direct voltage, in the case of pure sine waves, is equal to the peak voltage difference divided by π.

By drawing similar pictures for various types of waves and unbalance conditions, it becomes apparent at once that equation 4 does not hold true when there are even harmonics present; nor when some of the anode voltages are so low that no part of their voltage wave is contained in the direct-voltage wave; or when the direct-voltage wave drops to zero for a finite interval of time.

If a graphic proof of the theorem is not sufficient, a rigorous analytic proof is available which is more readily followed than the one given in the appendix of the paper. The direct voltage can be expressed in the conventional manner as follows:

$$V_{DC}=\frac{1}{2\pi}\left[\int_{\theta_{p1}}^{\theta_{12}} e_1 d\omega t+\int_{\theta_{12}}^{\theta_{23}} e_2 d\omega t+ \int_{\theta_{23}}^{\theta_{34}} e_3 d\omega t \ldots .+\int_{\theta_{p1,p}}^{\theta_{p1}} e_p d\omega t\right] \quad (1)$$

in which the e's are the phase-to-neutral voltages, θ_{12}, θ_{23}, etc. are the points of intersection of e_1 and e_2, e_2 and e_3, and so on. The limitations listed above must be written into this equation for any specific case. (See figure 1 of this discussion.) This equation can be readily transformed by the transposition of the limits of integration to

$$V_{DC}=\frac{1}{2}\cdot\frac{1}{2\pi}\left[\int_{\theta_{p1+\pi}}^{\theta_{p1}}(e_1-e_p)d\omega t+ \int_{\theta_{12}+\pi}^{\theta_{12}}(e_2-e_1)d\omega t+\ldots .+\int_{\theta_{p-1,+\pi}}^{\theta_{p1}}(e_p-e_{p-1})d\omega t\right. \quad (2)$$

or

$$V_{DC}=\frac{1}{2}\cdot\frac{1}{2\pi}\sum_{n=0}^{n=p}\int_0^{\pi} e_{n,n-1} d\omega t \quad (3)$$

in which index $n-1=0$ refers to phase p and $e_{n,n-1}$ is the difference of successive phase voltages referred to their intersections for zero time. If any of the limitations are violated, additional terms will appear in equations 2 and 3, and cannot be overlooked.

It appears, then, that S is shorthand for each term of the above equations; in the author's paper it is used at various times to designate a curve, an area, and an average ordinate.

The application of equation 4 to the derivation of reactance drop does not follow

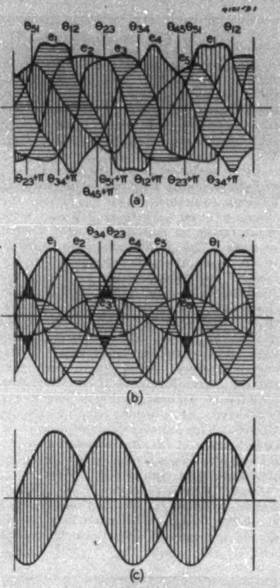

Figure 1

logically from the definition of S or from equation 4. In equation 6 the sum of the commutating reactances is multiplied by a direct current. There is nothing in what has gone before to justify such a procedure, and the explanation given in (6a) is merely the application of the older method of computing the area lost through overlap, applied to each individual overlapping period.

In this respect, it should be noted that if equation 6 holds true, the reactances must be the commutating reactance per phase and not from phase to phase. If the reactances are measured phase to phase, the right-hand side of the equation must be divided by 2. The rigorous derivation is as follows:

Reactance drop $d=$

$$\frac{1}{T}\left[\int_0^{\mu p1} L_{01}\frac{di_{p1}}{dt}dt+\int_0^{\mu 12} L_{02}\frac{di_{12}}{dt}dt+\ldots .\right] \quad (4)$$

If I is smooth,

$$d=\frac{I}{T}[L_{01}+L_{02}+L_{03}+\ldots .]$$

or

$$d=\frac{I}{2T}[L_{01}+L_{02}+L_{02}+L_{03}+L_{03}+\ldots]$$

$$=\frac{I}{2T}[L_{12}+L_{23}+L_{34}+\ldots .] \quad (5)$$

If the e's are sinusoidal,

$$d=\frac{1}{2}\cdot\frac{I}{2\pi}[X_{12}+X_{23}+X_{34}+\ldots .] \quad (6)$$

S cannot be applied as the reactance drop does not meet the requisites implied in the transformation theorem, although the final equations 5 and 6 resemble equation 4 of the paper. (See figure 2 of this discussion.)

In the expression for grid control, equation 7, the author states, and verifies in figure 3*b*, that when the release of the anode is delayed by grid control or other means, the voltage between adjacent anodes will still be integrated over a full 180 degrees, even though this integration will now include a negative part of the curve. This is a contradiction of what appears to be the only logical definition of *S* as an operator. We have here another case where the correct result is obtained because the answer was known beforehand. Figure 3 of this discussion shows the trick that must be resorted to to obtain the correct answer. There is certainly nothing in the definition of *S* or in the general theorem to justify this step.

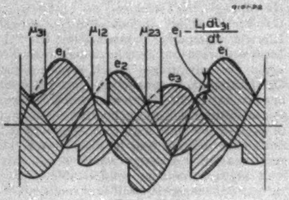

Figure 2

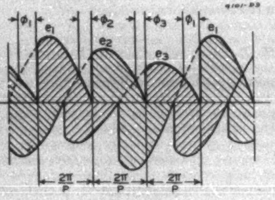

Figure 3

As in the case of overlap, the negative voltages and envelope must be assumed to be modified by grid control in the same manner as the direct-voltage wave. In the case of sinusoidal voltages this gives the same result as shifting both limits by an amount ϕ, but is more in keeping with the concept of *S* as the rectified anode-to-anode voltage.

Another point must be taken into account when calculating the effect of grid control under unbalanced conditions. Whether the phase angle or the magnitude of the phase voltages are unbalanced, or both, the intersections of the phase voltages will be unevenly displaced. Practically no grid-control systems in use today will have the releasing point of the anode displaced by the same amount. In other words, it will be necessary in each case to determine for each anode the actual amount of delay that will prevail under unbalanced conditions, and to calculate the drop in direct voltage for each anode individually.

It will be noticed that all the equations developed so far apply only to systems having all cathodes or all anodes tied together and having only one transformer neutral. When the system is split up by the use of interphase transformers, each group of phases obeys the above equations as if it were a complete rectifier system in itself. In other words, the number of phases, phase displacements, and output current are taken for that part of the system alone. Phase voltages are measured to the neutral of that group and not to the negative terminal of the rectifier unit.

Looking at it this way eliminates the necessity of visualizing the operator *S* as averaging the voltages of two or more different systems, which is another function not contemplated in the original definition of *S*. Furthermore, the voltages of the various groups will in all practical cases be equal to each other, and therefore need not be averaged. If these voltages are not equal, the load will be divided in accordance with the regulation of each individual group, and the correct answer is not obtained by a simple averaging of the output no load voltages. In such cases also it would be necessary in each individual case to determine whether or not the interphase transformers remain operative under such conditions of unbalance.

In a multiphase system such as shown in figure 5 of the paper, it will be seen that the rectifiers are divided into two groups, one group having all cathodes tied together, and the other having all anodes tied together. The first group will have a positive voltage with respect to neutral, and the second group an equal but negative voltage. Each group can therefore be treated separately, and the results added to give the total output voltage of the two groups which are effectively connected in series. This bears out the previous statement that for this connection the output voltage is the total area between the negative and positive envelopes of the phase voltages divided by time.

There are a few more minor points I would like to bring out. The statement is made that equation 8 is linear in p. Since this equation, however, involves terms such as $(e_a - e_b)$, which itself is a sine function of p, this statement is not strictly correct. As a matter of fact, it does not differ in this respect from the usual equation,

$$V = \frac{\sqrt{2}E \sin\frac{\pi}{p}}{\frac{\pi}{p}}$$

Since p is a fixed number for any given rectifier circuit, linearity or nonlinearity in p is of no practical importance.

The author's discussion of the difference between the calculus for rectifier circuits using interphase transformers with only half-wave rectification, as he states, and the calculus for star-connected or full-wave rectification, seems to me to be another example of how easy it is to be misled by an "operator". I cannot find any fundamental difference between the two rectifier circuits. Again we find that figure 2*a* gives the correct answer and that the output voltage of a rectifier with or without interphase transformers, and with odd or even phases, is measured by half the area contained between the positive and negative envelopes of the phase voltages of the individual groups. This fact seems to be entirely lost in the development of *S*.

In the author's explanation of equations 12, 13, and 14, it is stated that the double-wye output (meaning, I assume, output voltage) is $\sqrt{3}/2$ times the voltage of the fork-connected transformer, and that the voltage of the fork is 3/2 times the voltage of a single-phase rectifier. This assumes that the same phase voltage would be used in each case. If we assume the same direct voltage for each of the three connections, this statement is, of course, invalid. The author, however, goes further and states that the inverse voltage of the double-wye connection is $\sqrt{3}/2$ times that of the fork connection, and it may be implied that the inverse voltage of the fork connection is 3/2 times that of a single phase. Assuming equal direct-voltage outputs, no grid control and sufficient load to magnetize the interphase transformer, it will be seen that the inverse peak voltages of the double-wye and fork-connected rectifiers are equal, and the inverse peak voltage of the single-phase transformer is considerably higher than either of the other two.

The various numbered paragraphs in the author's conclusion presumably list the advantages of the new rectifier calculus over the older methods. Where unbalanced conditions exist, most of these advantages are real, at least as far as use is made of the differences between anode voltages instead of the phase-to-neutral voltages. However, where unbalance does not exist, but numerous harmonics are present (and this is becoming a very real problem), many of the advantages listed are imaginary. The notation certainly is simple, but the actual computation in most practical cases will still be found to require as much work as the conventional method.

W. M. Goodhue: F. O. Stebbins reports checking the formulas against known results for balanced systems and finding the formulas correct. He also reports labor saving observed in the new formulas, and asks if the rectifier calculus may be used to calculate wave shapes and power factor. Each S_y^1 term in the expansion theorem is a wave, for example a half-sine wave at light load when impedance drop is negligible. Because the S_y^1 wave is simple (180-degree interval) even for unbalanced rectifiers, its harmonics are known or very easy to obtain. The Σ sign in the transformation theorem indicates that the harmonics of a given frequency from different terms may be added, taking their phase angles into account. The result will be the output voltage expressed as harmonics; the d-c value previously obtained is a special case, that is the zero'th harmonic. This form of information would be suitable to telephone interference problems on the d-c side of the rectifier. For the a-c side, and for power factor, the subject, "alternating currents of polyphase rectifiers", given in the list of other application fields, is obviously involved. Developmental work in this field is not yet completed, but shows interesting possibilities. There are more factors involved such as harmonic cancellation. The author is indeed grateful to Mr. Stebbins for bringing out these interesting points.

C. C. Herskind reveals a characteristic of higher mathematical tools of showing great output when geared up to a really difficult problem. These problems will arise with continued industrial development. The author heartily agrees as to the importance of physical action, such as the rise and fall of anode current. Complex cases may be analyzed by the new mathematics, classified as to physical action, and the latter studied in a simple apparatus. Mr. Herskind asks if the new mathematics has been applied to problems beyond normal operating range of commercial rectifiers. Many of these problems are of transcendental (unknown angle) character, that is the fire and cutoff angles cannot be determined as elementary functions. The author has done some work, particularly with pulsating currents in the load; results indicate that such problems may be solved without cut-and-try methods. The subject is very complex, but promising.

A. Schmidt, Jr., agrees concerning the treatment of commutating reactance, and the application of the new mathematics to half-wave circuits. He suggests extending it to the nonlinear range of rectifier regulation. This may mean (1) treatment of nonlinear instantaneous arc resistance (instantaneous arc voltage drop plotted against instantaneous anode current not a straight line), (2) more than two anodes commutating, (3) peak current (hence boundary condition) unknown. Each of these subjects will be interesting to study relative to the new mathematics, though their study could undoubtedly fill a large book. The author is grateful for the suggestion. The problem of arc-back current calculation is complicated in addition by the variations possible in physical action, particularly voltage drops and inductance of the arc inside the rectifier; the previous problems should be tackled first.

I. K. Dortort goes into considerable detail in his discussion, and his recognition of the value of the theorem is appreciated. As he suggests, it is difficult to distinguish between operators and short-hand notation. This is a matter of personal opinion; some individuals have objected to recognizing the Heaviside operator d/dt, or even complex numbers such as $1+\sqrt{-1}$. But the usefulness of operators leads one to suspect that they "exist" as entities. More light on the new operator is given by considering other properties of rectifier circuits such as impedance, which may also be expressed by the operator. Considering a rectifier tube having but one anode, connected in series with load resistance R, electromotive-force source e, and having a constant tube drop E. The current i is given by:

$$iR=S^1(e-E)$$

Now solving for e:

$$e=E+\frac{R}{S^1}i$$

The instantaneous impedance may be defined as the voltage $(R/S^1)\ i$ divided by i, or R/S^1. The quantity R/S^1 evidently stands for an ideal rectifier tube connected in series with resistance R. Note particularly that R/S^1 is a derived quantity, not an arbitrary notation. Every operator, including the Heaviside operator, d/dt, begins with a notation, and ends with a system of derived quantities. The expansion theorem of the paper is another derived quantity. The impedance may be generalized by using any impedance z instead of resistance R, and extended by analyzing other circuit connections. The analytical treatment of the appendix, deplored by Mr. Dortort, was given only for those persons who insist on such things, and was placed in the appendix so that it could have been omitted if desired. Mr. Dortort must admit that the operator was responsible for the discovery of the theorem by the author. The integral method which Mr. Dortort suggests would hardly be applicable to the impedance calculations just given above, or the wave forms requested by Mr. Stebbins, since the wave forms are not integrals. Mr. Dortort is confusing the application to d-c calculation with the general theorem. His point on grid control is well taken, for only certain apparatus will preserve correct grid displacement under unbalanced conditions. For the general case it would be well to use a different cos α for each S_y^1 term in the expansion theorem, assuming only full-wave phase-angle conditions, which assumption is already made in the paper. The double-wye form, which Mr. Dortort wants to get rid of, is necessary to preserve the full-wave assumption. Written in simple half-wave form, the theorem would have to be restricted. Mr. Dortort's arithmetical calculations present merely a different form, as both the author and other discussers have checked the theorem. As to labor saving for balanced conditions, this depends on the form in which data are given; if the inverse or between-anode voltage is known, trigonometric functions such as $(p/\pi)\sin(\pi/p)$ may be avoided.

A New Measuring Instrument for Direct Current

Discussion and authors' closure of paper 39-92 by H. T. Faus and A. J. Corson, presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3–5, 1939, and published in AIEE TRANSACTIONS, 1940, pages 683–6.

G. F. Gardner (nonmember; General Electric Company, Schenectady, N. Y.): Messrs. Corson and Faus have demonstrated again the advantages which accrue when the fundamental properties of a material are considered in laying out a new design. By abandoning the orthodox conception of appearance and size, they have started out along a fresh line of attack and the result has been a design which is not only well adapted for compact portable instruments, but one which will find ready adoption for incorporation in larger apparatus such as potentiometers and the like. Since space and weight, combined with good accuracy, are the outstanding problems in this larger type of instrument, their contribution will be appreciated by the designers of this type of equipment, who can thus make available to the user apparatus which is lighter and more compact without sacrifice of desirable characteristics.

This new design is remarkably stable in calibration as careful tests made under service conditions have shown.

H. T. Faus: Mr. Gardner has mentioned the importance of space and weight in connection with some instrument applications. This feature has not been emphasized in the paper, but a reduction in the weight and in the space occupied by the permanent-magnet system naturally follows the efficient use of a permanent-magnet material of high available magnetic energy. As an example, the substitution of a concentric Alnico magnet system for the conventional chrome-steel and soft-iron construction in a recording instrument saved 15 per cent in the total weight of the instrument and doubled the sensitivity. The entire saving was due to the change in the permanent-magnet system, no change being made in the moving system of the instrument.

Mingled Hydro and Steam Power Production in California—Past, Present, and Future

Discussion and author's closure of paper 39-119 by A. H. Markwart, presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939, and published in AIEE TRANSACTIONS, 1940, pages 775–82.

E. E. George (Phoenix Engineering Corporation, New York, N. Y.): The history, condition, and prospects of power generation in California are presented in Mr. Markwart's paper in a very interesting, logical, and comprehensive manner. The importance of this subject to electrical engineers is self-evident. Similar studies of the other integrated areas might well be prepared for presentation to the Institute at future meetings. Many factors in the California situation are paralleled by those in the southeastern part of the United States except that the southeast is the victim of a more ruthless attitude on the part of governmental agencies. While development on the West Coast has been disturbed merely by a lack of orderly development, government policies in the southeast have tended toward the destruction of private ownership of generation, transmission, and distribution, instead of merely toward securing a preferred market for power generated under public ownership.

It is interesting to note that the marketing of Boulder Canyon power was based upon the idea of a delivered price equal to the local cost of substitute steam power, and that in the case of the Sacramento and San Joaquin River projects it was realized that co-ordination with steam power production was essential for minimum over-all cost, and that steam-plant construction on the part of the private power companies was encouraged.

Mr. Markwart refers to the change in policy following the Boulder Canyon project, in that later projects were launched without thorough investigation of markets and with no arrangement to liquidate any substantial part of the cost. This indifference of public authorities to economics has prevailed in Tennessee and neighboring territory since the inception of the TVA program in 1932.

Steam power production has never had any place in the plans of the Tennessee Valley Authority. The 56,000-kw steam plant near Muscle Shoals, accidentally acquired from the War Department, has never been operated to generate power for sale. Roundabout methods of compensating the city of Memphis or the private company in Memphis were worked out by TVA to avoid any necessity of operation of the Memphis steam plant by TVA. The proposed acquisition of the Tennessee Electric Power Company and its steam plants has brought up a problem of at least temporary operation of these plants to provide for the load and there is some disposition on the part of the friends of TVA to provide statutory approval by Congress for the operation of these steam plants. However, the TVA reports to Congress, their defense before the United States Supreme Court, and other official statements of their directors, indicate that they are relying solely on hydro production as the ultimate power supply for the area.

TVA has a present peak load and peak capacity of about 300,000 kw, including the Memphis load and the use of Memphis steam plant. The announced TVA program provides at least 450,000 kw of firm hydro power by 1943, but not including the firm steam-generating capacity proposed to be purchased from the Tennessee Electric Power Company. This figure does not include the Sheffield steam plant or any additional hydro units which have been included in the TVA ultimate program but which are not yet announced as to date of completion. Neither does the figure of 450,000 include any capacity at Gilbertsville, which might be completed before 1943. The peak capacity with any ordinary load factor would be at least 550,000 kw.

As on the West Coast, the entrance of the Government into the power business has greatly modified all the previously existing factors which would determine the trend of the industry. It has, therefore, been impossible for any utility in this region to make plans much in advance and little improvement in the situation will occur until the following factors take definite shape:

1. *Taxation Policy.* The taxation of private utilities amounts to 15 per cent or 20 per cent of gross income, or about 80 per cent of the wholesale cost of power using the wholesale rates realized by TVA during the past year. This is another way of saying that TVA would have to almost double their wholesale rates if the taxes previously paid by the private utilities were to be assessed against TVA. Their present 5 per cent wholesale tax paid to the states amounts to between 1 and $1^1/_2$ per cent of the retail revenue of their cities and co-operatives who handle the resale. But a small part of any tax on TVA power could be assessed against large power customers on account of competitive power costs, so that most of the increase would have to go against the domestic and commercial users.

2. *Co-ordination With Steam Power.* Government plans in the Southeast provide no part in the picture for steam generation. Overdevelopment of the rivers ahead of the market and use of storage reservoirs under dry-year rule curves for maximum power production are apparently counted upon to take care of the market without the economy that would result from mingling hydro and steam generation. Conflict between the operation of reservoirs for flood control and for maximum power production has been avoided by the fictitious assumption on the part of TVA that floods are impossible during the late spring, summer, and early fall months when the reservoirs are being filled, stored, and emptied. This conflicts with expert meteorological opinion and flood records in other areas.

3. *Territorial Limitations.* The implied distribution territory of the government projects in the Southeast has been outlined by the officials of these agencies and by congressmen friendly to them as covering about everything south of Pittsburgh and east of Little Rock. When the question of territorial limitation recently arose in Congress the chief power engineer of TVA stated that transmission limitations prevented hauling TVA power more than 25 or 50 miles, although the Norris-Wheeler line, which is 200 miles long, had been carrying 30,000 kw to Knoxville and East Tennessee and some power to the Aluminum Company for some time. Territorial protection is probably the most vital factor in determining future power production in the Southeast.

4. *Summary.* It is apparent that at the present time no safe prophecy can be made as to the future of either steam or hydro generation in the Southeast. It is therefore all the more interesting to note that conditions on the West Coast have crystallized to such an extent that engineers in that area can now agree on a fairly definite statement of the most probable projects during the next few years.

Mr. Markwart's paper brings out eloquently the disruption of planning that occurs when political objectives take the place of engineering and economic studies to which the power industry has always been accustomed.

Incidentally this paper contains an exceptionally clear discussion and explanation of the economies of mingled steam and hydro power production. It would be interesting if Mr. Markwart would discuss in a little more detail the effect of both daily and yearly load factors on operating procedure.

Although the objectionable features of governmental planning of power systems in the Southeast have been tempered by occasional technical excellence (as in the TVA hydraulic construction methods) these good features are more than offset by the increasing use of political pressure to counteract the loss of popular support. TVA, PWA, REA, states, counties, cities, co-operatives, and other organizations bound by common political ties and interlocking Federal appropriations make contracts with each other with an entire absence of the arms-length bargaining that the Securities and Exchange Commission is so insistent upon in its regulation of private companies.

In any discussion of government power policy before an engineering body, and particularly one in which reference is made to the taking over of certain private utility functions by the government, engineers will be greatly interested in the disposition of personnel as well as of plant facilities. The past and present policy of TVA is apparently to take over such personnel as are willing or are forced to accept governmental employment, and permit those who can become acclimated to work into the permanent government organization. For many field and miscellaneous employees this may work out satisfactorily but for the engineering and supervisory personnel who are familiar with some of the facts outlined above, the necessity of accepting a position with a government power agency to avoid complete unemployment is viewed as proof of the demoralization of private industry resulting from subsidized competition.

M. W. Edwards (California Railroad Commission, San Francisco): Mr. Markwart's paper ably presents a subject of great economic concern to all of us interested in power development. Furthermore, any planning that deals with the economic utilization of the natural resources of the State of California is of vital interest to every local citizen and taxpayer.

Due to the fact that this year we are facing a dry year (which so far is 40 per cent below normal in precipitation) our attention is forcibly directed to the question of what is a proper steam-hydro ratio. Also, the possibility of two dry years in succession must be visualized in setting this ratio. Steam power is cheap in California on account of the availability of natural gas and fuel oil. A good steam-hydro ratio in this state is necessary to carry over the dry seasons, and furthermore is economically sound.

The good management and farsightedness of the company is to be complimented on the present program of installing additional steam capacity in connection with process-steam production for certain oil refineries that will give low-cost additional energy.

The installation of additional capacity for the next few years in the form of steam will prove advantageous in the absorption of the state's Shasta power in the northern California market.

While power production in connection with the Central Valley project is a secondary consideration as far as being the reason for building Shasta Dam, still it is the principal means of paying for the cost of operation. In order that the project be nearly self-supporting and not prove too much burden on the taxpayer it will be necessary to sell the power for a higher price than the usual market value for this type of energy.

The economic value of Shasta power to the company is what it would cost to produce the same by some substitute means, either steam or hydro. However, on the other hand, if the duplication of transmission, subtransmission, and distribution lines, with their consequent long-run higher costs, is to be avoided a higher price will have to be offered for this power; or some other plan adopted whereby the state will share in the advantages of serving retail power. Public interest can best be served by some co-operative plan that will develop the Central Valley's water and power for the utmost benefit of the people and industry of the state as a whole at the lowest ultimate cost to the taxpayer.

L. A. Doggett (Pennsylvania State College, State College): I wish to make several comments on this paper. The first has to do with capital costs and running expenses of mingled hydro and steam power production. From a study of the statistics of some 55 electric utility companies in Moody's "Public Utilities," selected from the 1922 and 1934 editions, values of (total assets ÷ annual gross revenue) and [(operating expenses plus taxes plus depreciation)/annual gross revenue] were plotted versus (hydro kw/hydro kw plus steam kw installed). The medians of these points (figures 1 and 2) gave the following relationships:

$$\frac{\text{total assets}}{\text{annual gross revenue}}=4^1/_3+7\,\frac{\text{hydro kw}}{\text{hydro kw}+\text{steam kw}}$$

$$\frac{\text{operating expenses}+\text{taxes}+\text{depreciation}}{\text{annual gross revenue}}=0.7-0.2\,\frac{\text{hydro kw}}{\text{hydro kw}+\text{steam kw}}$$

For any given set of prices, load factor, locality, etc., formulas of this type may be fairly accurately derived. The above numerical values, however, should be considered as nothing more than crude averages. The formulas may nevertheless help to emphasize some of the general statements which the author has made on these matters.

The second point has to do with the general subject of conservation and aims at emphasizing the desirability of supplying energy by falling water rather than "by burning fuel". We see statements to the effect that our coal supply will last from three to four thousand years at our present rate of consumption. From the following simple analysis it is indicated that very small successive and regular increases in our annual consumption will produce surprising decreases in the life of our coal supply. Suppose that we have on hand a supply that at our present rate of consumption will last ten years and that we increase our consumption by 50 per cent successively each year; then

$$1-\frac{1}{10}-\frac{1.5^1}{10}-\frac{1.5^2}{10}-\frac{1.5^3}{10}-\text{etc.}=0$$

$$1=\frac{1}{10}+\frac{1.5^1}{10}+\frac{1.5^2}{10}+\frac{1.5^3}{10}+\text{ etc.}$$

$$1=\frac{1}{10}\,(1.5^1+1.5^2+1.5^3+\text{etc.})$$

which is a geometric series, summed as follows:

$$1=\frac{1}{10}\left(\frac{1-1.5^n}{1-1.5}\right)$$

In this simple illustrative case, where *n* equals 4.42 years, our 10-year supply has been reduced to a 4.42-year supply. The same reasoning applied to a 4,000-year supply and with a one per cent annual successive and regular increase gives

$$1=\frac{1}{4{,}000}\left(\frac{1-1.01^n}{1-1.01}\right)$$

whence *n* equals approximately 400 years.

Thus the practice of increasing our annual consumption by one per cent will bring exhaustion in 400 instead of 4,000 years. From the point of view of conservation our objective should be to use every drop of falling water and to build such steam plants as are necessary to accomplish this objective. On the whole California has attained this objective in a very commendable manner. Times have been when falling water was somewhat overdeveloped but sooner or later steam plants were built to match the hydro plants.

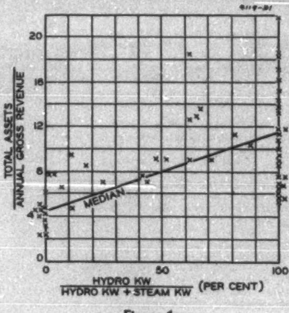

Figure 1

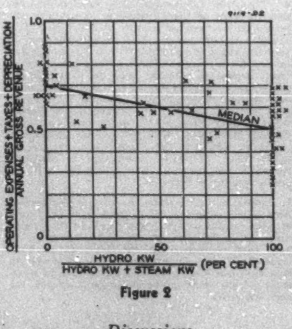

Figure 2

A. H. Markwart: The writer desires to express his appreciation of the written discussions contributed by E. E. George, Professor L. A. Doggett, and M. W. Edwards.

Mr. George has described the situation relating to power generation in the southeastern part of the United States and has called attention to the problems resulting from the entrance of the Federal government into the power business there. It is to be hoped that these problems will be solved with a minimum of economic disturbance to the taxpayers and the utilities now serving that area.

There are indications that the Government is adopting a milder attitude with the electric utility industry and there are evidences of co-operation with it. TVA has contracted to sell large blocks of power to the Arkansas Light and Power Company and to the Alabama Power Company and it is probable that the Bonneville administration will close a similar contract with the Portland General Electric Company. These contractual arrangements auger a modification of the policy on the part of the Government, which heretofore encouraged public ownership and in the Northwest actively supported formation of public districts. From a practical standpoint it appears obvious that the sale of substantial blocks of power in the near future must be done through the existing utilities. It is apparent that the Government now recognizes that the major outlet for its power projects lies in the established systems. This realistic solution of the problem of marketing power from government projects will result in substantial and immediate revenue.

Mr. George has suggested that the economics of mingled steam and hydro production might be discussed in more detail relative to the effect of both daily and yearly load factors on operating procedure. Insofar as the economics of mingled steam and hydro production in California is concerned, the writer has found it necessary to consider only monthly and annual load factors. Most California hydroelectric plants are provided with storage or with forebay facilities which permit daily and even weekly regulation of the water supplied to the plant so the installed capacity can be employed to its full extent when required to meet peak loads. A monthly study of available water supply is sufficient for such plants. Run-of-river plant capacity is small because none have been built for many years owing to the ineffectiveness of their capacity in low-water seasons. Such plants usually provide effective system capacity only to the extent permitted by continuous operation with the energy available in a critical dry month. The problem of providing the most economic new plant capacity in a regional system, aside from any consideration of standby, requires a study of the capacity and energy available in the existing run-of-river and peaking hydro plants, the capacity available in existing steam-electric plants, the cost of generating energy in existing steam-electric plants, and the cost of providing both capacity and energy by the alternate new plants under consideration. A study of the dry year is required to determine the effectiveness of the new capacity and a study of a period of years is required to determine the probable amount of energy which the new plant will provide. As stated above, a monthly study should be sufficient for this purpose if consideration is being given to a new peaking hydro plant or a steam-electric plant. However, if a large run-of-river hydro plant is under consideration, it may be necessary to study the daily load, particularly to determine the effectiveness of its capacity.

Professor Doggett, in addition to commenting on the desirability of developing hydro power in order to conserve our fuel resources, has presented some interesting formulas which can be employed to compare the financial operation of utilities which supply mingled hydro and steam power. The formulas show the effect of the hydro-steam ratio on the relation of assets to gross revenue and on the relation of operating expense to gross revenue. Professor Doggett states that for a given set of prices, load factor, locality, etc., formulas of the type shown may be accurately derived, but cautions that the constants shown in his formulas should be considered as nothing but crude averages. The writer confirms this idea because he has applied the formulas to the regional system supplying northern California and found a very close agreement (2 per cent) in the one case but a considerable discrepancy (25 per cent) in the other.

Mr. Edwards suggests that the possibility of two dry years in succession must be visualized in setting the hydro-steam ratio. For some years this possibility has been considered by the writer when determining the need for capacity to supply the northern California market, it being assumed that the combination of existing and proposed generating facilities should be capable of meeting the estimated load in two dry years like 1931 occurring in sequence.

Mr. Edwards also suggests that although the economic value of Shasta power to the company is what it would cost to produce the same by substitute means a higher price will have to be offered, to avoid duplication of transmission and distribution facilities, or some other plan adopted whereby the state will share in the advantages of serving retail power, because public interest can best be served by providing the benefits of the Central Valley Project at the lowest ultimate cost to the taxpayer.

It is not now known whether the Federal Government through the Reclamation Service, or the State of California through the Central Valley Authority, will contract the sale of the power of Central Valley Project. Should this be done by the Federal Government it is not improbable that the price at which Central Valley power is sold will be influenced by the price established by the Federal Government for power of other Federal projects. The rate structures thus far established by TVA and for other multipurpose projects are competitive, indicating that value has been considered, and with the new alternative wholesale rates for Bonneville power now proposed and under consideration, they follow the pattern developed by a two-part rate as suggested for Central Valley. What the policy of the State might be in the event it disposes of the power is unknown. However reports issued by the State from time to time and utterances by State officials indicate that calculated revenues of the project have been based on a price for power as determined by cost of power from alternate sources. It is inconceivable, in the light of low-cost competitive power available at Federal projects in the North and in the south (Bonneville and Boulder) that the public interest in northern and central California could best be served by establishing a price for Central Valley power above its value.

In the interest of water-supply development, the price for power should be as high as can be justified by the cost of power from competitive sources. This will not be injurious to the consumer because he would pay no more for power than were competitive sources developed, and furthermore, under regulation as it obtains in California, whatever is paid for power would be the cost allocated to power production in making up the power-rate structure, so there is no profit to the utility in purchased power. The only return to the utility would be on investment in distribution and transmission which the utility must make to deliver the purchased power to the consumer and this is but reasonable as determined by the regulatory body. Since the State makes no capital investment in this connection it is not clear why it should share in the advantages of serving retail power as suggested by Mr. Edwards.

Ionization Time of Thyratrons

Discussion of paper 39-116 by A. E. Harrison, presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939, and published in AIEE TRANSACTIONS, 1940, pages 747–50.

V. C. Rideout (California Institute of Technology, Pasadena): Mr. Harrison's investigations show that the ionization times for various thyratrons, when plotted against the grid overvoltage give curves that are approximately equilateral hyperbolas. Because tubes of different construction, with different gases at pressures not accurately known, and with various values of plate voltage, have been used, it is difficult to say how ionization time is affected by tube design. If the results for the *FG*-81 and the *FG*-57 are compared, however, it may be seen (figures 5 and 8) that the argon-filled *FG*-81, which is similar in construction to the mercury vapor *FG*-57, has somewhat shorter ionization times.

In figure 1 accompanying this discussion, I have shown recent data for two tubes manufactured by Electrons, Inc. These tubes, the EL *C1B* and the EL *C6J*, are filled with xenon gas and are similar in construction. The *C1B*, which has somewhat smaller elements, and smaller interelectrode spacings, has shorter ionization times than the *C6J*. The 885 which has the smallest interelectrode spacings of all the tubes tested, had the shortest ionization times. It would seem that the higher gradients in such a tube cause ionization to more rapidly neutralize the space charge and create a plasma.

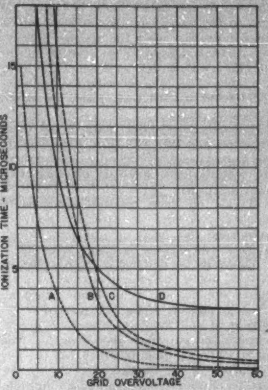

Figure 1. Ionization times as functions of grid overvoltage

A—EL *C1B* xenon-filled thyratron, 300 volts on anode

B—EL *C6J* xenon-filled thyratron, 600 volts on anode

C—EL *C6J* xenon-filled thyratron, 300 volts on anode

D—*OA4G* cold-cathode argon-filled, 300 volts on anode

Reynolds ("Studies With an Ionization Gauge," Neil B. Reynolds, *Physics*, volume 1, September 1931, pages 182–91) has shown that the probability of ionization for argon (ions per electron per centimeter path at one millimeter pressure) for a given electron velocity is about five times that for neon. This is probably the chief reason for the slower ionization times of the *KU*-610. The critical grid bias for this tube is positive, and it was biased positive about four volts under test. As there is a high resistance in the grid circuit of the test apparatus, there may be some slight error here. This was checked for the *OA4G*, a cold-cathode positive control tube, and the error was not found to be serious. The grid bias in this case was 67 volts positive. A curve showing ionization time characteristics of this tube has been included in figure 1 for comparison with Rockwood's data.

Fractional-Slot and Dead-Coil Windings

Discussion and authors' closure of paper 39-134 by Michel G. Malti and Fritz Herzog, presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939, and published in AIEE TRANSACTIONS, 1940, pages 782–94.

P. L. Alger (General Electric Company, Schenectady, N. Y.): This subject of fractional-slot windings is one of perennial interest to designers. There is literally no end to the variety of the problems which arise in practice when the designer endeavors, on the one hand, to use existing punchings for a wide variety of machines, and, on the other hand, to continually meet new requirements of good wave form, low noise, minimum reactance, and other factors. The highly specialized character of these problems, together with their great variety, has caused designers generally to solve them by common-sense methods differing with each individual. Although little has been published on these practical solutions to winding problems, an extensive body of knowledge has been accumulated, which confirms and extends the authors' results.

Typically, the designer is given a number of slots, poles, phases, and circuits for a required winding, and he must first determine the best possible phase belt pattern or sequence of coil numbers, and then decide whether this pattern is good enough. Not infrequently, a winding which has the denominator of the fraction representing the number of slots per pole per phase a multiple of the phases, and is, therefore, unbalanced, can be arranged to make the unbalance so small that it is quite satisfactory. At other times, a winding which is perfectly balanced, and is arranged for the highest possible voltage production, as prescribed in the paper, is found to have an objectionably high reactance, or double-frequency vibration, which can be corrected by a different winding sequence.

Thus, for each combination of slots, poles, and phases, there is a particular winding sequence which is best for reactance, and another that is best for voltage, and a third that is best for noise or vibration. With fractional numbers of slots per pole per phase, such as $1^1/_2$ or $1^4/_5$, these three types of winding all coincide, but with such numbers as $2^3/_{11}$ they are normally all different, and the designer must decide what is the best sequence for his particular purpose.

From my experience, the best way to solve these problems is to visualize each coil voltage or current as the chord of a circle spanning an arc equal to the electrical angle between consecutive slots. The highest possible voltage will be obtained if the maximum angular spread between the coils connected in each single phase is a minimum; while phase voltage balance will be obtained if the pattern of each phase is the same with the correct phase displacement. For minimum vibration or noise, it is a question of grouping the coils in each phase separately most symmetrically around the entire periphery. For example, with $1^2/_5$ slots per pole per phase in a three-phase winding, the best sequence of phase belts in each phase considered by itself is 2, 1, 2, 1, 1, if minimum vibration is required; while the best sequence for voltage production is 2, 2, 1, 1, 1. I have not found any satisfactory way to visualize the grouping that gives the least possible reactance, but I have found the methods for calculating this reactance given by Gabriel Kron in chapter 12 of his recent book ("Tensor Analysis of Networks," John Wiley and Sons) very satisfactory.

Table I. Ratio of Three-Phase Reactances

Pitch Deficiency (Slots)	Slots Per Pole Per Phase			
	$1^1/_2$	$2^1/_2$	$3^1/_2$	$4^1/_2$
$^1/_2$	0.0457	0.0178	0.00811	0.00703
$1^1/_2$	0.0582	0.01735	0.00866	0.00594
$2^1/_2$		0.0225	0.00957	0.00554
$3^1/_2$				0.00665

While fractional-slot windings are normally used for synchronous machines with their large air gaps and relatively high total winding reactance, they are much more rarely used for induction motors because of the increased reactance frequently obtained with the relatively small air gaps used.

If the more general use of fractional-slot windings is desired, it will be very useful to determine the differential leakage reactances of all usual fractional windings and compare them with the corresponding values for perfectly regular windings. The values of per unit differential leakage reactance calculated by Mr. Kron's method for three-phase windings with $1^1/_2$, $2^1/_2$, $3^1/_2$, and $4^1/_2$ slots per pole per phase are given in table I of this discussion.

These may be compared with the values in table II of this discussion for regular windings, taken from table XI of my paper entitled "The Calculation of the Armature Reactance of Synchronous Machines", which appeared in AIEE TRANSACTIONS, volume 47, April 1928, pages 493–513.

Table II. Differential Leakage Reactance of Three-Phase Windings

Pitch Deficiency b	$s/3$ = Slots Per Pole Per Phase			
	1	2	3	4
0	0.0966	0.0284	0.0140	0.0089
1	0.0966	0.0235	0.0115	0.0074
2	0.0966	0.0284	0.0111	0.0063
3		0.0284	0.0140	0.0069
4			0.0143	0.0089

If plotted in curve form, the fractional-slot windings given are found to have only a little higher reactance than the regular windings, but when other fractions than multiples of $^1/_2$ are considered, differential leakage reactances several times as high as for regular windings frequently occur.

M. G. Malti and **F. Herzog:** Alger mentions in his discussion the possibility of slightly unbalanced windings. This falls outside the scope of our paper because we deal only with balanced windings. Again Alger notes that different winding sequences may be used to correct for high reactance, noise, etc. This can be readily accomplished by referring to tables II to V of our paper. In our terminology Alger's suggestion of new patterns would simply mean adding any multiples of bn/m to the numbers listed in these tables. Particular attention should be given to tables V*a* and V*b*. In these tables the same multiples of bn/m should be added in corresponding positions of these tables. Physically this means interchanging coils among phases while maintaining a balanced winding with no regard to maximum voltage. Our paper, therefore, is inclusive enough and flexible enough to allow the change of winding pattern while, at the same time, assuring the designer that the final pattern he selects will give a balanced winding.

As an illustration we shall take Alger's example of $1^2/_5$ slots per pole per phase. This winding belongs to type *B*. Using table III we have for phase 1, assuming 42 slots:

k	1	2	3	4	5	6	7
kb	17	34	9	26	1	18	35
$kb+n/2$	38−	13−	30−	5−	22−	39−	14−

This is a 2, 2, 1, 1, 1 winding as Alger asserts. But by adding $bn/m = 238$ to the slots in column 6 we have:

k	1	2	3	4	5	6	7
kb	17	34	9	26	1	4	35
$kb+n/2$	38−	13−	30−	5−	22−	25−	14−

This is the winding 2, 1, 2, 1, 1 which Alger seeks.

The remainder of Alger's discussion relates to calculating reactance which, naturally, falls outside the scope of our paper and is, in our opinion, irrelevant.

12-Kv Metal-Enclosed Bus and Switch Structure, Station C, Oakland, Pacific Gas and Electric Company

Discussion and authors' closure of paper 39-127 by F. S. Benson and H. E. Strang, presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939, and published in AIEE TRANSACTIONS, 1940, pages 751–5.

A. Fulton (General Electric Company, San Francisco, Calif.): The authors of this paper have described many design and constructional details of this 12-kv metal-enclosed switchgear installation. Additional emphasis may properly be placed on the safety features which are inherent in this particular type of gear.

While the oil circuit breakers are of a design which has been proved by many years of satisfactory service, yet they meet the most modern requirements for low oil volume. The total oil required per triple-pole breaker is only 14 gallons, divided among six seamless steel vessels. The pair of these vessels per phase is completely enclosed and segregated from other phase units by steel barriers. Possible fire hazard thus approaches the irreducible minimum for any oil-circuit-breaker installation.

This factory-assembled metal-enclosed switchgear embodies another safety feature not commonly found in older types of construction. Accurately lined up steel work simplifies the problem of providing a sturdy and foolproof interlock between oil-circuit-breaker cell doors, and gang-operated disconnecting switches. Thus it is impossible for an operator to come in contact with any live parts in the circuit breaker compartment. Before obtaining access to the breaker cells, he must first open the disconnecting switches, as this operation automatically removes a solid bar lock from the cell doors.

The more conventional type of interlock between the disconnecting switches and circuit breakers is also provided, insuring against operation of the switches except when the breaker is in the open position. Gang operation of the six switches per breaker is easily accomplished through a worm-gear mechanism. A grounded steel barrier between the operator of the mechanism crank, and the switches, provides a still further safety feature.

As long as the job of controlling the production and distribution of electrical energy is entrusted to the functioning of human beings, we may expect occasional cases of erratic and unexplainable performance. The examples of this limitation, which have come to our attention, most frequently involve the operation of the wrong disconnecting switches. At times these result in nothing more serious than tripping out one machine or bus section, but too frequently they cause serious personal injury and considerable damage to nearby electrical equipment.

A recent investigation of several cases disclosed the fact that, in a majority of instances, the person involved was suffering from a temporary emotional disturbance, which apparently interfered with the mental processes accompanying even calm routine action to which he was accustomed. This is a condition which is definitely beyond the control of any employer, and from experience has been shown to affect even the higher-grade workers who are expected to think for themselves. It, therefore, appears obvious that a reduction in troubles of this kind can be expected only to the degree to which mechanical safeguards are provided to assist the human mind when necessary in making correct decisions.

J. P. Jollyman (Pacific Gas and Electric Company, San Francisco, Calif.): The authors have described a bus and switch structure which has been very carefully designed. Detaching the metal enclosures and the electrical conduits from the building structure has many advantages.

The necessity for a double 12-kv bus with two oil circuit breakers in each position arises from the functions which this station must perform. In seasons of average rainfall, the load in the East Bay area is fed principally from the hydroelectric transmission system with station *C* acting as a standby. Turbines are kept in operation at minimum kilowatt output, but with considerable reactive output to correct the power factor of the regional load. During dry seasons, the steam plant will be called on to carry a base load at very high load factors.

Station *C* is connected to two 110-kv stepdown substations with 12-kv cables of considerable length and to a third, station *X*, with a 110-kv cable as described by the authors. To secure the maximum output of the steam plant, power must be fed out through the three transmission substations. To control the flow over the three routes, the 12-kv bus at station *C* may have to be divided with certain loads and transmission connections on each section. It did not seem possible to secure the needed flexibility of operation with any other switching arrangement.

Load-ratio control was applied to 110/12-kv bank at station *C* so that the power factor might be controlled on the 110-kv cable. The flow of power over the cable may vary from full load into station *C* to full load out. A considerable range in transformer ratio will be required to meet this condition.

D. C. Prince (General Electric Company, Philadelphia, Pa.): This paper seems to indicate a trend of the times with implications rather broader than the mere subject matter of the paper. Prefabricated houses have come in for much debate and would probably be general by this time if it were not for preference for individuality in our separate homes.

In power plants we are striving for efficiency in functional design rather than individuality. Also, there is no immediate prospect of power houses in rows, whereas the interior arrangements should be harmonious for the best effect.

Therefore, the power switchboard and control plant generally lends itself to a prefabricated factory-built functional design. This paper may prove to be a sign post pointing the direction in which all sorts of structures will develop.

M. H. Hobbs (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper presented by Messrs. Benson and Strang is very interesting, and is a further indication of the increasing use of metal-enclosed switchgear for larger and more important stations. The equipment design worked out, in general, seems well suited to the application and the station layout, and the paper shows how the station design work was simplified by the use of factory-assembled switchgear.

One of the features emphasized is the running of control conduit exposed, the impression being given that this was a new development, although this may not have been intended. This practice was followed as long as 15 years ago by the Northern States Power Company in their Minneapolis steam station, and other companies have used it since that time. Recent examples are in the substations for the Bay Bridge electrification in San Francisco.

It would appear that some compromise with the most preferred practice was required to accommodate the type of circuit breaker used. It has been considered necessary, in important stations, to isolate the main bus from other parts of the circuit to localize a fault at any point. The arrangement shown places feeder circuits, potheads, potential transformers, and reactors in quite close proximity to the busses. In addition, the operation and interlocking of disconnecting switches appears to have been somewhat complicated by the location selected for these switches.

It would seem that a preferred arrangement for this important station would have placed the different parts of the structure on three floors, with the main busses at the top, the breaker with its disconnecting switches on the middle floor, and the outgoing feeders with reactors, potential transformers, and grounding devices on the lower floor. The authors' comments are requested upon this suggested arrangement.

Modern oil circuit breakers are very efficient, and are ordinarily safe and dependable. However, from a fire-hazard standpoint, too much stress should not be placed upon simply the reduction in oil content for a particular design. This is only one feature, which may be obtained at a sacrifice in other points, such as, for instance, complete immersion of contacts in oil, dead-tank construction, elimination of stuffing boxes, and operation of contacts in such a way that gravity tends to open them.

A recent rather serious case of trouble involved approximately three gallons of oil, and similar instances have occurred in the past. Complete elimination of oil rather than a simple reduction in amount would seem to be the more desirable aim.

It is believed the increase in permissible temperature rise is entirely logical, although the proposed standard of the Institute limits the rise of air temperature within the compartments to 15 degrees above the outside ambient, rather than the 25 degrees permitted on the equipment described.

The importance of reduction in weight for metal-enclosed switchgear mentioned in the paper, as compared with concrete cell structures, is a feature which is sometimes overlooked and merits attention. It may have a very real influence upon the simplification of station design and upon the construction cost of the building.

The operating company should be complimented upon its broad planning with regard to its installation schedule. The completion of building construction prior to commencing electrical work permits both to be handled most efficiently, and enables the greatest advantage to be taken of the use of factory-assembled switchgear.

F. S. Benson and **H. E. Strang:** A. Fulton's discussion properly stresses the low-oil-content design of the circuit breakers used, and also the mechanical interlocks so as to prevent station personnel from operating disconnecting switches under load.

J. P. Jollyman gives reasons for the double-bus construction which was used to better control power flow from the steam plant bus to the transmission system.

We agree with D. C. Prince that the switchgear described in the paper indicates a trend of the times with implications that are broader than the subject material presented. Such uniform design of cell construction produced a structure of low cost and permitted its erection in record time.

M. H. Hobbs raised certain questions which will be answered in sequence.

Referring to the exposed runs of conduit, perhaps not enough information was given by the authors to emphasize the fact that the building itself was erected as a loft-type building with only openings in floors and walls to accommodate passage of these runs of conduit from one section of the building to another. It was this design that greatly facilitated building construction. The building contractor did not need to worry about conduit runs, facilities, etc.

Mr. Hobbs states that preferred arrangement for such an important station would have placed the different parts of the structure on three floors. This was one of the proposed designs, the costs of which were very carefully worked out, and a decision was reached that a two-story building could be used. It is believed that by using this type of metal-enclosed switchgear and bus cell, no operating hazard is incurred by placing the bus on the same floor and in the same room with certain other related equipment, such as reactors, current and potential transformer cells, and that the extra expense of a three-story building, together with the extra cost of the switchgear built for three stories instead of two was not justified. Therefore, the oil-circuit-breaker cells were placed on the top floor, the bus structure with reactors, disconnecting switches, current and potential transformers, and potheads was placed on the floor beneath or the first floor. It was also decided that with the steel cell construction with phases adequately isolated by barriers, all openings between various phase compartments were reduced to mere cracks or were screened so that if a fault occurred and gas did migrate from one phase to another, or from one cell to another, it would be completely deionized and should, therefore, not tend to produce a short circuit in cells adjacent to the one where the original fault occurred.

It was not mentioned in the paper that this bus structure is protected by a bus differential system which we believe will adequately protect the bus from severe burning due to short-circuit currents. The design of the structure is such that faults occurring in one phase should be limited to line-to-ground faults and not contact adjacent phases or adjacent cells.

With reference to the comments regarding the presence of any oil (regardless of small quantity) presenting disadvantage as compared with the total elimination of oil, the following factors were of primary importance in the selection of circuit breakers and other circuit components for this important location: The excellent and thoroughly proved operating characteristics of modern oil circuit breakers combined with the small amount of American operating experience with air circuit breakers for such an important location, undoubtedly, was a major influence in arriving at a decision to stay with oil circuit breakers for this location. However, it was recognized that there are underlying advantages in restricting the insulation path (as far as possible) to class *B* material, such as porcelain. The fact that oil was eliminated entirely as an insulating medium was regarded as a strong point of advantage. The quantity of oil is limited to a total of 14 gallons per one 1,000,000-kva breaker and this small quantity is split up among six seamless steel forged tanks per breaker. This compares to 165 gallons of insulating oil divided among three fabricated tanks in conventional dead-tank breaker designs, further reducing the fire hazard or insulation breakdown which might cause an oil fire.

At the time the bus structure was designed, the proposed standard regarding the rise of air temperature within the compartments above the outside ambient had not been agreed upon and the decision was reached by us that 25 degrees centigrade rise would not involve any hardship on the equipment installed in the cells. It is to be noted that later equipment has been designed to follow the new standards, that is, an average temperature inside the enclosure 15 degrees centigrade higher than the ambient temperature outside the enclosure.

This steel-cell structure weighs much less than a corresponding concrete-cell structure. This effects a material saving in cost of building. The equipment was installed with a minimum of time and erected without any changes being made in the field, as all component parts were so marked and shipped that the erection was only a matter of assembling these parts in proper sequence.

Electrical Equipment of the Union Pacific Steam-Electric Locomotive

Discussion and authors' closure of paper 39-139 by M. R. Hanna and J. F. Tritle, presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939, and published in AIEE TRANSACTIONS, 1940, pages 756–64.

L. K. Sillcox (The New York Air Brake Company, Watertown, N. Y.): The writer has read with great interest this general description of the control and power circuits of perhaps as novel a steam-powered locomotive as the world has yet seen. Although resistance and regenerative braking are not new, the particular arrangement and control of the resistance brake on the driving axles warrants the highest commendations. In the past these means have been used generally to develop effective retardation only in the lower speed ranges. In this locomotive, however, some degree of resistance braking is made available over the whole speed range up to 120 miles per hour.

Just how effective is this type of brake? The impression may be gained by the reader of the paper that the resistance braking is adequate under all conditions to match air-brake performance and capacity of any type of train being hauled. Such is not the case.

This locomotive is designed primarily for hauling passenger trains, of both the conventional type and the "streamliners". In streamliner car equipment, the train air brakes, making use of a special electro-pneumatic straight air system, are much more powerful than are those fitted on cars of the more conventional order. The difference is automatically regulated on the locomotive by the difference in maximum air pressure admitted to the brake cylinders and in accordance with the air brake control system being used. In figure 13 of the paper, the authors show the maximum braking effort that can be developed by the resistance brake. The total output of 3,600 kw from the traction motors acting as generators is available only when the high-speed air-brake control system is being used and then only when it is calling for the maximum brake application that can be made. It will be noted that at 100 miles per hour the maximum braking effort is roughly 19,000 pounds. This is not all of the braking force, since low-powered air brakes are co-operating on the guiding axles, but the latter add no more than 6,000 pounds. The total of 25,000 pounds will produce a retardation of very slightly over one mile per hour per second. Even if it were possible to have the maximum of 35,000 pounds braking effort shown in figure 13 effective on the drivers for every second of a stop from any speed, the total retarding force on the locomotive would not produce a retardation in excess of two miles per hour per second.

In the high speed (streamliner) class of service, the braking effort on the cars is adequate to stop them from 100 miles per hour at an average retardation of over three miles per hour per second. The Bureau of Safety of the Interstate Commerce Commission has established 3,600 feet from 100 miles per hour as a satisfactory distance in which passenger trains should be able to stop under emergency conditions. To meet this requirement, the railroads are faced with the problem of developing three miles per hour per second retardation on their trains, including locomotives. It appears that the maximum braking effort developed by the resistance brake would give this locomotive an average retardation of less than half that required by this specification, and a great deal less than that of the train behind it.

As pointed out by the authors, the locomotive is also equipped with air brakes. The automatic control of the resistance brake comes from the air-brake system, and the latter applies brake shoes to the wheels in a normal manner in the absence of effective resistance braking. The design of the air brake is such that when it alone is used the retardation produced in the locomotive is truly but slightly less than that in the cars it is hauling, and is so proportioned that when it is coupled to one of the streamliners the train may be stopped from 100 miles per hour within 3,600 feet.

In all fairness, it must be pointed out that the lack of effectiveness of the resistance brake is only comparative. In absolute terms it is a very good brake indeed for a certain use, that of controlling the speed of the train. For grade work it has tremendous advantages as pointed out by the authors. As a brake for effective stopping under emergency conditions, however, it does not compare with and should not be considered as a substitute for the more powerful air brake also available on the same locomotive.

F. C. Lindvall (California Institute of Technology, Pasadena): The locomotive which is the subject of the paper involves a number of novel features, any one of which merits considerable discussion. One element however, may not command in the minds of electrical engineers the position of importance which it merits and that is the dynamic braking of the locomotive. Few engineers, particularly those who reside in territory involving no mountain grades of any consequence realize the acute problem which a long downgrade presents in the operation of modern railroad equipment. For example, situations occur in which a train must negotiate an average two per cent down grade at a speed of 35 or 40 miles per hour for a distance of 20 miles. (For a modern lightweight passenger car, this condition is equivalent to about 20 horsepower per wheel going directly into heat. The locomotive, with higher wheel loads, suffers even more acutely.) Under ordinary operating conditions, a locomotive with only air brakes actually does less than its proper share of braking and the wheels and brake shoes of the entire train at the end of such a grade are extremely hot. In one of the new Diesel-electric locomotives provision is made for water sprays to be directed at the tread of the wheels in order to afford some relief from such overheating. Stated very briefly and inadequately the high-speed train of so-called lightweight type has presented extremely serious braking problems, so serious in fact, that wheel destruction and brake shoe maintenance is a serious operating expense.

Dynamic braking is a very practical solution to this problem because under normal operating schedules speed upgrade is substantially the same as that downgrade due to curve restrictions and, as a result, the horsepower in braking required of the motors is less than that for traction by the amount of the train resistance factor. The problem, then, is one of heat dissipation, in other words, disposal of the energy taken out of the train in descending the grade. The condensing system in the steam-electric locomotive is adequate for the losses incurred in upgrade operation and, because of the inherent thermodynamic efficiency of the locomotive, is more than adequate for the power dissipation required in dynamic braking. The interesting feature of this new locomotive development is the rheostat which puts the train energy into the cooling system. A similar thing is, of course, possible in the radiator system of the Diesel-electric locomotive, but as yet dynamic braking has not been offered in that type of power plant. The designers of the steam-electric locomotive are therefore to be congratulated in having presented a solution to a problem which has been most annoying to western railroads operating a high-speed service.

J. V. B. Duer (Pennsylvania Railroad Company, Philadelphia): The paper prepared by Messrs. Hanna and Tritle outlines in a concise and complete way the electrical equipment necessary fully to control a steam-electric locomotive of entirely new design with the detail parts so co-ordinated as to provide a complete working unit.

The Union Pacific and the General Electric Company, as well as their engineers, are to be commended for their vision and courage in developing this unit, which is definitely of a pioneer type. Full automatic control of the steam boiler with its allied power units is provided. In addition, the multiple control of locomotive units includes a feature not previously accomplished; that is, the multiple control of steam power plants. The operating requirements of the locomotive have been worked out in a way which is particularly noteworthy. Fuel and air are automatically proportioned for best combustion and the electrical control apparatus permits using full output of the power plant, when required, without overloading. The braking system permits hard and continuous braking with entire flexibility of manipulation by the ingeniously combined action of electric and pneumatic brakes.

The detail to which the electrical equipment has been developed is apparent in the provision for extra ventilation above that normally needed by the main motors, to permit carrying protracted overload when required by the service.

This locomotive provides a type of motive power entirely different from any previously available and represents a definite forward step in that it combines the advantages of steam with those of electrical equipment in a single unit.

C. P. Kahler (Union Pacific Railroad Company, Omaha, Nebr.): The authors of this paper deserve to be complimented on the complete description which they have presented of the electric equipment on the new steam-electric locomotive of the Union Pacific. Also, on the manner in which they have shown the importance of the electrical equipment in tying together and making possible the operation of the steam locomotive.

While a good deal of the equipment and apparatus used is not new, the authors of this paper have clearly shown many new ways in which tried out equipment has been assembled in this locomotive.

As outlined by the authors, this locomotive was designed to handle:

1. Streamline trains with the many electrical services necessary
2. Conventional passenger trains with coaches, observation cars and Pullman cars, and
3. Freight trains

This would necessarily make the electrical apparatus on this locomotive very much more complicated than would be the case if it had been designed for any one of these services.

One of the outstanding features of this paper is the description of the electric brakes which is the first application of its kind that I have heard of. The manner in which the electrical energy generated when braking is converted into heat and a good portion saved by transferring to the steam circuits is certainly unique. Also, the reduction in brake shoe and wheel wear which results is also outstanding.

The test runs made with this locomotive have been very encouraging. They have also demonstrated that considerable simplification of the electrical apparatus can probably be made and that the trend of the development of this locomotive will be in the direction of simplification.

M. R. Hanna and **J. F. Tritle:** The authors appreciate the favorable reception accorded their paper on "The Electrical Equipment of the Union Pacific Steam-Electric Locomotive" and particularly wish to thank those submitting written discussions. The paper describes the electrical details of a pioneer development which we hope will serve as a milestone in the forward march of modern transportation vehicles.

Mr. Duer calls attention to the numerous automatic features of this locomotive which permit combining the advantages of steam with those of electrical equipment and the provisions for multiple-unit operation of two or more locomotives with their steam-boiler and power-plant control. He mentions the electric braking system capable of hard and continuous braking which is especially valuable in maintaining fast schedules descending long mountain grades. The super blowing of the traction motors also included in Mr. Duer's discussion is intended for use in transcontinental service, permitting trains to be handled without pushers up mountain grades where the demand would be considerably in excess of the normal continuous tractive effort rating of the motors. This is obviously better economy than to burden the locomotive with much greater motor capacity which would be fully utilized for not more than two or three hours in a transcontinental trip.

Mr. Kahler mentions the various service conditions under which the locomotive is designed to operate, and emphasizes the desirability of simplifying the electrical and mechanical equipment. Experience obtained under actual operating conditions and further studies have already enabled the designers to improve the accessibility of the apparatus and reduce the number of devices. We agree with Mr. Kahler that future developments should be in the direction of simplification.

Professor Lindvall emphasizes the importance of the dynamic braking feature in descending mountain grades. This feature is particularly applicable to the steam-electric locomotive and is obtained by minor additions to the equipment required for motoring. A novel design of braking resistor permits a continuous transfer of braking energy as heat into the main condenser by means of water diverted from the boiler. This is permissible since during the braking cycle only a small percentage of normal steam flow is required from the boiler.

Mr. Sillcox discusses the unique way in which the dynamic brake is blended with the air brake and the excellent results obtained when the two systems are properly co-ordinated. The dynamic brake was not intended to be used as a substitute for the air brake, but as an auxiliary to it. It is especially useful for slowdowns and for holding the train on grades. For emergency stops the air brakes must be applied on the cars as well as on the locomotive in order to meet the requirements of the Bureau of Safety of the Interstate Commerce Commission. Within the limits of motor and generator capacity the dynamic brake is very effective at all locomotive speeds. It can be applied instantaneously and there is no danger of sliding or overheating the wheels. When used in conjunction with the air brakes it appears to give the ideal combination for locomotives in high-speed service.

Tests on Oil-Impregnated Paper—IV

Discussion and author's closure of paper 39-113 by Hubert H. Race, presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939, and published in AIEE TRANSACTIONS, 1940, pages 721–9.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): Doctor Race's findings on the mechanism of tracking in high-voltage cable insulation agree with our findings in single-conductor solid-type 66-kv cable ("Studies of Stability of Cable Insulation," Halperin and Betzer, AIEE TRANSACTIONS, volume 55, 1936, pages

1074–82). We found, however, that tracking can start also at the sheath. While in our tests tracking was apparently started by gaseous ionization, free gas was not present in Doctor Race's samples and breakdown occurred at high stresses in the free oil space. Observations in Chicago on joints and on cable with expanded sheath showed that oil layers of considerable thickness may break down under lower stresses than thin oil films. The thickness of the oil film is, therefore, an important factor to consider.

One of the outstanding points of interest is the finding that copper and lead diffused throughout the insulation in spite of the fact that great care was taken to eliminate oxygen throughout the tests. It is, therefore, difficult to see how soaps could have formed, as acids, which could attack the metals to form soaps, require oxygen for their formation. These results, therefore, indicate that either the diffused metals were not in the form of soaps or that there was some other source of oxygen present in the samples.

In view of this absence of oxygen the amounts of metals in Doctor Race's samples were very large, namely, up to 9,700 parts per million of copper and 29,700 ppm of lead in dry paper. On the other hand, on badly deteriorated oil-filled cable removed from service the maximum amount of copper was 1,100 parts per million in dry paper in one sample and 2,660 parts per million in impregnated paper in another sample. The maximum amount of lead found was 40 parts per million of impregnated paper.

It was our understanding that the striations found sometimes in cable on the tape next to the conductor are caused by reaction of oxidation products in the oil with copper. In view of the absence of oxygen in Doctor Race's samples, what does the author consider to be the nature of the deposit appearing as striations?

The finding that most of the copper is in the oil-free paper rather than in the free oil agrees with our observations in cable tests.

We understand from table I that in all cases samples with washed paper had long lives and that none of the samples with washed paper developed general ionization.

We noted with interest the wide variations in specific inductive capacity of impregnated paper, namely from 2.00 to 4.92 according to table I. What is the cause of the unusually low value of 2.00?

The author states that layers of paper showing carbonization had high power factors. We found that if the carbonized paths are avoided in making the measurements, the power factors of the remainder of the paper are quite normal for aged samples. It seemed to us then that the increases in power factor of the insulation adjacent to the carbonized path resulted from chemical changes in the impregnating oil.

R. J. Wiseman (The Okonite Company, Passaic, N. J.): Our staff has read this paper with great interest and has noted some important facts that are worth keeping in mind whenever we study the breakdown of cables. We think it was ingenious of Doctor Race to take a metal rod and cut it to reproduce in grooves the corrugations which exist when we made up a concentric stranded conductor. He eliminated a very important variable—the flexibility of the stranded conductor, yet obtained the concentration of electric stress of a stranded conductor where the paper contacts the top of the threads. We have enjoyed reading the section on maximum electrical gradient. We are in accord with the views expressed, but we feel he could go a little further in his explanation as to why the brown markings were not at the point of contact of the paper and top of the thread. We believe that the variation in the dielectric strength of the oil in the film of varying thickness between the paper and the electrode are the controlling factor in the breakdown. Referring to figure 5, the dielectric strength of the oil film between flux line *A* and the point of contact of the electrode and the paper, as shown in figure 1 of this discussion at *B*, is higher than at *A*, due to the thin-film characteristic and the breakdown voltage will be higher than that at flux line *A*, even though the actual distance to the outer electrode is less than along flux line at *A*. To the right of line *A*, the oil films are larger between the inner electrode and the paper, and therefore, the dielectric strength per mil of the oil film is less. However, the length of flux lines to the outer paper electrode is greater and the voltage necessary to cause breakdown will be greater. In other words, failure will occur along line *A* because it takes the least voltage to cause breakdown. The thickness of oil film at *A* may only be five or ten mils and it is possible that it will have a dielectric strength of 1,400–1,800 kv (peak) per centimeter as Doctor Race suggests. It is fortunate for us that it requires such a high voltage to cause failure. Otherwise our cables would have to have smooth-surfaced conductors. We know this is so for dry paper insulation as we have reproduced the brown tooth markings on some high-gas-pressure dry-insulation experimental cables and they were offset slightly from the point of contact of the paper and conductor. As we operate our impregnated paper cables at much lower stresses than Doctor Race finds will produce this marking which is the beginning of failure, we do not need to be too greatly concerned about the importance of using stranded conductors, provided, of course, that oil is present around the strands. If not, then we approach the case of the dry insulation condition referred to above, and the breakdown voltage will be low.

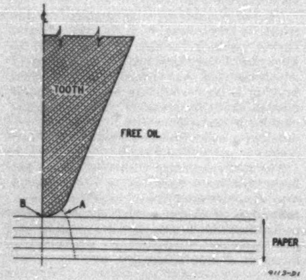

Figure 1

Our study of the radial power factor for the various specimens and tabulated in table I shows that many of the samples showed not only high power factor values at the electrodes but also at intermediate points. In figure 2 of this discussion we have plotted the radial-power-factor curve for specimen 305 to illustrate this. We, too, have observed this from studies we have made and have not been able to explain it. The upturns at the copper and lead electrodes have been explained as due to metallic soap or metallic ions. This will not explain the hump in the intermediate layers because the amount of copper and lead is very low. We hope that Doctor Race will be able to offer an explanation.

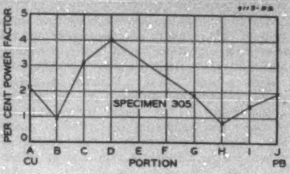

Figure 2

We are very much surprised at the comparatively large amount of copper and lead found on the paper at the lead and copper electrodes respectively for some cable samples. Doctor Race refers to this situation at the bottom of page 3 of his paper and believes it due to diffusion through the specimen because of temperature cycles and the electric field. It is puzzling to us, if there is diffusion, why the center does not show higher metal content. It would seem as if it is due to the high electric stress causing the rapid movement of the metallic ions. (May be initial stage of sputtering of electrodes. See Callender reports.)

Doctor Race's studies confirm Doctor Robinson's theory of breakdown, namely, that failure starts at the point of maximum stress. As the specimens were thoroughly filled with a degasified oil, any reference to the effect of gas voids cannot be made unless the gas is produced by the breakdown of the oil due to high electric stress. This study shows the value of using oil of such viscosity that it will thoroughly fill all the space between the paper and the conductor and also we should provide means for keeping the oil in place. The so-called oil-filled cable accomplishes this. It can also be obtained in solid cables by using oils of about one-fifth the viscosity we now use and let the oil flow through the spaces in the strands of the conductor.

H. H. Race: Near the end of the first paragraph of R. J. Wiseman's discussion he refers to stress "marking which is the beginning of failure. . ." I should like to make a slightly different interpretation of the evidence. Stress markings on oil-impregnated paper are found whenever extremely high gradients have existed for some time. However, they do not appear to be heavier at or near a failure point than elsewhere. In fact, failures may occur to an unmarked region (see also my discussion of Doctor Whitehead's paper on "The Dielectric Strength and the Life of Impregnated Paper", AIEE Transactions, volume 59, 1940, pages 1087–8). Also they have been found in test specimens which have not failed. Therefore, while the stress marks are unquestionably evidences of some chemical changes resulting from high electrical gradients, they are not necessarily indications of the beginning of ionization breakdown.

In the second paragraph, Doctor Wiseman asks for an explanation of the high power factor in the center of specimen 305. Table II shows that the materials responsible for the high power factor were largely but not entirely benzene soluble. Whether these materials were deterioration products of the oil or the paper or both we do not know. On the other hand, the accuracy of these measurements is not too good, probably because of irregularities in electrode contact so that the differences may not be significant except for general trends indicated by a number of samples. My guess is, however, that in addition to metal soaps there are other deterioration products of both the oil and the paper which contribute to dielectric losses. Such deterioration products might have resulted from the incipient ionization which, according to figure 3, penetrated through the first five layers of this specimen.

I agree with the comments in the third paragraph of Doctor Wiseman's discussion and further suggest that some additional chemical reaction, such as catalysis at the metal electrodes, must be postulated in order to account for the concentration of the metal compounds in the layers adjacent to the electrodes.

In his last sentence, Doctor Wiseman implies that the advantages of the oil-filled cable system can be obtained in "solid type" cables simply by using low-viscosity impregnants. I feel sure that he did not mean this since during changes in load, ionization cannot be prevented at rated voltage unless a positive liquid pressure of approximately one atmosphere is maintained at all points in the cable at all times. This requires reservoirs, oil channels, and stop joints and is the "oil-filled" system.

Although not mentioned in the paper we have had specimens whose inner electrodes were smooth instead of threaded cylinders and have observed failures starting from the outside, as noted by Halperin. The important point is that such failures appear to start from an electrode at a point of highest gradient caused by some irregularity such as a sharp point or edge on the surface of the conductor.

Paragraphs 2–5 of Halperin's discussion raise very interesting questions as to the chemical reactions resulting in the observed metal concentrations in the layers of paper adjacent to both electrodes. Mr. Van Brunt of our laboratory has made several attempts at identifying the dark-brown striations in these specimens. The microphotograph of the inner layer of specimen 250, shown in figure 3 of this discussion, shows that the colored material is concentrated in the interstices between the paper fibers and as films encasing the individual fibers. Under the microscope these appeared to be composed of discrete particles. The deposits are definitely *not carbon*, nor sulfides. The material seems much less reactive chemically than ordinary metal soaps. It may be a mixture or compound of metals with hydrocarbon polymers in varying degrees of polymerization.

In the absence of both free oxygen and oxidized oil but in the presence of very high electrical gradients and possibly incipient corona, it appears that chemical reactions are occurring which are very different from those which take place in the absence of the electrical field and with which the chemist is familiar. As pure conjecture I would suggest residual water as a source of oxygen or acidic decomposition products of cellulose or direct combination of metals and hydrocarbons as in catalysis.

I attribute the long life of the washed specimens simply to the shrinking of the paper around the sharp edges of the threaded electrode and the resulting elimination of the free oil space in the highest stress field so that the corona starting voltage was raised above the operating voltage.

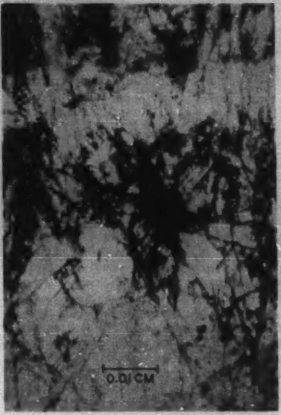

Figure 3. Microphotograph of portion of inside of inner layer of paper from specimen 250 showing concentration of dark-brown deposits in the interstices between and on the surfaces of the paper fibers in regions of highest stress at the edges of the area of contact between the paper and the inner conductor

In his seventh paragraph Mr. Halperin questions the meaning of the large variations in dielectric constant measured in a strip cell. I think these are simply poor measurements caused by wrinkles or irregularities in the paper which were not smoothed out by the moderate weight on the upper electrode, and therefore that they have little significance except to show the order of magnitude of possible errors.

Referring to Halperin's last paragraph, we could find little evidence that oil which could be centrifuged out of the paper was deteriorated. Table I shows the acid number of such portions to be uniformly low and in many specimens the power factor of the centrifuged oil was as low as new oil. Some of those showing high values contained free carbon resulting from incipient failures and carbonized paths in adjacent sections of paper.

The Dielectric Strength and Life of Impregnated-Paper Insulation—I—The Influence of the Density of the Paper

Discussion and author's closure of paper 39-111 by J. B. Whitehead, presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939, and published in AIEE TRANSACTIONS, 1940, pages 715–20.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): The effects observed by Doctor Whitehead all relate to cable fully impregnated with thin oil, as in oil-filled cable and I therefore confine my remarks to cable of that type, in contradistinction to the so-called "solid type."

The use of high-density paper in such cables is primarily for the purpose of increasing the dielectric strength against severe steep-front transient voltages of short duration and there appears to be ample evidence of its value for this purpose. A gain of 15 to 20 per cent in cable dielectric strength, by this means, appears to be entirely practicable.

While, therefore, Doctor Whitehead's study is a valuable contribution to our knowledge of the physics of cable under stress it relates only to a condition which never occurs in service, namely sinusoidal voltages of two to three times normal operating voltage for periods of 50 to 100 hours.

The significance of this study is, therefore, not in its bearing on present cable design but in adding another link to our chain of knowledge in relation to the mechanism of breakdown of impregnated-paper insulation.

R. J. Wiseman (The Okonite Company, Passaic, N. J.): As usual, Doctor Whitehead has written a very clear paper on the researches which he has been carrying out for many years on the dielectric, paper and oil in combination. We, in Insulated Paper Cable Engineers Association have followed this particular research during its progress and have had the opportunity from time to time of offering suggestions and explanations from the application side. It is a surprise to us to note a decreasing dielectric strength when the weight density increases. We have tried to explain it further than the stress in the oil gap as described by Doctor Whitehead. Our views are associated with our experiences in the testing of solid-type cables before and after we had begun to use high-density papers. We have noted higher breakdown voltages when using high-air-resistance paper, although not as much as one would expect if we took the short-time dielectric strength of a sheet of paper and calculated the theoretical breakdown. Doctor Whitehead used a very thin oil and much higher stresses than we test our cables at, so it seems as if there is a time-stress element that has a big influence on what results. Our company's feeling is that we have obtained some valuable fundamental knowledge of a combination of oil and paper that will require careful study in its application.

Our company, when considering the variation in the physical characteristics of paper, has not felt that we should consider the weight density as the important variable, but the air resistance per mil, or else the space factor, which in turn is related to the amount of oil that can be absorbed by the paper. Mr. Gooding replotted for me figures 1, 2, and 3, using per cent of oil by volume in place of the specific gravity of the paper. These are shown in figures 1 to 4, inclusive, of this discussion. Figure 1*A* shows the maximum breakdown stress against per cent oil volume. Papers *A*, *B*, and *D* fall fairly well on a straight line. Paper *C* shows a low stress for the corresponding oil volume. The breakdown values appear to correlate better with the specific gravity as shown by Doctor Whitehead's figure 1. In figure 1*B* breakdown stress is plotted against air resistance per mil and all points except *E* lie on a smooth curve.

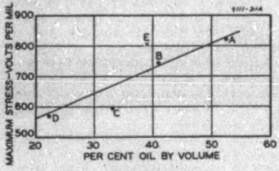

Figure 1A

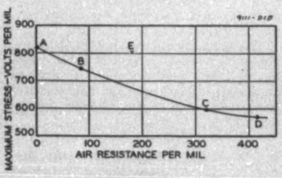

Figure 1B

Figure 2 shows the relationship of power factor and oil volume. It is possible to draw a straight line, and papers *C* and *D* line up better than if the specific gravity is used. Figure 3 shows the watt loss against oil volume, and here also a straight line can be drawn with papers *C* and *D* lining up better with the other papers than when specific gravity is used. Figure 4 shows the conductivity plotted against oil volume and we again get a straight line, but paper *A* seems to be out of line. The last three curves indicate that there is a direct relationship between volume of oil and the various electrical loss characteristics. Granting this is so, Doctor Whitehead is justified in considering the electric stress on the oil in the gaps as the controlling factor in determining the expected voltage breakdown. I think the agreement between the different papers on the oil electric stress, as given in paragraph 4 under "Discussion of Results", is good. Perhaps some of this variation may be due to a distortion of the electrostatic field in the gaps due to the narrowness of the gaps, and therefore, taking the ratio of the dielectric constants is not exactly correct, but is a very good first approximation, sufficiently close to believe that breakdown is a function of the electric stress in the oil gap. An improvement could be made if we knew more about the dielectric strength of oil in narrow gaps and thin films.

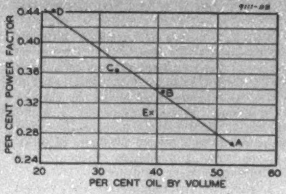

Figure 2

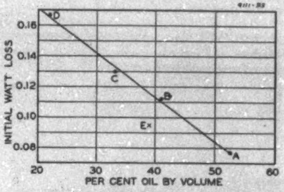

Figure 3

Paper *E*, which Doctor Whitehead used five years ago on another research, lines up fairly well with the other papers, yet its physical properties are quite different. Doctor Whitehead refers to the effect of age on this paper. We are inclined to believe it is due to fiber structure. If we refer to table I on the properties of paper and figure 4 on oil content, we note that it lies between *A* and *B* for gravity, between *B* and *C* for Gurley air resistance, and oil by volume, and just above *B* for oil by weight. It seems desirable, if possible, to find out if this paper was made the same way as papers *A* to *D* and if the same kind of pulp were used. Perhaps the lining up of the paper fibers may be different or more calendering was performed on paper *E*.

We wish to congratulate Doctor Whitehead on another valuable contribution to his previous papers on the electrical properties of oil and paper in combination.

W. T. Peirce and **G. M. L. Sommerman** (American Steel and Wire Company, Worcester, Mass.): Doctor Whitehead has made a signal contribution to the knowledge of the electrical properties of oil-saturated paper insulation. We would like to add our word of caution against the interpretation of these results as applied to solid-type cable insulation.

In the first place the life tests started at an average stress of 400 volts per mil and ran up to 700 volts per mil. Such stresses produce test conditions within the dielectric never encountered in ordinary use and therefore are *more* than accelerated tests.

The tests were made in an open bath using a very light oil which is thus able to move freely and without building up pressure under the mechanical forces set up by spots of ionization.

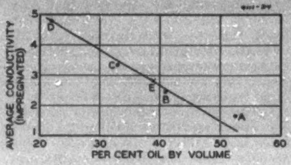

Figure 4

In tests made in an oil bath the property of paper to resist the migration of the heavier saturants ordinarily used in solid-type cables is not realized. This migration of oil is an important feature in the deterioration of actual solid-type cable and the somewhat greater resistance of high-density paper to such migration has not been evaluated by these tests.

The whole body of insulation tested was one grade of paper of relatively small thickness compared with cable insulation. Therefore, no effect of stress grading due to the different specific inductive capacity of the different grades of paper was realized. The effect of grading in some cases is to reduce the value of maximum stress which Doctor Whitehead has found to be so important, and to lead to a more uniform stress distribution throughout the dielectric.

The thyratron interrupter employed by Professor Whitehead and his associates in their cable-life tests makes it possible to obtain considerable information on the origin and mechanism of electrical breakdown of insulation. Thus Whitehead finds that most of the failures begin in the spaces between successive turns of paper tape. H. H. Race has also used the thyratron interrupter in cable life tests, and in table I of his recent paper, "Tests on Oil-Impregnated Paper—IV" (*Transactions pages 721–9*) data are presented on the starting point of, and the number of layers involved in, incipient electrical failures. We should like Professor Whitehead to give similar quantitative information, including the number of incipient failures which began in layer 1, the number which began in layer 2, etc.

The values for per cent oil content by volume given in table I apparently have been obtained by multiplying the per cent oil content by weight by the specific gravity of the paper and dividing by the specific gravity of the oil (0.90). From this it follows that the values given for per cent oil content by weight are in terms of the weight of the dry paper, and are not in terms of the weight of the impregnated paper.

It is, of course, well known that the dielectric constant of impregnated paper increases with increasing density of the paper. Values of dielectric constant which we have measured, however, are about 15 per cent lower than those experimentally determined by Whitehead, and are in good agreement with the average of Whitehead's computed values (for series-parallel distribution of oil and cellulose).

The increase in d-c conductivity of the impregnated specimens with increasing paper density is attributed by Whitehead to increases in supply of conducting ions from two sources: (1) soluble electrolytic impurities in the paper; (2) residual moisture retained by the paper. The first source is the predominating one for reasonably well dried paper, and the magnitude of it may be reduced by additional washing of the paper either during or after paper manufacture. More generally, therefore, the conductivity of impregnated paper depends on the degree of washing of the paper as well as on the density of the paper. As an illustration of this, the d-c conductivity of very well washed high-density paper is lower than that of regular-washed average-density paper. Such variations in conductivity, however, probably are not responsible for variations in breakdown strength and life, within the low range of conductivities covered in Whitehead's studies.

The power factor of impregnated paper at temperatures below 50 degrees centigrade depends almost entirely on the density of the paper and little if any on the degree of washing of the paper; it does not, therefore, vary with the d-c conductivity in all cases. At temperatures above 70 degrees centigrade however, the power factor does vary with the conductivity. Thus, the complete power factor-temperature curves of impregnated papers of different densities often cross each other in the neighborhood of 60 degrees centigrade.

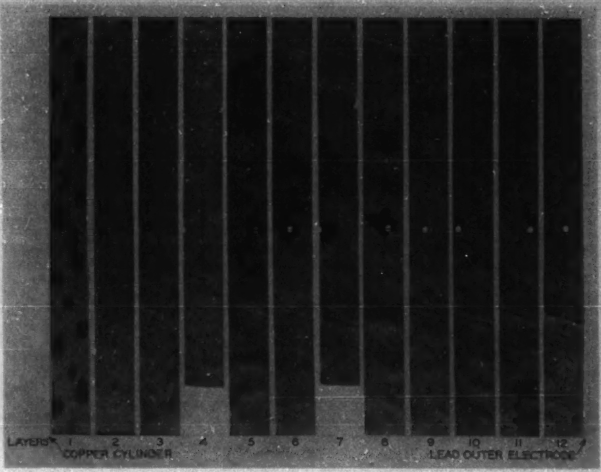

Figure 6. Cell 518, inside surfaces of all tapes in area of puncture

H. H. Race (General Electric Company, Schenectady, N. Y.): I am particularly interested in this paper for two reasons. First, this work, although different in several respects, is related to experiments we have been conducting for several years, as reported at the last four summer conventions; and second, I have been privileged to serve as chairman of the AIEE committee representing the sponsors of the project and have therefore followed its development in detail. I am sure that all members of the sponsoring committee will echo the thought that this project has been carried through in a very excellent manner.

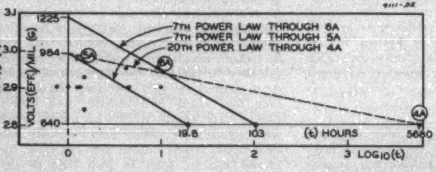

Figure 5. Plot showing that application of seventh-power law to high-voltage life-test data is misleading

We have conducted one experiment in an attempt to correlate my long-time life test at constant voltage with Doctor Whitehead's short-time voltage step-up tests. He sent us three specimens made early in this project and we made a glass cell with a sylphon reservoir so that a specimen could be assembled, evacuated, impregnated, and tested in the same manner as my smaller specimens have been tested. We have obtained a life run on only one specimen because it lasted 236.6 days or 5,680 hours at 30 kv (effective). Since the specimen was made with 12 layers of 0.004-inch (nominal) paper and had a measured thickness of insulation when dissected of 0.047 inch, this corresponds to an average gradient of 640 volts (effective) per mil or 356 kv peak per centimeter.

Several specimens identical to the one I tested were made and tested by Doctor Whitehead during the preliminary stages of his investigation and I am taking the liberty of listing them in table I of this discussion, since none of the specimens reported in Doctor Whitehead's paper is exactly comparable to the specimen I tested.

It has been suggested that we attempt to correlate these data by means of the "seventh power law" proposed by F. M. Farmer ("Tests of Paper-Insulated High-Tension Cables", AIEE JOURNAL, volume 45, May 1926, page 457). This relation may be stated $(t_x/t_l) = (G_l/G_x)^7$ where (G_l) is the gradient at which the specimen will fail in one hour (t_l) and (t_x) is the time in hours which the specimen should live at the gradient (G_x). From table I, I have plotted in figure 5 of this discussion the number of hours to failure of each specimen on its last voltage step. According to the seventh-power law specimen 5*A* should have lasted one hour at 984 volts per mil or 19.8 hours at 640 volts per mil. Actually the specimen had previously withstood 24 hours at 900 volts per mil. Now if this last point were taken as a datum through which to draw a seventh-power line the specimen should have failed in one hour at a gradient of 1,430 volts per mil and in 260 hours at 640 volts per mil. Actually the specimen failed in 1.25 hours at 950 volts per mil, whereas a companion specimen on my test lasted 5,680 hours at 640 volts per mil even though my test is more severe in that the specimens are subjected to daily heating and cooling cycles with a maximum temperature of 100 degrees centigrade. Even with the limited data available this analysis indicates that it may be extremely misleading to attempt to apply an empirical rule to conditions where a proportionately small change in voltage gradient may radically change the mechanism of failure and resultant life. W. S. Clark (AIEE JOURNAL, volume 45, November 1926, page 1158) pointed out in a discussion of Farmer's paper that for oil-filled cable "the exponent, instead of being 7, should be something in the neighborhood

Table I. Life Data on Specimens Made With 12 Layers of 0.004-Inch Grade E Paper Tapes With Edges Slightly Lapped—No Butt Spaces

Specimen number	4*A*	5*A*	5*B*	5*C*	6*A*	7*A*	8*A*	9*A*	10*B*	11*A*	11*B*	11*C*
Hours to failure	5,680	9.5	0.75	1.5	1.25	4.25	4.5	10	1	1.5	1.25	1.33
At volts per mil	640	900	800	850	950	900	800	800	800	700	800	800

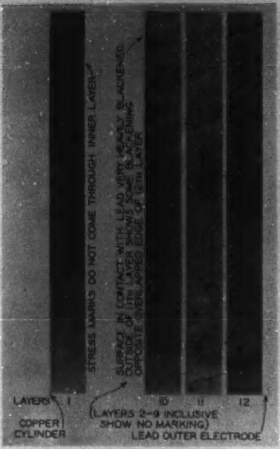
STRESS MARKS DO NOT COME THROUGH INNER LAYER
SURFACE IN CONTACT WITH LEAD VERY HEAVILY BLACKENED
OUTSIDE OF 11TH LAYER SHOWS SOME BLACKENING
OPPOSITE OVERLAPPED EDGE OF 12TH LAYER
LAYERS
1
10
11
12
(LAYERS 2–9 INCLUSIVE
SHOW NO MARKING)
COPPER
CYLINDER
LEAD OUTER ELECTRODE

Figure 7. Cell 518, outside surfaces of inside tape and three outside tapes

of 19". Note in figure 5 the line drawn through point 4*A* with an exponent of 20. I think that the absence or presence of ionization is one of these critical phenomena and that corona-starting-point measurements may be even more important than short-time extreme-voltage breakdown tests.

In connection with my paper in which I show that chemical deterioration (like cancer) may be the cause of death at a ripe old age if the patient is immune from ionization (like pneumonia), the appearance of the tapes removed from Doctor Whitehead's specimen is of interest (see figures 6 and 7 of this discussion). The inside of the first layer shows large areas of stress marking around each hole in the inner conductor although the puncture did not occur at such a spot in agreement with Doctor Whitehead's observation that the punctures showed no preference for such spots. The outer surfaces of the outer layers showed considerable darkening, indicating metal reaction products.

The results of our work agree in so many aspects with those reported by Doctor Whitehead that I can only take the space necessary to point out where we disagree.

1. Doctor Whitehead states that failure sometimes starts in oil channels "in the center of the insulation wall". True, the damage is sometimes more apparent here, as explained in my paper and sometimes a magnifying glass is necessary in order to find the small hole which constitutes the conducting path back to one electrode. However, close examination of hundreds of our specimens, many of which had multiple partial failures, has never revealed a single carbonized path which did not start at one electrode. I hope Doctor Whitehead has kept the paper from all his specimens so that he can check back on this point.

2. Doctor Whitehead states that calculated gradients of 1,500 volts (effective) per mil or 835 kv peak per centimeter are too high to be considered as breakdown gradients in oil. However, my paper on insulating liquids shows hundreds of measurements of this order and higher. It seems to me that our data make unnecessary the rather forced explanation that the dielectric constant of the oil is increased 50 per cent by ionic conduction and space charges. To be sure, ionic conductivity is enormously increased at high gradients but the energy concentration is still extremely small compared to that occurring in a spark discharge and I believe the latter is necessary before carbonization of paper can start.

3. The dielectric constants reported by Doctor Whitehead are considerably higher than those obtained in commercial cable made from papers of similar density. For comparison, table II of this discussion shows data taken in the factory on full lengths of commercial cable and on ten-foot samples made up for test purposes. I believe the reasons for these differences are that Doctor Whitehead's specimens are wound with smaller butt spaces and since the total insulation wall is relatively thin and the inner conductor is a rigid cylinder, the paper can be wound much more tightly than on commercial cable. Cable with thick insulation wall has to be built with adequate butt spaces and with decreasing tension at increasing radii in order to pass the severe bending and wrinkling requirements. Therefore the volumetric proportion of paper fiber to oil would be greater in Doctor Whitehead's specimens than in commercial cable with a correspondingly greater apparent dielectric constant.

R. W. Atkinson (General Cable Corporation, Perth Amboy, N. J.): Doctor Whitehead's results indicate that for his samples long life is associated with *low* paper density. Tests on cables, however, have indicated that long life in cables is associated with *high* paper density.

The correctness of Doctor Whitehead's conclusions for his samples is not open to question and his theory explaining his results appears adequate and reasonable. Similarly, we believe that the conclusions with respect to cables rest on an equally sound foundation.

The foundation on which cable practice rests is mainly the result of direct comparative tests made on cables insulated with paper of different densities, that is to say, the result of tests with the same variable as that used by Doctor Whitehead. Before presenting such data, however, it will be useful to give a statistical picture.

We have analyzed data taken from extensive reports of the Electrical Testing Laboratories, showing the results of specification tests by all cable manufacturers over the period from 1926 to 1931 (both inclusive). Specifically, data have been taken for so-called life tests under the Association of Edison Illuminating Companies specifications in which failure normally took place in from 12 to 24 hours. The data selected are limited to single-conductor cables because fewer extraneous conditions, such as terminal failures, are here involved.

The average dielectric strength, for each year, of all single-conductor cables covered in these reports is shown in figure 8 of this discussion. From 1926 to 1928 there is a one-third increase in dielectric strength. The year 1926 marks the beginning of important commercial introduction of wood paper. By the end of 1928 wood paper had practically supplanted manila paper. Wood paper, as initially introduced, had materially greater density than did manila, and during this period of time the density for both manila and wood pulp was continually increasing. It will be noted that there is a peak in the curve for the year 1929, with the values dropping off again for 1930 and 1931 to values only slightly higher than for 1928. Detailed analysis indicates that the high value for 1929 was due to unique causes making the data for that year not comparable with the data for other years.

That the large increase in dielectric strength has coincided with a large increase in paper density does not necessarily mean that there is a causal relationship between them. However, when coupled with the fact that manufacturers were under marked pressure to increase the strength of cables as measured by this test and the further fact that each, individually, had opportunity to evaluate results on cables made with steadily increasing paper density, the evidence is strong indeed that the effect obtained was at least in part due to this cause.

Over a period of many years extending to

Table II. Dielectric-Constant Data for Commercial Cable

	Number of Lengths	Diameter of Conductor (Inches)	Diameter Over Paper (Inches)	Density of Paper	Dielectric Constant		
					Maximum	Minimum	Average
Commercial lengths	10	1.265	2.465	0.75–0.78	3.54	3.42	3.46
	5	1.500	2.940	0.75–0.78	3.44	3.40	3.41
	1	1.678	3.428	0.75–0.78			3.46
Ten-foot samples		0.814	1.376	0.8 –0.83	3.52	3.24	3.35
		0.814	1.376	0.9 –1.0			3.58
		0.814	1.376	1.18			3.83

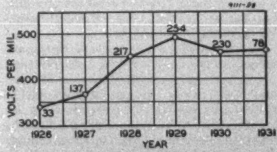

Figure 8. Average dielectric strength of single-conductor cables by years, as shown by the 1931 report of cable inspection of Electrical Testing Laboratories

The number at each point is the number of tests averaged the present time, and including a period of intense activity around 1928, much information involving *direct comparison* between lower and higher density paper has been obtained by cable manufacturers. Obviously, only a small part of these data can be given here, but it should be borne in mind that the trend of all the data on cables, extending to the present time, is entirely consistent with the typical tests here mentioned.

During 1928 we carried on in two laboratories (Perth Amboy and Pittsburgh) an extended program, including both short-time tests and tests extending to hundreds of hours duration. Paper of five-mil and eight-mil thickness, both manila and wood pulp, was used, the density range corresponding with the lower end and middle of Doctor Whitehead's. In the tests at Perth Amboy, the full thickness of insulation (9/32 inch) of the lead-covered cable was tested. At Pittsburgh the sheath and outer layers of tape were removed down to a remaining thickness of 6/32 inch and a foil outer electrode applied.

The results of the short-time breakdown tests from both laboratories are shown in figure 9 of this discussion. The dielectric strength is plotted against the air resistance (reciprocal of porosity), rather than the density of the paper. In general, increasing air resistance is accompanied by increasing density, although our tests have shown a closer correlation of dielectric strength with air resistance than with density. It will be noted that the dielectric strength increases with air resistance, rapidly for low values, and slowly for high values.

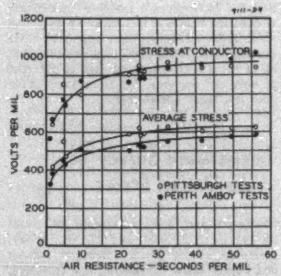

Figure 9. Breakdown stress of cables as a function of the air resistance of the paper used in the dielectric

All tests are evaluated to five minutes on the assumption of a seventh-power voltage-time relation ($E^7 = KT$)

Long-time tests at 45 kv (160 volts per mil, average stress, 275 volts per mil at the conductor) are shown in figure 10 of this discussion. Here there is even a more marked effect of air resistance, particularly in the lower range.

Tests were also made with graded insulation, using denser paper near the conductor than in the outer layers. The results of these tests are in general agreement with those just indicated. Other comparisons have been made, both long-time and short-time, covering a density range corresponding to the upper half of the range in Doctor Whitehead's tests. In these tests there is also a general tendency for longer life to be accompanied by higher density, although the actual difference in life between papers within this range is small. Similar results have been obtained on load-cycle aging tests made to represent accelerated service conditions. In none of these has there been a relationship between density and strength like that found by Doctor Whitehead.

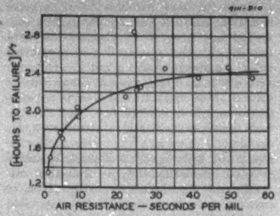

Figure 10. Seventh root of hours' life of cables at 45 kv (160 volts per mil, average stress, 275 volts per mil at the conductor) as a function of the air resistance used in the dielectric

It should be recognized that the importance of Doctor Whitehead's results is all the greater because of the apparent disagreement between his results and those just given. Moreover, it is not to be concluded that because the apparent disagreement is so great and so fundamental there can be no conciliation.

From the physical comparison of Doctor Whitehead's samples with cables it is not obvious why the density-dielectric strength relation should be so different. However, there are important physical differences and some of these must be responsible for the difference in performance. The specific dielectric strength of Doctor Whitehead's specimens is very high compared with that obtained in cables and this high relative strength is one measure of the importance of the actual difference between the physical character of his specimens and of cables. It is not at all unreasonable that the main difference between the effect of paper density in his specimens and in cables is due to developing in cables a much lesser proportion of the theoretical strength with paper of low density than with paper of high density.

Some of the differences between Doctor Whitehead's samples and cables are the following:

(*a*). Doctor Whitehead's conductor is a substantially smooth cylinder as contrasted with the stranded conductor of cables.

(*b*). The insulation on his samples did not need to be designed so as to permit bending. This gave greater latitude as regards paper application (tightness of wrapping, amount of valley space between adjacent turns) than is permissible in cables. (Aside from the rigidity of the conductor, I believe the insulation on his specimens would not permit the bending that cables are required to stand.)

(*c*). The dielectric constants reported by Doctor Whitehead for his samples are of the order of 10 to 20 per cent higher than are obtainable on cables made with equal density of applied insulation.

(*d*). Associated with most tests on *cables* there has been a certain amount of corona on test leads, whereas Doctor Whitehead has carefully avoided corona. It should be noted, however, that one somewhat limited experimental study of the effect of corona in cable tests has shown no difference in results due to corona.

(*e*). The fact that there is a longitudinal variation in even the most carefully made specimens gives some advantage to very short specimens such as Doctor Whitehead's. That this may not be unimportant is illustrated by the discarding even in Doctor Whitehead's data of the results from some specimens found subnormal on the tests.

It will be noted that, while obviously many of these differences help to explain the high strength of Doctor Whitehead's samples in comparison with cables, none of them seems an obvious explanation for the reversal in relative strength of high and low density paper.

Another relevant point may also here be mentioned. Commercial cables in this country fall, in general, into two classifications: "oil filled" and "solid type." Inasmuch as the saturant used in Doctor Whitehead's tests is similar to that used in oil-filled cables, his data are more directly applicable to that type. The tests and experience reported in this discussion are confined to "solid-type" cable, but commercial experience with "oil-filled" cables is not materially different from that with "solid type." Tests which Doctor Whitehead plans to make later, with a less fluid saturant, may perhaps show agreement with experience with "solid-type" cables.

While Doctor Whitehead's results are not directly applicable to cable design, their practical importance must not be underestimated. His results will be of great value even though there should be no further contribution than the fundamental data and analysis which have been given; and since fundamental information such as his usually goes on to practical applications not originally suspected, such a result may reasonably be anticipated from Doctor Whitehead's work.

D. M. Simmons (General Cable Corporation, New York, N. Y.): Mr. Atkinson has discussed Doctor Whitehead's paper largely with respect to "solid-type" cable. My discussion will be with respect to oil-filled cable. For several reasons, consideration of Doctor Whitehead's data in connection with oil-filled cable must be on quite a different basis than for "solid type."

First may be considered the requirement of service. In the case of solid-type cables, the limitation of voltage rating for a given thickness of insulation is essentially the long-time voltage strength of the cable. Normally, the strength for transient voltages is entirely adequate to take care of any transients which may occur. In the case of oil-filled cables, in general less than one-half the solid-type thickness of insulation is used for any given voltage and it is clear from tests, laboratory and field, that even considerably lesser thickness may be adequate so far as concerns the long-time strength. However, the transient strength of a given thickness of cable insulation is no greater for oil-filled than for solid-type insulation. Thus, while the margin for withstanding *normal 60-cyle voltage* is greater for oil-filled cables than for solid type, the margin for withstanding *transient voltages* is much smaller. While operating experience has clearly shown that with present insulation thicknesses of oil-filled cables there is ample margin for both operating and transient voltages, the difference in relative factor of safety seems to warrant placing emphasis on transient dielectric strength. For these reasons, any procedure tending to increase the transient voltage strength of oil-filled cables seems to be in order.

For the determination of the transient and of the long-time 60-cycle voltage strength of oil-filled insulation, we made up and tested, some years ago, two cables similar in all respects except as to the density of paper. Cable A was made with paper of approximately the highest density used by Doctor Whitehead, while cable B was made with paper of a density intermediate between Doctor Whitehead's two least dense samples. Both were saturated with "oil-filled cable oil." In the surge voltage tests, cable A gave a dielectric strength about 25 per cent higher than cable B. In the 60-cycle tests, made at a voltage such that failure occurred after several hundred hours, the results for both cables were practically identical. Thus, while the "long-time" 60-cycle strength in these tests on oil-filled cable was substantially independent of paper density, the transient voltage strength was definitely higher for the paper of higher density.

Another reason, important though nonelectrical, emphasizes the desirability of using dense insulation with oil-filled cables. It will be recalled that in Doctor Whitehead's tests the densest paper which he used contained, within a given volume, less than one-half as much oil as the least dense paper. The greater the oil volume, the greater will be the change in volume due to temperature changes and the greater the channel and reservoir capacity which must be provided. Thus, the denser the paper used, the smaller the oil channels and reservoirs would need to be.

Accordingly, in the case of oil-filled cable, technical and economic reasons give special and definite advantage to the dense paper without any corresponding disadvantage.

J. B. Whitehead: It is a pleasure to find that no one has questioned the accuracy of our conclusion that laboratory and accelerated life tests of impregnated-paper insulation of the oil-filled type indicate decrease of dielectric strength and of life with increasing density of the paper. The foregoing discussion has in fact taken the direction of an examination of this result as related to the design and the operation of present-day cables of standard types. Unfortunately our results are in apparent contradiction to the convictions of some of our most competent and experienced cable engineers, on the influence of increasing paper density, and also to existing practice in cable manufacture in which high-density paper is used with the aim to increase dielectric strength. Much of the discussion is the presentation of evidence and reasoning to the effect that our results should not be applied in cable design and that in cables, higher-density paper means higher and not lower dielectric strength and life.

Thus, we have before us a remarkable situation in which a series of carefully controlled laboratory tests planned and conducted with the co-operation of prominent cable engineers has given results exactly contrary to the present convictions of some of these engineers and of certain manufacturers. It is not the first time that we have found ourselves in a position of this character. We have frequently conducted laboratory investigations of single variables in cable insulation only to find that our results are discounted by cable engineers because of the large number of uncontrolled variables in cable insulation, and the belief that behavior due to one variable is not independent of that of the others.

It was not my intention in the paper nor is it my intention now to express an opinion as to the significance of our results for cable manufacture. The extremely able group of engineers who have discussed the paper are better qualified for this purpose and the discussion indicates clearly the reasons why most of them feel that our very clear-cut results should not be applied for modification of present-day methods of design. I shall therefore limit myself to commenting on only a few of the matters brought forward in the discussion.

Messrs. Atkinson, Del Mar, Sommerman, and Peirce have indicated clearly evidence and reasons why they feel that the results should not be applied to cables with solid-type insulation. No exception can be taken to these excellent surveys and moreover our experiments have been limited to insulation of the oil-filled type. Our experiments are continuing under conditions approaching very much more nearly the solid-type insulation and when this phase of the work is completed, it is possible that further light may be thrown on the question of paper density in solid-type insulation.

Messrs. Del Mar and Simmons have emphasized the importance of high-density paper in increasing the impulse strength of oil-filled insulation. The results they report are well known and cannot be questioned. It may be pointed out, however, that under impulse voltages the division of stress between paper and oil channels is the same as at low frequencies, that incipient discharges in the oil are possible, gas formation being suppressed by the sealing action of the oil, and that under a sufficient number of high-voltage impulses, a progressive attack of the paper structure by minute discharges is still possible.

Our results, it seems to me, apply particularly to the long-time stability and life of oil-filled insulation. Mr. Del Mar apparently attaches no importance to this question. Mr. Simmons, however, recognizes it as an important question although relatively unimportant as compared with the impulse strength. While the duration of our life tests was relatively short, there was plenty of evidence that the time element entered at any value of stress and that therefore progressive instability was present. Although our range of stresses was relatively high, there can be no doubt that even within the ranges of stresses of practice, there are always present a certain number of points of exceedingly high stress between paper fiber and oil from which initial discharges take place and progressive deterioration sets in. Our results, therefore, seem to me to apply definitely to the matter of the long-time stability and in some measure also to the repeated impulses on an oil-filled insulation. So far as I know, no data have been recorded on these questions except the single isolated example reported by Mr. Simmons. I do not see how it is possible to avoid the conclusion that so far as long time behavior is concerned the use of high-density paper works against long life. It is a pleasure to note that Messrs. Atkinson and Wiseman feel that careful consideration must be given to our results in balancing the various important elements in future cable design.

Several speakers have commented on the high values of dielectric constant and I am glad to report that we have found an explanation of something more than one-half of our excess values. The results as reported were based on values of charging current in the ground connection of the measuring electrode of our sample. More recent measurements based on the capacitance in the Schering bridge have shown that the former values are too high and that our correct values which have now been checked in various ways at both 60 and 1,000 cycles are only 6 or 7 per cent higher, instead of 15 per cent, than the values found by various manufacturers. The error in the ground-current measurement was due to a peaked form of the current wave, apparently due in its turn to a slight third harmonic in the apparently smooth generator wave. In my paper, I have given a possible explanation of our high values of dielectric constant and some of the discussers have suggested other explanations. It is obviously possible that all of these may enter and that an increase of only six per cent for their joint effect does not seem unreasonable.

Temperature Survey of the United States

Discussion and authors' closure of paper 39-141 by J. J. Smith and H. W. Tenney, presented at the AIEE combined summer and Pacific Coast convention, San Francisco, Calif., June 26–30, 1939, and published in AIEE TRANSACTIONS, 1940, pages 769–75.

P. L. Alger (General Electric Company, Schenectady, N. Y.): This question of limiting values of ambient temperature in which normal apparatus life may be expected under rated load operation has been discussed at length in recent International Electrotechnical Commission committee meetings. As a result, the IEC has proposed a dual value of ambient temperature of reference; a maximum or limiting value of 40 degrees centigrade being associated with a limiting value of 35 degrees centigrade averaged over any 24-hour period. The 35-degree-centigrade value was selected largely as a result of an investigation of limiting summer temperatures in Italy conducted by B. Cerretelli, who has kindly given me access to his data.

The cities of Milan, Naples, and Palermo were selected as representative of the extreme climatic conditions under which power apparatus is used in Italy, and a three-year record was made of the outdoor temperatures experienced. It was found that a 32-degree-centigrade average over a 24-hour period was normally associated with a 40-degree-centigrade maximum temperature. Since it was desired to adopt a value representative of the maximum value of daily average temperature, and because the maximum and average temperatures indoors usually differ less than the outdoor values, it was decided to assign the value of 35 degrees centigrade as the limiting 24-hour average temperature of reference for international rules.

It is interesting to note that the summer temperature conditions in Palermo are about identical with those in Chicago, as reported in this paper:

	Palermo	Chicago
Average summer temperature	23 deg C	23 deg C
Average daily variation	10 deg C	9 deg C
Maximum temperature recorded	40.5 deg C	39.5 deg C
Average number of days exceeding 32.2 deg C	13	12

Differences in usual operating loads as compared to rating, in continuity of loading, and in life expectancy, representative of the practices in different countries, must be considered in any international comparison of the standards of rating and the operating recommendations. It is to be hoped, however, that the data presented in this paper and its discussion will be helpful in bringing about a closer alignment of IEC and American practices in apparatus rating and operation.

A. Naeter (Oklahoma Agricultural and Mechanical College, Stillwater): The "Temperature Survey of the United States" is a real contribution to the literature available to the electrical engineer. Its authors are to be congratulated upon making available to the profession the various conclusions that may be drawn from the publications of the United States Weather Bureau.

Just a few years ago it was my privilege to supervise a research project involving the design of less expensive rural electric lines than were being built at the time. In one phase of the study determinations were made of the maximum probable temperatures that could occur, under the probability theory, during the life of the distribution circuit. All temperature records of the United States Weather Bureau in Oklahoma City were used to determine the probable maximum temperature in a 20-year period, the assumed composite life of such a power line. This computed temperature was used to determine the maximum sag, during summer months, and thus provide the required minimum clearance at the same time. Yet it was recognized that the maximum temperature of a 100-year, or even a 1,000-year period could come within that 20-year life. As a matter of fact, during an extreme heat spell two or three years ago, but after the completion of the research project, the heat produced temperatures that would not occur more often, than once in several hundred years. This risk must be recognized and considered in financing projects.

The same study gave data on the probable minimum temperatures that could be experienced during the same time, as well as data on wind and ice loading, etc. It showed, for example, that while the maximum temperatures in August were higher than those in July, the minimum temperatures were lower, thus producing a wider spread, as a possibility, for August than for July.

According to Smith and Tenney "the maximum temperature to be expected once in ten years is three degrees Fahrenheit below the maximum recorded to date". The probability approach to this value is a much safer one than placing dependence on maximum recorded data in that the study, of the conditions in a series, brings into the problem the frequency of lesser conditions, which indicate to a much greater degree of accuracy the probable frequency of future happenings.

In terms of the results of the probability study for Oklahoma City the maximum temperature to be expected once in ten years is five degrees Fahrenheit above the probable annual maximum. There is no fixed maximum to which this ten-year maximum could be referred, because the probable maximum for a 1,000-year period is certainly higher than that for a 100-year period. The difference in reference points is fundamental.

The authors report that a study of the maximum temperature recorded at all points in the country shows that 104 degrees Fahrenheit is an average of these maxima. In the probability study made for Oklahoma City the probable maximum temperature occurring annually is 102 degrees Fahrenheit while the median and the mean are 101.5 and 101.4, respectively. When the median approaches the mean in value, it is commonly an indication that the series of observations follow closely the laws of probability. It is clear that the value secured from the probability studies for Oklahoma City is in close agreement with the national average value indicated by the authors' paper.

Details of the probability studies on temperature are shown on pages 26–40 of Publication No. 20, Rural Line Design Investigations, Bernard E. Lowe, published by the Engineering Experiment Station of the Oklahoma Agricultural and Mechanical College in Stillwater.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have given in their paper some very valuable information on temperature conditions created by nature and have supplied thereby an excellent background for further considerations. From the data given it is quite evident that for large areas of the country there is a comfortable margin of several degrees between the ambient temperature set by the weather and the customary 40 degrees commonly used for many years as a basis for machine ratings. In a great many cases, in fact probably for the larger part of installed machinery and apparatus evaluated in kva capacity, there are factors which bring the ambient temperature appreciably above that supplied by nature in a shaded open-air location.

Most machinery is installed in enclosed areas. Electrical apparatus in itself always generates heat, thus raising the temperature within the enclosure above that outside in spite of reasonable provision for ventilation. With generating equipment, except that in hydraulic stations, there is furthermore the heat generated by heat engines, such as steam turbines, Diesel engines, etc., which contributes materially to an increased ambient temperature. Not only the generators but most of the auxiliary rotating apparatus as well as the converting apparatus in power stations and substations is exposed to it. Even with arrangements for forced circulation of outside air it is often difficult to avoid re-entry of some of the heated air into the system. This is true not only of stationary installations of machinery but also of traction equipment; especially when some or all of the cooling air is taken from the cab of a locomotive, the temperature of the cab is usually above that of the air outside. Again, in many industries using electric motors and apparatus, there are in addition to this electrical apparatus many heat-generating processes within the same building which result in elevated ambients. Many indoor motors, such as those used in the oil industry, are located in small enclosures exposed to considerable sun radiation; similar conditions apply to small switch housings, etc. In outdoor installations of apparatus the heat absorbed from sun radiation may be a factor of practical importance. These and other conditions are not merely of occasional occurrence but are more or less typical, each in its particular field of application, and, therefore, the margin furnished by nature does not warrant a feeling of added security but is really needed in many cases, if not the majority, in order to justify the 40-degree ambient temperature usually assumed. This is especially true with large apparatus and machines used in the central-station field, in some traction work, in many important industries, and also in marine applications.

On the other hand, it undoubtedly is true that the margin indicated in the paper actually is available in many installations of smaller motors, perhaps in an appreciable percentage of such motors (in number, not in horsepower). When these small motors are installed in rooms which are large in comparison with the motors, the indoor conditions will often be even more favorable than the natural outdoor temperature conditions. This is fortunate, because with small motors a certain margin is required for other purposes. For example, the power supply (voltage in particular) varies widely, and since the obvious economies of quantity production of small apparatus make the manufacture of motors for many slightly different voltages impractical, reasonable allowance must be made for this (see my discussion of "Determination of Temperature Rise of Induction Motors", by E. R. Summers, AIEE TRANSACTIONS, volume 58, 1939 (September section) page 474).

Although conditions vary a great deal, there seems to be considerable evidence that our existing rating structure is essentially sound for the majority of conditions met with in actual practice.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): Messrs. Smith and Tenney have presented some very valuable data on the weather conditions that can be used for many years to come.

Having been closely connected with the question of ambient temperatures under discussion for transformers during the past ten years, I am pleased to see that the data which the authors have presented justify the action taken by the AIEE transformer subcommittee and the ASA sectional committee on transformers in recognizing the fact that the daily average ambient throughout the country is about 30 degrees centigrade, coupled with a maximum ambient of 40 degrees centigrade. These facts have been used as a basis for the guides for operation of transformers by temperature, which are being issued as an appendix to the standards.

The values given in the sixth column, table I, show that the average number of days per year in which the temperature exceeds 90 degrees Fahrenheit (32.2 degrees centigrade) is approximately 25 days. The temperature, of course, does not exceed 90 degrees Fahrenheit during the 24-hour period. Assuming that it exceeds 32.2 degrees centigrade six hours each day, this means that the total time during the year is only 1.71 per cent. This indicates that a daily average ambient of 30 degrees centigrade as established in the transformer standards is a reasonable and conservative value in relation to actual conditions.

Several years ago it became quite apparent that, if certain electrical apparatus having 55-degree-centigrade temperature rise, and using class *A* insulation were operated continuously at full load, in a 40-degree-centigrade ambient, undue deterioration of the insulation would take place. This necessitated taking one of two steps; either reducing the temperature rise below 55 degrees centigrade for rating purposes, or setting up a lower reference ambient temperature in which the apparatus could be operated continuously at rated load with a reasonable life. It was decided to reduce the reference ambient to a value more nearly in agreement with actual conditions rather than reduce the temperature rise. Experience since that time has well justified this change.

Not only has the idea of establishing a daily average ambient, along with the maximum ambient, been accepted by the AIEE and ASA transformer committees, but the IEC has also established a maximum ambient of 40 degrees centigrade with a daily average ambient of 35 degrees centigrade.

In view of the progress being made in establishing both a daily average and daily maximum ambients here and in Europe, I am disappointed in the only conclusion reached in the paper, namely, that an ambient of 40 degrees centigrade selected for general rating purposes is a reasonable one. I am afraid that this conclusion may give the impression that it is safe to operate 55-degree-centigrade apparatus continuously in a 40-degree-centigrade ambient. I say this because the present standard ambient of 40 degrees centigrade for air, which was established in 1913, for rating purposes only, has been interpreted by many to mean that apparatus should be capable of carrying rated load continuously in a 40-degree-centigrade ambient.

While the data given in the paper can be used, if it is decided to establish both daily average and daily maximum ambients when revising AIEE Standards No. 1, I feel it would have been very desirable for the authors to have discussed this question in the paper.

J. J. Smith and H. W. Tenney: Mr. Montsinger has given a very interesting discussion on the basis of rating of apparatus. He has expressed his disappointment that the scope of this paper did not include this subject. The paper was intended only to furnish facts on atmospheric temperature conditions to the profession and to aid the AIEE standards committee in its work. This committee will undoubtedly consider such points as are introduced in the discussions of Messrs. Alger, Montsinger, and Hellmund. The brief reference in this paper to the 40-degree-centigrade ambient as a basis for rating, was merely made to indicate that the purpose of this paper was not one of advocating a change. There is apt to be confusion between the basis of rating apparatus and the basis of application. The temperature selected as a basis of rating is a benchmark for the purpose of comparison, whereas in the application of apparatus due consideration must be given to the conditions under which it is to be used.

The authors were glad to note Mr. Alger's reference to the international standardization situation. The efforts directed toward international standardization are, of course, important and must be given consideration in the evolution of standards in this country.

Professor Naeter has presented an illustration of the application of the information presented in this paper. In calculating the sag of a power line, the probable maximum temperature is a very important factor in establishing the minimum clearance. On the other hand, as far as insulation of electrical apparatus is concerned, if the maximum atmospheric temperature anticipated in a given period is exceeded infrequently no serious damage is done.

It was felt that the presentation of a summary of atmospheric conditions throughout the country for an extended period of time should prove useful in many ways. The scope of the paper was limited to just this and no attempt was made to analyze the uses to which it might be put and the methods used in its application.

Electronic Measurement of Surge-Crest Voltages

Discussion and authors' closure of paper 39-156 by J. M. Bryant and Morris Newman, presented at the AIEE Great Lakes District meeting, Minneapolis, Minn., September 27–29, 1939, and published in AIEE TRANSACTIONS, 1940, pages 807–11.

C. M. Foust: See discussion, page 1094.

E. J. Wade (General Electric Company Pittsfield, Mass.): Messrs. Bryant and Newman are to be congratulated on the excellent work which they have done in measuring short impulse voltages by means of electron tubes.

Now that the accuracy for steep waves has been demonstrated, the method should find wide acceptance, not only where the cathode-ray oscillograph is not available, but as an accessory to it particularly where a large number of similar measurements must be made.

The authors have shown that the circuit is capable of accurate results on waves which are so short that accurate results with the cathode-ray oscillograph are difficult or questionable. Under these conditions the use of the circuit will be attended by most of the difficulties encountered with cathode-ray oscillographs, such as oscillations induced on the circuit components and the errors of voltage dividers. However there is the danger in the use of the electronic trip circuit that the presence of fast oscillations may not be suspected whereas on oscillographic measurements the circuit must be modified to eliminate or reduce oscillations to an unobjectionable value.

This means that in the use of the electronic circuit the same technique must be adopted to avoid oscillations as is used in oscillograph work.

I would like to ask whether in the development of the electronic trip relay circuit an attempt was made to use the Eccles-Jordan circuit or its modifications, which require only a single power supply, which could be common to a multiplicity of stages. If equally short tripping times could be obtained this would permit a very material simplification on a ten-stage circuit as it would eliminate ten power supplies.

M. Newman: We quite agree with C. M. Foust in distinguishing between the functions of an electronic instrument which gives surge-crest values and of the cathode-ray oscillograph which records the entire phenomenon. It is to be emphasized that a direct-reading crest instrument of the type discussed does not replace but supplements the cathode-ray oscillograph by providing a convenient check on the oscillograph as well as giving considerable information more directly. Adjusting the operating range is no more difficult than in the case of the cathode-ray oscillograph; and, as E. J.

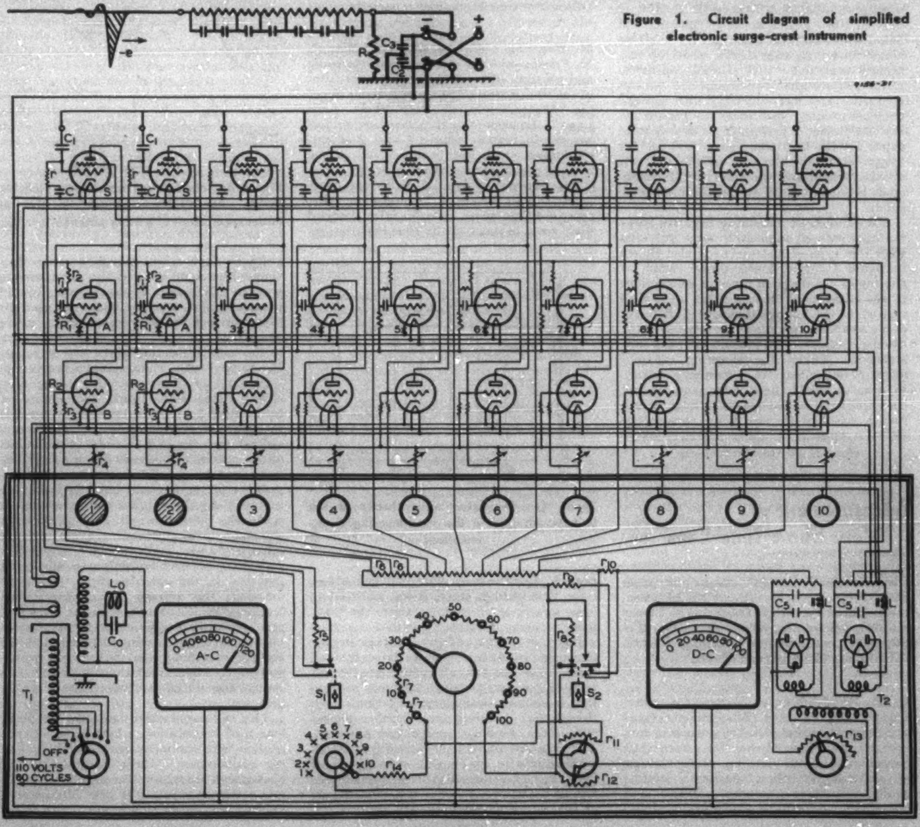

Figure 1. Circuit diagram of simplified electronic surge-crest instrument

Wade points out in his discussion, the problems of obtaining proper voltage division and freedom from superposed stray oscillations are the same as with the cathode-ray oscillograph.

Replying to Mr. Wade's question as to possibilities of using a common power supply to the various vacuum tubes, we present figure 1 of this discussion which shows a simplified instrument. In the original instrument described in the paper a maximum of versatility and flexibility was desired, and separate power supplies were incorporated so that each of the ten units could be used independently of any other. As it became apparent that we could find application for a number of such instruments in our research work, and as it was not so important to use the same instrument in a variety of ways, we combined the separate circuits, as shown in figure 1. Quite probably further simplification is possible. For instance, Doctor H. J. Reich in his book "Theory and Applications of Electron Tubes", pages 206–08, gives a "trigger circuit" modification using a single multigrid tube, and Doctor H. E. Hartig and M. W. Leslie at the University of Minnesota have worked out a very sensitive single-tube triggering device based on secondary emission. But general considerations indicate that combining the various operating functions necessary into a single tube may easily contribute to instability. We feel that it is preferable to use separate tubes for the bias comparison and for the trigger section; the cost of the tubes is relatively low, and the plate power supply drain is not increased by the use of separate tubes.

The method of measuring negative surge voltages, as well as positive surge voltages, with this type of instrument is also shown in figure 1. The double-throw switch at the top of the diagram reverses connections of the entire bank of measuring units with respect to the voltage divider. This introduces a capacitance across the low end of the divider, but it can be shown that for a low-resistance divider the shunting capacity effect is negligible. It is generally advantageous to shield the divider, and in that case the ratio of capacitances may be adjusted so that the shunting capacitance effect is completely compensated.

Measurement of Very Short Time Lags

Discussion and authors' closure of paper 39-157 by J. M. Bryant and M. Newman, presented at the AIEE Great Lakes District meeting, Minneapolis, Minn., September 27–29, 1939, and published in AIEE TRANSACTIONS, 1940, pages 812–16.

C. M. Foust (General Electric Company, Schenectady, N. Y.): That these authors are doing some real pioneering work in connection with instruments for short time measurements and time lag of breakdown is attested by these papers. Results achieved to date are important and it is hoped this work will be continued.

At present the electronic voltmeter arrangement appears to be most suitable to laboratory work where the surges to be measured can be brought to the apparatus and where some idea of amplitude is necessary to provide for preliminary setting of the electronic voltmeter. Of course in laboratory impulse work such a preliminary setting is generally possible and where shots can be readily repeated the electronic voltmeter certainly gives immediate information on crest values. This is often advantageous. The cathode-ray oscillograph while requiring film development and drying before analysis, and being, therefore, somewhat slower in this respect, still has the advantages of less preliminary adjustment for amplitude and a complete wave-shape record. As is usually the case, each type of instrument is fitted to particular fields of work.

In connection with the time lag data, figure 3 seems to indicate an overvoltage surge on the front of the applied wave. Is this present in the tests represented by figure 5 and if so do the authors believe it affects the time lag values reported?

M. Newman: We are very glad that C. M. Foust raised the question as to the cause and possible effect of the peak at the front of the timing wave T in figure 3 of our paper on "Measurement of Very Short Time Lags". In answering that question we feel we should present figures 1 and 2 of this discussion which had been left out of the original paper for the sake of brevity.

The curve a of figure 1 shows the theoretical output voltage of the surge generating unit and the curve b is the fully damped wave used in most of the time-lag tests reported. For the very short time lags of less than 10^{-8} second, where it was essential to have a very steep voltage wave, the wave form a was used on the basis that the overvoltages were high and the slight oscillatory overswing would introduce a relatively small error. For the lower overvoltages it was more important to have the surge voltage free from any oscillatory variations even though the damping introduced a slower rate of rise of voltage, and wave b was used.

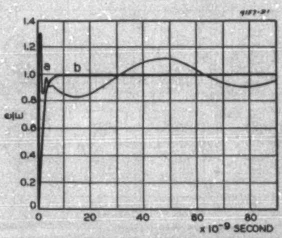

Figure 1. Calculated wave shapes of the voltages applied in the short-time-lag determinations

It was not possible to take fast enough oscillograms to check the wave fronts of figure 2. Therefore when it was desired to obtain oscillograms to illustrate the theory of the time-lag measurements we used an entirely different setup from that used in the short time lags of figure 5 of the paper. The example used was obtained in the case of a rod-gap breakdown on the front of a slowly rising voltage wave; and the timing wave T of figure 3 of the paper which Mr. Foust questioned is not the wave form applied to the gap, but is merely a timing wave initiated in synchronism with the time of application of voltage to the gap under test. The reason for and the method involved in producing that particular timing wave is indicated in figure 2 of this discussion. Diagram A represents the conditions in the case of the very short time lag tests reported in the paper, where the applied voltages were steep enough so that they themselves could be used also as the timing waves. Diagram B shows what would happen if the same method were used for slowly rising voltage wave forms—there would be discrepancies of Δt_1 and Δt_2 in the time of meeting of the two waves, as the applied surge-voltage values are varied. This kind of discrepancy in testing with slowly rising wave fronts was eliminated by devising a sort of automatically variable resistance $R(e)$ as shown in C. The timing voltage thus jumps to a value at which one of the gaps of $R(e)$ breaks down. This adds a resistor in parallel with the wave impedance of the line and tends to lower the voltage. As the applied voltage continues rising the voltage across $R(e)$ reaches the $R(e)$ gap breakdown value again and another resistance is "switched" in parallel. And so resistances are automatically being switched in through the gaps, tending to maintain the voltage at the breakdown level of the $R(e)$ series of gaps. The first gap breakdown occurs on the steepest wave portion so there is some time lag and the voltage overshoots the general gap breakdown level maintained thereafter, and thus causes the initial overswing that was in question.

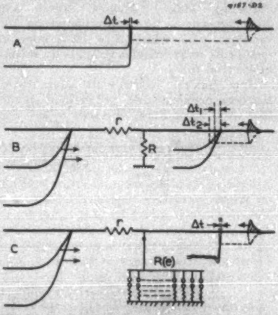

Figure 2. Method of obtaining a suitable timing surge synchronous with the time of initial application of a rising potential to a gap

This last explanation indicates the problem, and its solution, which arose in connection with the measurement of long time lag phenomena. These longer time lag phenomena are within the range of cathode-ray oscillography; still the "dual-wave" method is so much simpler and more direct that it lends itself to rapid quantitative results, and we are using that method over the entire range of time lags both short and long in a general study of gap characteristics. In the latter connection we wish to acknowledge gratefully the co-operation of the Works Progress Administration in assigning us S. H. Teresi who is assisting in making time-lag tests with this apparatus.

Importance of Gas in Electrodes for the Glow-to-Arc Transition

Discussion and authors' closure of paper 39-152 by F. A. Maxfield, H. R. Hegbar, and J. R. Eaton, presented at the AIEE Great Lakes District meeting, Minneapolis, Minn., September 27–29, 1939, and published in AIEE TRANSACTIONS, 1940, pages 816–21.

J. Slepian (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The phenomenon studied by Doctor Maxfield and his associates is the most important one in the physics of mercury-arc rectifiers and electric gas-discharge tubes. In spite of its great importance, until now, it has not been at all understood, and no theory which I am able to accept has yet been presented. This is true in spite of the fact that the practical design of mercury-arc rectifiers and ignitrons is determined principally by the use of empirically found devices for reducing arcback frequency, and the development of very high-voltage mercury-vapor tubes is still not successful, because of too frequent arcbacks.

As I understand the theory put forth in this paper, the authors postulate (last paragraph in paper) that "in a mercury-vapor tube the electrodes are probably continuously absorbing and re-emitting a certain amount of gas. . . . this gas is not emitted uniformly over the electrode surface but instead is emitted in very small bursts." This statement I presume is to hold for an electrode in complete thermodynamic equilibrium with the surrounding vapor. Otherwise it would be necessary for the theory to explain how applying voltage to an electrode, otherwise in equilibrium, brings about the emission of gas in bursts. The data of figure 4, for example, cover the mere application of voltage to an electrode which has been allowed to rest in contact with vapor overnight, and under conditions where the glow current is far too small to have any disturbing effect.

But the second law of thermodynamics requires that the gas emitted from the electrode shall be in the same state as that absorbed. If the gas is emitted in small bursts, it must also be received and absorbed in exactly similar small bursts. Otherwise, it is easy to devise "perpetuum mobile" machines which will turn out work at the expense of heat at a single temperature.

Now it is well known that the vapor reaching the electrode to be absorbed does not arrive in an absolutely uniform and steady stream, but that in sufficiently small volumes there are fluctuations of considerable magnitude from the average density. To this extent the "gas burst" theory of the authors is justified. However, the mathematical formula is well known for calculating the frequency of occurrence of these fluctuations. When such magnitudes for volume and density, as the authors state are necessary for the occurrence of an arc cathode, are used in this formula, frequencies are obtained which are very, very much smaller than those obtained by the authors.

The discontinuous emission of nitrogen from mercury which the authors describe, is not a case of an electrode in thermodynamic equilibrium with the adjacent gas. The compounds of mercury with nitrogen are unstable and generally denoted as explosive.

Apart from the theory, the data presented by the authors is very valuable. One must be careful, however, in drawing too positive and definite conclusions. The data of figure 4 for example would seem to contradict the conclusion drawn from figure 5, that at low densities of glow current, the arcback frequency diminishes as the vapor pressure is increased. In the experiment of figure 5 the glow current was kept constant probably by varying the exciting arc current at the mercury pool, and this may have had something to do with the shape of the curve obtained.

A number of years ago, one of my assistants, J. E. White, also carried out experiments similar to those of the authors, and concluded that the arcback frequency had a pronounced maximum at a temperature somewhat under 500 degrees centigrade. However, there were such inconsistencies in his results upon repetitions that I doubted whether the effect really existed. One point that seemed to me important in his case was that the surface temperature of the sleeve near the electrode tested was affected by the anode temperature, and thus the vapor density might be altered near the anode, particularly if there were any small drops of mercury on the sleeve.

F. A. Maxfield: Doctor Slepian has raised the question that the theory proposed to explain the results obtained in these experiments is contrary to the second law of thermodynamics. We thought that we had made it quite clear, both in this paper and in the one that preceded it (see reference 1 at end of paper), that the gas is not necessarily held on or in the electrode in a state of thermodynamic equilibrium with the surrounding vapor, since a high degree of degassing is only obtained after prolonged positive-ion bombardment.

Although we did not pretend to say how the gas is actually held on the electrode surface we did propose several explanations, any one of which could explain the results obtained and any one of which was compatible with the gas-burst theory. One of these was that the gas is held on the surface in the form of unstable complex compounds which break down under the action of positive-ion bombardment. Simply as an illustration of the possibility of these complex compounds accounting for the gas bursts, we cited an experiment with a U tube containing two mercury electrodes and a small amount of nitrogen. In this tube the formation of these unstable complex compounds between the nitrogen and the mercury of the electrodes was very evident and a large part of the gas could be made to disappear or appear again at will. As another explanation, we also suggested that the gas is simply held on or within the electrode surface in an uncombined state. Here again thermodynamic equilibrium is not necessary because positive-ion bombardment of the electrode is necessary before any large amount of gas can be released and even then it may be emitted at discrete points rather than uniformly over the electrode surface. As evidence that this process of nonuniform emission is possible, I might cite the work of E. Bruche and H. Mahl[1] or of A. J. Ahearn and J. A. Becker[2] on thoriated filaments. Using an electron microscope these workers showed that thorium does not diffuse uniformly to the surface of the filament. On the contrary it appears in the form of small bursts from discrete points randomly distributed on the filament surface and then spreads out over the filament surface.

Regardless of how the gas is held on or in the electrode it is evident that application of heat alone only removes a small part of the gas. This is very strikingly brought out in figure 1 of this discussion (reproduced from the paper by Maxfield and Fredendall.) This figure shows that the transition probability decreased continually as long as the electrode was bombarded by positive ions. However, when positive-ion bombardment was stopped and the electrode simply heated no change in the transition rate occurred. The figure shows that during the first 120 test intervals while positive-ion bombardment was taking place the transition rate continually decreased. Heat treatment alone apparently had no effect for after 477 intervals of heating, reapplication of high voltage showed approximately the same transition frequency as at the end of the last period of positive-ion bombardment. The transition frequency then continued to decrease and the data when plotted as a continuation of the first test (the dotted portion of the curve) all lay on a smooth curve. It is quite evident from this that positive-ion bombardment is necessary for effective gas removal and that the gas bursts are not occurring under conditions of thermodynamic equilibrium.

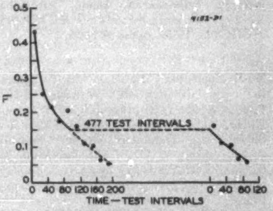

Figure 1. Effect of high-voltage treatment on gas removal from the cathode

Doctor Slepian also mentioned an apparent contradiction between figures 4 and 5 of the paper. It is difficult to compare figures 4 and 5 for the curves of figure 5 were obtained under conditions of constant positive-ion current, while figure 4 refers to a condition when no arcs were present in the tube. It is worth noting again that under this latter condition, tests with a well-treated electrode (curve 2 of figure 4) gave a transition probability of zero at all pressures used.

The effect of electrode temperature on the transition probability was completely reproducible at all times. There was no question of the transition being due to the evaporation of mercury drops from the electrode surface, for the same results were obtained when the temperature was reduced as when the temperature was raised. Although it is possible to explain this effect on the basis of the size and frequency of gas bursts it may simply be due to an exceptionally large gas emission at temperatures near 400 degrees centigrade. As a matter of fact, E. F. Lowry[3] in studying the rate of gas removal from oxide-coated filaments found a variation in emission of just this sort.

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2. A. J. Ahearn and J. A. Becker, *Phys. Rev.*, volume 54, 1938, page 448.

3. E. F. Lowry, *Rev. Sci. Inst.*, volume 4, 1933, page 606.

Dynamic Characteristics of a Single-Phase Induction Motor

Discussion of paper 39-160 by E. B. Kurtz, presented at the AIEE Great Lakes District meeting, Minneapolis, Minn., September 27–29, 1939, and published in AIEE TRANSACTIONS, 1940, pages 801–06.

W. R. Appleman (Marathon Electric Manufacturing Corporation, Wausau, Wis.): Papers of the type presented by Professor Kurtz, "The Dynamic Characteristics of a Single-Phase Induction Motor", indicate the increasing trend of the university to co-operate with industry by analyzing actual problems and giving the results to industry to use.

A number of the smaller manufacturers are not set up to run all of the tests which Professor Kurtz ran, nor do they feel that they can spend the time in such technical research. The university, therefore, fits very nicely into this scheme by performing this research and publishing its results.

There has been quite a bit of discussion for some years as to what constituted starting current. The motor manufacturers have always desired to measure locked amperes as it is a positive value readily measured by any of the standard types of ammeters. For a time there was quite a bit of discussion about measuring the swing of a well-damped ammeter to indicate the actual starting current of a motor. This, of course, varied very much with the type of ammeter used.

Figure 14 of the paper by Professor Kurtz indicates that the maximum true current drawn by the motor during the starting period occurs under locked condition. This chart shows a steady decrease in current with increase in speed.

If a series of such tests were run on all types of single-phase motors, then it could be shown definitely when the maximum starting current occurs, and whether this is under the locked condition. This would be a point of interest to both the manufacturers of motors and to the power companies. It would at least settle the question of what should be termed the starting current.

Analysis of Factors Which Influence the Application, Operation, and Design of Shunt-Capacitor Equipments Switched in Large Banks

Discussion and author's closure of paper 39-158 by J. W. Butler, presented at the AIEE Great Lakes District meeting, Minneapolis, Minn., September 27–29, 1939, and published in AIEE TRANSACTIONS, 1940, pages 795–800.

J. E. Hobson and W. H. Cuttino (nonmember; both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Butler's paper is of considerable interest at the present time since there is a definite trend toward the installation of large capacitor banks often arranged for separate switching of sections automatically controlled by voltage, current, or power-factor variations. Although the number of large banks installed with automatic switching equipment within the last ten years has been very small compared to the total installations of capacitors, yet the activity in this field is expected to increase considerably due to the increasing number of capacitors being applied on distribution circuits. As a typical example, quite recently a 1,350-kva bank was installed on a distribution bus, 4 kv, to be automatically switched in and out of the circuit in sections of 450 kva, the switching being initiated by voltage changes on the bus. After a careful study of the switching currents which might be expected in this installation, a small amount of impedance was added to each bank to limit the transient equalizing currents.

The existence of high transient currents between an energized bank and a bank switched in parallel with it was brought to our attention several years ago when, in a particular installation, the circuit breakers were throwing oil and giving other evidence of distress when performing such duty. In this case the difficulty was eliminated by introducing into the circuit between parallel banks the small impedance afforded by a short section of iron pipe. That such transient equalizing currents may reach high magnitude has been demonstrated a number of times by laboratory tests. In one case of two 75-kva banks very closely associated a transient equalizing current of 20,000 amperes, at a frequency of 50,000 cycles per second, was measured. The inclusion of a very small amount of impedance between banks reduced this current materially.

When switching large capacitor banks in parallel, with low circuit impedance between banks, consideration must be given to the selection of suitable switching equipment which can satisfactorily handle the transient currents without excessive maintenance, or the transient currents must be reduced and modified by additional impedances, or a combination of the two methods of approach must be made. Our experience indicates that it is economical in many applications to limit the current by external impedance and use a lower rated switch than would otherwise be satisfactory. It is necessary to have information regarding the characteristics of the device to be used for switching, particularly its behavior when closing high-current, high-frequency circuits. This information can only be obtained from actual test data for the particular device. It is unfortunate that such data are not provided nor discussed in this paper, that actual test data on capacitor transients are not given, and that the conclusions are therefore rather inconclusive.

In connection with the mathematical analysis in sections 2*a* and 2*b* of appendix *A*, we should like to point out that it is very difficult to obtain from the laboratory or by calculation accurate data on the circuit constants, and therefore any calculations which can be made give only very approximate results. Also, in highly oscillatory circuits such as those discussed, the validity of such assumptions as neglecting resistance in the calculations for the effect of added inductance and neglecting inductance in the calculations for the effect of added resistance, is open to question. In comparing the relative results of added inductance and resistance the effects upon the duration of the transient and upon the recovery-voltage conditions in the circuit as well as on the reduction of current magnitude should be recognized and evaluated.

We believe the matter of circuit closing under such conditions is more complicated than has been indicated, and that Mr. Butler's analysis does not recognize all of the phenomena which may be present during the switching of parallel capacitor banks. From experience gained with a number of installations, we agree that circuit breakers applied to satisfy other requirements will usually successfully handle the currents when switching capacitor banks in parallel. There are exceptional cases, however, and the consideration of transient currents cannot be entirely ignored. Attention must be given to automatic equipments in which the impedances between adjacent banks are quite low and the switches are expected to operate frequently.

In addition to the general considerations mentioned in the paper, the phenomena of ferroresonance between shunt capacitor banks and transformers has been encountered in a few rare cases during line switching operations, the ferroresonant condition usually appears in the zero-sequence equivalent network. Very high currents and voltages at the transformer and capacitor banks may result, but the condition may be avoided by changing the sequence of switching, or by the addition of resistance to the circuit. The phenomena of ferroresonance is quite well explained in part 3, chapter 5, of "Les Reseaux de Transmission d'Energie" by Jean Fallou, a book published by Gauthier-Villars, Paris.

C. Concordia (General Electric Company, Schenectady, N. Y.): In the appendices of this paper it will be noted that the operational expressions arising from those circuits having no losses are evaluated at once without the necessity of using the conventional form of the operational expansion theorem. This is not because the intermediate steps have merely been left out of the paper, but because of the application of a modified form of the expansion theorem which makes them unnecessary. This modification or short cut is believed to be of sufficient general interest to warrant presentation in this discussion. In addition, certain general concepts are given which may be helpful in making the application of the expansion theorem a purely mechanical and more or less foolproof process. The discussion to follow has importance only as a labor-saving device for the use of engineers; it has no mathematical significance. Moreover, it does not apply directly when two or more p roots are equal.

1. A Modified Form of the Operational Expansion Theorem

The conventional form of Heaviside's expansion theorem specifies the dependent variable as the sum of a series of exponential functions of the independent variable. In many cases, however, the most convenient and logical expansion is in a series of sine or cosine functions. For example, the motions of a mechanical system without damping and the currents in an electric network without resistance are commonly of the latter, purely oscillatory form. Such cases may be recognized by inspection of the operational solution of the differential equations of motion since if the operational solution is

$$\left.\begin{aligned} y&=\frac{N(p)}{D(p)} \\ p&=\frac{d}{dx} \end{aligned}\right\} \qquad (1)$$

Then $D(p)$ contains only even powers of p and the roots of

$$D(p)=0$$

are pure imaginary in p or real and negative in p^2.

To find y *directly* as a sinusoidal function of x we may use the following expansion formula

$$y=\frac{N(o)}{D(o)}+\sum\left[\frac{N_e(p)\cos p_nx+\dfrac{1}{p_n}[pN_0(p)]\sin p_nx}{p^2\dfrac{d}{d(p^2)}[D(p)]}\right]_{p^2=-p_n^2}$$

$$(\text{or } p=+jp_n) \qquad (2)$$

where p_n are positive real numbers,

$$p^2=-p_n^2,\ p=\pm jp_n \qquad (3)$$

are the roots of $D(p)=0$, the summation is over the roots in p^2 (or over *only* the positive roots in p of $D(p)=0$, and $N_e(p)$ and $N_0(p)$ are respectively the even and odd components of $N(p)$, that is if

$$N(p)=a_0+a_1p+a_2p^2+\ldots \qquad (4)$$

then

$$N_e(p)=a_0+a_2p^2+a_4p^4+\ldots \qquad (5)$$

$$N_0(p)=a_1p+a_3p^3+a_5p^5+\ldots \qquad (6)$$

$$[pN_0(p)]=a_1p^2+a_3p^4+a_5p^6+\ldots \qquad (7)$$

This expansion formula (equation 2) may also be expressed in the form

$$y=\frac{N(o)}{D(o)}+2\sum\left[\frac{N_e(p)\cos p_nx+\dfrac{1}{p_n}[pN_0(p)]\sin p_nx}{p\dfrac{d}{dp}[D(p)]}\right]_{p^2=-p_n^2}$$

$$(\text{or } p=+jp_n) \qquad (8)$$

If the even and odd components of $N(p)$ cannot easily be separated by inspection, the formulas

$$N_e(p)=\frac{1}{2}[N(p)+N(-p)] \qquad (9)$$

$$N_0(p)=\frac{1}{2}[N(p)-N(-p)] \qquad (10)$$

may be used.

Note that in equations 2 and 8 the grouping of terms is such that only real numbers need be used at any stage of the numerical substitution.

Equations 2 and 8 are derived from the exponential form of the expansion theorem simply by noting that the roots of the denominator occur in conjugate imaginary pairs and combining the corresponding pairs of coefficients of e^{pt}. These pairs of coefficients are themselves conjugate complex quantities; the real part arising from the even component of the numerator and forming the cosine terms of (2), and the imaginary part arising from the odd component of the numerator and forming the sine terms of (2).

It may be remarked that it is never necessary to differentiate the denominator D, since it is always required to know its p^2 roots; to know, e.g., that

$$D(p)=A(p^2+p_1^2)(p^2+p_2^2)(\ldots) \qquad (11)$$

whence

$$\left[\frac{d}{d(p^2)}D(p)\right]_{p^2=-p_n^2}=\frac{D(p)}{(p^2+p_n^2)} \qquad (12)$$

This simply means that the factor $(p^2+p_n^2)$ is left out of $D(p)$.

2. "Direct" Evaluation of Operational Expressions

The operational expression (1) can usually be evaluated most directly and simply by noting in it those factors which are evidently exponential or sinusoidal terms. For example, if $D(p)$ is given by (1), y is given by

$$y=\frac{N(o)}{D(o)}+\sum\left\{\left[\frac{N_e(p)/p^2}{D(p)/(p^2+p_n^2)}\right]\cos p_nx+\left[\frac{N_0(p)/(pp_n)}{D(p)/(p^2+p_n^2)}\right]\sin p_nx\right\}_{p^2=-p_n^2} \qquad (13)$$

The cosine terms are thus obtained by factoring out all the quantities which are recognized as cosines

$$\frac{p^2}{p^2+p_1^2},\ \frac{p^2}{p^2+p_2^2},\ \text{etc.}$$

in turn from y, and the sine terms are similarly obtained by factoring out all the quantities

$$\frac{p_1p}{p^2+p_1^2},\ \frac{p_2p}{p^2+p_2^2},\ \text{etc.}$$

in turn.

The concept may of course be applied also to exponential solutions by factoring out in turn the quantities

$$\frac{p}{p+\alpha},\ \frac{p}{p+\beta},\ \text{etc.}$$

to obtain

$$\left[\frac{N(p)/p}{D(p)/(p+\alpha)}\right]_{p=-\alpha}e^{-\alpha t},\ \text{etc.}$$

In general

$$y=\frac{N(o)}{D(o)}+\sum\left[\frac{N(p)/p}{D(p)/(p+\alpha)}\right]_{p=-\alpha}e^{-\alpha t} \qquad (14)$$

J. W. Butler: Messrs. Hobson and Cuttino cite a few cases where transient equalizing currents have required special attention. The case cited of a breaker throwing oil may have been due to the fundamental characteristics of the breaker however, rather than the duty to which it was subjected. At any rate the ease with which it was cured, namely, the introduction of a short section of iron pipe, bears out the authors' contention covering the relative ease with which these conditions can be cured if the phenomenon is recognized and understood. I would, however, contribute the success of the expediency to the reactance rather than the resistance of the iron pipe, since a very little inductance introduces a very large reactance to these high-frequency equalizing currents. For instance, 0.3×10^{-6} henry at 50,000 cycles would limit the current at 2,300 volts to 20,000 amperes. Since a circuit only 12 inches long of 1/0 copper wire with 15-inch equivalent spacing has this much inductance, I doubt the accuracy of the above figures given for a certain installation of two 72-kva banks. It is pleasing and encouraging, however, that Messrs. Hobson and Cuttino "agree that circuit breakers applied to satisfy other requirements will usually successfully handle the current when switching capacitor banks in parallel," which is in substance the authors' number 1 conclusion. It is hoped that the analysis presented will facilitate the application and understanding of the few special cases that may arise.

Mr. Concordia has called attention to a very useful and ingenious labor-saving device for solving operational expressions, defining circuits having no losses. It is felt that engineers in general should become more familiar with the operational calculus since it presents such a powerful and straightforward method of attack in many types of problems.

The Application of Class B Insulation to Auxiliary-Type D-C Motors in Severe Duty Service

Discussion and author's closure of paper 39-169 by F. A. Compton, Jr., presented at the AIEE Middle Eastern District meeting, Scranton, Pa., October 11–13, 1939, and published in AIEE TRANSACTIONS, 1940, pages 828–34.

P. L. Alger (General Electric Company, Schenectady, N. Y.) and **R. E. Hellmund** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): An important feature of this paper is the information given on the relative values of winding temperature-rise by the resistance and by the thermometer methods. Figure 5 indicates that, due to the time lag of the thermometer temperature, the ratio of the armature rise by thermometer to rise by resistance is only 0.60 for a half-hour test and 0.67 for one hour, as compared with 0.87 under a continuous load. These ratios are for a totally enclosed motor, and even lower ratios apply to open or forced-ventilated d-c machines.

Thus the practice of using a half-hour 75-degree-centigrade rise by thermometer specification to secure a reduced size of machine is only possible because of the great time lag of the thermometer rise, and by the same token the temperature actually obtained is an extremely uncertain quantity that varies widely with the precise location of the thermometer. The authors' suggestion that the resistance method be used instead is logical, but its acceptance requires agreement on very much higher hot-spot temperatures than 75 degrees centigrade for short-time or intermittent rated machines.

Before effecting the proposed change to the resistance method of temperature measurement, it is desirable to determine what the differential between the two methods of measurement is for typical machines of different types. The recent papers by E. R. Summers and C. P. Potter (published in the 1939 AIEE TRANSACTIONS, September section) indicate that in the case of open-type general-purpose induction motors the ratio of rise by thermometer to rise by resistance lies between 0.80 and 1.00 for the great majority of cases, averaging about 0.90. The winding temperature by resistance, however, averages only 0.95 to 1.0 times the maximum lamination temperature by thermometer, reflecting a relatively low copper loss as compared to iron loss on conservatively rated machines.

Comprehensive data on this subject are given in two articles by Jacottet[1] and Jacottet and Hillebrand,[2] who analyzed the temperature-rise values by the two methods on a large number of German machines, both alternating-current and direct-current. For d-c armatures, the average differential was 11 degrees centigrade at full load (roughly, 55-degree-centigrade rise by resistance), giving a ratio of thermometer to resistance temperature values of 0.80. Similarly, the ratio for d-c fields averaged about 0.90; for induction-machine stator windings 0.86; for turbine-generator stators 0.75; and for all machines tested 0.85. In the second article, armature-core temperatures by thermometer are compared with winding temperatures by resistance, the latter being found to be from 2 to 15 degrees centigrade, or an average of 8 degrees centigrade higher. This makes the ratio of winding temperature by resistance to maximum core temperature by thermometer roughly 1.15, as compared with the 0.95 to 1.0 value for the less highly rated American machines cited above.

Other evidence, on large d-c machines especially, indicates that there are many machines now operating which have a temperature rise of the winding of 55 degrees centigrade or more by resistance, even though they meet the normal rule of 40-degree-centigrade rise by thermometer.

From all these data, it appears reasonable that the winding temperature rise by resistance for standardization purposes should be obtained by dividing the present values of rise by thermometer by a factor of about 0.8, giving the following table of equivalent temperatures in degrees centigrade for continuous-rated machines:

40 by thermometer becomes 50 by resistance
50 by thermometer becomes 60 by resistance
65 by thermometer becomes 80 by resistance
75 by thermometer becomes 90 by resistance
80 by thermometer becomes 100 by resistance
100 by thermometer becomes 125 by resistance

These values seem to be suitable for standards applying to various types of machines in general. However, if in the standardization of an individual type of apparatus or winding by some standardizing body a change from the thermometer to the resistance method is decided upon, other values may be selected if a careful study and carefully conducted tests show this to be advisable.

REFERENCES

1. Statistischer Vergleich von gemessenen Wicklungs- und Eisenerwärmungen elektrischer Maschinen, Von P. Jacottet. *Elektrotechnische Zeitschrift*, March 24, 1932, number 12.

2. Vergleich der Wicklungserwärmungen elektrischer Maschinen, ermittelt aus Widerstandszunahme und Thermometer Messung, Von P. Jacottet und F. Hildebrand. *Elektrotechnische Zeitschrift*, July 23, 1931, number 30.

F. A. Compton, Jr.: Messrs. Alger and Hellmund have derived from figure 5 ratios of thermometer rise to rise by resistance for armatures of totally enclosed d-c motors.

It should be noted that figure 5 shows results obtained from test on one particular size of motor and is not necessarily representative of the average of all ten sizes of motors tested. The average ratios of the ten frame sizes tested, and the ratios obtained by Messrs. Alger and Hellmund from figure 5 are as follows:

	Ratios of Thermometer Rise to Rise by Resistance on Armature Winding of Totally Enclosed Motors	
Time of Heat Run (Hours)	From Figure 5	Average of Ten Frame Sizes Tested
0.5	0.60	0.70
1	0.67	0.77
8	0.87	0.89

For field coils the average ratio of thermometer rise to resistance rise for eight-hour heat runs of all totally enclosed motors tested was 0.96. In all motors the field-coil temperatures were less than the armature temperatures, hence the greater ratio for field coils as compared to armature coils.

The results given by Messrs. Alger and Hellmund of tests reported by Messrs. Summers, Potter, Jacottet, and Hildebrand on other types of machines will prove helpful in deciding on resistance rises to replace the present thermometer rises for standardization purposes.

From the available data, Messrs. Alger and Hellmund have proposed that the winding temperature rise by resistance of continuous-rated machines for standardization purposes be obtained by dividing the present values of rise by thermometer by a factor of about 0.8. This factor will probably serve the purpose for standardization of various types of machines in general, but each type should be considered separately.

The following factors should be given consideration in arriving at a rise by resistance to replace the present thermometer rise standard for any given type of machine:

1. Class of insulation
2. Winding construction
3. Type of ventilation, if any
4. Application
5. Time rating
6. Ratio of thermometer rise to rise by resistance

Lightning Investigation on a 220-Kv System—III

Discussion and author's closure of paper 39-165 by Edgar Bell, presented at the AIEE Middle Eastern District meeting, Scranton, Pa., October 11–13, 1939, and published in AIEE TRANSACTIONS, 1940, pages 822–8.

C. M. Foust (General Electric Company, Schenectady, N. Y.): Mr. Bell has called attention to the fact that when incoming currents at the tower tip are added, the sum is usually greater than outgoing currents. These incoming currents flow in along the ground wires and up through the tower structure. The outgoing currents under consideration are usually those flowing up through the lightning rod. An explanation which seems reasonable to account for the differences has to do with multiple strokes. Multiple-discharge lightning strokes have been recorded many times by various investigators using both oscillographic and photographic methods and the general understanding now is that about 25 per cent of all cloud-to-ground strokes are of the multiple-discharge nature. While it has not been demonstrated experimentally, still it is reasonable to assume that each discharge of a multiple-discharge stroke is associated with the discharge of a particular cloud area. Particular cloud areas are located in different positions as regards the transmission line and the point of contact of the lightning stroke. Assuming that a first discharge gives overhead ground wire and ground currents flowing into the tip of the lightning rod from one direction, and that another discharge because of the different location of the cloud area participating gives overhead ground wire and ground currents flowing in from the opposite direction, it is obvious that the two in-flowing discharge currents will not be simultaneous. In such a case the magnetic links on the lightning rod will record only the maximum value of current which will not be the sum of the in-flowing currents.

It must also be recognized that a circumstance similar to this might also be present in the case of single-discharge strokes. The currents in the early portion of a discharge, because of different cloud-field positions, may be more associated with incoming currents from one direction than from the other. In fact that circumstance of exactly balanced distribution of induced earth charges around the point of contact of the stroke should be considered a very exceptional one. Maximum current values, therefore, flowing inward from both directions will not be simultaneous and an exact check between incoming and outgoing currents will not be obtained.

Mr. Bell has also called attention to occasional cases where outgoing currents are larger than incoming currents. The best explanation of this seems to be that magnetic link readings on some of the incoming wires have been reduced because of reversed currents. It is entirely reasonable, therefore, that different results will be obtained when comparing incoming and outgoing currents for particular strokes. However, the broad fact is that these differences are not great and that in many cases incoming and outgoing currents do agree well.

A. F. Bang (Pennsylvania Water and Power Company, Baltimore, Md.): Mr. Bell calls attention to the effectiveness of overhead ground wires and efficient tower-footing grounding as a preventive against lightning flashovers. I wish to underscore this statement by giving a few records taken from the transmission system of the Pennsylvania Water and Power Company. In 1911 this company operated about 80 circuit miles of 70-kv circuits; in 1939, 573 circuit miles, of voltages varying from 66 kv to 220 kv. In 1911, the first lightning season in the history of this company, the number of momentary outages due to lightning was 60 per 100 miles; in the past 1939 lightning season, 1.04 per 100 miles. This substantial improvement is essentially due to the installation of counterpoise on all but 3 per cent of the company's transmission circuits, together with heavy insulation and overhead ground wires. Since 1930 all the transmission lines built by this company have been equipped with carefully designed and tested counterpoises when built; during the same period practically all the older circuits have gradually been improved in the same way.

Edgar Bell: I concur with Mr. Bang in endorsing overhead ground wires and low tower-footing resistance for prevention of lightning faults on effectively insulated transmission lines.

Mr. Foust's discussion of some of the apparent inconsistencies in surge-crest-ammeter analysis is logical and may apply in some cases. Other explanations include fundamental accuracy of individual link measurements, accuracy of interpretation of two-link measurements, and the justification for algebraic summations as made.

An encouraging feature of the surge crest ammeter is that in many cases the various associated records hang together surprisingly well, in spite of the adverse factors encountered.

New Developments in Current-Transformer Design

Discussion and author's closure of paper 39-173 by G. Camilli, presented at the AIEE Middle Eastern District meeting, Scranton, Pa., October 11–13, 1939, and published in AIEE TRANSACTIONS, 1940, pages 835–42.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): Mr. Camilli is to be congratulated on his very important contribution to the art in designing current transformers with the arrangement of the insulation corresponding so nearly to the ideal as utilized in high-voltage cables. Two factors account for the tremendous reduction in size which the author has made in the cable-type design. These factors were not mentioned in the paper, and I believe it is well to point them out in this discussion.

To understand the situation it will be necessary to state the basic laws of the electrical breakdown of insulation, and of the division of voltage stresses when voltage is placed across two different kinds of insulations having different dielectric constants. These may be stated as follows:

1. For nonuniform dielectric fields the required thickness of insulation, including oil, solid, or solid and oil in series, varies approximately as the 1.5 power of the test voltage, whereas in uniform dielectric fields the thickness varies approximately as the first power of the test voltage.

   Stated the other way around, the dielectric strength of insulation varies as the two-thirds and first powers of its thickness, depending on whether the dielectric field is nonuniform or uniform, respectively.

2. The division of the dielectric stress of oil operating in series with solids is inversely proportional to their dielectric constants.

The author, like the cable engineer, has taken advantage of both of these laws in reducing the amount of insulation required in the cable type of design. That is, he has produced practically a uniform dielectric field in the insulation and has eliminated free oil ducts. To do this necessitated the use of an oil-impregnated insulation having a low power factor.

The significance of eliminating free oil ducts in series with solid insulation can be gained from the following assumed example:

If 100 kv is placed across one-half-inch solid and one-half-inch oil, the division of voltage will be 67 kv across the oil and 33 kv across the solid, because the ratio of their dielectric constants is approximately 1:2, that is 1 for oil and 2 for solid.

Since the dielectric strength of oil is somewhat less than that of solid insulation, this means that the oil breaks down long before the solid approaches its breakdown value. This means that part of the space—the solid—cannot be effectively utilized.

As mentioned before, since the author has employed electrodes comprising the smooth metal ring surrounding the high-voltage winding and the outside ground shield, giving approximately uniform dielectric fields, he can use thick tapings without being handicapped due to the fact that the dielectric strength varies approximately as the first power of the thickness of the insulation. A few calculations will show that if it requires the same insulation for uniform and nonuniform fields for 100 kv test, for a test of 650 kv the amount of insulation required, when the thickness of insulation varies as the test kilovolts raised to the 1.5 power, is 2.54 times that required when the insulation thickness varies as the test kilovolts raised to the first power. In other words, by eliminating sharp corners and edges, Mr. Camilli has made it possible to reduce his insulation to approximately 0.4 that required for a nonuniform dielectric field.

With reference to the reduction in size of the cascade design as compared to the old standard—the author mentions the fundamental reason why this is possible, namely, that when the dielectric strength of insulation varies as the thickness raised to some power less than unity, it is possible to reduce the amount of insulation by dividing it into steps.

The author states that for oil strike the thickness of insulation varies as the 1.6 power of the test voltage. The data which I have obtained, and also considerable published data, indicate that the power should be 1.5 instead of 1.6. If we assume that the power is 1.5, a few calculations again will show that even with a nonuniform field, by dividing the unit into six voltage steps it was possible to use approximately 0.4 the amount of insulation required for the same transformer built in only one voltage step. This, of course, is possible only when the winding is shielded to give approximately a uniform impulse-voltage distribution. That is, each unit must not be subjected to more than its share of the voltage.

Someone may ask: if it is possible to make such drastic reductions in the insulation of cable-type current transformers, would it not be possible to do the same thing for other transformers; that is, distribution and power transformers? The principal reason why it is not possible to do so is because other transformers have many times the copper loss to be dissipated from the windings. It is not practical to enclose the windings with thick insulations on account of heating requirements. Provision has to be made for the circulation of oil through the windings. As previously shown the introduction of free oil in series with solid requires for the same test kilovolts from two to three times the spacing required for solid alone.

There are, then, two reasons why it is not possible to design other transformers with insulation so closely approaching the ideal cable design. Up to the present time it has not been practical to make the dielectric field uniform, and to eliminate all free oil spaces between the high-voltage and low-voltage windings. Some day we may be able to do it.

A. Boyajian (General Electric Company, Pittsfield, Mass.): As cables are old, one may wonder why it was that the current transformer with cable-type insulation featured in this paper was not thought of many years ago. One reason may be that Mr. Camilli was not born earlier; a second reason, that the Spirakore construction of the magnetic circuit was not invented until very recently. To explain this: a high-accuracy current transformer requires a magnetic circuit with minimum possible exciting current. Referring to figure 4*a* of the paper, it will be seen that the Spirakore construction of the magnetic circuit is essential for this type of current transformer for the higher voltages. If the core in figure 4*a* were ring punchings, the high-voltage winding shown there could not have been wound around it and insulated as shown. If the high-voltage winding was first wound and completely insulated as shown, and then a core were assembled around it out of straight pieces, joints would be introduced into the magnetic circuit, and the accuracy of the current transformer would be correspondingly impaired. Figure 4*a* attempts to illustrate how a jointless magnetic circuit is placed around the fully insulated winding by the Spirakore principle. A single figure cannot adequately explain this principle: complete details of it will be found in reference 3 listed at the end of the paper.

Strange how each new invention leads to many others, and the art grows at an accelerated rate. What is impractical in one art today may become highly practical later by an invention in another art.

John H. Chiles, Jr. (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The author has presented a very interesting paper covering a wide variety of current-transformer designs. This paper indicates a rather exhaustive study ranging from the single-stage conventional design of excessive weight through two types of cascade designs and on to a type of single-stage paper-taped transformer. While these designs show a remarkable reduction in weight and oil volume, this reduction is not so striking when compared to other modern current transformers of conventional core-type design. For comparative purposes, the author's table I is reproduced below with the addition of the figures for a 230-kv conventional core-type design with a steel tank and condenser bushing.

	The Author's Designs		Modern Core-Type Design
	1929	1939	
Weight (pounds)	40,000	4,200	5,600
Total height	23 ft	13 ft 8 in.	18 ft
Gallons of oil	3,200	115	294

Comparable metering and overcurrent accuracy.

Figure 1 of this discussion shows the type of design referred to above. Figure 2 shows the core and coil construction of this unit. Figure 3 shows an interesting application of this core and coil design to a 161-kv porcelain-cased current transformer. This particular design shown in figure 1 is for 161 kv and is somewhat higher and heavier than necessary due to the use of a bushing with a long gas space so that it will be interchangeable with the bushings used on a power transformer. This design lends itself very readily to transportation and installation. It would appear that the units described by the author would be difficult to handle due to their small metal case and the high center of gravity above the case. The design described above has the further advantage of having the live parts well above ground, without the necessity of a supporting structure to accomplish this. Unlike the designs described by the author, the core and coils of the 230-kv design are readily adaptable to metering transformers and to double secondary construction. They are also readily changed in ratio by the purchase of new coils only, while in designs, figures 4 to 7, described by the author practically a complete unit is necessary.

The author's revision of the three-stage porcelain-spool cascade design to a two-stage paper-taped design is interesting. "However, in the wound-type current transformers for high-voltage service we have not found a willingness to accept errors of five per cent at 10 to 12 times normal current with a *Z* burden. There are perhaps many applications where such performance is entirely acceptable. However, these can generally be much more economically filled by the use of a ring-type current transformer." In the case of the 287-kv breaker, the question is raised as to whether two current transformers are used in order to make up to some extent for the poor overcurrent performance. If there is justification for two separate secondary circuits it would seem much simpler and more economical to apply a single current transformer with a double secondary. The steel-cased core-type design shown in figure 1 lends itself very readily to the application of a double secondary, that is, two separate secondary cores and windings with a common primary winding. This application is not readily possible in a cascade type of unit.

Cascade current transformers were built in Europe some years ago, but apparently were given up for a design similar to the paper-taped single-stage unit described by the author. Several years ago porcelain-spool cascade current transformers were introduced in this country. Apparently due to their poor overcurrent performance, these did not prove successful. We believe that cascade-current-transformer designs have little merit except for specific applications where poor overcurrent performance can be tolerated.

Apparently, a great deal of work has been applied to the designs described by the author in order to reduce the height and the amount of oil. It is difficult to see that much of this work is justified, for a conventional unit, as shown in figure 1, might be just as readily applied to the breaker. It is apparent from the author's figure 12 that the breaker is mounted well above ground such that a tank of reasonable height could be used in the structure. If the breaker is such that a reasonably small volume of oil constitutes a hazard, a compound-filled condenser bushing would eliminate any hazard from this source. Certainly years of experience have shown that high-voltage current transformers with which we are familiar are most reliable, so that the small volume of oil in the steel case need be of no particular concern. The oil in the steel tank of the unit in figure 1 would not be subject to release by rupture from external forces as might be the case with porcelain-cased designs.

Two interesting points were brought out by the author: the application of the well-known condenser-bushing principle to the lead of the current transformer, and application of the sealed-case construction. Approximately four years ago the company with which I am associated discarded the open-air breather on the high-voltage current, potential, and metering transformers.

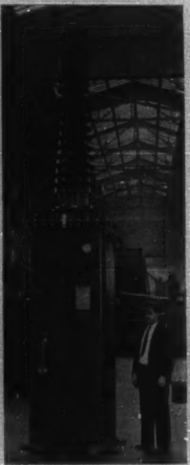

Figure 1(left). 161-kv core-type current transformer

Height $16^1/_2$ feet, weight 5,700 pounds, gallons of oil 370

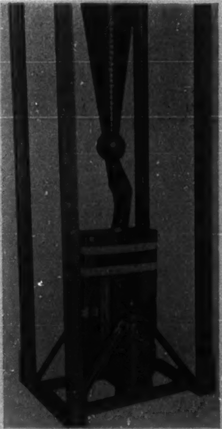

Figure 2. Core and coil construction of the 161-kv current transformer shown in figure 1

The customer was given the alternate of sealed cases, Inertaire equipment, or dehydrating breathers.

The author has stated that his paper-taped designs are worked at somewhat lower gradients than is conventional in cable design. The reason for this is readily apparent since the junction of the lead tube and the primary ring containing the coil makes a difficult taping operation. Apparently, a great deal of this taping is a hand operation requiring the greatest of care and supervision. One difficulty with a heavily taped design is that of dissipating the heat generated in the primary winding. The paper tape is a fairly poor conductor of heat. The heat in the coil leads is rather easily dissipated as its exposed surface is large compared to its watts loss. Doubtless actual coil temperatures have been measured as a resistance measurement would only give the average of the coil and lead temperature. Under short circuit up to the thermal rating of the unit if the winding reaches the usual 240 degrees centigrade temperature gas bubbles will doubtless be generated. In normal designs the clearances are materially greater than those used by the author, and steps are taken to provide cooling ducts so that any gases generated will be readily released and not trapped in the winding.

The application of wound cores to current transformers is not new, as such cores have been in active commercial use for at least eight to nine years. Likewise, the method of compensation described by the author was proposed by an associate of the writer about nine years ago and was placed into commercial use on wound-type cores at that time. This method has been found particularly useful in current transformers with a small number of ampere turns.

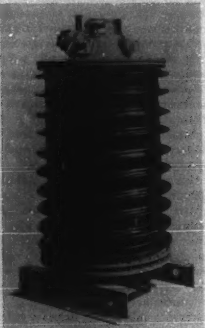

Figure 3. Porcelain-cased 161-kv current transformer applying the core and coil shown in figure 2

Height 6 feet 6 inches, weight 2,500 pounds, gallons of oil 90

Another method of compensation that has been found more effective for transformers with many ampere turns is the method which uses an equalizer winding on the core to prevent leakage flux from entering the core. Such a scheme is shown in figure 4 of this discussion. This scheme is readily adaptable to either a rectangular or ring type of core.[2] The effect of this scheme is somewhat different than that shown in the author's figure 22. This scheme not only compensates the ratio over the range of 5 per cent to 100 per cent current, but, because the leakage flux is excluded from the core, the overcurrent performance will be better than for the same transformer without any compensation.

It is difficult to see the economy of a wound core with a secondary coil wound by hand through the very restricted space between the core and the primary insulation. The problem can be solved easily with form-wound coils and a cruciform core of I-plate construction. The elimination of the leakage flux by equalizer windings more than makes up for the hypothetical value of a wound core without gaps.

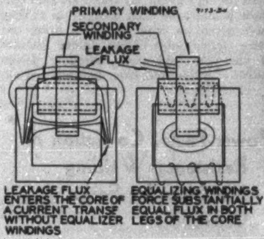
PRIMARY WINDING
9173-3
SECONDARY WINDING
LEAKAGE FLUX
LEAKAGE FLUX ENTERS THE CORE OF A CURRENT TRANSF WITHOUT EQUALIZER WINDINGS
EQUALIZING WINDINGS FORCE SUBSTANTIALLY EQUAL FLUX IN BOTH LEGS OF THE CORE

Figure 4

REFERENCES

1. Gibbs Patent, number 1849485, March 15, 1932.
2. Steinert Patent, number 1932051.

D. C. Prince (General Electric Company, Schenectady, N. Y.): The size and cost of a tank-type high-voltage circuit breaker are determined to a considerable extent by the need of bushings. If advantage were to be taken of low oil content or air types of circuit-breaker design, which have no bushings, an acceptable substitute for the bushing current transformer had to be found. On the basis of the larger transformer of figure 1 the Boulder Dam circuit breakers are impossible. On the basis of figure 12, they are still pretty expensive.

The new design of figure 14, is a valuable step in the right direction, while the success of such designs as figures 6 and 7*a* may, when sufficient experience has been obtained with them, bring about a final abandonment of tank-type designs in high-voltage circuit breakers.

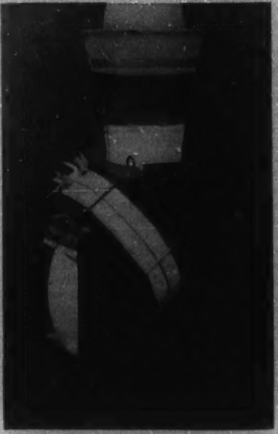

Figure 5. Internal view of the current transformer shown in figure 4b of the paper, showing two independent secondaries

G. Camilli: Mr. Chiles has drawn up a comparison between the latest conventional core-type design made by his company and the cable-type spiral-core design described in the author's paper. I hesitate to say much regarding this comparison in view of the fact that there seems to be some doubt about the correctness of Mr. Chiles' data in that the weight and amount of oil are greater for his 161-kv transformers than for the 230-kv transformers. However, a few comments will be offered assuming that the data are at least approximately correct.

While Mr. Chiles' data show that his present transformer marks a vast improvement over what was available in 1929, they also show that it is still 33 per cent heavier, 32 per cent higher, and requires 156 per cent more oil than the Spirakore design. Incidentally in the latter design we secured the advantages of a ring core with its low secondary impedance and the safety of a grounded shield between the high-voltage and low-voltage windings.

Although the Spirakore transformer is shorter than the conventional transformer if additional clearance to the live high-voltage terminals is required, it can be obtained at low cost by using a concrete pad or steel frame.

As a matter of fact, the compactness of the Spirakore design often permits mounting in the bus structure rather than on the ground and hence out of reach. Undoubtedly the transformer shown in Mr. Chiles' figure 3 would be similarly mounted.

Since these units have the highest standard accuracy classification of $^1/_4X$, $^1/_4Y$, and $^1/_4Z$ they are adequate for any metering purpose. The maximum voltage rating for which it is practicable to build combined metering equipments depends upon the size of the elements. Therefore, since the Spirakore transformer is smaller than the conventional design, the use of it in metering equipments will tend to raise this voltage rating limit.

Mr. Chiles claims that the type of design described in the paper is not adaptable to double-secondary construction. As a matter of fact, the cable-type Spirakore design is adaptable to any number of secondaries and the 230-kv unit described actually had two secondaries. This is shown in figure 5 of this discussion, which was omitted from the original paper for the sake of brevity.

The transformer of figure 4*b* indicates how easily the ratio of the Spirakore design may be changed. The top structure contains a number of external terminals by which six combinations of primary turns are readily secured. This construction obviates the necessity for changing primary coils.

It will be noted that a 5 per cent error at 10 to 12 times normal applies to cascade designs which were built for 287 kv only and that the 230-kv transformers are good up to 30 times normal. The usual ratio of the 287-kv current transformers is 600/5 and therefore 10 times normal is 6,000 amperes or about 3,000,000 kva. It is believed that interest in errors above this current is rather slight since the accuracy in the expected short-circuit current region is adequate for relaying.

The height of the insulating columns of the oil circuit breaker of figure 12 was dictated by the impulse strength requirements. Since it was possible to insert the entire current transformer within a column it seemed unnecessary to add a steel tank below. It also seemed unnecessary to develop bushings for current transformers capable of supporting the weight of a circuit breaker. It may be of interest to know that the porcelain is merely a weather shield and its rupture would not release the current-transformer oil.

Another point raised by Mr. Chiles is with reference to the heating of these transformers. By actual measurements using thermocouples, the temperature rise of the windings of these transformers is well below the AIEE limits.

As to the generation of gas bubbles, there is practically no possibility of this in the Spirakore design. The reason for this is very interesting. The high-voltage winding is enclosed in a metal shell connected to the winding. The coil is cooled by oil circulating within this shell. There is not voltage stress in this oil and, therefore, even if any bubbles were generated in it under short-circuit conditions, no harm would result. The cable-type paper wrapping which insulates the high-voltage form does not transmit any heat and therefore cannot be subjected to any bubble generation under any conceivable condition. It has been our experience that it is rather in the conventional type of design that bubble generation is a problem to the engineer, because in this design the same oil acts both as major insulation and as the heat carrier.

The use of two current transformers, one on each side of the circuit breakers, in the Boulder Dam installation was necessitated by the scheme of relaying and had nothing to do with their overcurrent accuracy which was entirely adequate for the purpose as is indicated by the fact that additional units have since been purchased. The scheme of relaying was described by D. C. Prince in his paper, "Circuit Breakers for the Boulder Dam Line," ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), 1935.

Mr. Chiles mentions the fact that wound cores have been used in current transformers for eight or nine years. The reasons for changing to this construction were so obvious that we had no intention of taking any credit for introducing this feature. However we feel now that we must do this since Mr. Chiles infers that someone else was responsible for it. Furthermore, it was not until the advent of the Spirakore principle that wound cores could be used in anything but bushing-type current transformers. They cannot be applied to the transformers shown in figure 2 of my paper and figures 1 and 2 shown by Mr. Chiles.

It might be inferred from Mr. Chiles' comments that the Spirakore transformer could be improved by the use of equalizing windings. This, however, is not the case as the Spirakore construction is inherently free from the difficulties which necessitate such compensation and which are present in the conventional core-type transformer.

In closing I might say that I am glad that these points have been brought up as they have given an opportunity to bring out more clearly some of the unique features of the new cable-type Spirakore design which were omitted from the paper itself.

Power-Factor Correction of Resistance-Welding Machines by Series Capacitors

Discussion and author's closure of paper 40-11 by L. G. Levoy, Jr., presented at the AIEE winter convention, New York, N. Y., and published in AIEE TRANSACTIONS, 1940, pages 1002–09.

J. W. Butler (General Electric Company, Schenectady, N. Y.): The use of capacitors connected in series with the load, as well as in shunt with the load, is increasing in popularity at a notable rate. This is due in part to both the price reductions effected during the last few years through improved design and production methods, and an increased knowledge of the art of application. At first, shunt capacitors were generally used to improve power factor and series capacitors were used to improve voltage regulation. At present, it is not uncommon to see shunt capacitors switched for voltage regulating purposes; and now Mr. Levoy describes a fundamentally sound method of improving power factor with series capacitors. It is believed that the area between these two uses of the series and shunt capacitor represents a large potential field of application for the capacitor.

Fundamentally, a capacitor at rated load represents leading kilovolt-amperes on the system. Therefore, if it is desired to improve a certain lagging-power-factor load, leading kilovolt-amperes can be added to the circuit by capacitors connected either in series or in shunt. At rated load, a given amount of capacitor kilovolt-amperes will give the same power-factor improvement whether in series or in shunt. Practically, however, for a given load characteristic and voltage source, either method cannot be chosen at will because the effect on the load voltage is widely different for the two types of application. If one were to start from scratch and design a complete circuit from the voltage source to, and including, the load, we could often use either capacitor connection to accomplish a certain objective. Of course, a decision in the matter would be largely based on the economics of the situation being considered.

The design of a circuit and load to include capacitors is illustrated by a recent installation of a series capacitor rated approximately 2,000 kva for power-factor improvement of a graphitizing furnace. This type of furnace load has a negative-resistance characteristic and as the resistance decreases the power factor goes down to approximately 30 per cent or 40 per cent. This condition, of course, throws a very heavy reactive load on the supply source with resultant well-known ill effects. The furnace charge is resistive; the low power factor is a result of the reactive kilovolt-amperes required by the bus and furnace reactance. This reactive requirement, as previously mentioned, could have been supplied by either shunt or series capacitors. The use of the series capacitor proved a "natural" because it supplied reactive automatically with the changing requirements; its ohmic value was made equal to the ohmic value of the bus and furnace reactance. With the series capacitor the current drawn from the supply is substantially unity power factor over the entire range of the furnace loading. Fundamentally, of course, it is recognized that this application parallels that described by Mr. Levoy for welders, in which the supply-transformer voltage, welder voltage, and series-capacitor rating are all co-ordinated to make a workable unit.

One may conclude that this principle could not be applied to distribution circuits. True enough, the compensation cannot go as far but the only difference is in the degree of compensation. In general, series capacitors are not applied in voltage ratings in excess of 15 per cent of the circuit voltage. The application of a 15 per cent series capacitor to a circuit delivering a 70 per cent power-factor load would improve the power factor to 78 per cent on the source side of the capacitor. It is believed that such incremental improvements in power factor obtained with series capacitors should be credited to their installation and thus help justify their purchase. It is believed that with a given amount of money available for improving or increasing the rating of a given distribution area, a portion of it could be well spent for series capacitors when it is remembered that they give automatic instantaneous voltage regulation as well as that they supply reactive kilovolt-amperes equal to their rating, at full load, at which time it is most needed.

Another possible use of series capacitors is for obtaining the required load division between parallel circuits, such as a cable and an overhead line. This application is not new, but it should be noted that series capacitors can be installed in each line to give the required load division and at the same time provide both automatic voltage regulation for flicker or steady loads and power-factor improvement.

As the possibilities of the series capacitor become more generally recognized, it is believed that the application of series-capacitor equipments will show a marked increase over the notable record of recent years, both in number and type of use.

E. F. Dissmeyer (Commonwealth and Southern Corporation, Jackson, Mich.):

Engineers are encountering an increasing number of cases where it is necessary to shield power-supply systems from the voltage fluctuations caused by single-phase resistance-type welders. In many cases it has been necessary to provide special supply circuits or other methods of isolation for such loads. In general, these remedial measures have been expensive, and their cost cannot be justified by the power-supply companies due to the fact that resistance-type welders consume a comparatively small amount of energy. Occasionally it has been necessary for customers to install special motor generator sets to supply their welder loads.

It is, of course, recognized that a series capacitor installation, equipped with the electronic-type control described by Mr. Levoy, may not provide a solution for all welder supply problems, especially in those cases where a large number of small welders are used in a customer's plant. It appears, however, that the equipment described can be satisfactorily used on certain large welders.

There is no doubt that the proper application of series capacitors will provide an economical and satisfactory solution for many resistance-welding power-supply problems.

J. D. Stacy (General Electric Company, Pittsfield, Mass.): Mr. Levoy's worthy paper on the application of series capacitors to the resistance-welding-machine load problem emphasizes the necessity of frequent review of utilization problems from both technical and economical viewpoints. Although this problem might have been solved technically by such an application of capacitors several years ago, it has been the rapid development in capacitor design with attendant increase in reliability and lower costs that now makes this solution of the problem economically sound in many cases.

To apply this system successfully requires co-ordination of the supply voltage, the capacitor kilovolt-amperes, and voltage rating and the welder power factor, kilovolt-amperes, and voltage rating. In order to give a clearer conception of this statement a tabulation has been prepared in reference to several assumed welder conditions (see figure 1 and table I of this discussion).

Control devices using ignitron-type tubes are usually not supplied for voltages under 230 volts because of the starting characteristics of these tubes. In cases where the new supply must be below 230 volts, an intermediate transformer may be used at *B-B* having a kilovolt-ampere capacity equal to that of the welder. It is also shown that higher-voltage welder transformers require higher-voltage ratings of capacitors and since capacitor costs per kilovolt-ampere are lower at these higher voltages, a considerable gain can be obtained by using higher-voltage welder transformers which will permit the voltage transformation from the power lines to be located at *A-A* where the kilovolt-ampere capacity may be based on the lower, corrected power factor load. In many cases the welder transformer and capacitor voltage can be selected such that no transformation is needed, avoiding the necessity of providing an additional transformer.

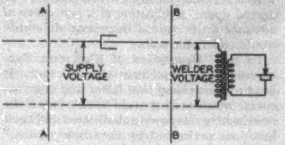

Figure 1

Table I. Capacitor Kilovolt-Amperes and Voltage and Supply Voltage Required to Correct a 100-Kva (Maximum) Welder to Unity Power Factor

Welder Voltage	Welder Power Factor	Capacitor Kva	Capacitor Voltage	Supply Voltage	Kva Demand
230	0.20	97.9	225	46*	20
230	0.40	91.6	210	92*	40
230	0.60	80.0	184	138*	60
460	0.20	97.9	450	92*	20
460	0.40	91.6	420	184*	40
460	0.60	80.0	368	276	60
575	0.20	97.9	563	115*	20
575	0.40	91.6	527	230	40
575	0.60	80.0	460	345	60
920	0.20	97.9	900	184*	20
920	0.40	91.6	840	368	40
920	0.60	80.0	736	552	60
1,150	0.20	97.9	1,126	230	20
1,150	0.40	91.6	1,054	460	40
1,150	0.60	80.0	920	690	60

* Indicates where synchronous control and ignitron contactors must be used with a transformation at *B-B* in order to secure proper operating voltage for these devices.

H. C. Louis (Consolidated Gas Electric Light and Power Company of Baltimore, Md.): Laboratory tests in Baltimore on series capacitors showed transient oscillations similar to those described in the paper by Mr. Levoy. These were especially serious in the case of transformer-coupled capacitors. A question of practical interest is whether the special method described for eliminating this transient can be applied to a transformer-coupled capacitor.

L. G. Levoy, Jr.: Mr. Butler's remarks are of interest in showing that the series capacitor is finding widespread use on distribution circuits. The application of series capacitors for power-factor correction of resistance-welding machines differs from the usual distribution-circuit application in two important respects; namely (1) resistance-welding-machine power-factor correction is usually calculated on the basis of bringing the load to unity power factor; (2) where the proper type of synchronous control is used, precharge is established on the capacitor to permit uniform transient-free operation, which is most important where precise welding conditions are required.

During the oral discussion following presentation of the paper, it appeared that some power companies were more concerned with small welding machines, since they made special provision for the large ones. Other companies were troubled only with large installations. The system of power factor correction described in this paper is applicable to all sizes of machines, and it is primarily a question of economics whether power-factor correction should be applied, or the system reinforced to serve the uncorrected load. The most economical solution for a given size machine will differ on different systems and also at different locations on the same system, consequently each case must be considered on its own merits.

It should be remembered when considering power-factor correction of small machines, that the cost of the corrective capacitor required diminishes almost in direct proportion to the kilovolt-ampere demand, and consequently such correction may be economically sound on relatively small welders in some cases. In the case of a large number of small resistance-welding machines in a single plant, as mentioned by Mr. Dissmeyer, the total capacitor kilovolt-amperes required to correct each machine individually would total the same as that required to correct one large machine having a demand equal to the combined demands of the small machines. For this reason the feasibility of power-factor correction should not be discarded just because a large number of small machines are involved. The cost of converting the controls when necessary, and co-ordinating the feeder and welding transformer voltages must be considered in the case of existing equipment, although this factor does not enter when laying out the design for new equipment.

Referring to Mr. Louis' question regarding the elimination of transients on a transformer-coupled capacitor, this can be accomplished with synchronous control, by establishing a precharge on the capacitor equal to the voltage which it would have in steady-state operation at the phase angle of closure of the switch. In order to establish such a precharge, it is necessary to disconnect the capacitor from the transformer to permit the establishment of this precharge. In the case of the resistance-welding machine with series capacitors, the synchronously controlled power tubes serve the function of controlling the flow of current as required, and also serve to disconnect the capacitor during the "off" interval so that the proper precharge voltage can be established or maintained.

The tabulation presented by Mr. Stacy is of interest in showing the kilovolt-ampere and voltage relations under a variety of assumed conditions. In interpreting this table it should be remembered that the power factor of a resistance-welding machine varies over a wide range from short circuit of the electrodes to higher values on various kinds of work. The "working" power factor is, of course, the one which is important in determining conditions during welding. The short-circuit values are used for design criteria only. The "kilovolt-ampere" is demand kilovolt-amperes under working conditions and not the welding machine name-plate value. Where electronic-type controls are used, suitable allowance must be made for tube arc drop. This factor will change both the supply voltage and kilovolt-ampere demand values given in the table, and must be considered where electronic controls are used. Present-day mercury-vapor tubes which are commonly used have an arc drop of approximately 15 volts. This arc drop is essentially independent of current in the operating range.

The requirement of voltages of 220 or more does not apply to magnetic-contactor controls, nor is it peculiar to synchronous controls, except in so far as these controls may use some types of electronic tubes which do not function well at extremely low values of voltage.

The many odd values of supply voltage listed suggest the use of the autotransformer circuit shown in the paper as figure 4. Ordinarily, however, the supply voltage is taken at some standard value and the welding transformer and capacitor operate at the odd values. A similar table can readily be made up on this basis.

Synchronizing Transients and Synchronizers for Large Machines

Discussion and authors' closure of paper 40-26 by R. D. Evans, F. H. Gulliksen, and C. B. Myhre, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 965–73.

H. T. Seeley (General Electric Company, Philadelphia, Pa.): The authors are to be complimented on their thorough analysis of the dependence of transient current magnitudes upon the differences in voltage magnitude, phase, and frequency, during synchronizing onto the sending bus of a transmission system, but the calculations and the design seem to be based on a maximum voltage difference of 8 per cent between machine and bus. This will almost certainly be exceeded during system emergency conditions, especially in the case of the first machine to be connected to the bus, and it is under just such conditions that prompt synchronizing is most needed. Allowance for operation at system voltages down to 80 per cent of normal has long been considered standard for control equipment in unattended installations; the synchronizer described by the writer in 1934 (see reference 4 of the paper) was designed to operate under such conditions, and will operate with voltage differences exceeding 30 per cent of rated voltage.

Referring again to this same synchronizer (reference 4), the test limit we have always used in regular production happens to be identical with the five-degree limit of error mentioned by the authors under "Requirements for Good Synchronizing," and this limit has been applied out to 45 degrees desired advance angle for breakers having closing times down to 0.375 second and out to a frequency difference of one-third cycle per second for faster breakers. Each relay is tested with a phase shifter driven by an adjustable-speed motor, the advance angle is determined for various frequency differences within the operating range, and adjusted so as not to exceed the plus or minus five-degree limit of error, and marked at the proper setting of the advance adjusting resistor. In the case of the relay described in the present paper, it would be desirable to know what limits of frequency difference must be accepted in order to maintain the error within this angular limit which seems to be agreed upon; publication of a test curve of advance angle versus frequency difference would cover this point.

In the "Description of Electronic Synchronizer" the statement is made that "It has become evident that faster and more accurate synchronizing could be obtained if the interlocking relays were eliminated and their functions performed by electronic means." This statement is not evident to the writer, because in the interlocking-relay type, the interlocking functions need affect only the accuracy of cutoff, which is relatively unimportant; and the total time of the internal relays involved in giving the closing indication is likely to be only about 0.1 second, which is automatically compensated for in the adjustment of the advance angle. It appears significant that the elimination of interlocking relays was simultaneous with the addition of two features that have been available in the market since 1934, namely, the compensation for voltage difference magnitude and the avoidance of errors due to variations in d-c control voltage; and it seems likely that the improved performance reported was obtained by the addition of these two features which experience has shown to be desirable, and not by the elimination of the interlocking relays.

The use of thyratrons and of smaller components in general has permitted a commendable reduction in size, but users will need to know what limits of ambient temperature are imposed by the thyratrons. They will also be interested in the volt-ampere burden, and in the possibility of applying this relay to line synchronizing where voltage differences in excess of five per cent are common and where the limitations of the thyratron filaments may determine the minimum percentage of rated voltage at which safe operation may be obtained.

E. E. George (Ebasco Services, Inc., New York, N. Y.): The authors have used a very effective method of determining synchronizing requirements and, having established these, have carried out a rather clean-cut electronic design job. The a-c calculating board is a very effective tool for such analyses. The electronic equipment described is unusually simple and should perform satisfactorily provided the thyratron tubes have reasonably permanent characteristics. In the past, thyratron devices have given considerable trouble due to the use of the ordinary types of thyratrons which have extremely variable characteristics with ambient temperature. Most of the types also require preheating of the filament circuit, and are inoperative for several seconds after power failure. The authors might reassure us on these two points; as to what has been done, or can be done to provide better operation under such conditions.

The conclusions of the authors to the effect that phase difference is more important than frequency difference applies particularly to generator synchronizing and not to the synchronizing of large interconnected systems. Perhaps this conclusion should be so limited. The *rate of change of frequency* is the important variable to be measured in paralleling large systems.

The authors' general approach to the problem of designing the best possible synchronizer is apparently that of providing a device which will synchronize with the least possible disturbance to the machine. This viewpoint is somewhat like that of the new operator who is proud of his skill in paralleling without any disturbance. Nevertheless, the operator who gets on the honor roll in the dispatching office is the operator who can get a machine on the line in the minimum time, with the frequency perhaps several cycles below normal and varying widely, while other plants or companies are also paralleling, and perhaps other system disturbances are occurring. In such emergencies, the shock to the machines or the system is seldom considered, as long as none of the windings come out and no other switches open from the synchronizing surge.

It would be a real contribution to the operation of large interconnected systems if such a synchronizing device as the authors have described were provided with two controls. One would be for normal operation in bringing machines or tie lines in service (as described by the authors), and would perform the operation as fast as consistent with minimum disturbance (perhaps less than full load current). The other control to be used in emergency would *parallel in the shortest possible time*, independent of the resulting disturbance so long as the equipment and systems would stay together after being paralleled. The inrush current might be more nearly comparable to full-voltage short circuit, rather than to normal load, but the permissible emergency limits for any particular machine or interconnection could likely be determined at the time of installation, and the equipment adjusted accordingly.

L. N. McClellan (Bureau of Reclamation, Denver, Colo.): The field acceptance tests which were made at Boulder power plant on the electronically-interlocked synchronizer described in the paper by Messrs. Evans, Gulliksen, and Myhre, have demonstrated that this equipment is reliable and quite accurate for synchronizing the large generators with circuit breaker closing speeds of approximately five-tenths second. This confirms the conclusion of the authors that the electronic synchronizer is superior to the interlocked-relay type of synchronizer, which was initially used at the Boulder power plant. The original interlocked-relay type synchronizer did not prove satisfactory in use on the 82,500-kva generators because of the extreme accuracy required and the need for greater speed in completing the synchronizing operations. With this type of synchronizer the voltage vector must make at least one full revolution before synchronizing can take place.

As pointed out in the paper, the 82,500-kva generators have low reactance and extremely large flywheel effect to afford stability for the Boulder Dam-Los Angeles transmission lines. These characteristics of the generators increase the importance of minimum phase and frequency differences when synchronizing. To minimize the shock of synchronizing, the phase angle must be kept as close to zero as possible and should not exceed three degrees. Frequency differences between the incoming and running machines are not permitted to exceed the figure of 40 seconds per revolution of the synchroscope given in the paper.

The typical system used by the authors in their calculations compares with the Boulder generator connections and transmission system, and although the authors' conclusions indicate that a frequency difference at the moment of synchronizing may cause less system disturbance than a phase angle difference, the large flywheel effect of the Boulder generators has resulted in a very exacting requirement in this respect. In this connection it might be well to mention that satisfactory full automatic synchronizing depends to a large extent on the ability of the speed matcher and governor to control the frequency of the incoming machine. Excellent results have been obtained at the Boulder power plant with a speed matcher consisting of two synchronous motors differentially geared to the speed-adjusting mechanism of the governor on a 40,000-kva generating unit. It should also be pointed out that the electronically-interlocked type of synchronizer at the Boulder power plant is used in connection with two recently installed 82,500-kva generating units, each of which is connected directly to an individual bank of transformers, and all synchronizing is accomplished with the 230-kv high-voltage circuit breakers. Extensive field tests were made with the synchronizer before actual synchronizing was attempted, the results of which were very satisfactory. The angle of advance adjustment was such that the phase angle at which the main breaker contacts actually closed was as near zero as could be determined with a breaker closing time of approximately five-tenths second.

The authors have mentioned the occurrence of torques due to synchronizing out of phase, but they have not discussed the possible severity of such torques. At the Boulder power plant the maximum possible torque due to synchronizing out of phase was computed to be 15 times normal, whereas the maximum possible torque due to a line-to-line short circuit at the terminals of a generator was computed to be 11 times normal. It appears that little information has been published about the method of computing torques due to synchronizing out of phase, and it is suggested that a paper on the subject of torques due to synchronizing out of phase would be timely.

S. B. Crary (General Electric Company, Schenectady, N. Y.): The authors have presented an interesting analysis of the effect of differences in speed, voltage, and phase angle on the synchronizing transients. From the results presented for the synchronizing range studied, it would appear that calculations could have been made on the usual two-machine basis rather than on a three-machine swing curve basis. That is, it would have been only necessary to have calculated the incoming machine against the system as one equivalent machine. This would greatly simplify the calculations and would yield results which are as accurate as those presented for a general discussion of this problem, particularly when it is considered that the load characteristics were only approximately represented. The conclusion that phase angle is the important quantity to keep to a minimum to avoid shock to a machine and in order to minimize the system disturbance when synchronizing is undoubtedly correct for the range of voltage, speed, and phase-angle differences investigated.

R. D. Evans and **C. B. Myhre:** With reference to Mr. Seeley's discussion, we wish to emphasize that the electronically interlocked synchronizer described in this paper was developed primarily for synchronizing large generators with extreme accuracy under conditions which are obtained in large base-load generating plants. Extreme accuracy in synchronizing was the criterion for this design and every effort was made to obtain that end. The elimination of moving parts, such as interlocking relays, naturally contributed to faster, more accurate and consistent performance under all conditions.

Mr. George and Mr. McClellan have pointed out that the rate of change in frequency difference, when synchronizing, is an important variable. For best performance, fast-closing breakers should be used and determination to close the breaker should be made as close to the synchronism point as possible. Direct electronic interlocking, without relays, permits this "determination" to take place much closer to the synchronism point than would be possible when the operating time of interlocking relays had to be compensated for. Furthermore, when the "determination" to close the breaker is made very close to the synchronism point, there will be very little chance for the frequency difference to deviate while the breaker is closing.

Line synchronizing, where voltage control is not available, does not ordinarily require the high order of accuracy that is desirable for heavy generator practice, because of the cushioning reactance that exists between the machines to be synchronized.

An automatic synchronizer is used only a few minutes every day. For this reason the tubes should have a very long life since the normal continuous life of a thyratron is in excess of 2,000 hours. The filaments are kept continuously energized at a reduced voltage in order to permit synchronizing without a long time delay for bringing the filaments up to temperature. This preheating does not appreciably reduce the tube life because the plate circuits are not energized except when the synchronizing operations take place.

Preheating of filaments keeps the tube temperatures within such limits that an ambient temperature range from freezing to 150 degrees Fahrenheit will not change the accuracy of performance. Safe operation, with a liberal margin of safety, is obtained over the entire AIEE standard operating voltage range.

The burden of the synchronizer is five volt-amperes on the bus, and eight volt-amperes on the machine potential devices. The circuits are so arranged that the breaker will not close in the event of tube failure when synchronizing. The synchronizer is adjustable for frequency differences from 0 to 0.2 cycle per second, and breaker speeds ranging from 0.3 to 0.8 second, and is compensated to operate accurately for all values of voltage differences that are permissible to the incoming generator when synchronized against other generators in the power house. The voltage difference limitations referred to in the paper are determined by the requirements for "quiet" synchronizing of large generators and not by the limitations of the synchronizer.

Mr. Crary has suggested that the two-machine solution is suitable for the study of synchronizing transients. Such a method gives fair results but requires the assumption that the sending-end and receiving-end machines maintain a fixed angular relationship. Actually these machines will oscillate with respect to each other as shown in figure 5 or 6. For this reason it seemed preferable to use the three-machine basis for preparation of the principal curves of the paper. Incidentally, the two-machine basis was used for the "approximate method" in connection with curve *D* of figure 7. Furthermore, certain synchronizing phenomena, as discussed in the paper, require curves such as shown in figures 5 and 6 for their proper interpretation.

Generalized Bridge Network for Dielectric Measurements

Discussion and authors' closure of paper 40-21 by J. C. Balsbaugh, A. H. Howell, and J. V. Dotson, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 950–6.

W. J. Seeley (Duke University, Durham, N. C.): There is available at the present time a mass of excellent literature dealing with bridge networks, the bulk of which, for the most part, being concerned with some particular network or application. It is a rather herculean task, however, to attempt to go through this literature for the purpose of arriving at a general over-all picture of what might be called a parent network, that is, a network of such generality that any particular bridge network might be derived from it as a particular case. A number of papers are available which tend toward such generality. These papers, in general, follow one of two lines of approach, namely, either by means of a generalized network, or else by reducing all possible networks to a classification. Examples of the first method are papers by Kouwenhoven and coauthors and Balsbaugh and coauthors, and of the second method those of Ferguson and Seletzky. What is needed now is a merging of these two methods, together with the one mentioned in the next paragraph, so as to make available a single parent network from which all possible networks may be derived. The development of the present paper falls in the first of the two categories mentioned above and is a distinct forward step toward the derivation of an ideal all-inclusive bridge network. Furthermore, it considerably clarifies the tie-up between network algebra and the required operating technique; that is, it shows distinctly the necessary manipulative procedure required to verify the various balance relations resulting from theoretical deductions, and it is simply done.

There seem to be two schools of thought influencing the design and construction of high-precision bridges. The one school, which is well represented by the present paper, relies upon numerous balancings of shield impedances and coupling impedances before the actual bridge balance may be obtained. It is a method requiring a skilled manipulation and complicated switching: the bridge must be calibrated, so to speak, each time a measurement is made. The other school relies upon complicated shielding structures, often in connection with a Wagner ground, to take care of the stray coupling unbalances, thus leaving a simple manipulation to the operator for obtaining the final balance. In this case the bridge is more or less precalibrated during the process of construction and requires less skill in use.

I should like to ask the authors if they had considered more complicated shielding and less manipulation, as manufacturers of bridges so strongly seem to advocate, before finally settling upon the structure described. In other words, what were the determining factors in the selection of this particular bridge structure rather than one containing more elaborate shielding?

Eric A. Walker (Tufts College, Medford, Mass.): There are two questions I would like to ask Professor Balsbaugh regarding his equations for the five-terminal bridge network.

First, I am interested to know what effect the capacitance of the generator to ground has on his calculations, and where this is taken into account. Is this capacitance fixed, or does it change in magnitude when the generator is shifted from the connections of figure 1*a* to figure 1*e*?

The other question involves the distributed capacitance to its surrounding shield along any balancing impedance which has physical length. This distributed capacitance has impressed upon it different voltages per unit length, depending on the total voltage on the impedance, and depending also on the position of the tap or slide connection. Undoubtedly this stray distributed capacitance can be represented by a fixed shunting capacitance, even though such a fixed capacitance is fictitious. Moreover, the actual value is of no consequence, because the object of the test is not to determine the stray shunt capacitance, but because there are distributed charging currents to the shield, the actual impedance of the balancing resistor will vary with the frequency of the impressed voltage, and therefore an error can be introduced into the results.

This defect is not peculiar to Mr. Balsbaugh's bridge, but is present in all a-c bridges. I would like to know if Mr. Balsbaugh considers such effect important, and if so, what steps have been taken to overcome it.

G. B. Hoadley (Massachusetts Institute of Technology, Cambridge): The authors have pointed out several interesting and important facts about the theory of the coupling between a bridge circuit and its Wagner ground. Perhaps the most important of these is contained in equation 11. The outstanding feature of this equation is that the unbalances in the shield and coupling circuits are combined as a product in their effect relative to the effect of unbalance in the main bridge circuit. As the authors point out, the product relationship means that if the shield and coupling circuits are balanced to the same degree, they need be balanced with a precision which is no greater than the square root of the precision desired in the bridge balance except in unusual circumstances. Thus, if a precision of 10^{-6} is required in the bridge balance, the shield and coupling circuits need be balanced to a precision of only 10^{-3}. Consequently less-expensive components can be used in the shield and coupling circuits, because they do not need to have exceedingly fine steps in their adjustments, and they need not be as stable as the components in the bridge proper.

Another advantage which arises from the appearance of this product is that the electrical properties of dielectrics which are changing can be followed with the bridge without adjusting either the shielding circuit or the coupling circuit, even though, as occurs when guard rings are used, the changing dielectric is causing almost as much change in the coupling circuit as it is causing in the bridge circuit proper. Thus a per-unit change up to 10^{-3} could be followed without causing an error of more than 10^{-6} in the measured value, if both the shield and the coupling circuits were originally balanced to a precision of 10^{-3}. It must be remembered that equation 11 is good only for conditions when the bridge, shield, and coupling circuits are all near balance. It is incorrect to conclude, for example, that when the dependency factor is small, the coupling balance may be dispensed with. The bridge for which the authors give data in row *j* of table I would be very difficult to balance if Z_4 were made infinite, even though the dependency factor calculated from equation 17 is approximately 10^{-3}.

The method by which the authors express unbalances is very satisfactory. Their equations 18 to 23 show that when unbalance is expressed on a per-unit basis, it matters not, except for sign, which one of the four bridge arms shall be considered to be out of adjustment. Usually, bridge detectors are insensitive to phase angle and thus to sign, so $\Delta Z_1/Z_1$ is a general term indicating the unbalance of the bridge proper. It may also mean $\Delta Z_2/Z_2$, $\Delta Z_7/Z_7$, or $\Delta Z_8/Z_8$. Likewise $\Delta Z_6/Z_6$ is a general term for the shield unbalance which could equally well mean $\Delta Z_3/Z_3$, and $\Delta Z_4/Z_4$ is a general term for the coupling circuit unbalance which could equally well mean $\Delta Z_5/Z_5$.

The generality of these unbalance expressions which occur in equation 11 leads to the conclusion that the expression for *D* should be symmetrical with respect to the bridge structure. However, the expressions for *D* in equations 12 and 17 appear to be very lacking in symmetry. Nevertheless, by judicious manipulation, equation 17 may be shown to be symmetrical. Upon factoring, this equation becomes

$$\frac{1}{D}=Z_5\left(\frac{1}{Z_2}+\frac{1}{Z_8}\right)+Z_3\left(\frac{1}{Z_1}+\frac{1}{Z_2}\right)+1$$

The first of these terms, $Z_5(1/Z_2+1/Z_8)$ is the ratio of Z_5 to the parallel impedance of Z_2 and Z_8. The balance equations 3 and 4 can be combined to get

$$\frac{Z_5}{Z_4}=\frac{Z_8}{Z_7}=\frac{Z_2}{Z_1}$$

whence it is evident that

$$Z_5\left(\frac{1}{Z_2}+\frac{1}{Z_8}\right)=Z_4\left(\frac{1}{Z_1}+\frac{1}{Z_7}\right)$$

The right-hand side of this is the ratio of Z_4 to the parallel impedance of Z_1 and Z_7. This term is then really a symmetrical one which could be thought of as a factor which indicates the relation of the coupling-circuit impedances to those of the bridge proper.

By following a similar line of reasoning, the term $Z_3(1/Z_1+1/Z_2)=Z_6(1/Z_7+1/Z_8)$ becomes the factor which indicates the relation of the shield circuit impedances to those of the bridge proper. Consequently the expression which the authors give for *D* is a symmetrical one containing the relation of the coupling circuit to the bridge proper, and the relation of the shield circuit to the bridge proper.

J. C. Balsbaugh: Professor Seeley has given a very interesting analysis of bridge circuits and methods. The determining factors in designing the bridge discussed were: (*a*) sufficient flexibility to permit operation over extended ranges of voltage, frequency, and power factor and capacitance of test sample, (*b*) use of the conjugate Schering or detector-symmetrical bridge in view of its high inherent sensitivity for precise measurements on small samples, of the order of five micromicrofarads, (*c*) use of the Schering or source-symmetrical bridge for measurements at relatively high applied voltages, and (*d*) a generalized bridge network for experimental study of bridge theory.

With regard to Doctor Walker's questions, the effect of the generator or source may be represented by a three-terminal network which in the bridge and shield balances is connected between points *A*, *B*, and *G*. Thus the capacitances to ground are included in Z_3 and Z_6. The other impedance, being across points *A* and *B* or the source, has no effect on the balances. Thus the effect of source capacitances to ground are controlled by the shield or guard balance, and are not changed in switching from the bridge to the guard balance. In the coupling balance, in which *A* and *B* are tied together and the source connected between this point and ground, the foregoing impedances are shunted out or are directly across the source, thus producing no effect. Doctor Walker has raised an interesting question with respect to the effects of distributed capacitance. It is believed that the effect is sufficiently small over the present frequency range of the bridge so as not to be a determining factor in the precision of the measurements. However, there is no doubt that this effect may become significant in certain cases.

Doctor Hoadley has given an interesting discussion with respect to the coupling balance. His analysis with respect to the dependency factor *D* is an important contribution.

Transient Analysis of Symmetrical Networks by the Method of Symmetrical Components

Discussion and author's closure of paper 40-2 by Louis A. Pipes, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 457–9.

W. V. Lyon (Massachusetts Institute of Technology, Cambridge): A method, such as Doctor Pipes proposes in his paper, which avoids solving a high-order determinantal equation, is welcome. I should like to point out two things. First, the condition of symmetry which Doctor Pipes imposes is more severe than necessary. Second, the resolution of the determinantal equation into factors can be accomplished by means of symmetrical components *alone* without recourse to matrix theory. For example, consider six equal circuits, each having the same operational impedance. Let each of these circuits be coupled to one of six equal coils equally spaced around a uniform magnetic ring, so that the mutual inductances between adjacent coils are equal, between alternate coils are equal, and between diametrical coils are equal. Let Z_{ab} be the operational coupling impedance between adjacent coils, Z_{ac} between alternate coils, and Z_{ad} between diametrical coils; and let Z be the resulting operational self-impedance of each of the six loops. Since the self-impedances of the six loops are equal, and because of the symmetry imposed on the coupling, the potentials of each sequence are proportional to the currents of that sequence *alone*. For simplicity, assume that an electromotive force, E, is impressed on one loop a and all other loops, b, c, d, e, f are short-circuited, there being no initial currents or charges. The symmetrical components of the applied electromotive force are each $E/6$. Consequently the zero-sequence equation is

$$E_0 = E/6 = Zi_0 + Z_{ab}i_0 + Z_{ac}i_0 + Z_{ad}i_0 + Z_{ac}i_0 + Z_{ab}i_0$$

The last two terms represent the mutual effect of the zero-sequence currents in coils e and f. Hence,

$$E_0 = E/6 = (Z + 2Z_{ab} + 2Z_{ac} + Z_{ad})i_0 \qquad (1)$$

Similarly,

$$E_1 = E/6 = Zi_1 + Z_{ab}(\alpha i_1) + Z_{ac}(\alpha^2 i_1) + Z_{ad}(\alpha^3 i_1) + Z_{ac}(\alpha^4 i_1) + Z_{ab}(\alpha^5 i_1)$$

where $\alpha = \epsilon^{-j2\pi/6}$ is the characteristic operator for a six-phase circuit. Since $\alpha + \alpha^5 = 1$, and $\alpha^2 + \alpha^4 = -1$, and $\alpha^3 = -1$,

$$E_1 = E/6 = (Z + Z_{ab} - Z_{ac} - Z_{ad})i_1 \qquad (2)$$

Similarly it is readily shown that

$$E_2 = E/6 = (Z - Z_{ab} - Z_{ac} + Z_{ad})i_2 \qquad (3)$$

$$E_3 = E/6 = (Z - 2Z_{ab} + 2Z_{ac} - Z_{ad})i_3 \qquad (4)$$

$$E_4 = E/6 = (Z - Z_{ab} - Z_{ac} + Z_{ad})i_4 \qquad (5)$$

$$E_5 = E/6 = (Z + Z_{ab} - Z_{ac} - Z_{ad})i_5 \qquad (6)$$

Equations 2 and 6 show that $i_1 = i_5$. Also equations 3 and 5 show that $i_2 = i_4$.

The impedance operators in equations 1, 2, 3, and 4 are the factors of the determinantal equation. Having determined the component currents, i_0, i_1, i_2, i_3 from equations 1, 2, 3, and 4, the currents in the loops a, b, c, etc., are

$$i_a = i_0 + 2i_1 + 2i_2 + i_3 \qquad (7)$$
$$i_b = i_0 + \alpha i_1 + \alpha^2 i_2 + \alpha^3 i_3 + \alpha^4 i_4 + \alpha^5 i_5$$

Since $i_1 = i_5$ and $\alpha + \alpha^5 = 1$, and since $i_2 = i_4$ and $\alpha^2 + \alpha^4 = -1$,

$$i_b = i_0 + i_1 - i_2 - i_3 \qquad (8)$$

$$i_c = i_0 - i_1 - i_2 + i_3 \qquad (9)$$

$$i_d = i_0 - 2i_1 + 2i_2 - i_3 \qquad (10)$$

$$i_e = i_c \qquad (11)$$

$$i_f = i_b \qquad (12)$$

Note that the same principles which apply in formulating equations 1 to 6, inclusive, also apply in writing equations 7 to 12, inclusive, for the loop currents.

R. D. Evans and A. C. Monteith (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Doctor Pipes has presented from the mathematical point of view, an interesting exposition of the transient analysis of networks by the method of symmetrical components. While it appears true, as Doctor Pipes has suggested, that the mathematical phase of the subject has been neglected, the subject has, nevertheless, received considerable attention from the engineering point of view. A pertinent reference in this connection is the paper "The Determination of Circuit Recovery Voltages by Analytical and A-C Calculating Board Methods", presented by Evans and Monteith at the 1937 AIEE summer convention in Milwaukee (AIEE TRANSACTIONS, volume 56, 1937, pages 695–705). Examination of that paper will show that the method of symmetrical components was used in the analytical determination of recovery-voltage transients. Of perhaps greater interest is the use of the a-c calculating board to provide miniature circuits that are equivalent from the transient point of view to the actual power system under consideration. In this method the various power-system elements were represented by corresponding types of equivalent circuits having the same sequence impedance characteristics. Furthermore, sequence networks were used in various connections to give transient results equivalent to those of a three-phase system subjected to various disturbances. The paper also gives the results of field tests substantiating the method. This method has subsequently been used by several investigators for various studies of circuit recovery voltages and other switching transients. For such general investigations of power-system transients this method of using equivalent circuits on the a-c network calculator is considered, from an engineering point of view, as the most suitable method available.

Murray F. Gardner (Massachusetts Institute of Technology, Cambridge): The method which Doctor Pipes presents requires: (1) the Laplace transformation of the integro-differential equations of the electrical network, introducing therewith general initial conditions, (2) resolution of the transformed loop voltages and currents into symmetrical components, (3) formation of the sequence impedances from the loop and mutual impedance functions, (4) solution separately for the (symmetrical) component currents, and (5) the synthesis of these components to gain the actual loop currents. He uses matrix algebra, particularly for steps (2), (3), and (5). This is an unjustifiably long process for handling the class of problems to which he restricts his discussion. The major claim for the method seems to be that by use of it, solution of the characteristic equation of the network for its roots in the usual way can be avoided.

One acquainted with the theory of symmetrical components and also the Laplace transformation (or even operational calculus of the Heaviside type) can write the separate sequence equations directly; and, once each is solved, can combine the components to secure the actual currents—and do this without the use of matrix algebra. Even one not acquainted with symmetrical-component theory can solve problems of the special class treated here in direct manner by: (1) modifying the determinant of the network so that the invariant factors of the characteristic equation become readily apparent, (2) making a partial fraction expansion of the transforms for the actual currents, based on these factors, and (3) carrying out the inverse Laplace transformation of these expansions to gain at once the actual loop currents

As an example of the latter method, consider a four-loop, electrically symmetrical network in which all the loop self-impedance functions are alike and the mutual-impedance functions are one of two forms. This is a slightly more general problem than any discussed by Doctor Pipes, as he restricted his treatment to networks in which *all* the mutual impedances are alike. Here, with s a complex variable,

$$\begin{aligned} z_{11} = z_{22} = z_{33} = z_{44} = z_a &= Ls + \frac{1}{Cs} \\ z_{12} = z_{23} = z_{34} = z_{41} = z_b &= M_1 s \qquad (1) \\ z_{13} = z_{24} = z_c &= M_2 s. \end{aligned}$$

Then, if $I(s)$ is the Laplace transform of a loop current $i(t)$, and $T(s)$ is a loop excitation function composed of the sum for that loop of: (1) the Laplace transform of the net driving voltage, (2) the initial net flux linkage, and (3) the initial net capacitor voltage divided by s, the system of transform equations can be written

$$\sum_{k=1}^{4} z_{jk}(s)I_k(s) = T_j(s),\ j = 1, 2, 3, \text{ and } 4 \qquad (2)$$

Solution for $I_k(s)$ gives

$$I_k(s) = \sum_{j=1}^{4} Y_{kj}(s)T_j(s),\ k = 1, 2, 3, \text{ and } 4 \qquad (3)$$

in which

$$Y_{kj}(s) = \frac{N_{jk}(s)}{\Delta(s)} \qquad (4)$$

$\Delta(s)$ being the determinant of the system, and $N_{jk}(s)$ the cofactor of z_{jk} in $\Delta(s)$.

Here

$$\Delta(s)=\begin{vmatrix} z_a & z_b & z_c & z_b \\ z_b & z_a & z_b & z_c \\ z_c & z_b & z_a & z_b \\ z_b & z_c & z_b & z_a \end{vmatrix}=$$

$$(z_a-z_c)^2(z_a+z_c+2z_b)(z_a+z_c-2z_b) \quad (5)$$

These factors can be readily found by: (1) transforming the determinant into a factor times a determinant having the first element of the first row unity, and all other elements of this row zero; (2) reduction of this determinant to a third-order determinant; and (3) transformation of this third-order determinant into a factor times a determinant having the first element of its first column unity, and all other elements of this column zero. A similar but briefer procedure reduces $N_{jk}(s)$ to a product of factors.

To complete the example, the current in loop 1 will be found, considering T_2, T_3, and T_4 are zero, and $T_1=\gamma/s$, γ being the net initial condenser voltage in loop 1. Here

$$I_1(s)=\frac{N_{11}(s)}{\Delta(s)}=$$

$$\frac{(z_a-z_c)(z_a^2+z_az_c-2z_b^2)}{(z_a-z_c)^2(z_a+z_c+2z_b)(z_a+z_c-2z_b)}\frac{\gamma}{s}=$$

$$\left[\frac{K_1}{z_a+(z_c+2z_b)}+\frac{K_2}{z_a-z_c}+\frac{K_3}{z_a+(z_c-2z_b)}\right]\frac{\gamma}{s} \quad (6)$$

in which

$K_1=K_3=0.25$ and $K_2=0.5$

It will be noted that the partial fraction expansion has been made considering z_a as the variable.

In the terminology of symmetrical components, $(z_a+z_c+2z_b)$ will be recognized as the zero-sequence impedance, (z_a-z_c) as the first- and third-sequence impedances, and $(z_a+z_c-2z_b)$ as the second-sequence impedance. Taken with the factor γ/s, the first, second, and third terms are the Laplace transforms, respectively, of the zero-sequence component, the combined first- and third-sequence components, and the second-sequence component of the current of loop 1.

Replacing z_a, z_b, and z_c by their respective functions of s,

$$I_1(s)=\gamma\left(\frac{A_1}{s^2+\beta_1^2}+\frac{A_2}{s^2+\beta_2^2}+\frac{A_3}{s^2+\beta_3^2}\right) \quad (7)$$

in which

$A_1=0.25(L+2M_1+M_2)^{-1}$
$A_2=0.5(L-M_2)^{-1}$
$A_3=0.25(L-2M_1+M_2)^{-1}$
$\beta_1^2=[C(L+2M_1+M_2)]^{-1}$
$\beta_2^2=[C(L-M_2)]^{-1}$
$\beta_3^2=[C(L-2M_1+M_2)]^{-1}$

Finally, the inverse Laplace transformation of $I_1(s)$ gives

$$i_1(t)=\left(\frac{A_1}{\beta_1}\sin\beta_1t+\frac{A_2}{\beta_2}\sin\beta_2t+\frac{A_3}{\beta_3}\sin\beta_3t\right) \qquad 0\leqq t \quad (8)$$

Since this four-loop network was electrically symmetrical (its constants being the same, viewed from any loop), its determinant could be readily factored, and it was possible to avoid the solution of the characteristic equation for its roots in the usual manner. It was not necessary to employ matrices, nor the aid of symmetrical-component theory. Furthermore the procedure was the usual one throughout, being shortened simply by the ease with which the determinant could be factored.

In this discussion the direct Laplace transformation used is the ordinary form, that is,

$$[f(t)]=\int_{\sigma_a<\sigma}^{\infty} f(t)e^{-st}dt=F(s) \quad (9)$$

in which $s=\sigma+j\omega$, $j=\sqrt{-1}$; t, σ, and ω are real variables; and σ_a is the abscissa of absolute convergence for the integral with the function $f(t)$ present. The transformation differs from the "s-multiplied" form used by Doctor Pipes by the factor s.

P. C. Cromwell (New York University, New York): I am glad to see this paper by Doctor Pipes. Although the method of symmetrical components is well known in the study of electrical transients, to my knowledge no systematic proof of its value in the solution of symmetrical systems has been published.

As I read Doctor Pipes' paper I began to wonder why a simple multiplying process made it unnecessary to solve for the roots of the determinantal equation. Let us take a perfectly general symmetrical two-mesh network quite similar to the first example in the paper. Using the same symbols we have

$$\begin{aligned} Z_0i_1+Zi_2&=E \\ Zi_1+Z_0i_2&=0 \end{aligned} \quad (1)$$

Solving for i_1

$$i_1=\frac{Z_0E}{Z_0^2-Z^2}=\frac{Z_0E}{(Z_0+Z)(Z_0-Z)} \quad (2)$$

The denominator of the first term in equation 2 factors readily and the second term may be evaluated directly in any numerical case by using a table of operational transforms.

For a three-mesh system

$$\left.\begin{aligned} Z_0i_1+Zi_2+Zi_3&=E \\ Zi_1+Z_0i_2+Zi_3&=0 \\ Zi_1+Zi_2+Z_0i_3&=0 \end{aligned}\right\} \quad (3)$$

and the expression for i_1 is

$$i_1=\frac{(Z_0^2-Z^2)E}{Z_0^3+2Z^3-3Z_0Z^2} \quad (4)$$

which is easily shown to be

$$i_1=\frac{(Z_0-Z)(Z_0+Z)E}{(Z_0-Z)^2(Z_0+2Z)} \quad (5)$$

After dividing numerator and denominator by (Z_0-Z) equation 5 could be evaluated in the usual manner.

I have also been able to "solve" a four-mesh equation in a similar fashion although in this case it was necessary to solve a quadratic equation.

In every one of the simple cases considered it was unnecessary to solve for the roots of the determinantal equation in the usual sense of the word. I should like to suggest that perhaps the proof given in the paper could be extended to show that the use of symmetrical components can be replaced by an entirely new and much simpler set of relations which would automatically (*a*) solve for the roots, and (*b*) expand the operational form of the equation in partial fractions.

K. Y. Tang (The Ohio State University, Columbus): The currents for the circuit shown in figure 1 of this discussion are determined in two ways:

1. Pipes' method
2. Another method

The amount of work involved in Pipes' method of solution of this problem is very much less than that of the second method.

METHOD 1

Following the procedure given by Pipes and with the boundary conditions when $t=0$,

$i_1^0=0,\ i_2^0=0$
$q_1^0=0,\ q_2^0=0$

equations 36 and 37 reduce to

$$I_{1S}(p)=\frac{E/2}{Lp+\frac{1}{Cp}+Mp}=$$

$$50\,\frac{10^{-4}p}{1.1\times10^{-4}\left\{p^2+\frac{10^4}{1.1}\right\}}$$

$$I_{2S}(p)=\frac{E/2}{Lp+\frac{1}{Cp}-Mp}=$$

$$50\,\frac{10^{-4}p}{0.9\times10^{-4}\left\{p^2+\frac{10^4}{0.9}\right\}}$$

Substituting $\frac{\sin\omega t}{\omega}$ for $\frac{p}{p^2+\omega^2}$ then

$$i_{1S}=\frac{0.5}{\sqrt{1.1}}\sin\frac{100}{\sqrt{1.1}}t$$

$$i_{2S}=\frac{0.5}{\sqrt{0.9}}\sin\frac{100}{\sqrt{0.9}}t$$

From equation 43,

$$i_1=i_{1S}+i_{2S}=\frac{0.5}{\sqrt{1.1}}\sin\frac{100}{\sqrt{1.1}}t+\frac{0.5}{\sqrt{0.9}}\sin\frac{100}{\sqrt{0.9}}t$$

$$i_2=i_{1S}-i_{2S}=\frac{0.5}{\sqrt{1.1}}\sin\frac{100}{\sqrt{1.1}}t-\frac{0.5}{\sqrt{0.9}}\sin\frac{100}{\sqrt{0.9}}t$$

METHOD 2

Start with Kirchhoff's electromotive-force equations

$$L\frac{di_1}{dt}+\frac{1}{C}\int i_1dt+M\frac{di_2}{dt}=E$$

$$L\frac{di_2}{dt}+\frac{1}{C}\int i_2dt+M\frac{di_1}{dt}=0$$

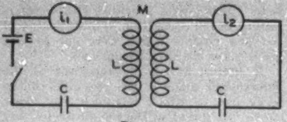

Figure 1

$L = 1.0$ henry
$C = 10^{-4}$ farad
$M = 0.1$ henry
$E = 100$ volts

Capacitors are initially uncharged

These can be written

$$\left(Lp+\frac{1}{Cp}\right)i_1+Mpi_2=E$$

$$Mpi_1+\left(Lp+\frac{1}{Cp}\right)i_2=0$$

The roots of the determinantal equation (a fourth-degree equation) are

$$p_1=j\frac{100}{\sqrt{0.9}} \qquad p_3=j\frac{100}{\sqrt{1.1}}$$

$$p_2=-j\frac{100}{\sqrt{0.9}} \qquad p_4=-j\frac{100}{\sqrt{1.1}}$$

By examination of the circuit, the forms for the two currents are:

$$i_1=I_1'e^{p_1t}+I_2'e^{p_2t}+I_3'e^{p_3t}+I_4'e^{p_4t}$$
$$i_2=I_1''e^{p_1t}+I_2''e^{p_2t}+I_3''e^{p_3t}+I_4''e^{p_4t}$$

Determining the coefficients of the different terms on the right-hand side and substituting them in the equations, the expressions obtained for i_1 and i_2 are the same as those given by Pipes' method. By substitution, the currents satisfy the two Kirchhoff's electromotive-force equations.

Louis A. Pipes: The method of obtaining the transient solution of networks presented in this paper is a special case of a more general method which may be briefly described as follows:

Following the notation of the paper, let

$$(e)=[Z](i) \tag{1}$$

represent the canonical equations of the general n-mesh circuit under consideration. As in the paper, let the equation (1) be subjected to a Laplacian transformation of the p-multiplied type. The equation is transformed to:

$$[Z(p)](I)=(E)+p[L](i^0)-[S](q^0) \tag{2}$$

Now let the transformed current and voltage matrices be subjected to a linear transformation of the type:

$$[B]^{-1}(I)=(I)_t \tag{3}$$

$$[A](E)=(E)_t \tag{4}$$

where $[B]^{-1}$ and $[A]$ are nonsingular matrices whose elements are complex numbers. If we substitute for (I) and (E) the expressions given in (3) and (4) into (2), we obtain:

$$[Z(p)][B](I)_t=(E)+p[L](i^0)-[S](q^0) \tag{5}$$

If (5) is premultiplied by $[A]$, we obtain:

$$[A][Z(p)][B](I)_t=[A](E)+p[A][L](i^0)-[A][S](q^0) \tag{6}$$

Now, if we let:

$$[A][Z(p)][B]=[Z]_t \tag{7}$$

we have:

$$(I)_t=[Z]_t^{-1}\{(E)_t+p[A][L](i^0)-[A][S](q^0)\} \tag{8}$$

If we choose the elements of $[A]$ and $[B]$ so that $[Z]_t$ has the diagonal form, then $(I)_t$ may be readily computed from (8) and the inverse transform $(i)_t$ is then obtained by the usual operational methods. The actual currents are obtained from $(i)_t$ by the equation:

$$(i)=[B](i)_t \tag{9}$$

In the method of the paper, the matrices $[A]$ and $[B]$ were defined by the equations:

$$[A]=[C]^{-1} \tag{10}$$

$$[B]=[C] \tag{11}$$

where $[C]$ is the fundamental matrix of the theory of symmetrical components. If $[A]$ and $[B]$ are specified in this manner, the matrix $[Z]$ must have a certain symmetry for it to be diagonalized. The reduction of a given matrix to the diagonal form by a transformation of the type (7) is one of the fundamental problems of matrix algebra.

In his discussion, Professor Lyon points out that the assumed symmetry of the matrix $[Z]$ which was considered in the paper is more severe than necessary. He shows that the matrices of circuits that are coupled in a certain manner so that they have "ring symmetry" are diagonalized by the symmetrical-component transformation. The fact that the symmetrical-component transformation diagonalizes the matrices of networks having "ring symmetry" makes the method applicable to a much larger class of problems.

It would appear from this that an investigation of the sufficient conditions that must be satisfied by the elements of the impedance matrix $[Z]$ of the general n-mesh network in order that this matrix may be diagonalized by a symmetrical-component transformation is in order. A brief treatment of this question will now be given.

The general transformation matrix, $[C]$, of the theory of symmetrical components may be written in the form:

$$[C]=\begin{bmatrix} 1 & 1 & 1 & \cdots & 1 \\ 1 & a^{-1} & a^{-2} & & a^{-(n-1)} \\ 1 & a^{-2} & a^{-4} & & a^{-2(n-1)} \\ \cdots & \cdots & \cdots & \cdots & \cdots \\ 1 & a^{-(n-1)} & & \cdots & a^{-(n-1)(n-1)} \end{bmatrix} \tag{12}$$

where $a=\epsilon^{j\frac{2\pi}{n}}$

Now, from the basic equation connecting the sequence currents and the sequence voltages, we have:

$$[C](E)_s=[Z][C](I)_s \tag{13}$$

Let us assume that only the rth sequence voltage is impressed on the system and that this produces only rth sequence currents. This is equivalent to stating that the matrix $[Z]_s$ is diagonalized by the transformation

$$[Z]_s=[C]^{-1}[Z][C] \tag{14}$$

If we introduce a column matrix defined by:

$$(P_r)=\begin{bmatrix} 1 \\ a^{-r} \\ a^{-2r} \\ \cdots \\ \cdots \\ a^{-(n-1)} \end{bmatrix} \tag{15}$$

Then the assumption that the rth sequence voltage produces only rth sequence current leads to the matrix equation:

$$E_r(P_r)=[Z](P_r)I_r \tag{16}$$

These equations may be solved for the ratios of E_r to I_r and we have n equations of the type:

$$\left.\begin{aligned} Z_r=E_r/I_r&=Z_{11}+a^{-r}Z_{12}+a^{-2r}Z_{13}+\\ &\qquad\ldots+a^{-(n-1)r}Z_{1n}\\ Zr=E_r/I_r&=a^rZ_{21}+Z_{22}+a^{-r}Z_{23}+\\ &\qquad\ldots+a^{-(n-2)r}Z_{2n}\\ &\cdots\cdots\cdots\cdots\cdots\\ Z_r=E_r/I_r&=a^{(n-1)r}Z_{n1}+a^{(n-2r)}Z_{n2}+\\ &\qquad\ldots+Z_{nn}\end{aligned}\right\} \tag{17}$$

The sufficient condition that the matrix $[Z]$ be diagonalized by a transformation to symmetrical components is obtained by equating the n values of Z_r. This may be done for all the sequences. When this has been done, it will be apparent that if the matrix $[Z]$ has all the elements of its principal diagonal equal, and its other elements have the ring symmetry that Professor Lyon mentions, then the various Z_r quantities are equal and the matrix $[Z]$ is diagonalized by this type of transformation. When this sufficient condition is satisfied, then the first of the equations (17) may be used to compute the various elements of the diagonal matrix.

In the example discussed by Professor Lyon, we have for the impedance matrix:

$$[Z]=\begin{bmatrix} Z & Z_{12} & Z_{13} & Z_{14} & Z_{13} & Z_{12} \\ Z_{12} & Z & Z_{12} & Z_{13} & Z_{14} & Z_{13} \\ Z_{13} & Z_{12} & Z & Z_{12} & Z_{13} & Z_{14} \\ Z_{14} & Z_{13} & Z_{12} & Z & Z_{12} & Z_{13} \\ Z_{13} & Z_{14} & Z_{13} & Z_{12} & Z & Z_{12} \\ Z_{12} & Z_{13} & Z_{14} & Z_{13} & Z_{12} & Z \end{bmatrix} \tag{18}$$

The diagonal elements of the transformed matrix are given by:

$$Z_r=Z_{11}+a^{-r}Z_{12}+a^{-2r}Z_{13}+a^{-3r}Z_{14}+a^{-4r}Z_{13}+a^{-5r}Z_{12} \tag{19}$$

where $a=\epsilon^{j\pi/3}$

Combining terms, we obtain:

$$[Z]_s=[C]^{-1}[Z][C]=\begin{bmatrix} Z_0 & 0 & 0 & 0 & 0 & 0 \\ 0 & Z_1 & 0 & 0 & 0 & 0 \\ 0 & 0 & Z_2 & 0 & 0 & 0 \\ 0 & 0 & 0 & Z_3 & 0 & 0 \\ 0 & 0 & 0 & 0 & Z_4 & 0 \\ 0 & 0 & 0 & 0 & 0 & Z_5 \end{bmatrix} \tag{20}$$

where:

$$\left.\begin{aligned} Z_0&=(Z+2Z_{12}+2Z_{13}+Z_{14})\\ Z_1&=(Z+Z_{12}-Z_{13}-Z_{14})=Z_5\\ Z_2&=(Z-Z_{12}-Z_{13}+Z_{14})=Z_4\\ Z_3&=(Z-2Z_{12}+2Z_{13}-Z_{14})\end{aligned}\right\} \tag{21}$$

In the general case, the diagonal elements of a general matrix having ring symmetry may be computed from the first of the equations (17).

Professor Gardner points out that it is not necessary to use matrix algebra or symmetrical-component theory to solve problems whose impedance matrices involve ring-symmetry. He discusses the solution of a problem whose impedance matrix has ring-symmetry and is given by:

$$[Z]=\begin{bmatrix} Z & Z_{12} & Z_{13} & Z_{12} \\ Z_{12} & Z & Z_{12} & Z_{13} \\ Z_{13} & Z_{12} & Z & Z_{12} \\ Z_{12} & Z_{13} & Z_{12} & Z \end{bmatrix}= \begin{bmatrix} Z_a & Z_b & Z_c & Z_b \\ Z_b & Z_a & Z_b & Z_c \\ Z_c & Z_b & Z_a & Z_b \\ Z_b & Z_c & Z_b & Z_a \end{bmatrix} \tag{22}$$

This matrix may be readily diagonalized to the form:

$$[Z]_s=[C]^{-1}[Z][C]=\begin{bmatrix} Z_0 & 0 & 0 & 0 \\ 0 & Z_1 & 0 & 0 \\ 0 & 0 & Z_2 & 0 \\ 0 & 0 & 0 & Z_3 \end{bmatrix} \tag{23}$$

where

$$\begin{aligned} Z_r &= Z_a+a^{-r}Z_b+a^{-2r}Z_c+a^{-3r}Z_b \\ a &= e^{j\pi/2} \end{aligned} \tag{24}$$

in this case we obtain:

$$\left.\begin{aligned} Z_0 &= (Z_a+2Z_b+Z_c) \\ Z_1 &= (Z_a-Z_c)=Z_3 \\ Z_2 &= (Z_a+Z_c-2Z_b) \end{aligned}\right\} \tag{25}$$

Having diagonalized the impedance matrix, the remaining work by the method of the paper is very straightforward. It may be pointed out that Professor Gardner's equation 2 is essentially a matrix equation, and his procedure of solving for $I_k(s)$ in his equation 3 involves essentially the concept of the determination of the inverse of a matrix. It may be also mentioned that the method of the paper involves merely the concept of naive matrix multiplication and avoids troublesome partial-fraction decompositions.

It must be realized that the method presented in this paper is only a special case of the general method discussed above. This general method is applicable to the solution of sets of linear operational equations with constant coefficients. The essential features of the method are the determination of two nonsingular matrices, $[A]$ and $[B]$ so that the matrix $[Z]_t$ given by $[Z]_t=[A][Z(p)][B]$ has the diagonal form.

As has been mentioned, this is one of the fundamental problems of matrix algebra. The concept of transforming a given matrix to the diagonal form, appears to have been due to H. J. S. Smith in 1861. The method was later extended by Frobenius in 1878. He seems to have been the first one to use the term, "invariant factor" for the elements of the diagonal matrix $[Z]_t$. An excellent account of the development of the theory is given in Sir Thomas Muir's "History of Determinants," volume 4, pages 435–53.

The application of the theory presented in this paper is based on a certain choice of the general matrices $[A]$ and $[B]$ as defined by equations 10 and 11 herein. By such a choice we are able to diagonalize the impedance matrices of circuits having symmetrical "ring coupling" as pointed out by Professor Lyon.

Further research along the lines of the general theory will no doubt result in the discovery of other choices for the matrices $[A]$ and $[B]$ that will diagonalize the impedance matrices of networks having different types of symmetry. To advance the theory in this direction, matrix notation will prove indispensable.

Lightning Currents in Arresters at Stations

Discussion and authors' closure of paper 40-59 by I. W. Gross and W. A. McMorris, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 417–22.

S. K. Waldorf (Pennsylvania Water and Power Company, Baltimore, Md.): Messrs. Gross and McMorris have presented a valuable analysis of the burden placed upon station arresters. There is one factor which they have not evaluated and which is believed to be of importance. This factor is tower-footing resistances on a shielded line and I wish to inquire of the authors if their original data will enlighten us on this point. The experience of the Pennsylvania Water and Power Company may also shed some light upon it.

The authors of the paper under discussion show in their table III no appreciable advantage resulting from the use of overhead ground wires on steel-tower lines. Without overhead ground wires, the number of single-pole discharges recorded per number of three-pole arrester years is 0.81 as compared to 0.79 for the same quantity where ground wires exist. This is no surprise, as lightning surges are transmitted to station equipment when flashovers occur, even though the lines may be provided with well placed overhead ground wires. However, when ground wires provide good shielding and footing resistances have been co-ordinated with line insulation level, the duty of lightning arresters is practically nil. This is because flashovers are then very infrequent and induced surge effects on higher voltage lines are negligible.

Since 1937, surge-crest-ammeter links have been installed on the lightning arresters connected to transmission lines of the Pennsylvania Water and Power Company. Arresters on the three well-protected lines of the company have shown no discharges through them during the three lightning seasons during which observations have been made, even though the lines have received an average of 1.26 strokes per mile per year. These lines are a 66-kv line, 23 miles long, a 132-kv line, 32 miles long, and a 220-kv line, 92 miles long.

The experience on the 132-kv line is of particular interest as two of its four circuits have operated successfully without any lightning arresters since the end of 1934. Its other two circuits have had arresters on them only for the last two years. These latter circuits were provided with arresters because of the connection to them in 1938 of two new circuits which were not as well protected as the two original ones.

Of interest also is a comparison of the observations made at the two ends of the 66-kv line to which reference was made above. As stated, at the well-protected end of the line, no discharges through the arresters were recorded. At the other end, where the well-protected steel-tower line connects to an unprotected wood-pole line, once each year for two of the three years that observations have been made, discharges through the arresters have been recorded. These can be accounted for by the presence of the unprotected wood-pole line.

Lightning arresters connected to a line provided with expulsion tubes and to other lines relatively unprotected, have experienced numerous discharges during the three years' study outlined here.

The data presented in this discussion strongly indicate that lightning arresters are of relatively small importance when connected to circuits well protected from flashovers and direct strokes to conductors. If, as seems to be the case, surges of appreciable magnitude can reach the terminals of a well-protected line only through the medium of a flashover, and as it is probable that the flashover will itself cause tripout, a lightning arrester does not seem to offer any advantage over a suitable spillgap. The latter is the simpler, cheaper, and more easily maintained of the two.

L. G. Smith (Consolidated Gas, Electric Light and Power Company of Baltimore, Baltimore, Md.): The collection of data initiated by the authors provides information of considerable interest to the industry. It is gratifying to note that the authors plan to continue the study. To be of maximum value, the collection of discharge currents in arresters at stations should be extended to other systems. The fact that no surge currents greater than 19,000 amperes were measured is of considerable interest. Might not this lead to the conclusion that the probability of direct strokes occurring in the immediate vicinity of substations is low?

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): In Chicago, we have obtained by means of surge-crest ammeters some data on magnitude of lightning currents through arresters on our relatively small amount of overhead lines on the 12-kv three-phase neutral-grounded system. The lines are on wood poles and have no ground wires. The maximum current, 16,000 amperes, was comparable to the maximum reported by the authors. The next largest current was 5,500 amperes and then the values drop to about 2,000 amperes or less. Most of the currents, including the two largest ones, were of negative polarity. No troubles with arresters resulted from these surges.

The authors seem to focus attention on surges of about 5,000 amperes, but it appears desirable in connection with lightning and equipment to figure on the basis of much higher currents from the standpoint of having freedom from trouble.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper by Messrs. Gross and McMorris is a most interesting report on arrester performance. There have been in the past other reports on arrester performance in the field; but usually these have left considerable question in many users' minds whether arresters did any good, and little, if anything, was ever said about the trouble they had prevented. This paper gives a definite estimate of the number of times that arresters have prevented outage or equipment damage in the sixth column and the last column of table VI. One might say that finally the arrester has become articulate and found means of expressing itself when it has accomplished something. If now we could have some estimate of dollars saved by those operations which prevented damage, a valuable entry could be made in the economics of lightning protection.

In this connection it is significant that several of the contemporary papers dealing with insulation co-ordination and basic insulation levels also place considerable and deserved reliance on the arrester as a determinant of protected level.

In regard to discharges through arresters, some current measurements recently obtained on a high-voltage installation are of interest. The fulchronograms shown by figure 3 of a contemporary paper on "Lightning and Lightning Protection on Distribution Systems" by Bergvall and Beck (AIEE TRANSACTIONS, volume 59, 1940, pages 442–9) were recorded on a high-voltage station-type arrester. These show currents of 1,600, 5,200, and 6,400 ampere crest with total durations of 200 to 350 microseconds. There had been, previous to the recording of these currents, De-ion protector tubes installed at three locations on the transmission line ahead of the arrester. While the tubes were installed, several discharges occurred as shown by an automatic cathode-ray oscillograph connected to record discharge current. These currents were small, 100 amperes and less. However, the oscillograms obtained showed the reflections between the arrester and the tubes, proving that the tubes had discharged and limited the discharge currents through the arresters. In order to increase the duty on the arrester and find out what it might be called upon to discharge without tubes, the tubes were removed and the fulchronograms shown in the Bergvall and Beck paper were obtained. These are all of much greater magnitude than those recorded before the removal of the tubes. These records are in line with the Gross and McMorris data and they are indicative of the effect of protector tubes in limiting the duty on arresters. The careful investigation described in the paper presents excellent evidence for the lightning arrester as an important link in the economics of system operation.

I. W. Gross and W. A. McMorris: Mr. Smith has suggested that the absence of currents in excess of 19,000 amperes in this investigation might lead to the conclusion that the probability of direct strokes occurring in the immediate vicinity of substations is low. The 19,000-ampere value was obtained by adding together the currents in three arresters that discharged in parallel, the highest measured current in any one pole of an arrester being 15,000 amperes.

The exposure to lightning of many lines is less in the immediate vicinity of the stations, than over the rest of their length. However, we do not believe that the absence of higher currents through the arresters necessarily means that there were no direct strokes near the stations. Rather it indicates that if such strokes occurred, much of the direct-stroke current flowed to ground through other paths.

We have records from a location not included in this particular co-operative investigation, where a direct stroke one span from the end of a 115-kv steel-tower line resulted in an arrester discharge current at the station of 2,400 amperes, and another direct stroke two miles out resulted in an arrester current of 1,200 amperes. These values are far below the usual crest current values of direct strokes. Flashover of the line occurred in both cases, thereby reducing the duty on the arresters. In neither case did line flashover obviate the need of arresters, for as shown by table VI, both of the above currents represent traveling waves that would have been dangerous to station equipment in the absence of arresters.

We agree with Mr. Waldorf in that well-located overhead ground wires in combination with low tower-footing resistances are effective in reducing the duty on arresters, but they do not make arresters unnecessary unless all of the following conditions are met:

1. There must be enough ground wires and they must be so located that they will successfully intercept *all* direct strokes, allowing *none* to terminate on the line conductors.

2. Tower-footing resistances must be low enough to limit tower potentials, even when the tower is conducting the highest lightning currents, to values below the line insulation level.

3. Tower heights, span lengths, and ground-wire clearances must be such that steep-front strokes contacting the ground wires cannot flash over to the line wires before the voltage is relieved by a reflected wave from ground. Such flashovers are theoretically possible even with perfect shielding and zero ground resistance.

4. There must be no voltages of dangerous magnitude induced on the line conductors by the release of bound charges.

It is not sufficient to limit traveling-wave voltage at the ends of lines to the transformer insulation strength. Since the voltage may approximately double there because of reflection, the voltage of the incoming traveling wave must not exceed about *one-half* the transformer insulation strength.

Measures which are designed to meet the above requirements may involve much greater expense than the installation of arresters for the protection of equipment at the station, and at the present stage of the art it cannot be insured that the requirements will be fully met. In some locations low footing resistances cannot be obtained. The fact that these requirements are not easily achieved is forcefully illustrated by a recent case on a thoroughly modern high-voltage line. Every effort had been made to make the line immune to the effects of lightning; yet a direct stroke of lightning missed well-placed ground wires, and contacted a line conductor.

Mr. Waldorf is fortunate in having a location where low footing resistances can be obtained. Performance records from his lines and other similar ones over a period of several years will be required to establish very definitely how free they are from lightning trouble. In this connection it is significant that it has been the experience of several operating companies to have little trouble from lightning for several years, and then to suffer considerable damage in one severe storm.

All of the data presented in table VI for 132-kv circuits were obtained on steel-tower lines with ground wires. About one-quarter of the cases in which dangerous voltages at stations are indicated, if arresters had not been used, were on lines with tower-footing resistances of the order of 15 to 20 ohms, and three-quarters were on lines with footing resistances of the order of 50 ohms. The former group has considerably lower footing resistances than the probable average for all lines, and the latter is about average. Considering the type of line construction of all of these circuits, and the lower-than-average footing resistances of many of them, it is evident that the lightning duty on 132-kv arresters as shown by table VI was obtained on circuits with better-than-average line protection.

In most cases data are not available which would show whether or not line flashover occurred coincident with the recording of arrester discharge currents at the stations. Among the cases for which data are available, are 62 records of arrester discharge which did not correlate with line tripouts or any other evidence of line flashover. Nearly 70 per cent of these discharges represented waves that would have been dangerous without arresters. The proportional number of these waves of dangerous magnitude which definitely did not correlate with tripouts was only five per cent less for 132-kv steel-tower lines with ground wires and tower footing resistances of 15 to 20 ohms, than for the average of the entire investigation.

Even though this proportion may be altered some as the study continues, the data certainly do not bear out Mr. Waldorf's contention that waves of dangerous magnitudes will appear on such lines only as a result of line flashover, and will therefore almost certainly be coincident with line tripout. The use of spillgaps at the stations in place of arresters probably would have resulted in a considerable increase in the number of line tripouts, and an appreciable sacrifice of the low impulse protection level provided by the arresters.

Since it is so difficult in many cases to build a line that is substantially immune to lightning flashover, the money required to provide elaborate ground-wire systems, counterpoises, and low footing resistances can often be used to better advantage in providing protector tubes. Lightning voltages that occur on the line, if of sufficient magnitude, will be discharged by the tubes, and line tripouts will be minimized. Since the line flashover voltage is reduced, the duty on the arresters is less, but in general the expected reduction would not be as great as in the case referred to by Mr. Beck. When the currents which an arrester must discharge are reduced, a lower level of protection results for the station equipment.

We would like to emphasize the variation in lightning severity from year to year and between different locations, as illustrated by figure 6, and to point out that because of such variations, conditions are likely to be encountered in service which may be quite different from those reported here. It also follows that any accurate determination of the average or the extreme conditions which exist in service must be based upon experience over a period of several years, a wide range of localities, and with different types of line construction.

Lightning and Lightning Protection on Distribution Systems

Discussion and authors' closure of paper 40-60 by R. C. Bergvall and Edward Beck, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 442–9.

D. D. MacCarthy (General Electric Company, Pittsfield, Mass.): Messrs. Bergvall and Beck have presented an informative discussion of the division of long-duration lightning currents between arresters and the transformers connected to grounded-neutral circuits. This analysis confirms the prediction made by Doctor K. B. McEachron[1] in February 1939 when he discussed some of the long-time discharges which he recorded oscillographically as early as 1937. In this article Doctor McEachron states, regarding continuing lightning discharges to grounded neutral systems: "... the transformer winding represents a relatively small impedance to the flow of such unidirectional currents."

Following Doctor McEachron's suggestion, the writer and his associates in Pittsfield investigated the discharge of unidirectional currents through distribution transformers. The results presented by Messrs. Bergvall and Beck seem to agree with ours and show that the transformer inductance is reduced due to saturation of the iron. This reduction of inductance occurs in a time sufficiently short so that continuing lightning will cause appreciable currents to flow in grounded-neutral transformers, and our tests show this to be true, even though the transformer secondary circuit is open.

Messrs. Bergvall and Beck confined the discussion of current drainage to the case of a grounded-neutral feeder supplied from a single-phase transformer. Many single-phase feeders branch out from three-phase systems supplied from a three-phase bank of transformers. From our investigation it appears doubtful that the analysis given by Bergvall and Beck for the current drainage through substation transformers applies to such circuits.

We agree with the authors that more data are needed in order to establish the wave shape of the currents which arresters discharge in service. Comprehensive data have been published by McEachron and McMorris[2,3] on the magnitudes of arrester discharge currents. At the time of publication of the first paper[2] records were available from 1,225 installations which had been in operation for slightly more than one year in territories which were widely separated. A large amount of data was necessary to establish typical values for discharge currents because of the extreme variability of lightning due to seasonal variations and differences due to locality and perhaps type of circuit construction. Similarly it seems likely that several hundred measurements must be obtained in order to establish an accurate criterion for the wave shapes of arrester discharge currents.

Figure 7 of the paper has been prepared to permit estimates to be made of the magnitude and duration of the discharges to which distribution arresters may be subjected. It is based on the assumption that the continuing part of the lightning stroke consists of a constant current of 1,000 amperes lasting for times as long as 5,000 or 6,000 microseconds. The authors do not establish that such a discharge is typical of lightning. However, we feel that the difference in current drainage on urban and rural circuits may account for the higher failure rates of lightning protective devices sometimes experienced on rural circuits.

Figure 10 shows the current, measured by a fulchronograph, which was successfully discharged through a new Autovalve arrester. It is of interest to inquire if rated power voltage were applied to this arrester when the discharge occurred. In case no power voltage were applied, do the authors consider this operation to indicate that the arresters will stand such discharges in service?

There appears to be some inconsistency in the data on arrester protective characteristics given in table I. Using 58 kv for the breakdown of the 9-kv arrester and the data from table I for the 3- and 6-kv arresters, we obtain:

Arrester Rating (Volts)	Impulse Ratio
3,000	1.01
6,000	1.18
9,000	1.17

The impulse ratio of the 3,000-volt arrester is much lower than the impulse ratios for the arresters rated 6 and 9 kv which use the same type of gaps which seems inconsistent. In his contemporary paper (AIEE TRANSACTIONS, volume 59, 1940, pages 429–33) Mr. Berkey of the Westinghouse Company gives impulse ratios of 1.05 for single mica-spaced gaps and 1.20 for single ionization gaps which are at the top of the Autovalve arresters. Neither of these gaps has an impulse ratio as low as 1.01.

REFERENCES

1. LIGHTNING TO THE EMPIRE STATE BUILDING, K. B. McEachron. *Journal* of the Franklin Institute, volume 227, February 1939.

2. DISCHARGE CURRENTS IN DISTRIBUTION ARRESTERS, K. B. McEachron and W. A. McMorris. AIEE TRANSACTIONS, volume 54, 1935.

3. DISCHARGE CURRENTS IN DISTRIBUTION ARRESTERS—II, K. B. McEachron and W. A. McMorris. AIEE TRANSACTIONS, volume 57, 1938 (June section).

D. R. Pattison (nonmember; Pennsylvania Electric Company, Johnstown): The authors of this paper have approached the lightning-protection problem from a new angle to explain the high distribution-arrester failure rate under operating conditions. Their investigation does explain some of the high failure rate and operating men have often suspected something of this sort being responsible for the blowing of heavy arrester fuses and heavy cutout fuses or leads burned off in small transformers but could just never put their finger on the cause.

Referring to their summary in which it is stated that the most important problem is the matter of eliminating arrester failures, I believe that the weight of operating evidence indicates that a most important point in their investigation has been overlooked, that is, the problem of permanent sealing of the arresters.

Our operating experience has indicated that a large percentage of the distribution failures is caused or in some way effected by the deterioration of the gasket seal which allows the arrester to breathe and results in the entrapment of moisture and finally the failure of the arrester. Even the most recent developed units after short service periods show signs of leaking. Several test methods have been developed and used with varying results. Of these the sectional dielectric loss test and the high-voltage resistance tests seem to be the most reliable, but, oddly, units which have tested good with the above methods, have shown definite signs of moisture when subjected to sudden chilling of the gap ends with dry ice and immediately broken open. We do not believe this moisture got there during the manufacturing process for we have found some dry ones.

There is little doubt in anyone's mind that entrapped moisture does effect the arrester in several ways.

First, it does lower thermal capacity of the valve element and when the arrester is called on to pass current, the moisture is vaporized and the resulting pressure explodes the shell.

Second, a process develops in which the moisture condenses in the gap structure, and the higher potential gradient on the gaps at the line side is effective in breaking down the air at this point, the moisture becoming mixed with it to form corrosive acids which build up conducting salts finally causing sparking and more or less *permanent radio interference* until the first gap unit is completely short circuited. This process is again repeated on the next gap unit until enough of the gap structure has been short circuited to cause permanent breakdown and eventually failure of the arrester.

Third, during rapid temperature drops moisture is condensed on the inside of the arrester shell and becomes mixed with the other parts of broken-down air to form conducting islands and raises the potential gradient to a point where sparking occurs, causing *temporary radio interference*. Then as the temperature equalizes the moisture either freezes and becomes nonconducting or it is thrown off in suspension and the noise stops. This effect is probably the most troublesome item in the operating field for it starts the noise up with Lowell Thomas and usually ends with Paul Sullivan, requiring repeated investigation to clear the trouble.

Fourth, there is simply the result of numerous temperature cycles which eventually produce a conducting path on the inside of the shell which entirely short circuits the gap structure and impresses line voltage on the valve element and finally results in the failure of the arrester.

In the light of the foregoing, we believe that the problem of standardizing radio-noise levels, referred to in the paper, is not so necessary but rather the permanency of any adopted level would seem to be the important object and this cannot be had without some type of fused seal. Our operating experience with all types of gaskets has indicated that any gasket in contact with oxygen has a very short natural life, possibly around five years, and that anything done to increase the arrester capacity to provide for higher surge current or long-tailed waves would seem to be all the more justification for permanent sealing as an approach toward the perfect arrester.

Certainly the distribution engineer will anxiously await the full investigation of the subject of this paper for besides being a general nuisance it has been a costly item. Therefore, it deserves the full co-operation of both the manufacturers and the users of distribution lightning arresters.

H. A. P. Langstaff (West Penn Power Company, Pittsburgh, Pa.): The paper presented by Messrs. Bergvall and Beck reviews briefly the lightning-arrester problem and its many diversities involving performance, inspection, records, new designs, and means for obtaining more complete lightning discharge characteristics. This is all of great importance to a further improvement in the continuity of electrical service.

They prove that there is necessary further information with respect to the characteristics of lightning discharges which materially affect the arrester design. Many improvements have already been made and it is to be hoped that these improvements will continue. The West Penn Power Company has for several years been securing actual lightning discharge-current records for a number of lightning arrester installations varying in voltage from 132 kv down to 6,900 volts. Our studies have been confined mostly to the 132-kv and 25-kv systems.

We have installed a number of surge recorders consisting of two metal electrodes separated by a thin paper disk. A "densometer" and calibrated curves provide our means of obtaining approximate current values from the paper disks. The results of these records are shown in tables I and II of this discussion.

We have no record of surge current value over 5,000 amperes on 132-kv solidly grounded system.

Table II. Number of Records for 25-Kv System

Amperes	1935	1936	1937	1938	1939	Total
Up to 250	15	8	3	0	7	33
251–500	12	7	0	2	3	24
501–1,500	11	10	0	1	12	34
1,501–5,000	1	0	1	3	8	13
5,001–10,000	2	0	0	1	2	5
10,001–20,000	1	1	0	1	5	8
20,001–30,000	0	1	0	0	3	4
75,000	0	0	0	0	1	1
Total	42	27	4	8	41	122

From table II it is noted that 75 per cent of the discharges have been 1,500 amperes or less and 25 per cent above this value and up to 75,000 amperes. Our records show an accumulative percentage failure of approximately 2 per cent per year. We have for the past several years and are continuing to apply only line-type arresters on all voltages up to and including 44 kv. We have one 132-kv line-type application and probably would have more if erection facilities for this type of arrester were more feasible.

We have secured discharge records from all but one of our surge recorders. This has been in service for approximately five years at an indoor station supplied by seven 25-kv circuits protected by station-type arresters. The recorder is in the ground circuit of three 20-kv arresters connected to the main transformer bus.

During August 1939 we installed a fulchronograph recording unit in the ground circuit of three 25-kv line-type arresters located at the end of a $3^1/_2$-mile 25-kv circuit. Several arresters had been demolished at this station. We have obtained only one record of less than 100 amperes for approximately 100 microseconds. We hope to secure more records in 1940.

It may be of general interest to outline briefly several outstanding discharge records as they have a bearing on general applications.

Example 1. At one station in vulnerable lightning-discharge territory, we have a transformer bank protected by three 20-kv arresters connected to the 25-kv system with neutral grounded through resistance. This station has given 25-kv incoming lines each protected by a 25-kv electrolytic arrester. There are several 6,900-volt low-voltage feeders each protected with low-voltage arresters. In 1935 our records show 17,000 amperes discharge through the recorder with no equipment failure or outage. At a later date there was recorded a 28,000-ampere discharge through these same arresters with no equipment failure or outage.

Example 2. At another substation on the end of a $9^1/_2$-mile 25-kv line we have the transformers protected by three 20-kv line-type arresters and one record shows a discharge of 30,000 amperes arrester was destroyed.

Table I. Number of Records for 132-Kv System

Amperes	1935	1936	1937	1938	1939	
Up to 250	41	11	6	0	4	62
251–500	2	2	0	0	0	4
501–1,500	4	0	1	0	2	7
1,501–5,000	2	0	0	3	0	5
Total	49	13	7	3	6	78

Example 3. At another station having three 25-kv lines we have a record of 19,000 amperes discharge through 20-kv line-type arresters protecting the transformer bank with no equipment failure or outage.

Example 4. At a station fed by three 25-kv lines each protected by 25-kv line-type arresters we have the transformer bank protected by three arresters approximately 13 years old but reduced to the equivalent of a 20-kv arrester. Our records show a 12,000-ampere discharge through these old arresters with no equipment failure or outage.

Example 5. At the station where we have installed the fulchronograph recorder we have a record of 25,000 amperes discharge through 25-kv line-type arresters where one arrester was destroyed. We have lost a total of 11 arresters; five 20 kv and six 25 kv at or adjacent to this station.

Example 6. We have one record showing 75,000 amperes discharge through a 25-kv arrester and one of the three units was demolished with no other equipment failure. One of the $9^1/_2$-inch gaps on transformer bushing flashed over.

Based on our arrester experience, I suggest that the manufacturers continue to consider in their line-type arrester designs an arrangement whereby the arrester will clear itself in case of failure. Horizontal rather than vertical mounting might be considered. The mounting of 132-kv arresters would be simplified if the arrester columns could be made self-supporting.

Edward Piepho (The Detroit Edison Company, Detroit, Mich.): The authors have indicated some changes in our thinking with reference to distribution arresters which will be necessary considering the new conception of long-duration lightning strokes. There are other aspects of the lightning-protection problem which must also be reconsidered. For instance, flashovers caused by long-duration strokes will limit the benefits to be realized by high-speed switching or other similar schemes directed toward rapid fault interruption and reclosure.

It is also to be expected that long-duration strokes will increase the erosion rate and shorten the life of expulsion-tube arresters. During the past lightning season three cases of excessive tube-arrester erosion occurred on the Detroit Edison system which could be explained only by assumption of multiple or long-duration strokes.

The practice of interconnection of arrester grounds with other ground circuits is also involved in this problem. In some cases it may be undesirable to risk bringing long-duration lightning currents into other circuits by interconnection of arrester ground leads with these circuits, even though the anticipated lightning current may be low in magnitude. During one of last summer's storms in Detroit, an area of approximately nine square miles was subjected to severe lightning. Observers described some of the strokes as "flickering" in nature. Numerous customers' complaints resulted, some of which were due to low voltage caused by blown transformer fuses, and others due to troubles in customers' premises such as blown fuses and damaged wiring, or simply blown fuses. There were also four distribution transformer failures and seven watt-hour meters damaged in this area by the storm. An unusual feature of these troubles was the large number of customers' branch fuses blown without blowing of main fuses or apparent trouble in the house wiring. These troubles may have been caused by insulation failure between transformer primary and secondary which brought primary voltage in on secondary wires, or they may have resulted from direct strokes to customers' service wires. It is also possible that the trouble may have been caused by long-duration lightning currents flowing from the arrester ground wire to the secondary neutral by way of the interconnection, and thence through the transformer winding onto secondary wires and into customers' wiring.

I should like to inquire concerning the type of failure of arresters 4 and 5 whose electrodes are shown in figure 1*b*. Were the failures by flashover or puncture of the characteristic element?

In connection with impulse tests on the arrester described, oscillograms of which are shown in figures 9, 11, and 12, it is of interest to note that crest values of discharge voltage for the current surges used may be approximately duplicated with current waves having the same ampere-per-microsecond wave front but not necessarily as high crest values. Thus for example, a current wave of only 1,000-ampere crest but with 1,000-amperes-per-microsecond front will produce approximately as great crest discharge voltage as the wave shown in oscillogram 5 of figure 9. This relation appears to hold generally for all arresters having characteristic elements which discharge current partly by an ionization process.

I. W. Gross (American Gas and Electric Service Corporation, New York, N. Y.): In the early days of applying lightning arresters for protection, very little was heard about the 60-cycle rating of the arrester itself. It was customary to accept the manufacturer's rating of such a device and hope that it would supply the desired protection under lightning conditions about which very little was then known.

Many of the early lightning-arrester installations were made under conditions where the arrester was so well protected from 60-cycle failure by excess 60-cycle rating that it was not possible for such arresters to supply protection to equipment which in many cases had inherently lower insulation than the level maintained by the arrester. On our own system, as early as 1928, we reduced the 60-cycle rating of many arresters then in use by removing part of the active element, thus lowering the protective level provided by the arrester. This was done, as shown by operating experience since that time, without in any way encroaching on the lightning arrester permissible 60-cycle voltage.

From the days when lightning-arrester failures were often attributed to direct strokes, we have now come to the stage where only isolated cases of arrester failure can be charged to the magnitude of lightning currents.

To account for the low percentage of arrester failures which has been observed in practice, the authors have suggested that this is due in large part to long-tail current waves and not the old bug-a-boo of high lightning currents. From our experience where arresters have failed, and from spot-checking of arrester conditions in the field, it is believed that some of these failures may be attributed to arrester design or deterioration or a combination of both; just how large a part, at the present time, is uncertain. In other words, gap corrosion observed in the field has undoubtedly been responsible for many arrester failures. If long-tail current waves are responsible for a large part of the arrester failures which have been reported, it would seem that the frequency of such long-tail waves would have been observed to some extent at stations as well as at isolated installations on distribution lines. Our experience with lightning-arrester failures at stations does not indicate that the long-tail current wave has been the major cause of the relatively few arrester failures at stations.

If I understand the author's line of reasoning, they maintain that long-tail lightning-current waves are a hazard to arresters on rural lines remote from the source of power supply. But at the source of power, such currents do not impose as severe a condition on the arrester because the tail of the wave is drained off through the impedance of the station equipment and ground which is usually inherently low.

If such is the case, it would appear that the high-capacity station-type arresters properly belong out on the line, and the distribution-type low-capacity arrester may more properly be installed at the station if its protective characteristics are suitable. Since the economics of the situation does not permit this, would it not be possible to use distribution or line-type arresters at the station, with considerable resultant economy, and no increased hazard as regards arrester failure?

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): Operating experience showing that distribution arresters fail due to lightning, in spite of the general absence of the high-current surges, tends to support the authors' suggestion that these failures are often caused by surges of low current magnitude but long duration. I am wondering whether multiple surges could also be considered a factor in some cases. I am sure that those concerned with the protection of distribution-line equipment will await with interest the additional measurements of lightning surges on such systems which the authors are conducting to augment their preliminary investigations.

Following the authors' reasoning, we should expect low rates of arrester failure on the four-kv neutral-grounded system in Chicago. There is usually about one mile of cable from the substation transformer to the center of each circuit, with overhead extensions spreading out for a few blocks from cable taps to the main cable. The density of arresters is high and the average circuit has approximately 55 distribution transformers with a total capacity of over 2,000 kva. These factors apparently all work together to provide a low-impedance path for drainage of surge currents of long duration. The average rate of failure of arresters due to lightning is about 0.2 per cent per year, or about the same as some national figures, although many of the arresters are unusually old, that is, 15 or 20 years or older, and have a discharge capacity of only 1,000 or 1,500 amperes. For modern types of arresters, the annual rate is roughly $^1/_{40}$ of one per cent.

While the paper deals exclusively with the problem of arrester failures, it should be emphasized that we are primarily interested in protection of equipment. Nevertheless, it is quite gratifying to note the improvements in the arrester to make it more resistant to failure, while retaining good protective characteristics.

L. G. Smith (Consolidated Gas, Electric Light and Power Company of Baltimore, Baltimore, Md.): If, as the authors state, the cause of the disproportionate rate of lightning-arrester failures on some distribution systems has been determined, a considerable advance has been made. As is stated in the paper, data collected on the length of surges, the tests on arresters in the laboratories, the examination of arresters failing in service, and theoretical considerations all indicate the presence of long-tailed waves and therefore justify further intensive investigation to obtain probability data concerning these impulses of extremely long duration.

From the theoretical analysis presented, it appears that the sole limitation upon the length of that part of the wave that passes

Table III

Range of Average Ground Resistance (Ohms)	Lightning-Arrester Failures Per 100 Transformers			
	Standard Connection		Interconnection	
	All Types of Territory	Rural Territory	All Types of Territory	Rural Territory
0– 30	0.578	0.450	0.720	0.940
40– 75	0.384	2.940	Incomplete data	
80–125	2.67	No data	Incomplete data	
200–250	3.84	4.07	Incomplete data	

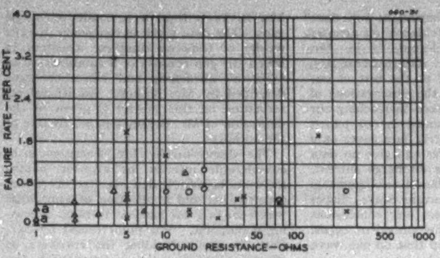

Figure 1. Urban and suburban, wye; a indicates less than one ohm

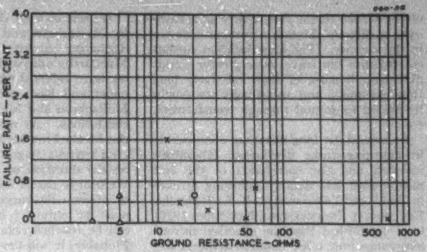

Figure 2. Urban and suburban, delta

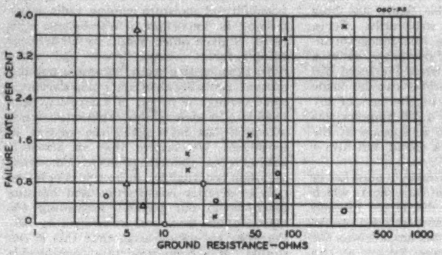

Figure 3. Rural, wye

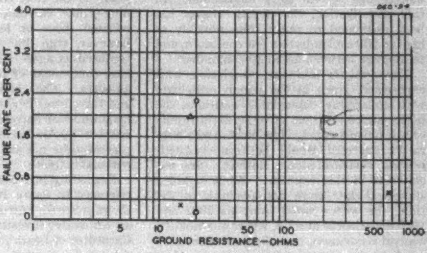

Figure 4. Rural, delta

Figures 1–4. Solid, gapped, and standard connections, four kv

X—Standard
O—Gapped interconnection
Δ—Solid interconnection

through the arrester when a line flashover does not occur is the relative values of (R_1+R_2), (X_1+X_2), and R_3 (see figure 4 of the paper). The greater the value of R_3 (ground resistance of arrester grounds), the higher will be the voltage forcing drainage current through other paths. Since this theory is presented to explain why the arrester failure rate is low on one system and high on another, it is necessary to examine the probable variables in order to account for the differences. For similar types of areas served, R_1, X_1, and X_2 should not differ greatly for various systems. However R_2 (substation ground resistance) may vary. Likewise R_3 (arrester ground resistances) may vary. It would be of interest to correlate arrester failure rates with these factors. It is believed that substation ground resistances are substantially lower in urban and suburban areas than in rural areas. Therefore, it follows that arrester failure rates in rural areas should be higher than in urban and suburban areas.

In order to check this conclusion, the data summarized in reference 2 of the paper has been examined. For 1934 to 1936 lightning seasons the comparison in table III of this discussion is drawn. While not entirely conclusive, these data indicate somewhat higher failure rates in rural areas. Failure rates also increase with arrester ground resistances.

Although operating data indicate that as the arrester ground resistance increases the arrester failure rate becomes greater as the authors predict, theoretical considerations seem to show a reverse trend. It would be interesting if the authors demonstrated the theoretical basis for this contention. Arrester failure rates from data collected from various companies since 1934 have been analyzed by type of territory, grounded or ungrounded neutral, and ground resistance. An inspection of charts on which shotgun diagrams of the data were plotted indicates:

1. No definitely defined trends can be observed.
2. It cannot be concluded that arrester failure rates are higher on delta systems than on grounded-neutral star systems.
3. There seems to be a slight increase in arrester failure rates with increasing arrester ground resistance.

J. R. North (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper presents a very interesting "story behind the development", and it is gratifying to learn of the studies made to improve the characteristics of these lightning arresters.

In paragraph 3, some rather definite statements are made concerning the distinctive markings left by surge discharge currents on the gap electrodes of lightning arresters, and it would be interesting to have supporting data in addition to that shown in figure 1a. We should also like to learn if these same types of markings occur irrespective as to the type of gap and with different types of arresters.

The paragraph under "Effect of System Characteristics" and figures 11 and 12 are apparently intended to illustrate tests which have been made on the new type of arrester. It would be interesting to have a direct comparison of these characteristics with those of the former design.

Referring to the first paragraph under "Summary", the fifth line states that ". . . it is believed that the most important problem is the matter of eliminating, as far as possible, the failures of protective devices. . ." True enough, the reduction in failures of lightning surge protective devices represents an important problem but, in our opinion, the "most important" problem is the matter of apparatus protection, and the infrequent failure of a lightning-surge protection device may be preferable to the failure of the equipment being protected. In years past, several very unfortunate failures of apparatus have occurred due to the inadequate protection afforded by what might be termed "self-protecting arresters".

The results of these studies suggest that further thought be given to modifying the present standard lightning-arrester test waves to test adequately the ability of arresters on long waves as well as on short ones. This subject is being given consideration by the AIEE lightning-arrester subcommittee.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Messrs. Bergvall and Beck have presented an interesting and important contribution on the influence of the network on the relative amount of lightning currents discharged through lightning arresters. The high-current component, because of its short duration and the high voltage associated with it, finds its way to ground within a short distance from the point of incidence of the stroke to the line, either as a flashover or directly through a protective device in the vicinity. The long-duration current components can travel over greater circuit distances, because of their relatively long time constants, and accordingly are partly by-passed to ground through transformers, etc.

In regard to long-duration components in lightning-stroke discharges, laboratory research and the correlation of the test results with field data ("Lightning Strokes in Field and Laboratory", P. L. Bellaschi, Electrical Engineering, November 1939) have established that the effective current may range from a few hundred amperes and less to several thousand amperes. The corresponding duration ranges from approximately one second to a millisecond. In this investigation, the electric charge has been estimated to range from possibly as high as 200 coulombs to a few coulombs, and to average between 25 to 50 coulombs.

The analysis and the correlation with experience, given in the paper, anticipate with apparently dependable assurance the performance expected of lightning arresters on various networks. The most severe duty to be expected would be from isolated-neutral systems which, however, are not common in this country. While detailed statistical data were not available at the time, the writer noted from his 1937 and 1939 experiences abroad, where the isolated neutral is common, that the service severity on lightning arresters was greater than originally expected—due, perhaps, in part also to switching surges.

An important aspect which affects the protection problem resides in the characteristics of the protective device itself. The protector tube is a very fitting illustration. By virtue of its operating principle, this and similar devices are relatively unaffected by the thermal and heating effects associated with long duration current components. The various applications and services (high-voltage lines, distribution circuits, and distribution transformers), for which this type of device has been developed and applied, give ample proof that not infrequently this type of device has been subjected to the test of full lightning-stroke discharges, yet, its performance on this score has been good. In fact, if anything, as the paper by Messrs. Sels and Gothberg (AIEE Transactions, volume 59, 1940, pages 328–31) indicates, there has been a demand for greater high-current-carrying capacity. Thus, it appears that given the inherent characteristics of the type of protective device and, in a measure, the nature of the protection problem is established.

R. C. Bergvall and **Edward Beck**: Mr. MacCarthy comments that many single-phase feeders branch out from three-phase systems supplied from a three-phase bank of transformers and that from his investigation he is doubtful that the analysis given by the authors of the paper for the substation transformer applies to such circuits. The authors' analysis was based on such installations and used conventional line-to-neutral methods of calculation. The system may be either three or single phase. If Mr. MacCarthy has some definite misgivings, we regret that he did not go into sufficient detail for us to give a more specific answer.

We acknowledge the important work done by McEachron and McMorris on arrester discharge currents referred to in Mr. MacCarthy's discussion. As a matter of fact, we have referred to it in the text of our paper and have used the observations of McEachron and McMorris in reaching our conclusions. As mentioned in our paper, the McEachron-McMorris data cover only crest current magnitudes and give no data on the duration of the discharge current. We believe that a number of measurements of arrester-discharge-current wave shapes must be obtained before any conclusions can be reached regarding prevalent types. Probably, it will develop that no one wave can be selected as typical.

Regarding the assumption of 1,000 amperes as the long component of the lightning-stroke current, this figure has been selected more or less arbitrarily. It is, however, true that the fulchronogram introduced as figure 10 of our paper, shows a long component of between 1,000 and 600 amperes. The calculations can be made and the curves derived on the basis of any magnitude of current; 1,000 amperes was selected by us because it appears at the present time not only a round, but also a reasonable figure.

Regarding Mr. MacCarthy's question in reference to figure 10 of our paper, which shows a stroke current discharged through an Autovalve arrester and recorded on the Cathedral of Learning, the arrester was not subjected to rated power voltage at the time of the discharge. The installation on the Cathedral of Learning was made to measure the complete stroke current and also to test the arrester with such a discharge current. If a transformer had paralleled the arrester in order to apply power voltage to it, a great part of the long component of the stroke would have passed through the transformer and not through the arrester, thereby considerably lightening the duty on the arrester. The follow current which an arrester of this type will pass following a surge discharge is small, in the order of 15 to 20 ampere crest at full rated voltage. Although we do not know definitely, it is our opinion that the performance of the arrester in this case would not have been altered by follow current. We wish again to call attention to the opinion expressed in the paper; that the current discharged through lightning arresters in service and the current in direct strokes may be quite different. At any rate, it is not possible at this time to correlate the two. It is our opinion that the discharge current through arresters in service would not be as severe as in the stroke which hits the system and produces the arrester discharge current. On the other hand, undoubtedly currents more severe than that shown in figure 10 will be experienced in a small percentage of cases.

Mr. MacCarthy calls attention to variations in the impulse ratio of breakdown of the arresters of different ratings. The 3,000-volt arrester is most favorable because of the short gap and the small number of its component parts. We have found that as the length and number of parts of the gap is increased, the impulse ratio is also increased slightly. It is to be noted, however, that the impulse ratios found on the 6,000- and 9,000-volt arresters are low and that the impulse breakdowns are consistent. Mr. Berkey's tests were made on single gaps. The data given in our paper are the results of tests on complete arresters in which the gap consists of a combination of both the quenching and ionization gaps described by Mr. Berkey. Additional information on this subject is given in the discussion of Mr. Berkey's paper by one of the authors.

The discussion by Mr. Pattison is of considerable interest to arrester manufacturers because it describes factors in the construction and application of apparatus which are of great moment from an operating standpoint. The point raised by Mr. Pattison about avoiding the entrance or effects of moisture in arresters with the resulting possibility of moisture causing arrester failure and the much greater possibility of moisture causing radio interference is important. We believe this factor is given much weight by all arrester manufacturers. Some years ago when radio broadcasting really became a popular pastime, probably all arrester manufacturers went through a period of excitement and trouble. In view of this experience, much attention has been given the matter of moisture. We believe that in arresters built in recent years the problem of moisture exclusion is negligible; at least it is so in the type of arrester construction and housing described in our paper. Regarding the matter of the fused seal mentioned by Mr. Pattison, we acknowledge that this is desirable. It is being used to a considerable extent in Autovalve arresters of the higher voltage ratings. This subject is receiving continual study but no economical way has yet been found of commercially incorporating this method of sealing in the lower-voltage arresters.

In connection with the matter of radio interference, it has been the authors' experience that occasional complaints are received that modern arresters are causing radio interference. It has been our practice always to investigate each case in the laboratory, and we have found only a negligible percentage of arresters in which there might be a bare possibility of interference with radio reception in the field. It has been quite difficult for us to coordinate the complaints with our laboratory tests and usually upon requesting further information as to how the tests in the field were made and how the interference was run down to the arrester, the subject dies. We have found cases of complaints where some microvolts of noise current were registered in the laboratory on arresters which were returned with conductors still spliced to them. In these cases it developed that the seat of the noise current was the splice, and that removal of it, or an improvement in the splice, eliminated the noise current or reduced it to a negligible value. Although we do not mean to assert that there may not occasionally occur a case of radio interference on an arrester which is made in mass production, we would like to call attention to the fact that in many cases the interference which was experienced in the field will have been caused by the connections.

Mr. Pattison calls attention to a method of determining the moisture content of the air in an arrester by chilling it with dry ice. There is always some amount of moisture in the atmosphere at room temperature such as exists on a manufacturing floor. If this atmosphere is chilled sufficiently to pass the dew point, then moisture will be precipitated. This possibility of moisture in sufficient quantity to be hazardous being introduced into an arrester during the assembly period has been recognized, and in the arresters discussed in our paper the assembly is carried out in an air-conditioned room in which the temperature and moisture content is controlled.

We agree with Mr. Pattison that this problem merits continuous study and we believe a good deal of good can be accomplished by co-operation between the user and the manufacturer, especially if this enables some co-ordination between laboratory tests and field results, leading to reliable and accepted methods of laboratory tests.

Mr. Langstaff has collected a considerable amount of significant data on discharge currents on his 132- and 25-kv systems. The indications again are that low discharge currents preponderate, as 75 per cent of his records show not more than 1,500 amperes. It is hoped that the fulchronograph installation mentioned by Mr. Langstaff will yield some significant data on wave shape to augment the records of crest current. The location appears to be a vulnerable one since 11 arresters have been lost in its vicinity.

Mr. Langstaff's records on the 132-kv system show no discharges in excess of 5,000 amperes in a total of 78 covering a period of five years. This, together with the data presented by Messrs. Gross and McMorris in a contemporary paper suggests that the performance of arresters at 5,000 amperes is a good point for co-ordinating insulation and arresters on high-voltage lines, at least.

Mr. Piepho has made some interesting observations on possible practical effects of surges of long duration. Since the Detroit Edison distribution system is one with a free neutral, arresters should be subjected to the full duration of the stroke currents, except for the effect of line flashovers. We understand that the arrester failure record in Detroit is very low, and this is of considerable interest as it indicates that some other factor may be operating in that locality perhaps to make the long-tailed discharge very exceptional. Further observation in this connection should be of value. The ground resistances or soil resistivity might be involved in this. On the other hand, Mr. Piepho cites a storm whose visible manifestations indicate long-duration strokes. Some further investigation of the blowing of customers' branch fuses would be desirable. We would suggest that Mr. Piepho make some surge tests on common house fuses and determine what it takes to blow them on surges.

Regarding Mr. Piepho's question about the type of failure experienced in the cases of 4 and 5 in figure 1 of the paper, the actual data on these particular arresters are not at hand, so the question cannot be answered specifically. On the type of arrester represented in this figure, the Autovalve arrester preceding the distribution arrester described in our paper, the prevalent type of block damage found in the arresters examined has been a partial flashover usually accompanied by small short punctures at the ends near the edge. There have been more cases of this kind than pure punctures. Parts 4 and 5 of figure 1 were probably such, although they might have been of either type. We have not found that the appearance of a damaged block is a guide to the duration of the discharge. The observations on damaged blocks led to the development of a new baked insulating coating for the block which, as far as our experience in the laboratory and the field goes, appears to eliminate the flashover, and thereby permits full use of the capabilities of the block itself. This finish is white in color as shown in figure 8 of the paper.

Mr. Gross, as well as Mr. Pattison has mentioned corrosion and other factors as possible causes of arrester failure. We have not intended to assert that long-tailed waves are the only cause of arrester failure, since other causes are recognized in the case of arresters of early construction more so than in those built within the last six or eight years. This was indicated at the beginning of the paper. It is our opinion, however, that the long-duration discharge, particularly on rural distribution systems, is an important factor in arrester performance and that greater immunity to this type of discharge should result in longer average arrester life. From our examination of a large number of distribution arresters that had been in service from one to eight years, we were impressed with the negligible evidence of moisture or deterioration.

As stated by Mr. Gross, the duty on arresters on rural lines increases with distance from a grounded-neutral source. This is shown by figure 5 of this discussion, which gives the result of some calculations on an actual distribution system, in comparative wave shapes of the arrester discharge currents, assuming a 1,000-ampere continuous current in the stroke.

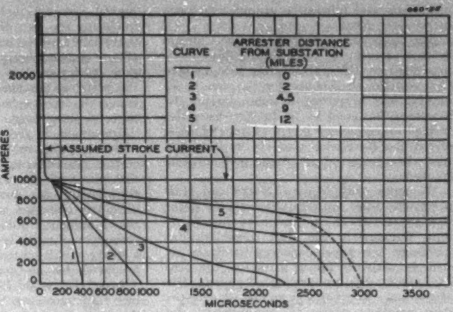

Figure 5. Current discharged through arresters; calculations on actual system

The selection of line-type or station arresters is largely influenced by economics, protected level, and discharge capacity. Line-type arresters cost less. Station arresters provide greater discharge capacity, and they also establish a lower protected level. From papers previously published, there is some indication that on some systems good results are achieved by line-type arresters. We do not believe Mr. Gross' question can be answered categorically with assurance until considerably more data are available.

We are glad that Mr. Smith raised the important question of ground resistance. In the explanation below figure 4 of the paper the following statement is made, "Variation of R_3 through a 10–50 ohm range does not change I_1 appreciably." This is based on the neutral wire forming a metallic path to the substation ground that is in parallel with the arrester grounds. Also, the neutral wire parallels many arrester grounds. Space limitations prevented our presenting the differential equations worked out for various values of R_3. The results for two typical cases are tabulated below:

Case 1. Time for I_1 to Reach 1,000 Amperes

R_3	5	10	50	100
Micro seconds	1,515	1,375	1,320	1,320

Case 2. Current I_1 Versus Time for Various Values of R_3

	Amperes		
R_3 (Ohms)	300 Micro-seconds	500 Micro-seconds	1,000 Micro-seconds
5	325	470	835
10	400	525	845
50	535	650	890
100	560	675	900

For a system where the only ground is at the substation so that there is no metallic connection between the arrester grounds and the substation ground, a higher R_3 will effectively force drainage current to the substation as Mr. Smith states. In this case, a low substation ground resistance R_3 is important because the voltage drop across it will keep I_1 from building up and relieving the arrester. For the connection in figure 4, R_3 is not such a factor because there is a low-time-constant metallic shunt path in parallel with the arrester through the line, substation transformer, and neutral wire in which E_a is acting to increase I_1 and unload the arrester. This question of ground resistance effects is so nebulous at present that it is an open field for much further work.

This rough analysis indicates that to be of value the statistical work must consider the type of system in evaluating the effect of arrester and substation ground resistance on arrester duty.

Mr. Halperin's experience on the Chicago system appears to corroborate the theory presented in the paper. We also believe that multiple strokes account for some cases of damaged arresters, in fact, among the arresters examined by us in the study described in the paper, the appearance of some of the gap electrodes aroused the suspicion that the arresters had discharged multiple strokes.

We agree with Messrs. North and Halperin who have both emphasized the point that protection of equipment against lightning damage is the first consideration. Although the paper concerns itself almost entirely with the life of arresters, the protection aspect is not considered closed. We agree that it is preferable to lose an arrester than another piece of equipment. In writing the paper, however, it appeared to us that with the arresters now available and with careful application, the protection problem is in quite satisfactory condition, and that at the moment the matter of freedom from damage to the arresters themselves was the more important, particularly in distribution systems to which the paper has confined itself.

A large number of laboratory calibrations of the surge marks left on gap electrodes have been made, of which figure 1*a* is typical only. Our calibrations are applicable only to the gap construction used in Autovalve arresters. Although we have not calibrated other types of gaps, we have found that the appearance of the marks is influenced by the material and form of the electrodes and will alter with significant changes in the gap spacing.

As remarked by Mr. North, figures 11 and 12 of our paper are tests made on the new arrester described in the paper. The discharge characteristics of the earlier type of Autovalve distribution arresters are of the same order.

Mr. Bellaschi's discussion lends further weight to the authors' suspicion of long-duration discharges, and we wish to acknowledge that Mr. Bellaschi's well-known researches in the laboratory and his coordination of observed natural phenomena with laboratory results gave us considerable assurance during the writing of our paper.

Enclosed Spark Gaps

Discussion and author's closure of paper 40-61 by Wm. E. Berkey, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 429–33.

L. J. Berberich and **M. E. Bell** (nonmember; both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Berkey has pointed out a very interesting phenomenon in connection with lightning-arrester spark gaps. This was first observed when a rutile spacer was substituted for the usual porcelain spacer. The wide difference between the dielectric constants of rutile and of air leads to large stress concentrations at the edges of the spacer. Radiations are produced in these highly stressed air spaces which aid in initiating the gap discharge, resulting in a materially lowered impulse ratio. The same effects can be observed, but to a lesser degree, with porcelain spacers, particularly if the physical arrangement is such as to increase the stress concentrations at the metal-porcelain boundary.

Inasmuch as the behavior of a material under impulse voltage is determined to a certain degree by its high-frequency properties, the performance of the rutile gap as described by Mr. Berkey is due to the high dielectric constant at high frequency. As the information published on the dielectric properties of rutile is meager, we have made a study of this material. A paper has been prepared by us for publication in the *Journal of Applied Physics*. We will therefore limit ourselves to a brief discussion of the properties most pertinent here.

Rutile is one of three crystalline modifications of titanium dioxide. It has a body-centered tetragonal lattice, being the most dense of the three forms and the one stable at the highest temperature. Figure 1 of this discussion gives the dielectric constants and power factors over a range of frequency from 60 to 10^6 cycles for three specimens prepared from powdered crystals. Specimen 1 contained 98.2 per cent pure rutile and was fired at 1,675 degrees centigrade. Specimen 2 contained the same material with the addition of 5 per cent of a low-dielectric-constant ceramic binder and was fired at 1,330 degrees centigrade. Specimen 3 had the same composition as specimen 1, but was fired at 1,330 degrees centigrade. This shows that high-temperature firing is necessary for obtaining the highest dielectric constant and that a small admixture of low-dielectric-constant material can reduce the dielectric constant markedly. Specimens 2 and 3 are representative of material used by Mr. Berkey as gap spacers. Rutile is remarkable among dielectric materials in that it has a very high dielectric constant extending over a wide range of frequency accompanied by a low power factor. Infrared reflection data are available which show that the high dielectric constant persists to a frequency of about 10^{12} cycles (300 microns).

A qualitative explanation of the high dielectric constant can be made in the light of published work on weakly ionic crystals, assuming that the degree of ionic binding varies with the distance between titanium and oxygen atoms. This results in a disproportionately large transfer of electronic charge for a small shift in the positions of the titanium nuclei relative to those of the oxygen nuclei. This is in agreement with the evidence from infrared data which suggests an atomic polarization of an unusual type.

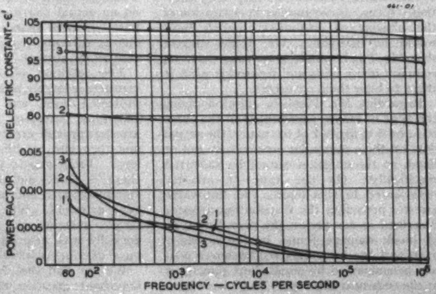

Figure 1. Power factor and dielectric constant versus frequency at 30 degrees centigrade for rutile specimens 1, 2, and 3

Theodore Brownlee (General Electric Company, Pittsfield, Mass.): Our laboratory experience confirms that of Mr. Berkey in that we find the same erratic impulse breakdown and high impulse ratio of gaps not subject to ionization, and much more consistency and lower impulse ratio of gaps in which the electrodes are exposed to ionization. Also we concur with his analysis of the reason for the good impulse performance of mica-spaced gaps and the rutile- or porcelain-spaced gaps of the designs he shows.

However, we disagree with the statement that this factor of ionization hitherto has been unrecognized. This factor has been recognized for many years and we have long used mica-spaced gaps to secure low and consistent impulse breakdown values in low-voltage protective devices which require the lowest possible impulse levels. It also has been recognized for a long time in Mr. Berkey's company. In 1926 an article ("Theory of the Autovalve Arrester," Joseph Slepian, AIEE TRANSACTIONS, volume 45, 1926, pages 169–74) concerning the mechanism of impulse discharge in the Autovalve arrester of that day, read in part as follows:

> "The mica, having a dielectric constant of six to seven, distorts the electrostatic field, very much as if it were conducting. Thus there is a concentration of electrostatic stress at the corners of the mica. . . Hence when a little more than 350 volts is applied, these highly stressed points break down and precipitate the discharge of the whole gap."

We have experimented with another method of reducing the impulse ratio. When porcelain spacers are replaced by similar-shaped spacers of high-resistance material, the impulse ratio is materially lowered. This improvement is attributed to minute ionizing sparks on the inner surface of the resistance gap spacers.

Still another method of securing low impulse ratio has been used in the Thyrite station arresters since 1933. This method operates on a series of gaps in close proximity, rather than on individual gaps discussed previously. The nine gaps in an 11.5-kv unit are resistance shunted. While the resistance values and gap settings are so chosen that none are overstressed at operating voltage, they are designed so that on impulse the gaps break down in a predetermined order starting at one end. The gaps are so arranged that the first gap to spark illuminates the remaining gaps, thereby effectively reducing their time lag.

In addition to this, a low over-all impulse ratio is attained, especially with several units in series, because the resistance shunts are selected to maintain a uniform voltage distribution at power frequency but a nonuniform voltage distribution at the rapid rates of voltage rise associated with impulse breakdowns.

There appear to be several inconsistencies in the data which Mr. Berkey presents. He states that leaded contacts improve the impulse ratio of porcelain-spaced gaps but do not affect rutile-spaced gaps. Comparing the results on rutile-spaced gaps in tables I and II we see that leaded contacts reduced the impulse ratio from 1.12 to 1.07. Also in table I we find leaded contacts improve the impulse ratio of porcelain-spaced gaps from 1.75 to 1.23 while the corresponding change in table II is from 1.72 to 1.36. In comparing average impulse ratios determined from several groups of breakdowns, we have usually found variations in the third significant figure and often the second. This very possibly explains the discrepancies mentioned above.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Berkey's paper describes an important research on factors which influence the impulse breakdown of enclosed spark gaps such as are used in arresters. The work done on rutile and other materials and arrangements explains the better than expected performance of plain porcelain-insulated gaps used for some time in the past in arresters. The preionizing effect is apparently always present. If properly controlled it results in consistent and low impulse ratios. If not properly controlled, the impulse breakdowns may be variable. The understanding of the phenomena gained by this research work also shows ways of controlling and improving it, as described in the paper. These improved methods have been applied in Autovalve arresters. Mention has been made of a type of gap with good impulse ratio in reference 11 of the paper.

A combination of the quenching and the ionization gap illustrated by figures 9 and 10 of the Berkey paper are employed in the new distribution arrester which has been described in the contemporary Bergvall and Beck paper on "Lightning and Lightning Protection on Distribution Systems" (TRANSACTIONS pages 442–9) and illustrated by figure 8 of that paper. Some data on the impulse breakdown performance of these arresters is of interest as indicative of the consistency and low ratio of such gaps.

In these arresters the quenching gap and the ionization gap are used together in series, and the number of gaps of each kind increases with the arrester rating. Mr. Berkey's tests were made on single elements of these gaps. The performance of the combinations is not necessarily proportional to that of the single elements, hence the typical test data given in the following is of interest.

A three-kilovolt arrester tested for breakdown on the front of a test wave rising at the rate of 90 kv per microsecond gave with seven positive impulses applied a minimum breakdown of 20 and a maximum of 21; with seven negative impulses applied a minimum of $18^{1}/_{2}$ and a maximum of 20. The 60-cycle crest breakdown was 18 kv.

Tests on three different nine-kilovolt arresters with a wave of 148 kv per microsecond with six shots on each arrester, gave a minimum breakdown of 47 kv and a maximum of 53 kv. The 60-cycle crest breakdowns were 42 to 46 kv.

The 3,000-volt arrester which had been subjected to a direct stroke on the Cathedral of Learning, when checked for impulse breakdown afterward gave impulse breakdowns of 20 to 22 kv.

Another indication of the presence of preionization as a result of local discharges is the following. A very large number of tests for radio interference have been made under various conditions on distribution arresters incorporating this gap structure. This was an important item in the development program. The tests were made in a standardized test circuit, microvolts of "noise" being measured directly in the arrester circuit. It was found that low microvolt measurements (5–20) were obtained at voltages applied to the arrester which were below the breakdown of the gap.

This might increase as the voltage is raised farther to 60-cycle breakdown. The 60-cycle breakdown voltages are very consistent.

The voltage at which the ionizing current just becomes measurable is well above the rated voltage of the arresters, so that there are large factors of safety against interference with radio reception in the field.

W. E. Berkey: The discussion by Mr. Brownlee has brought up several points.

(1). Concerning the time of recognition of the importance of ionization on spark-gap behavior, the second sentence in the introduction states, "To obtain a low impulse ratio it has long been known that some form of ionizing radiation must be present."[1-4] More than 50 years ago Hertz[2] discovered the effect and importance of ionizing radiation on the breakdown of a spark gap.

(2). The low breakdown effected by the mica spacer in the early Autovalve arrester was discussed with Doctor Slepian. He agreed that the breakdown theory proposed then and the theory of operation given in the present paper are not comparable. In the early Autovalve, breakdown apparently took place over the edge of the mica-ring spacer and quickly spread over the electrodes. The initial breakdown was essentially a surface flashover of insulation. The present paper attributes the observed lowering of impulse breakdown voltage in enclosed spark gaps to ionizing radiation emitted from normally invisible discharges located in the insulator-electrode contact regions. These ionizing radiations are effective in lowering the breakdown of a spark gap at a *distance* away from their source.

(3). The use of resistance spacers greatly reduces the intensity of the effective radiation. This is due to the fact that the usual clay bond used in such resistance spacers has an effective low dielectric constant at equivalent impulse test frequencies. A single resistance-spacer gap without auxiliary surface sparking has a high impulse ratio. However, when many single gaps are stacked in series another important factor appears, namely, the voltage balance over the stack. Resistance spacers are used to give a high 60-cycle breakdown. The impulse breakdown with a stack of resistance-spacer gaps is not much different from that obtained with porcelain-spacer gaps. Thus the lower impulse ratio obtained with resistance spacers is due to an increased 60-cycle breakdown.

(4). The data in table I are the average values obtained with several gaps of the same type tested on many days. In table II are found values obtained with one typical test all made on the same day. In general the day-to-day and gap-to-gap variations are greater than repeated tests made on the same day. The third place in the impulse ratio is significant only when comparing gaps with low impulse ratios or "fast" gaps.

Berberich and Bell have described the extremely interesting and important physical properties of rutile with respect to its application as a spark-gap spacer.

Mr. Beck has given test data on arresters utilizing the self-generated ionizing radiations. Several design and control problems are mentioned. As the intensity of this ionizing radiation depends upon the displacement current in the insulator, one may ask how the impulse ratio varies with the rate of impulse-voltage rise. A rutile-spacer gap was tested for impulse breakdown at 100, 50, 25, and 1 kv per microsecond rate of voltage increase. The impulse ratio was low over the range indicating that while the radiation intensity may be lower at slow voltage rises, the time of overvoltage is relatively long and therefore little change in impulse ratio could be expected. Conversely on fast rises the ionizing radiation intensity is increased which insures the formation of an electron on the cathode surface at the right place and time to insure a short time lag of breakdown.

The CAA-MIT Microwave Instrument Landing System

Discussion of paper 40-44 by E. L. Bowles, W. L. Barrow, W. M. Hall, F. D. Lewis, and D. E. Kerr, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 859–65.

C. Francis Harding: See discussion, this page.

The Development of the Civil Aeronautics Authority Instrument Landing System at Indianapolis

Discussion of paper 40-43 by W. E. Jackson, A. Alford, P. F. Byrne, and H. B. Fischer, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 849–58.

Harry Diamond (National Bureau of Standards, Washington, D. C.): It may be of interest to compare the Civil Aeronautics Authority system at Indianapolis with the system set up by the National Bureau of Standards at Newark about seven years ago.* Many instrument landings were made on the Newark installation and the setup was considered by many pilots as suitable for airline experimental use. The achievements represented by the Indianapolis setup lie primarily in the provision of control of the individual elements of the system.

It is gratifying to note that every one of the modifications indicated to be desirable by the Newark tests have been incorporated in the Indianapolis system. Bends in the runway-beacon course are eliminated by control of the space-radiation pattern. The required course sharpness is obtained by sharper radiation patterns rather than in the course indicator, thereby providing increased course stability. The marker beacons, taken over from the Army system, have means for controlling the radiated patterns to conform with particular installation requirements. Finally, the glide path is made to have a shape desired by pilots and more easily applied to airports having long runways.

The authors of the paper are to be congratulated on the excellent progress made. The provision of means for controlling the shape of the glide path represents, perhaps, the outstanding achievement. By virtue of these means, the constant-intensity glide path becomes definitely superior to the equisignal type. Aside from the greater flexibility of path shape obtainable, there is considerable simplification and economy of equipment. Careful consideration of all of the factors involved will show that the stability of path is equivalent for the two types.

Since the demonstration at Indianapolis, Y. C. Houghton of the Department of Justice and I have devised, on paper, a second arrangement for controlling path shape which may afford some advantage over that employed at Indianapolis or may be used in conjunction with it to simplify the antenna required. It consists of progressively varying the reflecting coefficient of the ground along a strip in front of the radiating antenna system so that the interference between the direct and ground reflected waves produce paths of constant intensity having shapes which are practically linear. For example, the strip may be 30 feet wide and 150 feet long, extending from a point 100 feet in front of the antenna. The reflection coefficient along this strip is dependent upon the value of the factor

$$K' = K - \frac{2j\sigma c^2}{f},$$

where K is the dielectric constant (electrostatic units), σ is the conductivity (electromagnetic units), f is the frequency (cycles per second), and c is the velocity of light (3×10^{10} centimeters per second). To secure a progressive change in the reflection coefficient along the length of the reflecting strip such as to produce the shape of glide path specified as desirable by the Radio Technical Committee for Aeronautics, the factor K' must be made to vary from a value of about 360 at the point nearest to the antenna to a value of about 2 at the point farthest from the antenna. The desired distribution of the factor K' may be accomplished quite simply by a system of variably-spaced copper strips parallel to each other and perpendicular to the length of the reflecting surface combined with the use of a material of low dielectric constant (such as asphalt) for the surface of the reflecting strip. Note that the maximum conductivity required is of the order of 2×10^{-11} electromagnetic units, approximately that of sea water.

C. Francis Harding (Purdue University, Lafayette, Ind.): No presentation nor discussion of papers upon the application of the cathode-ray oscilloscope to the blind landing of airplanes would be complete without giving recognition to that developed by Professors R. H. George and H. J. Heim of the electrical division of the Purdue Research Foundation.

This system differs from those described in that the oscilloscope in the cockpit of the airplane shows, upon approach to the landing field, six distinct bright spots directed from six small short-wave radio transmitters located at the four corners and the midway side locations of the airport or along the runway. These radio impulses are transmitted on a single wavelength in sequence around the field in such rapid succession that the persistence of vision of the pilot provides the impression of a rectangular outline of lights of proper contour to indicate the shape of the airport and the direction of approach of the plane from any point not necessarily on a radio beacon. Such an indication is, of course, available whether the lights of the airport can be seen or not.

These transmitters, very small in size may be located on the borders of the airport in such a manner as to provide no interference with the landing operation, while the receivers in the plane may be made of sufficiently small size and weight as not to be objectionable. Indeed, the George-Heim system is capable of providing the pilot with more information than the glide-path system, although the Civil Aeronautics Authority-Massachusetts Institute of Technology system approaches most closely to the former. By properly locating the transmitters the system developed at Purdue University can be made to supply the three-light pattern for a glide path as described in the papers under discussion. In addition the outline of the runway or the entire airport would be outlined. It would, therefore, be unnecessary to have other supplementary equipment. If six, or even less, transmitters are used along the runway, the pilot should be able to choose his glide path from the perspective of the light spots.

Such a system has been under development for some time at Purdue University and promising results have been obtained with ground tests. Equipment has been constructed for flight tests which will be undertaken this spring.

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* PERFORMANCE TESTS OF RADIO SYSTEM OF LANDING AIDS, H. Diamond. Bureau of Standards *Journal of Research*, volume 11, 1933, pages 463–90; *RP*602.

The Cross-Air-Blast Circuit Breaker

Discussion and authors' closure of paper 40-19 by D. C. Prince, J. A. Henley, and W. K. Rankin, presented at the AIEE winter convention, New York, N. Y., January 22-26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 510-17.

R. J. Wensley: See discussion, page 1127.

H. C. Louis: See discussion, page 1125.

J. B. MacNeill: See discussion, page 1123.

H. W. Haberl: See discussion, page 1129.

C. M. Gilt: See discussion, page 1125.

C. G. Suits (nonmember; General Electric Company, Schenectady, N. Y.): When the great complexity of the physical phenomena in a breaker of this kind is considered, it must be agreed that the authors have done a very satisfactory job of interpreting their measurements. Their discovery that there are regions in the current and voltage range of the breaker where displacement phenomena and cooling phenomena may be identified represents a fundamental advance in our knowledge of the interrupting mechanism. Too often in the past it has been the effort of workers in this field to prove that a single mechanism was active to the exclusion of all others. In view of the findings of this paper it is not surprising that it has been possible to make out a very good case for both a displacement theory and a deionization theory in specific instances. In the present case both are found and their domains are defined. The discussion of this question in appendix *C* of the authors' paper is particularly good.

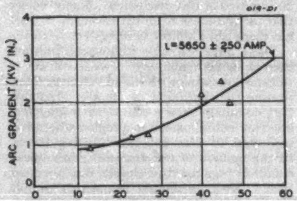


Figure 1. Gradient-pressure, cross-blast breaker

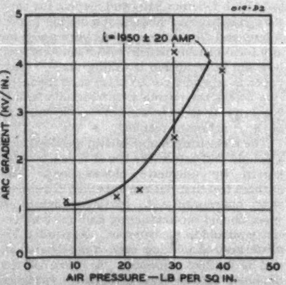


Figure 2. Gradient-pressure, cross-blast breaker

With regard to the question of the ranges of current and voltage in which displacement and cooling are important, it seems to me that the direct experimental evidence of the follow current as observed by the modification of the recovery oscillation provides much more convincing evidence than the physical argument based on the concept of Kesselring and Koppelmann. This concept has no experimental support and probably departs seriously from the detailed phenomena.

The use of impulse breakdown data of a rod gap as an index of the pressure improvement of interrupting ability as measured by the recovery strength characteristic is probably justified, but because the electrode shape factor may be very important in determining this pressure-breakdown curve (see, for example, I. Goldman and B. Wul, *Tech. Phys. U.S.S.R.*, volume 1, 1935, page 497; volume 3, 1936, page 16 and page 519), it would have been better to have taken the pressure-breakdown data on the same electrode structure used in the breaker.

Many questions may be raised concerning the physics of the breakdown between "ionized stumps" described by the authors as part of the displacement process. That the arc column does not cool uniformly, however, can be seen in the photographs, and in a very recent publication by Beams and Snoddy (*Phys. Rev.*, volume 57, 1940, page 63) a direct study of disruptive discharges between "ionized stumps" has been made. These authors used oxygen-gas flames as electrodes and observed a breakdown gradient of eight kv per centimeter over a ten-centimeter path, which is about one-fourth the value for sphere gaps, but is in excellent agreement with the impulse breakdown of the two-centimeter rod gap.

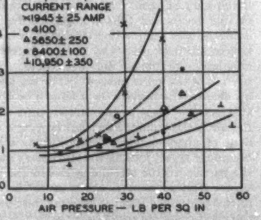


Figure 3. Gradient-pressure, cross-blast breaker

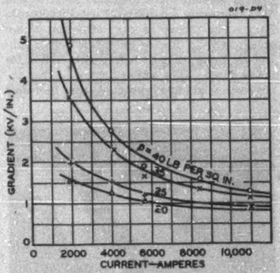


Figure 4. Gradient-current, cross-blast breaker (at indicated tank pressures)

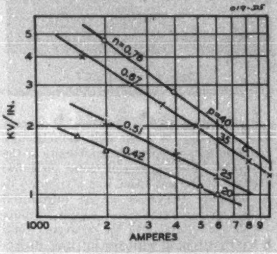


Figure 5. Log-log plots of gradient and current to determine n

It is stated in the paper that "there seems to be a purely random relation between arc drop just prior to clearing," by which I presume the authors meant to imply that the effect of cooling factors is not apparent in the magnitude of the arc drop. A close examination of table I, however, shows this effect very clearly. Figures 1 and 2 of this discussion show the gradient in kilovolts per inch as a function of air pressure in pounds per square inch for the case of the cross-blast breaker at 5,650 amperes and 1,950 amperes. These are the only two cases which have as many as six points for the curve, and unless the experimental error is ±50 per cent, there is definite evidence of an increase in gradient with air pressure. At the other values of current there are shown at most only four experimental points, and in most cases only three, so that for a necessarily large spread these few points do not make a very satisfactory curve. All points of table I, however, do fit in very well with a family of curves of gradient versus air pressure, as shown in figure 3 of this discussion. Considering the necessarily small deflections from which arc voltage was taken, the set of curves is quite convincing. This is just the type of variation in gradient with pressure that one would predict on theoretical grounds. From the set of curves of figure 3 one may plot the gradient-current characteristics as in figure 4 of this discussion, and determine the exponents of the gradient-current relation $E = \text{const } i^{-n}$ from log-log plots in figure 5 of this discussion. These exponents for pressures from 20 to 40 pounds per square inch are 0.42, 0.51, 0.67, and 0.76. In a paper on application of heat transfer data (*Phys. Rev.*, volume 55, 1939, page

1184) this exponent has been calculated for the case of forced convection leading to $n = 0.54$, which is satisfactory agreement when the spread in the experimental points and the assumptions of the theory are considered. That the arc gradient prior to the current zero depends upon the air pressure seems to be a necessary conclusion quite apart from the experimental data. Indeed, it seems difficult to imagine on what else the arc voltage could depend.

It may be argued that no failure tests are shown by table I. However, the curve of figure 5 shows the increase of interrupting ability with pressure, while the plot of figure 3 of this discussion shows the increase in gradient with pressure, so that there can be no doubt that a correlation exists between interrupting ability and arc gradient prior to interruption.

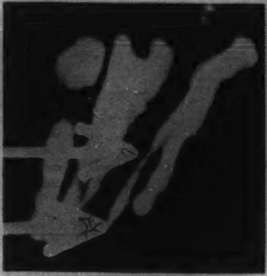


Figure 6. Picture 17

Joseph Slepian (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The theory of the extinction of a-c arcs in rapidly flowing fluids has been a subject of lively debate before the AIEE for many years. The senior author of this paper some years ago presented what he called a displacement theory, in which, at current zero, a wedge of fresh and dielectrically sound fluid interrupted the continuity of the arc path. The recovering total dielectric strength of the arc space was attributed to the growing thickness of the introduced wedge, and an attractively simple formula was derived correlating the recovered total dielectric strength with the intrinsic dielectric strength of the normal fluid and its velocity. Much data was given to support this simple formula.

The present discusser disputed strongly the interpretation of the data offered, and gave many arguments why the simple displacement theory must be invalid. Since these arguments are all printed in past Transactions, it is unnecessary to repeat them here, except perhaps the one incontestable point, that careful examination shows that the pure streaming of a fluid according to accepted hydrodynamical or aerodynamical principles, can in no way lead to the introduction of a wedge into the arc path.

In the present paper, the displacement theory is again offered, with the accompanying attractively simple equation, but there now begins to appear a sign of uncertainty. The displacement theory is now thought perhaps not to be applicable at current zero, but the strong diffusive forces of thermal conduction and deionization are now permitted to account completely for the recovered dielectric strength for as much as ten microseconds. This is most encouraging.

The present discusser has contended that diffusive effects, must in the last analysis be responsible for the arc extinction. Streaming, alone, cannot interrupt the continuity of the arc path, or by itself, change in a large way the temperature or ion density in actual gas volumes in which the arc has been playing. Streaming, however, can make diffusion very much more effective by changing the configuration of the arc. Streaming can make the section of the arc small and its other dimension very long so that cooling and deionization take place very quickly. Particularly, if streaming is accompanied by turbulence, the diffusive effects will be very greatly enhanced.

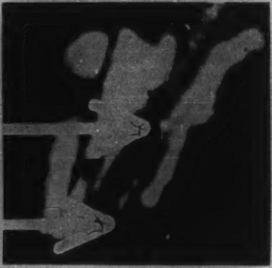


Figure 7. Picture 18

The high-speed pictures presented in this paper are most remarkable, and give just what theorists on arc extinction have been looking for. At last we may see directly what happens within the microseconds in which the arc space recovers its dielectric strength. The evidence which they show seems to me unmistakable. The gas flow places a considerable length of the arc in a position where diffusive effects are particularly strong. This considerable length, near current zero, recovers dielectric strength, apparently rather uniformly along its length.

I have enlarged pictures numbers 17, 18, and 19 of figure 4, of the paper so that more detail may be seen. In picture 17, figure 6 of this discussion, I have indicated by two white arrows the portion of the arc which I believe has been placed by the streaming in a position favorable for diffusive forces. This portion of the arc is pushed by the streaming up against the barrier for a length of about two inches. Next to the barrier, past which the air is streaming, there is, as every student of airplane-wing design knows, a thin boundary layer, joining the stationary gas at the barrier, to the streaming gas in the passage. Under some circumstances the flow in this boundary layer is laminar, but under the conditions obtained here, with the high mean air velocity, and with gas being rapidly evolved from the barrier material, there is no doubt that the boundary layer is quite thick and in an intensely turbulent state. It is precisely into this turbulent boundary layer that two inches of arc is placed.

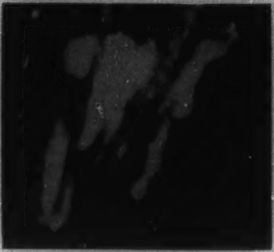


Figure 8. Picture 19

The apparent break in the continuity of the arc at the end of the barrier in picture 17 may be misleading. The lack of motion of this break indicates that it is not real, but the arc continues round the end of the barrier, back from the side of the chute, so that it is out of sight of the camera. Mr. Prince, with whom I discussed this point by letter, has kindly confirmed this statement.

In picture 18 (figure 7 of this discussion) we see that the favorably placed two inches of arc has hardly moved in space, but the intensity of its radiation has greatly decreased. The highly turbulent motion has so accelerated diffusive effects that the arc has been much cooled and deionized in the $16^{2}/_{3}$ microseconds between pictures. In picture 19 (figure 8 of this discussion), finally we see that the radiation from the two inches of arc has diminished almost to zero.

These most valuable high-speed pictures then show no evidence of a wedge of air, of thickness growing with sound velocity, interrupting the continuity of the arc. Instead, they definitely show a considerable length of arc, two inches, placed in a region where it is subjected to very intense diffusive forces. In the opinion of the discusser, they speak definitely against the simple displacement theory, with its related beguilingly simple equation.

D. C. Prince, W. K. Rankin, and J. A. Henley: The authors wish to thank particularly both Doctors Suits and Slepian for their very constructive discussions of this paper. A long and diligent search has been made for any form of tests which would show exactly how arc interruption is taking place. This paper is an endeavor to place such information before the engineering fraternity and it is gratifying to have a check on the validity of the conclusions reached.

The Kesselring-Koppelmann mechanism is a simple and attractive one if it can be supported by adequate observations. The authors feel that data in appendix *C* support that conception. A partially deionized space should be conducting and its conductivity should be measurable. Appendix *A* is a method of making such a measurement and is further evidence. A curve of arc diameter against time constitutes evidence along the same line by still another method. All point to the existence of a very brief time, less than 2 microseconds, during which deionization is taking place. If a sufficient potential is present during that time, some sort of discharge takes place which has been detected for 15 microseconds. In the absence of high rates of rise of recovery voltage, the discharge does not take place and the ionization may be swept away. Later application of voltage produces a disruptive breakdown if it produces any at all. Such a breakdown is characteristic of a clean dielectric.

In the matter of equivalent electrode shapes the wording of the paper must not be quite clear. The authors had a breakdown between "ionized stumps" in mind so that Doctor Suits' reference to Beams and Snoddy is directly confirmatory.

Again in the matter of effect of arc drop, the wording in the paper must not be entirely clear. No question has been raised concerning the interrelation between arc drop and cooling which is shown so beautifully in Doctor Suits' figure 3. The point is that the probability of clearing at any given current zero is not a function of arc drop. It seems that Doctor Slepian has not realized this same point. The authors found that by studying the high-speed photographs it was possible to relate the arc drop as measured by oscillograph with the observed arc length in the photographs. Such analyses add greatly to our knowledge of arcs, but the present investigation was carried only far enough to show that the arc drop which is a measure of intensity of deionization does not seem to be positively correlated with successful operation of the circuit breaker.

In the paper the attempt has been made to marshal all available evidence and the case should not be demolished without consideration of all of it. For instance, it is difficult to explain the 50 per cent increased capacity tests with carbon tetrachloride on any other basis than displacement.

Doctor Slepian feels that he has at last one "incontestable" point. If we understand the definition of incontestable, the authors have been contesting that point for years. They have been searching out all available evidence and trying to secure new and more direct evidence. At the moment, they feel that quite a good case can be made for the flow principle. Even if some readers do not agree with the conclusions of the paper, we hope they will agree that there are still two sides to the argument.

A New 15-Kv Pneumatic Circuit Interrupter

Discussion and authors' closure of paper 40-64 by L. R. Ludwig, H. L. Rawlins, and B. P. Baker, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 528–33.

R. J. Wensley: See discussion, page 1127.

H. W. Haberl: See discussion, page 1129.

R. C. Van Sickle (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): As an associate of the authors, I have been interested in the effect of various recovery voltage rates on the performance of the new pneumatic circuit breaker.

During the development, the breaker was tested with various rates of rise of recovery voltage. One range included circuit rates up to 2,200 volts per microsecond and represented service more severe than is imposed by most service locations. The tests made in this range resulted in arcing times averaging approximately one-half cycle. Inspection indicated that the arc was being extinguished in the first two passages of the chute and that the others were a reserve, not being used.

Because the arc was being extinguished at the first current zero after adequate contact separation was reached, there was no point in testing at lower, and easier, voltage recovery rates.

Consequently, the other range of circuit voltage recovery rates were higher and extended from 2,500 to 4,000 volts per microsecond. These rates were representative of extremely severe service. On part of the tests the arc restruck for an additional half-cycle, during which additional sections of the arc chute came into action, to bring about arc extinction at the next current zero.

Figure 1 of this discussion shows sections of cathode-ray oscillograms made during interruptions of currents having initial a-c components of 12,000 amperes at 13.2 kv. The oscillograms were made on a cathode-ray oscillograph having a film drum rotating at a speed of almost one revolution per cycle. The voltage across the breaker pole-unit was recorded. The heavy horizontal line is the zero line made before the contacts parted. The two curved lines represent parts of cycles of the 60-cycle restored voltage. The arc voltage immediately before current zero, and the recovery transient are recognized. The oscillogram on the left has a recovery voltage transient of 23,200 cycles per second corresponding to a circuit voltage recovery rate of 1,700 volts per microsecond. The oscillogram on the right has two frequencies, one 62,800 cycles per second and the other 32,200 cycles per second. The circuit voltage recovery rate was 4,000 volts per microsecond (2.35 times greater).

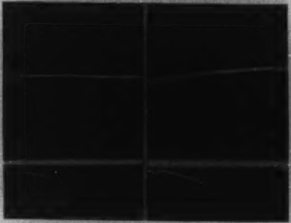


Figure 1

Figure 2 of this discussion shows two interruptions of current having initial a-c components of 20,000 amperes at 13.2 kv but having different recovery-voltage characteristics. The circuit used in obtaining the oscillogram on the left had a natural frequency of 26,500 cycles per second and a circuit recovery voltage rate of 1,920 volts per microsecond. On the right is an oscillogram of a similar circuit having natural frequencies of 32,100 and 85,000 cycles per second and a recovery voltage rate of 3,800 volts per microsecond (double the other).

Figure 2

Figure 3 of this discussion shows two sections of an oscillogram made on a circuit giving a 36,000-ampere a-c component at 13.2 kv. On the left is shown a current zero which occurred after about one quarter cycle of arcing. The arc restruck and cleared a half-cycle later as shown on the right. The transient has two frequencies 33,600 cycles and about 190,000 cycles per second. The restrike occurred after about a half cycle of the higher frequency during which time the recovery rate had averaged about 3,000 volts per microsecond.

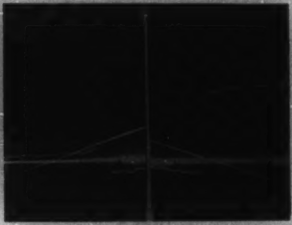


Figure 3

These oscillograms show the interruptions of heavy currents and demonstrate the ability of the breaker to interrupt the circuits with highest rates of rise of recovery voltage with little or no additional arcing.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Here we have for the first time in America a group of papers advocating the use of compressed gas for circuit interruption, with particular reference to high-power indoor equipments.

One naturally asks why have we in this country delayed the introduction of compressed gas and what are the factors which now make its use appear feasible.

In Europe where compressed air has been used for ten years, it is limited to approximately 750,000 to 800,000 kva for 15-kv service and below. This is not enough capacity to justify the introduction of the idea in America, since there are self-contained dry breakers requiring no storage facilities for the lower capacities. For the present we must have 1,500,000 kva and for the future we will need 2,000,000 or 2,500,000 kva at generator voltages. There are certain features of previous designs of compressed-gas interrupter which account for their limited rupturing capacity for indoor equipment. Briefly stated, the principal one is the tendency of the interrupter itself to choke up when subjected to high interrupting duty. To help this situation very high air pressures have been recommended and resistance shunts have been used. These devices, however, are not felt the most desirable solution for American practice.

As to the second question of what now makes compressed gas appear practical here, it is obvious from the group of papers presented that types of interrupter have been found which will handle large powers at those voltages used indoors with moderate gas pressures and with simple mechanical structures that are easily inspected and maintained, fit nicely into metal-clad housings and cell structures, of reasonable inherent cost, and which will provide necessary speed of operation both for synchronizing during closing and for switching speed on opening, and which is also adaptable for reclosing duty cycles certainly without greater derating than now afforded by oil breaker equipment.

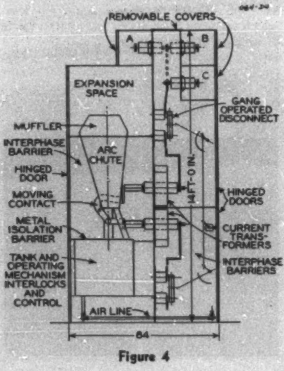


Figure 4

Figures 4 and 5 of this discussion show the device covered in the Ludwig-Rawlins-Baker paper arranged for metal-enclosed cubicle mounting with a capacity of 1,500,000 kva at 15 kv installed in a space no greater than required for many 500,000-kva oil circuit breakers. Several features of this design which make it commercially desirable are apparent from these figures:

1. The high-voltage leads and conductors are arranged well above the floor and make adequate arrangement for current-transformer mounting and provide space for potheads.

2. The moving contact is in full view when the cell door is removed and, when the breaker is open, the surfaces that have been subjected to arcing are visible without removal of parts.

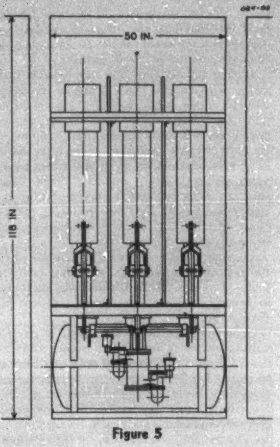


Figure 5

3. The relatively narrow arc chute provides adequate insulation space to intermediate and end walls so that a three-phase circuit can be arranged in 54 inches with 23-kv-class insulation test.

4. The air reservoir and all auxiliary equipment, including pistons, valves, interlocks, secondary control circuits, etc., are mounted in a lower compartment free from high-voltage connections and are readily accessible for inspection and repair.

5. The compressed gas proceeds in a straight-line direction from the tank through the feeder pipe to the arc chute and thence to the muffler without changing direction appreciably and thus giving minimum opportunity for eddy pockets of low dielectric strength during interruption.

6. The final air discharge is in a large grounded space where it can be handled without running up back pressure and is not objectionable to persons in the aisle.

The interrupter shown in the paper, figures 3 and 5, is of a type well adapted to handle high power. While it ordinarily opens the circuit at the end of the first half cycle of arcing, you will note that it has not at this time exhausted its ability, the space between stationary and moving contact being small. As the moving contact withdraws further from the stationary one there is introduced an added length of arc stream and added barriers, together with increased facility for gas disposal so that interrupting capacity is being continuously increased until the contact leaves the arc chute. These are the features which assure the high interrupting capacity necessary on large American systems.

Compressed gases such as those discussed here naturally have a flat arcing-time characteristic and in this way differ from previous conventional circuit breakers, both of the oil and air types. Figure 6, curve *A*, shows the arcing time versus current characteristic of a 15-kv oil circuit breaker. Note that the time required to handle light currents is considerably greater than to handle those heavier currents near rating, but that as the limit of the interrupting device is reached the arcing time again turns upward. Curve *B* shows the characteristic of a compressed-gas device which is flat up to the point where the interrupter becomes overloaded, where it in turn turns upward. For all types of device this final upturn of the curve should be beyond its rating.

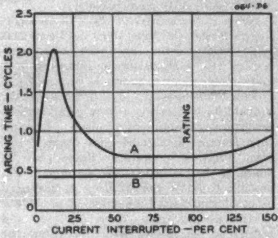


Figure 6

Considerable work has been done looking to standardized capacities for oilless interrupters and table I of this discussion shows the suggestion now being discussed between committees of manufacturers and users covering indoor equipment up to 34 kv. It will be noted that generally this suggestion lies within the framework of existing oil circuit breaker standards but there are fundamental differences which make some departures from existing standards desirable. The general reaction to date is that a standard at this time should be tentative in character. However, some of the more im-

Table I. A Suggested Schedule of Preferred Circuit-Breaker Ratings; Indoor Oilless Breakers

Nominal System Voltage	Circuit Breaker Rated Voltage	Rated Amperes	Interrupting Rating (Kva)	Interrupting Rating Total Amperes
2,300	2,500	600 to 2,000	100,000	23,000
	2,500	600	150,000	29,000
		1,200 to 2,000	150,000	35,000
5,000	5,000	600 to 2,000	100,000	11,500
	5,000	600 to 2,000	150,000	17,500
		3,000	250,000	30,000
13,200	15,000	600 to 2,000	250,000	10,000
	15,000	600 to 2,000	500,000	20,000
	15,000	1,200 to 3,000	1,000,000	40,000
	15,000	1,200 to 4,000	1,500,000	60,000
22,000	23,000	600 to 2,000	500,000	12,500
33,000	34,500	1,200 to 2,000	1,000,000	17,000
	34,500	1,200 to 3,000	1,500,000	25,000
	34,500	2,000 to 3,000	2,500,000	42,000

portant units which will go into general manufacture first appear well founded in station practice and standardization of their characteristics can be made with relatively little risk. As soon as practical such a standard should include desirable gas pressure both at the utilization device and in the main storage. At the present time it appears that 150 pounds utilization pressure will go a long way toward covering the indoor American switchgear field.

The authors of these papers and their associates are to be congratulated on the excellent development work done in the past year on this subject, which results in the practical assurance that compressed-gas interrupters can assume an important place in the American indoor switchgear field, and which makes possible the fine group of papers presented here.

C. M. Gilt (Consolidated Edison Company of New York, Inc., New York): To find extensive application, the various types of air breakers described must compete with oil breakers which, after many years of development and operating experience, may be relied upon with a high degree of confidence to perform successfully within their design limitations. The more recent addition of devices which, by controlling the oil and gas stream, control and shorten the arc, has given faster and more dependable arc interruption. This has been done so well that oil and its gases, while inflammable, have been the cause of extremely few fires and oil has been retained in breakers because of its excellent insulation qualities. However, the desirability of eliminating or at least reducing the amount of combustible material in stations is obvious, provided it can be done without too great a cost in other risks, or in dollars.

Laboratory tests and experience so far indicate that short-circuit arcs can be successfully extinguished by a properly controlled noninflammable gas with suitable arc chutes and cooling or deionizing devices. Experience and further investigations will undoubtedly modify the detailed mechanics of doing the job and, it is to be hoped with some simplification of the parts. It is unfortunate that some of the principles used in the self-contained equipment, such as the magnetic blast, do not offer promise of extension to the higher interrupting capacities and voltages.

The air blast obtained from a separate air tank appears to be very effective and tests which I have witnessed give very little outward or subsequent signs of any distress in opening short circuits of any magnitude within the breaker's rating. The high speed of operation is of course helpful, not only to the breaker but to the system. The separate air tanks and compressors are somewhat of a handicap, though not necessarily prohibitive. The proper operation requires an extremely close co-ordination and timing of the blast of air and the parting of the contacts. It is to be hoped that future development will arrive at a design in which this is co-ordinated in one mechanical operation rather than by mechanically or electrically interlocked valves and trips which, by a failure, may let the contacts part without an adequate blast at the time of parting. In at least one European design the operation of the mechanism produces its own blast of air by means of a piston, but it is reported that it is impractical to develop sufficient air volume and pressure by this means to be adequate for large interrupting ratings and high voltages. However, some design which combines these two operations in one mechanism is highly desirable.

The Consolidated Edison Company has installed two 15-kv 500,000-kva breakers of the air-blast type, one of which is carrying load and has opened one short-circuit of about 250,000 kva entirely satisfactorily. The company has on order some 5,000-volt 150,000-kva magnetic-blast breakers of two different manufacturers' design for generating-station auxiliary supply. However, it was decided to install oil breakers at individual motors in a boiler house using pulverized coal because of the dust problem. Experience with air-break switches of these designs in conditions of dust may confirm or disapprove the wisdom of this decision.

H. C. Louis (Consolidated Gas Electric Light and Power Company of Baltimore, Md.): The results shown in all of the papers presented justify the general conclusion that high as well as low-capacity circuit interruption can be successfully accomplished by various types of air circuit breakers. It is, however, desired to bring up certain questions of practical interest to operating men.

Normally, arcs in such switches are drawn internally without danger of external flashover or spreading to other apparatus. Abnormal conditions resulting from improper adjustments or defective materials are, however, possible. What consideration has been given in the design of such switches and related station layouts to guard against and minimize the hazardous effects of external arcing under the abnormal conditions mentioned?

L. R. Ludwig, H. L. Rawlins, and **B. P. Baker:** The authors were glad to receive the numerous discussions, indicating a considerable interest in air circuit breakers of relatively high interrupting capacity.

Mr. Wensley has brought out the importance of the blast valve in the design of breakers of this type. Positive operation of this element of the breaker is of paramount importance and for this reason we have utilized a mechanically operated blast valve which not only insures the supply of air for interruption because of the mechanical interconnection with the contact motion, but provides a high and positive force for actuating these valves.

Mr. Louis has raised the question of retaining the arc within the interrupting structure in case of faulty operation. Figure 7 of this discussion shows a $1^1/_2$ million-kva compressed-air breaker installed in a metal-clad housing. The provision of interphase barriers and the extension of the arc chambers to enclose partially the moving contact arm are design features incorporated with Mr. Louis' requirements for the purpose of keeping the arc enclosed within the phase structure in the event of faulty operation.

Mr. Gilt expressed the hope that a design will be arrived at in which there is close coordination between timing of the blast of air and parting of the contacts. The authors are in strong agreement and for this reason have described a compressed-air circuit breaker in which parting of the breaker contacts and opening of the blast valve are operated from the motion of the piston directly. With this construction, no mechanical or electrical interlocking is necessary.

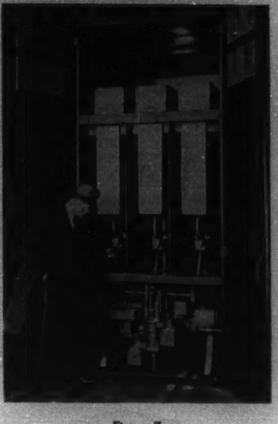


Figure 7

Magnetic "De-ion" Air Breaker for 2,500–5,000 Volts

Discussion and authors' closure of paper 40-65 by L. R. Ludwig and R. H. Nau, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 518–22.

H. C. Louis: See discussion, this page.

C. M. Gilt: See discussion, this page.

H. W. Haberl: See discussion, page 1129.

M. H. Hobbs (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Messrs. Ludwig and Nau's paper on the magnetic De-ion breaker presents the most recent chapter in the contribution of the De-ion air circuit breaker principle to the art of circuit interruption. Its 150,000-kva rating at 2,500–5,000 volts covers a number of industrial, distribution, and station-auxiliary circuit applications.

One of the first questions asked about a new breaker is "How will it work out in metal-enclosed switchgear?" For this reason the development of the breaker and metal-clad structure were carried on in parallel to produce a co-ordinated design of assembled unit. The breaker design has been directed in such a way as to facilitate its metal-clad application. In the same way, in the switchgear development, there has always been kept in mind the fact that the primary duty of the assembly is the best possible arc interrupting performance of the breaker.

Figure 1 of this discussion shows the complete removable element which carries the circuit breaker, the primary and secondary disconnecting devices and levering mechanism for removing and inserting the element into the housing. The assembly is as compact and simple as convenient and reliable operation will permit. It will be noted that the withdrawal movement is horizontal for greater convenience, and to permit the location of the breaker arc chute in position to promote the greatest interrupting efficiency. This is not simply a matter of opinion, but is the result of tests actually made in various positions to determine the best arrangement.

After withdrawal of the breaker unit, removal of the barrier assembly for inspection and maintenance is both quick and easy as figure 2 shows. This is arranged to be done without bolt removal from the barrier assembly.

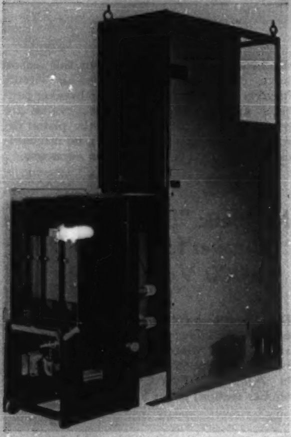


Figure 1

The housing which receives the removable element contains the stationary disconnecting contacts, safety shutters, busses, connections, instrument transformers, and such panel-mounted equipment as may be required. There are no exposed live parts in the compartments except potential transformer fuses, and each housing is isolated from its neighbor by a tight combination metal and Micarta barrier through which the busses pass.

Ventilation for temperature control and to carry off the products of arc interruption is provided at the top of the housing. The outside dimensions of the unit are width 26 inches, height 80 inches, depth 72 inches.

Proof of the breaker performance is incomplete unless it includes interrupting and momentary high current tests when in normal operating position in its metal-clad housing. This is also a check on the adequacy of the disconnecting contacts, insulating supports, and bus assembly design in the switchgear unit as well.

Figure 2

Supplementing the tests on the separate breaker, on which oscillographs were shown by Mr. Nau, the following were made on the breaker and the assembled metal-clad unit shown in figure 1:

57 at low currents of 9 to 1,000 amperes
66 at currents equivalent to 120,000 to 235,000 kva consisting of:
  28 opening only
  26 close-open
   6 standard duty cycles of two interruptions each, 15 seconds between the two
25 close-open with nine-second intervals on 1,500 amperes
25 close-open with nine-second intervals on 9.000 amperes

173 total

The series of tests at nine-second intervals was made to simulate the duty the breaker would have to perform if it were used for motor starting, although this is undoubtedly more severe than would be met with in usual practice except for "inching" a motor in steel-mill service.

The entire series of tests was made without maintenance work being done on the breaker or the disconnecting contacts in the housing, and the equipment was still in such condition that it showed only one degree higher temperature rise on the contacts carrying full rated current than before the tests.

Table I. Short-Time Tests

Five-second test—initial current	20,000 amperes
3 seconds	55,000 amperes
8 seconds	52,000 amperes
One-second test—initial current	20,000 amperes
2 seconds	55,300 amperes
2.7 seconds	73,500 amperes

At the end of the 1,500-ampere shots there was no evidence of gas accumulation when the breaker was withdrawn. After the series of tests at 9,000 amperes there was some smoke evident. However, this was insufficient to interfere in any way with successful interruption, and the structure withstood a one-minute potential test of over 30,000 volts without any cleaning of the insulation surfaces.

The five-second and momentary tests at the currents given showed no damage either to the circuit breaker contacts, the disconnecting contacts, or the bus structure. Normal ratings of this breaker are 50,000—five seconds and 60,000—one second so that these ratings have been adequately proved.

E. W. Boehne (General Electric Company, Philadelphia, Pa.): Messrs. Ludwig and Nau have presented a very interesting paper. With the more common use of the magnetic air breakers in the field of power interruption at the higher voltages, considerable importance should be placed upon the correct application of fundamental relations. The authors have implied that the energy dissipated in the arc increases with the arc voltage. Arc energy is the product of the arc voltage, arc current, and duration. Hence it would be just as correct, if it were correct, to imply that arc energy increases with arc current. The true relations of these quantities may be clarified from figure 3 of this discussion.

Here is shown a rheostat in place of a breaker in an inductive circuit. As the handle is turned inserting resistance to interrupt the current flow, three important changes take place. (1) The current is reduced, its locus following the circle diagram as indicated. (2) The phase angle of the circuit shifts from zero power toward unity power factor; and (3) the arc voltage builds up, its magnitude being shown by the circle at the top. Now watts is zero at maximum current and likewise zero at zero current. The wattage is maximum at 45 degrees phase angle when the arc resistance in ohms is equal to the external reactance of the circuit in ohms. Note at this point the current is reduced only 30 per cent or to 70 per cent of its maximum value.

This approach to the analysis of available oscillographic data in definable, measurable terms, familiar to the industry was the keynote of a paper "Magne-Blast Air Circuit Breaker for 5,000-Volt Service" presented at the 1939 summer convention (AIEE Transactions, volume 59, 1940, pages 202–08). Here it was shown that by magnetically controlling the arc resistance in a nonmetallic chamber that the current-limiting action would throw the current well over the wattage hump in a half cycle or less.

The distinct advantage of accomplishing this unique characteristic is the unity power factor, slow, sinusoidal recovery voltage as shown in figure 4 of this discussion of the interruption of 33,000 amperes at 3,600 volts, single phase. Note the low instantaneous voltage at interruption, the low current in the arc, the short arc duration, all of which result in quiet, easy performance. This test was repeated immediately as shown in figure 5, consistently producing arc-current crests far below the 70 per cent value. The arc chute employed to accomplish these desirable characteristics is shown in figures 9 and 10 of the above-mentioned paper.

The advantage of large phase-angle swing producing unity-power-factor interruptions lies in the fact that breakers are not affected by the severity of circuit to which they are applied. Figure 2 of the above-mentioned paper shows the three available recovery

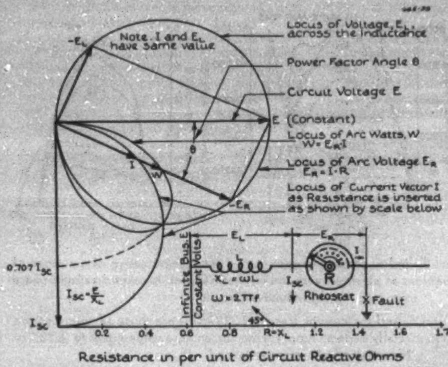


Figure 3. Circle diagram showing the inseparable relations between phase shifting, current reduction, watts, arc voltage, and initial recovery voltage as arc resistance is inserted into an inductive circuit

Note: (1) Maximum watts occurs at 45 degrees phase angle when the current is only 70 per cent of its zero power factor value; (2) the instantaneous value of recovery voltage will always be less than E_L because of the additional phase shift which takes place during the interval between current crest and current zero. Note that E_L and I are equal in magnitude although both do not exist simultaneously in the static diagram

rate characteristics of our high-power testing station in comparison with the low recovery rates obtained on Magne-Blast breakers, showing that Magne-Blast performance is independent of circuit severity. Such a characteristic eliminates all worry of applications to high-recovery-rate circuits. Throughout these tests thermocouples were imbedded in the chute and temperature rises of 2.5 degrees centigrade were recorded per each 100,000-kva interruption. These measurements confirm the many observations made by the well-known touch system. The Magne-Blast breaker was one of two breakers demonstrated to a notable audience last August 17, several months after the first field installations. Here, after a successful series of tests up to 40 per cent in excess of its rating, the breaker was opened for inspection. Its coolness was the subject of admiration.

Figure 4. Single-phase interruption of 33,000 amperes at 3,600 volts by one chute of 100,000 kva Magne-Blast breaker

Note the low arc current which is well below the 70 per cent value of maximum wattage indicated by figure 3 of this discussion. Also observe that the instantaneous value of recovery voltage in per cent is less than the maximum per cent of peak arc current as predicted by a study of figure 3

We conclude by stating that the authors' repeated reference to high arc energy must be founded upon their own experience. We have not encountered it—this being confirmed by simple calculations properly locating the decimal point.

Figure 5. This single-phase interruption of 32,000 amperes at 3,600 volts was taken immediately after the test of figure 4. Note the low arc current and the voltage recovering on a unity-power-factor basis

R. J. Wensley (I-T-E Circuit Breaker Company, Philadelphia, Pa.): The company which I represent has been in business for more than 50 years. During all of that time their slogan has been "air circuit breakers only."

We are very much gratified that the industry is showing signs of agreement with our policy.

Referring to the papers on air-blast circuit breakers, it is interesting to note that the authors have avoided the one feature that our experience has shown to be one of the most important in the design of air-blast circuit breakers. This is the blast valve. Our investigations convince us that an arc of any magnitude drawn in any sort of arc chute under practically any conditions can be extinguished if enough air is moved at a sufficiently high rate through the chute. The best form of chute for the most effective utilization of air will undoubtedly develop as the art progresses. Our own experience has indicated that the cross-blast breaker is better than the longitudinal blast. In our opinion, the blast valve should be made as simple as possible with no sliding parts which might stick at inopportune moments due to corrosion or dirt.

I would like to ask the authors of the paper on the magnetic De-ion circuit breaker as to the nature of the blast which they report as occurring in the arc chute of their breaker. They describe it as an electron bombardment of gas particles. Is this not a technical description of a very simple phenomena which could be illustrated by lighting a match? Our experience indicates that the large movement of gas is produced by the thermal expansion of gases in the arc chute coupled with the actual movement of the arc up through the barriers.

I would also like to ask the authors of the paper if the transverse barriers which they describe as being non-gas-forming are really of non-gas-forming material, or is the term relative? In other words, is it simply to distinguish from a free-gassing material such as boric acid, or is it actually a new material not hitherto available to the industry?

R. H. Nau: The lively discussions show that both the manufacturers and operating engineers are keenly interested in the development and possibilities of air circuit breakers of relatively high interrupting capacity.

R. J. Wensley, of the I-T-E Circuit Breaker Company, suggests "thermal expansion of gases in the arc chute coupled with (or associated with) the actual movement of the arc up through the barriers (that is, arc-shield and arc-chamber sides)" as an explanation for the gas-blast characteristic in arc interruption by the magnetic De-ion circuit breaker. It has been demonstrated through testing that the thermal-expansion theory does not hold by simply cutting the magnet coil out of the circuit, yet leaving the panel-end horn (see figure 1 of the paper) connected to the upper stud. Further consideration points to the fact that there should be no reason for suspecting thermal expansion as the cause of the characteristic gas blast. In less than 100 microseconds after the arc is drawn (position 1, figure 1 of the paper) it has looped to the region as indicated by position 4. In this region the effect of the current loop to produce movement is relatively small. Here the effect of thermal expansion is to blow highly ionized gases back down upon the contacts and permit the arc to play unimpelled in the space enclosed by the arc shield and arc chamber sides.

However, it might be pointed out that an interrupter can be designed to give quite effective operation wherein thermal expansion does become a factor in aiding an interrupting blast.[1]

Mr. Wensley asks further concerning the gas-forming properties of the spaced insulation plates of the arc chamber. As compared to free-gassing materials such as boric acid or fiber, the plates are truly nongas forming. The material is of a high-grade refractory ceramic which has been specially developed by the Westinghouse company. Even when subjected to temperatures of the magnitude found in a 60,000-ampere rms arc, only minute quantities of gases are evolved from this refractory ceramic.

M. H. Hobbs, of the Westinghouse company, commends the magnetic De-ion circuit breaker for its desirability in modern metal-clad switchgear applications. Particular reference is given to the 173 (total) tests made on the breaker as an assembled metal-clad unit shown in figure 1 of Mr. Hobbs' discussion. These tests demonstrate the high efficacy of the magnetic De-ion circuit breaker to function at and above 150,000 kva.

H. C. Louis, Consolidated Gas Electric Light and Power Company of Baltimore, raises a question also asked by many of the operating engineers—"What consideration has been given in the design of such switches and related station layouts to guard against and minimize the hazardous effects of external arcing under abnormal conditions (failure to interrupt circuit)?" During the early development period the magnetic De-ion breaker, without an enclosure, was tested to destruction. The laboratory back-up breakers were blocked closed, permitting the arcing to go rampant for more than a minute before the excitation was removed from the generator supplying the circuit. Although the contact parts and stud blocks were left in a devastated condition, the arc was kept enclosed within the arc chamber. If the breaker had been enclosed in a cubicle such as was discussed by Mr. Hobbs, the breaker would have been completely isolated by the structure for a considerable period of time.

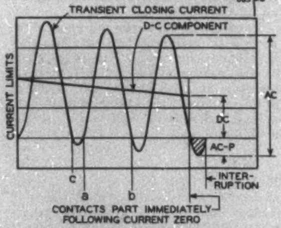


Figure 6. Oscillogram of an asymmetrical current wave interrupted on the small current loop

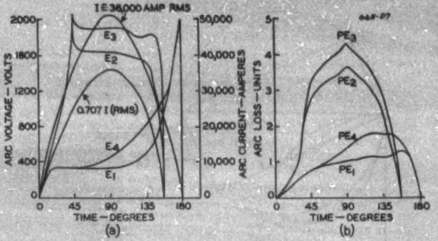
AC—Symmetrical a-c component of current interrupted, 36,000 amperes rms

DC—D-c component of current interrupted, 34,800 amperes rms

AC-P—Current value during arcing period, 11,200 amperes rms (unsuppressed)

The rms value of *AC* and *DC* is 50,300 amperes

Figure 7. Oscillograms of current interrupted and corresponding arc-loss curves



(a)—The arc voltage traces E_1 and E_4 and the current trace *I* are representative of a number of 36,000-ampere rms interruptions as obtained with the magnetic De-ion breaker on single-phase tests, interrupting inductive circuits

The arc voltage traces E_3 and E_2 and the current trace 0.707*I*(RMS) are those of interruptions wherein the arc voltage was specially made to suppress the current to a value equal to 0.707 of the unimpeded current as trace *I*

(b)—Arc-loss curves for corresponding currents and arc voltages of figure 7*a*

Mr. Boehne and his associates have prepared an elaborate treatise[2] on the theory of interruption of 2,500 to 5,000-volt circuits capable of producing fault currents equivalent to 150,000 kva. The central theme of Mr. Boehne's discussion follows that of his paper, and there are certain fundamental factors or relationships to which he should give further consideration. In fact, practicing electrical engineers would be greatly perturbed to find now that their beautiful tools—Heaviside's operational calculus, symmetrical components, tensor analysis, differential equations, complex functions, and other operational means—for analyzing circuit phenomena in transient states were to be supplanted by such elementary steady-state relations of an a-c circuit as expressed by the circle diagram (figure 3 of Mr. Boehne's discussion). The circle diagram, of course, does express notable relationships within a circuit under steady-state conditions.

Fundamental expressions for instantaneous and transient circuit conditions were used to check the oscillographic data given in the following paragraphs. The actual data demonstrate that arc energies of at least two to three times the values obtained with a low-arc-voltage device are encountered when the arc voltage characteristic is of such nature and magnitude as to suppress the arc current of the circuit to 70 per cent or less of the rms value of a practically sinusoidal half-wave of current.

When the current of a given circuit is not noticeably suppressed by the arc during current interruption, the value of the current interrupted is usually obtained by measuring the first cycle of arc current from the current-wave envelope. However, in the event of half-cycle arcing under asymmetrical conditions, the last half-cycle of current may be either of much smaller or much larger value than the average. Accordingly, particularly difficult or unusually easy interruptions may be obtained with asymmetrical currents. These conditions are clearly illustrated in figure 6 of this closure. The a-c symmetrical current measures 36,000 amperes rms, and the d-c component 34,800 amperes rms. The rms value of the asymmetrical current interrupted is then 50,300 amperes rms, in accordance with AIEE Standards No. 19—appendix. Practically, the current interrupted is the rms value of the shaded half-cycle of current *AC-P*, which measures only 11,200 amperes rms. Had the breaker contacts parted immediately following the current zero at *a*, figure 6, and the interruption been completed at *b* the current interrupted would within reasonable accuracy be that of the half-cycle *a-b*. The relative areas under the wave *a* to *c* and under the wave *a* to *b* give an indication of the accuracy of using one instead of two half-cycles in calculating the arc current.

In the paper and in this discussion the AIEE standards have been used to measure interrupted currents. However, relatively easy tests as given by a small last half-cycle of arcing have been excluded from all data. Note has been taken of the fact that by means of rather simple test circuits synchronous closing may be utilized so as to obtain repeatedly easy asymmetrical current interruptions by the test breaker.

Several sets of oscillographic data have been superimposed on one co-ordinate axis as shown in figure 7*a* of this discussion. The arc voltage traces E_1 and E_4 and the current trace *I* are representative of a number of 36,000-ampere-rms single-phase interruptions obtained with the magnetic De-ion breaker on an inductive circuit. The arc voltage traces E_3 and E_2 and the current trace 0.707*I* are those of interruptions where the arc voltage was specifically made to suppress the current to a value equal to 0.707 times the unsuppressed current of *I* amperes rms. The corresponding traces of instantaneous arc power loss are shown in figure 7*b*, being obtained by multiplying the arc voltage and arc current point-by-point. The areas beneath these curves are directly proportional to the respective arc energies dissipated. Thus, it is shown that to suppress current by increasing the arc voltage actually does cause a considerable increase in energy dissipation.

Mr. Boehne assumes in his analysis an rms voltage drop across a resistor through which an rms current is flowing, and calculates the wattage loss as the product of these factors. The arc energy is then presumably equal to this rate of loss multiplied by the arcing time. However, since the arc voltage is far from sinusoidal, it appears that the only reasonable method of obtaining the actual arc energy for the purpose of comparing interrupting devices is to integrate the product of the instantaneous voltage and current over the arcing period. This procedure fully confirms the claims made in the paper regarding the advantage of low arc voltage in interrupting devices.

Inasmuch as Mr. Boehne's discussion is a résumé of the paper, "Magne-Blast Air Circuit Breaker for 5,000-Volt Service",[2] I refer the reader to a discussion submitted by R. C. Dickinson of the Westinghouse company, on that paper [AIEE TRANSACTIONS, volume 59, 1940 (April section) page 210].

REFERENCES

1. A NEW 15-KV PNEUMATIC CIRCUIT BREAKER, L. R. Ludwig, H. L. Rawlins, and B. P. Baker. AIEE TRANSACTIONS, volume 59, 1940 (September section), pages 528–33.

2. MAGNE-BLAST AIR CIRCUIT BREAKER FOR 5,000-VOLT SERVICE, E. W. Boehne and L. J. Linde. AIEE TRANSACTIONS, volume 59, 1940 (April section) pages 202–08.

Circuit Interruption by Air Blast

Discussion and authors' closure of paper 40-66 by W. S. Edsall and S. R. Stubbs, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 503–09.

R. J. Wensley: See discussion, page 1127.

H. C. Louis: See discussion, page 1125.

C. M. Gilt: See discussion, page 1125.

J. B. MacNeill: See discussion, page 1123.

D. C. Prince (General Electric Company, Philadelphia, Pa.): The very interesting paper which Messrs. Edsall and Stubbs have presented should provide an opportunity for comparing the practice and performance of Continental-type air-blast circuit breakers with the practice and performance of those being developed in this country.

For instance, it should be possible to compare figure 9 of the Edsall paper with figure 2*A* of the paper, "The Cross-Air-Blast Circuit Breaker" by Messrs. Prince, Henley, and Rankin (TRANSACTIONS pages 510–17). Also it should be possible to compare figure 8 of the Edsall paper with figure 2*B* of the Prince paper. Such comparison should show whether in the cross-air-blast paper a fair comparison has been made between the axial-blast and the cross-blast designs. Unfortunately this cannot be done because of the lack of specific information in connection with the foregoing figures 8 and 9 of the Edsall paper. In figure 8, port diameter and recovery rate are absent, while in figure 9, pressure and recovery rates are both absent.

The data in the cross-blast paper, which might easily be considered as biased and giving an unfair comparison between the American and European developments, could be quickly evaluated were the additional information available.

H. W. Haberl (Montreal Light, Heat, and Power Consolidated, Montreal, Canada): The subject of arc interruption by air blast has always been of great interest to operating companies in Canada, where temperature changes are in excess of those experienced in the south, and where extreme cold has a tendency to congeal oil circuit breakers and perhaps slow their operation or decrease their interrupting rating.

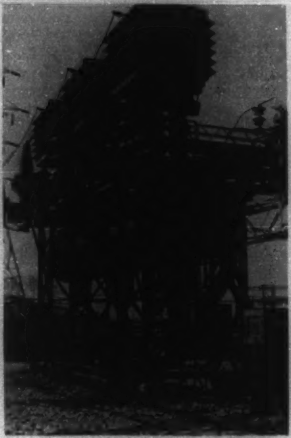


Figure 1

Our company has been interested and doing experimental work on air blast breakers since 1926. Our first commercial installation was made in 1934. This breaker was installed at a new hydroelectric power house, the switch yard of which is adjacent to the tail race, where during very cold weather a mist deposits frost, etc., on the switch-yard apparatus.

Figure 1 of this discussion shows this breaker installed at the power house, and figure 2 shows a general view of the switch yard with the three breakers installed.

Figures 3 and 4 show a general outline of the 1936 breaker, and at the present time a fourth breaker is in the process of installation; the outline of this new switch is similar to those of 1936.

The power-house superintendent, in a paper given before the Canadian Electrical Association, in June 1938, gave data on the operation of these breakers. He brought out the necessity of drying the air before storage in the breaker tank (the original installation only included one drying mechanism). At the present time there are twin dryers installed, one dryer is being re-activated while the second dryer is in service. The other points reported by the operating superintendent were the necessity for having the moving parts free-operating without packing glands in this outdoor installation. This was remedied by the removal of all nonmetallic packing in the operating cylinder and the use of annular rings in their stead.

The report also covered the fact that there was no maintenance work required or done on any of the current-carrying parts, and the contacts have neither been cleaned nor filed in any way, although these breakers have interrupted numerous short circuits on the transmission lines protected.

The newer breakers in service have a clearing time of 3 cycles, which includes relay time. The breakers themselves operate in from 2 to $2^1/_2$ cycles. Our company is quite satisfied that air-blast circuit breakers have decided advantages over other means of circuit interruption, which points have been covered so well by the papers presented on the subject.

W. S. Edsall and **S. R. Stubbs:** D. C. Prince has stated that the data presented in the Edsall and Stubbs paper should provide an opportunity for comparison between the practice and performance of Continental-

Figure 2

type air-blast circuit breakers with that of air-blast circuit breakers being developed in this country. It should be borne in mind in this discussion, that the device described in the Edsall paper is not a Continental design, but an American development, based on a particular Continental design, in which certain factors have been introduced which have so increased the interrupting efficiency as to make it quite acceptable to American power users.

Mr. Prince refers to the possibility of comparison between figure 2*A* of the paper "The Cross-Air-Blast Circuit Breaker" and figure 9 of the paper "Circuit Interruption by Air Blast." In this connection, it should be noted, in plotting figure 9 of the Edsall paper, any operation having more than one-half cycle of arcing was regarded as inefficient. The resultant nozzle area-kilovolt-ampere characteristic, drawn through the points of maximum kilovolt-ampere interrupted with an arcing time of one-half cycle or less, approximated a straight line. If this procedure is followed in the case of the points plotted on figure 2*A* of the Prince, Rankin, and Henley paper, an exponential curve is obtained (for nozzle diameter versus kilovolt-ampere). It is interesting to note that if these data are plotted on the same nozzle area—kilovolt-ampere bases as for figure 9 of the Edsall paper, the three points obtained, for areas equivalent to diameters of $^5/_8$, $^7/_8$, and $1^1/_4$ inches, are substantially in a straight line.

Figure 2*C* of the Prince paper shows a maximum a-c component of arc current of 28,000 amperes for the radial-type nozzle at zero rate of rise of recovery voltage. As stated in Mr. Nye's discussion, this figure was presumably prepared from data obtained from tests made on a European breaker whose design differs greatly from the American design described in the Edsall paper. In the way of emphasis on this point, it is interesting to note that the breaker described in the Edsall paper has interrupted an a-c component of arc current of 36,500 amperes with the arc-paralleling resistors disconnected and with an arcing time of less than one-half cycle. The rate of rise of recovery voltage was 306 volts per microsecond; nozzle diameter $1^3/_8$ inches; and air pressure 200 pounds per square inch. The equivalent value for the breaker, upon which figure 2*C* of the Prince paper is based, would apparently be of the order of 16,000 amperes.

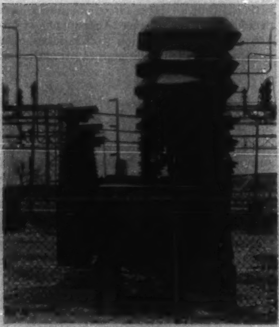


Figure 3

Figure 4

With further reference to the comparison between the European radial-nozzle type breaker and the American cross-blast design, it is noted that Mr. Prince, in transferring the characteristic from his figure 2*C* to his figure 9, seeks to compare a European breaker characteristic, based on half cycle maximum arcing time, with an American breaker characteristic based on one cycle maximum arcing time; a comparison which does not appear to be strictly relevant.

J. B. MacNeill has made reference to the interrupting-capacity limitations of some European designs of air-blast circuit breakers and to the use of high air pressures and resistance shunts. The 500,000-kva 15-kv circuit breaker, with a $1^3/_8$-inch diameter nozzle, described in the Edsall paper, has successfully interrupted as high as 60,000 rms amperes approximately with an arcing time not exceeding one-half cycle.

We see no particular reason why air pressures higher than 150 pounds per square inch should not be used, if by so doing greater interrupting efficiency can be obtained. It is interesting to recall that a decade or so ago, boiler pressures of 200 or 300 pounds per square inch were considered to be a safe maximum, whereas today, in the interests of greater efficiency, boiler pressures of 1,000 pounds per square inch and higher are used.

The advantages accruing from the use of resistors automatically inserted into the circuit by the action of the arc during interrupting have been described in the original paper, and their use as an aid to the interrupting process has been amply verified on test. As the breaker consistently operates with not more than one-half cycle of arc, and as the resistor is only connected into the circuit momentarily, toward the end of the arcing period, failure of the resistor is extremely unlikely. During high-current tests, made on a breaker equipped with resistors, there was no appreciable heating of the resistors; proving that the resistors used have ample factors of safety. The use of resistors for the control of the rate of rise of recovery voltage is not new, but this particular design of air-blast circuit breaker lends itself so aptly to the use of this device that it has been considered advisable to take advantage of the possibilities of recovery voltage control thus rendered available, in the interests of greater interrupting efficiency and improved system stability.

H. C. Louis has referred to the danger of external flashover resulting from improper adjustment or defective material. The axial-air-blast circuit breaker described in the Edsall paper has been proved to have adequate operating and insulation factors of safety. Positive sequential timing, inherent in the breaker design, minimizes the possibility of incorrect adjustment. In common with all air-blast circuit breakers, incorrect adjustment or operation beyond rating may result in external flashover, but on the breaker described, such a flashover will not amount to more than an ordinary insulator flashover, since no explosive gases or vapors are present.

Reference has been made by C. M. Gilt to the necessity for co-ordinating the timing of the blast of air and the parting of the contacts. As, in the axial blast design described, the arcing contacts cannot be parted until sufficient air pressure for arc interruption is present in the contact chamber, possibility of the arcing contacts parting at a time when an adequate air blast is not available is eliminated.

Mr. Nye has referred to the use of the terms "cross blast" and "axial blast", and if Mr. Nye's interpretation of the term "axial blast" is expanded to read "The air blast leaves the point of contact separation in a direction parallel to the axis of the moving contact part and is parallel to a substantial portion of the arc path", it will be readily understood that the action of the air blast on the arc in the cross-blast types of breakers described in the Prince, Rankin, and Henley, and in the Ludwig, Rawlins, and Baker papers, is generally quite similar in principle to that obtaining in the circuit breaker described in the Edsall paper, since in both these cross-blast designs a substantial portion of the arc is subjected, during interruption, to a blast of air which is parallel to the sides of the arc chutes and is, therefore, parallel to those considerable portions of the arc which are adjacent to the arc chutes.

The main advantage of the axial-blast design described in the original paper so far as the interrupting process is concerned, is that it enables full use to be made of the three air-pressure characteristics which influence arc interruption, namely dynamic effect, cooling effect, and dielectric effect. It is the efficient use of these three characteristics which is responsible for the maximum arcing time of one-half cycle consistently obtained with the axial-blast design. As a further indication of the high efficiency of air blast obtained in the axial-blast design, it is interesting to note that the actual amount of air used in a 500,000-kva three-phase breaker for arc interruption, is less than one cubic foot of free air.

Design and Construction of High-Capacity Air-Blast Circuit Breakers

Discussion of paper 40-63 by H. E. Strang and A. C. Boisseau, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 522–7.

R. J. Wensley: See discussion, page 1127.

H. C. Louis: See discussion, page 1125.

J. B. MacNeill: See discussion, page 1123.

H. W. Haberl: See discussion, page 1129.

C. M. Gilt: See discussion, page 1125.

Compressed Powdered Molybdenum Permalloy for High-Quality Inductance Coils

Discussion of paper 40-20 by V. E. Legg and F. J. Given, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 865–72.

William Fondiller (Bell Telephone Laboratories, Inc., New York, N. Y.): The paper by Messrs. Legg and Given, adds another interesting chapter in the development of new magnetic materials. In the past 25 years we have seen the cores of loading coils change successively from hard-drawn four-mil iron wire to compressed powdered electrolytic iron; then to powdered Permalloy, and finally to molybdenum Permalloy. As a result of these developments, the effective permeability of the magnetic cores has been raised from approximately 35 to 125. We are dealing here with the initial permeability, that is, the permeability at very low magnetizing forces, and with flux densities of the order of ten gausses. In this same period developments in connection with sheet Permalloy have made available material having an initial permeability of about 2,000. Looking at the matter superficially one might question whether it was worth 25 years' effort to raise the permeability of compressed powdered cores to 125, with this high permeability sheet material ready at hand. I think that a study of the curves of core loss and stability with superposed magnetization just shown by Mr. Given will demonstrate that no continuous sheet-metal core, no matter how thinly rolled the metal, could be made to meet all of the requirements as satisfactorily, considering cost and quality.

Another matter which is of interest in connection with the coil developments described concerns the testing problems involved. The energy losses, due to eddy currents and hysteresis, are by design made extremely low. In order to secure the exceedingly uniform characteristics needed in these coils, it has been necessary to develop refinements in testing technique, particularly for making measurements of modulation characteristics and temperature stability of this core material. Since the modulation can be predicted from the hysteresis factor, equipment has been developed for accurately evaluating this characteristic. For this purpose carefully shielded bridges using amplifier detectors, and precisely calibrated standards have been devised. As a result, absolute accuracies in the measurement of losses in the coil of the order of ±0.2 per cent or ±0.02 ohm are realized. In the evaluation of the core-loss factors, involving the measurement of incremental resistance changes in test coils due to successive changes in current and frequency, an accuracy about ten times this, that is, ±0.002 ohm is realized. The testing of cores for temperature stability involves inductance changes which are measured to an accuracy of ±0.005 per cent. This involves measurements of a fraction of a microhenry in a coil of six millihenries.

In order to attain these relative precisions it is necessary not only to calibrate accurately the bridges along with their meters and power sources, but it is essential that all the equipment be maintained under constant conditions of temperature and humidity. In addition to these precautions, the test samples are specially designed to minimize the effects of stray capacitance, as well as wire and dielectric losses. It is also essential that the coil under test be well dried and that it be kept at constant temperature during measurement. In order to eliminate small errors, due to contact resistance and other effects incident to moving the coil under test, d-c resistance measurements are made in the same circuit as employed for a-c testing. In making the temperature-stability observations means are provided for heating and cooling the test samples which are hermetically sealed in containers to keep them dry.

It should be noted that while these tests were originally developed as laboratory tools, it has been necessary for the Western Electric Company in manufacturing a product having as refined requirements as loading coils and filter coils, to install and operate similar testing equipment for control purposes in its manufacturing operations.

No discussion of loading coils is complete, I feel without a mention of the contributions of the late Professor Pupin. To those of us whose good fortune it was to know him well, he evoked in us feelings of affection and admiration. In this connection, I want to refer to the winter convention of 1926, at which a paper on loading was presented by Thomas Shaw and myself. Professor Pupin, who was then president of the Institute, gave a rather lively discussion of this paper. In the course of his remarks, referring to his invention of the toroidal loading coil, he said, in his own inimitable way, "An invention that cannot stand the companionship of other inventions is probably a poor invention. The loaded-conductor invention takes the repeater invention into its arms, and they hug each other, and make a beautiful pair of congenial companions".

It is quite apparent from the present paper that, using Professor Pupin's criterion of the goodness of an invention, the loading art has also contributed materially to advancing the design of filters which form so vital a part of multiplex telephony using carrier frequencies.

J. W. Campbell (American Telephone and Telegraph Company, New York, N. Y.): The installation of loading coils within the splice sleeve, which is made possible by the reduction in coil size which Mr. Given mentioned in his paper, has a number of important advantages. In new installations a large part of the program loading on cable pairs used for broadcasting services is being done in this way. The arrangement avoids the cost of the cases and of assembling the coils in them and it eliminates the chance of mechanical trouble such as that due to sheath breaks in the stub cables of the loading coil cases, as well as troubles associated with the cases themselves. The use of the individual coils also affords greater latitude in the location of the coils from a spacing standpoint on aerial cables and helps to minimize congestion in manholes.

In the exchange plant, the use of these individual coils provides an economical means of loading small complements of pairs in cables that are not otherwise loaded, and permits the installation of coils in line manholes where space might not be available for placing standard cases. Also the individual exchange loading coils can be employed effectively in loading small submarine cables such as those used to serve islands off shore or small communities separated from the rest of the exchange by a bay or other body of water. In this instance the coils can be placed in the splice between adjacent lengths of cable by using a lead sleeve slightly larger and longer than normal. A steel splice case of essentially the same type as would otherwise be used to cover the splice is applied to provide adequate mechanical protection. This method of loading avoids the use of a separate submarine loading case which is heavy and costly and requires two splices to connect it to the cable. It has the further advantage of greatly facilitating the underrunning of the cable in connection with maintenance operations.

The availability of the relatively compact lead-sleeve cases containing exchange coils is likewise advantageous since it permits loading in line manholes without encroaching unduly on the space designed for splicing and racking cables. In aerial cables, the lead-sleeve cases can be suspended from the strand in most instances, thereby minimizing the likelihood of troubles in the stub cable which may occur when the case is fastened to the pole and the stub is carried up the pole and bent to follow the main cable to enter the splice. The same type of lead-sleeve case is suitable for installation in both aerial and underground plant whereas the normal steel case requires a different outside finish depending upon whether it is to be used underground or aerially.

Taking a look at the effect of these reductions in unit size on the present-day requirements for manholes to house the larger loading coil cases, a comparison with the requirements of 15 years ago is rather striking. For example, to load eight 300-pair 19-gauge cables with coils of the iron-wire or iron-dust type, which were in use up to about 15 years ago, 24 large loading-coil cases were required necessitating a manhole of something over 1,500 feet cubage. Today, using the new molybdenum-Permalloy coils, these cables could be loaded with 8 cases of smaller dimensions, the required manhole having a cubage of some 270 feet, a reduction of about 80 per cent. Incidentally, the number of splices required to connect the loading coils would be reduced by half, thus reducing splicing costs and the troubles to which wiped joints are subject.

In addition to the reduction in plant investment offered by the opportunity to employ smaller loading manholes a no less important advantage is gained from the improvement in subsurface conditions. The smaller manholes naturally take up considerably less space in the street, which is of advantage during initial construction particularly in the congested subsurface conditions of metropolitan areas, and also, occupying less space in a direction paralleling the curb, access by other services to adjoining property is facilitated materially.

High-Speed Voice-Frequency Carrier Telegraph System

Discussion and author's closure of paper 40-51 by F. B. Bramhall, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 873–9.

C. M. Brentlinger (The Western Union Telegraph Company, New York, N. Y.): The introduction of carriers into the Western Union Telegraph system is a comparatively recent development, and has brought with it some interesting operating problems. Prior to 1929 practically all circuits were operated on physical conductors, utilizing ground-return circuits. With circuits of this type, transpositions in outside wire construction are not necessary, and the wire plant was therefore largely made up of nontransposed conductors and, except for short sections in congested areas, was and is practically all open-wire construction. With the introduction of carrier systems it was, of course, necessary first to do a considerable amount of outside plant work in transposing pairs, eliminating excess resistance, and loading cable sections to obtain suitable pairs for the carrier system. In order to keep the expense for this work at a reasonable minimum, the outside plant work was limited to those pairs actually needed for the carrier systems, so that the carrier leads now contain two or more transposed and balanced pairs for carrier operation, in addition to many wires not transposed and carrying ground-return telegraph circuits of various classifications. In this connection it is interesting to note that while the use of nontransposed wires is generally unsatisfactory in carrier operation, we have in emergencies successfully bridged breaks in carrier pairs by patching with nontransposed conductors of the same ohmic resistance in lengths up to 50 miles.

Perhaps the most significant new problem has been the increased seriousness of wire failures, involving carrier pairs. Assuming that all ten channels of a carrier system of this type are multiplexed for four-printer operation, the carrier group totals 40 duplex printer channels, and a failure of any one of the four wires supporting this system will interrupt all 40 of these channels, as well as the circuit assigned to the physical conductor. While carrier groups are not ordinarily assigned to full capacity, the New York-Chicago carrier, for example, has 28 duplex printer and Morse circuits normally assigned on the ten carrier channels, and the loss of such a considerable group of facilities is indeed serious.

As protection against such failures, each carrier system is provided with an alternate route, in addition to the regular route over which service is normally maintained. The two routes are usually over different pole lines with repeater stations in different cities. In the case of the New York-Chicago ten-channel system the regular route is from New York through repeaters at Binghamton, Jamestown, Akron, Marion, and Hammond to Chicago, while the alternate route is from New York through repeaters at Bound Brook, Harrisburg, Bedford, Pittsburgh, Wheeling, Columbus, Cincinnati, Indianapolis, and Hammond to Chicago. It is interesting to note that while the wire mileages of the two routes are practically equal the alternate route has twice as many repeaters as the regular route. This difference is largely due to a greater amount of cable in the alternate route. Over-all attenuation of the two routes compares approximately in the ratio of nine to five.

Alternate routes are lined up daily and maintained intact from terminal to terminal, ready for instantaneous use in case of trouble on the regular routes. Changes to the alternate routes are made by prearrangement between terminals, with practically no interruption to the carrier system. Individual multiplex circuits that lose synchronism because of the change are back in service within one to five minutes after the change in a carrier route is initiated. In addition to the terminal-to-terminal alternate routes, repeater stations on different routes are in some cases provided with connecting carrier pairs to permit rerouting promptly in the extreme cases of failure of both routes on opposite sides of the connecting pairs. As a matter of fact, the necessity for such changes is of infrequent occurrence, and it is not uncommon for a carrier system to operate over its regular route without change to an alternate route for a period of a week or more. With fall-back facilities of this character it can be safely said that circuits assigned to carrier channels are at least as reliable as those assigned to physical conductors.

In addition to the daily line-ups of all carrier routes, regular and alternate, as mentioned above, which include the adjustments of repeater gains at all terminals and repeater stations, frequent checks are made of the balances within pairs, and of the cross fire between pairs. While all of the repeaters on carrier systems now in operation are in regularly maintained repeater stations, a few of these stations are closed during certain hours of the night. Unattended operation of the repeaters during these hours has been entirely satisfactory.

Experience in the operation of the four carrier systems that radiate from New York north to Buffalo, west to Chicago, and south to Washington and Atlanta has amply demonstrated the adaptability of this type of carrier to our particular needs. Additions to the system are planned as the need for additional facilities arises, with the expectation that carriers of this general type will eventually be in operation over most of the principal trunk routes.

R. B. Shanck (Bell Telephone Laboratories, Inc., New York, N. Y.): It is interesting to compare the voice-frequency telegraph system, designed for multiplex printer operation, described in this paper with other systems intended primarily for single-channel start-stop printer operation. The ratio of spectrum efficiencies of at least two to one in favor of multiplex working mentioned in the paper seems high. The 170-cycle spacing used by the Bell System was made liberal in order to allow for possible speed increases and to promote transmission stability of complicated circuit networks. In private-line service, such as that furnished to the press, the government, and others, it is required to operate many sections in tandem and, in the case of teletypewriter exchange service, transmission must be satisfactory over a number of sections switched together in tandem without line-up. It is believed that satisfactory transmission at the usual speed of 23 dots per second could be obtained with a spacing of about 100 cycles without excessive cost for selecting circuits, if it were not required to operate a comparatively large number of sections in tandem. The present standard 170-cycle channels have in a number of cases been used successfully for three-channel multiplex operation at speeds of 40 dot cycles per second and upwards, on a one- or two-section basis. As regards usage abroad, it is understood that a speed of 33 dots per second with 120-cycle spacing is common practice. From the foregoing it appears that for practical purposes the advantage of the multiplex is little more than the 40 per cent advantage of the five-unit code with respect to the seven-unit. It is obvious that for certain classes of service, particularly private-line service with many drops and switched message service, the start-stop method has very important advantages as regards flexibility.

Concerning the effect of changes in level of received signal current, it has been found of great advantage in Bell System service to build into the amplifier-detector circuit of each channel a correcting feature known as a "level compensator". This has practically eliminated the necessity for manual adjustment of the sensitivity of the receiving apparatus. A description of this device will be published in the near future.

It is stated in the paper that it has been found desirable to furnish individual monitoring equipment in conjunction with each channel terminal. In Bell System service it has been found sufficient to provide generally about one monitoring set for each 12 channels. This contrast can doubtless be accounted for by differences in character of the services.

Mention was made of the effect of exact phasing of the various carrier currents at the sending end and it was stated that experience indicated that little is to be gained from exact phasing. A study of this question has indicated that with a number of carriers, such as ten or more, provided in fixed phase relation it is probable that a random arrangement will not be objectionable from the standpoint of excessive peaks. If, however, the various frequencies are furnished by independent sources it will happen at times that substantially all the various currents will add directly to produce peaks which will cause serious modulation effects in vacuum-tube repeaters unless the latter are designed to have considerable excess power capacity.

I should like to ask Mr. Bramhall whether the figure of 12 minutes per day average lost time for a 1,000-mile multiplex carrier circuit includes periods of storm break, and also what type of tuned circuit is used in the carrier system.

F. B. Bramhall: C. M. Brentlinger, in clarifying the broader picture of one or more carrier systems operating on a heavy lead alongside a large number of grounded telegraph circuits, may have raised a question concerning the interference from the latter. All polar-operated grounded physical circuits are, and have for many years, been equipped with a noise-suppressing filter for the suppression of interference to paralleling telephone circuits. These are quite adequate for the protection of the carrier system.

It was not my thought to intimate that the 170-cycle spacing as used in the Bell System voice-frequency telegraph carrier was excessive or wasteful. I can well appreciate the rigidity of the transmission requirements for a system where a number of links are tied together in tandem.

System level changes are the bugaboo of all long-distance carrier circuits. Numerous solutions have been offered and some are highly effective but all add somewhat to the cost and to the complexity of the equipment. I shall be anxious to learn of the "level compensator" mentioned by Mr. Shanck.

The desirability of having individual monitor sets for each channel is, as Mr. Shanck suggests, accounted for by the class of service. Failure of the duplexed multiplex circuit on the individual channel interrupts the work of 16 operators. Any reasonable expense for facilities which will minimize the duration of the interruption is easily justified.

In our experiments with optimum phasing of carriers it was found that good could be accomplished by such phasing as long as the lines and repeaters were a fixed quantity. However, if the generator was lined up for best results on one route and then the system was swapped to another route of greater length and more repeaters an entirely different phasing was required for best results. Our answer has therefore been to build repeaters with ample overload capacity.

The lost time of 12 minutes per day on the multiplex circuit operated on the carrier channel includes all interruptions not directly chargeable to multiplex apparatus failures or to grounded polar-operated extensions beyond the carrier terminals. It does include storm breaks which interrupt, simultaneously, two routes not separated by an average distance of perhaps 125 miles. The question on tuned circuits is a bit out of my domain but I can say that our tuners comprise a series of tuned-circuit elements coupled together by transformers which may or may not contribute to the frequency-discriminating properties. This general type of tuners, I am told, is described in a paper by E. K. Sandeman "Transformers as Band Pass Filters", published in *Electrical Communication*, April 1929.

Calculation and Design of Resistance-Coupled Amplifiers Using Pentode Tubes

Discussion and authors' closure of paper 40-52 by F. E. Terman, W. R. Hewlett, C. W. Palmer, and Wen-Yuan Pan, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 879–84.

True McLean (Cornell University, Ithaca, N. Y.): This paper should be of considerable value to engineers charged with the duty of designing practical amplifiers, since it assembles for the first time almost all of the important factors involved in the particular type of circuit discussed.

Beginning with the well-known relation of equation 1 of the paper, first given by Hull and Williams in 1926, the authors add the three factors α, β, and γ of equation 2. The factor α is treated at length elsewhere by Doctor Terman, so the vital contribution of the paper is the rational derivations and uses of the factors β and γ. There appears to be no handy means of control over these factors that would tend to make them compensating, or reduce undesirable variations in α. The most desirable condition would therefore appear to be realized by making β and γ each equal to unity for all frequencies. This can be done in the circuit of figure 1 only by making the capacity of capacitors C_f and C_{sg} infinite. The practical difficulties are greatly aggravated if several stages are cascaded for the amplification of very small voltages. In this case a very small mutual impedance in the power supply will cause low-frequency oscillation (motorboating), and piling up capacitors on C_f and C_{sg} does not cure the trouble, but merely reduces the frequency of oscillation.

Several years ago (1931) the writer was led to a solution of this difficulty that has recently become rather common. It consists in arranging a circuit that actually takes advantage of the voltage drops in R_s and R_f to improve the general performance, so that capacitors C_f and C_{sg} are not only superfluous, but actually detrimental. The result is accomplished by adding a second tube and interstage coupling network to form a push-pull or balanced circuit. The important thing is that a single common resistor R_f is used for both cathodes connected together, and another common resistor R_s is used for the two paralleled screens. Capacitors C_f and C_{sg} are omitted entirely.

Briefly, an oversimplified description of the circuit action is as follows: The alternating components of the cathode currents of the two tubes are in phase opposition, and tend to cancel each other as far as the production of a-c potential drop in R_f is concerned. Exactly the same relations exist for the two screen currents, while correct direct-potential conditions are maintained by merely using resistors of half the size that would be used in the circuit of figure 1*a*. This arrangement has been criticized on the theory that it depends on balance and accurate matching of all elements on the two sides of the circuit; the tube elements being notoriously bad in this respect.

A thorough analysis of the push-pull circuit with common screen and cathode impedances under unbalanced conditions is quite lengthy and obviously cannot be given here. It is hoped that this may be made the subject of a separate paper in the near future. One result of the complete analysis pertinent to the present discussion is that the circuit works just about as well under severe unbalance as it does balanced. The reason is that the common cathode and screen resistors constitute a differential feedback circuit which always works in the direction to improve the balance. By proper circuit design this "balance forcing" action may be made very powerful, and results in an over-all response curve for the amplifier the same as curves 1 of figures 7 and 8 of the paper. The complete removal of phase shifts due to factors β and γ is a powerful advantage when stabilized feedback is attempted on the amplifier. In addition, a high-gain amplifier made of several stages displays a remarkable lack of sensitivity to common impedance and a-c ripple in the power supply.

Frederick Emmons Terman: The use of push-pull circuits throughout an amplifier as advocated by Professor McLean is a very effective means of making the factors β and γ unity. Such arrangements are to be recommended where the amplification must be maintained down to very low frequencies.

At the same time it is to be kept in mind that in many cases one does not desire uniform amplification at very low frequencies but prefers some form of low-frequency cutoff. Under these conditions the screen-grid and bias impedances can be so proportioned as to aid in achieving the desired results as pointed out in our paper.

Control of Inductive Interference to Telegraph Systems

Discussion and author's closure of paper 40-50 by J. W. Milnor, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 469–74.

F. A. Cowan (American Telephone and Telegraph Company, New York, N. Y.): Mr. Milnor's paper is indeed interesting and should prove a worthwhile reference for those concerned with the problems of mitigating interference on telegraph systems.

Although in the Bell System most of the telegraph services are handled over carrier or metallic systems, there are still several hundred thousand miles of grounded d-c telegraph circuits on which it is necessary to employ suitable means to reduce interference to tolerable values.

In the past we have used various circuit arrangements, some of which were similar to those discussed in the paper. For some types of circuits, use is made of an arrangement similar to the multiwire neutralizing-transformer arrangement discussed. It has been our experience that other forms of interference, such as 25-cycle inductive interference, cross fire, lightning and forms of static, ground potential, both that due to magnetic disturbances and that due to local conditions such as d-c transportation systems coupled with high ground resistivity may frequently be as important as 60-cycle interference.

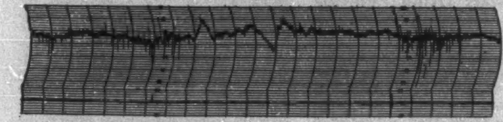


Figure 1. Bias in telegraph signals received simultaneously over two telegraph circuits on the same line, one equipped with balancing wire and other without

Neutralizing or balancing wires have the advantage over selective filter arrangements in that such wires tend to reduce interference in all forms. For this reason balancing wires are the most extensively used mitigative measure in the Bell System. In order to secure full benefit from the balancing wire it is customary to have it extend over the full length of the telegraph repeater section. The usual practice is to employ a balancing wire similar in make-up to the wire to be balanced and to connect this wire to an extra winding on the receiving telegraph relay. The poling of the operating and balancing windings is such as to reduce the interference. An example of the effectiveness of such arrangements is shown in figure 1 of this discussion. This is a record of the bias in telegraph signals received simultaneously over two telegraph circuits on the same line, one equipped with a balancing wire and the other without. The variations in bias on the circuit without a balancing wire originate from two sources, the slow swings being associated with ground potential differences and the sudden momentary changes being associated with a local lightning storm.

J. O'R. Coleman (Edison Electric Institute, New York, N. Y.): The paper by Mr. Milnor presents excellent and much needed information on measures which can be applied to telegraph systems for co-ordination with power circuits. Previously papers pointing out measures applicable to power and telephone circuits have been presented before the AIEE, but this is the first presentation of measures specifically applicable to the telegraph system.

Mr. Milnor points out certain specific applications of many of the devices which he describes, but not their full range of application of their limitations.

It appears to me that the developments in rural power supply will result in many more problems involving co-ordination of power circuits and telegraph circuits. The telegraph circuits in general follow railway rights-of-way and the demand for rural power service will frequently necessitate a distribution line on an adjacent highway. Such conditions largely eliminate the possibility of separation and means must be found economically to co-ordinate the two systems under such conditions.

Work has been contemplated jointly between the Edison Electric Institute and the Western Union Telegraph Company to study these factors for some time, and it appears that we will shortly have an opportunity to actively start this work.

R. K. Honaman (Bell Telephone Laboratories, Inc., New York, N. Y.): Mr. Milnor's paper draws attention to the use of the neutralizing-transformer principle in telegraph circuits. This same principle, of course, has applications in other fields of communication protection

The neutralizing-transformer idea has had a very considerable appeal for mitigating the effects of induced voltages since it first appeared on the scene a number of years ago. It is simple and uses only coils with no moving parts or contacts to involve high maintenance.

The principle embodies merely a wire to pick up a voltage from an induction field and a transformer by means of which the voltage thus picked up may be inserted serially in any one or more working conductors which experience the same induction. By proper poling, the voltage thus applied to the working lines may be made substantially to counteract the voltage which they experience by induction, thereby preventing the flow of any induced currents of appreciable magnitude in the working circuits and the effects resulting from such currents.

From the beginning, however, it has been appreciated that while the device was very simple, its design would have to be carefully made because of the fact that it is inserted directly in working conductors and the presence of the transformer windings has a potentiality of interfering with the normal transmission of signal currents.

Mr. Milnor has pointed out the effect of the added impedance in each telegraph wire and where a multisecondary transformer is used, the more important effect of cross fire between the various circuits arising because each necessarily operates through a winding which must be coupled to windings connected to the other circuits. When used on telephone circuits, these difficulties take the form of transmission loss due to the insertion of the impedance, reflections because of the lumped character of these impedances, longitudinal unbalances which may react upon noise, and in the case of a multisecondary transformer cross talk between circuits.

The refinements involving the addition of amplifiers in Mr. Milnor's arrangements are largely brought about by the necessity for avoiding these adverse reactions on the telegraph circuits plus the fact that he is primarily interested in the relatively low continuous (so-called normal) induced voltages. Under the latter conditions, the self-impedances of the coils are relatively low which makes it desirable to tune the primary circuits so as to minimize the effect of the impedance of the primary pilot wire itself.

It may be of interest to point out a different set of conditions under which the neutralizing-transformer principle is being used without these additional refinements. The application I have in mind has to do with the protection of telephone circuits operating into power stations; protection against abnormal rise of ground potential due to the flow of short-circuit current through the station ground. The application of this device is particularly appropriate under these conditions because, in addition to the ordinary telephone communication, wires into power stations frequently carry pilot-wire relaying, remote control, remote indicating, and similar circuits, many of which cannot tolerate even the brief interruption of service that would accompany the temporary discharge of protector gaps. In these cases the voltage to be neutralized is considerably higher, ranging up to 4,000 volts and not infrequently even higher. The problem is somewhat simplified by the fact that the voltage is concentrated at one point and it can be completely picked up in a relatively short pilot circuit. The voltage is picked up between the power station ground itself and either the sheath of an aerial cable from the central office, if it is available, or some point substantially outside the field of the station ground. In many cases such a point would be not more than 2,000 feet away.

A transformer to neutralize several thousand volts under these conditions has a primary winding impedance which is quite high compared to the impedances which would be used on telegraph circuits, and therefore substantially all of the voltage of the pilot circuit appears across the primary winding without any additional tuning. It is practicable to neutralize all but about 100 volts out of 4,000 (60 cycles) with a primary circuit whose impedance does not exceed about 35 ohms. For this use it has been found most convenient to install the transformers on the basis of one transformer for each circuit so that the cross-talk problem does not become involved.

The Joint Subcommittee on Development and Research of the Edison Electric Institute and the Bell System made an investigation some years ago of the use of the neutralizing-transformer principle for mitigating induction under abnormal conditions in a 24-wire toll line. The transformers, of which there were 12, each contained 24 secondary windings connected to wires over which physical and phantom telephone circuits and d-c telegraph were operated. The design of these transformers was made with special care to minimize transmission loss through the windings and to reduce reflections and unbalances. They were designed to neutralize only 250 volts per transformer in contrast to the much higher voltages used for the power-station service and the primary wires required to pick up these voltages in each section were considerably longer. Under those conditions, it was necessary to use a capacitor to tune the primary winding at the interfering frequency to prevent too large a part of the induced voltage appearing as drop in the external primary circuit. This arrangement very effectively neutralized the induced voltages, leaving only about five to ten per cent of the applied voltage as a remanent voltage under steady-state conditions. It was found, however, that at the beginning of a transient disturbance, the initial charging of the capacitor delayed the initiation of action of the transformer, thereby permitting a transient remanent voltage in the telephone circuits which rose to values of 30 per cent or more of the applied voltage. While this persisted only for a brief interval, it was sufficient to cause breakdown of the telephone protectors.

No further application of the neutralizing transformer to the mitigation of transient induced voltages in toll telephone lines has been made and up to the present time it appears that, all things considered, it is less desirable for this type of service than other measures which are available.

J. W. Milnor: The discussion by Mr. Honaman supplements the paper in illustrating the broad field of usefulness of the relatively simple neutralizing transformer. He indicates a difficulty in correcting transient disturbances due to the shunting effect of the tuning capacitor which parallels the transformer primary. In our work we have met this, at least in part, by placing a reactor in series with the capacitor. Such an arrangement with two capacitors each in series with a reactor, for mitigating transient voltages is described in the paper.

The neutralizing amplifier also is finding a broad field of usefulness, and with further experience the synchronous selective system is likely to be of much value where the induced voltages are not subject to rapid changes.

Problems of inductive co-ordination of power and telegraph systems are the natural result of the desire of each utility to expand, and the desire of each to use its plant at a maximum of capacity and efficiency. The responsibility for co-ordination, to the extent that it is justified, is recognized as joint. As Mr. Coleman indicates, the growth of rural power distribution lines is lending increased seriousness to the problem. Their co-ordination may be especially expensive because the exposures tend to be distributed. In some such cases it may be practicable to reduce the inductive influence of the power systems; in others reliance must be placed upon devices such as those herein described.

The Design Characteristics of Amplidyne Generators

Discussion and author's closure of paper 40-10 by Alec Fisher, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 939–44.

C. R. Hanna (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper on the design of the familiar metadyne is very interesting. Where regulation accuracy requirements are not too great, this device may be used, but as the author points out, a nice balance between the compensating field and the armature ampere turns due to load current is required for high amplification, and this balance, of course, is subject to definite practical limitations.

It may be interesting to compare the power amplification of the metadyne with that of a simple standard exciter generator with an electromechanical rheostatic voltage regulator such as the Silverstat[*] or any similar device preceding the exciter. Such regulators cover the full range of resistance variation for changes in applied voltage of less than four per cent and in the middle of the range the flatness is equivalent to less than two per cent. The change in power of the input circuit of the Silverstat represented by two per cent change in voltage is about 0.4 watt. Taking the exciter output as 4,000 watts, to correspond to the size of machine mentioned in Mr. Fisher's paper, the power amplification is 10,000. This is somewhat better than that of the best reported value of a four-kw metadyne at full load. The Silverstat, however, can control much larger exciters with the same low input power. For example, the largest size may regulate a 100-kw exciter with 0.5 watt input, giving an amplification of 200,000.

Such a combination of regulator and exciter has the advantage of being less fussy to design and manufacture. It is inevitable that machines designed to perform several functions are subject to a greater number of limitations and compromises.

A further advantage of the electromechanical regulator is that it uses the force of a spring as a reference and this is much more stable than any conveniently available electrical reference current such as required in the metadyne circuits.

K. K. Bowman (nonmember; General Electric Company, Schenectady, N. Y.): Mr. Fisher has pointed out that the shunt quadrature field reduces the armature heating during steady-state conditions. However I believe a few remarks are in order regarding the effect of the shunt quadrature field during transient conditions. In circuits using control field forcing the armature short-circuit current may be instantaneously larger when the shunt quadrature field is used than when it is omitted. This is due to the close coupling between the shunt quadrature field and the short-circuit axis of the armature. Hence in certain applications omitting the use of the shunt quadrature field may reduce the armature heating as well as increasing transient stability and response.

Mr. Fisher's figure 4 shows the effect of the shunt quadrature field upon amplification. When we consider that there is a wide variety of fields that may be used all of which affect amplidyne characteristics, some idea of the flexibility of the amplidyne for special and difficult control jobs can be realized.

Another very important practical characteristic of the amplidyne, other than its very high volt-ampere amplification, is that this amplification increases rapidly as the size of the machine increases. That is, assuming the same general design in accordance with good mechanical and electrical practice, the volt-ampere input to the control field will tend to remain constant regardless of the size of the amplidyne. This means that one size of control apparatus, such as a high-vacuum tube amplifier may be standardized to control jobs requiring power from a few watts to many kilowatts. This means a real simplification and will be extremely important with the increasing use of automatic control.

K. M. Sparrow and **E. C. Watson** (nonmember; both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): We have been using a rotating regulator similar to the amplidyne described in the paper since 1928. There have been some problems arising out of its use. In some cases this amplidyne has been designed without interpoles, reliance being put on the brush drop to commutate the currents involved. In applications where close regulation is required and where the control voltage must reverse, the brush drop is objectionable for it causes an error in the response. This regulator has two sets of brushes and so it appears that this error will be amplified. Would the authors please give their experience with the brush drop?

It has been our experience that in this type of regulator the residual voltage must be kept at a minimum. In Mr. Fisher's paper he mentions that it is desirable to keep the iron loss at a minimum. We would like to have the author's experience on the effect of the residual voltage on the accuracy of response.

The author is to be complimented on the work. The trend in industry is toward a greater use of regulating devices and this type of machine doubtless will find a number of applications.

Alec Fisher: See discussion, page 1137.

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[*] "Recent Developments in Generator Voltage Regulation," C. R. Hanna, K. A. Oplinger, C. E. Valentine. AIEE TRANSACTIONS, 1939, page 838.

Dynamoelectric Amplifier for Power Control

Discussion and authors' closure of paper 40-7 by E. F. W. Alexanderson, M. A. Edwards, and K. K. Bowman, presented at the AIEE winter convention, New York, N. Y. January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 937–9.

P. L. Alger (General Electric Company, Schenectady, N. Y.): The advance of engineering in recent years has been especially marked by the joint use of widely different branches of engineering knowledge in a single structure, as, for example, the combined electrical, mechanical, and thermal theories underlying such developments as the Magne-Blast breaker and the hydrogen-cooled machine.

Heretofore, the experts in the communication and electronic fields have seemed to live almost in a world apart from their fellow engineers who have dealt in industrial types of power equipment, very few of either branch being interested in the work of the other. Now, however, it has been shown that the circuits and concepts of the electronic art can be built into power equipment; and that in fact there is a remarkable similarity between amplification and stability problems throughout these fields.

These papers on amplidynes, therefore, seem to me to be of historic importance. They mark for the first time the union of the talents of the electronic and industrial motor engineers, resulting in the creation of power amplification systems of the greatest variety and potential value to industry. Hereafter designers of d-c machines will take a real interest in negative-feedback power amplifiers, and communication engineers will think of d-c machines as possessing far more than the prosaic character with which they have generally been endowed in the past.

K. M. Sparrow and **E. C. Watson** (nonmember; both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The amplidyne described is very interesting. Since 1928 our company has been manufacturing a rotating regulator and are using it extensively on variable-voltage elevator and planer sets.

In Mr. Alexanderson's paper he mentions the difficulty caused by back coupling and he states that it required a rather delicate adjustment. In most industrial plants we have found it is best to avoid delicate adjustments. The paper also states that the tendency toward regenerative oscillation is present when the amplifier is used to feed highly inductive loads such as field windings on large machines. He comments that they have always been able to obtain stable operation in "one way or another". It appears that this would tend to complicate the application as compared with a simple easy-to-maintain regulator.

Mr. Alexanderson's paper mentions that the electromechanical resonance is apt to be a real problem in certain applications of this amplifier. Does this mean that it cannot be applied to an existing drive unless new motors and generators are also supplied? There are many applications where it is desired to add a regulator without having to change the present drive.

The regulator as described in the papers should find its place in industry where there is a growing demand for all types of regulators.

E. F. W. Alexanderson: The question asked by K. M. Sparrow and E. C. Watson is very appropriate, and I am glad to have an opportunity to make the situation clear.

The delicate state of balance of forces which make possible a high ratio of amplification is a problem for the designer, but if the machine is once correctly designed this does not involve any complication or difficulty in operation. Quite the contrary, the better the design the less will the operator be aware that there ever was a problem. In most cases, it is only necessary to have a specially designed generator, whereas existing motors can be used. It is also usually possible to use an amplidyne generator as an exciter for an existing generator, which in its turn controls a motor or performs whatever function is desired.

Now we have endowed the dynamoelectric machine with a new property, that of an amplifier, and the implications of this new requirement must be understood if the principle is to be successfully applied. We have also hoped that our analysis of the problem will convince those who wish to become interested in the practical use but find it hard to believe that a ten-kw dynamo can be controlled directly by a small radio tube.

Industrial Applications of Amplidyne Generators

Discussion and authors' closure of paper 40-23 by D. R. Shoults, M. A. Edwards, and F. E. Crever, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 944–9.

K. M. Sparrow and **E. C. Watson** (nonmember; both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Since 1928 our company has been applying a rotating regulator to certain applications. We have had many problems arising out of its application.

In many industrial plants where they have their own power station we have found that the frequency is allowed to vary. This variation in frequency is objectionable on this type of regulator especially in cases where it is used against a matching voltage supposedly supplied by a constant-potential exciter such as in the paper-mill drive. Would the authors please give their experience with the effects of variation of frequency? It appears that the amplidyne would amplify the changes in frequency.

In some cases this amplidyne has been designed without interpoles, reliance being put on the brush drop to commutate the currents involved. In applications where close regulation is required and where the control voltage must reverse, the brush drop is objectionable for it causes an error in the linear response. This regulator has two sets of brushes and so it appears that this error will be amplified. Would the authors please give their experience with the brush drop?

The paper states that in the simple circuit, figure 1, the accuracy obtained is within a few per cent. From further discussions and diagrams it appears that to get better accuracy it is necessary to use a "composing amplifier". Nothing is said in the paper about the "composing amplifier". Is the simple amplidyne accurate only within a few per cent? Our experience has been that most applications requiring a regulator must have greater accuracy.

Can this regulator be applied to a constant-voltage circuit such as an adjustable speed motor or must it always be used on variable or modified variable voltage?

The authors are to be complimented on their work. The trend in industry is toward a greater use of regulating devices and this type of machine doubtless will find a number of applications.

R. W. Porter (General Electric Company, Schenectady, N. Y.): Messrs. Shoults, Edwards, and Crever have pointed out the fact that the amplidyne generator, because of its low control-power requirement, can often be used as a regulator or closed-cycle controller without any additional amplifiers. Although the amplidyne characteristics differ from those of an ordinary machine only in order of magnitude, this difference opens up an entire new field of applications which previously have been impractical.

For example, in the scheme shown by the authors in figure 1, there are two opposing control fields. The magnetomotive force which controls the amplidyne is the difference in magnetomotive force of these two fields. If the per cent regulation of the controlled quantity is to be R, the reference field must produce $(1/R)$(A.T.) and the control field must go from $1/R$ to $(1/R)(1+R)$(A.T.), where (A.T.) is the number of ampere turns required to control the amplidyne over the necessary output range when only the one control field is excited. Since the watts dissipated in a field winding containing a fixed amount of copper varies as the square of the ampere turns, the total dissipation in the two bucking fields will be approximately $2/R^2$ times the power required for one field alone, or $4/R^2$ times the power required if the copper space in both fields were used in a single large control field.

An ordinary generator requires about one per cent of its rated output for excitation. If it were to be used in a differential-field scheme such as this, designed for ten per cent regulation, it would have to dissipate 400×0.01 or four times its rated output in the form of field heating. The amplidyne, however, needs only one $^1/_{100}$ as much power for excitation and therefore will dissipate only four per cent, instead of four times its rating in the two opposing fields.

In the differential-voltage scheme, shown by the authors in figure 2, field heating is no longer a problem, but still an amount of power approximately equal to $1/R$ times that required for excitation must be transferred either from the primary source B into the load, or in the reverse direction. Furthermore, if the range of control is great, the potentiometer across B must be of low resistance, and will absorb at least as much power as this, possibly several times as much. For the ordinary generator in a design for ten per cent regulation, B would have to be capable of supplying at least 20 to 30 per cent of the generator rating with exceptionally good voltage regulation, and the potentiometer would be large and expensive. On the other hand, if an amplidyne generator is used, B will need to be good for only a fraction of a per cent of the amplidyne output, and the potentiometer will be small.

Thus it is evident that with the amplidyne a variety of simple control circuits are brought within the range of practicability.

The truth of the authors' statement regarding stability is illustrated by the following criterion for the stability of a closed-circuit system having three time constants, T_1, T_2, T_3, and a total amplification around the circuit of A. Such a system will be unstable unless

$$1+A<(T_1+T_2+T_3)\left(\frac{1}{T_1}+\frac{1}{T_2}+\frac{1}{T_3}\right)$$

This same criterion holds whether the system is composed of three generators, or an amplidyne and a standard generator, connected to regulate voltage, in which case the time constants are all of the form L/R, or a motor with an exciter which is connected to regulate speed, in which case one of the time constants is determined by the inertia and armature resistance of the motor. The worst situation is obviously when all three time constants are equal, for then the amplification is limited to eight. If one of the time constants is $^1/_{20}$ of the largest one, then the amplification can be a large as 44. Similar but more complicated expressions hold when the number of time constants is more than three. The short time constant of the amplidyne, therefore, makes possible definite improvement in the stability of a regulating system.

A. Fisher, D. R. Shoults, M. A. Edwards, and F. E. Crever: The comments by Messrs. Bowman and Porter in connection with these papers are particularly appreciated because of the wealth of experience which they have had with these machines and with their application. Mr. Bowman touches upon one point which might well be discussed further and that is, that the trend in design of the amplidyne generators has been such as to result in a practically constant control-field input even though the size of the machine is materially increased. Several 50-kw machines have been built and tests on these machines have shown that full control of output could be obtained with a control-field input expenditure of considerably less than one-quarter of a watt. The amplification factor of these machines then is materially greater than 200,000:1, which is the figure mentioned by Mr. Hanna as being representative of the performance of a 100-kw exciter with field control by means of a rheostatic-type regulator.

A mere statement of amplification factor is, however, not a complete statement of the utility of such an amplifier. Mr. Porter has given a criterion as to the relationship of the stability factor of a closed control system as determined by the over-all amplification and the number and magnitude of the independent time lags of the various devices in the system. As the number of time lags is increased, the stability problem becomes more difficult even though the time constants may be short in each case. It is particularly necessary to have as short a time lag as possible in most amplifiers for industrial purposes as there are usually at least two other components in the system which necessarily involve time delays and if the time delay of the amplifier itself can be made very small in proportion to the others, then the amplification may be correspondingly large with less difficulty in obtaining stability. It is realized, of course, that stability can be obtained with high degrees of amplification by means of various types of circuits feeding back into the input of the amplifier. The problem of design of such feedback units is made easier however, with high amplification and with low time lag of the amplifier.

The comments by Mr. Hanna and also Messrs. Sparrow and Watson refer generally to regulators and regulating devices. The authors wish to stress that the amplidyne generator as such is not a regulator and is instead, as stated in the paper, an amplifier. A regulator must contain within itself the standard of comparison and differential operating means for producing the required control function. The amplidyne generator is adaptable however, to use with other regulators and comparison circuits as is any power amplifier. The essential requirements of an amplifier are that it must be capable of covering its required output characteristic range within a certain limit of input control power. As applied, the regulating circuits or devices should be capable of providing this amount of input control power within the permissible deviation of the final output characteristics from the specified ideal. Thus, in using the amplidyne generator, the comparison circuits or supplementary devices must be engineered with this point in view. It was not intended that these papers should cover a complete description of such circuits or such amplifiers, as many such devices are generally known and available.

As Mr. Porter has ably pointed out, if the amplidyne generator is used in comparison circuits, the required amount of excitation loss in the machine is insignificantly small as compared with the excitation which would be required for a conventional d-c machine operated in the same type of circuit with opposed fields and also that the required power output of a differential voltage circuit with a conventional machine would be prohibitively large as compared with the same circuit used with an amplidyne generator. It is perhaps for this reason that such factors as brush drop have not been of detrimental influence in the operation of amplidyne generators as these factors are of themselves small as compared with the resistance of circuits and saturation in the magnetic paths, all of these factors being lumped together in the complete control power requirements.

The name "amplidyne" is intended to distinguish between this new development of a variable current, controllable constant-voltage generator, and the "familiar metadyne" as mentioned by Mr. Hanna. The metadyne machine is usually thought of as being a constant-current machine and as such contains within itself the regulatory or comparison characteristics. The constant-current machine has, of course, a vastly different volt-ampere characteristic than the relatively flat one of the amplidyne generator. The restoring effect or tendency to bring about any particular output is internal in the usual metadyne arrangement and must be supplied by external means or circuits with the amplidyne generator.

Messrs. Watson and Sparrow have questioned the effect of frequency variations on such applications as the paper-mill drive described. In this particular application the standard of performance with which the operation of the machine is compared by use of the tachometer generator, is set up in the composing amplifier as a voltage standard and as such is independent of frequency or similar variations. Hence the only effect of a frequency variation would be to change slightly the amplification factor of the amplidyne by varying its rotational speed. The effect of this change is negligible as far as causing any disturbance of speed of the main drive.

The amplidyne generator is not limited in its application to any particular type of speed-control system, but is adaptable to any system requiring power amplification of a high degree and with time limit requirements considerably more severe than those usually met by conventional d-c machinery.

Analysis of Bushing-Current-Transformer Performance

Discussion and authors' closure of paper 40-69 by J. P. Gebelein and J. A. Elzi, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 541–6.

C. O. Werres (General Electric Company, Schenectady, N. Y.): One point which has been mentioned by Messrs. Gebelein and Elzi should be given more consideration sooner or later. I refer to the saturation that occurs in relays when they are subjected to overcurrents. This, of course, changes their impedance so that characteristics determined by either test or calculation which are usually made on the basis of nonsaturating burdens are not correct. Fortunately, the actual burden is less than the supposed burden and hence the transformer is better than indicated by the test or calculation. Offhand this does not sound too serious and it is not, if, for example, the errors in all transformers involved in a given setup using a percentage differential relay are kept below the relay setting. However, if this should not be done, it is conceivable that the effect of saturation of the burden might effect one transformer more than another to such an extent as to cause trouble.

P. O. Langguth (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have aptly described the problems besetting the application engineer in the analysis of relay problems involving

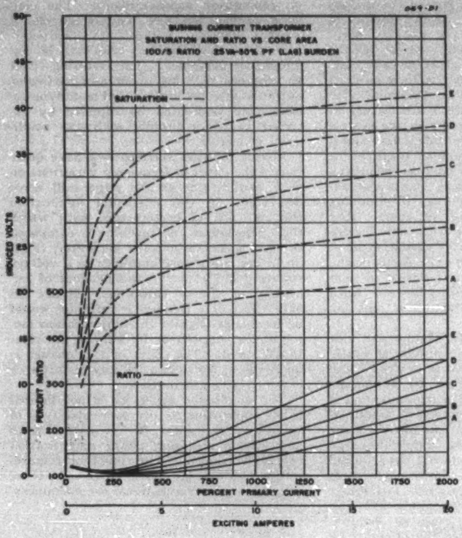


Figure 1

the use of bushing current transformers. They are to be congratulated on the extent and exactness of their investigation to obtain data that is a real value to the operating engineer.

It is important to note that the use of transformer excitation characteristics is more generally applicable in the solution of their problems than the usual ratio curves. The authors' discussion on burdens is indicative of the variety of loading conditions such as saturating burdens, introduced which render the use of ratio curves none too satisfactory. If the burden impedance varies with the value of secondary current, a ratio curve taken on some constant impedance burden is not suitable over the entire current range of the curve.

Relating to the variation in transformer characteristics with core volume, it is interesting to note the data shown in figure 1 of this discussion. Five transformers are represented here having the same internal and external diameters with the area ranging from normal to twice normal by changing the axial dimension. Steps represented are 20 (*B*), 40 (*C*), 80 (*D*), and 100 (*E*) per cent increases in core area. Similar data were taken by changing the external diameter only which gave approximately the same results. The improvements obtained are negligible under low-current conditions but easily apparent under high-current or high-flux conditions. This type of information is very important in the analysis of applications involving transients. The saturation characteristic is an index to the limit of operation with transients.

In commenting on the effect of wave form, it seems desirable to bring out the fact that the methods of measurement and the types of instruments used in making transformer tests have considerable bearing on the results obtained. In taking saturation characteristics for instance, we have found that the presence of harmonics in either the voltage or current waves or both, does not materially affect the results obtained if a Rectox voltmeter is used. However, ratio and phase-angle data in the saturated region will be affected somewhat by harmonics as pointed out by the authors. Since null methods ordinarily involve the fundamental only it may be desirable to suggest that a common level be approached for this type of testing.

The application of transformers involving circuits such as presented by the authors in their figure 6 is apparently simplified by using the transformer equivalent impedance. Ratio and phase-angle curves can be ignored when making analysis on this basis. The authors have nicely demonstrated a method of using the saturation characteristic in solving relay problems and have verified the over-all procedure by test measurements.

J. P. Gebelein and J. A. Elzi: The discussions which have accompanied the presentation of the group of papers on bushing-type current transformers indicate a widespread interest in this subject and bring out many valuable points.

The data presented by Mr. Langguth in his figure 1 is appreciated because it supplies information showing the effect on transformer performance for various values of core area. Since transformers were not available for test, data such as these were not included in the paper. It appears that the data presented in this figure could be considered as giving a true indication of the results which could be expected in any transformer of similar construction.

We feel that the points brought out by Mr. Langguth regarding the care which must be exercised in making measurements for saturation curves and the type of instruments to be used cannot be overemphasized inasmuch as experience has shown that considerable error can be introduced if these precautions are not taken.

Messrs. Werres and Langguth discussed the importance of taking account of the saturation characteristics of burdens from the standpoint of the harmonics which they introduce into the circuits and the change in burden resulting from saturation. We agree with Mr. Werres in that in many cases the effect of burden saturation is not of sufficient consequence to affect seriously the results obtained, but due judgment must be exercised in taking the effect of burden saturation into consideration.

A Proposed Method for the Determination of Current-Transformer Errors

Discussion and authors' closure of paper 40-68 by G. Camilli and R. L. Ten Broeck, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 547–50.

C. A. Woods, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper, like the Gebelein-Elzi (TRANSACTIONS pages 541–6) and Woods - Bottonari (TRANSACTIONS pages 554–60) papers, proposes a method of calculating the steady-state performance of current transformers from their fundamental data; consisting of the excitation characteristics, and the values of secondary resistance and leakage reactance. It differs from these other papers in that it is not restricted to bushing-type current transformers, in which the leakage reactance has usually been found sufficiently small that it may be neglected for most relay applications.

The calculation or measurement of leakage reactances in current transformers has been recognized as a problem, and is often quite serious in wound-type current transformers. Methods for reducing its effects such as equalizing windings as employed by the Westinghouse Company have been in use for many years. Hence, the treatment of this subject is important due to the ease and simplicity of performance calculations when adequate information on the leakage reactance is available. For the range covered, the material and methods presented seem very practical and worthy.

The authors state that the secondary leakage reactance is not necessarily constant, but varies with the secondary current and distribution of the winding. By test they have found the changes are quite limited, and from a practical standpoint may be assumed constant. I believe at high primary-current values additional factors should be considered. These include the magnitude of primary current, location of the primary conductors, the relative size of the core with respect to these spacings and currents, and the mutual or working flux in the core.

It would be interesting to know the spacings and locations of the primary conductors for the conditions under which the particular transformers were tested. It is realized that the method of measuring the leakage as given is subject to limitations in currents, but additional data at current values considerably above the 600 amperes primary and 10 amperes secondary, covered by the paper, would be quite valuable.

If the characteristics of the current transformers, including the secondary leakage, are known, then the use of the equivalent circuits and excitation characteristics gives an excellent method of calculating the overall relay and transformer performance. Perhaps additional work and data on the determination of secondary leakage reactances at the higher values of operating currents would accelerate the acceptance of such a method for applying current transformers in the relay field.

C. O. Werres (General Electric Company, Schenectady, N. Y.): The fact that we have five papers on this general subject shows clearly (1) a growing realization of its importance, and (2) definite progress in providing means for analyzing current-transformer-relay problems. The introduction of calculation methods carries three implications: (1) ordinary ratio and phase-angle data are inadequate for certain analyses, (2) because of the difficulties and expense involved in making such tests only a few central laboratories have been able to do this work, and (3) sufficient ratio and phase-angle data have not been available until recently to satisfy those who prefer it for those cases in which it is useful. I would, therefore, like to review the subject of direct testing because (1) some still prefer test data as is evidenced by the demand for it, and (2) it is valuable as pointed out by Messrs. Wentz and Sonneman in verifying predictions obtained by calculation.

For over 20 years the overcurrent ratio characteristics of General Electric bushing-type current transformers have been obtained by a simple comparison test which is fairly fast and economical. Another simple ratio test that may appeal to those who have to do less of this work than the manufacturer is described in our literature on bushing-type current transformers (*GEH*-229).

A few years ago the demand was extended to cover phase angle as well as ratio. As a result of this, the test equipment described by the writer in the August 1937 *General Electric Review* was built up around the shunt method apparatus used for high-precision normal-current tests. This was higher grade equipment than was necessary for overcurrent testing, but it was applied because it was so readily adaptable.

More recently, equipment which is much simpler and at the same time faster has been installed. Readings are taken on portable instruments, hence only one application of current is necessary for each point taken, whereas by the old method five to ten applications were sometimes necessary. The advantage from the standpoint of heating, hence time and expense, will be quite evident.

This new method has been a very good improvement as far as small current transformers are concerned. However, some of the high-voltage transformers have so much insulation that their thermal characteristics make testing rather tedious, even by the improved method. The difficulty is not in preventing the windings from reaching a dangerous temperature, but it is rather the time involved in waiting for them to cool down to a suitable temperature before taking the next point. Direct tests on such transformers are a real problem so for these cases an indirect method should be especially useful.

However, I would like to emphasize one point brought out in these papers, namely, that the calculation of a current transformer is simpler and results are more reliable if secondary leakage reactance can be neglected. However, this is not always possible and when this factor exists it should be recognized that the accuracy cannot be as good as otherwise, particularly if saturation occurs. Because of this fact, the method of determining reactance as proposed by Messrs. Camilli and TenBroeck promises to be helpful.

E. C. Wentz (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The authors assume the existence of a constant secondary leakage reactance, which is equivalent to a transformer with no leakage, but with a constant reactance in series with the burden. This is the same as assuming an equivalent leakage flux which flows around the whole magnetic circuit. A study of the actual flux paths in the transformer core will lead one to doubt the existence of such an equivalent constant reactance, or equivalent leakage flux. Figure 4 of the Wentz-Sonnemann paper (TRANSACTIONS pages 481–8) shows that the leakage flux flows through only a part of the core, and it is well known that the flux density in the core varies from point to point around the core. Any equivalent flux cannot possibly be the same as the real leakage flux which flows in only part of the core.

As long as the real density is not near the saturation value, the assumption that an equivalent flux in the whole core will cause the same ratio and phase-angle error as the real flux in part of the core is not very much in error. If the burden is high the leakage flux is small relative to the working flux, and errors in assuming the equivalent value of leakage flux will be less important.

However, if the burden is relatively low so that the working flux to induce the burden voltage is low, and the leakage flux, therefore, relatively high, and the flux density is near the saturation value in part of the core, it is no longer even approximately correct to say that a certain flux flowing in the whole core is equivalent to a greater flux flowing in only part of the core.

However, even though the assumption may not be exact, the resulting errors in calculation can probably be made to be always on the conservative or pessimistic side. The errors in calculation will be greatest when the burden is low. If the authors can show that at the lowest usual burden and highest possible current the measured ratio error will always be less than the test ratio so that results are always conservative, the method of calculation may have a real value to the industry.

The tests in the paper do not show this, as they are at high burdens.

For precision calculation a π circuit instead of a T circuit has been used in our work for a considerable number of years. This has the advantage of being more accurate—the disadvantage of requiring more numerical work.

C. T. Weller (General Electric Company, Schenectady, N. Y.): The operation of differential relays for generator protection on through short-circuits some 20 years ago led to overcurrent tests on the current transformers involved. These were of the bar type with rectangular cores and were rated at 2,000 amperes for 15-kv circuits. Tests were made up to about 25 times normal current at various values of matched and unmatched burdens. Two general recommendations were based upon the results: First, to adopt a short time delay for the relays in order to by-pass the larger part of the transient differences between the current transformers and second, to limit the differences between the burdens supplied by each pair of transformers in order to reduce both their transient and permanent differences at high overcurrents. The adoption of these recommendations has resulted in fairly satisfactory performance. However, the desirability of instantaneous operation on true internal faults has stimulated further investigations. Current transformers, as well as burdens, have been matched and special designs of transformers have been applied.

In matching current transformers, their secondary leakage reactance must be considered and in this connection, I should like to quote from a discussion presented in 1925: "For current transformers of the ring-core type with evenly distributed windings, the secondary leakage reactance may be taken to be zero. For current transformers of moderate voltage ratings and of low ratios of the square-core type, the leakage reactance may be taken to be approximately one-quarter of the total leakage reactance obtained from the short-circuit impedance test. This value applies over the range of rated current. For moderate overloads, the value may reach over one-half of the total leakage reactance. At heavy overloads, the saturation of the core greatly complicates the results. These results were obtained by calculation from the ratio and phase angle and exciting-current results and were referred to the results of the short-circuit impedance tests." The current transformers tested were of 1:1 ratio and were for use on 4.5-, 7.5-, and 15-kv circuits, respectively. Although the method used was far from exact, a considerable range of leakage reactance overcurrent ranges ordinarily encountered was indicated. From the standpoint of leakage reactance alone, ring-core type current transformers offer an advantage over rectangular-core types.

In conclusion, it appears that all of the work done on this general problem of overcurrent characteristics of current transformers, with particular emphasis on the papers here presented, has created a much better understanding of the difficulties involved and of the possibilities inherent in special treatment of individual cases.

R. L. TenBroeck: In regard to the secondary leakage reactance, which seems to be the main topic under discussion, our tests have indicated that in bushing-type current transformers with concentrated winding, the total secondary leakage reactance is constant up to the complete saturation of the core.

In wound-type current transformers the method of determining the secondary leakage reactance outlined by the authors, seems to be of particular value in all these cases in which the primary winding is so located that it does not saturate a portion of the core much in advance of the remaining portions of the core.

The use of fundamental values for the exciting current, instead of effective values, seems to result in better agreement between the calculated and test results.

Overcurrent Performance of Bushing-Type Current Transformers

Discussion and authors' closure of paper 40-67 by C. A. Woods, Jr., and S. A. Bottonari, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 554–60.

G. B. Dodds (Duquesne Light Company, Pittsburgh, Pa.): Aside from the calculation of ratio curves and determination of leakage currents, there are other uses of excitation curves. One of the most useful and easiest to use is a quick method of determining the approximate range over which the current transformer will perform without any great deviation from normal ratio for any given combination of burden and secondary currents. This is covered in appendix III, item *c* (linear performance range). For example, in figure 1 of this discussion are shown excitation curves of various types of current transformers, including both wound primary and through type. Ratio curves of the same current transformer at burdens of four ohms and two ohms respectively are shown on figures 2 and 3. It can be observed that overcurrent ratio performance of various types of current transformers is in the same order as the excitation curves shown in figure 1; that is, the current transformers having the lowest voltage excitation curves perform the poorest during heavy overcurrents.

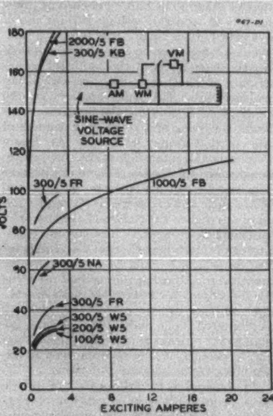
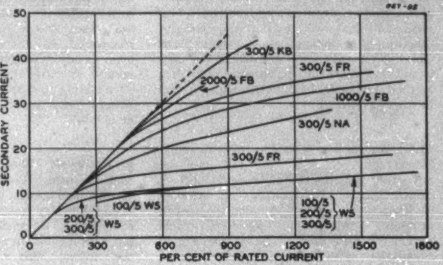


Figure 1. Excitation curves

It has been agreed among current-transformer designers that the ratio curves could be rather accurately computed from excitation curves of ring-type current transformers. This same accuracy has not been obtained in other than ring-type current transformers. However, it is felt that, even in wound primary and bus-bar types of current transformers, the excitation curves are of some value in roughly determining performance at overcurrents.

Figure 2. 100-volt-ampere burden, 50-per-cent power factor



In figures 1, 2, and 3, the *W-5*, *NA*, and *KB* are wound-primary-type current transformers, the *FR* is ring type, and the *FB* is the rectangular bus-bar type with two coils. It can be seen that, at the voltage corresponding to one ampere excitation current, considerable departure from straight-line voltage characteristics has already occurred. Considering the *NA* current transformer, 60 volts is required to produce one ampere excitation current. Considering the ratio curve of this current transformer with a burden of 50 volt-amperes at 5 amperes, which is two ohms, it is seen that at 30 secondary amperes, which, with a two-ohm burden, required 60 volts to produce, the current transformer has broken off in ratio roughly 16 per cent. Similar rough ideas of the performance of the other current transformers from their excitation curves can be obtained.

Figure 4 shows the excitation and ratio curves of an oil-filled wound primary type of current transformer and a rectangular bus-bar through type with four coils. In these cases, ratio curves were made with extremely high burdens in order to obtain breakdown without necessity of using large primary currents. Again, it can be determined that there is a rough relationship between the excitation curves and the ratio curves.

It is hoped that the investigation of uses of excitation curves in connection with overcurrent performance of current transformers and relaying will be continued. Usefulness with ring-type current transformers has been definitely established. It is believed that further investigation may enable us to apply them similarly to rectangular bus-bar types and wound primary types with an accuracy approaching that of the ring-type current transformer.

C. C. Cole (nonmember; Duquesne Light Company, Pittsburgh, Pa.): The authors have given us a method of calculating the performance of current transformers from data obtained from a secondary excitation test; these data being taken by impressing sine-wave voltage on the secondary and reading the effective value of the exciting current with an rms ammeter and the iron losses by means of a wattmeter. This method appears to give quite satisfactory results on the ring-type transformers of the kind considered in this paper. The authors have also called attention to the similarity of the saturation curves obtained by using sine-wave exciting voltage and by using sine-wave exciting current when the readings of excitation voltage are obtained with a voltmeter which gives a deflection proportional to the average value of voltage. We believe that this point is worthy of special emphasis because of the wide variety of conditions encountered in field and laboratory tests. In many cases, even when these tests are performed in the laboratory, the supply voltage may be sufficiently distorted so that it could not be considered as a sine-wave voltage. Under field conditions this is even more likely to be the case. It has been our practice when conducting secondary saturation tests to use a rectifier type of voltmeter which is calibrated to read in rms terms. In case there is any doubt as to the distortion of the supply voltage, it is probably advisable to use two voltmeters, one reading average values and the other reading rms values. If there appears to be a considerable discrepancy between the readings of these two meters, it might be desirable to consider making a correction of the iron loss data obtained from the wattmeter.

It appears that the type of ratio curve which the authors are calculating will represent the ratio between the primary current (assumed sine wave) and the rms value of the secondary current. This would be the type of ratio characteristic ordinarily required for determining the operation of overcurrent relays. However, there might be cases where we were interested only in the fundamental components of secondary current. In this case it would probably be more accurate if the secondary saturation data were obtained by reading the inphase and quadrature components of exciting current

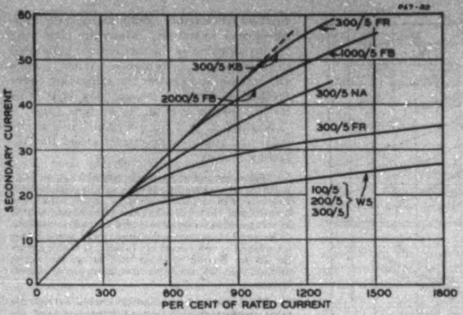


Figure 3. 50-volt-ampere burden, 50-per-cent powerfactor

with a wattmeter having a constant potential obtained from a phase shifter. This method would, of course, give only the fundamental components of exciting current, and when a ratio calculation was made with these data, we would expect to obtain the fundamental component of the secondary current. This is the type of ratio curve which might be desired when investigating the performance of directional relay elements in other similar devices.

There is a third kind of ratio characteristic that might be desired in some cases. When current transformers are used on differential circuits, there may arise cases where the equivalence of the secondary currents in rms value or of their fundamental components is not sufficient to insure a satisfactory balance. We are pointing out this possibility with the idea that in some cases it might be desirable to obtain both types of saturation curves for all the current transformers involved in the differential circuits, so that by comparing the various pairs of curves at least some qualitative idea could be formed regarding the performance of the transformers when used for differential protection.

The ideas which have been offered in this discussion are not intended to imply that more accurate methods of calculating performance than those suggested by the authors are generally necessary, but only with the thought that when more extended application is made of this method of calculation to all sizes of through-type transformers, some of which might have different core materials, there might occur cases where greater refinement would be required. One possibility that immediately suggests itself would be to apply the authors' method to wound-type current transformers. It has been our experience, which we believe has been borne out by the results of others, that the calculation of the overcurrent performance for wound-type transformers is not nearly so satisfactory as for the ring type. In the wound-type transformer it is probably not permissible to neglect the secondary leakage reactance either in calculating the ratio curve or in determining the secondary saturation curve. When secondary saturation curves are run on this type of transformer, even if the supply voltage is sine wave, the excitation on the core would still be distorted due to the voltage drop caused when the exciting current flows through the secondary impedance of the transformer.

H. A. P. Langstaff (West Penn Power Company, Pittsburgh, Pa.): The authors of this interesting paper on the overcurrent performance of bushing-type current transformers have plainly demonstrated the value of the saturation and loss curves as a means of analyzing and predicting the operation of bushing-type current transformers under normal and overload conditions. The usual ratio and phase-angle curves furnished with bushing-type current transformers are lacking in ability to supply information required in the application of high-speed relays. It has been difficult to obtain such curves extended into short-circuit-current ranges. The information concerning method of calculation given in the appendix of the paper should prove very valuable to the engineer who must investigate the action of current transformers under short-circuit conditions for high-speed relaying. The publishing of saturation and loss curves for bushing-type current transformers would be very helpful and along with the ratio and phase-angle curves which are satisfactory for usual relay considerations will give the relay engineer valuable tools with which to work.

We wonder to what extent manufacturers are prepared to supply saturation and loss curves for current transformers already installed in breakers and built over a period of the last 10 or 15 years. Are such curves on file in the design engineers' historical data? We suspect that they are not available and that in most cases it would be necessary to obtain characteristic data in the field at considerable expense. The physical position of the transformers and of their primary lead will have some effect on the curves thus obtained as compared to laboratory-obtained data. However, this possible variation due to secondary leakage reactance may, according to the authors, be neglected for currents up to 25,000 or 30,000 amperes.

As a result of reading this paper we are more than ever of the opinion that more information should be given operating engineers concerning the characteristics of bushing-type current transformers and we recommend that manufacturers supply that data in the form of voltage-saturation and watt-loss curves.

P. O. Langguth (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The several papers in this session are strongly indicative of the interest and possible trend in the use of saturation characteristics to determine bushing-current-transformer performance. Transient and steady-state performance of transformers in combination with relay systems can be more completely analyzed by the use of these basic data.

The performance of power transformers is determined by the use of excitation and impedance characteristics. Likewise, at this time it is apropos to consider a standardized method of evaluating the output of current transformers. Normally, the ratio characteristic only is available to the user and a rough calculation from this is made to determine the overall transformer and relay performance. This information is usually satisfactory where normal speed relays and simple overcurrent protection schemes are involved. However, where differential, high-speed, or ground relays are used, ratio curves do not normally provide sufficient information for determination of relay settings.

Since this paper indicates methods by which the excitation characteristics of bushing transformers may be used in the solution of application problems, it would seem that standardization of their performance could be made on a similar basis. Modern relay practice is requiring an increasing amount of current-transformer data. The present method of supplying ratio curves and in rare cases phase-angle curves is an outgrowth of wound-type current-transformer and metering practices. Such procedure is relatively simple for this type of transformer in the metering range, because one set of curves will suffice for an entire line of transformers of a given general type. Due to space limitations, however, the performance of bushing transformers will vary with each breaker size, ratio, etc., requiring numerous curves for each ratio and voltage class. An attendant problem is the extension of these curves to 10 and 20 times normal current using special nonsaturating burdens as compared to the practical burden which may be saturating.

Therefore, this seems to be the proper time

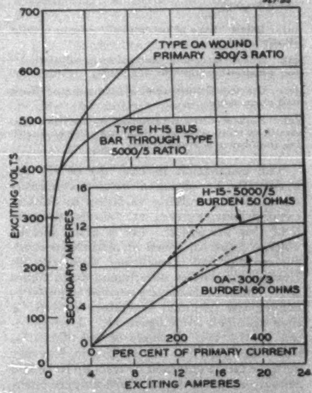


Figure 4. Ratio and excitation curves of current transformers

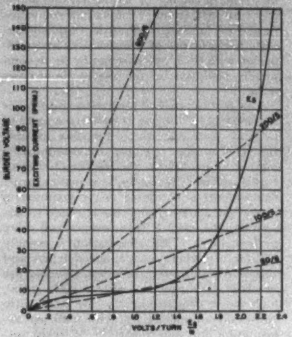


Figure 5a

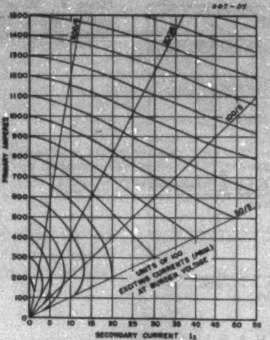


Figure 5b

to review our bushing-current-transformer practice from the standpoint of testing and publication of performance data. A single saturation characteristic for a transformer of each design is suggested as standardized procedure. This characteristic, including the watt and wattless components of the exciting current, could be drawn in one of several forms and extended well beyond the "knee of saturation". This thought can be carried further to obtain a measured standard of performance for each transformer size. A point selected beyond the saturation point would be well suited for this purpose.

The various papers indicate methods of using the saturation-curve data. However, in view of the proposed elimination of published ratio curves further reference is made to the methods given in this paper for calculating ratio, phase angle, and related relay problems. The extent of calculation depends on the accuracy required and the complexity of the relay scheme. The methods are listed in the order of their accuracy and complexity,

(*a*). Below "knee of saturation"—use turn ratio. (Ratio only)

(*b*). Above saturation approximate calculation—use simplified method in this paper. (Ratio only)

(*c*). Use complete method in this paper. (Ratio and phase angle)

(*d*). Represent the transformer in the equivalent circuit of relay scheme. (Use turn ratio and exciting impedance of transformer)

The calculation in case (*b*) or (*c*) can eventually be obtained from a composite curve chart involving variation in burden, ratio, primary current, etc., similar to figure 5 of this discussion. For a given burden voltage (case *a*), based on assumed secondary current and transformer ratio, the excitation current is determined from figure 5*a*. The actual primary current required to deliver the assumed secondary current is then obtained from figure 5*b*. A comparison of the two methods of calculation is shown in figure 6 of this discussion, together with the curve obtained by tests up to 70,000 primary amperes on a 3,000/5-ampere bushing current transformer.

Summarizing, the industry would be benefited by the adoption of a standard for circuit-breaker-bushing current-transformer performance which would provide:

1. Saturation characteristic for each transformer design, that is, for a given circuit breaker rating and voltage class.

2. Standardizing by means of a given voltage at a definite value of excitation current.

3. Ratio and phase-angle curves to 100 per cent normal current for the occasional transformer used in metering applications.

E. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper by Woods and Bottonari shows that, for bushing-type current transformers at least, the performance may be specified by three data.

1. The saturation curve giving exciting current or error current, versus voltage being induced by the transformer.

2. The exciting-loss curve, similar to a saturation curve but giving the loss component versus the voltage induced. This, together with the saturation curve fixes the power factor or phase angle of the error current.

3. The secondary leakage impedance, which added to the other burden of leads and relays determines the voltage the transformer must induce.

These three data comprise a complete description equivalent to the statement of ratio and phase angle. The expression and use of part or all of the data in this form for specifications or application work is usually referred to as the *saturation curve method*. Frequently only the saturation curve itself is needed. For many cases the secondary leakage impedance is also required. Only for the most exact work are the loss data also required. That these three data are equivalent to ratio and phase-angle curves, for the bushing-type transformers over a wide range of conditions has been shown in the paper by calculating the one from the other, with entirely adequate accuracy for overcurrent performance.

The paper by Camilli and TenBroeck (Transactions pages 547–50) shows equally good results for other than bushing types. However, there is still a question of the equivalence in certain types where primary leakage fluxes cause severe saturation of parts of the iron circuit.

The directness of the saturation-curve method has already swung many relay engineers over to its use. Many are clamoring for its adoption as the medium for standards of overcurrent performance. What, then is the case for and against the saturation-curve method in general, recognizing that it is much superior over-all for bushing types.

Against it, and for continued use of the ratio and phase-angle curves stand the following factors:

1. The ratio and phase-angle curves leave no loopholes for any misleading results with special designs. Regardless of the mechanism of action within the transformer they tell what the transformer will do as measured at its terminals under steady-state conditions and at the particular burden and power factor for which the test curves are taken. It is still a matter to be proved whether the saturation curve method will be adequate for all types of transformers.

2. The ratio and phase-angle curves extend the same method of performance expression used for metering at normal currents, into the relay field involving overcurrents. Since the same personnel is frequently involved in both problems this has its advantages.

On the other hand, *for* the saturation curve, stand the following prominent factors:

1. For a single current transformer feeding a burden of overcurrent relays or a trip coil, the error current and resulting secondary current may be determined readily. This can be done without the necessity of interpolating or estimating based on ratio curves taken possibly at a different power factor from the actual burden.

2. For transformers connected differentially the steady-state current in the differential connection is the vector difference between the error currents (given by the saturation curves) of the two transformers. The limiting case is generally when one transformer saturates and the others do not. The saturation curve gives directly the error current of the transformer which saturates and this is the false differential current. Ratio curves would not show this value unless the burden current happens to be in phase with the exciting current.

3. The saturation curve and secondary leakage impedance provide the necessary data to determine the performance when subjected to an asymmetrical primary current. This is of prime importance in differential protection of busses, generators, and transformers.

Instead of viewing saturation as reaching a certain flux density, it may be viewed directly as reaching a certain error current. A voltage may be determined from the a-c saturation curve corresponding to the particular exciting or error current which defines saturation in the particular problem. In my discussion of the Wentz-Sonnemann paper (Transactions pages 481–8) it will be shown that this voltage may be substituted for the quantities, flux density, iron area, and secondary turns in the formulas which determine whether or not saturation will occur and if so, when.

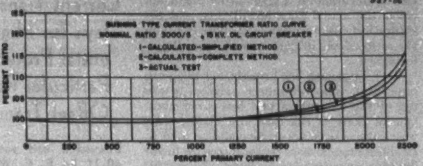
In conclusion, the advantages of the saturation-curve method are so great as to recommend its general use in preference to ratio and phase-angle curves. This has been shown to be feasible for bushing-type transformers over a wide range, and for many other types, although some test data seem to indicate that it may not be universal to all types. Further tests are desirable to show whether it may be sufficiently universal for standardization purposes for all types of current transformers. In any case its clarity and directness recommend its use quite generally for relay application work.

Roy M. Smith (Westinghouse Electric and Manufacturing Company, Newark, N. J.): This paper should be a real contribution to protection engineers, for it forms the basis of securing more accurate relay settings. In the past, considerable stress has been placed by relay men on obtaining precise overcurrent or distance settings, but many of the settings obtained were in error due to incorrect data, that is, incorrect short-circuit current values. Although the system of symmetrical components for the calculation of short-circuit currents has been available for some time, and is now familiar to most of us, and the d-c and a-c network analyzers can be used to simplify the calculations, the results have been merely approximate, since the real ratio of the current transformer has not often been used. This has resulted from the fact that the correction factors have not always covered the actual conditions met with. Now that a method of calculating the correct ratio for any condition is available, faster relay action should be possible. This is because any type of relay selection except definite time, automatically slows down the fault-clearing time when nominal current-transformer ratios are used in determining the secondary currents. Since the ratio always falls off at higher currents and sometimes quite badly, as indicated in figure 9*a*, slower relay times are bound to result unless the corrected ratio is used.

It is not meant that the correct ratio must be calculated for every relay setting, but where there is an effort to get the most from the relay in the form of high-speed protection, then the correct current-transformer ratio should be used.

Probably the greatest use of the procedure outlined in this paper will be the calculation of the "leakage" current through "dead" current transformers. This subject has been much discussed by relay engineers but my experience has shown that few people actually know the magnitude of the leakage for any single case. This factor is important in differential schemes and also in calculating the setting of an inverse-time overcurrent ground relay. Particularly is this latter true for a sensitive relay (and hence high burden) using a low-ratio bushing current transformer. Many operating men have found to their surprise that upon actual test a ground relay operated on less primary amperes when connected several taps above the minimum.

Figure 6



C. A. Woods, Jr., and S. A. Bottonari: The comments submitted on this paper by various members of the Institute indicate that the open-circuit saturation and loss characteristics of current transformers will become an exceedingly useful tool in the hands of the application engineer. These discussions call specific attention to the many points worthy of note in evaluating the material presented.

G. B. Dodds has stressed the usefulness of the saturation curve in determining the approximate linear performance of the current transformer, or in other terms, the burden capacity of the transformer, as indicated by the correlation of the data shown on the various curves given in his discussion. These data cover not only bushing-type current transformers, but also and to a greater extent, the wound and the bus-bar two-coil types. Even on these types, when neglecting the vector relationship between the burden and exciting currents, and the winding resistance, rough calculations indicate a fair correlation between the ratio performance and the data given on the saturation curves for all of these transformers. It is worth while to mention that the ratio curves in figure 4 were made by using extremely heavy resistance burdens, and a rather good approximation of performance may be calculated from the saturation curves if the vector relationship of the exciting and burden currents are taken into account.

A very timely question has been asked by H. A. P. Langstaff, relative to the possibility of furnishing saturation and loss data on bushing-type current transformers already in service. In a good many instances, the data are quite likely to be on file in the manufacturer's records, but where they are not available, they are quite readily obtained by field test. With the proper selection of instruments as outlined, saturation data, practically independent of wave form, may be easily obtained in the field without disturbing the transformer installation. The loss measurements, of course, require additional equipment, and the accuracy of field tests will probably not be as good as that made in the laboratory. However, extreme accuracy in the watt-loss measurements is not necessary. The effect of the secondary leakage reactance will be negligible when obtaining saturation characteristics.

The supplementary information contained in the discussion by C. C. Cole emphasizes the care which must be taken in measuring the excitation characteristics of the transformer with regard to wave shape, unless proper attention is paid to the type of meters used in making these measurements. Any appreciable error in the watt-loss measurements due to wave form would occur only in the region of high flux density, and yet, in this region, the relative magnitude of watt loss to exciting volt-amperes is such that correction for wave form is probably unnecessary.

As indicated in the paper, the authors have neglected the effect of harmonics in their calculations, and have assumed that the exciting current obtained by test is all fundamental. Based on data given in the paper, it is evident that unless the excitation current is held to sine wave, there will be some harmonics in this current. By adding the exciting current as all fundamental to the assumed secondary current, a primary current is calculated in excess of that which would be obtained if only the fundamental component of exciting current is used. Mr. Cole has pointed out the possible need of further analysis of the excitation characteristics by measuring both fundamental and rms values. The purpose of this would be to obtain some qualitative measure of the secondary current wave shape. It appears that any such procedure would require a similar analysis of the characteristics of the secondary burden. For example, with given value of primary current and burden impedance, a transformer will be operating at approximately the same flux density either when energizing a 100 per cent power factor burden or when energizing 50 per cent power factor nonsaturating burden. Oscillograms taken with these two burdens on a transformer operating at a high flux density show a marked difference in wave shape and crest values of secondary currents even though the rms values are approximately equal. Usually the excitation current with sine-wave impressed voltage will contain 80 to 85 per cent fundamental, and never less than 50 per cent. In a typical case, where the excitation current is 10 per cent of the assumed secondary current and contains 80 per cent fundamental, there is approximately 0.2 per cent difference between the fundamental and rms value of total primary current. Likewise, the error in calculation of primary current would be less than 2 per cent when assuming that all the rms exciting current is fundamental. Thus, only under conditions of severe saturation would the neglect of harmonics cause any appreciable error. In those relay applications, particularly differential protection, where harmonics might adversely affect the relays it would probably be desirable to use transformers of a design which would prevent a-c saturation. In this connection, the ratio curves which have been used in the past for the determination of current-transformer performance do not give any indication of the magnitude or character of the harmonics in the secondary current.

The viewpoint of an engineer actively engaged in the design and application of protective relays, as given by R. M. Smith is quite constructive. The effect of "dead" current transformers is forcibly brought to our attention by these comments, and it is evident that information for calculations of this type is not readily available from any other source of data.

E. L. Harder has very concisely reviewed the methods presented by the authors, and shown the superiority of the saturation and loss data over the ratio data for use in predicting the current-transformer performance. It is quite true that ratio and phase-angle data will continue to be the standard by which metering performance of all types of current transformers should be measured. The methods given in the paper cannot be applied, at present, equally well to the wound-type current transformers, but it is hoped additional work in the future will permit the use of the same methods to determine the overcurrent performance of these types of transformers. Quite often laboratory ratio tests do not fully portray the field performance due to the character of the secondary burdens, wave shape of the primary current, and the method of test. In addition, the design schedule of large installations and changing operating conditions may be such that definite test requirements are not available when the current transformers are available for laboratory tests. Hence, ratio curves made for a specific application on a given order might not cover the actual final installation, and the saturation curve would be of more use than the ratio curves in determining over-all performance.

P. O. Langguth has brought out two very important items of interest relative to the use of the saturation and loss-curve data. One of these is the ease and clarity with which the saturation curve could be used to establish standards of performance for bushing-type current transformers. As stated, due to the many variations and restrictions in the design of bushing-type current transformers as applied to circuit breakers, standardization for the various voltage classes and ratings of breakers becomes quite burdensome. This is particularly true when it is necessary to use some form of the ratio curve for such standardization. It appears that a material simplification would result by taking a definite value of excitation, such as ten-ampere, on a predetermined ratio. Such a point will always give a voltage over the so-called "knee" of the saturation curve, which, with the secondary resistance, would establish a standard level of design. Second, the possibility of adapting the formulas as given in the paper to various forms of compound curves has been well illustrated. The curves in figures 5*a* and 5*b*, illustrate one form which such compound curves could take, and with additional effort, the more accurate method for determining ratio could be similarly illustrated.

Figure 6 of this discussion illustrates the correlation between the calculated values by the simplified and the complete method as compared to actual tests for currents up to 75,000 amperes primary current. This illustrates the conservative viewpoint taken by the authors in limiting their statement on accuracy of the methods to 25,000 or 30,000 amperes primary current. Undoubtedly, with the accumulation of additional data, the method can be substantiated for all bushing-type current transformers in commercial use.

It will be noted throughout the paper and discussions that more attention has been paid to the saturation characteristics than to the loss characteristics of the transformer. The reasons are more or less obvious, but it might be of interest to point out that the excitation angle, θ_1 shown in figure 7 of the paper, is usually between 45 and 90 degrees. As the transformer core becomes saturated, its excitation angle rapidly approaches 87 or 88 degrees. Under these conditions, the watt-loss current is practically negligible compared to the magnetizing current, and hence it can often be neglected in making the various calculations.

The methods outlined in the paper are particularly applicable to the solution of relay problems. Since the majority of bushing-type current transformers are used in conjunction with protective relays, it is our hope that the saturation-curve method will become familiar to all engineers making such applications. Likewise, the solution of problems involving the transformer performance when subjected to offset transient currents (reference 3 of the paper) requires the use of the open-circuit saturation characteristics. Hence, it seems logical that the open-circuit saturation and watt-loss curves should supersede the ratio curves for defining the overcurrent characteristics of bushing-type current transformers.

Current Transformers and Relays for High-Speed Differential Protection, With Particular Reference to Offset Transient Currents

Discussion and authors' closure of paper 40-71 by E. C. Wentz and W. K. Sonnemann, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 481–8.

T. R. Halman (The Detroit Edison Company, Detroit, Mich.): The methods described in the paper by Messrs. Wentz and Sonnemann for determining the performance of current transformers under high values of primary current involving fully offset waves should be of great value to those interested in applying instantaneous bus differential protection. The problem is particularly important where it is desired to use existing current transformers having relatively low ratios and moderate-sized cores.

A practical example of such a problem arose on the system of The Detroit Edison Company in 1933. A large number of 120-kv busses was to be protected with instantaneous differential relays using 400-to-5 current transformers which were already installed in the breakers.

Before starting on the installation a trial setup was made using three current transformers, similar to those in the breakers, connected differentially to reproduce the actual service application as closely as possible. Current was applied unequally through the primaries in a manner corresponding to conditions existing during faults external to the protected zone and up to the maximum magnitude expected on the system which was 7,500 amperes. This should give no current through the differential relays but actually on some trials as much as 100 amperes flowed for a brief period.

This would, of course, cause incorrect operation as was demonstrated by observation of a test relay.

The problem was solved by taking advantage of a principle which is mentioned in the paper under discussion. That is, an increase in the impedance in series with the differential relays will reduce the transient current through them. An equally important corollary is that this impedance does not affect the steady-state current nearly so much as the transient and so does not prevent the current transformers from performing their desired functions satisfactorily within certain limits to be mentioned later. Resistance was found more effective than reactance, also as mentioned in the paper.

It was decided, accordingly, to insert 12 ohms of resistance in series with the differential-relay operating coils. Tests showed that this would eliminate the trouble and give at least a two-to-one factor of safety.

Of course, this high differential burden would cause the current transformers to saturate badly when forcing current through the relays in case of a high-current fault within the zone of differential protection. Tests showed, however, that they would still furnish enough current to permit proper operation of the differential protection when required except under system conditions giving minimum values of fault current. To take care of this case, an additional relay was connected to short out the resistor after one-half second. This delay permits the transient to subside and the differential may then safely be made more sensitive. This method results in instantaneous tripping for all severe faults with a slight time delay for minimum faults. A total of 126 bus years of operation have been experienced with this scheme. There have been two correct operations and no failures nor unnecessary tripouts.

A. R. Miller (Lehigh University, Bethlehem Pa.): The following method of analysis for current transformers operating under saturated-core conditions is based upon standard circuit theory and may offer a more readily understood method than those suggested or referred to in the papers relating to this topic. In order to conserve space the procedure is presented in the simplest fashion but can be carried out to any degree of accuracy under a given set of assumptions.

In order to solve equations pertaining to circuit theory in a practical way it is required that the circuit parameters be held fixed (there are certain exceptions to this statement), that is, the values of resistance, inductance, and capacitance are fixed quantities. However, when saturation occurs, the inductance of the circuit changes, or as we commonly say, the ratio of flux to exciting current is not fixed, as is indicated by a saturation curve. However, as the authors of these papers have assumed, this nonlinear characteristic can be approximated by two or more linear relationships as is indicated in figure 1 of this discussion, where two straight lines are used to approximate the magnetization curve. For values of exciting current less than i_1 the inductance is proportional to the slope of the first straight line *A*; when greater than i_1, it is proportional to the second straight line *B*, assuming the saturation curve to be approximated by two straight lines. This would usually be sufficient, but closer approximation would be

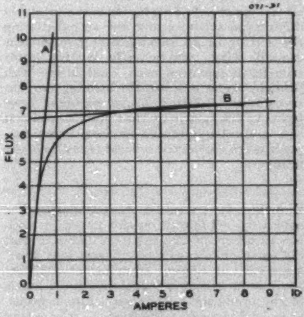


Figure 1

obtained by using a greater number of straight lines.

In calculating the current in the circuit, it may be assumed that the inductance is changed from a value of L_1 to L_2 when the current passes through the value i_1. In a magnetic circuit, the point i_1 at which this fictitious switching occurs, would depend upon the initial state of magnetization and whether the flux is increasing or decreasing in value. That is, in general, the hysteresis characteristics of the core would have to be known. But no matter what type of solution is used, as long as the current phenomena is dependent upon the hysteresis characteristics of the core material, this would be the case.

The calculations are outlined on the basis of the diagram in figure 2 of this discussion, which corresponds with the equivalent circuits used by the several authors in their papers. As the current in the exciting coil goes through increasingly larger values, and when a value of i_1 (figure 1) is reached, the switch S is assumed to be closed. Mathematically this is done by superimposing a current on the circuit which, together with the initial current that is assumed to flow through this part of the circuit, produces zero voltage across that portion of the circuit shunted by the switch S circuit. This satisfies the condition of the circuit and the circuital equations and is therefore a complete solution. With the above explanation it is believed the following analysis is self-explanatory.

Let

$$i = I_1\epsilon^{-at} + I_2\epsilon^{-bt}\epsilon^{j\omega t}$$

then

$$i_2 = \frac{Z'}{Z'+Z}\,i = \frac{L'p}{L'p+Lp}\,(I_1\epsilon^{-at} + I_2\epsilon^{-bt}\epsilon^{j\omega t})$$

where

$$Z' = Z_1 + Z_2,\ p = d/dt$$

and the resistances are assumed to be zero throughout the circuit. Under these assumptions the operational terms drop out and

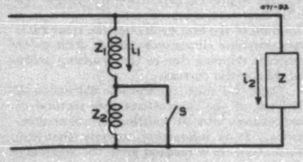


Figure 2

$$i_2 = \frac{L'}{L'+L}\,(I_1\epsilon^{-at} + I_2\epsilon^{-bt}\epsilon^{j\omega t})$$

The voltage across the switch S produced by the initial current is

$$e_s = i_1 L_2 p = L_2 p \frac{L}{L'+L}\,(I_1\epsilon^{-at} + I_2\epsilon^{-bt}\epsilon^{j\omega t})$$

Therefore, at saturation a new current Δi_2 is added to i_2, and

$$\Delta i_2 = e_s/Z_1 + Z$$

since it may be assumed that the impedance of the source is high with respect to Z_2. This gives, as the increase in the current in the secondary circuit of the current transformer

$$\Delta i_2 = \frac{L_2 L}{(L'+L)(L_1+L)}\,(I_1\epsilon^{-at} + I_2\epsilon^{-bt}\epsilon^{j\omega t})$$

and, after the current passes the value i_1, and until the instant when the saturation is assumed to change again

$$i_2 + \Delta i_2 = \left(\frac{L'}{L'+L_2} + \frac{L_2 L}{(L'+L)(L_1+L)}\right)\times (I_1\epsilon^{-at} + I_2\epsilon^{-bt}\epsilon^{j\omega t})$$

Taking the following to be relative values for a current transformer,

$$L_1 = 0.1L';\ L = 0.01L';\ L_2 = 0.9L'$$

$$i_2 + \Delta i_2 = 0.91(I_1\epsilon^{-at} + I_2\epsilon^{-bt}\epsilon^{j\omega t})$$

approximately during this interval of saturation.

The differential current between a perfect current transformer and a saturated transformer with the characteristics assumed would be approximately ten per cent of the primary current.

The inclusion of resistance in the secondary circuit would somewhat complicate the analysis but does not make it impossible. The relative value of resistance and reactance would ordinarily be such as to make the effect of secondary resistance quite negligible.

H. P. Sleeper (Public Service Electric and Gas Company, Newark, N. J.): This paper discusses a phase of the current-transformer problem in connection with relay protection which has but recently been recognized. Heretofore, ratio curves were considered to provide sufficient data for analyzing protective relay performance under conditions of known volt-ampere load and primary current. This picture has been entirely changed within the last two years, and the paper by these authors is one of the very first to discuss the problems incurred by offset transient currents. Since differential protection is coming into popular use in generating stations where such conditions may readily be present, it is most important that protection engineers be familiar with the problems presented to such relays.

This very situation was recently the subject of considerable study in connection with proposed differential protection on the 13.8-kv busses of a generating station on the system with which this writer is connected. The maximum value of short-circuit current was calculated to be approximately 60,000 amperes rms, and the system time constant by calculation was 0.21 second. Generating capacity of approximately 150,000 kva would be connected to the busses, and all elements of the problem described in the authors' paper would be present.

In order to ensure correct relay operation there appeared to be the following methods of solution:

1. Use of standard current transformers and differential relays, and application of a time delay of from 0.5 to 1.0 second to provide sufficient delay to permit the offset transient to recover. This would allow the relays on the unaffected busses to operate incorrectly, but the time delay would prevent the occurrence of false tripping, during which period the d-c effect would disappear and the relays would reset. By standard current transformers is meant those which have six to eight square inches of iron core.

2. Use of standard current transformers which, it should be understood, would not saturate on maximum alternating currents but would saturate when offset transients occur, and the use of two sets of Westinghouse type *CA-6* differential relays with multiple restraint coils. This scheme was described in a paper by Smith, Sonnemann, and Dodds, which is published on page 243 of the 1939 AIEE TRANSACTIONS.

Six relays are employed and the contacts of the two relays on the same phase are connected in series. Incorrect operation is theoretically prevented by the proper grouping of current transformers so as always to maintain restraint on at least one relay per phase during conditions when the offset transient tends to saturate the current transformers receiving the maximum currents.

3. Installation of current transformers with sufficient iron so that saturation does not occur due to the offset transient currents. This theoretically enables any standard differential relay to be applied.

An analysis of all factors led us to the following conclusions:

The number 1 scheme was not considered good operating practice because of the amount of damage at the point of fault and the shock to the system which would result from hanging on to faults of the magnitude and durations indicated. It was deemed inadvisable to use two relays in series (scheme 2) instead of one due to the expense involved, the difficulties of phasing, and the hazards of contacts in series. Therefore, the conclusion was to use the oversize current transformers (scheme 3). These transformers had approximately 20.5 square inches of iron in the core and were approximately 19.5 inches square and 11.5 inches long measured lengthwise of the conductor. The aperture for the conductor bushing was 8 inches in diameter. The completed unit weighed approximately 600 pounds.

It is recognized that the expense of such an installation cannot always be justified, and in some cases it is impossible to install current transformers with this amount of iron. The latter situation, of course, applies particularly to current transformers used in connection with existing outdoor circuit breakers. It appears, therefore, from the viewpoint of the operating engineer that the practical and economical solution of the problem has not yet been obtained.

Viewed abstractly there seem to be several possible avenues of attack to solve the problem. Suggestions are:

1. Development of an iron of such characteristics that, with a standard cross section of six to eight square inches, would not saturate under conditions presented by offset transient currents.

2. Use of secondary corrective devices which would eliminate the effects of d-c saturation.

3. Provision of wells on outdoor circuit breakers sufficiently large to receive oversize current transformers if the latter are dictated.

Still another problem is present in such differential relay protection which even the oversize current transformer does not solve. This occurs when ground differential relays are used and faults occur between two phases which leave the third phase with only load current. D-c transients will appear in the two phases and not in the third, and hence a resultant current will enter the ground relay which will have no compensating restraint. This will cause incorrect operation unless this relay has a suitable time delay, or is interlocked with another relay which responds to neutral current only. It is important that provisions be made to cope with this situation, which is inevitable under the conditions described.

R. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper is important in that it supplies to relay engineers, for the first time, a really practical method for predetermining current-transformer performance under transient conditions. It also gives an associated method for utilizing this information in predetermining relay performance. Both steps are simple, well grounded in fundamentals, adequately supported by test, and have proved an invaluable aid in making with assurance numerous bus protective applications since the method has been available.

I wish to give a graphic picture of the current-transformer flux during the transient interval, and preceding saturation, to clarify some physical aspects of this problem.

Figure 3 of this discussion shows for the simplest case of a perfect transformer and pure resistance burden, the various current, voltage, and flux waves. The fully offset current wave is broken up into symmetrical and transient components. These, in flowing through the resistance burden (which includes leads and transformer secondary winding resistance), create voltages as shown, which must be generated by flux change in the transformer. The flux variations required to generate the symmetrical and transient voltage waves are shown in the next two traces and then combined to show the total flux variation required in the transformer.

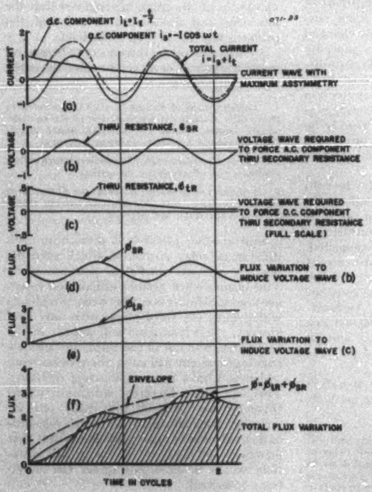


Figure 3. Current-transformer flux during assymetrical short circuit, one-cycle time constant

The resulting flux wave, figure 3*f*, has been shown for a one cycle time constant. Figure 4 gives the corresponding flux wave for a ten-cycle time constant.

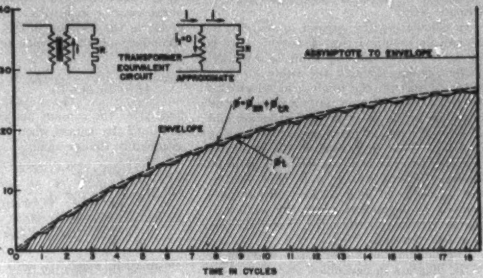


Figure 4. Current-transformer flux during assymetrical short circuit, ten-cycle time constant

The transformer saturates whenever this combined flux wave reaches saturation value. To be on the safe side the authors have used the upper envelope of the flux wave, their equation 10 giving the time for this envelope to reach the saturation value B_sA, or for the flux density to reach the saturation value B_s.

With but slight approximation when the time constant is longer than three cycles the term $1/\omega T$ can be omitted and a curve plotted, figure 5, giving the time to saturate as a function of the quantity NA/IRT for a particular assumed saturation density. The time may then be read as a fraction of the time constant. For example, with $(NA \div IRT) = 3$ the time to saturate is 0.26 "time constants", or 2.6 cycles for a 10-cycle time constant, 5.2 cycles for a 20-cycle time constant, etc.

The approximation of omitting the term $1/2\pi T$ amounts to neglecting the a-c flux density altogether. Hence figure 5 actually gives the time for the transient flux component to reach saturation density. The ratio of maximum a-c density to final transient density, is, for pure resistance burden, simply $1/2\pi T$, for time constant T expressed in cycles. In other words if T is three or more, the a-c density is not over $^1/_{19}$th of the maximum density which would be reached by the transient component alone, assuming no saturation. For $T = 10$ cycles the maximum transient density is 62.8 times the maximum a-c density. Obviously then, if the time to saturate being computed is longer than a time constant the error of the approximation could be offset by an assumed saturation density but few per cent different. If the time to saturate being calculated is a fraction of a time constant, the difference in time between passage of the transient component through a particular density, figure 5, or passage of the envelope through the same density, equation 10, is but a small fraction of a cycle, and hence does not affect the calculations in which the results are used.

The value of $(NA/IRT) = 13$ corresponding to infinite time to saturate is of particular significance in that it describes the conditions required to prevent saturation in the transformer. Satisfactory relaying can generally be secured if saturation would not occur due to the a-c component alone. With a ten-cycle time constant and allowing an a-c density of 10,000 gausses, this means that satisfactory relaying can be secured when NA/IRT calculates but $^1/_{30}$th of enough to prevent saturation altogether. In this case saturation occurs, of course, in a fraction of the first cycle, but the relay ratio and impulse characteristics are such as to prevent tripping due to the resulting peaks of differential current.

There has been considerable discussion of the use of the saturation-curve method of expressing current-transformer characteristics. It is interesting to note that the saturation-curve method gives the necessary data to determine transient performance also; that is, time to saturate, and differential current peaks following saturation. In a differential relay problem the worst case is usually when one transformer saturates and the others do not. The false differential current is then the error current or exciting current of the transformer which saturates. Saturation may be defined, then, as reaching a particular exciting current. For example, if a differential relay has minimum trip value of one ampere, an exciting current of 0.1 or 0.25 ampere might be chosen as the threshold of saturation since exciting currents below these values are unimportant. Use 0.1 ampere for example.

When a d-c transient current flows, the exciting current reaches a value of approxi-

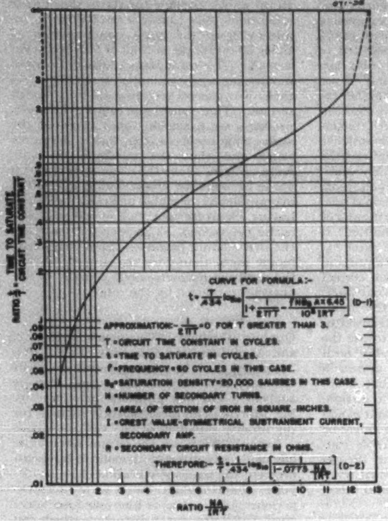


Figure 5. Current-transformer time to saturate

mately $0.1\times\sqrt{2}$* ampere instantaneous value, when the flux reaches the value associated with 0.1-ampere a-c exciting current under steady-state conditions.

Suppose then that from the saturation curve is read the voltage, E_s, which the transformer induces with the a-c exciting current, I_s, equal to 0.1 ampere in this case. The value of NB_sA to substitute in the equation of figure 5, is given from the relation of induced voltage to NB_sA, which is

$$E_s = 4.44 fNB_sA6.45\times 10^{-8} \tag{3}$$

or

$$\frac{fNB_sA6.45}{10^8}=\frac{E_s}{4.44} \tag{4}$$

Making this substitution in the equation of figure 5

$$t=\frac{T}{0.434}\log_{10}\left[\frac{1}{1+\frac{1}{2\pi T}-\frac{E_s}{4.44\,IRT}}\right] \tag{5}$$

which is obviously simpler than the original expression.

Also a curve similar to figure 5 can then be plotted against E_s/IRT as an abscissa, see figure 6 of this discussion. It has the advantage of always involving a saturation density corresponding to an exciting current I_s selected for the particular relay problem rather than using an arbitrary density.

Recapitulating:

1. From the a-c saturation curve may be chosen a saturation voltage E_s corresponding to an error current I_s.

2. Let the transformer be subjected to a fully offset primary current wave of time constant, T.

If E_s is substituted in equation 5 it gives the time for the transformer exciting current to reach a magnitude equal to the crest value of I_s. Or figure 6 may be used to determine whether saturation will occur and if so, when.

The remainder of the relay application method is of course unchanged.

Thus the saturation curve method of presenting the transformer characteristics lends itself especially well to the analysis of transient conditions.

E. C. Wentz and W. K. Sonnemann: Mr. Halman has presented an interesting picture of the experience of his company with reference to the use of a simple overcurrent differential relay. It is of interest to note that it is often practical to insert a great deal more resistance in the differential circuit than could be tolerated connected directly in series with the current-transformer secondary.

Mr. Harder has presented a valuable discussion and has furnished useful curves for the utilization of the authors' formulas. Mr. Harder's figure 5 should be used with caution, we believe, because of the saturation density of the 20,000 gausses quoted. This value is likely to be too high for the lower-ratio current transformers. However, the curve and formula of figure 6 are more useful in that the corresponding formula of the paper is interpreted directly in terms of the saturation curve. This formula represents a real simplification of equation 10 of the paper with regard to practical application.

Mr. Miller has presented an interesting discourse attacking the same problem with the benefit of operational calculus. However, it is not obvious from Mr. Miller's mathematics just how he arrives at the conclusions he does in the closing part of the discussion. For an example, any values of inductance may be chosen within reason, as Mr. Miller has done, but the authors have found that in most cases the differential current between a perfect current transformer and a saturated current transformer will run much higher than ten per cent. More important than this, however, is the erroneous conclusion that the secondary resistance is

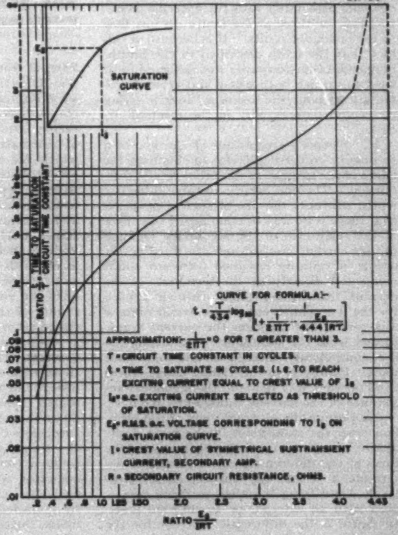


Figure 6. Current-transformer time to saturate

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* Form factor or ratio of crest forms is somewhat greater than $\sqrt{2}$, probably between $\sqrt{2}$ and 2.

quite negligible. Perhaps we have misunderstood Mr. Miller's statement, but there is no question of doubt, both from the theoretical standpoint and from test results, that the secondary resistance is by far the most important factor in determining current transformer performance on asymmetrical currents.

Mr. Sleeper's discussion is interesting through its presentation of the problems involved in choosing the proper relays and by his abstract view of possibilities for the future. With reference to existing possible relay schemes, we feel that Mr. Sleeper overemphasizes the difficulties of the use of two relays per phase as brought out in the Smith-Sonnemann-Dodds paper referred to. With particular reference to the use of contacts in series for the important function of tripping a bus, there is ample precedent for this in past relay practice. For example, directional overcurrent relays have been utilized for years in which two contacts in series must close to perform the tripping function. The principal difference is that in the case of the directional overcurrent relay the two contacts are in the same relay case, whereas in this case the contacts are in separate relays.

Mr. Sleeper also brings out that there is a possible false differential current involved for ground relays in the event of external phase-to-phase faults. This would be caused by unequal saturation of the current transformers in the two phases because of dissimilarities, even between nominally duplicate current transformers. More important than this, however, is the case of an external three-phase fault. In this case it is immediately obvious that not all of the three phases can have the same magnitude of d-c transient. Hence, for the external three-phase fault, there is even more possibility of a ground differential relay being subject to incorrect operation because of insufficient restraint. This is explained by the fact that the heavy currents associated with the external phase-to-phase or three-phase faults do not flow in the residual circuits. It is because of this fact that the relay described in the Smith-Sonnemann-Dodds paper was designed to be used to furnish both phase and ground protection without the use of a "fourth" relay connected differentially in the residual circuit.

Mr. Sleeper's suggestion of the use of a secondary correcting device to eliminate the effect of d-c saturation has often received attention. The one insurmountable difficulty which prevents the use of such a device is that there is no way of eliminating the effect of the resistance of the secondary winding of the current transformer itself. For example, a suitably designed transient shunt connected directly at the terminals of the current transformer might furnish a path for the d-c component which would materially reduce the duty on the current transformer from the standpoint of shunting the d-c component through a low resistance path. However, as mentioned above, there is no possible way to avoid the secondary resistance of the current transformer itself.

Continuing the abstract view of the problem from the standpoint of the most desirable relay, it is interesting to consider the form of the differential current itself to determine if it has any characteristics positively identifying it either as being associated with an external or an internal fault. In figure 7, the differential current for the cycle between the two highest peaks was analyzed and it was found that expressing the fundamental 60-cycle component as 100 per cent, the d-c component amounted to 329 per cent while the second and third harmonics amounted to 9.9 per cent and 3.6 per cent, respectively. Harmonics of higher order were 1 per cent or less. If it is assumed that the total harmonic components amount to 15 per cent, then it is seen that in order to adequately restrain a high-speed differential relay, the relay must be particularly sensitive to harmonics. For the above condition, it would appear that the relay should adequately restrain on 10 per cent harmonics or less to furnish sufficient safety factor. If such sensitivity were secured, the same exceedingly small percentage of harmonics in the differential current would block correct tripping of the relay for an internal fault. This would immediately involve the necessity for very high-quality current transformers. A second thought would be to use the d-c component to restrain the relay until such time as the harmonics finally appear in significant quantities. It is immediately obvious, however, that the same d-c component in the event of an internal fault would delay the relay so that it would no longer be a high-speed relay. The inescapable conclusion to be reached is that, so far, the use of fault current itself forms the most reliable means of restraining a differential relay.

Harmonic Theory of Noise in Induction Motors

Discussion and author's closure of paper 40-13 by Wayne J. Morrill, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 474–80.

Samuel Noodleman (Delco Products, Dayton, Ohio): The author has presented a very interesting attack on the problem of magnetic noise in single-phase motors. The theory is unique in that it develops a method of calculating the actual harmonic forces which act on the stator. There are, however, conditions which do not seem to agree with the results of this paper. For example, from the assumption of a uniform air gap and constant permeance it would be assumed that the magnetic noise of motors with tunnel-slot rotors would check very nicely with this theory. Actually the magnetic frequency spectra of motors with open-slot rotors agree much closer with the results of this paper than do those having closed rotor slots.

Motors with tunnel-slot rotors have predominating frequencies which are lower than those with similar open-slot rotors and thus lower than would be predicted from the presented theory. For example, in a four-pole 60-cycle motor having 32 stator slots and 48 rotor slots the magnetic noise at no load with the tunnel-slot rotor has a predominating frequency of 480 cycles per second, while the open-slot rotor has a frequency of 1,440 cycles per second. This may be explained by considering the frequency of the harmonic fluxes generated by the stator slots. In the above example the stator slots create a harmonic flux having a predominant frequency from the noise standpoant of 32 times 30 cycles per second or 960 cycles per second. The rotor slots similarly create a flux having a predominating frequency of 48 times 30 cycles per second or 1,440 cycles per second. The tunnel-slot rotor, however, produces its harmonic tones at about the same magnitude as the stator harmonic tones and thus the resultant magnetic noise contains a tone the magnitude of which varies between the sum and difference of the magnitudes of the stator and rotor tones and having a beat frequency of, in this case, 480 cycles per second. The open-slot rotor, on the other hand, produces much larger harmonic notes than does the stator and thus the resulting magnetic noise consists of frequencies produced by the rotor.

Quentin Graham (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper gives an excellent summary of a very useful method of noise analysis. The new notation introduced by Mr. Morrill in his force charts is valuable for the orderly handling of the various waves.

I should like to ask the author whether he has made any attempt to confirm by measurement the mode of vibration of the stator yoke. I feel certain, based on my own experience, that a two- or four-pole force wave gives a definite stator distortion which can be measured. However, when the number of force poles becomes 16, 24, or 32 on a fractional-horsepower motor it would appear that the stiffness of the yoke would be very great for the same mode of vibration. I admit that the noise frequencies are usually in agreement with the theory as outlined but I have often wondered if perhaps the teeth were not vibrating circumferentially more than the stator yoke in cases involving a high number of force poles. It would be useful to know just where the surfaces are that set the air waves in motion. Has the author explored the possibilities of any other types of vibration?

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Mr. Morrill's paper presents a method for predicting the frequencies of some of the noise components in a single-phase induction motor. His general method of approach is to set up equations for the flux waves due to the principal harmonics in the magnetomotive-force waves of both stator and rotor and to compute the forces due to the interaction of these various harmonic flux waves, and their frequencies. Such an approach necessarily leads to mathematics sufficiently involved that the author introduces a new notation. It is interesting to note that by a much simpler approach it can be shown that these same frequencies exist. By this same simpler approach it becomes evident that the frequencies given by Mr. Morrill depend solely upon the number of rotor teeth and revolutions per minute and not upon the number of stator teeth or upon the combination.

Consider any single stator tooth carrying flux. As each rotor tooth passes a stator tooth, a pull is exerted on the latter. If there are 48 rotor teeth revolving at 1,800 rpm or 30 revolutions per second, the frequency of the radial force on the stator tooth is

$48 \times 30 = 1{,}440$ cycles per second

If the stator is energized at 60 cycles, the frequency of the force of attraction between the rotor and the stator is 120 cycles. This 120-cycle force is superimposed upon the 1,440 cycles giving two side-band frequencies either side of 1,440. The three frequencies are, therefore,

$1{,}440 - 120 = 1{,}320$ cycles per second
$1{,}440$ cycles per second
$1{,}440 + 120 = 1{,}560$ cycles per second

which are the identical frequencies given in figure 3 of the author's paper.

The second harmonic of the stator tooth pulsation frequency due to the rotor teeth, and the 120-cycle side-band frequencies are,

$2 \times 48 \times 30 = 2{,}880$ cycles per second
$2{,}880 - 120 = 2{,}760$ cycles per second
$2{,}880 + 120 = 3{,}000$ cycles per second

The third harmonic and accompanying side-band frequencies are

$3 \times 48 \times 30 = 4{,}320$ cycles per second
$4{,}320 - 120 = 4{,}200$ cycles per second
$4{,}320 + 120 = 4{,}440$ cycles per second

Observe that these latter six frequencies are the remaining six frequencies shown in the calculated noise force spectrum in figure 4.

It is interesting to note that all of the frequency components given by Mr. Morrill are explained by the simpler method of approach just outlined. By this same simple method of approach, it is seen at once, that the three frequencies listed for each combination in the author's chart VII are the fundamental tooth-frequency pulsations set up by the rotor teeth and the two 120-cycle side bands.

Using this method of approach, which might be called the "permeance method", or the "reluctance method" it is evident that all the frequencies given by Mr. Morrill are due *solely to the number of rotor teeth.* I would like to ask Mr. Morrill if he agrees with this conclusion and also if it is evident from his method of analysis.

Pursuing the permeance method as outlined, we might expect to find other frequencies present which are a function of the number of stator teeth. The pulsation frequency in the rotor teeth at 1,800 rpm, with 36 stator teeth is

$36 \times 30 = 1{,}080$ cycles per second

We might logically expect a pull between the stator and rotor at frequencies of

$1{,}080$ cycles per second,

$1{,}080 - 120 = 960$ cycles per second

and

$1{,}080 + 120 = 1{,}200$ cycles per second

We might also expect to find frequencies of

$2 \times 36 \times 30 = 2{,}040$ cycles per second
$2{,}040 - 120 = 1{,}920$ cycles per second
$2{,}040 + 120 = 2{,}160$ cycles per second, etc.

I have actually found a number of these frequencies experimentally, though even when present they are of less importance than the frequencies due to the rotor teeth. I should like to inquire of Mr. Morrill if his method of analysis would lead him to predict these frequencies which are dependent upon the number of stator teeth, and if he has calculated their relative magnitudes. Also, has he ever observed or looked for any of these frequencies experimentally?

The permeance method as outlined above checks the author's "magnetomotive-force method" because the author uses only tooth harmonics of the magnetomotive-force and not those other harmonics which can be suppressed by a good winding distribution.

The permeance method is given in this discussion to supplement and not to supplant Mr. Morrill's method. I believe the former method is valuable in that it can be used to predict the important frequencies present by a very simple arithmetical computation and can, therefore, be used to check the frequency even if the author's more involved mathematical method is used. The author's method has the advantage, however, of leading to quantitative results, although, as he himself admits, it would be necessary to take into account variations in permeance if a complete analysis were to be made. I believe Mr. Morrill's paper is a worth-while and useful contribution to the art.

Wayne J. Morrill: While I agree with Mr. Veinott's comments concerning the noise frequency being dependent upon the rotor tooth frequency and often make use of the same method to determine where to look for the various component frequencies in a motor under test, I do not feel that the possible noise frequencies are of much help in predetermining the noise of a motor. It is well known that with the same rotor the magnitude and quality of the produced noise will vary greatly with the number of stator slots used. Experience shows that the difference between the number of rotor slots and the number of stator slots is a better criterion of the noise which may be expected than is either the number of slots in the rotor or the number in the stator. My method uses both the stator and rotor slots and attempts to predetermine not only the frequency of the motor vibration but the magnitudes of the magnetic forces and the modes of vibration as well. I believe that a successful theory for motor noise must take into account all these factors.

I do not agree with Mr. Veinott's expectation concerning noise frequencies approximating to the frequency of the stator teeth with respect to the rotor. I believe he is overlooking the fact that the rotor teeth are moving and that therefore it could not be expected that a flux in the rotor would produce an air noise of the same frequency.

I have looked for frequencies other than those outlined in my paper and while I have frequently found others they have been small, as Mr. Veinott says, and they have not necessarily recurred in successive motors of the same design. I have concluded that these other noises, being inconsistent in frequency, must be due to deviations of the motor from the ideal. An eccentric air gap, a high-resistance squirrel-cage bar, or a buzzing lamination are all deviations of this type and should cause noise frequencies and magnitudes to change.

I agree with Mr. Graham that as a rule the stiffness of the stator yoke should greatly limit the noise where 16 or more force poles exist, unless resonance happens to be present.

The tangential forces on the teeth are so much smaller than the radial forces that it seems reasonable to neglect these forces. Undoubtedly, however, the stator teeth and windings load the stator yoke and affect its vibration.

We have looked for other types of vibration but believe the radial motion of the stator yoke to be the important one.

Mr. Noodleman's comments concerning the effect of closed rotor slots upon the noise spectrum. With closed slots magnetomotive-force differentials occur on the rotor surface only as a result of saturation of the magnetic bridges above the bars. It would be expected that such saturation would have the effect of rounding the corners of the rotor magnetomotive-force stair-step curve corresponding to the dotted portion of my figure 1. It is well known that rounding the corners of such a curve has the effect of minimizing the higher harmonic components. It would seem logical, therefore, for a closed-slot rotor to act with the stator to produce less of the high frequencies than would an open-slot rotor.

Our experience has been that the closed-slot rotor has all the frequencies of the open-slot rotor but the higher ones are reduced in magnitude. It would seem that a closed-slot rotor would, therefore, always be better for noise than an open-slot rotor but we have not found it so. It seems that, due to variations in the thickness of the magnetic bridge or the harmonics of saturation, the closed-slot rotor produces some new low pitched noises all its own. The over-all noise instead of being reduced by the closed-slot rotor seems actually to be increased though, due to the large low-frequency component, it is lowered in average frequency.

The Design and Operation of a Metropolitan Electrical System From the Viewpoint of Possible Major Shutdowns

Discussion and closure for the author of paper 40-30 by S. M. Dean, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published ,in AIEE TRANSACTIONS, 1940 (October section) pages 575–9.

W. C. Stone: See discussion, page 1154.

W. J. Lyman (Duquesne Light Company, Pittsburgh, Pa.): The so-called "loose link" system described in Mr. Dean's paper is an excellent example of a sound principle of system design which strikes an economic balance between two opposing relationships. In general, the more extensive the generation and distribution system which runs tied together in a close network, the better is the generating economy and the more efficiently are the circuit capacities utilized. In contrast to this, as the extent and complexity of the network is increased, the protection becomes more expensive, short-circuit currents require larger breakers, and many of the operating problems become difficult, particularly during trouble. These two opposing tendencies fix an economic limit to the size of the system which should be closely tied together. Following this principle, a large system can be divided into two or more parts, loosely linked together, without any over-all sacrifice in economy and with much better operating characteristics. Relatively high reactances can be placed at natural division points which normally carry little load but which can still take advantage of the diversity between load areas through moderate interchange of power.

As Messrs. Bryson and Hayward brought out so well in their paper, the Duquesne Light Company system is divided into two parts by reactors at the two main power stations. Each half therefore consists of one half of the 66-kv ring and one half of each power plant. Before these reactors were installed (1928), the 66-kv lines operated on one bus at each power station, and heavy line faults near a power station pulled down the voltage on both halves of the ring, leaving little synchronizing power between stations. Much power load was shaken off and severe hunting followed. Since addition of the reactors, 66-kv faults near the power stations leave about 80 per cent normal voltage on the other half of the ring, furnishing ample synchronizing power and reducing the disturbance to customers.

As regards the effect of voltage on system load, after a severe case of instability which occurred in 1927, an analysis was made of the characteristics of the load, taking into account the amount of lighting, induction motors, synchronous motors, and other types of load. At that time it was calculated that a 10 per cent reduction in the load voltage would reduce the kilowatt load 10 per cent and the reactive load 67 per cent. A pure resistance load would be reduced 19 per cent by a 10 per cent voltage reduction, while a motor load, (kilowatts) would be very little affected until the voltage reaches a point where the motors begin to stall.

This analysis was for a Sunday night when the power load would be at a minimum. At other times, the decrease in kilowatts would be less than the decrease in voltage. The effect of induction voltage regulators is important in this respect.

E. E. George (Ebasco Services, Inc., New York, N. Y.): This discussion concerns the four papers on service restoration on metropolitan systems. Troubles on metropolitan systems are always intriguing to engineers of transmission systems. The situation is something like a military expert in this country having the opportunity to hear by radio from an observer in a foreign country a round-by-round description of an air battle or naval battle. A lot of valuable information becomes available to the listener at a safe distance without effort or risk. Undoubtedly the engineers of the transmission companies have profited from the difficulties occasionally experienced on metropolitan systems. The very fine group of papers presented at this session will be of value to the industry generally. The analysis of certain difficulties and their remedies will be of value to design and operating practice on transmission systems. Of primary interest here is what transmission practice can contribute to prevention of trouble or restoration of service on metropolitan systems.

Several items of possible application are mentioned briefly below. On some of these there is a diversity of opinion in the metropolitan groups and other items have been absent from the discussion at this session.

Mr. Juhnke has well expressed the improbability or general impossibility of any operating force handling perfectly any major interruption. Transmission experience would corroborate this viewpoint, further recognizing that metropolitan failures are more difficult to deal with than those on systems of the transmission type.

PREARRANGED RULES

Transmission companies have based their emergency operation very largely on definite written prearranged rules for each major generating station and substation on the system. It is interesting to note that although most of these rules were based on conditions many years ago when very limited communication was available in emergency, the use of these rules is just as extensive today with a very high grade of communication. Communication is a further safeguard and not a substitute for prearranged rules. Although such rules have been in satisfactory service on many transmission systems for at least 20 years and today many companies could not operate successfully in serious emergency without them, only one of the metropolitan companies appears to have given much consideration to the subject.

COMMUNICATION

Communication, not as an afterthought, but as a primary design feature is one of the most important safeguards to the operation of transmission systems according to practically universal experience. The use of independent routes to important stations, of one-way trunks, improved protection (such as neutralizing transformers, and unit-type relay protectors) have done much to maintain communication in serious emergency. Machine switching available in the metropolitan centers lends itself to almost any special type of service required for power-system operation. With machine switching it is possible to give dispatching calls right of way, get back of a busy signal, or automatically tear down conflicting private-branch-exchange connections when emergency calls should go through.

GENERATOR VOLTAGE REGULATORS

A few metropolitan interruptions have been due to instability. Voltage regulators of the high-speed type have accomplished much on transmission systems in the way of improving stability. Some of the metropolitan systems do not use voltage regulators and the reasons therefor have apparently not been presented to the Institute at any time. One interesting development on metropolitan systems is that of phase-angle control between generating stations. Some such arrangement might be used for interlocking voltage regulators to prevent instability in emergency. There is also the possibility of interlocking regulators with governor position. Five or ten per cent overvoltage on customers for a few minutes may be preferable to an out-of-step condition which necessitates starting all over again to restore service.

OVERLOAD RELAYS

The last few years have seen almost complete elimination of overcurrent relays for overload protection on transmission systems. Overcurrent relays are, of course, used to clear phase-to-phase faults but where there is no fault on the system, although some element of the system is working beyond its continuous safe rating, an overcurrent relay is of highly questionable value. Most of the system elements subjected to overload have a thermal time characteristic of several minutes and if automatic rather than manual protection is desired, the relay should be of the thermal rather than the overcurrent type. On transmission systems there is an increasing tendency to let the dispatchers and operators look after overloads reported by temperature alarms.

AUTOMATIC SWITCHING

Practice on transmission systems tends to performing of switching operations automatically. It is difficult to see why an operator should have to clear a bus manually if an undervoltage relay can do the same thing after two or three seconds of power failure. Automatic equipment is available to reclose a dead bus on the first energized line.

OUTDOOR SUBSTATIONS

The industry has had several serious switch-house fires. In some cases the use of more outdoor-type substations might be considered.

UNDERGROUND NETWORKS

The pilot-wire type of network control described by Brownlee and Dent on pages 685 to 690 of the 1938 AIEE TRANSACTIONS has not only had a good operating record but contains interesting possibilities for emergency sectionalizing of a network system. Communication is easily provided from the substation to each vault. Additional pairs of control cable are cheaply available for remote control of emergency secondary sectionalizing.

STAGED TESTING

The transmission companies can rather easily make staged tests of the emergency performance of certain sections of the system without serious inconvenience to customers. The situation in metropolitan companies is entirely different but there are certain opportunities for testing individual elements of the system just as the fire department in the absence of frequent fires carries out fire drills to test out their engines, alarm systems, traffic control, pumping equipment, etc. Transmission experience indicates that it is almost impossible to secure proper operation of equipment or personnel in emergencies occurring only once or twice a year, unless there is considerable testing and rehearsal.

UNUSUAL TYPES OF EMERGENCY

Most of the discussion of metropolitan troubles has centered around floods, fires, instability, and other natural causes resulting from weather or the limitations of materials. Any apparently satisfactory plan for the restoration of service in emergency takes on a much less attractive appearance if it is viewed in the light of what would happen if, for example, the essential personnel of the operating force were not available. Suppose there is intentional damage to equipment, especially at night and in isolated locations. Such has been the experience of several large transmission systems in the middle west. After trying to cope with this source of interruption to service, most operating men thereafter regard major storms, floods, or bus fires as merely minor annoyances.

The good attendance and general interest in this group of papers on metropolitan service restoration leads to the suggestion that the Institute hold a similar symposium on restoration of service on transmission systems. The group of papers last year on the New England hurricane was well received, indicating general interest in procedure following all major emergencies.

H. E. Wulfing (Commonwealth Edison Company, Chicago, Ill): This group of papers on the metropolitan bulk power systems presents valuable information on system planning to insure continuity of service, and operating procedure to provide for the restoration of service after a complete shutdown. Many of the design principles outlined in the papers have been applied by the Commonwealth Edison Company in its system layout where they can be economically justified. It is my purpose to discuss the design features rather than the operating practice.

As you know, Chicago is supplied by four generating stations within the city limits and is connected to three other large sources of power to the south, west, and north by means of 132- and 66-kv transmission systems. The favorable geographic location of these stations, together with their heavy interconnecting ties, adequate reserves (both running and stand-by) tends to insure a continuous source of energy. The problem is to plan the bulk supply system so that a fault in one zone does not involve others.

The 66-kv system in Chicago is similar in many respects to the systems of Philadelphia and Pittsburgh. This system is completely underground and functions to tie solidly together the various generating and distributing stations within the city. It is operated as two independent units, designated in Chicago as the "red" and "blue" systems. All the terminals use phase isolation and all the 66-kv cables are single conductor so that the likelihood of a phase-to-phase fault is very remote. The red and blue busses are *not* tied together except through the 12-kv system, and reactance of this system, together with high-speed relaying, permits isolating of the red and blue 66-kv systems before a fault in one can involve the other. Each red and blue bus is subdivided into sections, and at many strategic points two circuit breakers in series are used in order to limit the possible outage to only *one* section in case of a fault in *one* of the *two* section breakers.

As in other areas, Chicago makes use of high-speed relaying to isolate faults in all of the major elements of the system, including station busses. To provide for failure of the relay equipment, back-up protection is extensively applied. The principle of phase isolation and fault-bus protection has been incorporated in all the modern stations and many of the large substations to limit the destructive effect of short circuits.

The 12-kv busses in the generating and distributing stations are also split into the red and blue systems to correspond with the 66-kv system. Each red and blue bus is further subdivided into sections to limit the amount of equipment taken out of service for any one fault.

The 12-kv transmission system in Chicago is similar to that used in New York in that the load in each generating station is segregated from the load in other zones. This method of supply is very effective in preventing failures in one zone from affecting other zones. In Chicago, however, there are no low-voltage ties between busses in different stations, the only tie being through the 66-kv system.

The supply to substations in Chicago is somewhat different than that in other cities, although the general principle of sectionalization of busses and segregation of blocks of load is common to all. The substations are arranged in groups consisting of from one to five substations, depending on the magnitude of load, geographic location, etc. From three to eight transmission lines supply each group of substations, so that the maximum load supplied by any one group is limited to approximately 50,000 kva. This limits the destructive effect of a fault should it occur in this group. The lines to any one group are distributed among various generating-station bus sections to obtain diversified supply and prevent the interruption of the group in case of failure of any section. In large substations the bus is sectionalized to limit the service interruption resulting from a fault.

In the downtown area supplying the d-c network each substation has a red and blue bus so that a complete interruption of supply on one of the systems at the generating station would not completely interrupt the substation supply.

The bulk power supply system of Detroit is quite different in principle from that of the Chicago system. In Detroit the various generating stations are tied together rather loosely and the system is split into various zones at time of trouble. In Chicago the various zones are closely tied together through the 66-kv system and only a major disturbance can cause a disruption of this system. Both types of systems have their places in bulk power planning, the choice depending on load densities, the geographic distribution of load, the short-circuit current values, and the type of relaying used.

In the design of the a-c low-voltage network the practice in Chicago is practically identical with that of New York. This system is relatively small in Chicago, but it has reached the point where it is planned to segregate it into sections with no low-voltage ties between sections. Each section will be supplied by eight high-voltage feeders with a load of less than 50,000 kva.

The d-c network in Chicago has been undergoing a change in the past few years. The network has been permanently sectionalized and provision has been made to further isolate it into small blocks in case of serious trouble.

The practice of sectionalizing all parts of the system and segregating blocks of load has been found very effective in preventing extensive system shutdowns. Also, as indicated in the papers, this practice simplifies the problem of restoring service in case this shutdown should occur.

The reports of actual experience in restoring service in Pittsburgh and New York are extremely interesting and should be very valuable to the operating engineers in determining what they should do, or possibly what they should not do, in case they should experience interruptions similar to that of these two cities. I am glad to say that we have had no experience with major interruptions in the Chicago area within the past 20 years. However, our plans, designs, and operating procedure are being developed to prevent such an occurrence and to be ready to cope with it in case it should occur.

E. S. Fields (The Cincinnati Gas and Electric Company, Cincinnati, Ohio): The design of the modern power system compared with that of only a few years ago reveals the developments that have taken place to avoid, in so far as is economically possible, major shutdowns. Occasionally, however, unforeseen conditions and unpredictable circumstances do cause such experiences.

The designers and operators of metropolitan electrical systems have for many years been applying layouts and developing operating procedures with the thought of starting up these systems in the event of complete shutdown.

Up to about 15 years ago there were, in general, two types of systems in the metropolitan areas; namely, the d-c network (Edison system) and the radial a-c system. The radial system has not presented a problem from the standpoint of starting up after shutdown, since the individual radial feeder capacities are usually very small compared with the size of generating stations and even individual generating units. The technique of starting d-c networks has undergone many years of development, originally by use of the standby batteries and at present by means of load-limiting equipment applied to the synchronous converters and motor generator sets.

The introduction of the low-voltage a-c network system of distribution has presented a different problem, as large blocks of load served from multiple feeds must be "picked up" instantly. A substantial part of the total number of supply feeders must be energized simultaneously to prevent transformer, protector, and secondary-main burnouts and the block of load energized must be within the range that the available generating capacity can support when it is applied instantaneously.

Both conditions suggest limitation to the size of individual networks. The value of 10,000 kw at Detroit, referred to in Mr. Dean's paper, is of interest in this connection. In Cincinnati, we feel that we can handle at least twice to three times that much load and now have about 25,000-kw peak load on one network. A part of our program for restoration, however, includes disconnection of some of the larger customers' services until the network is restored adequately.

There are other aspects of the problem of starting a large metropolitan system after shutdown that should be referred to even at the risk of repetition:

1 Steam-plant auxiliaries and their steam or electric supply must be designed for starting the plant under totally de-energized conditions. If auxiliaries are largely electrically driven, the layout for the power supply must be based on the fact that it might be necessary to start the plant with one or more of the main and auxiliary busses out of service.

2. Control batteries should be of sufficient capacity to permit considerable switching during the emergency while the charging equipment is out of service.

3. Some emergency lighting should be provided for in the principal operating areas in the power plants and main substations and in the system power supervisors' and telephone operators' offices.

4. Instruction of personnel that would be engaged in starting up should be carried on.

5. Too much stress cannot be put on the layout of the communication system between system-power supervisors, power plants, and main substations.

6. A definite procedure should be at hand for unloading the system in case of partial or entire shutdown, so that load restoration can be carried out in an orderly manner after the generating capacity is restored.

C. A. Corney (Boston Edison Company, Boston, Mass): At the winter convention in January 1939, an attempt was made with considerable success to develop common ground for principles of switch-house design. A similar attempt is now being made to find a uniform basis for logical system development principles to minimize the effects of trouble and facilitate picking up load after a major interruption.

On two of the systems described in these papers the several generating-plant busses are connected via direct lines either overhead or underground. These lines serve practically no intermediate loads and are used for economy load transfer and for emergency capacity transfer.

On the other two systems the plant busses are connected via lines which serve intermediate loads, one apportioning the load on the several plants by direct control and the other largely by transfer of blocks of loads from one plant area to another at interlinking points.

In general it may be said that these papers, each in their own way, develop certain common principles applied to greater or less degree as follows:

(*a*). The systems are developed to provide load areas supplied from one plant as a normal source.

(*b*). Tie facilities are either direct between stations or via the transmission system for mutual support.

(*c*). Load areas are subdivided into blocks small by comparison with the capacity of the plant when considering its own resources and those of its interconnections.

(*d*). These blocks of load are served from segregated sections of power plant switch houses and of substations.

(*e*). Generating capacity is installed as close to load centers as conditions permit.

(*f*). Layouts are as simple as possible.

(*g*). Interconnections with other systems are in use for economy and mutual support.

In Boston the general system policy is developing along these lines:

(*a*). Generating stations tied by direct lines between busses for economy and mutual support.

(*b*). Interconnections via 110-kv and 66-kv lines.

(*c*). Each generating station to serve its own load area and be as near the load center thereof as geographical and other pertinent features allow. Emergency transmission facilities available for each load area from contiguous load areas.

(*d*). Switch houses and other features of the plant developed on the unit principle of segregation. Electrical equipment of adequate capacity properly protected by relay devices and fire-fighting equipment.

(*e*). Substations and networks both direct current and alternating current fed by lines so disposed on the several generator-bus sections that any section may be out of service without loss of supply to any block of load.

(*f*). Urban areas supplied by existing d-c network and by a-c networks with gradual discontinuance of d-c supply.

(*g*). Suburban areas supplied by radial circuits and by primary network mains.

(*h*). A completely co-ordinated plan for handling any type of major emergency.

(*i*). In our experience it is frequently better judgment to shut a station down on the occasion of major trouble than to attempt to continue operation and thus take the chance of spreading the damage. The length of time necessary to restart may thus be materially reduced.

I would like to comment on the Detroit plan and ask a question:

It would seem that their plan of "loose linking" requiring daily shifting of load between load areas to provide for economical loading of plants would encounter complications in relaying, load, and trouble dispatching, provide chances for operating errors due to frequent switching, and require more generating capacity for a given load than the "tightly linked" system. It would be interesting to know whether the saving in cost of direct tie between plants approaches the cost of added generating capacity.

It appears that the Trenton Channel "double throw" generators are an application of the direct-tie principle applied at short range.

The Duquesne paper refers to the small amount of trouble experienced due to storms, hurricane, or sleet. They say their geographical location precludes any great probability of a hurricane or tornado impairing the transmission system. That is what we thought in New England previous to the hurricane of 1938. They say the sleet hazard is great but do not state what is being done to cope with it.

The "service priority list" developed for the Pittsburgh area calls for restoring street lights before domestic, commercial, and industrial service.

In New England, after restoring service to police, fire, hospital, telephone and telegraph, water pumping, etc., we took industrial, commercial, and domestic loads in that order before restoring street lighting.

We thought it best to get industry, commerce, and homes going before restoring street lights. This sequence seemed to meet the desires of the municipalities which we serve.

T. R. Halman (The Detroit Edison Company, Detroit, Mich.): The discussions have shown a fundamental agreement on the basic principles involved in the design and operation of a large metropolitan electrical system from the standpoint of best meeting the hazards of possible major shutdowns. It has also been pointed out that many of the refinements adopted by certain operators cannot be fully appreciated without the background of one who has lived with the individual system involved.

From the discussion particularly of Mr. Juhnke, Mr. Wulfing, and Mr. Corney, it is evident that this is somewhat true regarding the advantages and relative simplicity of the principle of "loose linking" as used by The Detroit Edison Company. There appears to be an impression that this plan of operation tends to prevent efficient loading of power plants, requires additional running-reserve capacity, complicates the relaying, and is inherently awkward and difficult to live with. Much of this might be true if care had not been exercised from the beginning of the modern system design to see that all elements are kept in proportion and balance to yield a maximum of flexibility and utility of all components.

It is relatively easy to obtain maximum operating economy among plants of varying efficiency where all plants and loads are tied together as closely as possible. Conversely it is easy to guard against the spread of trouble on a system consisting of several weakly connected areas. Loose linking as developed and applied in Detroit is an attempt to overcome, by careful system design and operation, the apparent contradiction of these two ideals and preserve within practical limits the outstanding advantages of each.

In general on most systems, the load is somewhat distributed whereas the power sources are concentrated in a few plants usually some distance apart. These power plants are located as close as possible to the heaviest load centers and supply this load directly or through radially fed substations. There are also very likely to be considerable load concentrations which are not very near any source but approximately midway between a pair of power houses. An efficient method of supplying these loads, and at the same time tying the various power houses together into a unified system, involves the use of switching stations at these intermediate load centers, each having transmission ties in two directions to adjacent power sources.

The total tie capacity from both directions must always be considerably greater than the maximum expected value of the local load to allow for outages and sudden load growth. Thus the ties from either direction will carry most of the load excepting during extreme peak conditions. Therefore, by simply installing, in accordance with a well thought out plan, the transmission lines necessary for carrying safely these intermediate loads, it is possible to obtain a great deal of flexibility of area limits without any extra investment in transmission lines. This makes it easy to shift a very large proportion of this load even at peak condition, and probably all of it at intermediate or light-load conditions. Fortunately, it is during these intermediate and light-load conditions that such shifting is most desirable, because at heavy-load periods most of the particularly economical capacity can be loaded without reaching so far. It should be evident that this system design and plan of operation does permit efficient plant loading.

Admittedly the above principles are fairly commonly used. Also it is not unusual to provide back-up relaying in ties between sections of a system to prevent the spread of uncontrollable trouble. The unique feature of the loose-linking method is that as the supply to the intermediate loads is shifted by the altering of the relative loading on the ties from opposite directions, the relaying on these ties is also altered so that in case of separation these loads will remain in the area of the plant which had been supplying them. This is done by having sets of separation relays each side of critical load centers, and cutting in, by means of suitable relay-control switches, only the proper ones to cause tripping of the ties on the side away from the preferred source.

This makes it possible, without running any additional reserve generating capacity, to keep each load area balanced with its plant capacity so that the combination will be self-sustaining and can operate satisfactorily even if completely separated from the remainder of the system. This permits the use of separation relaying sufficiently sensitive so that it should accomplish its purpose of saving the balance of the system in case of an uncontrollable fault in one area and yet does not sacrifice the chance of maintaining service completely should the fault eventually clear. It also permits the use, without hazard, of a rather drastic plan of emergency switching, which calls for opening any area ties at the existing designated points of pull-apart should the voltage fall below 60 per cent of normal for one minute.

It should be understood that this loose-linking principle has been developed without necessitating dangerously weak ties between power houses or load centers, and also without involving relaying of the hair-trigger variety. Including the lines necessary for feeding intermediate load, described above, and the inter-area ties, each power-plant load area has a total transmission capacity across the area boundaries from the balance of the system of the order of 100,000 kw on a short-time emergency basis, and the loose-linking relays are set to take advantage of it. This is ample to care for emergencies such as the loss of the largest generating unit at any plant. Thus it can be seen that no extra generating reserve is required on account of loose-linking operation. During many years of experience with this method there has never been an unnecessary separation either due to relays or emergency switching.

It should be noted that the method used does not involve any major switching but only manipulation of relay-control switches, and cannot be hazardous to service under any normal conditions of operation. In case of major faults, it is true that should the wrong separation points operate, either due to faulty supervision or faulty relaying, the full benefit of loose linking would not be realized. It should be obvious, however, that conditions could never be worse than where no provision is made for sectionalizing the system in case of uncontrollable faults, or where no attempt is made to keep the elements in balance, and therefore self-sustaining after separation. The loose-linking plan practically guarantees that a large portion of the system will be saved, whereas without it the whole is in jeopardy. Without close attention to a self-sustaining policy, even should the system be sectionalized, there is grave danger that the unfaulted portion may be left with too much load so that the generators become swamped.

The supervision of the daily load cycle in each area and shifting of separation points to match may appear rather burdensome; but experience has shown that once a routine has been developed it can be followed with little trouble and actually has a compensating advantage in that it helps to keep the operating personnel alive to bulk power and emergency problems.

From the relay standpoint, it must be admitted that this system does involve additional labor in calculating settings and maintaining some additional equipment, but much of this really should be done anyway on any system where it is desired to have a comprehensive and adequate plan of back-up protection against major faults.

Whether the principle of loose linking can be applied successfully to other systems, requires the intimate familiarity with all details of the system in question which has been mentioned; but on a suitable system it can result in a great increase in safety to service with very little or no penalty in the form of loss of economy or complication of operation.

Restoration of Service on a Metropolitan Power System

Discussion and authors' closure of paper 40-32 by W. C. Bryson and A. P. Hayward, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (October section) pages 563–70.

A. P. Hayward: Mr. Corney has mentioned the statements concerning the probability of hurricanes and sleet storms occurring in the Pittsburgh area. The weather records do not disclose any hurricanes of the type experienced along the seaboard. The mountains to the east are believed to be a very effective barrier to the full force of any hurricane coming inland and to a certain degree prevent the occurrence of any destructive tornadoes of the type experienced in the Midwest. However, local wind storms of hurricane velocity are not uncommon and, in general, the overhead structures are constructed and adequate clearance provided to withstand these conditions. The sleet hazard is relatively great in this area but for some unforeseen reason there have been no major sleet difficulties on the Duquesne Light system for many years, in spite of severe sleet storms immediately adjacent to the Duquesne Light territory.

It was also mentioned that in Boston the priority schedule for restoration of service placed street lighting at the end of the list. Conditions in Pittsburgh were somewhat different than in Boston during the hurricane as the flooded areas were evacuated and all the inhabitants moved to higher land and concentrated in relief centers. This required great precautions to provide adequate protection for the deserted homes in and around the flooded area. Although martial law was in effect, the early restoration of street lights greatly assisted in detecting marauders and assisted in policing these territories. Mention has been made of excessive flood risk in the Pittsburgh area. It is true that floods are quite frequent, but this area has always considered the flood hazard and in general has provided protection for the normal yearly floods. A 46-foot flood stage such as occurred in 1936 had never been experienced since the settlement of the district. The highest previous flood the utilities have experienced occurred in 1907 with a flood stage of 38.7 feet. Probability studies show that a 46-foot flood stage should occur about once every 250 years. The probability of major floods is much lower than the probability of hurricane-velocity winds in the New England area. Since the 1936 flood much work has been accomplished in providing flood protection for the upper Ohio River valley. One flood-control dam has been completed and several others are under construction. It is estimated that upon the completion of this work a flood similar to the 1936 flood will have its crest reduced approximately 8 feet. This will bring these major floods within a range where they will cause less inconvenience to the public at large.

The paper has mentioned the occurrence of several major electrical failures at the Colfax power station, and mentioned that the unit operating setup prevented these failures from interfering with the operation of the balance of the station. A factor which greatly assisted in minimizing these disturbances is the practice of immediately isolating and removing from service all equipment involved in electrical failures and not attempting to reclose this equipment or restore service until the location of trouble is determined.

Mention has been made of skull practice in preparation for emergencies. Flood drills are made during or immediately preceding flood emergencies to review the emergency routine and to train the operating personnel in the proper flood procedure. These drills involve the procuring of material and supplies, the removal of flood gates from storage, and the setup of certain protective equipment and operation of the emergency flood pumps as outlined in the instructions for emergency operation.

Another factor which has been of considerable assistance in training the operating personnel has been an educational program which has been devised to acquaint the operating and maintenance personnel in the plants with the general operating problems of the system. The boiler-room and turbine-room operators have attended electrical courses which have discussed the operation of generators and transformers, interconnected system operation and transmission problems, all referred to specific problems that have arisen on the Duquesne Light system. In this manner they have a much broader realization of conditions that exist on the system and in emergencies are capable of responding in the proper manner.

P. B. Juhnke: See discussion, page 1157.

E. E. George: See discussion, page 1150.

H. E. Wulfing: See discussion, page 1151.

W. C. Stone: See discussion, this page.

E. S. Fields: See discussion, page 1151.

C. A. Corney: See discussion, page 1152.

Restoration of Service From Large Metropolitan Systems After Complete Shutdown

Discussion and authors' closure of paper 40-31 by H. A. Dryar and Raymond Bailey, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (October section) pages 571–4.

E. E. George: See discussion, page 1150.

H. E. Wulfing: See discussion, page 1151.

E. S. Fields: See discussion, page 1151.

C. A. Corney: See discussion, page 1152.

L. G. Smith (Consolidated Gas Electric Light and Power Company of Baltimore, Md.): The paper on the Philadelphia system covers the problem of re-energizing a system supplied by both steam and hydroelectric plants and also interconnected with other systems. On such a system it is difficult to formulate a single procedure of restoring service after a complete shutdown. Several procedures must be considered, depending on the relation between hydro and steam generation; the loads in the various load areas at the time of the trouble; and the amount and direction of power flow in the interconnections. The authors have emphasized one point that is believed to be essential: that is, to clear the busses immediately. Whether one or all stations are involved, the busses in the affected stations should be cleared if differential-relay protection has not already accomplished this. This procedure may prevent the spread of the trouble and will facilitate the orderly restoration of service. The need for co-ordination with the boiler operators stressed by the authors is also of vital importance. Any instructions should require the notification of the boiler operators of the load to be restored so that load will not be connected unless it can be carried. This might involve a detailed listing of duties and responsibilities of all operating personnel.

In a system having several generating stations and load areas, a complete shutdown can be classified into two types; namely, (1) all busses can be re-energized, and (2) the bus on one bulk power source cannot be re-energized. A serious problem is introduced if either a complete or partial system shutdown is experienced and it is found that a 13.2-kv bus in a generating station cannot hold voltage.

In the past the most serious failures to service in metropolitan areas have been due to bus failures in generating stations. It is believed that the difficulty in restoring service has not been entirely due to the original failure but to the fact that it was not cleared promptly and could not be readily located. In such cases the operators know only that a bus is in trouble. The first step is to clear the bus. However, to determine what bus sections are in trouble and what sections can be used to re-establish service, the only procedure is one of trial and error; that is, to connect units to various bus sections in order to see if they will hold voltage. This practice often causes additional damage and sometimes a spreading of the trouble. While differential relay protection is proposed to prevent this type of trouble, in some cases it is difficult to install in existing stations. Might not some method of quickly testing the individual bus sections with a low-capacity potential testing device be desirable for installation in stations not having differential or ground-fault bus protection?

W. C. Stone (nonmember; Public Service Electric and Gas Company, Newark, N. J.): The electric system of the Public Service Electric and Gas Company is an arrangement of generating stations and switching stations linked together by 132-kv transmission circuits and interconnected with adjacent operating companies, the most important interconnection being the 220-kv Pennsylvania-New Jersey ring. With one exception, each generating station and switching station distributes power to areas of from 50,000 to 150,000-kva demand, one generating station feeding directly into the 132-kv system.

Restoration of Service to Generating Stations and Bulk Power Systems

The restoration of service to a generating station or bulk power system after a complete shutdown is necessarily a matter of coordination between several departments.

Without proper instructions and team work, it can be expected that unnecessary interruption to service will result.

Usually a complete station shutdown is the result of a bus short circuit or a failure of major equipment. The switchboard operators should open the necessary oil circuit breakers to isolate all rotating equipment from the station bus. Then open all oil circuit breakers on circuits which may act as feed backs from the system or other sources of supply.

The location of the fault and isolation of damaged equipment follows. While this is being done, the remaining radial-feeder oil circuit breakers should be opened and the turbine room, boiler-room engineers, and load dispatchers informed of what has taken place.

After the fault has been located and damaged equipment isolated, the station bus is ready to be energized from the main power system or another generating station. This applies to stations where there are no house turbines.

Energizing the bus and station power transformers from the system allows the starting up of all auxiliary equipment simultaneously, thereby speeding up the time of returning the boilers to full steam pressure and allowing a number of generators to be brought up to synchronism simultaneously. It also allows the immediate restoration of service to a number of feeders depending upon the capacity and number of the circuits available from the system.

As soon as the station shutdown occurs, it is the function of the load dispatchers to order in additional generators at other generating stations and arrange for purchase of power from interconnected companies, in order that he may be in a position to restore as much of the interrupted service as possible should there be a delay in returning the generators to service in the interrupted station.

It may be necessary to relieve the system of overload caused by loss of generation. This is done by dropping blocks of load listed in a manner showing those least affected by loss of service.

Bulk Power System Interruption

Each power system is somewhat different from its neighbor in design of busses and transmission arrangement, and the methods of restoration necessarily differ to some extent.

It becomes the responsibility of the load dispatcher to take control of the situation immediately after the fault in the generating station has been isolated and the operator reports "ready for service."

Orders are given to energize feeders to various substations or groups of substations as fast as generators and boiler capacity are available, synchronizing wherever necessary until all service is restored.

No difficulty should be experienced in restoring service to the various substations or networks, as the majority of substations do not exceed a demand of 15,000 kva. Those that do exceed 15,000 kva can be restored by way of group oil circuit breakers, energizing several transmission circuits simultaneously.

Differential-Relay Protection

All generating station, switching station and important substation busses are fully equipped with differential-relay protection or soon will be.

Bus-section oil circuit breakers which are part of the differential-relay scheme should be backed up by a second oil circuit breaker at points where a failure of a breaker will result in a station shutdown.

AUXILIARY STATION POWER

The key to service in a generating station is the auxiliary power supply.

Station power transformers should be properly isolated and, if possible, receive their source of supply from bus sections which cannot become involved in the same fault, the secondary bus being provided with two or more sectionalizing breakers.

Where it is not practicable to feed station power transformers from two or more sources, their common primary bus should be provided with two sectionalizing breakers.

DESIGN

It is desirable to give busses and switching equipment, either indoor or outdoor types, as much clearance and isolation as possible. Fire walls should be installed between transformer units and fire-extinguishing methods should be provided.

The pedestal type of outdoor bus structure is preferable, we believe, because it is economical and convenient, and safe for maintenance.

The indoor busses, transformers, and equipment should be separated in compartments where failures can be confined to prevent as far as possible the spread of trouble to other equipment which might cause a major interruption.

INSTRUCTION OF OPERATORS

It being impracticable to shut down generating stations for the practice and education of the operating personnel, the next best method must be adopted, that of issuing general instructions which will act as a guide to each department that may be involved pointing out the desired methods to be used. These instructions should be reviewed periodically, and often, in order that the efforts of each department may be directed in the proper course without confusion or loss of time, and that each department may know what the other departments are attempting to do, in short, team work.

In closing, I wish to point out two important items:

1. The choice of men who will later become engineers, operators, watch-foremen, and load dispatchers.
2. The necessity of properly and continually instructing them and training them in their duties.

H. A. Dryar and Raymond Bailey: The opinion of L. G. Smith that the difficulty in restoring service is attributable mainly to the duration of the fault conditions finds support in our experience of a ground fault on a generating-station bus having to develop into a phase-to-phase failure before relays become operative. In the meantime, both busses were affected and complete service restoration was delayed until the equipment was cleaned of soot and smoke.

In our opinion the remedy is to be found in a greater number of bus sections, each differentially protected. With this arrangement, bus faults will be relayed promptly, equipment damage will be minimized, and interruptions will be reduced.

The opinion of P. B. Juhnke that prevention is the better part of the cure, and that approach to this very desirable end is to be found in adequate spacing to avoid fault communication and such sectionalizing of the system with adequate relay protection as will permit isolation of a fault without interruption to load, has our hearty support.

As the power system grows in size and complexity, assuming increasing importance is the training of the men whose responsibilities demand complete knowledge of the system, instant and correct analysis of disturbance reports which are not always accurate, and an initiative that begets instant decision as to the measures to be applied.

Because of the variety of possible forms of trouble and the variations in procedure to cope with them, the training seeks to promote the habit of a daily review of potential problems, the effects, and the methods of restoration.

Our training method is to submit monthly to each load dispatcher via the telephone 12 hypothetical problems and grade him on the basis of knowledge, analysis, decision, and explicitness of instructions. Two supervisors, one giving the problem and the other listening, are the judges. His individual rating is then given him as a spur to continued good performance or an urge to greater efforts.

We dislike to admit the inability of a good dispatching organization to handle satisfactorily major system troubles. A few minutes daily given to a conscientious study of hypothetical system problems by each member of the organization will so improve one's performance that the monthly test problems should indicate his ability to function properly under stress.

Provisions for Re-energizing the Electric System of the Consolidated Edison Company of New York, Inc.

Discussion and authors' closure of paper 40-33 by G. R. Milne and H. C. Otten, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (October section) pages 579–85.

E. E. George: See discussion, page 1150.

H. E. Wulfing: See discussion, page 1151.

W. C. Stone: See discussion, page 1154.

E. S. Fields: See discussion, page 1151.

C. A. Corney: See discussion, page 1152.

W. E. Caldwell (nonmember; Consolidated Edison Company of New York, Inc., New York): The paper by Messrs. Milne and Otten presents a practical procedure for re-energizing a large electrical system and the comments I have to offer deal primarily with the incidental problems in the mechanical plant and more especially in the boiler plant.

The response characteristics and limitations of moderate-pressure boilers with various combustion systems is fairly well established and the suitability for meeting normal load variations is seldom questioned. In meeting extreme conditions, such as re-energizing large network areas, little direct experience is available.

The rate of load pickup on moderate-pressure boilers may be limited by the response of the combustion system and/or carry-over resulting from rising water level with increasing load. The rising water level is caused by an increase in the steam-water ratio in the boiler, coincident with a sudden decrease in pressure accompanying increased steam demand. A rapid increase in heat input produces a similar effect but in general this is not a serious handicap in a well-designed boiler. This fault in some boilers is partly compensated by maintaining a low water level, at low rating to afford a greater margin to accommodate the swell which accompanies increase in output. Boilers with large water capacity in relation to drum capacity are sensitive in this respect.

From the standpoint of response or ability to take large load increases, the boiler should have water capacity consistent with the heat inertia of the absorbing surfaces and the lag of the combustion system. Some accumulator effect is also desirable for maintaining more uniform combustion conditions so that minor variations in steam demand will have little effect on steam pressure, which is the usual source of control of the combustion regulators. The steam drum should be large enough to provide a reasonably stable water level. A certain amount of water capacity is necessary to act as a heat accumulator during the normal period of load pickup preceding the increase in heat input, which may vary from a few seconds to a half minute.

Acceptable characteristics are economically obtainable during the design stage by a rational allocation of surface between boiler, economizer, and air heater. There are other important considerations in boiler design but these need not be treated here. Aside from lag deliberately introduced in regulators, a delay in response is due to the combustion system and part is due to the materials transmitting the heat in establishing the new heat-flow gradient. A reasonable figure for normal load pickup may be taken as 15 per cent capacity per hour per minute. It is possible to better this increase substantially but it represents a fairly safe range for control apparatus setting with due consideration to minimizing smoke and cinder emission.

Obviously, a steaming boiler responds more readily than a banked one and a pickup from 50 per cent to 75 per cent capacity is quicker, on account of the available overtravel capacity, than from 75 per cent to 100 per cent. There are differences in the rate of response of the various combustion systems, that is, stoker and pulverized coal, but these differences generally are of minor significance. For example, a modern stoker-fired boiler at Hudson Avenue (300,000 pounds per hour capacity) developed

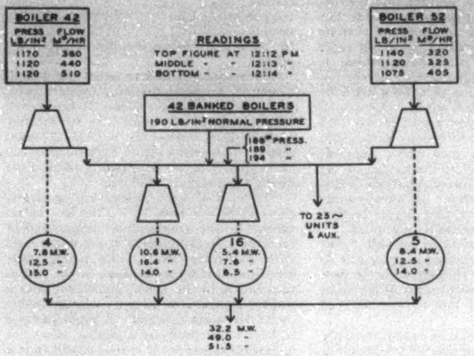


Figure 1. Load-change test, Waterside station, December 17, 1939

an increase, on test, of 35,000 pounds per hour per minute from a banked condition. Another Hudson Avenue boiler (150,000 pounds per hour capacity) was raised from 50,000 pounds per hour to 120,000 pounds per hour in 27 seconds, or a rate of increase of 155,000 pounds per hour per minute. A unit-fired pulverized-fuel boiler at Hell Gate, with type *B* ball mills picks up 15 per cent capacity per hour per minute, while other boilers in the same plant with impact mills respond quicker. East River boilers with bin system of pulverized coal are capable of still more rapid response. With constant header pressure more heat input is required for a given increase in steam output than with a declining pressure, since heat is absorbed by the water as a result of increasing boiler pressure incident to increased flow.

In re-energizing the electric system by the method described in the paper, lag in boiler response is minimized by proper co-ordination in closing the switches controlling the network feeders. The lag in repositioning regulators, etc., is entirely eliminated and a major portion of the lag in the combustion response is eliminated so, under these conditions an instantaneous pickup of 25 per cent by the boiler plant is a reasonable expectation. Substantial loads have been picked up by the system unexpectedly and much greater increases may be accommodated by prearranged co-ordination.

While the moderate-pressure plants on the system have demonstrated their ability to meet re-energizing conditions, such information was lacking in the case of the high-pressure installation at Waterside. Accordingly, tests were made to provide practice in picking up loads anticipated in the paper and to determine inherent characteristics of the high-pressure equipment. Briefly, the 60-cycle high-pressure and low-pressure units at Waterside were separated from the system with enough load on the bus for almost full load on the two high-pressure boilers in service. About 15,000 kw of this bus load was supplied by a tie from Hell Gate, so that the tripping of a breaker dumped this load on the high-pressure units. Banked boilers were floated on the 200-pound header as a precaution and adequate safeguards generally were provided. The results of the test were very reassuring, the general arrangements for which are shown diagrammatically in figure 1 of this discussion.

While the high-pressure units were separated from the main system the governor regulation was entirely satisfactory. Previously, some erratic behavior of the units had been ascribed to the high-pressure governors, so this experience with isolated operation dispelled any suspicion of inadequacy.

In picking up the load all speed control was effected on the high-pressure machines with the boilers on manual control. There was no increase in steam from the low-pressure banked boilers, as may be seen from the pressure indications at the high-pressure exhaust which actually increased with the load.

The momentary dip in frequency, indicated in figure 2 of this discussion, occurred immediately after the load was dumped and within the time required for governor response the greater part of the drop was restored. After a readjustment of speed by the operator the normal frequency was practically restored within eight seconds, although a longer period was required for the boilers to recover. Actually, the frequency was substantially restored before the heat input to the boiler had appreciably increased.

Some of the more important observations of the test are presented graphically in figure 3 of this discussion.

The steady water level with this rapid increase in load is due largely to the greater density of the high-pressure steam, as contrasted with lower pressures but it also reflects good design proportions.

During this test the back-pressure control was not in service on the high-pressure machines. The stable back-pressure characteristics indicated in this test result from the low inertia of the rotating masses and more responsive governor on the 3,600-rpm high-pressure machines than on the lower-speed low-pressure units. With increasing load the steam flow through the high-pressure unit increases before the low-pressure governors have responded, tending to raise the cross-over pressure and, with falling load, the pressure declines.

From the curves it may be seen that the boilers responded adequately to the instant demand of 28 per cent increase in output with but slight drop in pressure. This test confirms our belief that the high-pressure units are capable of meeting re-energizing demands, just as effectively as the time-tested low-pressure installations.

L. G. Smith (Consolidated Gas Electric Light and Power Company at Baltimore, Md.): This paper is of considerable interest to the system design engineer as well as to the operating engineer, because it presents fundamental points to consider in the design of the transmission and distribution system in order to facilitate restoration of service. By a system designed so that each load area is wholly dependent upon one generating-station bus, the New York system greatly simplifies the operating procedure of restoring service. However, the generating-station bus becomes a vital element in the supply of electric service to the load area. Therefore, as the authors state, extreme care is required in the design of generating-station switching. Experience indicates that walls with doors in them do not constitute adequate isolation. Walls with no openings or even geographic separation are necessary to insure adequate isolation. The company with which I am associated has a project in progress in an important generating station to obtain the necessary isolation by adequate barrier walls and by moving part of the switching from a switch house to an outdoor structure.

In the procedure outlined for re-energizing an a-c network by simultaneously closing a number of feeders, a difficulty may be introduced by one or more of the oil circuit breakers to which the feeders are connected being unable to withstand operating voltage. In the event of a bus failure, damage may be done to the wiring or oil circuit breakers associated with a feeder, making it necessary to determine which switching equipment is in operating condition.

In re-energizing parts of a system service may be restored to parts. In restoring

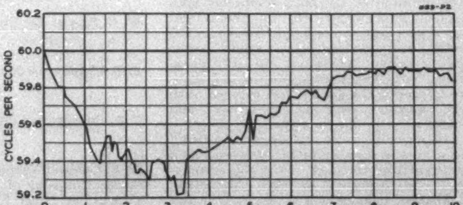


Figure 2. Waterside bus frequency during loading test

15,000-kw load interrupted at 12:12 p.m., December 17, 1939 service to other parts sufficient generating capacity may not be available; however, by lowering the voltage on the parts of the system to which service has been restored, the load may be reduced so that sufficient generating capacity is available.

P. B. Juhnke (Commonwealth Edison Company, Chicago, Ill.): I somewhat recklessly promised to discuss two of the papers presented here today, those dealing with New York and Pittsburgh. On closer scrutiny I found that it is no easy task to keep this promise. In systems of such size there are fine points which the authors have fully in mind, but which an outsider can absorb only after a prolonged study and intimate contact. My remarks, therefore, will be very, very general.

There occur in these and other papers references to the training of the operating force, and especially of system operators or load dispatchers. This training is one of the knotty problems confronting us. We cannot well maneuver with great power systems, yet something of this sort is really called for. Expecting a dispatcher to size up a system in trouble and quickly to map out a strategy to cope with it is expecting a great deal. The difficulties incidental to such a task are fully appreciated by anyone who has ever sat at an inquest following a bad case of trouble. There, with every item of available information in predigested form, with premeditation covering perhaps a day or more, it is sometimes difficult, if not impossible, to reach correct conclusions, such as the operating personnel must reach in moments of uncertainty aggravated by lack of information given or by conflicting reports received.

To give the men in our organization charged with this responsibility some familiarity with system procedures, we have simulated cases of trouble by stating effects and have quizzed them as to the proper procedure dictated. Difficulty lies in shaping a probable problem, and in the unreality of it.

We have also prepared an operating routine for cases of trouble in generating and distribution stations, unknown in location. The study given to these by the operating personnel has had the effect of broadening appreciation of the problems involved.

It should be borne in mind that the responsibility for training of these specialists rests with the operating executives, that the problem is far from being solved, and that much more remains to be done. I personally doubt that a really big case of system trouble can ever be efficiently dealt with by the best operating organization in existence. A big case of trouble corresponds to retreat for an army, and retreat always dictates tremendous sacrifices.

With specific reference to the New York system, I have some doubt as to whether 20 out of 60 d-c substations can pick up the entire load without damage to equipment, but a positive answer requires more information than is available from the paper.

With reference to picking up the a-c network, we are averse to making relays inoperative, though the size of the networks in New York seems to make such action necessary.

From an outsider's point of view, the concentration of load at Hudson Avenue and Hell Gate stations would seem excessive, but again this is a mere opinion which may not be based on all the known facts. Lack of sectionalization would seem to dictate excessively expensive switch structures.

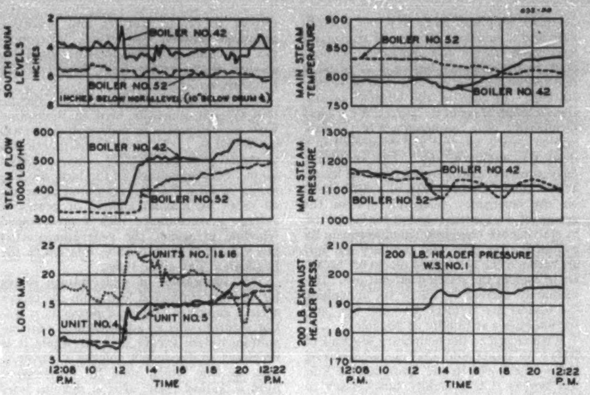


Figure 3. Rate of load change, topping-unit boilers, Waterside station test of December 17, 1939

In regard to the Pittsburgh system, this seems woefully at the mercy of uncontrollable forces of nature, which do not yet seem to have recorded their maximum in viciousness and brutality. The trouble resulting from them, however, involves submersion of load, as well as of generating capacity, which eases the responsibility considerably.

It appears that some of the cases of trouble of the past may be attributable to relay setting and limitation in relays, both of which, since the dates of those cases, have made great forward strides.

The Pittsburgh system, in its latest operating setup, seems to have made considerable progress since the days when those cases of trouble occurred, and is now perhaps better fortified than ever before to weather storms such as have befallen it before.

In regard to both of these systems, I lean to the belief, and express it rather meekly, that for them the ultimate in system structure has perhaps not yet arrived.

There have been recited here a number of cases of trouble which have occurred in the distant past. These cases have played a prominent part in shaping our systems of today. These cases were analyzed as to causes, mostly unforeseeable, and for whose anticipation not a single index was available. From such analyses conclusions were drawn. These conclusions all point to one outstanding fact, namely, that the effect of the initial trouble with its train of attending development was not sufficiently confined. If the design of the system involved had been such that the hazards effectuated had been limited in exerting their parallelizing effects upon smaller portions of the system involved, then the interruption which resulted would have definitely not been recorded.

May I be permitted to express my humble belief that sectionalizing of generating capacity and trunk transmission lines in load areas will render inoccuous most of the hazards reposing, but not identified in our systems? This sectionalization should be carried so far that every metropolitan system in reality will consist of two parallel systems, each sufficient unto itself and separated, if not in space, at least by effective means imparting the element of space, and so adequately relayed that in the event of misfortune befalling the one, the other will not be affected thereby except by resulting load transfers. This sectionalization should be extended to a point where the largest amount of generating capacity in any one location supplying each such section at that point, and the transmission elements on the same section may be lost without embarrassment to more than the load zone directly involved. Such structure can be envisioned, can be classed as economic in the light of the responsibility confronting electric service systems, and should contain a guarantee against general service interruptions in the future.

In conclusion, may I be permitted to paraphrase an utterance of a great authority: In the end there remains but these two, prevention of interruption and restoration of service, and the greater of these is prevention.

G. R. Milne and **H. C. Otten:** P. B. Juhnke expressed some doubt regarding the ability to re-energize the d-c network in New York City from a limited number of substations without damage to equipment. While there has been no actual experience to indicate the adequacy of the method prescribed for re-energizing this network, calculations show that if the operations are executed as planned, the magnitude of current which the d-c feeders, substation bus-tie breakers, and converters are called upon to carry will be within safe limits. No attempt would be made to re-energize the d-c system until an adequate supply of 25-cycle power is available and a sufficient number of converters have been started and connected, without load, to the main d-c busses in 27 or more selected substations out of a total of 41 substations. In each of the selected substations a group of six of the longest d-c feeders terminating near the boundary of the substation area is selected and connected to a dead d-c auxiliary bus. The remaining feeders are left disconnected from the busses at this time. Upon receipt of a code signal on the emergency telegraph system the groups of d-c feeders selected for initial use in starting are energized simultaneously by connecting the main and auxiliary d-c busses through the heavy bus-tie circuit breakers in each of the selected substations. Immediately thereafter, all of the remaining d-c feeders are connected to the live substation busses as rapidly as possible.

With regard to the temporary blocking of relays we might add that in re-energizing the a-c networks in the Consolidated Edison system it is generally unnecessary to make inoperative any relays on the distribution feeders. In the case of the direct-tie feeders between generating stations it is considered advisable to block the back-up overcurrent relays only, to prevent tripping the ties as a result of the current surge at the instant of re-energizing a large network. It should be noted however that in any case, the ground relays, pilot-wire relays, and impedance relays always remain operative and that the first-line-of-defense protection for faults in the tie feeders themselves remains intact.

An impression seems to have been given that because of the relatively large capacity of the Hudson Avenue and Hell Gate generating stations the current-interrupting duty imposed upon the circuit breakers is severe and requires excessively expensive switch structures. The actual breaker duties are not excessive since the busses are always sectionalized. The number of sections operated separately at any given time is adjusted according to the amount of generating capacity connected. The interrupting duty imposed upon the breakers does not normally exceed 1,500,000 kva and the experience with breaker operation has been generally satisfactory.

E. E. George has, in his discussion, presented some interesting comparisons with respect to design features and operating practices between the typical "metropolitan" systems and the systems supplying smaller load centers from large-capacity transmission lines.

The fact that automatic voltage regulators are not generally used in connection with the generators in metropolitan system is referred to. It has been found practicable to render very satisfactory service without them. Service voltage on the large a-c networks in New York is regulated in nearly all cases by manual control of generator voltage in accordance with a prearranged schedule in which the generating-station bus voltage bears a definite relation to the bus load at any given time.

The application of undervoltage relays for clearing connected load from the busses after a complete failure of power in a station seems to offer no advantages in a metropolitan system except in cases where it is necessary to disconnect quickly a transmission tie line which may be re-energized from a distant point after a definite brief interval of time in accordance with a prearranged schedule. Likewise, equipment for automatically re-energizing busses from the first energized transmission line would have limited application in metropolitan systems because in any case of complete shutdown of a station, resulting from an electrical fault, it would probably be necessary to make a brief inspection at least, of the main busses and switchgear before attempting to re-energize the equipment.

L. G. Smith referred to the influence of doors upon the adequacy of barrier walls. In the Waterside 60-cycle generating station in New York, which was recently modernized to meet the requirements for supply to a-c networks, rather unusual provisions were made for the operating personnel to pass from one isolated section to another in the switch galleries. Instead of providing any doors in the heavy barrier wall itself, two open passageways or balconies were provided, one outside the building wall and one in the turbine room high above the turbine room floor. Each of these balconies is reached by doorways in the walls of adjacent isolated bus structures, the doors opening outward in each case. In the event of failure of a door as a result of a pressure wave originating within a bus structure the gases would be vented out of doors or into the upper space in the large turbine room and could not enter the adjacent isolated bus structure. In other cases where space was limited doorways in barrier walls were each equipped with two heavy doors opening in opposite directions so that a pressure wave originating on either side of the wall would tend to hold closed the door on that side of the opening.

In any case where a shutdown involves a failure of electrical equipment and renders one or more distribution feeders to a network unavailable temporarily, provision is made in the written procedures for indicating the minimum number and grouping of the available feeders which would be adequate for the purpose of re-energizing the network.

The reduction of load which may be effected by lowering the system voltage has been investigated. A reduction of 10 per cent below normal bus voltage at the generating station is considered to involve little risk of damage to motors and other utilization equipment. The effect of such a reduction of voltage was determined for a portion of the Consolidated Edison system including representative proportions of 60-cycle residential, commercial, industrial, and rapid transit load at about the time of the evening peak. The magnitude of this load was 460,000 kilowatts at normal voltage. A 10 per cent reduction of voltage caused a reduction of 13 per cent in true power and a reduction of 17.4 per cent in the reactive-power load. The voltage was gradually reduced to 90 per cent of normal over a period of five minutes, held at that value for two minutes, and gradually restored to normal over a period of five minutes. When normal voltage was restored the original values of true power and reactive power load were re-established. During this test all induction regulators at company substations were fixed at their normal operating position so that the reduction of generating-station bus voltage was effective throughout the area supplied. Reports of adverse effects on the operation of utilization equipment were negligible in number. The most noticeable effect was the fading of sound output in motion-picture-theater installations. It was estimated that at the time of this test the proportions of lighting and power load were 90 per cent and 10 per cent, respectively.

Railway Power Supply (40-4)

Power Supply for Railroad Electrification and Fundamentals of Power Contracts

(40-27)

Discussion and authors' closures of paper 40-4 by Philip Torchio and paper 40-27 by E. R. Hill, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 550–3 and 560–2.

Sidney Withington (New York, New Haven and Hartford Railroad Company, New Haven, Conn.): The papers by Messrs. Torchio and Hill cover in an interesting manner the salient requirements of railroad power supply. Mr. Torchio points out that where the railroad operates its own generating station, jointly with purchase of power, the less-efficient railway plants may be operated for a minimum number of hours, as is the case with utilities owning generating capacity of older vintage. This may be advantageous if the price of energy in connection with purchased power is sufficiently low. It is, however, often the fact that even in the case of a relatively inefficient railway plant, as modern plants go, the incremental or energy cost may be less than the incremental or energy charge of purchased power. This removes the incentive for the railroad to take all the energy possible through its purchased supply.

Mr. Torchio also mentions, in instances where a railroad purchases all its power, operating no plants of its own, that a guarantee of payment for a minimum amount of service each year should be included. This may be definitely not of advantage, and if insisted upon by the power company, may force the railroad to install its own generating facilities, rather than risk paying for dead horses in the event of reduction of service requirements during the term of the contract. As Mr. Torchio says, this feature may be taken care of in stipulating a minimum billing demand which will be a specified percentage of a previous demand in each month. It is of course necessary that the power company should be assured of some such protection; on the other hand, however, it may be the case that if railroad service diminishes to a point where this guarantee might be embarrassing to all concerned, the power company may be able to plan to use its installed capacity for supplying other customers, in which case the railroad should not be required to continue to pay for the capacity which it cannot use. This of course would not apply to facilities installed solely for the railroad supply which cannot be applied to other purposes.

Mr. Torchio points out the advantage of the utility furnishing housing and operating apparatus necessary for conversion of power of commercial characteristics to power of railroad traction characteristics. This is fundamentally important and may be a controlling factor in determining the economics of a given electrification project. If a railroad embarks on electrification of a section of its system, considerable capital expenditures are necessary. If the utility supplying power can be persuaded to install all power-supply apparatus, leaving to the railroad the furnishing of the distribution facilities only, the railroad's capital problem may be much reduced. Furthermore, as has been pointed out, and perhaps a more important consideration, the railroad organization is not as a rule adapted to handle technical details connected with power-supply and substation conversion apparatus, and it would be in many cases very desirable to relieve the railroad organization of such responsibility.

Mr. Torchio mentions the advantage in the power-supply utility continually improving the economics of power generation on account of the continual growth in load and improvements in the technique of power generation. This is important and in any contract covering a long term, some provision should be made to permit the railroad to get some advantage of subsequent increased economies in this respect.

Mr. Hill has mentioned the fact that peak loads occurring frequently at irregular and unpredictable times, tend to make the supplying by the utilities of such requirements somewhat difficult and relatively costly. This is no doubt true; on the other hand, however, demands of these characteristics may also increase the difficulty of generation by the railroad itself in an isolated plant, and such demands can often be handled more effectively by the utility having some diversity in load than by the railroad. If the railroad peak demands occur on off-peak times with reference to the utility's commercial load, the advantage is obvious in purchase of power as compared with production by the railroad, provided the utility will recognize the fact in preparing the contract.

Mr. Hill's tabulated statement indicating a load factor for suburban service (based on three maximum clock hours per month) of 33 to 34 per cent, would presumably be relatively considerably less if applied to an annual basis, on account of electric-heat requirements for suburban coaches. If the railroad is furnishing its own power, this would have to be recognized in the design of the power plant.

Mr. Hill has pointed out that where a substantial portion of the energy is generated in hydroelectric plants, a correction factor should be applied because of the high first cost and low operating cost of such plants. This thought might be expanded to permit the railroad to obtain the advantage of plentiful water in wet years, the power company, conversely, obtaining recognition of excess steam generation costs in unduly dry periods.

All things considered, as has been pointed out from time to time by the present commentator, it would seem logical in developing a contract for railroad power to so construct the contract that the power company would obtain its compensation as a return on the invested capital set aside for the railroad service, in the form of the demand charge, including interest (or dividends), depreciation, insurance, and other items which are not chargeable to operating expense; and not to expect any profit over and above this proper return on the invested capital in connection with the actual energy sold, but merely reimbursement to cover unit cost of operation per kilowatt-hour output of its plants. It might be thus specified that the power company would supply to the railroad each month a statement covering its actual operating costs and the kilowatt-hour output of its plants, and the railroad would pay exactly the production cost thus computed per kilowatt-hour. This would make unnecessary the inclusion of contingencies in cost which might never occur, and would take care of fluctuations in other items, such as fuel, labor, social security, and other taxes, etc., and both parties would be thus protected.

D. R. MacLeod (General Electric Company, Erie, Pa.): The papers by Messrs. Hill and Torchio on railway power supply are of very great importance, not only to the public utilities, but also to the railroads which will be electrified in the near future. The situation today is radically different from what it was 20 years ago, when steam locomotives were relatively inefficient. Great strides have been made by steam-locomotive designers in the past two decades, and today it is not unusual to hear operators quote figures which indicate that steam locomotives use approximately four pounds of coal to do the same work as one kilowatt-hour with an electric locomotive. Wide variations will be found in different applications and in different interpretations of what we mean by a steam locomotive doing the same work as an electric locomotive. Actually electric locomotives are in everyday service in this country which can do far more than any steam locomotive under certain conditions.

It is generally accepted that modern methods of generation can deliver one kilowatt-hour by burning one pound of coal. Thus from the standpoint of conservation of natural resources, electrification would save 75 per cent of the coal now consumed by the railroads. To accomplish this saving, great capital expenditures would have to be made. With present labor and material costs, only a fraction of the total railroad mileage would show any real saving if electrified. We have grown accustomed to the idea that the natural resources of the United States are inexhaustible, and therefore we must look to real savings, as understood in our present-day economy, for the justification of electrification.

After all the advantages of electrification are evaluated it is usually found that the cost of electric power should not be any greater than the cost of coal delivered on the tenders of steam locomotives. For the sake of this discussion we will assume that coal delivered to the tender of a locomotive costs the same as coal delivered to a power plant. If this is approximately true, the three pounds of coal which is saved per kilowatt-hour must pay for the capital, labor, and equipment cost of delivering the power to the railroad if we are to secure a balance between the cost of electric energy and the cost of fuel.

The tabulation given in Mr. Hill's paper gives a load factor of 62 per cent. While very few railroads would have a load factor as high as this, it will be used for this discussion. It may be assumed that the ratio of the average load to the maximum one-minute demand is of the order of 45 per cent, and that when due allowance is made for system growth, reserve power supply, unusual load demands, etc., that the installed capacity required may be at least three times the average load.

If coal costs 1.25 mills per pound and maintenance and wages on a plant enjoying a good load factor are taken as 0.75 mill per kilowatt-hour (*Electrical World*, October 9, 1937), the capital expense (including taxes, insurance, depreciation, and a fair return) must not exceed 3 mills per kilowatt-hour if the three pounds of coal that is saved is to pay for the cost of generation. With a capacity factor of $33^{1}/_{3}$ per cent the equipment required to deliver power to the railroad must cost between $70 and $90 per kilowatt.

These figures indicate that even with railroads having very dense traffic, such as the one considered by Mr. Hill, it is essential that the required generating capacity and the cost of that capacity be kept at a minimum. The situation is better in those regions where the cost of coal is high, but a number of railroads with fairly high traffic density are operating in regions where the cost of coal is lower than we have indicated.

Mr. Hill has called attention to the tremendous potential market which exists for power. His figures are a challenge to every power salesman to see what he can do to develop the market in his territory. Those of us who have lived with its development for a number of years are convinced that we have only seen the beginnings of railroad electrification. We are also convinced that this goal will not be achieved without effort on the part of the power companies interested in the sale of power and on the part of the railroads who wish to take advantage of the tremendous possibilities of electric traction.

The most obvious method of approach for the power companies is to work with the railroads in an effort to fit the railroad load cycle into their industrial and domestic load cycles so as to improve their system load factor. They may also consider means of reducing the capital expense of generation by locating plants near the load centers of the railroad.

The railroads can contribute by studying means of fitting their load cycle into that of the power company and of reducing the peaks in their power demand. A railroad is designed to move traffic, and therefore it is not possible to approach the ideal which would exist if trains could be made up and dispatched in accordance with the requirements of the power system. We will always have small trains with large trains and a somewhat random distribution of these trains. Improvements in the methods of handling trains in classification yards, in train dispatching, signaling, etc., have shown that it is possible to improve the load factor very appreciably while at the same time improving service.

H. F. Brown (New York, New Haven and Hartford Railroad Company, New Haven, Conn.): The transportation committee is to be congratulated on the number of valuable papers presented to the Institute at this convention. The papers of Mr. Hill and Mr. Torchio on power supply for railroad electrification are timely and worthy of careful consideration by both railroad and utility executives, in view of the national prominence now being given to the railroad problem. Both of these papers are very much in agreement in general, considering that one author presents his paper from the viewpoint of the railroads and the other from the viewpoint of the utilities. Both papers indicate that the cost of electric power for electrification is not one of the major reasons deterring the railroads from further electrification, and although it is possible that power costs for railway electrification may be somewhat further reduced, it is not clearly indicated that such reductions alone will result in a saving so pronounced as to encourage further investment in electrification by the railroads.

In spite of the more recent development and application of internal-combustion locomotives to the railroads, electrification is still the only solution for those particular railroad problems to which it has been applied during the past three decades, and no other form of motive power has been substituted for such electric operation. The electric locomotive is still the only form of motive power which does not have to haul its power plant around with it, and therefore can more readily handle traffic on grades. As both Mr. Hill and Mr. Torchio point out, operating savings alone are not the criterion of electrification advantages, and after a system has been electrified in part, the indirect savings may often outweigh the direct savings. Still greater proportional returns may be realized where specific conditions have already necessitated a partial electrification of a terminal, or part of a route, if such installations can be extended over enough mileage to utilize fully the investment in electric motive power, and to improve the power load factor. It would be of interest in this connection, to supplement the table shown in Mr. Hill's paper by one more column showing the average total unit cost of power for the various services operated, as this particular reference electrified system grew. It would doubtless show a distinct downward trend.

Electrification should be considered and adopted more in the light of what it will do to hold existing traffic and attract new traffic, such as was the case with air conditioning of passenger cars, rather than in the evaluation of its immediate and direct economies. Once adopted it has been demonstrated that it is one of the most flexible tools available to the transportation industry.

As to purchased power versus power from railroad-owned plants, there is still much to be said on both sides. Bulk electric power generated from steam stations may be said to consist of three important costs, each of which is surprisingly of about the same proportion, namely: one-third fixed charges, one-third fuel costs, and one-third other operating and maintenance costs. The larger the plant or system, the smaller the values which can be assigned to each of these three proportionate parts, but in general their range is within relatively narrow limits.

It is fair to assume that where power is purchased the vendor naturally must make a profit on the sale, as otherwise he would not be in business. Therefore, the railroads must weigh whether they can afford the capital to build smaller independent plants, and pay approximately the same unit cost for their own power as they would for purchased power from a larger system, which price includes the profit to the utility, and often, the cost of conversion to the type of electric power required. Total unit costs today vary between three-fourths of one cent to one cent per kilowatt-hour, and in the present state of the art there does not seem to be much prospect of greatly reducing these costs, either for railroad produced, or purchased power.

As Mr. Torchio points out, a utility, because of its expanding business, can afford to keep its power plants up to date with efficient modern generating equipment at increment investment costs which are soon amortized. If railroad electrification were to be considered as something to be started in a small way to relieve, for example, a terminal congestion, and slowly expanded to cover an entire important main route of considerable mileage, having important traffic, the question of power supply would be without doubt settled by (*A*) purchase where existing plants and interconnecting facilities would give the service with minimum investment charges, and by (*B*) railroad-owned plants interconnected with purchased power on parts of the route where such plants could be justified by a smaller total capital expenditure, with corresponding lower unit energy cost. Such railroad-owned plants could be developed with an increase in business, just as the utility plants are developed. To be sure, this requires a high degree of special technical skill, but this is as available to the railroads as it is to the utilities. An electrified railroad thus would become a quasi-electric utility in itself, (as did some of the former street and interurban railways) and oftentimes would be interconnected with other *defacto* electric utilities, and as such should be able to make more favorable interconnection and exchange rates, much as do the large interconnected electric companies themselves. Power contracts for such interchange of power should recognize the difference in production costs of off-peak versus peak power. Such power purchased by the railroads on this basis would be at a much more favorable rate where the electric-utility load factor was improved, than when the railroad demand added to the utility peak. A great deal could be accomplished in expert co-ordination of railroad-produced and utility-produced power under such conditions, with important savings to both producers and consumers.

The conclusions of both papers as to the future possibilities of electrification and the trend of electrification costs should be heartily endorsed by all railroad and utility electrical engineers.

F. A. Allner (Pennsylvania Water and Power Company, Baltimore, Md.): Mr. Hill's paper is a comprehensive review of the major economic aspects of the power-supply problem for trunk-line electrification of railroads comprising passenger and freight services over an extended area. From the point of view of a group of utilities that had co-operated with the author in the actual solution of this power-supply problem on a regional basis, it cannot be emphasized too strongly that because of its high investment cost, the whole power-supply system, from the source or sources of generation to the points of delivery at the step-down stations, should be planned for a minimum total capital expenditure by both the railroad and the utilities.

Especially in those regional power systems that comprise diversified sources of generation (hydro and steam), located in close proximity to the electrified trackage of the railroad, opportunities of various sorts will be found to effect over-all savings in investment cost, accompanied by other advantages such as economy in operation and greater reliability and flexibility of service. For example, it may be found advantageous to substitute in part for the conventional type of frequency-conversion equipment, direct single-phase generation; or, as pointed out by the author, portions of the railroad right of way may be used for transmission lines of the utility, or joint use by the utility of the railroad's electrification structures may be feasible. In that latter case, the same permanent structure on the railroad right of way supports the catenary construction, the railroad's single-phase transmission circuits, and a three-phase high-voltage circuit serving the dual purpose of large capacity tie line between load centers and of a power supply back-up for the frequency-changing equipment at or near the terminal point of the electrified zone.

However, no generalization should be drawn from the above example of joint use of right of way or structures as each case must be studied and solved under the specific conditions inherent to each problem. Thus, in the same electrified region in which the joint use of railroad structures was found advantageous along a certain straightway route between two large cities, along another route of approximately the same mileage, but the tracks hemmed in on one side by steep hills and on the other by a winding river, the very opposite solution was found preferable, namely, to limit the structures on the railroad's right of way merely to the support of the catenary construction and to let the utility erect the single-phase transmission circuits on a long-span tower line further back from the river over a newly acquired straight right of way through rural territory.

Characteristics of Load

Considerable experience is now available on the characteristics of railroad electrification load as described by the author and it may be of interest to engineers concerned in the planning of power supply for future electrification projects to hear a discussion in greater detail of some aspects of these load characteristics.

The average load factor for the whole electrified system of some 675 route miles is shown by the author as 62 per cent based on the average monthly billing demand calculated from the three maximum clock-hour power drafts but taken from separate days and, of course, excluding days when there were abnormal or emergency conditions. The author also estimates that this average load factor might have been of the order of 75 per cent if suburban electrification, consisting largely of commuters' traffic, were excluded. This trend for further improvement of load factor is well confirmed by the experience of that group of utilities that had been supplying the power for approximately 44 per cent of the electrified route miles of the system described by the author, an area which has only a negligible amount of suburban passenger traffic. In that area the average load factor calculated on the same basis as in the tabulation given in the paper in respect to the whole electrification, was 66.5 per cent during the year 1939. In that same area, also, in part at least, because of the negligible amount of suburban traffic handled by multiple-unit trains, the power factor of the single-phase supply measured at or near the terminals of the single-phase generators was relatively high, rarely dropping below 80 per cent and averaging in the neighborhood of 85 per cent. The other cause of the favorable power factor situation in that area, is the relatively large amount of leading-power-factor charging current contributed by a sizable mileage of high-voltage underground cables and overhead lines.

Utility engineers may also be interested in certain other aspects of electrification load that must be carefully considered in the planning for such power supply and in the successful carrying out of the operating task. In an electrified zone which has relatively little suburban traffic with its pronounced peaks occurring with clocklike regularity during commuting hours, the peak loads of the day as pointed out by the author frequently occur at irregular and unpredictable times. Nevertheless, taken over the year as a whole, the daily peaks occur most frequently during the hour or hours of the highest load demand of the utilities' other commercial load.

As pointed out by the author, fluctuations of load on short railroad electrifications are severe, but are described as not unduly severe where the railroad electrification is extensive and traffic is dense and composed of a large number of train units. Experience in the electrified area previously referred to confirms this statement in so far as it refers to load fluctuations expressed in per cent of clock-hour average. In the earlier period when the electrified zone was approximately one-half of the present, load swings frequently exceeded the one-hour integration by more than 100 per cent. As the electrified zone was extended, these load swings, while at times somewhat more severe in kilowatts, are now only about 40 per cent to 70 per cent of the one-hour average.

The variations of load above and below the one-hour average do not have the characteristics of a very sudden impact or dropping off as certain industrial loads of violently fluctuating character. Therefore, notwithstanding their greater magnitude, these load variations do not present a serious problem of voltage or frequency regulation, except in cases where a relatively small power system or the railroad's own plant would attempt to supply load requirements of this character. However, even in large regional power supply systems feeding an extensive electrification from several sources of supply, as described by the author, parallelism being maintained on the 60-cycle side as well as on the single-phase side at several points, these heavy fluctuations of railroad load create quite a problem for the proper control of power flow across the several tie lines or points of parallelism on the three-phase and single-phase side.

There is a gratifying diversity in daily energy requirements, as distinguished from peak-load requirements, of railroad electrification in respect to the occurrence of 24-hour heavy energy drafts of the other commercial loads of the utilities. Trunk-line electrification in an area with relatively little suburban traffic is a classical example of greater than average load on Sundays than on weekdays. What may be still more gratifying to utilities serving the average type of medium-size cities, including suburban and rural areas, is the fact that on the proverbially heavy Tuesdays of commercial load, the 24-hour energy requirements of railroad electrification are nearly at the lowest among the seven days of the week.

Disregarding the effect of holidays and other special events the seven days of the week, according to the average of daily energy requirements during the year 1939, range in the following sequence in respect to the 365 days average:

	Above Average +	Below Average −
Friday	6.6	
Saturday	6.1	
Thursday	2.5	
Sunday	1.9	
Wednesday		1.4
Tuesday		7.4
Monday		8.4

It should be pointed out, however, that there is no consistent relationship between heavy one-hour peaks of the week or the month and heavy 24-hour energy requirements. Large one-hour power drafts may fall on days of average or below average daily requirements and vice versa.

Power Contracts

With few exceptions, utility engineers will concur with the list of requirements expressed by the author in the concluding part of this paper as representing from the railroad company's point of view desirable features of power contracts.

These exceptions refer to the railroad's preference to purchase power on a straight kilowatt-hour basis rather than on the principle of fixed charges and energy cost. Such a move would be a distinct step backward and would be contrary to all trends during the several past decades in rate making and rules of regulatory bodies aimed at allocating to certain classes or groups of services those components of total power costs that may properly and correctly be allocated to any particular type of power supply.

In the case of railroad electrification loads, there are three elements of cost which can be identified and segregated from each other within relatively small margins of approximation. These three elements are, first, the cost of providing the prime-mover capacity for handling the railroad company's kilowatt demand of power; this will be a component of cost in the form of fixed charges more or less independent of the fluctuations in the amount of energy requirements of the railroad; then the cost of generation suitably expressed in a rate per kilowatt-hour, the dollar payment fluctuating in accordance with the amount of generation required for railroad purposes; and lastly, a third component of cost in the nature of fixed charges independent of the amount of railroad's load requirements in kilowatts or kilowatt-hours, namely, the carrying charges for various special equipment, such as frequency changers, single-phase substations, transmission lines, etc., which have been provided by the utilities exclusively for the purpose of serving the railroad and which are not suitable except to a very minor degree for any other use except that of serving the railroad electrification.

A brief analysis of the nature of these three components of power cost will suggest certain reasonable limits within which the utility may feel permitted to go to meet the railroad's wishes expressed by the author in regard to elimination of provisions on minimum demand or of guarantees of other payments to the power supplying agency. For example, compensation for carrying charges on special equipment not suitable for other than railroad uses cannot properly be allocated to other groups of ratepayers; on the other hand, the payment for energy, provided the unit rates stipulated in the contract are not intended to take care of any substantial element of fixed charges, may properly be expressed without restrictions as to minimum load factor to be paid for whether used or not; the power demand charges usually expressed in a certain rate per kilowatt may be permitted to vary within certain upper and lower limits subject to such limitations as to minimum payment over a given period that would be substantially less of a burden to the railroad than under conditions where the railroad itself would have provided its own generating equipment, and yet not too burdensome on the utility to the extent that it should be given the opportunity of a substantial time lag to permit the building up of load demand from its other commercial customers to make use of the generating capacity released by a sustained drop in the load demand of the railroad.

CONCLUSION

It is believed that there will be general concurrence with the author's conclusion that further extension of railroad electrification will in a large measure depend on the ability of both railroad and utilities to hold the combined capital expenditures necessary for the electrified service as a whole to a real minimum consistent with reliability of service and realization of the major advantages to be secured from electrification as described by the author. The cost of energy, that is, the running cost per kilowatt-hour has for some time past been at a relatively low level and any further reduction of this element of cost will probably appear of lesser importance than economies possible in capital cost for electrified service.

E. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Referring to Mr. Hill's paper, I believe the cardinal principle to be emphasized is that the power supply system—railroad and utility—should be so planned as to involve the minimum total cost consistent with the requirements of the service.

With a complete system of railroad high-voltage transmission lines along all electrified tracks, the utility cost is certainly a minimum. The utility has the choice of generating at the railroad frequency and phase, for example, 25 cycles, single phase, or at 60 cycles, three phase, and converting. Or it may use a combination of these methods if that proves more economical in view of the 60-cycle and 25-cycle load factors. There is a wide choice in the selection of power-supply location. In fact, the utilities may even secure a certain amount of off-peak or emergency load-transfer ability over the 25-cycle lines. However, these high-voltage transmission lines are an important element of over-all cost of electrification.

Passing to the other extreme, complete elimination of 25-cycle transmission lines would require power generation or frequency changer locations at spacings not much greater than present-day substation spacings, if the present trolley voltages are used. Frequency-conversion units at the resulting spacing and load factor would be highly uneconomical and generating units even more so. That is, even though the 60-cycle systems had at smaller pro-rata cost entirely adequate interconnections to distribute the railroad load on the desired plants, the cost of numerous frequency changers at short spacings and poor load factors might prove uneconomical because of the increased cost of frequency changers as compared with transformers.

The 25-cycle lines along a railroad served two main functions:

1. To transmit large blocks of power from one area to another whenever required.
2. To distribute power to the substations.

Utility 60-cycle lines can supplant only the first function, and that only at the expense of more frequency-changer capacity. However with the bulk transmission function eliminated from the railroad the wayside system may be designed most economically to perform the single function of getting power from the supply points to the trains.

Somewhere between these two extremes lies a system in which, by co-ordinated design of the railway and utility facilities, a minimum over-all cost is obtained.

Railway electrification is one of the bright spots in engineering contributions to society. It makes the railroad faster, cleaner, more comfortable, more economical, and more effective. It is certainly to the interests of all concerned to determine the over-all arrangement of power-supply facilities that will result in the minimum cost of delivering power to the trains with the requisite reliability.

N. E. Funk (Philadelphia Electric Company, Philadelphia, Pa.): Mr. Hill has presented a fine paper discussing the major features relating to the supply of power for the electrification of steam railroads. I am in accord with most of what he says. There are, however, some items which I feel must be amplified to prevent the development of an erroneous impression.

Under "Cost of Electric Energy," Mr. Hill states that the trend in average cost per kilowatt-hour has been downward for practically every type of power supply except the electrification of steam railroads, and that the reduction in that case is due mainly to improved load factor or other special conditions.

Fundamentally, the decrease in rates of all types of service has been due to the change in conditions of supply; in the residential supply, particularly, due to a very large increase in use per customer. The rate for the supply of power to an individual large customer such as a steam railway, where practically the only facilities involved are those of generation and the bulk power system, is generally based upon an individual contract of long-term duration which is negotiated with the railroad, and in which the cost of supply over the period of the contract has been considered rather than the immediate costs at the date when the contract was first effective. Thus, the railroad has received all the advantage of rate reductions which it should expect from the beginning of the contract.

The material improvement in steam-generating-station efficiency has been to a material extent offset by the increasing cost of fuel. As an example, for my own company the total cost of fuel f. o. b. stations in 1916 was practically the same per ton as the cost of freight, alone, per ton in 1939.

The tremendous increase in size of turbines and boilers which tends to produce lower over-all power plant construction costs has been practically offset by increased costs of material and labor, so that the net result has not been any material reduction in total power-plant investment cost per kilowatt of capacity. Thus, the fundamental factors affecting the cost of bulk power supply have not been as favorably affected by increased use as have the local transmission and distribution facilities which supply other customers.

Under "Power Contracts", item *d*, Mr. Hill mentions that the railroads would like to purchase power on a flat or sliding-scale rate per kilowatt-hour. It is quite true that this would materially simplify a contract. Unfortunately, the costs of supplying power do not vary in such a simple manner, and it is impossible for me to see at present how any equitable arrangement between the railroad and the power company could be attained unless the rate for power supply is established on the basis of a demand and energy charge.

Under item *h*, Mr. Hill states that no guarantee of load factor should be required of the railroad, nor a minimum demand guarantee depending upon any percentage of the previously established maximum. Since all lasting contracts must be based on fair treatment of both parties, it is hard to see how Mr. Hill could expect to obtain arrangements of unified supply from several utilities and thus obtain diversity benefits and protection from loss of power supply at any locality while omitting these two factors.

If two or more companies are to supply power to the railroad at different points, it is quite essential that both be protected as to the load factor and load supplied, since their investments are based on these considerations. Without these features, the railroad could claim the right, and probably would, to shift the load between companies to its own best advantage, irrespective of the investment made by the supply companies based on the understanding that each was to obtain all the load in its own supply territory, and that the rates in the contract were definitely based upon that fact. Either a minimum load factor or some device to effect the same result should be a definite contractual understanding between the parties.

The railroad must spend large sums of money for electric transmission and transformation facilities and for motive-power equipment; the power company for production equipment.

With variations in load, the railroad company can hardly expect to eliminate the fixed charges on the investment made in its facilities, so there is therefore no reason why it should expect to eliminate the fixed charges of the power company from electrification costs.

A guaranteed minimum demand based upon either two-thirds or three-fourths of the previously-established maximum demand does relieve the railroad of from one-fourth to one-third of the power company's fixed charges for their electrification, while the railroad must continue to pay the full 100 per cent on the facilities which the railroad installed. It seems to me that it is unreasonable to ask for more.

One of the material advantages of purchased power from the railroad's point of view is that after a railroad has once been electrified, the rate of growth is relatively small, and for that reason there will be little necessity of physical additions to their power production plant. Thus, its economy is fixed with the original installation with little chance of betterment, while with purchased power, the production system continually increases in size with the development of other types of business, so that the railroad has a joint advantage of the improvement in production economy that results from the installation of new high-efficiency equipment to care for the increased growth of the total business of the power company.

E. R. Hill: The discussions which have been contributed by engineers representing the railroads, utilities, and electrical manufacturers have been constructive and most interesting.

With reference to the point raised by Mr. Withington, the load factor for suburban service only, expressed on an annual basis, was 28 to 30 per cent—being materially affected by the severity of winter weather.

The data given by Mr. Allner on nature of railroad load, days of the week in relation to daily energy averages, and the diversity in such occurrence with respect to the other general power load, all point toward the feasibility of working out a plan that should result in minimum payments by the railroads for power and energy furnished by the utilities.

Mr. Allner's suggestion regarding the introduction of a "time lag" in working down to more nearly normal a particularly high demand established during a period of great prosperity and heavy railroad traffic is well worth developing in detail in connection with future contracts.

Mr. Funk's explanation of the lack of a pronounced downward trend in the cost of power to railroads is interesting—it lends background to the preference expressed by the writer to provision for payments on a cost basis which should automatically reflect year by year any reductions in unit costs due to advances in the art, growth of customer load, and improvements in diversity. Mr. Funk appears to hold that an unusually high demand established in time of great prosperity and heavy traffic should remain the yardstick for payment of demand throughout the life of the contract, whereas, as above cited, Mr. Allner thinks that this should be gradually worked down by means of a suitable "time lag" in which the utility might build up its load demand from other customers to compensate for a drop in load demand of the railroad.

From the railroad viewpoint the main objection to the guarantee of a minimum demand, expressed as a percentage of a previously established maximum, is not because of normal seasonal variations in load but rather because of periodic business cycles, times of great prosperity, and pyramided railroad load (for handling which the utility has probably had insufficient time to provide more plant capacity) followed by times of great depression. Such depression cycles are a burden on all business and every kind of utility, including the railroads, and one group cannot fairly be asked to take its own punishment and in addition protect another group against loss of load or business. If the percentage is applied only to the maximum demand established during, say, the preceding 12 months the burdensome feature of such a clause would be largely eliminated.

Philip Torchio: The contributions of the various discussers of these two papers have been very interesting and show a widespread understanding of the fundamentals of the contractual relations between railroads and utilities, as well as the broad economic bases on which further progress in electrification rests. I think we all agree that the provisions of railway-electrification contracts must be worked out individually for any given case, and the papers and discussions indicate that both railroads and utilities are agreed on the principal elements of the power contract and the desirability of supplementary provisions covering those special considerations applicable to particular situations. Mr. Withington's suggestion of a "rental" charge plus an apportionment of operating expenses is, basically, the principle which has been followed in all current "demand and energy charge" contracts, and would be directly applicable if the utility property involved were devoted to the exclusive use of the railway or the railway's demand were constant. Unfortunately, such is rarely the case, and consequently the demand charge, which varies the "rental" according to the use made by the railway, will probably continue. In any case, it is apparent that, granting the incentive to electrify, no difficulty in formulating a mutually satisfactory contract exists.

On the larger question of the underlying economies of further electrification, it is gratifying to note a general recognition of the possibility that the relative operating costs, while a very important factor, may not be the true criterion of whether or not to electrify. Up to the present, the relative costs have apparently been the controlling factors, and little attention has been given to the indirect advantages of electrification. There is no serious question of the fact that the electrified road offers attractive possibilities for holding present business or expanding it in competition with other carriers, because of its many advantages of speed, flexibility, cleanliness, reliability, increased capacity, operating efficiency, etc. Mr. Hill's paper and Mr. Brown's comments both support me in this. Unfortunately, these factors are difficult to evaluate, and the tendency has been to disregard them.

But they do exist and must be evaluated before the true value of electrification can be demonstrated. Therefore, I cannot urge too strongly the necessity for continuous study and analysis of the relative economies of steam and electric traction in order to arrive at convincing and acceptable data. As long as we continue to *speculate* as to the indirect value of electrification, progress will continue to be hesitant and uncertain. It is only by a clear demonstration of the net results of all the direct and indirect advantages which are accruing to those roads which have electrified that the incentive for further projects, with great benefit to both railroads and public, will be provided.

Network Coupling by Means of Static Electronic Frequency Changers

Discussion and author's closure of paper 40-16 by Othmar K. Marti, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 495–503.

I. K. Dortort (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): Mr. Marti's paper recapitulates the development of static frequency changers. Much has already been written on this subject, pioneering installations have been made, and there can be little doubt that the near future will see an expansion in this field.

One factor which will probably cause a great deal of misunderstanding among engineers not familiar with this subject is the effect of the single-phase load on the three-phase supply system. Besides the difficulties inherent in the analytical solution of this problem, there are and will be additional difficulties due to lack of agreement on terminology and the loose use of terms originally designed to cover narrowly limited quantities, now expanded far beyond their original meaning.

One of the chief victims in this respect is the term "power factor". Originally this term was applied to sinusoidal voltages and currents, and is generally used to designate synonymously the ratio of watts to volt-amperes, and cos ϕ. As a matter of fact, these definitions have been under fire for a long time and now the situation is still further complicated by the advent of the static frequency changer.

As pointed out in the paper, power pulsations of the single-phase load may be reflected in part or in whole in the three-phase supply. Ammeters in the supply line will measure the rms value of the current integrated over a complete cycle of the low-frequency system, as this current will consist of a 60-cycle fundamental modulated at the lower frequency. This current will, of course, have in-phase and reactive components, to say nothing of the distortion caused by normal rectifier operation. The question is, then, how shall we define the power factor of such a system? It would appear that the term "power factor", if used at all, should be used only in its broadest sense, namely, ratio of real to apparent power.

Two additional complications must be included. The first one applies to frequency-changer systems having an integral frequency ratio. In this case, the three line currents will not be equal, and the in-phase and reactive components of the three phases will therefore be unequal. The second complication can arise in certain systems in which a reactive single-phase load will not only cause fluctuation of the three-phase power, but may actually cause reversal of power flow. This reversed flow takes place not twice in each 60-cycle period, but twice in each low-frequency period.

A rational attempt to define, segregate, and evaluate some of these quantities appears in an article by Doctor R. Feinberg, published in the October 1939 issue of the *Journal* of the Institution of Electrical Engineers of Great Britain. Doctor Feinberg makes use of the term "efficiency factor", defining it as the ratio of "active power" to "mean apparent power". It is equal to the product of the "fundamental wave factor" and the "displacement factor", cos ϕ. This product, as well as its component parts, may not stand rigorous analysis, but a logical means of comparison is provided.

The term "efficiency factor" may cause some trouble. It is used in the same sense that "load factor" is used for daily load cycles, but applies actually to the average loading over a fraction of a second. The term "loading factor" might be applied here with less confusion.

There are two more points that need to be stressed. The statement is frequently made that a certain system can transmit reactive power. It is not really that simple, since it will seldom occur that the power factor or, let us say, displacement factor of the load and supply will be the same. As Mr. Marti has pointed out, at least in some systems the reactive kilovolt-amperes of the load will be taken care of by power pulsations and distortions on the primary side, and there is no simple relationship that can be applied generally.

The other point I wish to make concerns the misunderstanding, or perhaps misinterpretation of the term "leading-power-factor load". One of the systems described in this paper includes a rectifier and an inverter with a d-c link between them. The statement is frequently made that an inverter or frequency changer of this type can supply only a leading-power-factor load. To those familiar with the subject it may seem strange that anyone can misunderstand this statement. Nevertheless, it has been the cause of some confusion. The suggestion was actually made that a frequency changer of this type be employed to correct the power factor of a network. The line of thought must have been something like this: "The inverter can supply only leading power factor and, when connected to a network, will operate at leading power factor. It will therefore improve the power factor of that system." The confusion, of course, is due to a failure to appreciate the direction of power flow. If the inverter can supply only leading-power-factor load the system to which it is connected must supply reactive power to the inverter, and the power factor of the system is therefore decreased and not increased.

D. R. MacLeod (General Electric Company, Erie, Pa.): The rapid development of the multianode grid-controlled mercury-arc rectifier, during the last decade, for d-c conversion applications has been accompanied by intensive investigation of the frequency-changing schemes discussed by Mr. Marti, besides others suggested by our own engineers and by foreign investigators. The use of the grid to control the voltage of a d-c rectifier opened up a new field of investigation and presented new problems for the designer. In the last 15 years a great deal has been learned of the effect of the circuit on the performance of the rectifier and many other factors which govern the performance of a rectifier. The fund of knowledge which we have accumulated will enable designers to produce electronic frequency changers for applications in which technical and economic considerations warrant their selection. New circuits will undoubtedly be developed which will have advantages not possessed by those which have been investigated, and it is hoped that this discussion will be of some assistance in developing them. The discussion is limited to the three-phase to single-phase frequency changers, which would be used to supply power to 25-cycle railway electrification, in the United States.

About six years ago one of the power companies became interested in the work which we were doing in developing electronic frequency changers which might be used to supply power for a single-phase 25-cycle railway electrification. After considerable investigation it was decided that in the present state of the art the electronic device would be higher in cost and did not offer sufficient compensating advantages over the rotating frequency changer to justify its application. In our opinion the situation is essentially the same today in spite of the fact that great improvements have been made in the art and the single-anode ignitor type of unit has entered the picture in the interval.

The most obvious advantage of the electronic frequency changer is the elimination of heavy moving parts and the special foundations which these require. In place of these we have electrical equivalents such as capacitors and reactors, and it becomes a question whether the cost of one offsets the cost of the other.

Mr. Marti claims an advantage in efficiency for the electronic frequency changer, and this claim is well founded when comparing 5,000-kw sets as shown by his figure 11. In major railroad electrifications, large frequency changers are used. The paper does not hold out any hope for an improvement in efficiency of electronic frequency changers as their sizes increase. We assume, therefore, that in the present state of the art, electronic frequency changers three to eight times the size of the one discussed by Mr. Marti and using the circuit which he advocates will have an efficiency of approximately 90 per cent at full load, 93 per cent efficiency at 60 per cent load, and 85 per cent efficiency at 25 per cent load, as shown by his figure 11. Contrasting with this, the efficiency of the motor-generator type of converter increases as the size increases. For example, the 42,900-kva three-phase 60-cycle to single-phase 25-cycle motor generator sets supplying a railway system in this country have an efficiency of 94.5 per cent at full load, 94 per cent at 60 per cent load, and 89.5 per cent at 25 per cent load. Even the 21,500-kva sets show better efficiencies than those indicated by Mr. Marti's curve. The reason for this disappointing showing is that the rectifier loses its essential simplicity when used in a frequency-changer circuit and the consequent losses nullify the advantage which the rectifier has in d-c work. It is our hope that the efficiency curve shown by Mr. Marti will be improved as the art develops. In one analysis made by our engineers the efficiency of electronic frequency changer worked out about two per cent higher than shown by Mr. Marti. It is nevertheless doubtful whether significant improvement can be shown over the efficiencies of large rotating frequency changers in the present state of the art when all the losses are properly compared. The sets operate at high load factor so that loads below 25 per cent of the rating seldom occur for any appreciable length of time. The expectation of an advantage in efficiency has not yet been realized.

Another hoped-for advantage is that the grid control may be used to shut off the flow of short-circuit current. Tests on d-c rectifiers and experience in the field have shown that arc suppression by grid control is a valuable means of reducing the duty on circuit interrupting equipment but that they cannot be depended upon to the exclusion of conventional circuit-interrupting devices. Even when arc-suppression devices are developed to the point where they have the same reliability as conventional circuit-interrupting devices it will be found that circuit breakers will be required to clear certain faults that cannot be handled by the grid control.

A disadvantage of all electronic circuits is that transformer equipment is required to give the necessary number of phases. A rotating frequency changer on the other hand may be connected directly to the three-phase bus as the large sets are usually located in generating stations. This leads to some question in our minds in regard to space requirements. If existing designs of rectifiers are used, we would expect the electronic frequency changer with its auxiliary equipment to occupy more space than one large 42,900-kva rotating-type frequency changer with its starting equipment. The reverse is true in the case of the small equipments discussed by Mr. Marti.

One of the principal disadvantages of the electronic frequency changer is its inherently poor wave shape. On the 25-cycle side this will cause inferior commutation on the series motors, with the result that the locomotive ratings in tonnage which they can haul would have to be reduced appreciably. On the 60-cycle side the poor wave form will cause severe losses in the alternators due to the fractional harmonics due to the pulsating nature of the single-phase load. It would probably be necessary to use amortisseur windings on the turboalternators with any economical design of filter circuit.

When frequency changers are used to supply power to 25-cycle single-phase railway systems, consideration must be given to the fact that the railroad's transmission and distribution is in effect a tie line between parts of the same system or between systems which are tied together by 60-cycle lines. The performance of the frequency changers and the associated 25-cycle tie line must be studied in relation to the remainder of the 60-cycle systems of which they are a part. In some cases it may be desirable to limit the power that may be transmitted through frequency changer sets and variable-ratio units are used which limit the power that can be transmitted through the set within the range of frequencies for which it is designed. In some systems synchronous equipments are used and the sets are free to transmit power in accordance with the difference in phase between the two ends of the 25-cycle tie. They are then able to help hold the 60-cycle systems (or part of the same system) in step, assuming that their ratings are adequate for this purpose. Even in the case of large single-phase systems the ultimate result may be worse with the rigid tie than with the variable-ratio tie, and only careful analysis of the actual systems will show which to use. Either the electronic or variable-ratio rotating-type of frequency-changer set will permit power to flow to the 25-cycle system, even though parts of the 60-cycle system are not tied together. This is a definite advantage of this type of equipment, but it is not apparent that the single-phase electronic frequency changers discussed in this paper would have any great advantage over the rotating type which they would replace.

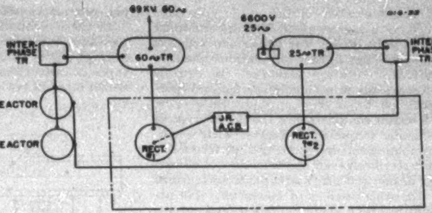


Figure 2. Schematic diagram of main connections, electronic frequency converter

A rotating type of frequency changer delivers the reactive kilovolt-amperes required to transmit blocks of power at nearly constant voltage in a 25-cycle single-phase transmission line and the reactive kilovolt-amperes required by the series type of motor. This is accomplished by means of the field on the single-phase generator under the control of a voltage regulator. The portion of the reactive output handled by each set may be varied by means of the phase shifter. The automatic control of the reactive in accordance with the instantaneous requirements of the 25-cycle system is an important advantage of the rotating-type frequency-changer set which does not appear to have a satisfactory equivalent in the electronic device.

In a companion discussion A. G. Darling discusses the three-phase to three-phase electronic frequency changer and compares them with conventional types.

A. G. Darling (nonmember; General Electric Company, Schenectady, N. Y.): The paper by Mr. Marti describes various means of obtaining frequency conversion using electronic types of converters, and discusses applications of electronic converters to transportation systems.

Transportation-system use of frequencies differing from the usual 60-cycle frequency, in this continent, offers a moderate field for converters. The single-phase transportation system, as contrasted with the three-phase industrial and public-utility system, involves some characteristics and solutions peculiar to itself. When electronic frequency conversion is contemplated for the single-phase system, solutions of the pulsating current, real and reactive, must be found so that the reflections on the three-phase system will not be objectionable. Still another field for frequency conversion, possibly found more frequently, is that of the industrial plant and other power systems which, for one reason or another, have found, and are likely to continue to find, need for electrical conversion ties between their own and other frequency systems.

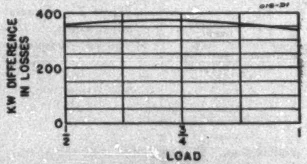


Figure 1. Kilowatt difference in losses in favor of electronic as compared with Scherbius frequency-converter sets of 10,000-kw output, 25 to 60 cycles

The progress made in the design and successful operation of mercury-arc rectifiers is sufficient reason for considering their use as frequency converters. Their relatively high efficiency and low cost of housing tend to offset their lagging reactive current.

Some system ties can be at fixed frequency ratios while others appear to require variable-ratio frequency converters whose load transfer can be adjusted at the converter.

Of the various a-c types of converters using a mechanical link, the Kraemer and the Scherbius are the conventional varieties which are capable of varying their frequency ratio and therefore are capable of keeping the normal load transfer under control. Still another possibility which considers present and future uses for power and equipment consists in converting alternating to direct current and direct to alternating current of another frequency. Many industrial plants have large investments in constant-potential d-c utilization equipment and means of obtaining direct current from a-c sources. Continued use of the present a-c to d-c conversion equipment is the objective in frequency conversion of this order. Additions of d-c to a-c conversion equipment to complete the circuit to the second-frequency power is the next consequence. With the d-c machines paralleled, power may flow from either frequency to d-c load or to the opposite frequency, or to both simultaneously. This method, while expensive in initial investment and less efficient in operation than other systems considered herein, has the distinct advantage of permitting salvage of a large part of the initial-frequency to d-c and second-frequency to d-c conversion equipment as the necessity for continuation of either frequency diminishes. Motor generator sets, synchronous converters and inverters, as well as mercury-arc rectifiers and inverters, all of suitable direct voltage for plant use, are the several means of making possible this type of conversion while supplying d-c plant load.

The mercury-arc rectifier and inverter, when used in the circuit illustrated in figure 2 of this discussion, gives the designer the opportunity to select that voltage of d-c tie which best suits the conditions to be met. For economy and efficiency of the frequency changer, a voltage will be chosen, which, in general, will be much higher than in common use in industrial plants—say 1,500 volts or somewhat higher. This type of electronic converter has been used to compare with a Scherbius set as given in later paragraphs.

The industrial plant offers an ideal location for the electronic converter to interconnect parts of its system or with the system of a public utilities. The variable nature of the industrial plant load from low valleys to high peaks at rapid rates of change suit

Table I. Comparison of 10,000-Kw Frequency Converters, Electronic Versus Scherbius Type

	Electronic	Scherbius
Normal operation		
Motor, input	3 phases, 25 cycles, 6,900 volts	3 phases, 25 cycles, 6,900 volts
Power factor	0.88 underexcited	1.0
Generator output	10,000 kw, 3 phases, 60 cycles, 69,000 volts	10,000 kw, 3 phases, 60 cycles, 69,000 volts
Reactive kva	5,250 kva underexcited	7,500 kva overexcited
Permissible 25-cycle frequency variation	1¹/₄ cycles from 25 cycles	1 cycle from 25 cycles
Maximum loads (per cent)		
One minute	200	200
Momentary	225	200
Frequency ratio without regulation of frequency-changer ratio	Fixed 25/60	Will change with load 25/60 at no load to 25.35/60 at full load
Reversed operation		
Motor, input	3 phases, 60 cycles, 69,000 volts	3 phases, 60 cycles, 69,000 volts
Power factor	0.88 underexcited	0.88 overexcited
Generator output	10,000 kw, 3 phases, 25 cycles, 6,900 volts	10,000 kw, 3 phases, 25 cycles, 6,900 volts
Reactive kva	5,250 kva underexcited	3,300 kva overexcited
Permissible 25-cycle frequency-variation	1¹/₄ cycles from 25 cycles	1 cycle from 25 cycles
Isolated-operation capacity		
Stub feed 60 cycles	None	10,000 kw, 0.8 power factor overexcited
Stub feed 25 cycles	None	None

the characteristics of the static converter which has a high overload capacity and small loss at light load. The equipment requires less space and lighter foundations than an equivalent rotating machine. It can be automatically or manually started and operated.

A recent comparison of the d-c tie type of electronic frequency converter with a synchronous-Scherbius rotating-type frequency converter brings out certain points of advantage in one and other advantageous features of the other variety of converter. No attempt has been made to draw a final conclusion since local economic as well as operating factors must be included to obtain the final answer.

The general requirement to be met by the equipment under comparison for this case was the conversion, under load control, of power from one frequency to power of the second frequency and in reversible directions without break in continuity of service. The details of normal and reversed operation are given in table I.

A comparison of the losses of the two types is distinctly in favor of the electronic converter giving a difference of 7 per cent of the one-half load output, 5 per cent of the three-fourths load output, and 3.4 per cent of the full load output to assist in justifying the use of the electronic type. Figure 1 of this discussion illustrates the difference in losses.

Installation arrangements and housing will vary with local conditions and with personal preferences. However, the cost of installation arrangements and housing for the two types of converters should be comparative for equivalent specifications. One specification, illustrated in figures 3 and 4, was selected for comparison. In this specification, main transformers, reactors, and 60-cycle high-voltage switching were to be installed outdoors on suitable foundations and with transfer tracks and trucks for transformers. The 60-cycle high-voltage switching is not illustrated in these figures, but would be identical for both types of converters. Approximately 1,400 square feet more space is required for outdoor apparatus for the electronic type than for the outdoor apparatus for the Scherbius type. All other apparatus and equipment was housed with foundations, cooling arrangements, switchgear, control, station service power, crane for heaviest parts, oil storage tanks, pumps, and installation material. For the electronic converter, housed floor space came to 2,400 square feet on one elevation, while 7,800 square feet on two floors was required for the Scherbius set. The crane rail in the building used to house apparatus was 18 feet in height for the electronic set and 44 feet for the Scherbius set.

Considering the cost of apparatus and installation material, installed, connected, and housed as planned, the electronic set is estimated to cost in this case 10 per cent more than the Scherbius set.

Many of the more recent installations of rotating frequency converters have been of the so-called "outdoor" type which includes a sheet-metal housing for the converter. Cranes are sometimes omitted. If a comparison of relative installed, housed costs of the electronic and Scherbius converters were made on the basis of "outdoor" designs, the difference in cost would favor the Scherbius type by a much larger amount than the 10 per cent mentioned for the "housed" types.

In these comparisons no effort was made to evaluate the effect of the reactive component of power input and output of the two types of converters. This again is a factor which must be reviewed in the light of local conditions. One system may have an excess of overexcited machines and another a deficiency. The cost of adding capacity to correct the lagging component of the electronic set will have a material bearing on its selection inasmuch as the cost of purely corrective capacity is relatively high. For certain surrounding conditions, the wave shape of the electronic set may need correction. The cost of improving the wave shape, like the cost of adding power-factor correction, should be investigated to draw a conclusion. It is expected that the operating and maintenance costs of the two types (not including cost of losses) will be approximately equal.

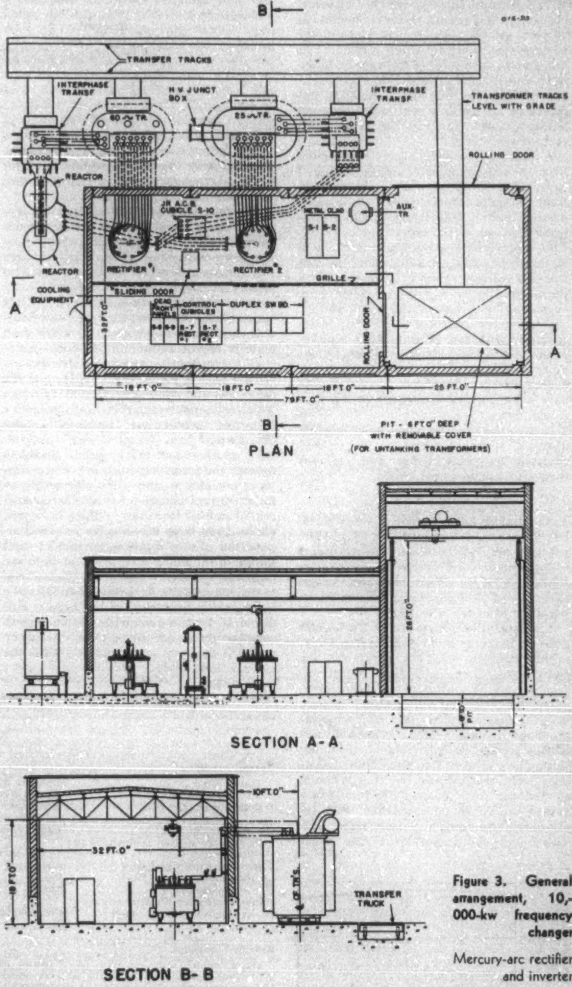
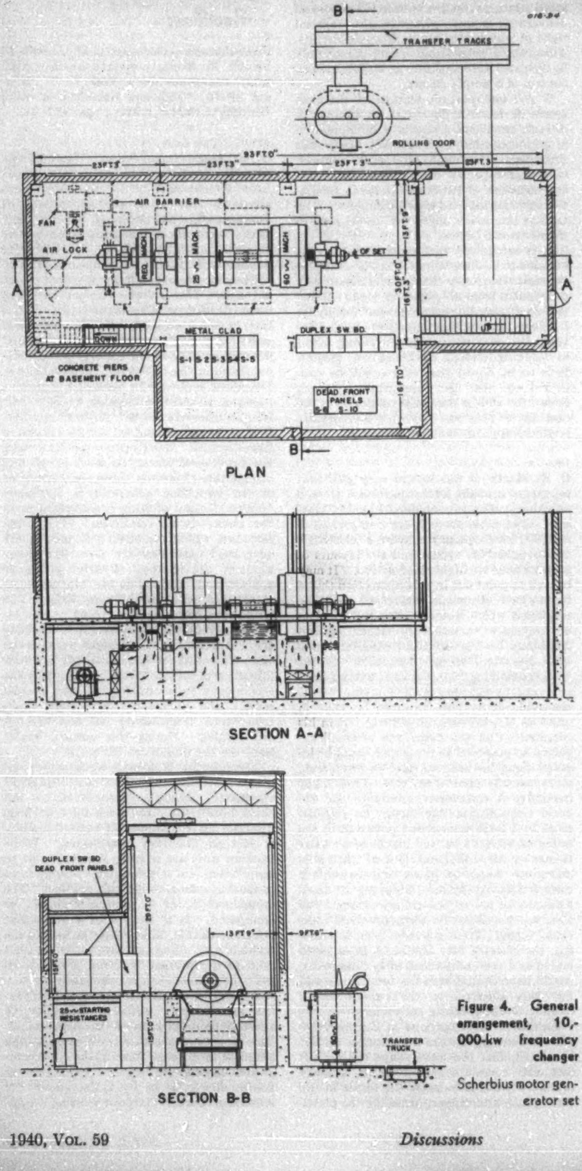

B
A
TRANSFER TRACKS
INTERPHASE TRANSF
60 ∿ TR.
H.V. JUNCT BOX
25 ∿ TR.
INTERPHASE TRANSF
TRANSFORMER TRACKS LEVEL WITH GRADE
ROLLING DOOR
REACTOR
JR A.C.B. CUBICLE S-10
METAL CLAD
S-1
S-2
AUX. TR.
REACTOR
RECTIFIER #1
RECTIFIER #2
GRILLE
COOLING EQUIPMENT
SLIDING DOOR
DEAD FRONT PANELS
CONTROL CUBICLES
DUPLEX SW BD.
S-7 RECT #1
S-7 RECT #2
32 FT 0"
ROLLING DOOR
18 FT 0"
18 FT 0"
18 FT 0"
25 FT 0"
79 FT 0"
PIT - 6 FT 0" DEEP WITH REMOVABLE COVER (FOR UNTANKING TRANSFORMERS)
PLAN
28 FT 0"
PIT
SECTION A-A
10 FT 0"
32 FT 0"
18 FT 0"
TRANSFER TRUCK
SECTION B-B

Figure 3. General arrangement, 10,000-kw frequency changer

Mercury-arc rectifier and inverter

With equal fuel cost, 25-cycle power is more expensive to generate than 60-cycle power; 60-cycle apparatus is more available than apparatus for other frequencies. There are so few speeds obtainable with greater economy at 25 cycles than at 60 that the over-all cost of drives from 60-cycle source is less, in general, than from 25-cycle sources. For these reasons there is a tendency to discontinue the use of 25-cycle industrial power and substitute 60-cycle power. Interconnection of power sources often is advantageous at equal as well as at odd frequencies. These several factors draw attention to the types of frequency-converting equipments that are at present available. Without prejudice toward one or another type, the conclusion reached from various investigations and a review of the design, reliability, as well as purchase cost and operating cost, leads to the conclusion that the electronic converter has many merits deserving consideration in comparison with several points of advantage obtainable in the rotating converter for use in three-phase to three-phase frequency conversion.



PLAN

SECTION A-A

SECTION B-B

Figure 4. General arrangement, 10,000-kw frequency changer

Scherbius motor generator set

W. S. H. Hamilton (New York Central Railroad, New York): The writer was very much interested in the presentation and discussion of this paper and also in those by Messrs. Torchio and Hill (Transactions, pages 530–3 and 560–2) which were presented at the same session covering a closely related subject. The discussion of those two papers especially brought out the need for keeping the total investment for the supply of railway power to a minimum regardless of whether this is financed by the power company or the railroad company, in order to secure the minimum over-all cost of power.

Mr. Marti's paper deals with a phase of this same subject and a very interesting one, namely the possibility of securing 25-cycle single-phase power from a 60-cycle three-phase system at good efficiency and by static conversion.

It seems to me, however, that more emphasis has been placed in recent years on the single-phase 25-cycle trolley and its related transmission lines than is really justified by the facts. By using 3,000 volts direct current on the trolley, the power-supply problems for both the power companies and railroads are greatly simplified, inasmuch as this system uses static rectifiers operated from three-phase, 60-cycle commercial power systems and no special railroad transmission lines are necessary, except in those localities where the power companies have no adequate tie lines of their own. Such cases are quite scarce now.

It would seem to me that a further study of the 3,000-volt d-c system especially by the power companies might indicate the advantages of this system of electrification more forcibly than they appear to have been brought out at the present time.

Also in this connection the possibility of a high-voltage 60-cycle single-phase trolley system should not be lost sight of. The Electric Railway Traction Supplement to the *Railway Gazette* (London, England) for December 8, 1939, gives an interesting description of a locomotive for such a system which is under development in Russia at the present time. It is of course too early to discuss the probable economics of such a system but it can well be given further consideration.

A. Schmidt, Jr. (General Electric Company, Schenectady, N. Y.): Mr. Marti has made a timely contribution to the literature in this country regarding the use of electronic devices in static frequency changers. Several equipments are already in operation in this country.

The first static thermionic frequency changer built by the General Electric Company is a 300-kva 60-cycle three-phase to

$25{}^{5}/{}_{7}$-cycle two-phase unit, built for the New York Shipbuilding Corporation in 1934 and described in Electrical Engineering, October 1935. This is a fixed-ratio type of frequency changer with no d-c reactor. The number of anodes firing in each half-cycle is the same, but the firing position of an anode changes in successive one-half cycles, consequently there is no low-frequency pulsation in the output voltage and anode duty is uniform. With this circuit reactive power is supplied by the 60-cycle system to the load. An output frequency of 25 cycles with similar anode duty would require a five-phase 60-cycle transformer. Grid control is through 60-cycle saturable grid transformers, saturated periodically through a thyratron-controlled $25{}^{5}/{}_{7}$-cycle saturating circuit.

In 1936 a d-c link type of frequency changer was installed between a 40-cycle power source in Mechanicville, N. Y., and a 60-cycle system in Schenectady, 17 miles away. This is the first d-c transmission system of commercial size in America. Power up to 5,250 kw is transmitted at 30,000 volts direct current. A total of over 86,000,000 kilowatt-hours have been transmitted to date. The combination of separate rectifier and inverter, with d-c link, results in wave forms in both a-c circuits having minimum interference factors for a given number of anodes.

More recently, a model-size frequency changer has been built and operated in a textile mill, where a variable-frequency output of 100–210 cycles is needed. No intermediate d-c link is employed in this circuit. Reactive power is supplied by static capacitors and electronic voltage-regulating means are provided.

It may be noted that there is a choice between, on the one hand, the d-c transmission type of frequency changer with separate rectifier and inverter and a d-c link which may be feet instead of miles in length, and on the other hand, a composite type of circuit with no d-c link. The designer must weigh the relative advantages of the two circuits. The total number of anodes is the same for given current wave forms. The d-c transmission unit requires two anodes in series, with resultant higher losses than the composite circuit. On the other hand, the composite circuit requires more complicated transformer construction and grid circuits which combine control functions at both frequencies.

While this is a recent application of a power-converting device which has comparatively recently reached its majority, the reliability of the mercury-arc rectifier merits its consideration in this field. As a measure of its acceptance as a power-converting device, it may be noted that the present world installed capacity of rectifiers exceeds ten per cent of the installed generating capacity in the United States.

Sidney Withington (New York, New Haven and Hartford Railroad Company, New Haven, Conn.): Mr. Marti's paper is very stimulating and opportunity is taken, from a railroad viewpoint, to emphasize some of the advantages which he has mentioned in the development of the static frequency changer for railroad power supply. The motor-generator frequency-changers which, for economic reasons, are installed in large capacities in concentrated substations for railroad electrification, make necessary the installation of transmission facilities along railroad right of way. In some cases these railroad transmission circuits would be obviated by the installation of apparatus described by Mr. Marti, which, in relatively small units, might be installed outdoors at convenient points wherever the railroad right of way is crossed by 60-cycle circuits. These static substations would presumably be operated automatically or by supervisory control of a simple nature.

A not unimportant feature of the electronic device described is as an interrupter of fault currents by means of manipulation of grid excitation, to which Mr. Marti calls attention. This might well make unnecessary high-capacity circuit breakers which represent one of the costly items in substations serving railroad traction circuits. The savings thus made might well be credited to the electronic device.

It is recognized that much development remains to be done before it can be said that the static frequency changer is commercially practicable from all points of view; nevertheless, it may be said somewhat definitely, I believe, that if such apparatus can be developed it may constitute a deciding factor in some individual electrification project. It is to be hoped that efforts will be continued and that the practicability of such apparatus will be demonstrated, as was the case 12 or 15 years ago with mercury-arc rectifiers applied to railroad traction.

O. K. Marti: I was indeed very glad that my paper brought forth the release of such valuable studies as contributed by Mr. Darling. The table showing the comparison of a 10,000-kw frequency converter (electronic versus Scherbius types) and the layouts of such stations are highly instructive. It may be well to point out in this connection that a three-phase electronic converter could be employed which would permit full freedom of frequency variation in either system. This may be kept in mind when studying table I of Mr. Darling's discussion.

In considering Mr. MacLeod's discussion, referring to efficiencies of rotating versus electronic converters, it must be kept in mind as Mr. Withington pointed out in his comments that electronic sets of small capacity as proposed in my paper could be located along the railroad right of way, being automatically controlled, etc. This might partially or completely eliminate the 25-cycle transmission line along the railroad track. If large converter equipment of the order of 40,000 kva, for instance, as mentioned by Mr. MacLeod, is used, then it is necessary to step up power and transmit it over a 25-cycle system, stepping it down again to be fed to the trolley system, and this, as can easily be seen, involves additional losses. It would have been interesting, therefore, if Mr. MacLeod, in order to arrive at a fair comparison of both systems, would have enumerated the total losses and not only referred to the partial losses, namely of the rotating converter.

Extensive investigations in Europe have shown, contrary to the assumption of Mr. MacLeod, that the wave shape did not affect the commutation of series railway motors nor did it increase the losses in the three-phase alternators, including the power pulsations, more than it does in a single-phase generator of a present rotating converter.

"Excitron" Mercury-Arc Rectifiers

Discussion and author's closure of paper 40-74 by O. K. Marti, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 927–31.

G. F. Jones (nonmember; Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Marti has given an interesting description of the Excitron. He has also made several statements about the ignitron which we believe he would not have made had he been more familiar with its construction and operation.

The single-anode tank rectifier is of course not new. The ignitron rectifier has been under development for eight years. It has been in commercial operation for over three years. A paper describing it was presented during the 1939 winter convention and was published in the 1939 AIEE Transactions, December section. In view of the present paper it is most interesting to note Mr. Marti's discussion of the paper referred to.

The method of ignitron ignition shown in figure 2 of Mr. Marti's paper has been obsolete for several years. A choice of schemes using either thyratron tubes and capacitors or an impulsing generator is available. Neither method of firing is dependent upon the power-circuit conditions. The firing impulses, which are peaked in nature, are accurately controlled for time and power input to the ignitors. Positive pickup is assured, and, contrary to Mr. Marti's statement, delayed firing does not exist. The losses of either system are small.

The adverse effects of an anode misfiring are exaggerated. In regular commercial electrochemical service, one anode has been taken out of service for 24 hours. With the remaining anodes carrying full load, normal service was maintained with only slight transformer temperature rise and without any surging. During this unusual operation, the harmonics are increased.

The need for a cathode insulator for the ignitron rectifier has been very definitely eliminated. Except experimentally, it has never been used. The need for a cathode insulator for a continuously excited rectifier is just as definitely established. While vacuum seals are reliable, it is desirable to keep them to a minimum. Large-sized insulating seals are a difficult problem. The cathode insulator in particular should be eliminated. It is located in a vulnerable position; that is, adjacent to the heat of the cathode spot. The number of vacuum seals used in the present Excitron design is 50 per cent greater than for the ignitron.

The excitation arc for any type of rectifier must supply sufficient ionization to ensure reliable pick up of the main anodes. This ionization is sufficient to cause a high arc-back rate unless deionization is accomplished during the negative half cycle by shields and grids or by extinction of the ionization source. A figure showing a complete cross section of an Excitron would have been of interest. We believe it would show additional baffles in the arc path. Some, not shown, are mentioned in the paper. "The ignition and excitation anodes are arranged and shielded in such a way that little ionization is added to the residual holding over during the negative voltage period." Figure 6 shows a rapid increase in arc drop with increase of load. This rate of arc drop increase is comparable to that of a conventional sectionalized rectifier of similar rating. The increase in arc drop from 2,000 to 4,000 amperes is three volts. For the ignitron the comparable figure is approximately 50 per cent of this value.

Based on our experience with single-anode rectifiers and the results of extensive testing we question whether the arc drop values given in figure 6 can be obtained in a rectifier having the freedom from arc back required for commercial service. Similar low arc-drop values have been obtained from experimental ignitrons. However, the arc drop of the ignitron, suitably designed to ensure reliable operation, is somewhat higher than the values given by the curve. At the same time, the efficiency of the ignitron, for the conditions specified in the paper, is several per cent higher than the efficiencies given for the Excitron. The information is not consistent.

The Excitron excitation circuits and apparatus shown in figure 5 are not complete. The starting anodes with their d-c source consisting of transformers, Rectoxes, and control relays or equivalent equipment are not shown. The paper lists and the figure shows only a simple transformer. If the additional devices necessary had been shown, it would be apparent that the excitation system is quite complicated and that there are many moving parts requiring maintenance. It might be pointed out that the circuit shown is not operable. There is no provision for maintaining the excitation arc, from the single-phase source, through the zero-voltage period. The individual excitation circuits are subject to failure due to apparatus faults and arc instability. This of course would give rise to misfiring of the associated main anodes. While we do not believe this is of much consequence, Mr. Marti regards this operation as a hazard. Are there any precautionary measures taken?

According to records available to us, there have been approximately 40 power rectifier units, having a combined capacity of 140,000 kw, purchased during the past 12 months. Of these units, 36 are ignitrons with a combined capacity in excess of 130,000 kw.

H. C. Myers (nonmember; Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The writer would like to call attention to figures 2 and 3 of Mr. Marti's paper. Here are illustrated a diagram of connections for a six-phase ignitron rectifier with a diametrical six-phase connection, and the corresponding voltage wave form when one ignitron fails to carry load.

It has been the practice of the company which originated the ignitron type of rectifier to operate power rectifiers in multiples of three-phase groups. With this method of operation, the direct voltage produced by the rectifier appears quite different from that produced by a diametrical six-phase rectifier when one anode fails to carry load. The writer has drawn a curve to illustrate this condition (figure 1 of this discussion). It will be noticed the disturbance to the voltage output is relatively unimportant compared to the case of the diametrical six-phase-connected rectifier.

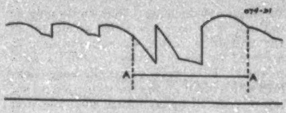


Figure 1. Diagram of voltage wave of double three-phase rectifier

Section *A-A* indicates wave form when one anode fails to pick up load

The writer has conducted tests on rotating machines of the series-motor type when operated from double-three-phase-connected ignitron rectifiers in which one ignitron was deliberately prevented from carrying load. The quality of commutation observed was very good with either six-anode or five-anode operation, the difference being of very small degree.

Joseph Slepian (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The major conclusion towards which the author seems to strive is apparently the first sentence in his closing section labeled "Conclusion". "From the above it may be seen that a single-anode mercury-arc rectifier having continuous excitation can be just as reliable and free from backfires as a rectifier with intermittent ignition". However, nowhere in the paper are any figures given for the backfire rate for either type of single-anode rectifier.

During the course of the development of the ignitron rectifier at the Westinghouse company the question as to whether similar results could not be obtained with continuous excitation was repeatedly raised. At at least three stages in the development this question was put to experimental test.

At the very beginning, before the discovery of the ignitor principle, it was attempted to run single-anode tubes with unshielded anodes close to the mercury with continuous cathode arc excitation. For currents of even less than 50 amperes and with less than 300 volts, this proved quite impossible. The arcback rate was many times per hour. But as soon as intermittent excitation was tried, successful operation was obtained. In fact one of the first commercial ignitron rectifiers, installed in coal-mine service at Union Collieries, Pittsburgh, three years ago, is of just this simple type and has been operating at rated load of 167 amperes per anode at 275 volts with no arcbacks reported during that period.

To carry larger powers, even with the ignitron, so far it has been found necessary to interpose baffles and shields between anode and mercury. When a structure was developed in the laboratory which was suitable for 400 amperes per anode at 600 volts, it was felt necessary again to check whether continuous excitation might not give results as good as intermittent excitation. The same tubes were used in the two tests. For the continuous excitation, direct current was applied to the ignitor terminals and a continuous arc was maintained, the ignitor upper terminal serving as excitation anode. When sufficient continuous excitation current, about 20 amperes, was used so as to insure stability of the arc, and reliable pick up of the main anode, six arcbacks in a single day were obtained, with currents not over 300 amperes. But with intermittent excitation a laboratory run of three days did not give a single backfire.

I do not want to appear dogmatic and say that satisfactory single-anode rectifiers with continuous excitation cannot be made. However, I do wish the author had given some figures for the arcback rate actually obtained in his rectifiers. But whatever the results with continuous excitation in any one tube, my own experience indicates that much better results can be obtained if intermittent excitation is substituted for the continuous.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Marti has presented an interesting description of a single-anode tank rectifier. It is gratifying that Mr. Marti has finally published an arc-drop curve for conventional rectifiers and also has agreed that the arc drop increases with increase in size of tank. Incidentally, single-anode tanks have this same characteristic.

It is unfortunate that Mr. Marti failed to include some of the points on which his single-anode-rectifier arc-drop curve was based, and also a statement of the method of measurement. Arc drop is influenced by the amount of protection given the anode, that is, the shielding around the anode and the anode-cathode spacing. Although not the only cause of arcback, an important cause is the ionization in the tank during the negative-voltage portion of the cycle and the most important reason for protecting the anode is that the tank is not "blacked out" during all of this period. Following commutation of current to another anode, there is a residual ionization determined by the current commutated and the geometry of the space. This residual ionization decays over several electrical degrees and is the same for ignitron or Excitron. In addition to this residual ionization, the Excitron has an ionization due to the excitation arc that continues over the entire cycle. In view of this, it is fundamental that the anode protection required with continuous excitation could never be less than that with synchronous excitation. Of course, there is a relationship between the degree of shielding, arc drop, and arc-back frequency. Reliability may be sacrificed by an attempt to lower the arc drop by reducing the shielding.

The "relatively high current" required for ignition of an ignitron and the "very small current" sufficient for excitation of the Excitron are mentioned in the paper, but no figures are given. I would be interested in the current and voltage required for Excitron excitation for reliable cathode-spot maintenance. Present ignitron ignitors require a current of the order of 25 amperes for only a few microseconds, for reliable and accurate firing. From the operator's point of view, it is the excitation loss that is important and this varies with the circuit used. In any event the loss of ignitron excitation circuits is not usually the determining factor in choice of method.

I wish to correct the impression given in the paper that ignitors are delicate and require highly accurate immersion. No trouble has been encountered in maintaining satisfactory immersion, which may vary as much as one-fourth inch, in normal service even during extreme overloads. With proper excitation circuits, ignitors do not erode away, or otherwise fail, due to either normal currents or severe arcbacks.

Mr. Marti again discusses arc extinction during fault conditions by grid blocking. This is an interesting possibility of controlled rectifiers of all types, but not highly important because it is not reliable, and it is no more reliable in single-anode tank rectifiers than it is in multi-anode rectifiers. Such arc blocking somewhat reduces duty on breakers, but it does not eliminate breakers because it is not dependable. The performance is only from 80 to 90 per cent.

O. K. Marti: I agree with Doctor Slepian that in my paper more specific references should have been made to the backfire rate. The rate of backfire, however, was assumed to be similar to that experienced in connection with the well-known multiple-anode type of rectifiers and the recently introduced ignitron rectifier. Therefore, my statement that the Excitron rectifier is just as reliable as the ignitron rectifiers is based on the fact that it showed about the same backfire rate as any type of commercial rectifier on the market today.

I was indeed very glad to learn of the experience Doctor Slepian had with single-anode rectifiers having continuous excitation. From his figure given for the excitation current as being 20 amperes, it may be concluded that the unsatisfactory results obtained by him are due to his high excitation current. In our excitron rectifier only a fraction of this value is necessary for reliable operation.

It is possible that using a design where the anode is relatively close to the cathode (three or four inches), the backfire rate may be less if intermittent excitation is used. However, we found that in order to obtain an appreciable output from a tank the anode had to be located at such a distance from the cathode that no difference would result whether intermittent or continuous excitation was used.

As to Messrs. Cox's and Jones' references to the arc-drop curves as shown in my paper, I would like to say that the curves were chosen simply for illustrative purposes and were measured using methods approved by the AIEE Standards. The slope of the curve for the single-anode tank is somewhat steep due to the fact that the tank was very restricted in diameter, being less than ten inches, which may explain to Mr. Jones the rapid increase with increasing load current. Mr. Jones must realize that the slope of the arc-drop curve is a function of the design of a single-anode tank rectifier, similarly as in the case of a multiple-anode rectifier. The tank as illustrated in my paper had only a grid, and no shield as inferred by Mr. Cox and Mr. Jones, and still the arc-back frequency was well within the limits I mentioned in connection with Doctor Slepian's discussion. We do not agree with Mr. Cox that arc-back susceptibility is a function of the degree of shielding. This would only be the case if all backfires would occur during the transition period as was erroneously assumed by many rectifier engineers until recently. It is possible that the very backfires which occur during the transition period can be reduced by additional shielding, and if the tanks are increased in diameter or if the current for a given tank is raised to a high degree, then shielding may mean an improvement in arc-back rate.

D. C. Prince's publications in the book "Mercury-Arc Rectifiers and Circuits", refer to several tests calling attention to the fact that arc backs could not be noticed during the transition period. This fact was also more or less confirmed by data disclosed by Messrs. Pakala and Batten in their paper presented during the 1940 winter convention in New York (AIEE TRANSACTIONS, volume 59, 1940, June section, pages 345–7), and was also proved by many investigations our company carried on during the last three years. Therefore, the rate of arc backs cannot be affected by merely reducing ionization by the means referred to by Messrs. Jones and Cox.

Referring to Mr. Jones' discussion of the typical connections for ignitron and Excitron rectifiers, I would like to say that they were shown of the simplest kind for both types, merely to illustrate the difference between both in the principle used. I am fully aware of the latest method used in connection with the most recent schemes of ignitron rectifiers, which, as Mr. Jones admits, requires the use of additional auxiliaries, such as capacitors, etc., which were apparently all necessary to obtain stable operation and a proper picking up of the main anodes.

With reference to Mr. Myers' discussion, it will be understood, of course, that the diametrical connection is seldom used by any one, and figure 3 was based on this connection merely for simplicity. We cannot understand, however, how Mr. Myers obtained the wave form which he shows in his discussion for five-anode operation. We wonder if that figure was obtained from actual oscillographic measurements. We find that from purely theoretical considerations the wave shape of the direct voltage, when using an interphase transformer and one anode missing, the voltage not only comes down to zero, but stays there at least one-sixth of a cycle, which is considerably worse than the case illustrated in figure 3. It would seem that Mr. Myers failed to take into account the fact that the output voltage will be the average of the two three-phase groups, and that the group in which one anode is not firing will go negative by an amount equal to the voltage of the other group in the positive direction.

Operation of a double three-phase system with only five anodes firing for any length of time may cause damage to the interphase transformer, as a high 60-cycle voltage will be impressed across the interphase transformer which is designed for a 180-cycle voltage of much lower value.

Papers are in preparation which will describe in more detail the design of the Excitron unit as well as the method used for ignition and excitation. As I said before, the purpose of my paper was merely to bring out the reasons which induced designers to make a new attack upon the development of a single-anode tank, and furthermore to give some comparative data between a rectifier having intermittent ignition-excitation and one with continuous ignition-excitation, and to show that such a tank can be designed to operate properly using either method of ignition.

Electric Braking for Railroad and Urban Transit Equipment

Discussion and authors' closure of paper 40-28 by F. H. Craton and F. M. Turner, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 489–94.

Sidney Withington (New York, New Haven and Hartford Railroad Company, New Haven, Conn.): One item in this valuable paper which it may be of interest to note, is that in development of dynamic braking as compared with regenerative braking on heavy trains, the dynamic method is relatively more common abroad. It is of interest to note that in this country the regenerative braking has been developed in territory where the power supply is either from hydroelectric sources or from power plants located in the midst of coal fields where the cost of fuel is minimum. The value of the regenerated energy in both cases is thus relatively very low.

Joseph F. N. Gaynor (Great Northern Railroad, Wenatchee, Wash.): There is an old axiom among railroad operating men that lack of speed is only a small worry. The real worry is "How fast can I stop in a pinch". Regarding the advantages of regenerative braking, the saving in power alone adds up in direct money value to the saving in brake shoes and wheel tires varying with location, but here in the Cascade Mountains the over-all amounts to 25 per cent of the power to operate the system. In addition to this we haul twice the tonnage which we used to in 25 per cent of the time. The overtime saving and the ability to haul fewer, faster, heavier trains are the direct result of (1) electric locomotives, and (2) regenerative braking. The limits of regenerative braking are mainly motor capacity and adhesion. Our experience on the Great Northern on 2.2 per cent and 1.6 per cent grade operation indicates 15 per cent to 20 per cent adhesion as the ultimate tonnage hauling limit, and commutation or field-to-armature ampere ratio of one to three as the top speed limit with any given tonnage. When regeneration capacity of locomotives is inadequate to hold a tonnage train between the two limits given, supplementary air braking is used freely to maintain top speeds beyond the regeneration range or to limit the substation regeneration load. On the Great Northern system, power is supplied by one 25-cycle 5,000-kw hydro station and two 6,500-kw frequency-changer stations. Hence the maximum regeneration load at the frequency changer stations must not exceed 8,000 kw. To keep within this limit, by many road tests we have rated our station regeneration kilowatts against train gross-ton miles per hour for the different grades. For example, on 1.6 per cent grade the gross-ton miles per hour per kilowatt is 30. Taking the gross-ton miles per hour capacity of locomotives of 225,000, with a tonnage freight of 6,570 gross tons including locomotives, the speed at which the locomotive continuous capacity will not be exceeded is 35 miles per hour on a 1.6 per cent grade. Under this condition the frequency station regeneration input is 7,500 kilowatts. Hence in figuring regenerative-braking limits it will be noted that adhesion, motor capacity, commutation limits, locomotive mechanical speed limits (often approached with lighter tonnage trains on the Great Northern grades), and finally the system capacity for absorbing regenerated power all combine. Theoretically, train sheets can be made up so that trains are regenerative while others are motoring to help absorb regenerated power, but practical railroading can only move tonnage as it is received and operation is based on that system.

In decelerating trains in regeneration, once continuous speed and full load have been reached, all decelerations must be made by air braking. The Great Northern has found by experience that in fast passenger service even where ample capacity for regenerative deceleration exists, more satisfactory operation results and time is saved by using air in station stops or short distance slow orders. Comfort and safety in both passenger and freight service are guaranteed by regenerative operation as slack surges are missing.

During steam and all-air braking operations over mountains, the wrecker was a constant visitor to the territory. While we have had derailments pulling trains up grade where draft gear failed or cars folded up, in 13 years there has been only one derailment chargeable to regeneration, when cars were telescoped in a freight. This was found on investigation to be a case of the helper engineer holding too much in the middle of the train, stretching out slack between the head-end engine and the helper and then suddenly shutting off power as the head end began to stop. The slack piled down against the head end kicking cars out of the train.

From observation of our Great Northern 1,000-horsepower Diesel-electric switcher, I have noted one thing lacking to make this locomotive an even better switching unit. Switching moves are practically all accelerations and decelerations kicking cars, etc. The steam locomotives which the Diesel replaced were easy to stop quickly. While the brake at present can be set up for a quick stop, due to rotational effect of armatures and the concentrated weight of the engine itself of 125 tons on four axles, a severe brake is very likely to skid wheels and the engineers must get accustomed to the new power. The result is that they are afraid of flattening drivers and thus do not use as much power as is available so that safer stops can be made. The final result is that the locomotive is not being worked at the speed and capacity it is capable of simply for lack of a good dynamic brake. A fairly moderate outlay for equipment would pay for a good dynamic brake which would more than pay for itself in increased work which the engine would put out because of being able to stop more quickly.

There is no reason why the same kind of application would not work satisfactorily on a streamliner as in stopping a train where the locomotive weight is approximately 30 per cent of the train weight, dynamic braking would help reduce wheel checking and skidding.

Joseph C. McCune (nonmember; Westinghouse Air Brake Company, Pittsburgh, Pa.): I would like to congratulate Messrs. Craton and Turner upon their able paper, a paper particularly well written and very effectively presented.

Confronted with an audience of electrical engineers, I am not tempted to venture comments upon the technicalities of electrical apparatus. The authors have, however, and especially in their introduction, made mention of the capabilities of air brakes. Since the air-brake field is the one in which I am directly engaged, I would like to point out, from an air-brake viewpoint, some of the problems encountered when electrical brakes are employed to supplement air brakes.

These problems are not new as electrical brakes of one sort and another have been employed for many years. Thus, combined dynamic, track, and wheel-tread brakes were introduced some 40 years ago; regenerative braking some 30 years since; and eddy-current brakes have been employed in England for highway vehicles for a considerable number of years. Modern developments have greatly improved these brakes, especially in the direction of giving the operator a more precise and positive control of the retardation produced and of reducing initial and maintenance costs.

As the authors state, electrical brakes of the regenerative type, installed on the electric locomotives of certain large railroads, have made an excellent showing in actual service. Automatic interlocks have been devised which very satisfactorily interlock electrical and air brakes so that each type is adjusted to its proper place in the combination brake system. This experience indicates that electric locomotives used in grade service offer an attractive field for electric brakes, the air brakes being held in reserve for emergencies.

To the air-brake engineer, however, this is quite a limited field. By this I mean that grades where electrical braking might be employed probably amount in the aggregate to less than three per cent of the total railroad mileage of the country.

A condition exists in this service, moreover, which should not be overlooked. As the authors of the paper point out, if a certain locomotive is able to take a train up a grade, it should be able, with even less difficulty, to get the same train down the same grade. But many important grades, particularly on ore and coal roads, involve the movement of empty cars up the grade, as to a mine, and the movement of loaded cars down the grade. If the electric locomotive is designed with only sufficient capacity to take the train up the grade, it is clearly inadequate to take the loaded train down. It is obviously uneconomic to increase the size of the locomotive merely to be able to use electric braking in the descent of the grade.

But grade braking, although very critical in isolated instances, represents, as I have already pointed out, only a very small percentage of train miles operated. The most important task of the brake is to stop trains in emergencies to save lives and avoid property damage. These emergencies may arise at any time, in either grade or level road service. To the engineman confronted with a washed out bridge, a stalled automobile, or a displaced rail, no stop can be too short.

When an attempt is made to provide for short emergency stops in high-speed service, with any form of brake, two difficulties are encountered. First, rail adhesion makes the minimum stop possible from high speeds of considerable length and, in fact, the best that can be done, from the adhesion viewpoint, is none too good. Second, the heat to be dissipated in a high-speed stop taxes the capacity of any form of brake.

The adhesion available makes it impossible to do all the braking on the locomotive because then the stops would be prohibitively long. It is clear that if satisfactory stops are to be obtained, each wheel in the train must be braked close to the limit of adhesion. Only by distributing the braking throughout the train and by developing the maximum practical retarding force at each wheel, both on cars and locomotive, can short stops be secured. To shorten stops for the new high-speed operation, therefore, it is necessary to provide effective brakes on both cars and locomotives. The problem is so closely associated with available rail adhesion that it is important to work both car and locomotive brakes, as nearly as can be done, to the limit, having in mind the adhesion realizable in practical everyday service.

The second problem faced by any brake in high-speed service is the matter of heat dissipation. A brake may be considered to be a mechanism for converting mechanical energy into heat just as a motor is a machine for converting electrical energy into mechanical energy. Brakes tested for service on modern cars have dissipated, as an instantaneous maximum, 2,000 or more horsepower per axle, which obviously represents a heat problem of considerable magnitude.

Braking apparatus for high-speed service must, therefore, be adapted to producing braking torque on all wheels of the train, both cars and locomotive, approaching the slipping value. The braking apparatus must also be capable of dissipating very great quantities of heat in very short times. But it cannot have too much bulk, for space on cars and locomotives is at a premium. It cannot be too heavy but on the contrary, its weight should be an absolute minimum. It must bring the train to a dead stop and hold it stationary on grades. It must be absolutely dependable because the lives of passengers cannot, in any way, be jeopardized. It must retain its efficiency with the minimum of maintenance attention in order that equipment may be freely interchanged throughout the country. All this must be accomplished without an undue initial or maintenance cost because the railroad must have low operating costs to meet competition. All importantly, the brake apparatus must be of a character which will permit it to harmonize with existing brakes in order that the ability to interchange cars freely over the North American continent may not be impaired. In view of these requirements, which represent by no means an exhaustive catalog, it seems to me that the authors are somewhat too optimistic as to the part electrical braking may play in future high-speed train operation. New forms of friction brakes, applicable to both cars and locomotives, seem likely to overcome the current difficulties with conventional brakes.

L. K. Sillcox (The New York Air Brake Company, Watertown, N. Y.): The authors are to be complimented on their achievement in covering a broad subject most effectively in a few concise paragraphs.

The applications of regenerative and dynamic brake equipment upon locomotives must always be supplementary to a complete conventional air-brake system. Electrical-braking means and the value of any dual equipment for braking service possess merit from a reliability standpoint; yet any possible supplementary safety features of electrical braking forms are of negligible value as such, in view of the variety of automatic safeguards surrounding conventional air-brake designs. For instance, the Bureau of Safety of the Interstate Commerce Commission recognizes the automatic protective features of air brakes as they are now installed and in no cases does it recommend a supplementary system.

As the authors point out, electric braking is available only on the power unit and the measure of its adequacy is governed by the proportionate weight carried upon the wheels to which the regenerative or dynamic braking may be applied. Consequently, its value is limited to that of control. On descending grades a brake which dissipates train energy electrically, limiting its action to the power car, is of peculiar value. In controlling the speed of trains down long grades by the use of air brakes, it is the practice to apply the brakes on all trailing cars, holding the brakes upon the locomotive released during much or all of the braking cycle. This is the opposite practice of that employed with regenerative or dynamic substitute. The merit in employment of the locomotive brake for the purpose resides in the fact that, in braking a train from the forward end, slack between cars is compressed throughout the train and, in the usual case, the inherent resistances due to changes of grade or by reason by intervening stretches of curved track, cause no change in the slack condition. On the other hand, with pneumatic braking, brakes applied to all cars uniformly, any portion of the train is susceptible to local changes in track resistance which may cause slack directional changes and internal train shock of relatively minor consequences so long as there is no interruption in the descending grade.

The dynamic brake is offered as a practicable application for Diesel-electric locomotives. The necessary electric equipment can, of course, be readily applied but the problem of dissipating the heat generated must always prove a burden to this type of motive power. Provision of elements for this purpose will be supplementary to any other equipment provided and the use of the cooling water is undesirable inasmuch as no greater capacity is built into the cooling system than is required for normal engine operation. The tremendous cooling overload due to heat dissipation in braking would have to be separately provided for.

Electric-retardation control is complex and has ever been costly, adding considerable weight in many cases. It has definite merit which can be readily evaluated but, under present operating conditions and with the equipment as it has been made commercially available to date, anything which is done to assist the flexibility of control by superimposing a regenerative or dynamic braking system must be in addition to a complete and co-ordinated air-brake system.

F. H. Craton and **F. M. Turner:** As Mr. Sillcox points out, the electric brake cannot be expected to displace the air brake. Electric braking inherently fades out at low speed. This in itself necessitates the use of the air brake in conjunction with the electric. Furthermore, it seems probable that in any practical application of electric braking on individual cars, the electric brake would be used to provide only a portion of the total braking power at medium and high speed. There would be some optimum point of division of the braking duty between air and electric braking on a given car. The electric brake would be of such capacity that in high-speed decelerations it could relieve the air brake of that portion of the load which now makes the difference between an easy, practical problem and an extremely difficult one. It would have sufficient capacity to handle all or most of the braking duty in retardations on mountain grades. The air brake would assist the electric brake in all duty demanding more than the electric brake could handle and the electric brake would handle entirely any braking requirement within its capacity.

Electric braking supplementing the air brake is a similar application in successful operation on hundreds of trolley coaches, and although the electric brake alone can produce only two miles per hour per second deceleration against a maximum of seven miles per hour per second with the combined electric and air systems, brake-shoe life has been multiplied several times.

We are indebted to Messrs. McCune and Sillcox for their valuable comments on this subject. No one can fail to have the greatest respect for the aggressive way in which the air-brake manufacturers have assailed the problem of high-speed braking. The accumulation of fundamental data, of which there were none in the region of 80 to 100 miles per hour, was carried out in the now famous Zephyr tests in 1934. New devices were developed to refine the air-brake control to meet the demands of new conditions. Experiments with nontread friction brakes are under way. Today, high-speed trains are in successful operation using 100 per cent air equipment except for electropneumatic control. Why then any need for another approach? Only because friction braking appears to be approaching its limit. Even at today's peak speeds of about 100 miles per hour, it may well be that partial electric braking can provide the relief necessary to ease a difficult operating problem at best. With any appreciable increase in top speeds, it is quite conceivable the braking demands may prohibit the use of 100 per cent friction braking from the standpoint of practical everyday operation. It is suggested that the proper approach to the problem may be a blending of friction with electric braking of the eddy-current type, which will project the practical top speed range well beyond anything we have today.

One point in connection with railroad braking which Mr. Gaynor brings out in his excellent report on electric braking on the Great Northern, is the possible application of electric braking to Diesel-electric switchers. In competing with steam switchers, the Diesel-electric is handicapped by a deficiency in braking power due to absence of a tender whose adhesive weight is used to augment the braking power of the steam locomotive. Because of the importance of braking in speeding up switching operation, particularly in kicking cars, the reduced braking power of the Diesel-electric is a handicap which now must be made up by faster acceleration and easier handling. While Mr. Gaynor recommends dynamic braking, we wish to suggest also the use of magnetic track brakes to supplement the wheel brakes on the Diesel-electric. Magnetic track brakes would overcome a large part of this deficiency, resulting in a clear gain in switching performance over present Diesel-electric operation. Although magnetic track brakes have not, to our knowledge, been employed in such an application, the handicap in braking performance mentioned is one frequently cited, and magnetic track brakes, possibly in conjunction with dynamic braking, offer a solution.

In connection with Mr. Withington's comment regarding the common use of dynamic braking abroad, this may be influenced by the electrification systems employed. There are various applications of regenerative braking on high-voltage d-c systems in Europe and South America. The application of regenerative braking to single-phase systems using locomotives with a-c commutator motors has presented a difficult problem although we believe that when there is a demand for such braking in this country the problem will be worked out satisfactorily.

A brief review of the principal types of electric brakes used in urban transit service appears desirable in this closure. The electric holding brake, which is primarily designed to hold a vehicle at a safe speed while descending a grade, is not a particularly new application. This form of electric brake is most successfully applied only over a relatively limited speed range, and usually requires a separate manipulation of a controller handle or actuating switch to place it in operation. This type of electric brake has been used almost universally on the older types of gas-electric busses, but its limitations in application preclude its general use during service stops.

The more recently developed types of electric service brakes, which are operated automatically by either the conventional power or brake control pedal, and which are utilized during each service stop, have the widest field for future application. The major portion of new street cars and trolley coaches is being furnished with electric service braking incorporated in the equipment. It is believed that the use of electric service braking on gas- and Diesel-electric busses will also show a marked increase in the near future.

There are three principal types of electric service braking now in successful operation.

1. A single-rate, self-regulated dynamic brake designed for use with one or more series-wound motors. This type of service brake is applied to older-type street cars and trolley coaches equipped with manually operated, nonautomatic control, and to gas- and Diesel-electric busses.

2. A multipoint variable-rate dynamic brake designed for use with one or more series-wound motors. Applications of this type of brake are limited to modern street cars and trolley coaches equipped with automatic control.

3. A multipoint variable-rate dynamic or regenerative brake designed for modern trolley coaches equipped with a single compound-wound motor and automatic control.

We wish to thank Mr. Clardy for bringing up the subject of using accelerating and braking resistors to heat the vehicles, with a consequent reduction in energy demand during cold weather.

Unsolved Problems of Electrical Engineering in the Field of Transportation

Discussion and author's closure of paper 40-15 by J. A. Noertker, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 992–6.

G. M. Woods (nonmember; Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): A common weakness of human nature is to feel that one's particular line of work is very ably conducted, and there is room for only minor improvement. Mr. Noertker's paper points out new lines of activity for improvement of electrical engineering in the transportation field.

There is no question of the ultimate desirability of using commercial frequencies for a-c electrification and the subject warrants continued investigation. Present commercial systems probably can be adapted to handle the single-phase loads imposed by major electrifications.

The commercial compound motors available for trolley coaches reduce resistor losses in slow-speed running. But the over-all energy consumption is slightly greater than that of the modern series motors. It is interesting that the author recognizes that the metadyne is too complicated and heavy for general application to urban transportation units.

The modern capacitor-type lightning arrester answers the requirement of an effective d-c arrester without high maintenance.

The PCC street car is an excellent example of a successful development financed by operators and manufacturers. However, any new apparatus requires development costs, regardless of the organization doing the work. There is no evidence that an organization of the type suggested would be able to make these developments even as economically as the manufacturers. What can be accomplished is to co-ordinate the efforts of the operators and avoid a multiplicity of parallel wasteful developments for both operators and manufacturers.

L. C. Josephs, Jr. (Mack Manufacturing Company, Allentown, Pa.): Mr. Noertker's paper has been particularly interesting to the writer, because of the very stimulating viewpoint. The writer has been a nonconformist in transportation matters for many years, and, therefore, particularly appreciates some of the suggestions. Although the nonconformist's life is not always smooth sailing, there ought to be a certain number of them in every industry. They are a means of keeping industry moving by insisting on being different, and out of these differences, come the better things.

Coming now specifically to the trolley coach, is the idea of 600-volt d-c series a "sacred cow" that we all must worship? If we could get over this fixed idea, there might be many new fields of improvement opened up. For example: a-c 60 cycle; the possibility of using a motor that had more induction motor characteristics; the possibility of using some type of control built around the Spinner principle. It might even be an advantage to use a gear box or two-speed axle with mechanical ratio changes to improve the efficiency of the electrical system.

The metadyne control development in Europe is apparently a step along this direction. Whether the advantages to be gained are worth the price is always the deciding question. Usually, the answers to these questions cannot be found from a theoretical discussion, but someone has actually to try these out, and learn painfully all the things that are wrong with it.

I want to express again my thanks to Mr. Noertker for his very valuable contributions to this subject.

D. R. MacLeod (General Electric Company, Erie, Pa.): The 12-kv 25-cycle single-phase system of electrification has been developed for use by railroads handling commuter traffic and heavy through trains. No particular technical problem would be encountered if it were decided that contact-line voltages of 24 kv, 25 cycles, or even higher were desirable. The 3,000-volt d-c system has also been developed for the same purpose. The public utilities are the logical suppliers of power for these electrifications; nevertheless, it is important to note that in the case of both the a-c and the d-c system, special generators or means of converting power must be supplied since the three-phase 60-cycle power cannot be used directly on the existing systems. This introduces additional losses and increased cost of power, which is always important in any electrification study. The cost and difficulties of operation of a two-wire overhead permitting the supply of three-phase to rolling stock prohibits its use in main-line work. Single-phase 60-cycle locomotives could be built, although they would probably prove uneconomical. It would also be possible to design a distribution system using this frequency. Unfortunately, the large blocks of single-phase power would cause such losses in the three-phase system that it would be necessary to use phase-balancing equipment. This would largely nullify the advantage of going to 60 cycles. It is possible to use different phases of a three-phase system to supply different trolleys so that a reasonable balance could be obtained. This method has been tried on 25 cycles and while it was satisfactory for a small local electrification it would be difficult to justify this compromise on a main-line electrification.

The picture is somewhat different, although the conclusion appears to be the same on an urban transportation system, especially on those properties where the trolley coach with its two-wire overhead is designated. The load factor on the power supply to existing d-c trolley coach systems is very poor and the all-day efficiency is probably between 30 and 50 per cent from the 60-cycle power supply to the wheels of the coaches. Modern rectifiers have over-all efficiencies of the order of 90 per cent and load-responsive starting by automatic control is available to reduce the standby losses. The efficiency of the power supply is therefore comparable with that of direct transformation. The maintenance expense of rectifier substations and their relatively high cost are the principal disadvantages of this method of power supply. On the other hand the recent development of the d-c compound-wound traction motor has improved the efficiency and flexibility of the d-c trolley-coach equipment while retaining the light weight which makes this type of equipment so desirable. The d-c equipment is of the order of 50 per cent of the weight of any 60-cycle competitor. "Metadyne" equipment with d-c motors would give efficient control with no greater increase in weight than would be required for a-c equipment.

The part of a trolley-coach system which costs the most and is used the least is the overhead trolley. The conversion equipment also has a very poor capacity factor. The restriction of the use of this equipment to the trolley coaches is therefore almost as uneconomical as if the streets on which they operated were restricted for their use. Here then lies the principal argument for a 60-cycle trolley-coach system. If the utilization of the overhead can be increased by using it for other 60-cycle services along the right of the way it may be possible to justify the disadvantages to the rolling stock of the single-phase 60-cycle system. A 60-cycle trolley coach could be developed since the experience on 25-cycle series motors could be extended to 60 cycles. A 60-cycle series motor would probably weigh over twice as much as a d-c motor, and would require a step-down autotransformer. The individual currents taken by a coach are relatively small and a compromise scheme of balancing the phases as indicated before would probably be quite satisfactory without the use of rotating-type phase balancers.

In another part of this paper Mr. Noertker suggests that there is a great need for a better lightning arrester. The capacitor-type lightning arrester was developed a number of years ago for d-c circuits. It does not have the valve action possessed by the aluminum-cell arrester, but functions by sloping the front of the lightning impulse. The capacitor is charged through the surge impedance of the line which may be considered as a resistance for the purpose of approximate analysis. This arrester is used on practically all PCC car equipments, and is applicable to other equipments as well. They require no maintenance and may be left in service the year round. They are not effective on relatively low frequency switching surges. The Thyrite arrester for a-c circuits has a gap unit which interrupts the follow-up current when it passes through zero. Since there is no means of forcing direct current to go through zero with a simple gap unit, this type of arrester has not been developed for d-c systems. Thyrite can, however, be used on d-c circuits. For example, it is used to reduce the inductive kick which accompanies the opening of d-c solenoids.

E. W. Brandenstein (General Electric Company, Erie, Pa.): Mr. Noertker's paper peers into the transportation of tomorrow, and stimulates our consideration of future problems. Undoubtedly the best method of tackling a problem which deals with future transportation is, first, to set down the aims of the rail transportation industry relative to future improvements in their service. It is, therefore, desirable and necessary for designers of transportation equipment to work very close to operating men so as to obtain from them their ideas as to future requirements.

There are no continuous electrifications in this country of, say, 1,000 route miles or more. The economics of a long electrification of this nature are, therefore, unproved. Such an electrification would result in high locomotive mileage; low engine maintenance; high load factors and commensurate low power costs; and a type of superior service which has never been provided anywhere in the world. An electrification of this size would introduce a large number of possibilities which would automatically assist in solving a number of the problems suggested in Mr. Noertker's paper.

A trunk-line railroad should be able to set down specifications relative to the size of passenger trains, the weight of cars, the maximum speeds, the distance between stops, the frequency of trains, the heating requirements, air-conditioning requirements, lighting requirements, the freight-train sizes, freight-train spacing, freight-train speeds, the train communication requirements, etc. With such basic specifications, it would be possible for the electrification engineers to make comparative studies and definitely determine the trolley voltage and head-end power for auxiliaries, so that once the distribution voltage was decided upon, the other problems would be comparatively easy to solve. It is, of course, understood that the solution would apply to electrified trunk line because the cars as well as the locomotives would have to be assigned to that system and would not be transferable to other roads.

Modern locomotives should be designed with due consideration for the many auxiliary power requirements on passenger and freight trains. It might be possible to design a head-end auxiliary power plant for a steam or Diesel-electric locomotive that would duplicate the head-end power furnished by an electric locomotive. This would permit interchange of passenger cars between roads having electric engines and modern steam or Diesel-electric locomotives.

It would be highly desirable to consider the possibility of forming a committee which would work with representatives of the American Association of Railroads in order to formulate definite specifications relative to passenger-train lengths, car weights, schedule speeds, number of stops, top free-running speeds, air-conditioning loads, lighting loads, heating loads; freight-train sizes, car weights, top speeds, communication requirements, and other general specifications on which railroad operating officials would agree as being necessary for meeting competition in the future. These various problems should then be tackled by the designing engineers for two types of railroading—electrification and steam or Diesel-electric locomotives.

The results of such an investigation should lead toward a standardization of the fundamental power equipment for the various train services.

Sidney Withington (New York, New Haven and Hartford Railroad Company, New Haven, Conn.): The AIEE is to be congratulated in the presentation of this paper which courageously covers many items of fundamental interest. The proposed research group which Mr. Noertker suggests would appropriately consist of men with primarily scientific approach, supplemented of course by operating men in making practical, operating application of the analysis. The primary stimulus, however, should be that of the scientist rather than the operator.

Many other items which might well be analyzed by such an organization as has been mentioned, would be the economics of electrification. The unprejudiced and scientific viewpoint of this important problem in railroad operation would be, in the long run, invaluable.

Mr. Noertker mentions the advantages of standardization. It is apparent that it is not logical to wait for perfection, as many feel necessary, before definitely adopting a standard. Had this philosophy governed, the rail guage would not have been standardized and untold confusion would have resulted in multiple gauges. The same reasoning would apply to car couplings, air brakes, and other devices now generally used in railroad operation and necessarily adapted to interchangeability of equipment.

Matters which might be referred to the committee which Mr. Noertker suggests, include servicing of air-conditioning equipment at terminals, which now must be capable of supplying cars with battery voltages of 32, 64, and 110 volts direct current and with a-c supply of several different characteristics. Standardization in this regard must necessarily ultimately take place, and the sooner agreement is reached, all things considered, the less will be the operating inefficiency.

J. W. McNairy (General Electric Company, Erie, Pa.): Those of us directly interested in transportation are indebted to Mr. Noertker for this paper. It is both timely and stimulating. Let us hope that the challenge will not go unheeded and something practical can be accomplished, at least along some of the lines suggested.

The paper mentions many lines of development, some of them radical, all of them interesting. A few brief comments having to do with the activities of equipment manufacturers along some of the lines suggested in the paper, particularly in connection with urban transportation, might be of interest.

Wasted power is always an admission of imperfect engineering and the lack of perfection is strikingly illustrated by the figures given on the paper applying to existing types of trolley coaches and PCC cars. As suggested in the paper, considerations of first cost, low maintenance, and simplicity at the expense of power wasted determined the use of the straight rheostatic type of control on these vehicles. Some compensation for these relatively high rheostatic losses has been attained in modern equipments by using this energy for heat, and some further improvement results from the use of motors which permit a relatively large amount of field control, and by the adoption of relatively high accelerating rates to reduce the length of time the rheostats are in the circuit. The extent to which these innovations can be carried is obviously limited by practical considerations of size, weight, and cost in the one instance, and rail adhesions and passenger comfort in the other.

The balance between first cost, maintenance, and cost of power, of course, varies with the location. Series-parallel equipments of the PCC type with dynamic braking have been developed and are now operating in at least one European country. In so far as transitions are concerned, a very practical method of making these transitions over the entire range of accelerating rates without noticeable surges or shocks has been worked out on these equipments and tests and operating experience in service have demonstrated that a degree of smoothness comparable with that of the rheostatic type can be obtained. The first cost and additional equipment required are such, however, that the rheostatic type still meets with the most favor with the power costs as they are in this country. The more complicated series-parallel type appears to be justified in localities such as that mentioned where power costs are relatively much higher.

The application of the compound motor to the trolley coach both in this country and abroad has provided a degree of flexibility which is equal to and, under some circumstances, decidedly superior to the series-parallel type of control used in combination with standard series motors. At the same time this flexibility is obtained without sacrificing simplicity and low maintenance but the power wasted is still high. This type of equipment is meeting with increased favor and its popularity is attested by the fact that 57 per cent of all trolley coaches ordered in this country during 1939, and 67 per cent of those having electric braking, were of this type.

The armature-excited d-c machine, one type of which is known as the metadyne, has introduced numerous new possibilities both for the control and conversion of d-c power. The English application of a machine of this type on subway equipments has aroused considerable interest and is being followed very closely. Comprehensive studies of the possibilities of this type of equipment to subway systems in this country have also been made. Unfortunately, at the present stage of the development, the amount of control apparatus is not reduced and the complications and added weight have interfered with commercial developments. Attractive energy savings are possible, however, and these machines do have enticing characteristics and great flexibility. It is therefore difficult to predict at this time what the future may bring forth in this direction.

It might also be interesting to note in passing that a d-c transformer of the general type suggested in the paper, making use of relatively small light mechanical parts in combination with highly efficient static apparatus operated at frequencies considerably higher than 60 cycles was built more than ten years ago. A relatively large amount of d-c power was transformed in various ways by this equipment. Certain mechanical difficulties in maintaining exact synchronization and timing between mechanical parts and the electric frequencies necessary to insure arcless commutation prevented commercial application. Perhaps there is something more to be learned in the light of modern developments.

Vibrating inverters have been successfully applied for fluorescent lighting of railway cars, taking power from the low-voltage battery circuits as a source. As this development proceeds it is possible that an extension of this type of equipment to other fields may take place and it is also possible that the development may proceed to a point where power can be taken directly from third rail or trolley circuits and inverted for use by certain auxiliaries. At the present moment the motor generator set seems to meet with the most favor for converting high-voltage rail or trolley power for use by auxiliaries and there are numerous applications operating successfully. Designs of tube-type inverters for applications of this type have been worked out and tested but so far they have not been competitive from a cost, weight, and reliability standpoint, with the more conventional motor generator sets.

Several methods of modulated heat control for passenger vehicles have been studied and there seems to be no engineering reason why this type of control cannot be used. The use of saturated reactors or transformers on a-c powered equipments permitting fine graduations of power for heating with a relatively small amount of controlling energy presents attractive possibilities. Modulated vapor systems have also been developed which seem to meet all of the requirements. Here again the type of equipment adopted seems to have been dictated by consideration of first cost, and maintenance, but there still doesn't seem to be any good reason why a problem of this nature could not be satisfactorily solved if the project is given sufficient impetus and there is a persistent demand.

Perhaps, as the author suggests, the thing that is most needed in our field of endeavor is a better method of initiating our research programs and a better type of organization for directing such activities. Certainly it is important that we keep abreast of the times in transportation fields and to this end that we investigate each new scientific development in the light of what it might or can do for the transportation business.

W. S. H. Hamilton (New York Central Railroad, New York, N. Y.): Mr. Noertker's paper should be of great interest to all of us who are in or interested in the transportation field. It was very interesting and instructive to participate in the collection and collaboration of some of the underlying data for this paper, which was part of my duty as a member of the AIEE committee on transportation.

We owe a great deal to Doctor John C. Parker who first suggested that papers of this type be attempted, as the committee found it quite difficult to set down concretely in black and white a number of the problems that confront us in the transportation industry. I believe it would be well worth while to present another such paper in say five years from now, and it would be very interesting to see then how far we had progressed in the solution of these problems.

Mr. Noertker is to be congratulated on the very able and interesting presentation he has made of this matter, for the number of unsolved problems which he has enumerated that the committee as a whole did not think of, and especially for the suggestion he has made in regard to a centralized research organization for the transportation industry. The manufacturers of equipment for the transportation industry should view this paper as a challenge to their ingenuity and resourcefulness to produce solutions for these problems which have been enumerated primarily by those of us in the operating field.

It is not expected that all of these problems can readily be solved within the next few years and some of them, perhaps, never, but I have great confidence in the ability of the manufacturers to solve these problems once they are convinced of the desirability of so doing, which I hope we shall at least have demonstrated by our discussion of this paper today.

Now, I wish to give my own personal comments briefly on one of the problems of this paper, namely that of a standard system of electrification for trunk-line railroads in the United States. Many papers have dealt with this and also with the desirability of using commercial power sources for feeding the catenary system.

In considering such a standard system, I desire to point out that while the motive power is not always the governing factor in an electrification, it usually represents from 50 to 60 per cent of the total cost and hence it is on the side of economy to keep the cost of the motive-power units to a minimum. To accomplish this, it has usually been found most economical so far to do the converting of energy at the wayside substations and make the motive-power units users of energy only and not converters as well.

The difficult problem of the 25-cycle single-phase system is the operation of series motors on heavy mountain grades, particularly where helper service is involved, and wherever this system has been used to date in this country other and less efficient types of locomotives have been employed.

The difficult problem of the 3,000-volt d-c system is to keep down the cost of multiple-unit cars although this problem was well solved in the Delaware, Lackawanna and Western electrification.

Both systems require conversion of commercial power to special power for feeding the catenary and hence are pretty much on a par so far as this factor is concerned.

Many attempts have been made to combine the ease of transformation of the a-c system with the ruggedness of the d-c series traction motor but to date such combinations have been handicapped by high costs of motive-power units and by considerably lower efficiencies than either of the two systems they attempted to replace.

The Electric Railway Traction Supplement to the *Railway Gazette* (London, England) for December 8, 1939, gives a very interesting description of a locomotive to be operated from a 50-cycle 20,000-volt a-c trolley but with d-c motors supplied by a variable-voltage mercury-arc rectifier. If such a system can be worked out at reasonable cost, weight, and good efficiency for locomotives, and if rectifiers, let us say of the sealed-in type, can be made available for multiple-unit cars without unduly increasing their cost and weight, we might very well have a system suitable for universal adoption in the United States, incorporating therein the advantages of the 3,000-volt d-c system of which I have long been an advocate.

J. A. Noertker: The author appreciates the sympathetic and constructive nature of the discussion presented. There seems to be little dissent from the idea that the problems mentioned are both real and significant. The conclusion that there is need for a more effective research organization in the transportation field, seems to be accepted by both operators and manufacturers.

It is gratifying to have men of such wide experience and such diverse interests as Sidney Withington and E. W. Brandenstein point out additional potential values of a transportation research organization.

Mr. Josephs' comments concerning trolley-coach motor control are stimulating. It is a satisfaction to hear that trolley-coach builders are not satisfied with present types of control and realize that further development is possible and desirable.

Mr. Woods' pertinent comments indicate that the fundamental idea of the proposed research organization was not clearly expressed in the paper. The author had in mind that the primary purpose of this group would be to determine reasonable and possible performance specifications. There would be little overlapping with existing manufacturers research activities, which would continue to be devoted to the development of new and more efficient methods for meeting the required performance. Such an activity would parallel—in a modest way—in the railroad and urban transportation field, the work now being carried on for the benefit of the aviation industry, at Wright Field, where a huge staff of scientists and engineers are engaged at government expense, preparing performance specifications and testing equipment submitted.

J. W. McNairy has presented an excellent review of the status of many of the problems mentioned in the paper. It is apparent from his discussion that solutions for several of the problems mentioned are already at hand.

The discussion by D. R. MacLeod, in addition to containing very pertinent factual material, developed several novel ideas that could well be expanded into a paper. Mr. MacLeod's brief discussion of Thyrite for use on both a-c and d-c circuits was particularly informative.

Perhaps as several of the discussers have indicated, the simple capacitor-type lightning arrester is the answer for d-c circuits. From a maintenance standpoint there is no question, but the author is still doubtful as to whether they are equal to the aluminum cell in their surge discharge capacity.

W. S. H. Hamilton, as chairman of the AIEE transportation committee, was primarily responsible for having this paper written. It was at his request and through him that various members of the committee submitted suggestions concerning unsolved problems.

In closing the author thanks those who contributed to the paper by their suggestions and discussion.

Engineering Features of the 230-Kv Boulder-Chino Transmission Line

Discussion and authors' closure of paper 40-17 by Alex A. Kroneberg and E. M. Hunter, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 982–7.

Eric T. B. Gross (Ithaca, N. Y.): Appreciation is due to Messrs. A. A. Kroneberg and E. M. Hunter for publishing their paper, which contains some new and very important data, and seems to me to be one of the most interesting in the field of the application of earth-fault coils, published recently in this country. Although earth-fault coils have been used on 220-kv lines for the past ten years on the lines of the R.W.E., connecting the water-power stations in the region of Vorarlberg in Austria with the steam power stations in northwest Germany, by a double-circuit line not far from the French and Belgian border, no data concerning tests or practical experience have been published by the R.W.E. (Rheinisch Westfaelisches Elektrizitaetswerk) so far. But as I am somewhat familiar with these questions, I want to report that the results were satisfactory and that nothing unexpected has been observed, besides perhaps the necessity of cleaning the insulators more often in the misty coal-mine district near the Belgian border. The length of said double-circuit line is more than twice that of the Boulder-Chino line, but the in-phase component of the fundamental wave of the compensated earth-fault current of one circuit is only about 70 amperes or seven per cent of the whole capacitive earth-fault current. It may be worth mentioning, that earth faults on one of the circuits of the double line do not influence the other circuit, as decoupling coils are also in use.[1]

The residual current of about 80 amperes mentioned in the paper seems to be extremely high, as it is about 25 per cent of the capacitive earth-fault current. I should like to ask what the in-phase component of the fundamental wave of the residual current really was and whether it has been really measured, as described in the technical literature[2] by wattmetric measurements or found by analyzing oscillograms. From such measurements it is possible to find the "loss resistance" R in the resonant circuit of the earth-fault coils, and it would be interesting to see how R varies with increasing line-to-ground voltage of the faulted phase. One point of the variation curve of R can easily be found from the circle diagram[2] of the neutral-to-ground voltage and some hint can be found from the oscillograms of the decreasing neutral-to-ground voltage, following the quenching of the arcing ground. If we then eliminate too the influence of the Thyrite resistances shunting two of the coils, I would assume that the result will be near the percentage of in-phase component found for other very high-voltage lines. In accordance with previous investigations[3] I think that the value of the residual current itself is not so important as the smooth and slow recovery of the faulty phase due to exact tuning and small losses. As the corona losses decrease very rapidly with the decreasing neutral-to-ground voltage, the velocity of the recovery of the voltage of the earthed phase after a quenched arcing ground is determined mostly by losses which may be represented by a constant loss resistance $R_1 > R$ (shunting the coil in the simplified diagram of a compensated network), where nearly all additional corona losses are excluded from R_1. This consideration will always explain the satisfactory operation of earth fault coils in high-voltage lines when corona losses at normal system conditions are small. A similar investigation, fully confirmed by past experience, shows that the influence of higher harmonics in the residual current is always negligible, as far as the quenching effect is concerned, even if it increases the uncompensated residual earth-fault current to about 25 or 30 per cent of the whole capacitive earth-fault current. Among the more than 1,500 earth-fault coils with which I had experience in Europe, devices for compensating higher harmonics have been found advisable only in one case.

REFERENCES

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2. THE TUNING OF EARTH-FAULT COILS, E. Gross. *Bulletin* Schweiz. Elektr. Ver., volume 28, 1937, page 165.

3. EFFECT OF LOSSES OF THE NETWORK AND OF EARTHING RESISTANCES ON THE AUTOMATIC EXTINCTION OF ARCS TO EARTH, J. Osolsobe and E. Gross. Report 319, 1939, International Conference on Large Electric High-Tension Systems, Paris, 1939.

W. A. Lewis (Cornell University, Ithaca, N. Y.): The statement in the paper that the difference between the calculated currents in the ground-fault neutralizers and the measured currents "is derived from the capacitance in the towers, line insulators, transformers, and other connected electrical apparatus" appears open to question. Although the difference is not great, it is important to properly analyze the contributing factors, so that they may be taken into account in future installations. If the calculations can be made more precise, the engineers responsible for the application of such devices can proceed with greater confidence, and smaller margins are necessary in the design to allow for uncertainties.

In the first place I question whether a correction for corona is justified. The coil current is a residual current, composed of the zero-sequence components of current in the three phases. As such, these components are in phase and reach their maximum values at the same instant. On the other hand the potentials of the two unfaulted conductors are different, and reach their maximum values at different times, so that not more than one of them can be at its maximum value at the instant the coil current is maximum. Actually the coil current will lag the voltage from one unfaulted conductor to ground by approximately 60 degrees and the voltage of the other by approximately 120 degrees. Since corona is present only during parts of each cycle, near the maxima of the voltage wave, it does not appear that intense corona will be present on either unfaulted conductor at the maximum of the coil current. The stress at the surface of the faulted conductor will, if anything, be less than during normal operation, so that that conductor will have no corona unless corona is present during normal operation. Hence, it does not seem probable that corona will increase the effective diameters of the conductors near the maximum of the coil current wave, and therefore will not add appreciably to the fundamental of the coil current.

Although the space charges surrounding the two unfaulted conductors, remaining from the previous cycle, will affect the results to some degree, it appears that one will be positive and the other negative when the residual charge is a maximum, and therefore the effect on the residual charge, and hence on the residual current, will be compensating, leaving the fundamental of the residual coil current essentially unchanged.

Approximate figures are available for the capacitance of suspension insulators, from which the capacitance of a string of several insulator units in series may be estimated. Since the resultant capacitance of several capacitances in series is much smaller than one of them individually, the capacitance of a string of insulators will be very small. Such calculations carried out for lines similar to the one described here indicate that the capacitance of the insulators affects the residual capacitance by much less than one per cent. With approximately four towers per mile, it is difficult to see how the towers can have an effect which is appreciable or even comparable to that of the insulators, and the towers may therefore be neglected.

Capacitance of the transformers and other terminal apparatus are more difficult to evaluate, but here again the information available indicates that the effect is negligibly small.

From the above discussion it appears that the effects cited are, in this case, unimportant factors in the difference between calculated and measured current values. The important factor which has not been mentioned is the effect of distributed constants. In the calculation of transmission lines longer than approximately 100 miles, it is a well-known fact that the distributed character of the capacitance and inductance of the line must be taken into account, if precise results are to be obtained. A similar statement also applies to the residual or zero-sequence circuit used in the application of ground-fault neutralizers. To check the effect of this factor, approximate calculations were made for the line in question. From the data given in this paper and also the paper by Mr. Michener (reference 3 of the paper), the coil current was calculated, without correcting for corona or for distributed constants. For 234 miles the value obtained was 294 amperes. The authors of the paper give 301 amperes including 3.5 per cent for corona correction, which corresponds to 290 amperes without the corona correction, or a difference of approximately 1 per cent. The effect of distributed capacitance was then calculated, for different arrangements of ground-fault neutralizers. With a single coil, of full capacity, at one end only, the correction is approximately 14.5 per cent. For duplicate coils, at both ends, the correction is 3.1 per cent. In this case two-thirds of the coil capacity is at one end and one-third at the other, for which the calculated correction is 6.3 per cent, giving a corrected coil current in the two coils of 312 amperes, or within 1 per cent of the measured current of 314 amperes. Thus, in this case, the correction is just sufficient to account for the necessary difference between calculated and test values.

To obtain further verification of this agreement, similar calculations were made for another line which has been described in the literature. Messrs. W. D. Hardaway and W. W. Lewis have described the installation on the line of the Public Service Company of Colorado. ["Test and Operation of a Petersen Coil on the 100-Kv System of the Public Service Company of Colorado," AIEE TRANSACTIONS, volume 57, 1938 (June section) page 295.] Although tests made before the installation of the coil gave the residual charging current as from 123 to 138 amperes, when the neutral was ungrounded, later tests with the coil installed indicated that the coil current, when the system was in tune, was only 98 amperes. Sufficient data are given to allow approximate line calculations, and the coil current, without correction for distributed constants was calculated to be 92.7 amperes. In this case there is only one coil installed, and the correction for distributed constants was found to be 6.8 per cent, giving a calculated current of 99 amperes. The difference from the measured value of 98 amperes is approximately 1 per cent, indicating a very close agreement in this case also.

Although the accuracy obtained in these two cases would not always be expected, the results indicate that the correction for distributed constants is an important factor in calculating ground-fault-neutralizer applications to long lines, and should be taken into account if accurate results are desired.

It should not be inferred that the capacitance of insulators is always negligible. For lower-voltage lines, using pin-type insulators, short spans, and grounded metal crossarms, similar calculations indicate that the insulator capacitance may increase the coil current five to seven per cent, or in extreme cases, perhaps as much as ten per cent. Loads tapped at frequent intervals, either directly or through transformers, will add apparatus capacitance, which, in the aggregate may be appreciable. Hence some judgment must be exercised as to what factors have a significant effect in each case.

Alex A. Kroneberg and **E. M. Hunter:** The application of ground-fault neutralizers to the 230-kv Boulder line of the Southern California Edison Company is the first 230-kv installation in the United States and when it was first considered, reference was made to the technical press for operating experiences on 230-kv installations made in other countries. Very little information was available on this subject and we wish to thank Mr. Gross for his report on the 220-kv installation of neutralizers in Austria.

Mr. Gross has asked how the in-phase component of current in a line-to-ground fault on the system was measured. The figure of 80 amperes given in the paper is an rms value obtained from an indicating ammeter connected to a current transformer located on the path of the ground-fault current. This, of course, gives a value of current slightly higher than the 50-cycle rms value, the error being due to harmonics in the current.

W. A. Lewis questions the justification of a correction for corona in the calculations of the zero-sequence currents on this system. In this connection, we believe that the AIEE TRANSACTIONS records instances when tests on transmission systems show increases of charging current as the system voltage is raised beyond the corona limit. For example, in the Consumers Power Company Petersen-coil installation tests ("Petersen-Coil Tests on a 140-kv System," J. R. North and J. R. Eaton, AIEE TRANSACTIONS, volume 53, 1934) it was reported that about ten per cent less reactance was required in the coils for tuning at 140 kv than at 70 kv. It has been generally accepted that this apparent increase in the zero-sequence capacitance was due to corona.

On the Boulder line, corona was plainly visible at night when one conductor was grounded. Calculation methods which checked the Consumers Power Company tests results were used on the Edison company's application of neutralizers and indicated the 3.5 per cent correction credited to corona.

We are in agreement with Mr. Lewis that the distribution characteristics of the inductance and capacitance of the line should be taken into account in calculating the amount of current to be compensated by the neutralizers. However, some comments are in order on what is to be considered preciseness between test and calculating current requirements for neutralizers. The test values of current given in the paper probably have an error of at least five per cent. Experience with a large number of neutralizer applications in the United States leads to the conclusion that with existing methods of calculations, a correlation between test and calculations results closer than ten per cent cannot be depended upon. Almost invariably the measured currents exceed the calculated currents, the additional current undoubtedly being supplied by those structures on the system, the capacitance of which are not readily calculable.

What Wood May Add to Primary Insulation for Withstanding Lightning

Discussion and authors' closure of paper 40-25 by J. T. Lusignan, Jr., and C. J. Miller, Jr., presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (September section) pages 534–40.

R. M. Schahfer (nonmember; Northern Indiana Public Service Co., Hammond, Ind.): The authors' method of presenting data on the impulse value of wood alone and in combination with other types of insulators commonly found in transmission lines is new. The graphs accompanying the paper are excellent and are readily applicable for analyzing various structure problems.

In commenting on the paper, I have applied the figures to one of our problems of design. In so doing, certain questions were raised. These questions are those of a field engineer applying the data, rather than those of a laboratory man questioning the values and laboratory methods. The questions raised are summarized in the following comments.

1. The time to flash is largely controlled by span length. For very short spans 2 microseconds is about right. For spans in the neighborhood of 300 feet this value is about 4 microseconds. For longer spans, 800 feet and up, this value is from 8 to 12 microseconds; the authors' critical value, I believe. A curve for 4 microseconds, added to the graphs would increase their usefulness.

2. In figure 1, values for insulators nominally rated at 35 kv and 45 kv would be useful.

3. In figures 2 and 3, the two-microsecond curve seems to be based on values somewhat higher than values previously published.

4. In figure 6, it would be advantageous to extend values to six feet of wood.

5. In figure 8, it would be advantageous to extend values to eight feet of wood.

6. In figure 10, it would be advantageous to extend values to six feet of wood.

7. A graph similar to figure 10, but covering suspension insulators and crossarms would be useful.

8. Two graphs similar to figure 10 but covering porcelain insulation, wood and air in series; one graph for pin-type and one for suspension-type insulators are needed where ground lead wire is set away from the pole.

9. Wherever graphs involving porcelain insulation is involved, it would be helpful to have sufficient values to cover the amount of insulation commonly used. That is for pin type there should be insulators nominally rated at 35 kv, 45 kv, and 69 kv. For suspension insulators there should be values for one to ten units.

10. Apparently there is an error in Example 1. The first value for insulation flashover is given as 260 kv. The value in figure 1 is about 280 kv. Then the first ratio would be 100 per cent, and the second $99\frac{1}{2}$ per cent.

11. In the case of porcelain, wood, and air insulation in series, there is a question as to whether or not the reduction factor of 65 per cent for rain applies to porcelain and wood only, or to the porcelain, wood, and air total.

12. In the third paragraph under "Rain", the authors state that the reduction factor for critical flashover is 65 to 70 per cent, and for two-microsecond flashover 75 to 80 per cent. Would it then be conservative to use the following reduction factors:

Critical	65 per cent
Four microsecond	70 per cent
Two microsecond	75 per cent

And for creosoted pine which would take up more moisture than lacquered wood and less than untreated wood:

Critical	70 per cent
Four microsecond	75 per cent
Two microsecond	80 per cent

As stated previously we are studying and have constructed a short section of a new-type 33-kv wood-pole structure which we designate as type *F*. This structure is rigid and well balanced mechanically. It is less expensive than conventional designs due to absence of braces. Our problem now is to determine whether or not modifications should be made to balance the various lightning flashover paths.

A number of points previously mentioned appear in this problem and are not covered in the paper. This is the reason for our questions. The authors' comments on the points I have raised and on the different basis of treating our practical problem will be greatly appreciated.

In conclusion I believe the number and length of my comments is a testimonial of the value of the authors' work done and of the practical results which will ensue. I believe it only needs to be expanded to cover a little more of the field, and it will become the handiest "tool" available to solve this type of transmission design problem.

W. C. Schmidt and **C. E. Waits** (The Commonwealth & Southern Corporation, Jackson, Mich.): This paper presents the findings of a very comprehensive study of the value of wood as secondary insulation. The curves and other data presented are readily applicable to actual problems of line design and we are reviewing our pole line standards in the light of this information with a view toward insuring well-balanced designs. These data should prove invaluable to designers of transmission and distribution lines.

Figure 7 gives wood-insulation values for the combination of suspension insulators and wood crossarms in series. It would be helpful to have similar values applying for the combination of air and wood in series.

The small reduction in insulation strength of lacquered crossarms under rain conditions suggests the possibility of waterproofing crossarms to recover the 15 to 25 per cent loss under wet conditions.

The authors present a good case for the use of arching horns in parallel with crossarms to avoid shattering. To be effective, however, the horn gaps must protect for the wet condition, thereby decreasing the insulation strength approximately 25 to 35 per cent. Then there is a question whether the protection afforded by the upper 25 to 35 per cent band under conditions that do not approach the severe AIEE rain test is not of greater value than the prevention of the fewer flashovers under the more severe conditions. Also a crossarm flashover is not always accompanied by dangerous shattering.

In closing we wish to again compliment the authors on making these data available in such a readily usable form.

Edgar P. Brantley (Georgia Power Company, Atlanta, Ga.): First I would like to compliment the authors on their paper which provides much needed data regarding the effect of moisture on the impulse flashover of wood as used in transmission-line construction, also, on their rather novel method of estimating impulse flashovers for combinations of porcelain and wood. This method due to its simplicity will make the data presented most useful.

That moisture decidedly reduces the impulse flashover of wood crossarms as evaluated in this paper was first established for us by a series of impulse tests on newly steam seasoned and, later, on kiln-dried creosoted pine crossarms conducted by the Ohio Brass Company in their high-voltage laboratory at Barberton, Ohio, during 1937 and early 1938 for the Georgia Power Company.

Prior to the running of these tests general observations and records extending over a number of years indicated that in general, new lines constructed of freshly steam-seasoned and creosoted southern-pine poles and crossarms suffered more lightning damage than older lines of similar construction. Lightning attacks to the new lines appeared more explosive in nature, peeled deeper into the wood of poles and crossarms than did lightning attacks to the older lines.

Many transmission line operating records also indicate a decrease in lightning damage after the first one or two lightning seasons, undoubtedly due to the gradual air drying of the crossarms while in service. This is illustrated by the record of a 61-mile 110-kv line constructed in south Georgia during 1936, on which overhead ground wires were omitted for economic reasons and insulation consisted of seven $5^3/_4$-inch by 10-inch disk insulators in series with 6.5 feet of clear wood in the double-plank crossarm. The crossarms were steam seasoned and creosoted from green stock. When installed their moisture content was approximately 40 per cent to 50 per cent based on the weight of oven-dry wood. The following is the operating record of this line:

Year	Number Months	Lightning Trip Outs	Single Planks Changed
1936	5.5	7	19
1937	12	19	17
1938	12	4	5
1939	12	16	5

Lightning storm days for 1938 were approximately 62 per cent of those recorded for the other years shown.

After considering the above tests, observations, and records, we began experimenting with kiln-dried crossarms in all transmission line sizes. The arms being dried to a maximum moisture content of 20 per cent, based on their oven dry weight, 16 per cent to 20 per cent being the average equilibrium moisture content which can be obtained by air drying for three to five years in the open in our territory.

By predrying our transmission-line crossarms, we expect to see the increased flashover reflected in the number of lightning interruptions and also to avoid much of the early damage and so justify the 10 per cent to 12 per cent increase cost due to kiln-drying. To date, unfortunately, we have no line comparable to our Americus-Tifton 110-kv line, noted above, where kiln-dried arms were used with sufficient record to indicate that this is accomplished. However, on a 44-kv pin-type triangular-spaced pole-cap line 41.72 miles long completed in south Georgia the latter part of 1938, five inches by five inches by eight feet creosoted-pine kiln-dried crossarms, having three feet clear wood paths in series with the 50-kv pin-type insulators and wood poles were used and to January 1, 1940, no arms have been replaced and only one damaged to any extent.

We believe these records to be in agreement with the results noted in Mr. Lusignan's and Mr. Miller's paper and to be the result as pointed out by them of what is to be gained by reducing the moisture content of wood transmission-line crossarms.

L. G. Smith (Consolidated Gas Electric Light and Power Company of Baltimore, Baltimore, Md.): The data included in the paper should be of considerable value to engineers designing wood-pole lines. A knowledge of the effect of wood upon impulse levels is necessary, otherwise a balanced design cannot be obtained. Several years ago the company with which I am associated had some tests made on the types of construction used on its system. However, the authors, by assembling their data in general form, have made it possible to estimate the impulse level of any type of construction. If the data are applicable for all types of construction, it will not be necessary to make laboratory tests on every design of wood-pole structure considered. It is of interest to note that the effect of such factors as creosote treatment, rain, moisture content, and cross section of wood have been studied and evaluated.

In the tests to determine the effect of creosote treatment, it is questioned whether or not the reduction of impulse strength of pine crossarms might be due to their moisture content. Frequently pine is treated green or partially seasoned. Moreover the type of treatment does not remove moisture but may actually increase the moisture content. This point is substantiated by the statement that creosote treatment has no appreciable effect upon the flashover voltages of Douglas fir. There is no apparent reason why the presence of creosote should have any different effect on the impulse flashover of pine than on fir.

The results of the impulse-strength tests made on several types of structures for the Baltimore company were checked with calculated flashovers based upon the method and the data in the paper. The tests on structures were made using a $1^1/_2$x40-microsecond wave. The values of impulse strength were the minimum crest value that would cause flashover on a 50-50 basis. It is believed that these values correspond closely to the "critical" values obtained by the authors. In comparing the results, it is interesting to note that where the feet of wood involved was from one to four feet, except in one case, the calculated values were within 20 per cent of the values obtained in the Baltimore tests. For less than one or more than four feet of wood, the differences were as much as 47 per cent. These differences may be the result of steel crossarm braces and other metal parts. As the test results were usually below the calculated values, where three or more feet of wood are involved it might be well to include an estimated correction in order to be conservative.

In the tests demonstrating the effect of moisture content of the wood, it would be of interest to know if the moisture-content determinations represented the average moisture in the entire cross section of the wood or only the moisture content of the wood near the surface. Since the impulse flashover is a surface phenomenon, might not the surface condition be the significant factor?

I. W. Gross (American Gas and Electric Service Corporation, New York, N. Y.): The authors are to be complimented on reducing to usable, workable form a mass of data obtained from extensive tests on wood insulation, wood crossarms along, and in combination with porcelain. The paper by Messrs. Lusignan and Sporn presented two years ago presented a great deal of new information on the impulse characteristics of wood, and the authors of the present paper, have carried this investigation further, particularly to more thoroughly investigate the effect of cross section of the wood, kind of wood, wood treatment, and the effect of moisture content.

The conclusion that they have reached as a result of their tests, that the impulse strength of wood under wet conditions may drop as low as 65 per cent of the dry flashover, should be of considerable interest and value to the transmission line designer.

It is interesting to note that while these tests were made on wood alone, or in combination with porcelain, somewhat similar results showing a considerable reduction in the impulse flashover of bushings and insulators alone, under wet conditions, have been reported abroad by Doctor Wanger, his figures showing a reduction in the order of 15 to 30 per cent. I would like to ask if the authors have obtained any data during their tests which seem to indicate the insulators themselves have an appreciable reduction in impulse flashover under wet conditions.

The practical application of the data is shown in typical examples in the paper. In example I, for instance, it is shown that by adding the separate impulse flashover of a 69-kv pin insulator and four feet of wood, and applying a reduction factor, a net flashover is obtained which checks closely the actually measured value of the combination. However, if only one foot of wood is used the combined insulator and this length of wood, still using the same reduction factor of 0.65—gives a combined calculated flashover of 225 kv against a flashover of the insulator alone of 260 kv.

The question therefore arises, should not this practical reduction factor be different when the flashover of the wood alone is small in comparison with the insulator alone?

Perhaps the authors can throw some light on this question.

It is particularly gratifying to find that the flashover data presented is not only in *usable* form, but also that the limits of tolerance are given, the figures being ±20 per cent as a maximum. Too often data of this type are given as *average* values *only* which introduces an element of uncertainty in its practical use.

J. T. Lusignan, Jr., and C. J. Miller, Jr.: In the preparation of this paper, the authors were constantly aware of the inherent variability of the impulse flashover of wood, both alone and in combination with insulators. No two pieces of wood behaved exactly alike and the best that could be obtained was the band in which the various flashover voltages would most probably be.

The amount of impulse testing necessary to produce even a single curve is enormous and requires considerable time. In order to cover the many different phases in the use of wood as auxiliary insulation against lightning, it became necessary to limit the amount of testing on each particular item. Accordingly, many of the curves stop short of the maximum lengths of wood which might be used in connection with insulators.

Mr. Schahfer has offered some excellent suggestions as to extension of the various curves to longer sections of wood. The authors feel that figures 6, 8, and 10 could be extrapolated conservatively to give the desired impulse information on longer sections of wood. Only the two-microsecond and critical flashover voltages were given to avoid confusion which might result from too many lines on a given curve. It is felt that if four-microsecond values are desired, an interpolation midway between the two-microsecond and critical curves would yield satisfactory values. The flashover values for the 69-kv pin-type insulator, given in figure 1, were included only for the purpose of illustration in the examples. The flashover voltage of any size pin-type insulator may be readily obtained from various sources. While figure 10 was drawn for pin-type insulators, it may be used satisfactorily with suspension insulators. In this case however, the horizontal flashover from the line conductor directly to the down-lead on the pole must be considered. This flashover voltage will be given approximately by the rod gap curves of figure 3.

The need for a curve showing the amount of impulse insulation added by air gaps to combinations of insulators and crossarms became apparent by the number of requests for such information from others besides Messrs. Schahfer, Schmidt, and Waite. Accordingly, data have been obtained from tests and the results are presented in figure 1 of this discussion. Since rain does not appreciably alter the impulse flashover voltage of an air gap, the 65 per cent reduction factor due to rain should be applied to the crossarm plus insulators before adding the air gap insulation value from figure 1 of this discussion. While this figure was drawn primarily for pin-type insulators, it may be used satisfactorily with suspension insulators. In this case it is important to consider the horizontal flashover voltage from the line conductor to the down-lead.

It would seem quite satisfactory to use the reduction factors suggested by Mr. Schahfer in comment 12 of his discussion. He is also correct in pointing out in his comment 10 of the error in example 1 due to improper reading of figure 1.

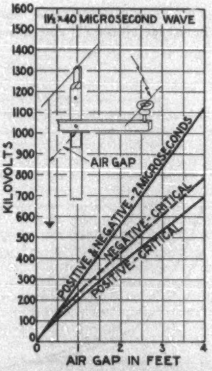

Figure 1. Impulse insulation added by air gaps to combinations of pin-type insulators and crossarms

Messrs. Schmidt and Waite bring up the question of the spacing of arcing horns in parallel with wood crossarms to prevent shattering. The usual procedure in the field has been to set the arms so that they protect the wood under average severe conditions. This practice has been quite effective in protecting crossarms and more or less indicates that the 65 per cent reduction factor due to rain gives a pessimistic flashover value. A combination of events necessary for a given crossarm to be subjected to the severe AIEE rain and a lightning voltage at the same time would seldom occur in practice. Accordingly, a factor of 65 per cent and possibly somewhat upward might apply for any given set of conditions.

Mr. Brantley's description of his company's experience with kiln-dried crossarms is most welcome. It illustrates an active design effort to gain maximum transmission line insulation strength through careful control of the moisture in the wood products purchased.

Mr. Smith's comments are very interesting, especially those comparing estimated values from the paper with his experimental values. Deviations of 47 per cent from the estimated values bear out the authors' findings of considerable inherent variations in the impulse insulation of wood. While none of the authors' tests were as much as 47 per cent away from the average, it seems quite probable that such a result might be obtained on an occasional piece of wood, especially too if metal braces and other parts are present to complicate the electrostatic fields as suggested by Mr. Smith.

In the authors' work, the moisture content determinations were made from cross-sectional blocks removed from the crossarms immediately after test. Accordingly, the values of moisture content given are an average value over the entire cross section. While flashover is a surface phenomena the voltage gradient governing it along the wood is believed to be affected greatly by the moisture within the wood, as this moisture probably has appreciable to do with the internal series impedance of the wood and its electrostatic field. The tests illustrated in figure 14 would seem to bear this out, as the lacquered specimens where only surface moisture could exist showed little change of impulse flashover under wet conditions.

In reply to Mr. Gross' question regarding the impulse flashover voltage of insulators under wet conditions, the authors have found that no appreciable change in flashover value has been observed. The application of the 65 per cent reduction factor to short sections of wood in series with insulators sometimes gives a net flashover value less than that of the insulators by itself. Obviously, such is not the case, as under no conditions would the flashover voltage be less than that of the insulator by itself. The 65 per cent rain reduction factor was deduced from tests made on a four-unit string of suspension insulators in series with a four-foot section of crossarm. Obviously, this factor will have to be increased to unity as the length of the crossarm decreases to zero. The designer can readily modify this factor to fit his conditions, being guided by the fact that the flashover voltage of the insulator itself is the limiting condition.

The flashover voltages determined with the aid of the various curves presented in this paper should be considered as indicating the most probable flashover voltage of a given structure with the actual flashover voltage of each structure of a given line falling within a plus or minus 20 or 25 per cent band.

Surge Characteristics of a Buried Bare Wire

Discussion and author's closure of paper 40-9 by E. D. Sunde, presented at the AIEE winter convention, New York, N. Y., January, 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 987–91.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The author is to be congratulated on his mathematical analysis of the impedance characteristics of counterpoises for lightning currents. The author by using the fundamental constants of resistivity, inductivity, and resistivity of the earth arrives at values of surge impedance which begin at zero at the moment the current wave arrives, reach a maximum value, and decrease to a value equal to the ground resistance of the counterpoise.

This same result had been obtained previously by experiment and by way of partly empirical calculations.[1] At that time it was believed by some that the surge impedance of a counterpoise could not be zero at the beginning of a surge but would have to be equal to that of a line in air because the effect of leakage resistance had not started. It is gratifying to see that the author by entirely independent and mathematically more correct methods arrives at values of surge impedance throughout the range of the calculations at values which are in very close agreement with the test results, even numerically.

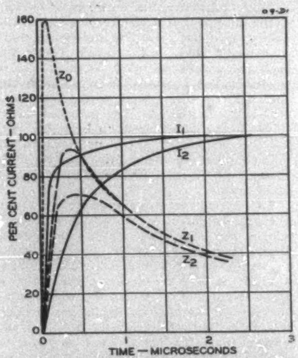

Figure 1. Counterpoise impedance as affected by front of current wave

Z_0—Calculated according to procedure outlined in previous discussion[1]

Z_1—Surge impedance when current I_1 is applied

Z_2—Surge impedance when current I_2 is applied

The author shows very little difference in maximum values of surge impedance (figure 6) between unit step current and a front of 2.5 microseconds. This is in disagreement with the test results.[1] The time functions of the current waves applied to the counterpoise in figure 2[1] are shown in figure 1 of this discussion. The first current wave has a distinctly greater rate of rise than the second one. A pronounced difference in maximum surge impedance values for the two wave fronts is evident. It should be expected, therefore, that the maximum impedance for rectangular front should be greater than is indicated by figure 6 of the paper. Perhaps the assumption made that the surge impedance of a wire buried in soil a distance d equals one-half the sum $S(a,t)$ and $S(b,t)$ is not correct. A comparison of figure 4 of the paper for $S(a,t)d=0$ and the dotted curve $R_3\times Z_3/R_3+Z_3$ on figure 4 of the previous discussion[1] show very good agreement as to the crest value, see also curve Z_0, figure 1 of this discussion. It seems reasonable to assume that considerable differences in crest values of an impedance, which is a function of time, should occur when subjected to currents of such entirely differing fronts as 0 and 2.5 microseconds.

While there still is some doubt about some of the factors, two principal points stand out.

1. The surge impedance of a buried wire rises rapidly from zero to a maximum value and decreases slowly to a value equal to the d-c resistance of the wire to the ground plane.
2. The maximum value of the surge impedance is a function of the front of the applied current wave.

The results of previous investigations[2,3] can now be used with considerably more confidence.

References

1. Discussion of paper THEORY AND TESTS OF THE COUNTERPOISE, J. H. Hagenguth. ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), volume 53, August 1934, pages 1163–72.

2. Paper cited in reference 1.

3. FIXING COUNTERPOISE LENGTH, L. V. Bewley and J. H. Hagenguth. *Electrical World*, March 2, 1935.

L. K. Swart (Bell Telephone Laboratories, New York, N. Y.): An interesting point in connection with the surge characteristic of a buried wire is that after one or two microseconds, the impulse impedance is somewhat smaller than the leakage resistance as measured with direct or low-frequency current. Other investigators through measurements with high-voltage surges have observed similar characteristics. These have usually been attributed to corona effects or breaking down of the resistance between soil particles at high voltage and at high current. Measurements recently were made on buried wires in New England using relatively low-voltage transient impulses as well as steady-state high-frequency current, also of low magnitude. The results of these measurements confirm Mr. Sunde's suggestion that it is due to contact resistance and capacitance between wire and ground.

This parallel combination of capacitance and contact resistance is considered in series with the leakage resistance in the earth. When d-c measurements are made of the leakage resistance, the latter includes the contact resistance, together with the resistance of the earth. However, under transient conditions the capacitance to ground of the conductor shunts out the contact resistance and the impedance measured, therefore, is the leakage resistance of a wire in perfect contact with the earth.

The test arrangement and the results of one of the tests mentioned previously are shown on figure 2 of this discussion. A buried wire which had a length of about 400 feet was cut in the middle and a d-c measurement was made of the leakage resistance of the two wires in series. This resistance measured 133 ohms. One terminal of a surge generator was connected to one of the buried wires and the other terminal to the other buried wire through a resistor of 133 ohms. The relation of the impulse impedance to d-c leakage resistance can be found by comparing the voltage of V_C across the

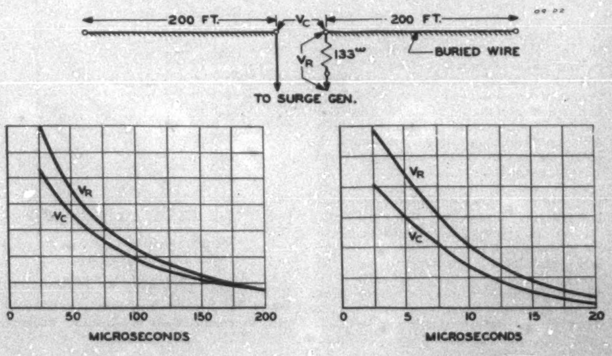

Figure 2. Comparison of impulse impedance and d-c leakage resistance of a buried bare wire

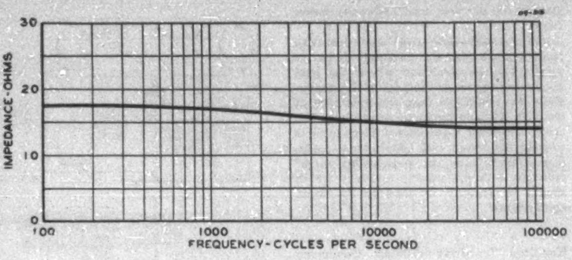

Figure 3. Impedance versus frequency of driven-rod ground system

terminals of the two buried wires with the voltage V_R across the resistor. The surge generator was discharged at about 500 volts.

As the results show, the impulse impedance is somewhat smaller than the d-c leakage resistance. No attempt was made to record the voltages during the initial one or two microseconds.

Tests have also been made on a buried wire using high-frequency steady-state currents of low magnitude and it may be of interest to note the reduction in impedance of the buried wire as the frequency is increased, as illustrated in figure 3 of this discussion. In all of the grounds tested a reduction in impedance with increase in frequency took place. For certain types of grounds, however, the reduction was greater than in others.

E. D. Sunde: In his discussion, Mr. Hagenguth questions the small difference shown in figure 6 between the maximum value of the surge impedance for unit step current and for a current of 2.5-microsecond wave front. The surge impedance for the latter current (figure 6, curve 2) is derived from the surge impedance for unit step current (figure 6, curve 1), using the superposition theorem expressed in equation 17 of the paper. It would thus seem to follow that the two impedances shown in figure 6 are mutually consistent, regardless of the assumptions on which the surge impedance for unit step current is based. The voltage in response to a unit step current is, of course, considerably larger than for a current of 2.5-microsecond wave front.

That the surge impedance for small values of time does not depend to any considerable extent on the wave front may also be concluded by inspection of the surge impedance for unit step current, as shown in figure 2 of the paper. For a wire at a depth of 0.3 meter, there is a rather small variation in the surge impedance in the interval between 0.01 and 0.1 microsecond, so that it may without serious error be regarded as constant, and somewhat larger than 50 ohms, for times less than 0.1 microsecond. On the latter assumption the surge impedance for times less than 0.1 microsecond would be the same regardless of the wave shape of the current, and somewhat larger than 50 ohms. Since the maximum surge impedance is obtained for small values of time, it would appear that the latter does not depend to any considerable extent on the wave front, when the latter is less than 2.5 microseconds, as implied by Mr. Hagenguth.

The Accuracy of Watt-Hour Meters on Intermittent Loads

Discussion and authors' closure of paper 40-40 by M. A. Faucett, C. A. Keener, and M. S. Helm, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 433–41.

Wm. M. Young (Ohio University, Athens): This paper indicates very interesting results on a very major problem, a problem which vitally affects the revenue received by utilities and the charges made to consumers that have loads which are not steady in character. The problem is extremely important and naturally creates wide interest.

Because of the large number of meters studied, general statements as to behavior can hardly be made, however, particularly with reference to the more modern meters, might I inquire if the authors have studied effects of change in ambient temperature on an instrument under intermittent load? Certainly it would seem that the heating effects caused by currents of short duration which produce a 200 or 300 per cent overload if checked by an indicating wattmeter might cause local heating which would aversely affect the results.

A major problem still exists in determining the effect of a complex wave of either current or voltage or both simultaneously. This problem has been partially attacked by another paper presented at this same time ["Watt-Hour Meter Performance With Power Rectifiers," C. T. Weller, H. E. Trekell, and F. O. Stebbins, AIEE TRANSACTIONS, volume 59, 1940 (August section) pages 749–57]. It would seem, however, that it is extremely important to have definite data on the behaviorism of modern meters under these highly complex conditions. Have any results been obtained for a period less than one-half cycle or for periods less than the deceleration time of the instrument?

With present meters if the load cycle changes, figure 5 is apparently of considerable importance. It indicates that each type of intermittent load of short duration should be separately studied by the utility and the meter very carefully adjusted if the intermittent load constitutes a major portion of the customer's load. In order that charges be fair and revenue just, the compensator apparently must be adjusted to the center point of its range between forward creep and reverse creep.

Has the effect of the back lash in gear trains been studied? Certainly we are dealing with a type of gear which must be produced very cheaply and one in which the torque for all angles of mesh of the gears is not constant. Other companies in other fields than electrical engineering have had considerable difficulty with variation in torque because of variation in the contact angle between gear teeth as rotation proceeded. This variation in torque may be of considerable importance, particularly for cycles of rather short duration because of the static friction effect.

It is to be assumed that the time of circuit closure was governed and controlled although this is not definitely stated as far as I could discover. There is much more interesting work to be done on this problem and I certainly hope that we will have more good subsequent reports of progress in the future.

H. E. Trekell (General Electric Company West Lynn, Mass.): This paper by Messrs. Faucett, Keener, and Helm has proved very interesting, first, because of the novel approach to the problem of determining the response of watt-hour meters to loads of short duration, and second, because of the magnitude of the errors found. In addition to finding the magnitude of errors, the method of analysis should also serve to point the way to improvement in case it is warranted.

It has not been generally believed that errors as large as those found were present in watt-hour meters of recent or modern construction. For this reason tests have just been completed on the type *I*-30 single-phase meter now being manufactured by the General Electric Company. In the first of these tests, the aid of Professor H. E. Edgerton of the Massachusetts Institute of Technology was enlisted. By the use of the high-speed stroboscopic-light camera, pictures were taken of the meter disk during acceleration and deceleration periods. Figure 1 of this discussion shows the response found at 100 per cent load and corresponds to figure 1 by the authors.

During acceleration the meter reached

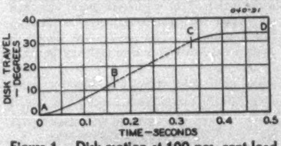

Figure 1. Disk motion at 100 per cent load unity power factor

A–B—Acceleration period—to within one per cent of steady-state speed

B–C—Steady-state period

C–D—Deceleration period—to within one per cent of steady-state speed within one per cent of the ultimate speed in about ten cycles or 0.17 seconds. Also, the time required for deceleration to within one per cent of the speed at the start of the deceleration period was found to be very close to ten cycles. The loss in displacement during acceleration and the gain during deceleration were found to be nearly equal to the steady-state displacement during two cycles. The difference between the loss in displacement during acceleration and the gain during deceleration was so small that greater accuracy in reading the displacement was needed to assign finite values to the error found. The results did show, however, that for the conditions of figure 2 of the authors' paper, the error was less than one-fifth of that shown. The displacement was read to 0.1 degree by using the teeth on the edge of the disk. In the tests the current was started and stopped at the zero of the steady-state wave and a load of constant resistance was used.

It was found that the damping and inertia constants of the meter disk were the predominant factors in determining the response as the speed-time curve was nearly a pure exponential function with an exponent of $-(D/J)t$ where D is the damping torque per unit speed and J is the polar moment of inertia. If T be the steady-state torque applied or released, it may be shown that the loss in displacement during acceleration is equal to TJ/D_1^2 for a true exponential function and that the gain during deceleration is TJ/D_2^2 where D_1 and D_2 are the respective damping constants. The value for D_2 should be less than that for D_1 and by the amount of the current-flux damping torque which is small in modern meters. This undoubtedly explains why the error is positive and small and also why the error is larger in older meters.

For loads of very short duration such as are shown by the authors in figure 3, it is thought that the above concept must be modified to include the reactance of the meter disk. It is thought that the disk reactance may keep the steady-state driving torque from being established instantaneously causing a delay of a small fraction of a cycle which effect would be difficult to evaluate or detect by means of the stroboscopic-light pictures. A second test which may be considered more straightforward was employed in finding the response for these loads of very short duration. This consisted of measuring the intermittent load by a reflecting dynamometer wattmeter which was sufficiently ballistic that the light beam did not oscillate more than one per cent of the scale reading. The registration of the watt-hour meter was then compared with the wattmeter in conventional manner. The results are shown in table I for loads of one to eight-cycle duration at 100 per cent load and unity power factor.

Table I

Time Load on (Cycles)	Time Load off (Cycles)	Error in Watt-Hour Meter Registration: Current Starting Near Zero of Steady-State Wave (Per Cent)	Error in Watt-Hour Meter Registration: Current Starting Near Peak of Steady-State Wave (Per Cent)
8	22	+0.6	−0.1
4	26	+0.7	−0.7
1	29	+0.5	−1.9

These results show errors much less than those reported by the authors. Negative results for the condition where the load begins and ends near the peak of the steady-state wave indicate that the reactance of the disk is also a factor in determining the error. However, regardless of the conditions causing the errors with intermittent loads, it is evident that the present-day meter performs with remarkable accuracy which should be entirely satisfactory for most applications.

Hamilton Treadway (Illinois Commerce Commission, Springfield): The subject of watt-hour meter accuracy is one of considerable interest and importance to engineers engaged in the regulatory field. In the past the standards of meter accuracy formulated by the state public-utility commissions have been predicated on the basis of constant load. Most of the rules were adopted when the principal use of electricity by the residential and commercial customers was for lighting and small motors. With the increase in the use of electric-welding equipment, flashing electric-light signs, X-ray machines, and other rapidly fluctuating loads the problems raised by the operation of intermittent loads on the distribution systems of the electric utilities have become of increasing importance. The authors' paper in dealing with one of the problems raised by these changes in the character of the use of electricity is particularly timely.

Many of the existing rules of the state commissions permit errors in meter registration up to 4 per cent. The authors' conclusion that the maximum error on very rapidly fluctuating loads may reach 20 per cent would therefore prove of considerable importance in the case of a customer whose load is continually transitory in nature and is metered separately. A meter serving such a load separately, though meeting the present standards of accuracy under existing methods of testing in the field, may actually be considerably in error. In such cases in the future it may be necessary to give some thought to the character of the load served by the meter when tests are conducted.

The authors do not indicate whether the tests were made under conditions of constant voltage. In the field we know that fluctuating loads and voltages go hand in hand. Since the potential flux of the meter will vary directly with the voltage the effect of the fluctuating voltage would seem to be of some importance. Except where the distribution system has been specially designed for a heavy fluctuating load the instantaneous drop in voltage when such loads come on the line is usually quite large. Information on the effect of these fluctuations in voltage on the accuracy of meters serving intermittent loads should prove of value.

A. B. Craig (Boston Edison Company, Boston, Mass.): During the fall of 1927, an investigation of the performance of watt-hour meters on intermittent loads was made by C. H. Ingalls, then meter engineer for the Boston Edison Company. The results of Mr. Ingalls' investigations have not been published. Our comments are based upon the data obtained in this original investigation.

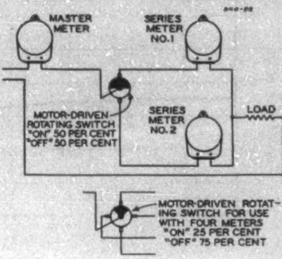
MASTER METER
SERIES METER NO. 1
SERIES METER NO. 2
LOAD
MOTOR-DRIVEN ROTATING SWITCH "ON" 50 PER CENT "OFF" 50 PER CENT
MOTOR-DRIVEN ROTATING SWITCH FOR USE WITH FOUR METERS "ON" 25 PER CENT "OFF" 75 PER CENT

Figure 2

The fundamental approach to this problem by the authors and by Mr. Ingalls are radically different. The authors have analysed individual impulses whereas, in our test methods, the average of a large number of impulses was used. Figure 2 of this discussion shows the test methods employed by Mr. Ingalls.

A constant load registers through the master meter and, by means of a motor-driven rotating switch this load is shifted from one series meter to another. For intermittent tests with loads "on" one-half the time and "off" one-half the time, a one-half segment commutator is used with two meters. For tests with intermittent loads "on" one-quarter of the time and "off" three-quarters of the time, a one-quarter segment commutator is used with four meters. The difference between the registration of the master meter and the sum of the series meters is of course the error in registration due to intermittent loads.

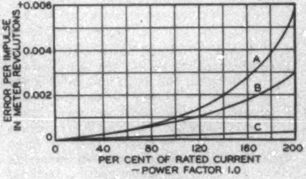

Figure 3

A—Disk constant 0.25
B—Disk constant 0.3
C—Disk constant 0.6

This method of test has the advantage that as many impulses as are necessary or desirable can be obtained. In some of the tests conducted by our company, over 100,000 impulses were used. Special registers were placed on the watt-hour meters so that the actual number of disk revolutions could be determined to one-tenth of a revolution. In all tests, loads were applied for a sufficient time so that no meter had

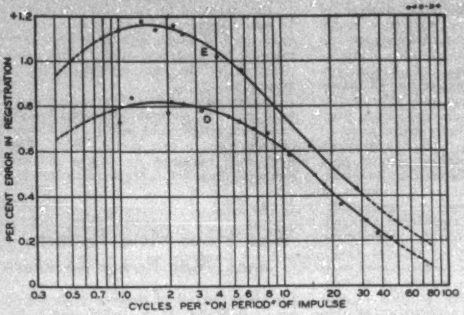

Figure 4

100 per cent load, unity power factor, disk constant 0.25

D—"On" 50 per cent, "off" 50 per cent

E—"On" 25 per cent, "off" 75 per cent

fewer than 100 revolutions of the disk. The maximum error in reading is thus one-tenth of one per cent.

We believe that this method is superior to that described by the authors, in that it is a simple, practical test method and one which can be made more nearly to simulate actual field operating conditions. Although we are in agreement with the authors as to the types of errors involved, we differ widely as to the magnitude of these errors.

From the data presented in figure 3 of this discussion, we draw the following conclusions:

1. That the error in registration is positive and is proportional to the load.
2. That for meters of the same rating the error is inversely proportional to the square of the meter test constant.

The data in figure 4 of this discussion are in close agreement with the conclusion of the authors, that the maximum error occurs when the load duration is from two to four cycles. However, in figure 3 of their paper the authors show errors from four to seven times greater than measured by us. Although they state that there may be errors as great as 20 per cent, our data leads us to the conclusion that, when the meter is subjected to full-load impulses, the maximum error will not exceed $1^1/_2$ per cent, and that for smaller loads and modern meters a much smaller error may be expected.

We are offering no discussion on various other conclusions drawn by the authors inasmuch as they are of a very general nature and our own conclusions can be considered as well within their statements.

L. A. Burckmyer, Jr. (Cornell University, Ithaca, N. Y.): This paper states that, as the duration of the "on" period of a load cycle is diminished, the watt-hour-meter error increases to a maximum and then approaches zero as the load duration approaches zero.

This statement is supported by (*a*) a brief verbal argument, (*b*) curves from direct tests, and (*c*) curves derived from measurement of acceleration and deceleration rates.

The argument reads "The registration error due to intermittent load must approach zero as the acceleration portion of the load cycle approaches zero or 100 per cent of the load cycle. At some other value of the duration of load the error must be a maximum." There is no question but that the error due to intermittent load must approach zero as the acceleration portion of the load cycle approaches zero, that is, as the load approaches a condition of not being intermittent. But the conditions stated for the other extreme are not so acceptable. Incidentally, the other extreme is not even correctly defined in the passage quoted, for it is obviously impossible for the acceleration portion of the load cycle to approach 100 per cent of the load cycle.

The meaning of the curves of figure 3 however, appears to be entirely clear. The registration error is represented as rapidly declining to zero as the duration of load is reduced from about three cycles to zero. This result, contrary to the implications of the passage quoted above, could not well have been anticipated. Let us consider a meter in which the driving torque is directly proportional to load power, the retarding torque due to the permanent magnet is directly proportional to disk speed, and the friction and potential-flux torques are exactly equal to the torque produced by the light-load compensator at all speeds. We will recognize, however, the existence of a further retarding torque proportional to the product of current and speed. The qualitative performance of such a meter agrees closely with all the characteristics observed in practice and with those described in this paper but for one exception: The percentage error on intermittent load loads *reaches a maximum as the duration of load approaches zero.* (See appendix of this discussion.) The value of the percentage error is finite, and determined by the relation between the retarding torque due to load current and that due to the permanent magnet. With torque values that make performance at low power factors and at overload acceptable, this error is small.

While this analysis disposes of the quoted argument presented by the authors, it of course cannot refute their test results. It must be borne in mind, however, that the region under dispute involves load durations of less than one cycle. There is a question of the reliability of the authors' incompletely described method of direct test under such conditions, and undoubtedly the determination and graphical application of speed-time curves for such brief load duration is subject to error. In the curves of figure 1, 0.9 of the steady-state slope is reached in about 0.2 second, so even at the end of what is called the "constant-velocity" period the slope is about one per cent less than that which would occur under continuous load. This makes it difficult to locate accurately the asymptotes to the curves. It seems that a direct test with a ballistic wattmeter would be indicated.

It is admitted that there may be little practical importance in the question raised, and that the pulsating character of most of the torque components may make the duration of load less important than the instantaneous value of voltage at the moment load is applied. Further, the nature of the transient associated with some types of intermittent load might invalidate any laboratory or analytical predictions, even those involving load durations of a number of cycles.

In regard to light-load adjustment, the authors repeatedly stress the need of making the setting with extreme "care". Since it is stated, however, that meters adjusted within the limits of commercial accuracy may show 20 per cent error under intermittent load, and that this error can be reduced by readjustment of the light-load compensator, it appears that the proper setting of this control is not a question of care but of technique. Detailed instructions for securing optimum adjustment would have formed a valuable contribution. In their absence it must be inferred that only by direct experimental study of performance under intermittent load can this be accomplished. Such procedure is, of course, impracticable in most cases.

Appendix

If a constant load is applied continuously, a speed (N) is attained, at which the driving torque (T) corresponding to load power is just equal to the retarding torque (T_m) due to the permanent magnet plus that (T_c) due to current.

During *acceleration*, at any speed n, these retarding torques are reduced to $(T_m+T_c)n/N$, or Tn/N, so the torque available for acceleration is $T-Tn/N$ or $(T/N)(N-n)$. Therefore

$$\frac{dn}{dt}=\frac{T}{4\pi^2IN}(N-n)=C(N-n) \tag{1}$$

where I is the moment of inertia of the disk and C is a constant for a given load condition. The solution of equation 1 for an initial condition of $n=0$ at $t=0$ gives

$$n=N(1-\epsilon^{-Ct}) \tag{2}$$

The number of disk revolutions completed in a time t_1 after power is applied is

$$\int_0^{t_1} n\,dt=Nt_1-\frac{N}{C}(1-\epsilon^{-Ct_1}) \tag{3}$$

During *deceleration* with zero load current, the decelerating torque becomes T_mn/N, so

$$\frac{dn}{dt}=-C'n \tag{4}$$

where

$$C' = C\frac{T_m}{T_m+T_e} \tag{5}$$

The solution of equation 4 for an initial condition of $n=N$ when $t=0$ gives

$$n = N\epsilon^{-C't} \tag{6}$$

and the number of disk revolutions completed between the time t_2 and standstill is

$$\int_{\infty}^{t_2} n\,dt = \frac{N}{C'}\epsilon^{-C't_2} \tag{7}$$

To determine the number of total revolutions, we must find the value of t_2 which corresponds to a disk speed equal to that at the end of the acceleration period. From equation 6 and equation 2,

$$t_2 = -\frac{1}{C'}\log\left(1-\epsilon^{-Ct}\right)$$

and substituting this value into equation 7, the number of revolutions during deceleration, following application of power for a time t_1 is

$$\frac{N}{C'}\left(1-\epsilon^{-Ct_1}\right) \tag{8}$$

so the total number of revolutions, from equations 3 and 8, is

$$Nt_1 - \frac{N}{C}\left(1-\epsilon^{-Ct_1}\right) + \frac{N}{C'}\left(1-\epsilon^{-Ct_1}\right)$$

It should be Nt_1, so the per cent error due to intermittent load is

$$100\left(\frac{1}{C}-\frac{1}{C'}\right)\frac{1-\epsilon^{-Ct_1}}{t_1} \tag{9}$$

As t_1 approaches 0, the quantity in brackets approaches the value C, or the meter over-registers by 100 T_e/T_m per cent.

M. A. Faucett, C. A. Keener, and **M. S. Helm:** We are pleased to note that several of those who discussed this paper have presented experimental data. Their results agree with ours in many particulars and we wish to thank them for their contributions. In the interest of brevity, we necessarily omitted many details which might have been of interest and the discussions have been valuable additions.

Many factors which might be of importance, in particular applications of meters, were omitted in this investigation. To have included such factors as voltage variation, temperature changes, and wave form, would have made the interpretation of results difficult. Such an increase in the number of variables would lead to so complex a situation that the effect of any one would be nearly impossible to evaluate. The influence of such factors as were omitted was minimized by keeping conditions involving them as nearly constant as was feasible. In answer to Mr. Young's question regarding the temperature effects of heavy loads of short duration, it might be pointed out that the thermal rating of meters is for continuous loads of such magnitudes and no errors of importance should be expected from this cause.

It is difficult to reconcile the third paragraph and appendix of Mr. Burckmeyer's discussion. The mathematical analysis of the performance of a watt-hour meter under even the simplest circumstances requires more than the assumption of simple exponential acceleration and deceleration. The component torques are not all simple functions of time or flux and differ greatly in their natures. Conclusions drawn from an analysis based on incomplete premises may be misleading. When equations fail to explain an observed natural phenomena, it is usually Nature that is correct.

In the interpretation of the curves of figure 3, only such points as represent integral cycles should be considered, although a continuous curve was used to indicate the trend of such points. Fractional cycles introduce harmonics or unidirectional components in the current waves. This complication was purposely avoided in this investigation. An investigation, by the authors, of the influence of these factors has been in progress for some time as a part of the study of the magnetic conditions in meters. It is recognized that intermittent loads, in general, need not be of integral cycles duration and that the behavior of meters on the general types of loads would probably deviate somewhat from that indicated for integral cycles.

In a like manner, under practical conditions, no guarantee can be made that the time of circuit closure will repeatedly occur at the same point on the cycle. A notable exception to this is the electronic-controlled welder. The curves of figures 1 and 3 should be considered as the average of a large number of possible cases. The experimental test curves were obtained with circuit conditions approximating the electric welder.

It is difficult to present quantitative results for meter studies without specifying all of the conditions involved, including the make, the type, or the age of the meter. For this reason, it was the intention of the authors that the study be considered more in a qualitative than a quantitative sense. For the presentation of curves, however, quantitative results are necessary. The cases chosen as illustrative are neither the best nor the worst, but are between those extremes. The quantitative results obtained on any meter were dependent upon the adjustments of that meter, especially the friction compensation.

The 20 per cent error mentioned in the paper was the greatest encountered in the investigation. This value should not be considered as the maximum possible error or that which might be expected in any particular meter. In all cases, the meters were adjusted within the limits of commercial accuracy. The meters used in the tests had no gear trains. However, it would seem probable that the gear train would not offer appreciably more opposition to disk rotation with an intermittent load than under normal operation. In a meter in good condition, this component torque is practically negligible.

The magnitude of registration errors reported by Mr. Trekell compares closely with that found by the authors in tests on that type of meter. The conclusions he has drawn check those included in the paper regarding the use of the modern meters on loads of intermittent character.

In the preliminary tests made by the authors, a switching arrangement similar to that reported by Mr. Craig was used. It was, however, put aside in favor of the other method which seemed to offer more material for analytical study. The errors observed by the two methods compared favorably.

The test data submitted in the discussions bear out the authors' observation that maximum errors occur with load durations of from two to four cycles. Since these data were obtained by widely different methods, the check is of particular interest.

Watt-Hour-Meter Performance With Power Rectifiers

Discussion and authors' closure of paper 40-42 by C. T. Weller, H. E. Trekell, and F. O. Stebbins, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 449–57.

Walter C. Wagner (Philadelphia Electric Company, Philadelphia, Pa.): The authors are to be sincerely congratulated upon the effective work which they have done and upon their method of presentation. All research work of a reliable character that can be done on this subject will serve to make more clear the present atmosphere of uncertainty that faces the operating companies when measuring loads of this character.

Some three years ago, when I approached some of the electrical manufacturing companies, plans were outlined for making a joint investigation, combining the work on the test floor with tests in the field in order to include all elements of the problem involved in metering six-phase rectifiers. At that time, in 1937, the General Electric Company representatives stated that, at the first opportunity, when a power rectifier was available on their test floor, they would arrange to have the necessary integrating and indicating meters installed, and tests made following the procedure proposed to their engineers. In 1938, upon their advice and invitation, we, in conference with their representatives, completed the details of the tests and co-operated in making the tests on a six-phase power rectifier on the test floor at Schenectady. During these years and later, tests were made jointly with others, and also independently, on six-phase rectifiers, in line with the original program. The data obtained from these other sources are in the course of examination, but the information so far is not uniformly in a satisfactorily close agreement with the data presented in this paper. It will probably be necessary to make additional tests to determine what factor or factors give rise to these differences.

Watt-hour meters operating on the induction principle are susceptible to inaccuracies due to departures from sine wave and changes in voltage, power-factor, frequency, and temperature to varying degrees, depending upon the design of the watt-hour meter. No reference has been made to the possible effect upon the metering current and potential transformer performance and particularly when operating under service conditions. Note also that the actual periodic field calibration of service meters is not made with the current having the wave form of the rectifier, but with a wave form derived from the voltage supply. Wave-shape characteristics of rectifiers, even when designed for the same service, apparently differ considerably and may be affected by unbalance of phase voltages, and the wave form of supply. The harmonics of voltage and current concerned are also not only affected by the reactance and capacitance in the supply circuits, but may also be influenced by inductive effects from the apparatus which the d-c circuit supplies.

It is of related interest that even the relatively small percentages of harmonics that may occur in the test circuit of a transformer loading device designed for low-power-factor tests on meters, may at times show appreciably larger deviations between the performance of two standard watt-hour meters of the same manufacture, but of different types, than are shown in this paper for rectifiers which give greater departures from sine-wave values.

In general, it is expected that the rectifier active and reactive metering inaccuracies may not be large, but just how large under service conditions is still a question.

As stated initially, test-floor data, while occupying an important part in the investigation, must be supplemented by field research. The great difficulty in the field has been in the inability to compare indicating with integrating instruments under service load conditions.

About a year ago I proposed, in writing, to the meter manufacturers of this country, the development of an energy-integrating device consisting of a wattmeter whose position would be integrated by means of the movement of mechanical parts driven by a synchronous motor. This principle of integration has for years been applied to certain fluid meters whose position of a moving element is determined by a difference of pressure across an orifice or nozzle in the pipe through which the fluid passes. The position of this moving element is integrated by means of a synchronous motor-driven mechanism and is very satisfactory where the frequency is closely controlled. As a result of my request, one manufacturer has designed and developed such an electrical integrating instrument, for general metering service and for precision measurements which is shown in figure 1 of this discussion. Note that for steady load conditions the kilowatt indication may be used. Since a wattmeter is not appreciably affected by wave form, power factor, or frequency, the integration of the wattmeter position can be directly compared with the integration of an induction-type watt-hour meter in series with it and the load. When sufficient data have been collected from such comparative measurements in service and on the test floor and such data related to results reported in this paper, then will we have a more definite knowledge of the accuracy of service meters used in the measurement of power rectifiers or other loads having considerable departures from sine waves of current or voltage.

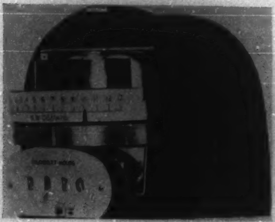

Figure 1. Dynamometer-type watt-hour meter

Wm. M. Young (Ohio University, Athens): The paper is decidedly interesting and the results gratifying because of the close agreement between calculated and experimental data. There is here one more verification of the fact that present-day watt-hour meters are capable of doing a good job of metering on unusual applications wherein the wave form of both current and voltage may be badly distorted. It is comforting to know that the many harmonics introduced by rectifiers have an insignificant effect on the reading. At first sight it would seem that the 20 per cent fifth harmonic and 14 per cent seventh harmonic would create much more disturbance; however, since these harmonics operate on small impedances, there is little error in the actual reading.

The main problem is still present with polyphase watt-hour meters and, particularly, with single-phase watt-hour meters. It seems that there should be made a careful study of the effect of the various harmonics in the voltage and current waves for various power factors on both of these meters. Numerous organizations are at present working on the various phases of the problem and the data obtained, when completely assembled, will fill in the many blanks in the complete story.

Might it not be worth-while for the committee on instruments and measurements to take under advisement the problem of co-ordinating the work being done on polyphase and single-phase watt-hour meters so that various investigators may be able to interchange ideas and proceed more rapidly to obtain a complete analysis of this very important problem?

Certainly manufacturers are to be complimented on producing metering devices at such low cost which are so vastly superior to the old measuring devices.

Everett S. Lee (General Electric Company, Schenectady, N. Y.): The paper by Messrs. Weller, Trekell, and Stebbins is another outstanding contribution to the accuracy of the watt-hour meter. It is also an outstanding contribution in measurements technique for when one must measure differences in watt-hour meter performance of a few tenths of one per cent within the accuracy which the authors believe to be within two-tenths per cent, the greatest of attention to every measurement detail is necessary. Particularly is this so when the measurements must be made in conjunction with large power apparatus together with the actual performance characteristics which apply and which must be taken into account.

Such tests are very costly because of their magnitude and it is for this reason that they may not be made to cover every conceivable case. In this particular case of the effect of wave form of mercury-arc rectifiers upon watt-hour meters, the question came up and had to be answered some years ago even without the results of actual tests, but from a knowledge of watt-hour meter performance this was done by the engineers, and the tests which Messrs. Weller, Trekell, and Stebbins have now reported in their paper confirm these earlier calculations and statements with exactness. This should be a continued assurance to the accuracy and dependability of the watt-hour meter.

As with the advance of more exacting performance requirements the microscopes of inquiry have been trained more and more upon it, the watt-hour meter has revealed itself to have the requisite characteristics demanded of its most exacting requirements. And all this in a particularly simple device produced and used by the millions.

As an example of a new method of approach to the performance of the watt-hour meter on intermittent loads, we have in the paper by Messrs. Faucett, Keener, and Helm (AIEE TRANSACTIONS, volume 59, 1940, pages 460–4) new statements of performance and these, too, as described in Mr. Trekell's discussion and in Professor Faucett's further statement, agree that differences found are so small as to be negligible in well-designed watt-hour meters. This, again, adds to the remarkableness of the performance of the watt-hour meter.

I. E. Weston (nonmember; Board of Transportation, City of New York, N. Y.): The paper gives us for the first time the results of tests on equipment comparable in size to the rectifiers used in traction work at present. This type of load is continually increasing, the Board of Transportation at the present time having some 240,000 kw capacity, all supplied under conditions discussed in the paper.

In connection with the power loss due to the various harmonics present, in a master's thesis at Brooklyn Polytechnic Institute in 1936 and in a paper in preparation, I give the following expression for this loss at light load for the delta-double wye, six-phase, six-anode connection:

$$P_{F0}=\frac{9RI^2}{2\pi^2}\left[1+\frac{1}{5^2}+\frac{1}{7^2}+\frac{1}{11^2}+\frac{1}{13^2}+\ldots\ldots\right]$$

which shows that the harmonic power loss varies inversely as the square of the order. Furthermore, the total harmonic power loss as given in this equation is about seven per cent of the fundamental power loss. If the latter is ten per cent, then the harmonic power is seven-tenths per cent which agrees with the authors' figure at light load. This harmonic power is all consumed in the feeders as I believe the authors have implied.

It would be interesting to know what transformer connections were used in the 6- and 12-phase rectifier tests as it is known that the value of harmonics varies with different transformer connections of the same number of phases. For example, the harmonic power loss in a star-connected rectifier is $33^1/_3$ per cent greater than in the double wye connection due to the different current-wave shapes.

Regarding the test results for the 6-phase rectifier, the increased meter speeds at higher loads is interesting. It may be due to one or several conditions. The effect of poorer power factor may show up at heavier loads. This tends to increase registration at the higher harmonics. Or it may be due to nonlinear effects in the iron circuit of the meter as another investigator has suggested. There seems to be some doubt in either case.

Regarding the harmonic volt-amperes in table V, why is the 11th and 13th greater than the 7th? Could these frequencies be approaching series resonance of the supply system, in which case maximum harmonic current is obtained?

Another question is why is the percentage of harmonic power greater at 156 per cent than at rated output (0.9 per cent compared with 0.6 per cent)?

Watt-hour meters on rectifier installations are tested against a rotating standard using only the voltage wave as a source of supply for both coils. This standard was previously calibrated on a sinusoidal wave at the laboratory. Thus the meter is not tested under actual operating conditions as it does not carry the current wave during the test.

Due to the fact that the feeder circuits to each rectifier installation are different, a single correction constant cannot be used for rectifiers at different locations. If the magnitude of wave distortion warrants a correction factor, it would have to be determined separately for each installation.

L. A. Burckmyer, Jr. (Cornell University Ithaca, N. Y.): The discussion of the technical aspects of this paper will be left to others, but I cannot refrain from expressing my appreciation of the presentation. It seems obvious that any paper should enable a reader quickly to discover whether its subject, method, or conclusion are of interest to him. If they are, full details must be available in order that he may form his own opinion of the validity, limitation, and application of the results.

This paper is outstanding in its *logical order of presentation*, giving the reader an immediate clear understanding of the object and scope of the work, of the results obtained, and of the authors' conclusions. Confidence in those conclusions, and facilities for checking them are provided by the *specific identification of all imporant apparatus*, with a tabular list of *complete ratings*, a concise but *complete description of connections and test procedure*, and a self-explanatory tabular presentation of detailed results. Curves, figures, and oscillograms are given *complete subtitles*, and *all test points are shown on the curves*.

Consideration of the requirements of the reader and of economy of his time often seems to receive little attention in the presentation of papers. Such a marked exception deserves special comment.

C. T. Weller, H. E. Trekell, and F. O. Stebbins: We are gratified at the interest shown in our paper and appreciate particularly L. A. Burckmyer, Jr.'s approval of its "logical order of presentation". In preparing the paper, we were somewhat uncertain as to how much rectifier theory and practice should be included, since the investigation covered primarily the performance of watt-hour meters. While the rectifier section of the paper is quite condensed, we believe measurement engineers will be able to determine from it the harmonics to be expected from 6- and 12-phase power rectifiers, both at light load and at rated output under representative conditions (see table IV, part 2, *A* and *B*).

I. E. Weston referred to the percentages of harmonic power at different outputs and to the relations between the volt-amperes at the various harmonics. It should be noted that the harmonic power values given represent the probable maxima, not the actual. These values of harmonic volt-amperes or power are based upon analyses of oscillograms; it is well known that such analyses are not accurate when the percentages of harmonics are small, as is the case with the voltage waves in these tests.

The 6-phase rectifier transformers were connected delta primary, 6-phase double-wye secondary and the 12-phase transformer was connected delta primary, 12-phase quadruple zig-zag secondary. Both of these circuits are widely used for power-rectifier railway and electrochemical installations, particularly for large kilowatt ratings. It is shown in reference 1 of the paper that for a given angle of overlap, the percentage values of harmonics of the same order in the input circuit currents for 6-phase rectifiers are independent of the transformer connections used. Thus, for a given kilowatt rating, the harmonic power loss in the supply system also is independent of the transformer connections. However, the wave shape of the currents is affected by the connections. In regard to the wave shape of the system voltages, we believe the harmonics are comparatively small in the majority of cases where rectifiers are supplied. This would indicate that watt-hour meter checking based upon such voltages is accomplished under approximately the sine-wave conditions obtained in our tests.

W. C. Wagner suggested additional tests on rectifiers, but the high cost emphasized by E. S. Lee constitutes a barrier. The work involved is indicated by the fact that over 4,000 instrument readings were taken in the 6-phase rectifier tests alone; the averaging and correction of these readings supplemented the analyses of 24 chronograph charts. Figure 5 of the paper shows part of the equipment necessary. Field tests, of course, would be very desirable and in this connection, W. C. Wagner's description of the new meter is interesting. As W. M. Young stated, numerous organizations are now working on various phases of the general problem of the effect of harmonics on watt-hour meters; this is most encouraging.

E. S. Lee also emphasized the question of accuracy. It should be noted that a change in the estimated accuracy of the present results of from 0.15 to 0.2 per cent was forced merely by rounding off the readings to the nearest 0.1 per cent. This involved shifts up to 0.05 per cent in individual values and up to 0.1 per cent in the differences between rectifier and sine-wave results shown in table II.

The var(rva)-hour and power-factor-meter results are of value chiefly in showing up certain other differences which enter the measurement picture along with harmonics, especially when these are confined largely to the current waves. Such differences have long been a source of misunderstandings. Thus in test numbers 2 and 2*a* with the 6-phase rectifier, a maximum difference of about three per cent was obtained between the power-factor-meter reading and the value calculated from the standard formula

$$\text{power factor} = P/\sqrt{3}EI \qquad (1)$$

The respective values are 0.976 and 0.946 [see formula (3)]. If the corresponding angles of about 12.5 and 19 degrees are substituted, the instrument evidently is indicating but 66 per cent of the calculated angle. A similar statement applies to the power-factor angle derived from the var-hour meter reading.

The most important result of the tests is the demonstration that the effect of 6- and 12-phase power rectifiers on the performance of typical watt-hour meters is less than 0.5 per cent. Also, the effect of a 6-phase rectifier on a var-hour meter is less than one per cent. It is concluded that limits of twice these respective values should cover all practical cases. The effect of a 6-phase rectifier on the power factor derived from the var-hour meter results or shown by the power-factor meter is about three per cent.

In table III of the paper, the performance of all usual instruments and meters is shown under conditions which are ideal, except for prominent harmonics in the line currents; the circuit voltages and currents are balanced and the harmonics in the line-to-line voltages are comparatively small. Thus the data and formulas in the paper afford an introduction to the effect of harmonics on instruments and meters and to the significance of some of the pertinent ASA definitions (ASA *C*42).

Application of and Operating Experience With Hydrogen-Cooled Synchronous Condensers and Alternators

Discussion and authors' closure of paper 40-76 by Philip Sporn and F. M. Porter, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 890–907.

Raymond Bailey (Philadelphia Electric Company, Philadelphia, Pa.): This paper is particularly valuable not only because it covers a subject which is of widespread interest, but also because it combines design and experience information in a way that is so useful to the engineer.

The statistics presented by the authors indicate considerable outage time for synchronous condensers on account of the water-cooling facilities and troubles with the rotors. It would seem to me that in view of the present state of design a very high proportion of outage time should not be required on account of either one of these items. Incidentally it is my belief that the design of both synchronous condensers and generators should be such that in the event of a leak in the water coolers the water cannot drain into the winding, but will be carried to the bottom of the machine.

It would appear that the authors' remarks about the use of hydrogen at pressures higher than nominally one-half pound are important and that this should receive the attention of others so that experience will be gained through the operation with hydrogen at higher pressures.

It would be interesting to learn of the viewpoint of the manufacturers' engineers on the effect of hydrogen on the design of windings for use at voltages above 13.2 kv.

It does seem to me that the auxiliaries needed for the use with hydrogen in generators are relatively complicated, but it may be that experience in the future will bring about a desirable simplification.

C. M. Gilt (Consolidated Edison Company of New York, Inc., New York): While the operating experience of the Consolidated Edison Company with hydrogen-cooled equipment is much more limited than that of the American Gas and Electric Company, its experience has likewise been generally very encouraging. There are now in operation in its Waterside plant two 53,000-kw 0.9-power-factor 13,800-volt, 3,600-rpm turbogenerators with hydrogen cooling and two 65,000-kw 0.8-power-factor units are on order for the same plant.

The first unit was put in service with air cooling on September 10, 1937. Trouble developed immediately with the shaft seals when oil was introduced for tryout purposes carrying oil into the generator. Certain changes in the design of the seals and particularly the oil drain off were made but it was agreed that the machine should be run without oil in the seals and at a somewhat reduced rating with air cooling until the bearing supports could be modified and the rotor slotted to reduce vibration. The desirability of keeping the machine in service delayed this work and hydrogen cooling was not introduced until July 6, 1939. Since then the hydrogen-cooling system and seals have given excellent service.

The second unit was put in service on June 29, 1938, and after preliminary tests hydrogen was introduced and has operated entirely successfully since. Up to January 1, 1940, the first machine had had 4,156 hours of operation in hydrogen and the second 11,762 hours.

While the distribution of losses is somewhat different in the two machines both have an appreciably higher rating in hydrogen than in air. The temperature rise of the first machine above the inlet air was 50 degrees for the stator and 100 degrees centigrade for the rotor when carrying 45 megawatts at 96 per cent power factor. The temperature rise of the stator was 35 degrees centigrade when carrying 45 megawatts at 96 per cent power factor when operated in hydrogen. The second machine when carrying 16 megawatts at 99 per cent power factor in air had a stator temperature rise of 42 degrees centigrade and a rotor temperature rise of 73 degrees centigrade. When carrying 46 megawatts at 97 per cent power factor in hydrogen, the stator temperature rise was 39 degrees centigrade and the rotor was 56 degrees centigrade.

The design of the equipment is of the low-pressure type maintaining a pressure of less than one-half pound. One of the new units will be designed for low-pressure operation but with provision so that the pressure can be raised to and maintained at 15 pounds, though with increased usage of hydrogen. The makeup of hydrogen on the two present units amounts to about 350 cubic feet of hydrogen each per week.

The hydrogen-control system of the two units, while differing in detail of equipment, performs essentially the same functions and is automatic in its operation and provided with alarms. The hydrogen in both machines is cooled by finned cooler tubes installed in the frame of the unit through which distilled water is circulated. Equipment and connections are provided for automatically turning on water from the city water mains in case of trouble in the normal water-cooling system.

The generator is scavenged with CO_2 in changing from air to hydrogen or the reverse. Three to four hours are required in the process of filling with hydrogen and about 1,900 cubic feet of hydrogen are used to fill a unit with a hydrogen volume of 1,400 cubic feet.

M. D. Ross and **C. C. Sterrett** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The writers are to be congratulated on their paper, which covers the field of experience with hydrogen-cooled machines from the earliest days of such apparatus and leaves little to be added in the way of comment. Such papers are of real value to both operating companies and the manufacturers as it brings out a frank discussion of problems encountered and their solution.

The earlier hydrogen-cooled condenser units were in some cases equipped with more auxiliary apparatus than was needed and there has been a gradual process of simplification in later units. As an example, one operating company dispensed with the automatic gas inlet valve and introduced gas by hand every few days. With such low gas leakage figures as indicated in the paper, this method of operation is satisfactory unless the operating procedure is such that the operator checks the unit at infrequent intervals when an automatic inlet valve would be justified. The company with which the writers are associated is also building two hydrogen-cooled condensers in which the hydrogen-purity meter is dispensed with and a portable Orsat gas analyzer being used to check the gas mixtures in filling and scavenging. As the gas pressure in the condenser is always above atmospheric pressure and gas flow is always outward, the purity meter does not seem to be a vital part of the equipment. The purity meter is desirable, however, on a turbine generator where the sealing oil may carry a certain amount of air into the hydrogen enclosure and the meter is needed to keep check on the purity and sound an alarm in case the purity drops below a certain value.

The authors mention the fact that carbon dust from the collector rings gets into the condenser. It should be noted that the New Carlisle condenser is equipped with a separate cooler and ventilating blower for the collector housing. The collector chamber forms a recirculating system in itself separate from the main enclosure so that carbon dust cannot get into the main enclosure.

The authors' data on their units certainly prove that hydrogen-cooled apparatus has progressed beyond the experimental stage and has a definite field of application in the larger high-speed equipment.

D. S. Snell (General Electric Company, Schenectady, N. Y.): The excellent and timely paper by Messrs. Sporn and Porter well illustrates the value to industry of co-operative effort between manufacturer and consumer in the development of a new product. Nineteen years ago, hydrogen cooling for electrical machinery was only an idea. Today, thanks to the willingness of the utilities to accept a radical innovation, and to the efforts of the manufacturers, hydrogen cooling is an established fact.

When this new type of cooling was first proposed, in 1921, its most desirable application was considered to be in connection with turbine generators, since this type of apparatus had the highest windage loss of any of the common types and would therefore show the greatest gain in efficiency by the use of hydrogen cooling. Accordingly, development of the hydrogen-cooled generator was early begun, and by the end of 1927 a developmental generator of 6,250-kva capacity had been in successful operation in hydrogen for nearly a year.

However, due to some reluctance on the part of the power industry to accept a hydrogen-cooled machine unless it was capable of carrying full-load rating in air, which was not an economically practicable proposition, interest in the hydrogen-cooled generator was slow in developing.

In 1928 the hydrogen-cooled synchronous condenser was introduced. This type of apparatus represented a considerably more simple application for hydrogen cooling than the turbine generator, since shaft-sealing equipment was not required. One of the first of these machines to be built was installed at the Turner station of the Appalachian Power Company and is described in the present paper. Later, several more hydrogen-cooled condensers were purchased for other companies on the same system. Experience with these machines, as reported in this paper, and with other machines of this type, was highly satisfactory and amply demonstrated the many advantages claimed for hydrogen cooling.

During the 1933–1935 industrial depression, considerable interest was developed in the superposed or "topping" turbine, as a means of increasing the efficiency of existing plants, which made desirable the construction of generators at 3,600 rpm in capacities considerably in excess of those previously considered practicable with air cooling, or which, if air cooled, would require external fans for ventilation. This made the use of hydrogen cooling for such generators highly advantageous, for the increased output per pound of material obtainable with hydrogen permits larger unit outputs and the construction of such units with internal fans. Also the saving in power with hydrogen cooling for large 3,600-rpm generators is a significant item.

As a result of the several years' successful operation of hydrogen-cooled synchronous condensers, there was little opposition, when business activity was resumed, to the adoption of hydrogen cooling for generators of the large 3,600-rpm type; hence the greater share of the generators ordered for operation with superposed turbines were hydrogen cooled. The first of these units to be ordered was the 50,000-kva Logan generator, mentioned in the present paper, and much credit is due Mr. Sporn and his organization for their foresight and courage in fostering this new product.

There have been placed in service to date, by the General Electric Company alone, a total of 14 hydrogen-cooled generators, with a combined capacity of 863,800 kva. Twenty-nine hydrogen-cooled generators are now under construction at the factory, having a total capacity of 1,578,000 kva. These figures are significant of the rapid acceptance by the power industry of this new type of generator, an acceptance which has benefited both the industry and the manufacturer and which, as has been pointed out, has been largely due to the co-operative efforts of both. Much appreciation is due the authors of the present paper and their associates for the significant contributions which they have made to this important development.

E. S. Henningsen (General Electric Company, Schenectady, N. Y.): It is seldom that a paper of so much interest and practical value to both operating and designing engineers is presented to the Institute. The authors are to be congratulated for this well-arranged record of the various developments, successes, and failures that have occurred during the little more than a decade since hydrogen cooling of rotating apparatus was initiated. It is particularly valuable in that the record covers such a large percentage of all of the hydrogen-cooled machines installed during that period.

Mr. Sporn's aversion to complacency on the part of designing engineers is evidenced by the record of changes and the number of new ideas tried out, such as: condenser speeds of 1,200 and later 3,600 rpm, 24,000-volt armature winding, electronic exciters, higher-pressure hydrogen operation, and 3,600-rpm hydrogen-cooled generators. He and his company have always been more than willing to try any suggestion that gave reasonable promise of being a step forward or a means of getting worth-while experience.

I think everyone will agree that the overall performance of these hydrogen machines has been good. Troubles experienced have largely been the same kind of troubles encountered on air-ventilated machines. The authors' figure of 29.9 per cent of the forced outages being attributable to construction for hydrogen operation is overly pessimistic in that they have included everything chargeable to this construction with no credit for the fact that many of these same outages probably would have occurred on air-ventilated machines. For example routine inspection of main shell and collector compartment account for 8.17 per cent of the forced outages. Similar inspections certainly should be made on air-ventilated machines although admittedly they would not take as long to make as with the hydrogen construction. Similarly, as the authors call attention to, collector and brush troubles are by no means confined to machines operating in hydrogen. These and other items charged lead me to express the opinion that only one-half to two-thirds of the 29.9 per cent of forced outages should be charged against the difference in construction between hydrogen and air-ventilated machines.

When one thinks back to the state of the welding art in 1927 when the first hydrogen condenser was built, it is no wonder that subsequent tests, as the authors report, showed them incapable of holding hydrogen at 15 pounds pressure. The wonder is that they held hydrogen at one pound and this was the biggest problem in the design of the first machine. The advance in knowledge in the welding art has made possible welded joints that are gastight at pressures higher than it is desired to operate.

It would be very interesting if a similar paper could be presented giving a record of operating performance over a long period of time of an equal number of air-ventilated condensers.

R. W. Wieseman (General Electric Company, Schenectady, N. Y.): The operating experience of the first eight hydrogen-cooled synchronous condensers installed on the American Gas and Electric system is a valuable contribution to the electrical industry. This experience, which covers nearly 60 machine years, shows that the machine availability factor is 96.1 per cent, but the availability factor for the hydrogen features alone is 98.8 per cent. This paper, therefore, indicates that the performance of the hydrogen features is quite satisfactory, in fact, it substantiates the claims on hydrogen-cooled rotating machines made nearly 20 years ago. The popularity of this development is indicated by the fact that in this country over four million kva of hydrogen-cooled rotating machines are in service or under construction. These machines range from 300 to 3,600 rpm, 4,150 to 24,000 volts, and 12,500 to 176,470 kva.

When hydrogen-cooled rotating machines were proposed, the explosion hazard was one of the debatable issues. To offset this, it was natural to build the first machines with an explosionproof shell which is still being used on present-day machines. This fear of explosion, however, has definitely diminished because, as stated in the paper, inert scavenging gases are no longer used when synchronous condensers are filled with hydrogen.

It is of interest to note that the first machine to be installed in 1928 has had a normal hydrogen leakage of only 4.4 cubic feet per day, or about nine cents per day. The bearing oil in this machine has never been changed. The main part of the unit including the bearings is inspected only once every five or six years. Since 1928 many improvements have been made such as in the collector compartment and seal arrangement, welding, surface-cooler construction, shell joints and gaskets, piping, and accessories, etc. Further improvements will no doubt be made especially with regard to simplifying the accessories. The hydrogen-purity indicator, for example, may be omitted on condensers since, as stated in the paper, it is of real service only in filling the machine with hydrogen.

One of the important features of a hydrogen-cooled machine which appeals to me is the excellent operating conditions provided for the winding insulation. Aging of the insulation due to oxidation no longer takes place. Corona damage on high-voltage windings is conspicuously absent. Harmful foreign matter which sometimes forms a blanket around an air-cooled winding has been eliminated and, as reported in the paper, the insulating varnish on the surface of the winding retains its high-luster finish. Obviously under these conditions insulation is at its best, and if properly maintained and protected, it will give many years of dependable service.

This is a most interesting and timely paper. The authors are to be congratulated on the thorough manner in which the subject matter is presented.

Soren H. Mortensen (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The operating experience with hydrogen-cooled machines so ably presented in Messrs. Sporn and Porter's paper not only proves the practicability of hydrogen ventilation but also indicates paths for further developments, such as increased ratings by increased pressures as well as simplifications in the hydrogen control.

For comparison of generator outputs obtainable under various operating conditions, test results depicted in curves, figure 1 of this discussion, may serve. These tests were made some years ago upon a turbogenerator designed for hydrogen cooling. Constant loads were maintained until temperatures became constant with machine air-filled and later hydrogen-filled with pressures of one-half pound and 8.8 pounds per square inch respectively. Figure 1 shows that for 60-degree generator temperature rise, 100 per cent load was carried with air cooling, 126 per cent with hydrogen at one-half pound pressure, and

163 per cent with 8.8 pounds pressure per square inch. This test also brought out that the hydrogen leakage through generator walls, joints, and welds, increases approximately with the square root of the gas pressure. As the oil flow through the shaft seal was maintained approximately constant during the respective tests the hydrogen contamination or entrainment in oil was not appreciably affected by the test pressures.

Subsequent operating experiences indicated that shaft seals, generator materials, welds, and joints, can be made adequate to meet the requirement of higher gas pressures, thereby making possible further increases in generator outputs obtainable at 3,600 rpm.

Another of the features suggested in the authors' paper is the desirability for simplifications of the hydrogen control. One simplification made possible by the Allis-Chalmers type of generator shaft seal was described in the writer's discussion of hydrogen-cooled generators [AIEE TRANSACTIONS, volume 59, 1940 (January section), pages 48-9]. This discussion brought out the practicability of substituting vacuum scavenging for scavenging with inert gas in preparation for filling or evacuation of generator hydrogen. The elimination of scavenging with inert gas further permits simplification to the extent of omission of the piping otherwise used for this purpose.

Additional operating experience with Allis-Chalmers Company's hydrogen-cooled generators indicates the possibility of omission of the hydrogen-purity meter control. This simplification seems possible as the air volume entering the generator hydrogen with the seal oil is so small that the new hydrogen admitted to maintain pressure by making up for hydrogen leakage has been adequate to keep the hydrogen purity above the 97 per cent generally maintained by the purity meter. If further experience substantiates this, the purity meter can be omitted from the installations where vacuum scavenging is used.

In machines where the volume of the sealing oil contacting the generator hydrogen is small, special vacuum treatment of this oil becomes unnecessary and it can be taken directly from the turbine supply and returned to it through a detraining tank.

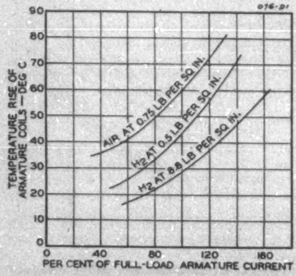

Figure 1. Comparison of air and hydrogen cooling at various pressures. Temperature rise of the armature coils in a 31,250-kva turbine generator

In conclusion I want to express my appreciation to the authors, for their very complete and constructive paper.

F. A. Lane (American Gas and Electric Service Corporation, New York, N. Y.): The Logan generator* went into service as a hydrogen-cooled machine on December 2, 1937, and from that time until September 30, 1939, the unit had run a combined total of 400 days. The running time lost during this period has not been occasioned by any trouble or maintenance in connection with the generator proper or its gas sealing system. The maximum load which has been carried over a one-hour period is 48,500 kw at 99 per cent power factor. Continuous loading of approximately 45,000 kw has been maintained without any operating difficulties, and without necessitating gas pressure above one-half pound per square inch to effect the required cooling of stator and rotor windings except for two weeks in September 1939, when the pressure was raised to approximately two pounds per square inch because of extremely high-temperature cooling water. In figure 2 of this discussion curves are shown giving the temperature rises which have been obtained for various loads and for one-half pound per square inch hydrogen gas-pressure operation. These rises are based upon the temperature of the ingoing pad liquid. Curve 2 indicates the temperature rise of the gas after it has passed through the finned-type pad coolers at the center of the machine. The maximum gas temperature reached at this point was 50 degrees centigrade. Stator temperatures are obtained by means of ten resistance temperature detectors located at various points in the coil slots and curve 3 shows the highest stator temperature rise. The highest stator temperature which has been reached at any time is 86 degrees, whereas the maximum permissible temperature is 100 degrees centigrade. The field temperature rise as given by curve 4 was obtained by means of a Leeds and Northrup recorder which indicates temperature change as measured by the rise of field resistance. The maximum field temperature which has been obtained is 108 degrees centigrade compared with the permissible maximum of 125 degrees centigrade.

PAD LIQUID SYSTEM

Plant condensate is used as make-up for the pad liquid system of the Logan generator. In order to eliminate the possibility of air pockets in the pads, it is necessary to obtain a high vacuum before filling with condensate. In this respect some difficulty was experienced initially but by circulating the pad liquid for some time and by wasting a portion from an overhead expansion tank, it was possible to remove practically all of the air still remaining in the pads. The operation of the system has been satisfactory and the pad liquid has not been changed since the initial filling. A partial inspection of the interior of the generator after a year's service gave no indication of any water leakage in the machine. Such an occurrence during operation would be detected immediately by the oil and water leakage indicator which is equipped with alarm contacts. The two heat exchangers for the pad liquid are used during the warmer months but only one exchanger is needed during most of the year.

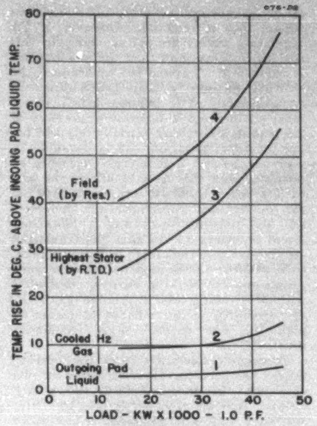

Figure 2. Load-temperature curves, Logan 50,000-kva turbogenerator

SHAFT SEALING SYSTEM

No difficulty of any great importance has been experienced with the shaft sealing system and its accessory equipment. No failure to maintain the seals has occurred at any time. This system as of September 30, 1939, had operated 500 days. The purity maintained during the first 224 days varied gradually from 99 per cent to 97 per cent but with continuous running it finally dropped as low as 93 per cent and scavenging with pure gas was necessary to bring the purity back to 95 per cent. It was determined that the difficulty was primarily because the seal-oil flow was greater than anticipated. As a result, the vacuum treatment of the oil was less than expected because of the greater amount of seal oil to be treated. Furthermore, a large quantity of air and moisture had to be removed from that portion of the seal oil which is drawn from the main oil tank to replace the oil flowing to the air side of the seals.

Changes were made in seal clearances and additional venting was provided in the bearings, which decreased the seal-oil flow 60 per cent. Since then the machine has been operated at $^1/_2$ pound per square inch gas pressure over a period of 161 days and a purity of from 98 to 99.5 per cent has been maintained. The seal oil pressure is automatically held at approximately 3 pounds per square inch above the gas pressure. With the machine stationary, the seal oil is cooler, its viscosity higher, and the differential pressure at the seals becomes approximately $3^1/_2$ pounds per square inch. At the same time, there is less liberation of air inside the generator, and the purity therefore builds up to approximately 99.5 per cent. The shaft sealing system is suitable for operation up to 15 pounds per square inch gas pressure, in which case a seal oil pressure of not less than 17.5 pounds per square inch is required. However, if the regulating valves are set to maintain this seal oil pressure, and then the gas pressure is reduced to $^1/_2$ pound per square inch the seal oil pressure automatically drops to approximately 5$^1/_2$ pounds which represents an increase in differential pressure of 2.5 pounds above that actually required for low-pressure operation. It is desirable to maintain as low a differential pressure as possible, since this directly affects the seal oil flow, which in turn determines largely the gas purity and consumption. Therefore, for high-pressure operation it has been found necessary to readjust the regulating valves to obtain the proper differential pressure desired. This adjustment is a very simple one and could be entirely eliminated without seriously affecting either gas leakage or purity, if intermittent operation at high and low pressure were frequently involved.

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* Refer to section IX, paragraph A-5, of the paper.

Emergency operation from the bearing header has been run experimentally on two occasions. In the first case the purity dropped from 98 to 90 per cent in 15 hours, and later with a 60 per cent reduction in seal oil flow a similar purity drop occurred in 26 hours. During these operations the gas pressure actually increased slightly because of the air being released inside the generator more rapidly than the hydrogen was absorbed. With such operation it is possible that hydrogen may be released in the main oil tank, but the vapor extractor on this tank is depended upon to remove any hydrogen which might otherwise collect. During the test runs just mentioned it was not possible to detect the presence of any hydrogen in the main oil tank.

HYDROGEN CONTROL AND ACCESSORY EQUIPMENT

Experience with the gas-pressure-reducing equipment and accessories for automatically maintaining pressure in the generator has been excellent and similar to that obtained with synchronous condensers. Trouble was experienced initially with the purity indicator because of excessive vibration of the panel on which it is supported, but by the installation of springs and rubber mountings under the control cabinet reliable performance was obtained. One factor affecting the accuracy of this instrument is the possible presence of moisture in the sample of gas being circulated through the analyzing equipment which may cause inaccurate indication of two to three per cent. It has been necessary to periodically replace the calcium chloride in the dryer of the sampling circuit. This purity device has not given as accurate results as are desirable, but in general it is satisfactory for this service. However, it is believed that a more rugged and accurate device should be developed for this application.

GASTIGHT CONSTRUCTION AND HYDROGEN CONSUMPTION

In the case of a generator the gas consumption is determined not only by leakage through the outer shell joints, but also by the amount of hydrogen absorbed by the seal oil which is later evacuated from the oil and discharged to the atmosphere. Tests have indicated no appreciable gas leakage through the shell joints even at pressures as high as 15 pounds per square inch. A compound consisting of vacuum-treated triple-boiled linseed oil was initially forced into the sealing grooves and was apparently satisfactory for this service. However, in November 1938, it was found that lubricating oil from the bearings was gradually leaking into the sealing grooves and dissolving the sealing material. It was decided to replace it with a plastic sealing compound known as "Copaltite." This material which is not affected by hot lubricating oil has given satisfactory performance for the past 12 months. From the limited experience with the metal-to-metal type of joint for gastight construction, it would appear that it is preferable to the gasketed joint now in use for synchronous condensers.

The amount of hydrogen lost at the bearing seals depends upon several factors, such as oil temperature, oil flow to the gas side of the seals, and the degree of vacuum treatment. It also is affected by whether the shaft is rotating or stationary, although leakage results to date indicate no appreciable difference in gas consumption between the running and stationary conditions. Operation in hydrogen at approximately one-half pound per square inch in gas pressure, over a period of 115 days during the latter part of 1938 resulted in an average daily leakage of 48 cubic feet. Changes in the bearings as previously mentioned, decreased the seal oil flow, increased the degree of vacuum treatment, and decreased the gas consumption. Operation in hydrogen at approximately one-half pound per square inch over a period of 98 days from May to August 1939, has resulted in an average daily consumption of 38 cubic feet. The cost of delivered gas at this location is approximately two cents per cubic foot thus making the yearly cost approximately $275.

In February 1939, tests were made to determine gas consumption at several pressures up to 15 pounds per square inch for respective periods of 2$^1/_2$ or more days with the machine operating normally and with similar daily load cycles. Results obtained were as follows:

Average Gas Pressure (Pounds Per Square Inch)	Average Seal Oil Pressure (Pounds Per Square Inch)	Average Gas Consumption (Cubic Feet Per Day)	Duration of Run (Days)
0.5	3.5	40	12
4.9	7.2	115	6
10.1	12.6	221	3
15.1	17.6	351	2.5

Because of the many factors involved in determining gas leakage on a running machine with varying load and temperature, the above results cannot be considered to be extremely accurate, but for practical purposes they demonstrate conclusively that as far as gas consumption is concerned, operation up to pressures of 15 pounds per square inch can be accomplished with economy and reliability.

OPERATION AT HIGHER GAS PRESSURES

In the preceding paragraph there were mentioned the tests which were made at approximately 5, 10, and 15 pounds per square inch, respectively. During these runs various loadings were maintained for periods of sufficient duration to permit the obtaining of reasonably accurate temperature-rise data for the field and stator windings. These comparative curves are presented in figure 3 of this discussion and show clearly what may be expected from high-pressure operation. In the case of the stator winding, only the end windings of which are cooled directly by hydrogen, it will be noted that for a load of 45,000 kw 1.0 power factor the temperature rise as measured by resistance temperature detector decreased approximately 2.5 degrees centigrade for each 5 pounds per square inch increase in pressure. In the case of the rotor, as would be expected, the cooling effect is more marked, at this same loading, and indicates a decrease in temperature rise of approximately 7 degrees for the first 5 pounds pressure increase, another decrease of 6 degrees for the 5 pounds increase to 10 pounds per square inch and a further decrease of approximately 4 degrees for the 5 pounds increase to 15 pounds. Further tests will be necessary at steady loads in order to definitely and more accurately determine the shape and position of these various curves. The temperature rise of the cooled gas also decreases with increasing gas pressure, in the same manner as previously demonstrated in the case of synchronous condenser tests.

The operation of the shaft sealing equipment was entirely satisfactory at these higher pressures, although operation over longer periods will be necessary fully to demonstrate this. In the past there have been experienced at Logan, raw cooling water temperatures as high as 100 degrees Fahrenheit and for this reason, and to definitely check its feasibility, the machine was designed for higher-gas-pressure operation. Advantage can thus be taken of this additional cooling means during the extremely hot periods. In September 1939, the machine ran for several weeks with raw cooling water temperatures ranging from

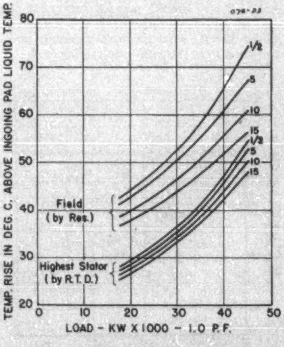

Figure 3. Load-temperature curves at various gas pressures, Logan 50,000-kva turbogenerator

Gas pressure in pounds per square inch indicated on curves

90 to 98 degrees Fahrenheit, with no difficulty whatever. The gas pressure was boosted several pounds per square inch to reduce maximum temperatures, but although loads as high as 45,000 kw unity power factor, were carried, no excessive temperature resulted.

Operating Experience With Windsor 66,667-Kva Turbogenerator*

This machine went into service as a hydrogen-cooled unit on April 21, 1939, and from that time until September 30, had run a combined total of 118 days. Running time lost has not been occasioned by any trouble or maintenance in connection with the generator proper or its shaft sealing system. The maximum load which has been carried over a one-hour period is 62,000 kw and 64,500 kva.

The shaft sealing system has been in continuous operation since April 1939, or for a total of 172 days (as of September 30, 1939). A purity of approximately 98 per cent is being maintained. At one-half pound per square inch gas pressure, the average daily leakage for a 100-day period with the machine running, from June to September 1939, was 54 cubic feet.

Since a portion of the oil for the seal system is drawn continually from the main lubricating oil tank, the vacuum-treating system for the seal oil is in effect a drying system for all the oil used by the turbogenerator. It is found, both at Windsor and Logan, that approximately one pint of water is extracted daily by the seal oil vacuum pumps.

After seven months' operation at Windsor, measurements were made to determine the amount of moisture in the hydrogen inside the generator. It was found that a typical sample of the cooled gas contained 5.5 milligrams of water per liter of gas. This corresponds to a humidity of 23 per cent at the temperature and pressure existing inside the machine at the point of sampling. No water has been collected in the leakage device at the bottom of the generator indicating that the humidity of the hydrogen is being held below the dew point. As long as any moisture is present in the lubricating oil or in the gas in the generator, it is evident that moisture will be released or absorbed by the oil at the shaft seals in a manner similar to that which causes air to be liberated or carried away at the seals. Therefore, the magnitude of the vacuum treatment and the rate of flow of the oil to the seals largely determine not only the hydrogen purity but also its humidity. Further determinations of moisture content in the gas are being made both at Windsor and Logan.

Philip Sporn and **F. M. Porter:** The general acceptance of hydrogen cooling for generators in the past few years has surprised and puzzled many engineers and operating men, to whom this modern type of cooling had just recently been introduced. The operating experience of the authors demonstrates that it is really a logical step following upon years of successful operation of many hydrogen-ventilated synchronous condensers. Quite a few papers concerning hydrogen applications have been presented, giving the results of tests, studies, and designs made by the manufacturers during the past 20 years, but little has been given regarding the practical application of hydrogen cooling. The authors have endeavored to fill this gap by presenting a fair picture, both detailed and general, of their experience with 12 machines running under all sorts of conditions.

Mr. Wieseman states that the elimination of the use of scavenging gas in the case of synchronous condensers shows that the fear of explosion has definitely diminished since the Turner unit was placed in service 11 years ago. This is undoubtedly true. Even in the case of generators the authors would not hesitate to change from air to hydrogen or vice versa without employing an inert gas, if the machine were stationary during the gassing operations. It is possible that in the future the scavenging procedures may be eliminated on all hydrogen-cooled machines except in cases where it is necessary to keep the generator in service when changing over from hydrogen to air. Mr. Wieseman agrees with the authors that the hydrogen-purity indicator, as far as condensers are concerned, is not an essential item and therefore may well be eliminated. As a matter of fact, a condenser is now being built for the Indiana General Service Company from which all devices for continuously measuring gas purity are being eliminated. A portable indicator will be employed to determine purity when gassing or degassing the machine.

Mr. Henningsen mentions that considering the state of the welding art some ten years ago, it is remarkable that the first machines hold gas as well as they do. Most of the leakage on the Turner unit during high-pressure tests was through the welds. An interesting comparison of approximate weekly gas leakage at 15 pounds per square inch on the following three units incidentally demonstrates great progress in the welding art:

Condenser	Built	Cubic Feet Per Week
Turner	1928	18,480
Scarbro	1930	1,000
Fostoria	1936	12

Mr. Henningsen believes that in table III the figure of 29.9 per cent given for outages due to construction for hydrogen is overly pessimistic. Actual work on bearings and other parts inside the machine has not been charged to hydrogen construction but the time required for removing gas and refilling is included. Inspections of air-cooled units can be carried out for the most part without shutting down the machine. It is possible that in all fairness the percentage above mentioned should be reduced slightly but the authors have felt it better to charge any questionable items against gas operation rather than otherwise. The important fact is that the availability of these machines is remarkably high and it is expected that operating results in the future will improve these figures to higher values. Similar data on air-cooled units will be necessary in order to be able to determine which type machine is more reliable and less subject to outage.

Messrs. Ross and Sterrett point out that in the case of the New Carlisle unit the collector housing is isolated from the main shell with separate coolers provided for removing heat losses in this compartment. This arrangement is provided to prevent the carrying of carbon dust into the main shell and it is undoubtedly an improvement in some respects over previous schemes. However, it does add the complication of additional coolers to be kept clean inside and out. Considering the small heat losses in this compartment, it should be possible to design the housing so that the heat can be dissipated without the use of coolers in the compartment or circulation of gas back to the main shell. As a matter of interest, for the past two years the Scranton unit has been operated almost continuously with the collector compartment closed off from the main shell and no excessively high temperatures have resulted in this housing. It is stated that two Westinghouse condensers are now being built with no permanent purity indicating equipment. The authors will be interested in following this simplification, which is likewise being adopted by them for a condenser now under construction as previously mentioned in this discussion.

Mr. Lane's description of operating experience with the Logan and Windsor generators demonstrates that the application of hydrogen to alternators gives the same satisfactory results that it does with synchronous condensers. The tests made at Logan at pressures up to 15 pounds per square inch are extremely interesting and instructive, and have contributed to the decision to have the new machines for Atlantic City and Philo designed for continuous operation at pressures up to 15 pounds per square inch in order to obtain additional economies possible with operation at this higher gas pressure. It is expected that the gas consumption at 15 pounds, on these units will be less than that which was obtained on the Logan unit. The humidity checks at Windsor over a period of months indicate that the gas is gradually becoming dryer. These tests were recently supplemented by similar checks at Logan, in which case a relative humidity of less than five per cent was obtained. As far as the generator gas is concerned, the drying action of the seal oil has thus been demonstrated on both units.

Mr. Gilt discusses briefly the experience of the Consolidated Edison Company with two hydrogen-cooled turbogenerators. Both from the standpoints of gas consumption as well as winding temperature rises, his experience has been similar to that of the authors.

Mr. Bailey points out that the auxiliaries for operation of the generator in hydrogen are too complicated. The authors are in agreement on this point but believe that after several more years' operation, existing shaft-sealing schemes will be greatly simplified or new ones developed, which will accomplish the desired results with less equipment and accessories than are now being employed. Mr. Bailey also states that the high outage attributed to rotors and water-cooling apparatus seems unnecessary in view of the present state of design of such equipment. As the authors pointed out, the rotors of the 1,200-rpm machines were of new design and this accounts largely for the high outage time chargeable to rotor trouble. As to the water-cooling equipment, there has been practically no trouble with surface coolers or piping inside the units. The breaking of a cooler vent pipe on one machine accounts for a great part of the outage chargeable to water-cooling equipment. It is believed that loss of service due to the above two causes will be greatly reduced in future operation.

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* Refer to section IX, paragraph *B*-2 of the paper.

Mr. Mortensen states that tests which he made on a turbogenerator at various gas pressures, indicate that gas leakage varies with the square root of the gas pressure. In the case of synchronous condensers, tests made by the authors have been inconclusive but indicate that in general the leakage varies approximately with the gas pressure (above atmospheric). In the case of the tests at Logan the leakage again varied almost directly with the gas pressure (above atmospheric) with allowance being made for the approximately constant amount of gas normally absorbed by the seal oil.

The authors are grateful for the many kind remarks which have been made regarding this paper on hydrogen-cooled machinery. They are glad to have had the opportunity to present this information before the Institute, and they hope that a study of it will be of help to convince those needing light on this problem that hydrogen as a cooling medium for large electrodynamic machines offers not only many economic advantages, but, properly applied, is as safe as air or water.

Water Solution in High-Voltage Dielectric Liquids

Discussion and author's closure of paper 40-77 by F. M. Clark, presented at the AIEE winter convention, New York, N. Y., January 22-26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 433-41.

T. D. Reimers (Consolidated Edison Company of New York, Inc., New York, N. Y.): This paper presents valuable data concerning the effect of moisture in liquid dielectrics and on solid dielectrics immersed in the liquid. It is evident that the data presented open a field for further research and may lead to the introduction of field service tests other than dielectric strength as a criterion of the operating condition of liquid dielectrics.

For a further understanding of the problem it would be desirable to expand figure 1 to show water solubility of transformer oil of various acidities. I would like to ask the author if any work has been done on this and if so what the data show.

In figure 2, water-solubility curves are shown for new and used Pyranol. What characteristic of Pyranol causes the increased solubility of water in used Pyranol? What are the characteristics of the used Pyranol tested as compared to new Pyranol? Can Pyranol in service obtain characteristics which will give greater water solubility than that shown for the used Pyranol curve?

Figures 1 to 6 indicate the greater solubility of water in Pyranol as compared to mineral oils. This suggests the need for exceptional care by the manufacturer in initially drying and sealing a Pyranol transformer and by operators in maintaining the unit. It suggests further development in gasket materials and gasket cements to seal adequately Pyranol equipment for long periods since there has been some indication that these materials are not as satisfactory as those developed for oil units. Perhaps the more general use of welded covers and the corresponding elimination of gaskets may be the ultimate answer to this problem.

Figure 7 indicates that wet Pyranol gives slightly more moisture to cellulose insulation than wet oil. Figure 8 indicates that wet cellulose insulation passes less moisture to Pyranol than to oil. This condition is unfavorable to Pyranol since the operator would prefer to have the moisture in the liquid where it can be more easily detected and can be more readily removed. It would be interesting to obtain similar data on used liquids.

The author uses figure 9 to illustrate the stability of the moisture in the cellulose insulation once it has entered and cautions that the moisture absorbed by the insulation is only released again by drastic treatment which involves the application of temperatures in the range of 95 to 100 degrees centigrade and even higher. To the operator this indicates a prolonged expensive process for the removal of such moisture. Apparently, mere filtering by a fullers' earth coated blotter press at room temperatures will not suffice and one gathers that it will be necessary to use loading transformers to maintain the temperature of the winding at 95 to 100 degrees centigrade to drive the moisture out of the cellulose insulation into the liquid where it can be filtered out by the fullers' earth coated blotter press. Figure 9 is for mineral oil and it would be interesting to have a similar curve for Pyranol.

Pyranol transformer conductor insulation usually consists of layers of paper and/or cotton covering which is not varnish impregnated presumably because no suitable impregnating varnish is known which would be insoluble in Pyranol. One wonders whether the impregnating varnishes generally used in oil transformers, provide a filler for the paper and cotton so as to reduce materially the amount of water they can absorb or appreciably prolong the time required to absorb moisture. Certainly, operators have had satisfactory experience in drying out flooded oil-insulated varnish-impregnated transformers without the application of heat as high as 95 to 100 degrees centigrade or higher.

Since the introduction of Pyranol transformers without varnish impregnation, similar construction has been extended to oil transformers. Perhaps it is better to continue the use of varnish impregnation on oil units until more complete data is obtained on the operation of nonvarnish-impregnated transformers.

The author mentions that the cloud-point formation temperature is a good indication of the amount of water in fluid dielectrics. However, he does not show any correlation between the temperature at which the cloud forms and dielectric strength of the sample at room temperature so that some comparison might be made between the cloud-point test and the more usual dielectric-strength test. Such a curve may be drawn from the author's data by replotting the dielectric strength values of figures 11 and 14 for 25-degree-centigrade testing temperature against the cloud points corresponding to the water content values as obtained from the author's figures 1 and 2. By this method figure 1 of this discussion is obtained.

The dielectric-strength measurements made by Mr. Clark were made with a 0.2-inch gap spacing. Plotted on this same figure is a series of tests made by the Consolidated Edison Company on 32 field and laboratory prepared samples of noninflammable insulating fluids with various cloud points and with dielectric strength tests made with 0.1-inch gap spacing. The curve obtained with the conventional 0.1-inch gap has a pronounced break not evidenced in the 0.2-inch gap curve. Admittedly more samples should be tested with the 0.1-inch gap to be certain that this curve is representative but the data indicate that further investigation should be made. It is to be noted that the change in dielectric strength with the 0.1-inch gap in the range of 35 kv to 13 kv is so rapid for relatively small changes in cloud point that any test value in this range indicates doubtful condition of the fluid. This has been confirmed by the variations obtained in successive field tests when the dielectric strength is in this range. In the past, dielectric strength values of 20 kv or better have been indicative of satisfactory insulating-fluid condition. These data would indicate that values of 35 kv or better should be used for Pyranol. No data have been accumulated for a similar transformer-

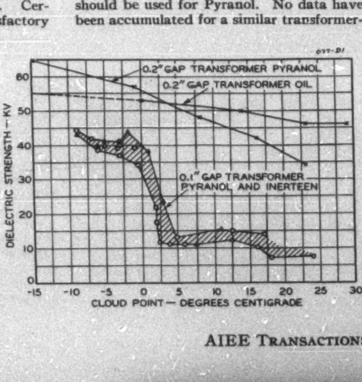


Figure 1. Dielectric strength-cloud point characteristics

X—Data from F. M. Clark's paper

△, □, ○—Data from Consolidated Edison tests

△—Field samples of Inerteen

□—Field samples of Pyranol

○—Laboratory-prepared samples of Pyranol and moisture oil curve but past experience would indicate that it does not have a similar sharp break above 20 kv.

Mr. Clark does not state what values of cloud point or dielectric strength should be considered satisfactory in service; and it is necessary to establish operating limits for these values.

Eric A. Walker (Tufts College, Medford, Mass.): In this paper Mr. Clark presents a tremendous amount of data from which all the possible conclusions are not immediately apparent. One further important conclusion might be arrived at from the data of figures 14, 17, and 18. In these figures Mr. Clark plots the dielectric strength of mineral transformer oil and transformer Pyranol as a function of temperature for various amounts of moisture. On these curves certain points are spotted with a *C*, indicating that by the presence of a cloud that some of the moisture was in suspension and some in solution. Now, because these points fall on the indicated curves it may be concluded that the dielectric strength of these two fluids is a function of the total amount of moisture present regardless of whether that moisture is in solution or in suspension. In other words, moisture in suspension is no more harmful than water in solution. Does Mr. Clark concur with this interpretation of his data?

Another surprising result of this investigation is the extreme slowness with which dry oil absorbs moisture from humid air. However, the author makes it quite clear that the test was carried out in a quiescent state. Very obviously this condition was chosen for experimental simplicity. Is it not possible that in a quiescent condition a thin layer of relatively dry air and a thin layer of relatively moist oil exists at the air-oil boundary and that the apparent slowness with which the moisture is absorbed is due to the slowness with which the moisture diffuses through the oil? It would be interesting to measure the effect of agitation or of pouring the oil over a thin lip as in passing it from one beaker to another.

Perhaps Mr. Clark might say a few words on why he chose the given method of water determination rather than other tests such as the Rodman or Grignard test. He might also comment on how accurate he believes his determination of moisture to be and also on the length of time required to test a sample.

Frank C. Doble (Doble Engineering Company, Boston, Mass.): Mr. Clark's interesting paper is a masterpiece in the preparation of curves from voluminous data that represent enormous man-hour efforts. The opening paragraphs of the paper give an accurate description of a very important problem exactly in the terms that have been recognized by the Doble client group for several years. The following comments about the rest of the paper appear to be in order:

Preceding the description of the water-extraction method, the subheading appears to be incorrect or misleading. Note that it reads: "A Laboratory Test for Quantitatively Determining the Amount of *Dissolved* Water in Insulating Oils." Apparently the method does not differentiate dissolved water from entrained or suspended water because inherently the apparatus measures the total of all kinds of water indiscriminately. Moreover, chemical authorities tell us that there is no known single method that will measure and differentiate dissolved water from suspended water in oil. Apparently the author tries to overcome this difficulty by relying upon the visible cloud test to determine saturation points on the assumption that invisible water must be in solution. Chemical authorities maintain that this is not a safe assumption because it is impossible physically to separate water in suspension and water in solution or to be sure that either form is present without the other.

"Solution" and "suspension" of water in oil are different physical states and may or may not be related directly to electrical characteristics.

We believe that a dielectric-loss measurement of oil may become a more practical criterion of its serviceability because it has a direct relation to electrical characteristics. Our use of a direct-reading meter type of dielectric-loss apparatus has allowed us to study transient conditions in the changes from water in solution to water in suspension. The direct-reading method gives indications of changes in form of water in oil which take place so rapidly that they cannot be followed by bridge measurements.

The amount of water in oil, whether in solution or suspension, is of great interest and the Doble group is working on the problem of its measurement with a method different from the one described by Mr. Clark. It is our hope that it may develop that the tolerance of oil for water in some form eventually may be a valuable criterion of its life condition from a deterioration standpoint. However, at the present time the operator of an electric-power system that is dependent upon oil as an important insulation is first concerned with the possible direct or potential hazard to operation due to the water, whatever its form may be.

From the standpoint of general utility, there is a need for some standard other than water in solution or water in suspension in oil since these two states are so difficult to differentiate and in view of the fact that both types of water may impair the electrical characteristics of the oil. This fact is emphasized by the author when he shows that the breakdown value of oil is lowered by the presence of water in solution, as well as by water in suspension.

As a supplementary standard, I would like to ask Mr. Clark why we should not give consideration to dielectric-loss measurements as criteria for good oil or for bad oil due to water whether it be in solution or in suspension. For this purpose, the terms "low loss" water and "high loss" water were suggested by me in an informal talk before the *D*-9 committee of ASTM at Atlantic City in June 1938, and again in a formal paper before the National Research Council meeting at Harvard University on November 2, 1939.

It is recognized that the dielectric loss of a sample of oil may include the effects of colloidal solution of varnishes and the products of oxidation, as well as water. However, none of these contaminants should be present in good oil. A "low loss" oil may, therefore, be considered suitable for service; a "high loss" oil obviously should be investigated to determine the nature and seriousness of its contamination.

Tentative standards for grading oil as to its serviceability by the dielectric-loss method have been in use by the Doble client group for a number of years and found to be of practical value to the operating companies.

We believe the difficulties that have been experienced in correlating dielectric loss measurements of oil with its dielectric breakdown strength and the presence of contaminants have been largely due to the presence of water in unknown quantities and unknown form. The development of a practical method for measuring the water content of oil should, therefore, facilitate further correlation of dielectric-loss measurements with the serviceability of oil.

For several years, as referred to above, the Doble company and four other laboratories have been collaborating on a general study of the oil problem from the power-company operating standpoint.

Mr. Clark and his associates have made a valuable contribution in this paper. We believe that the study of the problem should have the support of all interested parties, including oil producers, equipment manufacturers, and power-company users.

Since the work done by the Doble group, using a method different from the one described by Mr. Clark, has given results which vary somewhat from the data shown in his paper, may I suggest a symposium on the subject to be presented before the AIEE at the 1941 winter convention.

H. C. Louis (Consolidated Gas Electric Light and Power Company of Baltimore, Baltimore, Md.): The paper by F. M. Clark presents data of practical value for field as well as laboratory purposes. The indirect actions and effects in the transfer of moisture through oil to transformer insulation have not been as well understood or controlled as the more direct effects.

One special condition sometimes encountered with spare transformers on the Baltimore system is of interest in connection with this. Spare transformers of the air-breather type, after long periods of idleness, frequently show definite signs of moisture in the insulation, although the oil may show apparently excellent dielectric qualities. Such transformers are usually very difficult to dry out by ordinary methods. This indicates the advisability of occasionally exciting or loading spare transformers to avoid this condition.

A. E. Flowers (The De Laval Separator Company, Poughkeepsie, N. Y.): About 12 years ago the De Laval Separator Company made a study of the possibility of water being dissolved in oil.

Observation had shown sometimes that oil which had been centrifugally purified and had shown no visible evidence of water after purification and gave a high electric breakdown resistance, would a day or so later show low breakdown and cloud, or other evidence of water being present. Our investigation showed that water was truly dissolved in all oils, in amounts that increased markedly and increasingly with temperature, particularly at temperatures above 40 to 50 degrees centigrade.

We found further that on cooling, this water precipitated out, showing first a cloud and later droplets of water, which finally settled to the bottom of the container and during this precipitation a very low electric breakdown resulted.

We have since the time of that investigation urged users of our insulating-oil purifiers to carry out the separation at low or room temperatures.

We described this work in reports to the committee on insulating materials of the American Society for Testing Materials and in a brief paper in *Power*, February 16, 1932, "Solubility of Water in Mineral Oils" which doubtless escaped Mr. Clark's attention since it is not mentioned in the references.

Our laboratory has employed two types of tests in the measurement of the quantity of water present in insulating or other oils. One is the vacuum-distillation method and absorption and weighing in phosphorus pentoxide bulbs, substantially as described in Rodman's paper in the *Journal of Industrial and Engineering Chemistry* volume 13, December 1921, page 1149, "The Determination of Moisture in Insulating Oils".

The other method is by the use of a special high-speed test-tube centrifuge using a special type of glass test tube whose lower end is so shaped that volumes of one cubic millimeter may be read at the bottom tip, corresponding to 1/1,000 per cent or ten parts per million in a 100-cubic-centimeter sample. As this De Laval test-tube centrifuge runs at 6,000 rpm and the tube tips swing in a 20-inch circle, giving a tip force of 10,000 times gravity, a rapid and complete removal of all *free* water results.

Insulating oil from new drums or tank cars will have in solution an amount of water, depending upon the temperature, atmospheric humidity, and time of exposure. Equilibrium of large bodies of oil, such as in tank cars or large transformers may take several days, while exposure in a thin film on a watch glass may require only a few minutes. As the average atmospheric humidity is less than 100 per cent, and runs from 30 to 70 per cent such oil is generally *not saturated.*

For our investigation we carefully saturated the oil by vigorously mixing a large proportion of water to oil for periods up to 30 minutes, at or above the desired test temperature, then thoroughly centrifuged all free water out at this same temperature and decanted the *free water-free* oil to the vacuum-extraction bulb or into another test tube.

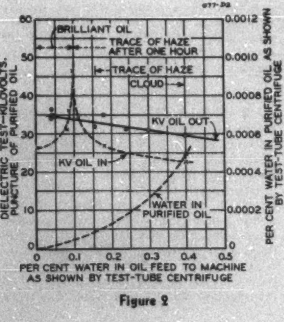


Figure 2

The procedure when using the test-tube centrifuge throughout was very simple, for if the oil is saturated at say 90 degrees centigrade then dropping the temperature in stages of 20 degrees centigrade each and holding at each level for half an hour or more, sufficient to bring about complete precipitation, allows the progressive measurement of the volume that had been dissolved in the oil at each temperature interval. An extension of this procedure below room temperature is quite feasible by refrigerant cooling of the machine and sample.

One advantage of the test tube is that it makes possible a clear distinction between *free* water and *solution* water. This is particularly important as electrical breakdown seems to be affected only by water in the *free* state. Samples can thus be prepared to enable studies of the effects of water in each state, that is, no water, dissolved water only, free plus dissolved water.

It is unfortunate that the author did not use the standard gap for electric breakdown tests nor add the breakdown values using the old standard of one-half-inch spheres separated 0.150 inch which is universally used abroad. Our laboratory has found quite erratic results with the present standard gap of one-inch disks spaced 0.100 inch when testing insulating oil with known quantities of *free* water.

Illustrative of one set of electric breakdown values, figure 2 of this discussion shows one set of such breakdown values. Another set of tests would show a generally similar shape but not identical values, as the breakdown is affected by the state of subdivision of the water and by traces of accessory contaminants that do not seem to be paper or other cellulose materials.

This whole question of the method of test for electrical breakdown is badly in need of being restudied by the Institute as well as the ASTM committee on electric breakdown.

The chemical method employed by the author is interesting and the results obtained by it should be correlated and compared with those obtained by other chemical methods such as those by

Evans, "Determination of Water in Paper-Insulated Cables and Insulating Oil," *Industrial and Engineering Chemistry*, October 15, 1939.

Larsen, *Industrial and Engineering Chemistry*, analytical edition, volume 10, 1938, pages 195–8.

Kipp, dissertation, Pennsylvania State College, 1939.

There are a number of pitfalls and difficulties in each of these methods and the volatility and reactiveness of some of the very light hydrocarbons that can come off from the best refined new insulating oils, leads to some question of these results. This is vividly brought out by the results reported by Doctor Kipp, where he found 0.03 per cent water in the most carefully prepared and carefully dried chemically pure benzene (C_6H_6).

The author is requested to add to his excellent paper an explanation of why he used the 200-mil 1½-inch-diameter disk gap; to describe how the samples were saturated and whether numerical figures could be given to the points on the curves marked *C* for the free water present.

F. M. Clark: Mr. Reimers asks concerning the relation between the solubility of water in transformer oil of varying acidity. The acidity of the oil, produced by oxidation, increases the ease of water emulsion in the oil. In general, it can be said that increased oxidation produces increased acidity and an increase in the solubility of water in the oil. To date, however, no definite relation between the oil acidity value and the water solubility in the oil has been able to be established.

Figure 2 must not be considered as a definite indication that Pyranol shows an increased ability to dissolve water as a result of commercial use. The difficulties inherent in this type of investigation caution against too fine a distinction between curves such as are shown in figure 2. In the use of Pyranol, it is desirable to eliminate all possibility of contamination by mineral oil or other soluble, oxidizable contaminants. The presence of such compounds in the Pyranol is reflected by increased acidity values and affects not only the fireproofing properties of the Pyranol but when present in sufficient quantity will lead to an increase in the ability of the Pyranol to dissolve water.

When it comes to the question of the drying of transformers in the field, it must be clearly recognized that it is necessary to carefully define the limits of the discussion. It may be that Mr. Reimers does not find it necessary to dry his flooded transformers at temperatures as high as 95–100 degrees centigrade. It is also undoubtedly true that he does not approach the degree of dryness which is originally obtained in the factory or which is under discussion in the data of figure 9. As stated in the paper, it is only when the moisture is "present in abnormally large amounts" that heating at temperatures below 95–100 degrees centigrade will be effective and then only to the degree that the "abnormally large amount" is present.

In figure 1, Mr. Reimers relates the dielectric strength of the noninflammable liquids with the cloud point and observes that there is a sharp "break" in the dielectric strength corresponding to a cloud point of 0 degrees centigrade and a dielectric strength of about 35 to 40 kv. This Mr. Reimers believes, to be different from the behavior of mineral oil and to warrant consideration of the application of more severe dielectric standards to the noninflammable liquids. This is of fundamental importance and warrants close inspection.

The measurement of cloud point is the degree centigrade. In discussing the cloud point and its correlation with the dielectric strength as Mr. Reimers has done, it must always be borne in mind that a change in the cloud point of one degree centigrade represents an increasing amount of dissolved water as the cloud-point temperature increases. This is immediately obvious from an inspection of figures 1 and 2 of the paper. This and the fact that the cloud point observed in commercial liquids may represent soluble products other than water, compels the exercise of great care in a technical inspection of such data.

In figure 1, Mr. Reimers shows a nonlinear S-shaped curve, relating the cloud and dielectric strength values for the noninflammable liquids. As indicated by Mr. Reimers, the data of the paper under discussion show a linear relation for both oil and Pyranol. This difference in behavior is attributable to the presence of secondary impurities, the effect of which is recognized to be of major importance in dielectric strength studies involving wet liquids. Of chief importance among such secondary impurities are cellulosic fibers which absorb moisture from the dielectric liquid with disastrous effect on its dielectric strength. For liquids in a condition approaching the completely dry state, the presence of these secondary impurities prevents the realization of the inherently high dielectric strength normally associated with the dry liquid, especially where the test gap used is only one-tenth inch. This gives an apparent maximum in the dielectric strength relation, as shown by Mr. Reimers. With increasing water content, the effect of water assumes increasing importance and ultimately the combined effect of the secondary impurities and the water results in a rapid fall in the dielectric strength. This Mr. Reimers has indicated would be at a dissolved water content corresponding to approximately 0 degrees centigrade cloud point (40 parts per million for Pyranol).

In the data of the paper under discussion, the presence of secondary impurities has been avoided. The relations established describe the primary effect of dissolved and suspended water on the dielectric strength of the liquid. At 25 degrees centigrade, the effect of increasing water content gives a linear decrease in the dielectric strength of the liquid. If however, fibers are incorporated in the wet liquid, the dielectric strength falls precipitously when the cloud point of the liquid is increased from −10 to +10 degrees centigrade. This is illustrated in figure 3 of this discussion. In obtaining the data on which figure 3 is based, the Pyranol is stirred with wet linen tape, thereby incorporating wet fibers in the wet liquid. The dielectric strength-cloud point relation of Pyranol treated in this manner closely duplicates the data submitted by Mr. Reimers. When, however, this same wet-fiber-containing Pyranol is filtered through a sintered glass filter, thereby removing the wet fibers, the dielectric strength of the wet-fiber-free liquid falls more slowly and in a linear relation to its cloud point, greatly resembling the relation presented in the data of the paper under discussion. A similar effect is illustrated in the relation illustrated on figure 4 of this discussion which describes the measured water content of the Pyranol before and after glass filtration. Tests on mineral transformer oil, similarly treated, have yielded similar results and relationships. From such data it is concluded that the relation presented by Mr. Reimers does not describe the effect of water, per se, on the dielectric strength of the Pyranol but describes the combined effect of water and wet secondary impurities. Since the consideration of the effect of secondary impurities is beyond the scope of the present paper, complete discussion is reserved for a later paper which the author hopes to present to the Institute.

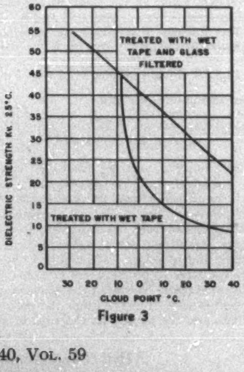


Figure 3

Complete consideration as to whether water in suspension is more harmful than water in solution involves many factors. Figures 14, 17, and 18 referred to by Mr. Walker indicate the effect of the total water present on the dielectric strength. Increased water content leads to decreased dielectric strength Since water in suspension is normally indicative of the presence of larger total amounts of water than are present in clear liquids, suspended water in insulating liquids can only be considered as presenting a more dangerous condition.

The method of test described was selected for use in order to avoid the many pitfalls inherent in a test such as that described by Rodman. The accuracy of the test method selected is approximately plus or minus 2 parts per million at a water content of 30 parts per million. The length of testing time and other details are given in the paper.

Mr. Doble draws attention to the well-known fact that solutions may consist of true solutions or colloidal solutions. In the paper the terms water in solution or suspension are used in the general sense. Water in suspension is distinguished from the general class of water in solution on the basis of visibility and ability to settle under gravity. No distinction is drawn between water in true or colloidal solution.

Mr. Doble is correct in his statement that the operator of an electric-power system that is dependent on oil is concerned with the direct and potential hazard to operation due to water whatever its form may be. In the paper under discussion, therefore, only the total water present and its effect on the dielectric properties of the liquid have been considered. Good dielectric strength has always been looked upon as an indispensable requirement. Recently the dielectric loss has been suggested as of equal importance. Mr. Doble's belief that this type of measurement may become a more practical criterion of oil serviceability is worthy of consideration. Whether this gives justification for a distinction between two dielectric types of water as suggested by him may be seriously questioned when the whole question of the water hazard is reviewed with respect to its effect on the oil and its effect on the cellulosic insulation.

Doctor Flowers asks why a 0.2-inch test gap was used in place of the more common 0.1-inch gap. The 0.2-inch gap was used merely to reduce the possibility of a complete bridging of the electrode gap by a single water droplet held in liquid suspension. It was believed that the use of a larger test gap would allow a more reliable study of the effect of water on the dielectric strength of the liquids.

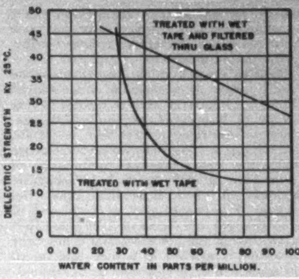


Figure 4

Determination of Short-Circuit Torques in Turbine Generators by Test

Discussion and authors' closure of paper 40-78 by E. C. Whitney and H. E. Criner, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 885–9.

S. B. Crary (General Electric Company, Schenectady, N. Y.): This paper is a valuable contribution in two respects. First, the instantaneous calculated short-circuit torques for three types of faults are confirmed within practical accuracy by actual test. Second, equations are developed for the unidirectional torques which influence the machine and system stability characteristics.

The torques developed by terminal short circuits as shown in the paper result in high instantaneous values of fundamental-frequency and twice normal-frequency torques. In some respects this makes them less severe than if they were at a lower frequency, depending of course on the physical construction of the machine and foundation under consideration.

If a machine is accidentally synchronized considerably out of phase to a system which has electrically a lower reactance than the machines being synchronized, the fundamental and twice normal frequency torques may be larger than that for a terminal short circuit and the low-frequency or unidirectional component may have a magnitude approaching as a limit the maximum instantaneous value of torque for a terminal short circuit. The amount of this high unidirectional torque due to bad synchronizing which is transmitted through the shaft is a function of the relative inertias of the generator rotor and the driving machine. The shaft torque would be higher in times normal for a frequency changer set and a condensing-steam-turbine-driven generator than for a water-wheel or noncondensing-steam-driven generator.

Another possible cause for high unidirectional torque is a terminal short circuit through a relatively high resistance having a value of the order of magnitude of the machine subtransient or transient reactance. For the case of a high-resistance three-phase fault, the high-frequency components of torque are quickly damped and may be critically damped depending upon the amount of resistance and, as is the case of synchronizing out of phase, the maximum magnitude of the unidirectional component of torque is reached in a time interval of the order of a cycle or less of the fundamental electrical frequency.

A third cause for high torques of low frequency, although in general of less magnitude than that for extremely bad synchronizing, and for a high-resistance fault, are the torque swings due to system faults, switching, and possible running out of synchronism. Running out of synchronism may be particularly serious if the relatively low-frequency torque pulsation so produced approaches the natural frequency of any of the mechanical parts of the machine.

Accordingly, in order to relieve the stress on an electrical machine, particularly with a relatively large driving machine WR^2, it is advisable to insure against bad synchronizing and of running out of synchronism.

The equations which the authors have presented are applicable only to low-resistance faults. This is not a criticism, for to include completely the effect of resistance is necessarily difficult. However, there appears to be some advantage, if only to confirm the physical understanding of this effect, to consider some of the additional effects produced by resistance. If this is done considering constant rotor flux linkages, to show the effect of armature-circuit resistance, the expression for the torque resulting from a three-phase short-circuit, assuming $x_d''=x_q''$, is,

$$T=\frac{re_0^2}{x_d''^2+r^2}-\frac{re_0^2\epsilon^{-\frac{r}{x_d''}t}}{x_d''^2+r^2}+\frac{x_d''e_0^2\epsilon^{-\frac{r}{x_d''}t}}{x_d''^2+r^2}\sin t$$

This expression shows that the unidirectional component of torque due to armature circuit resistance increases at a rate determined by the armature-circuit time constant, that the maximum magnitude of unidirectional torque is obtained when $x=r$, and also the manner in which the fundamental-frequency component of torque is affected by armature-circuit resistance.

The authors show in their equations a torque component, that due to the rotor resistance caused by the d-c component of armature current, which is ordinarily neglected in the usual stability study. This effect is of greatest importance for a solid fault at the terminals of a solid-rotor low-reactance machine and should not be neglected for this case. Otherwise if the fault is on the high-voltage system, the rate of decay of the d-c component of short-circuit current is usually sufficiently rapid to render this component of torque unimportant. For water-wheel-driven generators the effect is small even for terminal faults.

From a stability standpoint the three-phase and double-line-to-ground faults are the most severe, the line-to-ground being comparatively insevere and the line-to-line fault usually involves ground. Accordingly, it would seem worth while if the authors would include in their table I the expression for the torque produced by the double-line-to-ground fault as well.

It would be of interest to know if the authors were able to determine the value of R_2 for the machine they tested and how well it compared with their formula

$$R_2=\sqrt{2}R_{rs}+R_1$$

Also whether they were able to confirm by their tests that the rotor-loss component of torque varies as the 1.8 power of the current. In this respect, it would be of interest if the authors could show the numerical equations for the calculated torques presented in the paper so that the relative importance of the different components of torque could be readily evaluated.

Ernst Weber (Polytechnic Institute of Brooklyn, Brooklyn, N. Y.): The authors claim that "as far as is known the test results of this paper constitute the first experimental check . . . as applied to a turbine generator." I should like to point to the treatise by H. Rikli on, "An Experimental Investigation of the Sudden Short Circuit," published in the *Bulletin* des Schweizer Elektrotechnischen Vereins (of the Swiss Electrical Association), volume 16, 1925, page 217.

In that paper, oscillograms of currents, voltages, and speed are reproduced for various short-circuit conditions of a turbine generator for 2,500 kw at 3,000 rpm. The speed oscillograms are rather clear and permit, by differentiation, the evaluation of the torque variation. True, the accuracy in finding the slope of oscillograms is not as high as desirable, but the accelerometer utilized by the authors has a most annoying natural frequency which in turn impairs the accuracy of their results.

It might also be of value to refer to the series of books, "Electrical Machines", by R. Richter (successor to E. Arnold), published by J. Springer, Berlin. Volume 2 (1930) of this series devotes about 500 pages to synchronous machines, of which 74 pages deal with short-circuit phenomena.

Finally, would it not be more accurate to refer to all the formulas used by the authors in computing torque values as semi-empirical? One would be somewhat relieved to know that the remarkable agreement in certain cases between calculation and test is due to a compromise between theory and experience, rather than a unique feat of theory alone. After all, as the authors admit themselves, many factors have been disregarded from the start in formulating the expressions for the torques.

H. D. Taylor (General Electric Company, Schenectady, N. Y.): The authors' experimental check of the theoretical equations for short-circuit torques in turbine generators is a very interesting and valuable piece of work. The confirmation of the analytical work of Nickle, Pierce, and Henderson on the *alternating components* of torque is particularly gratifying. With regard to the unidirectional or *loss components*, it appears that in the absence of considerable external resistance, these are generally not large enough to have much effect on the torque peaks. If I have interpreted the authors' numerical results correctly, the loss components apparently add about 12 to 15 per cent to the peak torque values for three-phase and line-to-line short circuits on their 12,500-kva machine. In order to bring out the relative size of the various components, I would like to suggest that the authors give the numerical values of 60-cycle, 120-cycle, and loss components, as shown by their equations, for the cases given in table II.

E. C. Whitney and H. E. Criner: The authors wish to thank the various discussers for their very pertinent and interesting discussions.

In line with several suggestions concerning table I, the authors have revised the table to include a calculation for the double-line-to-ground fault. The table as printed in the paper is the revised edition which includes several corrective suggestions made by S. B. Crary and C. Concordia which did not affect the calculations of the tests shown in the paper as well as the torque calculation for a double-line-to-neutral fault. In the double-line-to-neutral fault several items in the nomenclature were added as shown in the table. X_1, X_2, and X_0 are used to indicate the total reactance in the positive, negative, and zero sequence networks respectively. Also there are several terms which involve larger approximations than in the other formulas due to the difficulty in determining the d-c component for this type of short circuit.

With respect to the comment concerning the factor R_{rs}, it is probably true that from the purely theoretical standpoint R_{rs} should be more nearly equal $\sqrt{2}(R_2-R_1)$ than $(R_2-R_1)/\sqrt{2}$. However, this portion of the unidirectional torque is a fairly large percentage and it would appear from the tests that the value given in the paper will yield better results and in this sense must be considered somewhat empirical. Since it was impossible in the time allotted to test R_2 accurately, this presents an additional possibility for error from this source.

To compare the relative magnitude of the various components of torque the per-unit torque coefficients of the various terms are shown below for the machine in question. When decay factors F and A for specific times are applied the results will add up to the values given at the peak of the calculated torque curves of figures 5 and 6. For the test machine $R_1=0.0064$ and $R_2=0.036$.

Three phase case:

$$T=11.5FA\sin\omega t+.85F^2+1.7A^{1.8}$$

For the line-to-line case:

$$T=11.5FA\sin\omega t-5.75\sin 2\omega t+1.77\times(1-\cos 2\omega t)F^2+1.69A^{1.8}+2.2F^{1.8}$$

The electrical torques resulting from a line-to-neutral and double-line-to-ground short circuit will always be smaller than the three-phase and line-to-line short circuits.

Mention has been made of the electrical torques resulting from synchronizing a machine out of phase. This subject is entirely too large to be discussed here as it depends on the relative size of the machine and the system as well as the angle between the machine and the system at the time of synchronizing. Also the natural frequency of oscillation of the machine with the system must be considered. However, as a limit of the rated-frequency torque pulsation, consider the case of synchronizing 180 degrees out of phase with an infinite system. This case resolves into the equivalent of a three-phase short circuit from twice rated voltage. This is, of course, a condition of very, very bad operation and should never be permitted to exist since dire results are possible.

With reference to the earlier paper presenting experimental data on the subject of short-circuit torques, the authors are sorry that they missed the earlier Swiss paper. However, upon reviewing this paper it appears it would be very difficult to differentiate the speed curves to get the torque accurately in the first place, and in the second place, the construction of European turbine generators of that vintage differs considerably from present-day American machines which partially nullifies the benefits of the test. In the third place this paper was written before any of the present-day reactance concepts were invented and thus there is little possibility of correlating the information with modern data. With respect to the textbook by Richter, there is practically nothing presented dealing specifically with this problem except part of the theoretical background of this paper.

While it is true that this paper necessarily presents some empirical data most of the factors involved were derived directly from theoretical consideration. All work done previously applying to turbine generators, except the work mentioned by Professor Weber, was entirely theoretical in character.

Basic Impulse Insulation Levels

Discussion and authors' closure of paper 40-83 by Philip Sporn and C. A. Powel, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (October section) pages 596–8.

R. T. Henry: See discussion, page 1201.

J. T. Lusignan, Jr.: See discussion, page 1205.

V. M. Montsinger: See discussion, page 1202.

J. E. Clem (General Electric Company, Schenectady, N. Y.): This report is another step in the advance of the industry toward the establishment of a uniform and orderly array of insulation levels to be used for the application of electrical equipment. Because the report affects all the major lines of equipment, it should be considered very, very carefully before the values suggested are definitely adopted.

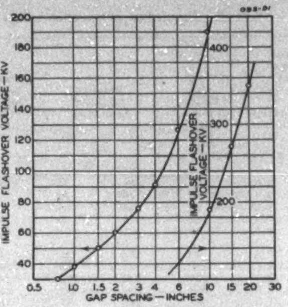


Figure 1. Impulse flashover, rod gaps

The report states: "Some of the porcelain insulators in bus and switch assemblies do not meet the basic impulse level proposed for the system voltage on which they are commonly used". This is rather disconcerting because the flashover data available a few years ago indicated that these same insulators were comfortably above the insulation levels being considered at that time and even above those proposed in the report. Accordingly, I have retraced some of the steps in the development of the proposed levels in an effort to discover the cause of the difficulty in which certain bus insulators now find themselves.

The initial report of the committee on basic insulation levels was adopted in January 1937 and was presented at the 1937 summer convention, and appears on page 711 of the June 1937 issue of ELECTRICAL ENGINEERING. In table I, "Basic Impulse Insulation Levels", a set of impulse insulation values, together with the corresponding gap spacing in inches, are given for the various voltage classifications. In the footnote the following statement appears: "Gap spacings are those giving 50 per cent flashover and 50 per cent full wave when subjected to 1.5x40 positive impulses with crest values given in column 2 under standard conditions of air density and humidity". With but one exception (345 kv, 106 inches instead of 105.6 inches), the gap spacings given are identical with the test gap for transformers appearing in table I, page 750, of the same issue of ELECTRICAL ENGINEERING.

Since we had gaps with us for impulse testing before we had kilovolts, it seems reasonable to recheck the kilovolt values corresponding to the given gap spacings. In the same issue of ELECTRICAL ENGINEERING, on page 712, there is a report of the laboratory committee, "Flashover Characteristics of Rod Gaps and Insulators". Table II of this report, page 713, gives average impulse flashover voltages for rod gap spacings.

Table I. Comparison of Flashover Values

Gap Spacing	Flashover Voltage*	Flashover Voltage†
20	350	346
30	505	504
40	650	656
50	800	807
60	945	956
70	1,095	1,103
80	1,240	1,297
90	1,385	1,390
100	1,530	1,536

* Values from table II, page 713, ELECTRICAL ENGINEERING, June 1937.

† Read from curve corresponding to empirical expression

$$\text{Flashover} = 182.1\left(\frac{S}{10}\right)^{0.926}$$

The data in table II of the report, for gap spacings up to 20 inches, were plotted on semilogarithmic paper, using the minimum values (for obvious reasons) over the unstable range. This gives a very smooth curve, as indicated in figure 1 of this discussion. Above this point the curve is a straight line on log-log paper and the values correspond very closely to those calculated from the empirical equation given in table I of this discussion, which table also compares the tabular values with the empirical values. Using the curve in figure 1, and the empirical equation in table I, the average flashover values for the various test gaps, or gap spacings, have been determined and are listed in column 6 of table II of this discussion.

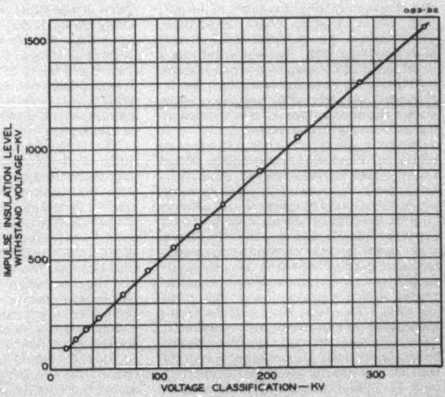


Figure 2. Required insulation level

Table II. Comparison of Insulation Levels

			Distribution Transformer Test Gap			Required Insulation Level (Kv)					Standard Insulation Levels (Kv)		
Insulation Class (1)	Gap Spacing (Inches) (2)	B.I.L. 1937 Kv (3)	Flash-over Kv (4)	Spacing (Inches) (5)	Test Gap Flash-over Kv (6)	(7)	(8)	Power Transformer Test Kv (9)	Proposed B.I.L. Kv (10)	Proposed B.I.L. Kv (11)	(12)	(13)	(14)
0.6										30			
1.2	0.8	32	32	0.8	32	29	30	60		45	60	30	
2.5	1.6	53	53	1.6	53	49	50			60		45	30
5	2.2	63	63	2.2	63	58	60	76	75	75	75	60	45
7.5/8.7	3.3	80	80	3.3	80	74	76	95	95	95	95	75	60
15	4.5	100	100	4.5	99	91	94	112	110	115	115	95	75
23/25	7.1	150	150	7.1	146	134	139	153	150	145	145		
34.5	10.2	190	190	10.2	190	175	180	195	200	190	190		
46	13.5	250	250	13.5	247	227	235	245	250	240	240		
69	20.6	360	360	20.6	356	327	338	345	350	345	345		
92	27.5	470	470	27.5	481	442	447	445	450	450	450		
115	34.5	570	570	34.7	576	530	547	540	550	550	550		
138	42.1	680	680	42.1	689	634	654	645	650	650	650		
161	49.0	790	790	49.0	792	729	752	750	750	750	750		
196	60.0	950	950	60.0	956	880	908	900	900	900	900		
230	70.4	1,100	1,100	70.4	1,110	1,022	1,054	1,050	1,050	1,050	1,050		
287.5	88.0	1,360	1,360	88.0	1,362	1,253	1,295	1,290	1,300	1,300	1,300		
345	106.0	1,620	1,620	105.6	1,617	1,488	1,537	1,540	1,550	1,550	1,550		

1. Same as column 1, table I, page 711, June 1937, ELECTRICAL ENGINEERING report "Basic Insulation Levels", with values 0.6, 7.5, and 23 added.

2. Same as column 3, table I, page 711, June 1937, ELECTRICAL ENGINEERING.

3. Same as column 2, table I, page 711, June 1937, ELECTRICAL ENGINEERING.

2–3. Footnote to table I quoted reads "Gap spacings are those giving 50 per cent flashover and 50 per cent full wave when subjected to 1.5x40 positive impulse with crest values given in column 2 under standard conditions of air density and humidity". Actually there are average flashover values based on data in table II, page 713, June 1937, ELECTRICAL ENGINEERING report "Flashover Characteristics of Rod Gaps and Insulators", and so are subject to the same deviation of plus or minus 8 per cent.

4–5. These values taken from columns 10–11, table I, page 750, June 1937, ELECTRICAL ENGINEERING report "Insulation Strength of Transformers". Although not specifically stated these test gaps apply only to distribution transformers over part of the voltage range, because power transformers received a higher test on account of the larger bushings used.

6. Values read from figure 1 and calculated from equations in table I of this discussion. Based on data in table II, page 713, June 1937, ELECTRICAL ENGINEERING report of subcommittee on correlation of laboratory data "Flashover Characteristics of Rod Gaps and Insulators" and therefore subject to a plus or minus deviation of 8 per cent. Check on columns 3 and 4.

7. Obtained from column 6 by multiplying by 0.92 to obtain minimum values in consideration of the deviation in test results of the data on which values in column 6 are based.

8. Obtained from column 6 using multiplying factor of 0.95 on account full-wave test on transformer is 5 per cent less than test-gap flashover.

9. These values taken from table II, page 752, June 1937, ELECTRICAL ENGINEERING and are the full-wave test values for power transformers.

10. From report by Powel and Sporn with values 60 kv and below omitted.

11. Proposed value of basic insulation level based on columns 3–6–8 and curve 2 of this discussion. It is understood that the implied allocation applies only to power transformers and large outdoor circuit breakers for insulation classes 15 kv and below and 34.5 kv and below respectively.

12. Application of insulation levels to power transformers for all entries and to outdoor circuit breakers 15 kv and up.

13. Application of insulation levels to distribution transformers, instrument transformers, indoor circuit breakers.

14. Application of insulation levels to some instrument transformers and indoor circuit breakers.

Columns 1, 2, 3 of table II are taken from table I, page 711, of ELECTRICAL ENGINEERING, June 1937, of the report of the joint committee on basic impulse insulation levels. The same numerical values throughout, except one at 345 kv, appear in the report of the transformer subcommittee on "Insulation Strength of Transformers", in table I, page 750, of the same issue of ELECTRICAL ENGINEERING, see columns 4 and 5 of table II of this discussion. The numerical values in column 6 and in column 3 are so close together that the situation as it affects bus insulators is not affected by variations in the results obtained by the process of translating gap spacing to kilovolts.

In the report of the laboratory subcommittee on "Flashover Characteristics of Rod Gaps" appearing on page 712, June 1937, ELECTRICAL ENGINEERING, the deviation in the test results of impulse flashover voltages is given as plus or minus eight per cent. Since the kilovolt values of basic insulation levels in the report of the insulation co-ordination committee are based upon the average flashover voltage of a rod gap, their deviation obviously is the same as that of the gap data. Therefore, when it is desired to translate them into withstand, or minimum, values it is necessary that they be multiplied by the factor 0.92. The result of this arithmetical operation is given in column 7 of table II. The values in this column represent the minimum withstand test values justifiable from the initial report of the Joint NEMA-EEI Committee on Co-ordination of Insulation.

However, established practice for transformers permitted a full wave test at a value five per cent lower than the average values given in the initial report. Because of this established practice, the change in the average values, already published, to proposed withstand values, was made by multiplying by the factor of 0.95 and the result of this multiplication is given in column 8 of table II. It should be noted that both multiplications were made upon the values given in column 6.

The values in column 9 are taken from table II of the report of the transformer subcommittee, appearing on page 752 of the June 1937 issue of ELECTRICAL ENGINEERING. The values represent full-wave test voltages for power transformers and it should be noted that they are higher than those for distribution transformers (which are five per cent less than the values in column 4 or 3) and that the values for distribution transformers correspond to the values of the basic insulation levels proposed in 1937.

The reason the values for the power transformers are higher is as follows: Initially, one of the required impulse tests on transformers was a flashover of the bushings with an applied overvoltage of not less than a specified amount. Bushings for power transformers had already become quite well standardized and were higher than the values proposed in 1937 for basic insulation levels for voltage classifications 69 kv and below and were also higher than the flashover characteristics for bushings as used in distribution transformers. It was thought to be inadvisable to reduce the bushing flashover and, consequently, the practice of giving power transformers an impulse test higher than the basic insulation levels in those voltage classes 69 kv and below became established.

In column 10 of table II are given the proposed values appearing in the paper by Sporn and Powel. These figures are identical with those appearing in the report of the joint committee on standardization of bushings. Comparing columns 9 and 10 it is quite apparent that the proposed basic-insulation-level values for voltage classifications 69 kv and below correspond closely to existing practice for power transformers, which, in turn, was based upon existing design of bushings, and this resulted in a definite increase above the 1937 proposals in the insulation levels required for voltage classifications 23, 34.5, and 46 kv.

It is in these three voltage classifications that the insulator and air-switch group find it most difficult to accept the values proposed in the paper by Sporn and Powel. The curve in figure 2 of this discussion has been plotted from the values in column 8 and shows that these values for 15-kv voltage class and up all fall upon a smooth curve, indicating that they are all consistent with one another. Inasmuch as the three troublesome values were increased because of power-transformer practice, which should not affect the insulation level required for bus supports and air switches, it seems only reasonable to expect that these values could be modified to a value not lower than the values in column 8, so that genera' acceptance of a consistent array of basic insulation values could be adopted.

Such a compromise proposal is listed in column 11. The values in column 11 are identical with those in the report of the committee on co-ordination of insulation, with the exception of five values. It appears that the extra value (inserted because of power-transformer practice) would line up more consistently with the others if it were 115 instead of 110. The other changes are for the 23, 34.5, 46, and 69 voltage classifications, where the values have been reduced a very small amount; namely 5, 10, 10, and 5 kv respectively. The acceptance of these figures will relieve considerably the difficulties of the insulator and air-switch group and the modified values would still represent an appreciable increase on a withstand basis over the values proposed by the committee in 1937.

I wish to make a definite recommendation that the subcommittee on insulation levels reconsider its action and give careful and serious consideration to the adoption of the array of numbers listed in column 11 of table II of this discussion.

M. S. Oldacre (Commonwealth Edison Company, Chicago, Ill.): The three papers on "Basic Impulse Insulation Levels," "Co-ordination of Electrical Equipment With Basic Insulation Levels," and "Rationalization of Transmission-System Insulation Strength" should assist very materially in giving a clear understanding of insulation co-ordination that will help all in the industry to either co-ordinate or rationalize the insulation of their installations.

We now have a proposal for a series of impulse insulation values that seem entirely logical and consistent for the different classes of service. They are stated on a withstand basis instead of flashover, thus corresponding to other insulation testing practices.

Therefore, it seems that they should be adopted, at least tentatively, as the standard for the industry. If some pieces of equipment do not comply with these proposed values, a policy of gradual change should be adopted and the new designs made to conform.

It should be clearly understood that in adopting the proposed basic insulation levels no hardship or economic waste is being imposed on the industry but, instead, a definite system of insulation strengths is being established, so that all will know the characteristics of the equipment and be able to select the equipment to meet their needs. Full rating information should be given on the name plate of the apparatus.

As now proposed, the basic impulse insulation levels are not directly and definitely associated with the usual insulation classes that have some reference to operating voltages. It seems that it would be of definite assistance to the ordinary user if such direct association was made. Thus a standard insulation class would mean that the equipment and its insulation would withstand certain low-frequency and impulse tests. The user could select standard apparatus or higher or lower classes as desired.

I should like to suggest that the term "basic impulse insulation level" be replaced by one that is more descriptive of the purpose when referring to equipment. It seems to refer more to a curve drawn when "rationalizing" the insulation strength for a particular installation or service.

The 95-kv basic insulation level now proposed should be associated with a standard insulation class, probably a specially designated 15-kv class to be used for the smaller or so-called distribution class of equipment.

To make the insulation co-ordination fully effective, definite test procedures should be incorporated in the standards for each kind of equipment at an early date as suggested in the paper "Recommendations for High-Voltage Testing."

R. L. Whitney (Westinghouse Electric and Manufacturing Company, Derry, Pa.): There is no doubt but that the work of the joint committee on co-ordination of insulation has done a fine piece of work and they deserve the commendation of the Institute.

As a manufacturer of insulators we have, of course, followed the work of the committee actively. Our test work in connection with the investigation has shown that with the exception of four standard ratings the industry now produces a complete line of standard units that meet the proposed basic insulation levels. Even these four ratings taken as a whole are not more than five per cent below the proposed levels. That deficiency can hardly be deemed a serious one since it lies within the variations encountered in a given test laboratory and well within the variation between laboratories.

The establishment of the proposed basic insulation level as an industry standard carries with it certain specific responsibilities that warrant examination. Necessarily it involves a major investment in new designs on the part of the insulator manufacturer and a duplication of stocks all along the line. Economically every user of apparatus insulators is directly interested in such points as the doubling of the cost due to tool maintenance, natural deterioration of moulds, and stock duplication. All of these are increments in the purchase price. Duplication of field stocks means added cost of and a certain confusion in maintenance work.

Field experience with present standard insulators indicates quite conclusively, we believe, that those units offer no particular impulse hazard. Furthermore, the servicing of equipment involving those units is readily carried out with little loss of time and at low cost.

In this respect the problems differ from those encountered on apparatus using bushings. The number of apparatus insulators involved is large. The number of bushings is comparatively small. Relatively the cost of a given apparatus insulator is low while that of a bushing is high. The flashover of an apparatus insulator seldom results in any system disturbance beyond the operation of a reclosing beaker while damage to a bushing from a flashover may, and usually does, result in a major outage. The replacement of an apparatus insulator is quickly accomplished while the replacement of an apparatus bushing is a major operation.

This comparison of an apparatus insulator to an apparatus bushing would seem to justify some differential between the two types of insulation in so far as hazard and required basic insulation level are concerned.

Messrs. Sporn and Powel's paper indicates that it may not be detrimental to the co-ordination program if apparatus insulators do not meet the proposed levels until the need for conformity has been more conclusively proved in the field.

It is believed that this statement was wisely included. The new levels are set forth as proposed levels. It is felt that users must indicate the extent to which these proposed levels will ultimately be used. Will they be limited to the more important portions of a system? Will their adoption depend on lightning conditions or switching practices of the particular system? Will there be a general replacement of insulation now in service which is below the proposed levels? The answers to these questions have a direct bearing on the economics of the proposed change.

L. G. Smith (Consolidated Gas Electric Light and Power Company of Baltimore, Md.): In the Sporn and Powel paper the culmination of the work of a number of years is presented as a table of basic impulse levels. The entire electrical industry is indebted to those who have been engaged in the work of correlating service requirements with practical manufacturing capabilities to derive some degree of standardization out of the variety of impulse levels that has existed in the past. In standardization of this type the problems are of a commercial as well as an engineering nature. Those of us who are operating engineers naturally desire the highest possible impulse level. On the other hand, sometimes the cost of high levels cannot be justified whereas at other times the operating results may warrant higher costs. Therefore, commercial and economical considerations must be included in the evaluation of engineering factors.

As stated by several representatives of the manufacturers of insulators, the effect of the proposed levels on bus and switch insulators requires consideration. For the four reference classes 23, 34.5, 46, and 69, the switch and bus insulators have withstanding impulse levels $3^1/_3$ per cent to $8^3/_3$ per cent below the proposed levels as shown in table III of this discussion. Except for the 350-kv basic insulation level these represent differences of five to ten kv. Therefore, if the proposed levels are adopted, the manufacturers must either redesign or derate the insulators in these four reference classes. As shown in column 4, derating will result in insulators having levels of 20 to 28 per cent above those required; the elimination of an insulator having a level between 110 and 190 kv; and the introduction of a new design for the 350-kv level. Attention is called to the fact that the points developed in this discussion are independent of the reference classes. Column 1 in the table can be deleted.

Table III

(1) Reference Class	(2) Proposed Basic Insulation Level	(3) Withstanding Test Level of Present Insulators	(4) Withstanding Test Levels of Present Insulators if Derated
15	110	110	110
23	150	145	190
34.5	200	190	240
46	250	240	320
69	350	320	350*

* New design insulator.

Operating companies have many bus and switch insulators in service. As these are broken they must be replaced with new units that are interchangeable with the old ones. Should the proposed levels be adopted and if insulators can be redesigned for the high withstanding tests so that the new insulators are interchangeable with the old ones, no difficulty should result. However, it is understood that this is not possible. Therefore, two lines of insulators will have to be manufactured. As this will increase manufacturing costs, the users can expect to pay this cost. If compliance with the proposed levels is obtained by derating, interchangeability with present standard units can be obtained by retaining the present 145-kv-level insulator as a special unit. However, the user will be penalized by using units 20 to 28 per cent above the required levels. Actually the effect of the small differences between the values in columns 2 and 3 upon operation should be insignificant. It is realized that in so far as bus and switch insulators are concerned, it is the practice to overinsulate. However, this does not solve the problem introduced. No change should be made that will result in the mounting dimensions of insulators being changed so that thousands of insulators now in use become nonstandard.

Elias Cornell (Delta Star Electric Company, Chicago, Ill.): I would like to congratulate the joint committee on co-ordination of insulation upon the fine work done, and the results accomplished.

As manufacturer of high-voltage switch insulator assemblies and switching equipment, we have taken great interest in this work. Recent tests on switch insulators indicate that all but four ratings now meet the proposed basic insulation levels. The four exceptions are the 23, 34.5, 46, and 69-kv ratings. The actual withstand impulse voltage for these four units runs approximately five per cent below the proposed basic insulation level.

It is with satisfaction that we note that the authors do not deem these nonconformities a serious detriment, when considering the general subject. It is, however, important at this time to weigh carefully several solutions that are possible.

1. If the proposed basic insulation level values are adopted and adhered to rigidly one of two things must be done:

(*a*). Use the present standard insulators as far as possible by derating them so they will meet the proposed basic-insulation-level values. This would eliminate the present 23-kv unit, derate the present 34.5-, 46-, and 69-kv units to the next lower rating, and introduce a new 69-kv unit.

Certain disadvantages appear at once to the above mentioned scheme. If users of equipment desire to maintain their present standard insulation on new equipment, the new name-plate ratings might cause confusion or embarrassment since in some cases these ratings might be considerably lower than actual operating voltage. If on the other hand the higher-voltage units were selected for new equipment it would naturally result in increased cost, greater spacings, larger structures, and stocking difficulties.

(*b*). A second solution would be to redesign the four insulators in question. From preliminary information we have from porcelain manufacturers it appears almost impossible to secure new designs that would be interchangeable with the present units. This of course would result in more types of insulators on the system, and stocking difficulties of spare equipment. We can also be sure that there would continue to be a demand for the present standard units for new equipment to match existing installations or replacements. The net result would be more items to manufacture and stock, with no increase in total number of units. Needless to say this would decidedly increase costs.

It is felt that neither of the above schemes can seem very satisfactory to many operators.

There are two other possible solutions that have been discussed:

1. Allow a 5 per cent tolerance factor for all switch and bus insulators below the proposed basic-insulation-level values. This can be justified because of the difficulties of securing consistent impulse test results; admittedly plus or minus 5 per cent within any given laboratory, and even greater for interlaboratory comparisons.

2. Accept the present standard units with the maximum withstand impulse ratings that manufacturers can now guarantee. This scheme could be given a trial period until we have gained further experience with co-ordination or until such time when a more definite demand for a change is evident.

It is my opinion that this last scheme is fully within the spirit of the author's paper as well as the two fine companion papers by Messrs. Sporn and Gross, and Messrs. Foote and North.

H. K. Sels (Public Service and Gas Company, Newark, N. J.): Knowing that the committees working on the subject of basic insulation levels would want a broad expression of opinion, I should like to present a few points for their consideration.

The committees have proposed quite an orderly array of figures arranged in practically arithmetic progression. Maybe they are not what some would like to see but at least they are consistent and orderly. I am pleased to note that they have raised the levels for the 15-kv to 69-kv classes, inclusive. In applying insulation we have for many years been using 23-kv and 25-kv insulation for 15-kv service and 46-kv for 34.5-kv service. Why not call a spade a spade and rerate the insulators in these classes? I like Mr. Cornell's suggestion that a new insulator be developed for the 69-kv class, the other classes be reduced one step, and the present 15-kv insulator be eliminated.

Like Mr. Montsinger, I have been trying to co-ordinate insulation since 1925 but just when I think that we have something under our thumb it slips away from us. Co-ordination seems to be a slippery subject. We started out trying to insulate our bus equipment—oil circuit breakers, air-break switches, and bus supports—at least ten per cent higher than the line and the transformers ten per cent above the bus. However, by the time the bus insulation started to deteriorate, particularly that of the oil circuit breakers, and transformers were rerated in the light of present-day knowledge, the steps were down from the line instead of up, leading I know not where.

Rounded off to the next even hundreds the basic insulation levels proposed require an equivalent number of standard ten-inch disk insulators; that is, starting with the 34.5-kv class, the basic insulation levels of 200, 300, 400, 500, 600, 700 up to and including the 161-kv class of 800 requires 2, 3, 4, 5, 6, 7, and 8 units. However, the 196-kv class with a basic insulation level of 900 takes 10 units and the 230-kv class with 1,100 takes 12 units, the 287-kv class with 1,300, 15 units, and the 345-kv class with 1,600, 18 units. If you will look over the table of transmission lines published periodically by the *Electrical World* I don't believe that you will find a single line operating in any class insulated with so few units For example, our 138-kv lines are insulated with 9 suspension units and our 220-kv lines with 16 units. This means that all lines are considerably overinsulated compared to the proposed insulation levels. Therefore we must depend entirely upon co-ordinating gaps or lightning arresters to protect the terminal equipment.

Messrs. Sporn and Gross' paper entitled "Rationalization of System Insulation" shows only a 10 per cent margin in the higher classes and 25 per cent margin in the lower classes between the basic insulation level of equipment and the protective level of arresters passing a mere 5,000 amperes. Mr. Smith says the average for all classes is 20 per cent. Therefore, with such a narrow margin as this, I want to sound two notes of warning for the manufacturers.

First, better arresters are necessary. I think placing dependence entirely on arresters is like leaning on a weak crutch. About every five years it has been necessary for us to hold an anniversary year of rebuilding or replacing our arresters. Second, it is all right to establish basic insulation levels which the transformers and other equipment will meet when new; but can they hold it? Our experience has been that transformers purchased in the roaring 20's for 132-kv operation but insulated for 161-kv class are now only considered equivalent to the 138-kv class and transformers insulated for 138-kv class have been derated to 90 kv. We have had internal flashovers with the latter units and I can only hope that lightning arresters which we are now installing will afford sufficient protection.

In other words, it is not only necessary to lower the protective level and improve the discharge capacity of arresters but also to improve the terminal equipment so that it will maintain its basic insulation level. We do not want to have to pay a premium to purchase equipment with a higher basic insulation level and operate it in a correspondingly lower-voltage class to improve this margin. It appears that the basic insulation levels proposed are a minimum that should be considered in every class.

Philip Sporn and **C. A. Powel:** It is particularly significant to find that none of the discussers on this paper have taken exception to any of the proposed basic-impulse-insulation values on the basis that they are ir-rational, unsound, or predicated on an analysis and line of reasoning which is in the least fallacious.

Several discussers have elaborated on the point that certain apparatus insulators notably in the 23-, 34.5-, 46-, and 69-kv classes, will not at the present time meet certain values in the proposed basic-insulation-level grouping, the deficiency being in the general order of five to ten per cent. This situation was briefly noted in the paper, and we are glad to see the situation more fully presented by Messrs. Whitney and Cornell. Several procedures for taking care of this situation have been mentioned. The four outlined by Mr. Cornell are:

1. Derate the present insulators.
2. Make new insulators where deficiencies now exist.
3. Allow insulators to fall five per cent below the proposed basic insulation levels (by allowing five per cent tolerances).
4. Accept present insulators as now used.

Mr. Clem has offered a fifth suggestion which is a new group of basic insulation levels which he has arrived at by applying correction values to test values of transformers which were set some years back.

Mr. Sels has pointed out his belief that the proposed basic insulation levels are minimum values which should not be reduced. Mr. Henry likewise voices the same sentiment in his statement that "... the principles involved in their (basic insulation levels) determination are sound and the proposal should receive very careful consideration."

Thus we have the general points of view, first, that the proposed basic insulation levels have been established on sound principles and that they should not in general be reduced and second, the point of view of insulator manufacturers, who find that certain equipment does not meet the proposed levels without derating its impulse rating. To effect a remedy they propose changing the basic insulation levels on that account alone regardless of the principles involved.

The final determination of what values can be most satisfactorily accepted can be determined only after joint committee action by the triple joint committee. It should be pointed out, however, that while considerable time has elapsed since the work was first started on this subject of insulation co-ordination, up to the present time each step has been a progressive one forward. Considerable care should be exercised at the present time to make certain that no action is now taken which is detrimental to the general forward progress and development in the art.

Several comments have been made in regard to the slight labor expense involved in replacing apparatus insulators should they fail in service. This is a point of view which is not at all consistent with the operating performance of a high-grade system. The failure of even one bus insulator in a station may result and has in the past resulted, in a complete station outage, while the cost of replacing the one insulator which failed may be only a few dollars, the consequential damage to other equipment and service may be relatively enormous. The fact that in a great many cases operating companies are today using the next higher class of insulation above system rating is convincing proof that the flashover of insulators in the medium-voltage class is in general not considered adequate at the present time for many systems where high-grade service is required.

The point raised by several discussers that to change apparatus insulator designs in several of the insulation classes would result in an economic burden requires careful consideration. Since on a sound engineering basis it appears that the basic insulation levels proposed are required, it should lessen the economic burden, if appreciable, by permitting such changes to be made over a period of time, as has been suggested, or even derating equipment. This procedure would seem at the moment to be preferable over attempting to adopt as a standard, impulse strength of equipment which had been initially designed on a 60-cycle basis only without consideration of how its impulse strength fits into the insulation co-ordination system.

All the discussions offered on this paper and the various suggestions made will be helpful in acquainting the electrical industry with the problems confronting insulation co-ordination, and is welcomed as an aid in carrying on the work to a conclusion.

Rationalization of Transmission-System Insulation Strength—III

Discussion and authors' closure of paper 40-36 by Philip Sporn and I. W. Gross, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (October section) pages 591–5.

M. S. Oldacre: See discussion, page 1199.

J. T. Lusignan, Jr.: See discussion, page 1205.

C. A. Powel (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper by Messrs. Sporn and Gross, dealing with the margin between the levels to be protected and the levels held by the protective devices, is very important, because on these margins depends the entire justification for the basic levels to be submitted for consideration by the joint committee on insulation co-ordination, as proposed in the Sporn-Powel paper. It must be recognized that a proper choice of basic levels is of great economic importance to the industry. The margins shown in the paper are very moderate and might be considered in some cases inadequate except for the fact that other factors, such as rapid decay of the lightning surge and the small probability factor of heavy surges, come into the picture and help the situation.

Because of limitations imposed by the necessity of retaining assurance that the arrester will maintain its integrity at operating frequency, it would seem that the protective ratio now obtainable in good lightning arresters is about as low as we can expect. Certainly no major reduction is to be expected, and this being the case and some margin being recognized as necessary, the likelihood of a reduction in basic levels in the future is quite remote.

R. T. Henry (Buffalo, Niagara and Eastern Power Corporation, Buffalo, N. Y.): The problem of co-ordination of insulation has been one of the major problems before the industry for many years. A great deal of effort has been directed toward its solution, particularly since the formation of the EEI-NEMA Joint Committee on Co-ordination of Insulation nearly ten years ago.

Careful consideration of masses of data and a great many different ideas on the subject have finally developed a logical proposal based on sound engineering analysis of the problem.

This proposal involves establishing "basic insulation levels" which can be used to determine the insulation strength required for apparatus in the various voltage classes. The values proposed are based on the assumption that, in each voltage class, a reasonable margin of insulation strength should be provided above the voltage to which apparatus may be subjected in service, up to the level of protection which can be provided with modern lightning-protective equipment.

This proposal represents the concensus of opinion and judgment of a large number of the outstanding engineers of many of the utilities and manufacturers. Whether the values proposed are exactly right or not, the principles involved in their determination are sound and the proposal should receive very careful consideration.

There are differences between some of the values proposed and the characteristics of present-day apparatus. The next step in the solution of this problem is to adjust these differences. Fortunately, the situation in regard to lightning protection has improved through the years to such an extent that it is now possible to protect the insulation for any given service voltage at lower levels than could be done some years ago. Otherwise, it would have been necessary to propose values considerably higher than those now proposed. As it is, most of the differences are small and can probably be eliminated by minor adjustments, either in the values proposed or in the design or rating of certain apparatus. In a few cases where the difference is appreciable it may be necessary to modify the design or the rating of certain apparatus. In considering these adjustments, it should be kept in mind that most major apparatus such as transformers, circuit breakers, etc., will meet the proposed values and that most of the present-day insulators which will not meet them are commonly used, not at their present rated voltage but at voltages below their present rating.

It seems to me in this connection that we should avoid establishing tolerances in any case and we certainly should not establish lower levels for some apparatus as compared with other apparatus in the same voltage class. The basic idea behind this proposal is to select insulation for each device such that all of the apparatus in a given station can be protected and on this basis there should be no lightning flashover on any of the apparatus. This would seem to eliminate any consideration of the relative seriousness of failures in one device as compared with other devices and any justification for different values for different devices in any voltage class.

There is some tendency in certain quarters to withhold agreement to this proposal because some present-day apparatus would not meet the proposed requirements. In order to avoid any undue burden which might result from the adoption of this proposal, I would suggest that the "effective date" might be postponed, several years if necessary, in order that agreement may be reached without further delay. Meantime the industry could have the benefit of a definite objective to work toward. This would permit the completion of a number of standardizing activities which are held up pending such agreement.

It seems to me that the time has come to eliminate petty considerations, make adjustments in the values where possible, modify designs or ratings where necessary, and reach agreement in order that the industry may realize the benefits which will result from a logical solution of this problem.

W. J. Rudge, Jr. (General Electric Company, Pittsfield, Mass.): Messrs. Sporn and Gross, and Foote and North, have presented excellent treatment of the subject of insulation levels. The procedure of establishing levels outlined in these papers appears rational and well thought out. Both papers have recognized that the protective levels established by modern arresters are determined by the system operating voltage line to ground.

Modern lightning arresters are classified into two general classes: those suitable for grounded-neutral circuits and those suitable for nongrounded or isolated neutral circuits. For circuit voltages above 15 kv the factor between the arrester's maximum line-to-ground voltage rating and the circuit's normal line to neutral volts is approximately 140 per cent for grounded-neutral circuits and 180 per cent for isolated-neutral circuits. These values have been arrived at largely through experience in the past with numerous systems.

With the advent of the transient analyzer, the a-c calculating board and improved methods of checking up system operating voltages, it should be possible to examine the various system conditions and establish the normal and maximum voltages existing on systems today and to evaluate the magnitude of overvoltages which may occur under the various operating conditions.

The analyzer and calculating-board studies are all given in percentage of the normal operating system voltage. It is as vital to know the actual operating voltage as it is to know the percentage system overvoltages occurring under abnormal conditions, as the actual operating voltage is one of the essential factors which determines the proper arrester to be used at a given location. We note that Messrs. Sporn and Gross propose a factor of 130 per cent for grounded-neutral arresters. This undoubtedly will be satisfactory for a well-grounded system, that is, where the ratio of X_0 to X_1 is equal to or less than 2 and for a value of R_0 to R_1 not greater than 1. Our experience over a great many systems has indicated that there are a number of grounded-neutral higher-voltage systems which have a higher ratio of X_0 to X_1 than 2 and which do not have as close voltage regulation as the six per cent mentioned in Messrs. Sporn and Gross' paper.

Likewise, in the case of the ungrounded systems, the studies on the network and transient analyzers and measurements on some of the actual systems indicate that 180 per cent is below the voltage which may be found on these systems in the event of a single-line-to-ground fault. Yet as far as can be determined at present, there has not been an excessive number of arrester failures on this type of system.

We feel that to set up the final values for the basic insulation levels a survey should be made of the operating voltage of the various companies to find out the maximum operating voltage in each voltage class with the ultimate goal of setting levels which will cover the majority of systems, as any standard lines must be so set up to be of greatest value to the industry.

In addition to the determination of actual operating voltages, there should also be a determination of the results of X_0 to X_1 found on the various systems so that the factor selected for both grounded and isolated neutral arresters can be chosen to cover most systems.

The importance of such a study cannot be overemphasized, particularly in view of the trend toward arresters of higher discharge capacity and lower protective ratios. It becomes obvious that modern arresters which have low impulse ratios will be more sensitive to overvoltages because they are designed to pass higher current for a given overvoltage, whether it be from lightning or system power-frequency voltage. This makes it all the more important to select arrester ratings which will fit the system operating conditions, as by so doing a premium will be realized on both the prevention of equipment failures from lightning and the probabilities of damaging the protective device.

It is of particular interest to note that Messrs. Sporn and Gross propose 5,000 amperes as the current to be used for establishing basic insulation levels. This seems a rather low current when compared to the discharge currents being considered by the lightning-arrester subcommittee which is rewriting lightning-arrester standards. However, it is to be noted that when comparing the arrester levels with the basic insulation levels that arrester maximum values were used to compare with basic insulation levels which are minimum, or with standard values. Actually, to demonstrate basic insulation levels it will be necessary to set the impulse generator so that the voltage will not come below the basic insulation levels which means that the actual values demonstrated will be between the basic insulation level and some value perhaps in the order of ten per cent higher. The average demonstrated value will be about five per cent higher.

The average arrester performance value is 15 per cent lower than the value used. Hence, there will be approximately 20 per cent factor between the average apparatus test and the average arrester level which does not appear in the table.

This could be considered as a factor to maintain a margin below the basic insulation levels when the discharge current is greater than 5,000 amperes and if we refer to the arrester values presented in the lightning-arrester subcommittee report [AIEE TRANSACTIONS, volume 59, 1940 (June section) pages 345–6] it may be found that when the arrester voltage increases 20 per cent above the discharge voltage at 5,000 amperes, the current increases to approximately 20,000 amperes.

We are particularly interested in the procedure used by Messrs. Sporn and Gross when selecting the rod-gap setting given in line *n*. It is stated that the gap spacings are taken as $3^1/_4$ times the maximum expected line-to-ground voltage. It appears from the values used that this assumption must have also been that the gaps were dry. It appears that this gap should have been based on the wet flashover, as there is usually rain at the same time that lightning occurs. If this were done, it would require a 10 to 20 per cent increase in gap spacing with a corresponding increase in flashover voltages.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): As one who has been associated with the problem of co-ordination of transformers since the inception of co-ordination in 1926–27, I am glad to see the progress that is being made on the broader subject of co-ordination, and the sound foundation on which it is being built, namely the available protective devices like lightning arresters. As pointed out in these papers, the arrester level of protection is dependent primarily on the low-frequency voltage of the system.

For many years, up until the time of the co-ordination movement, the insulation level of transformers was determined solely by the system voltage. For example: for isolated-neutral systems, transformers were generally insulated for twice line-to-line voltage. When the neutrals of high-voltage systems were solidly grounded, it was common practice to insulate the transformers for an induced voltage test of 2.73 times leg voltage to ground, without regard to whether protective devices capable of protecting the lower level of insulation were installed on the system. Where lightning was prevalent, it was found impractical in many cases to provide sufficient protection to apparatus with such low insulation levels, and as a result the 2.73 transformer is rarely used today.

The next and most logical thing to say was that the insulation requirement of apparatus was dependent only on the protective levels of protective devices. This might lead one to conclude that the system voltage was no longer the controlling factor in the selection of apparatus insulation levels. This, of course, is not true because the protective level of the protective devices themselves is dependent on the system voltage, that is, the maximum dynamic line to ground voltage. In other words, while it can be said that the insulation level of apparatus is dependent on protective devices, yet the system line-to-ground voltage still remains the principal factor that determines the co-ordination of insulation. This is evidenced by the fact that it is proposed to use one level lower of insulation on solidly grounded-neutral systems where the lower-rated arrester can safely be used.

As to the system dynamic overvoltages, I note that Messrs. Sporn and Gross suggest a 20 per cent increase in voltage for circuits up to 8.3, a 15 per cent between 8.3 and 15 kv, and a 6 per cent increase above 15 kv.

While these values might be met on an exceptionally well-regulated and well-grounded system, it is doubtful if such a small tolerance on dynamic overvoltage should be used as standard.

Our experience indicates that many systems are outside of these limits. Therefore, if we are to set up basic insulation levels which will cover the requirements of the majority of systems, it seems in order to suggest that a program be set up by the operating companies to obtain factual data which will include the maximum continuous line-to-line operating voltage as well as the maximum line-to-ground voltage under both normal and abnormal operating conditions. If this data were submitted to the EEI-NEMA committee, it would serve as a sound basis for setting up firm values for the basic insulation levels.

Considered from the standpoint of the assignment of apparatus to the proposed basic insulation levels, so far as transformers are involved there will be no trouble in meeting the new basic insulation levels with the exception of distribution transformers rated 12.5–15 kv and intended for connection line to line which have been assigned in table I of the Sporn-Gross paper to the 110 basic insulation level. Transformers of this rating have previously been built for a full wave test (or basic insulation level) of 95 kv. This 110-kv level, if adopted, means that the insulation of these ratings must be increased to a point between the present 15-kv and the 25-kv insulation classes, obviously at higher costs especially for the smaller pole-type transformers. Some operators who use transformers of these voltage classes may object on this basis.

With reference to the definition of basic insulation level in the paper by Sporn and Powel, I feel that the word "suitable" is subject to misinterpretation. "Suitable" was criticized by some of the members of the NEMA group on the ground that what is suitable to one may not be suitable to someone else. It really means "apparatus insulation as demonstrated by standard tests". So why not add the word "standard" and say, "suitable standard tests . . . ."

L. G. Smith (Consolidated Gas Electric Light and Power Company of Baltimore, Md.): The two papers by Sporn and Gross, and Foote and North, present a logical engineering approach to station lightning protection. The authors of both papers emphasize that adequate lightning protection depends upon the proper correlation of the voltages permitted by surge protective devices and the impulse insulation strength of station equipment. While the data presented on specific protection problems are the best available at present, a warning against their too literal use is believed necessary. As this warning is predicated on the lack of statistical data concerning lightning, no criticism of the tabulated data is implied.

While reasonably accurate information is available concerning impulse strength of electrical equipment, probability factors of the magnitude of voltage permitted by surge protective devices have not been established, because of the lack of statistics concerning lightning such as rates of rise of currents and voltages and magnitude of surge voltages and currents. In the Sporn and Gross paper, the protected levels are based upon surge currents of 5,000 amperes. While this assumption is confirmed by the data in the paper by Gross and McMorris, it is believed that the data on which it is based are insufficient. Moreover, if only a small percentage of the surges exceed 5,000 amperes, we cannot afford to permit such a small margin that equipment failures may occur, causing loss of electric service to large numbers of customers.

The basing of the equipment insulation levels upon the lightning arrester applied should make it necessary to use different levels for grounded- and ungrounded-neutral systems. In the Foote and North paper attention is called to the withstanding impulse level of current potential transformers on feeders in table I. Since current and potential transformers may be essential elements of feeder relay protection, the wisdom of a level of 50 kv for instrument transformers as compared with a general level of 60 kv is questioned. A failure of an instrument transformer associated with the relay protection of a feeder may prevent the feeder from tripping, which may result in the loss of service from an entire substation.

S. B. Crary (General Electric Company, Schenectady, N. Y.): As pointed out in the paper by Messrs. Sporn and Gross the voltage rating of an arrester is influenced by the maximum dynamic voltage that may be obtained at the arrester location, and this in turn is an important factor in determining the protectable level. During the last few years, the subject of power-system overvoltage has been given considerable attention so that it may be in order to review briefly some of the factors which influence the maximum voltage on systems and which may be controlled by giving attention to the system design.

1. Line-to-ground faults, in general, result in the highest dynamic voltages on the unfaulted phases and are practically completely controlled by the method, number, and arrangement of system grounding points.
2. High dynamic voltages due to sudden loss of load and overspeeding, particularly of waterwheel generators may be reduced in magnitude by modern regulators and excitation systems which act quickly to reduce the excitation under such conditions.
3. High dynamic overvoltages may result due to unbalanced faults on lines to which are connected salient-pole generators, particularly under the condition when a line or lines having appreciable capacitance, is left connected to the generators but disconnected from the load. These overvoltages may be minimized by properly designed amortisseur windings in the generators and operation of the system so this condition is not likely to occur.
4. Use of single-phase switching devices, fuses, etc., in three-phase systems may, in some circumstances, result in high dynamic voltages. Edith Clarke, H. A. Peterson, and P. H. Light have submitted to the Institute a paper treating this subject (40-105, 1940 AIEE summer convention; scheduled for inclusion in 1941 Transactions). They show the conditions under which high voltages may occur. By proper application of such devices, the probability of obtaining these overvoltages may be reduced or eliminated.
5. Switching surges may under some circumstances result in severe overvoltages. Again by giving attention to the method of system grounding arrangement, and employing circuit-interrupting means which have a minimum tendency to restrike, such effects may be minimized.

A. C. Monteith (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): With reference to the example in the paper by Messrs. Sporn and Gross, it is noted that a factor of 30 per cent was added to the phase-to-neutral voltage to determine the dynamic voltage rating of the lightning arrester for application on an effectively grounded-neutral system. This factor is satisfactory for a large percentage of the cases where the arresters are applied on the larger systems but there are a number of systems where this might not be an adequate factor. This brings up the age-old question of the definition of an effectively grounded system.

In an attempt to put this application on a simplified engineering basis, consideration has been given to the use of ratios of zero-sequence resistance and reactance to positive-sequence reactance as viewed from the lightning-arrester location and it has been found that a fairly simple set of curves can be plotted. The methods of calculation and some of the curves are presented in references 4 and 5 of the paper by Messrs. Sporn and Gross. As a result of this work and a review of the number of systems, it was concluded that a lightning arrester with a rating of 80 per cent of the maximum line-to-line voltage can be used in the majority of cases when all points of power supply are solidly grounded. Incidentally, in the past the selection of 80 per cent has been associated with a grounded-neutral lightning arrester. The application on the basis of the limiting ratios of X_0/X_1 of $3R_0/X_1$ of 1 for the 80 per cent arrester allows a ready check with breakers closed and open, whether the arrester is applied on the transformer side or the line side of the line circuit breakers. The curves referred to above are such that as soon as the ratios are determined the limiting voltages are immediately available.

The example cited in table III of the paper by Messrs. Foote and North is of interest in this connection. They have selected a station with the transformers solidly grounded, yet the 30 per cent rule would not apply for the lightning arresters on the bus, nor would the 80 per cent rule apply for the arresters on the line side of the breakers when the breaker is open. It is therefore felt that before using lightning arresters with reduced ratings, a check should be made for the particular application and then a corresponding basic insulation level selected as outlined in Messrs. Foote and North's paper.

In connection with the paper "Recommendations for High-Voltage Testing" it should be noted that although the different laboratories are not in agreement on the short-time values for volt-time curves the disagreement being as much as plus or minus 20 per cent, nevertheless volt-time curves can be used in co-ordination work. This statement is true provided that any comparison is made with data originating in the same laboratory.

Philip Sporn and I. W. Gross: Both Messrs. Rudge and Monteith have brought up the point that the basis used in our paper for the application of lightning arresters on grounded-neutral systems, based on 1.3 times the normal line-to-ground voltage gave too low 60-cycle voltage values for some systems. This factor of 1.3 is one which in our experience based on calculated voltages has never been exceeded on our 132-kv solidly grounded-neutral system, except on one part of the system close to large hydro generation. A rough survey of conditions on our 12,000-volt systems indicates that here too the 1.3 factor is adequate. We appreciate, however, that there are grounded-neutral systems in the country where the voltage may go higher and thus justify a higher maximum rated arrester.

Likewise Messrs. Rudge and Montsinger believe there are many systems in the country where the regulation exceeds 20 per cent, 15 per cent, and 6 per cent for the circuit voltage ranges of 8.3, 8.3–15, and above 15 kv. In justification for these values as well as the 1.3 factor for grounded-neutral lightning-arrester application, we wish to point out that we attempted to select values which would be conservative from the point of view of justifying a low basic impulse insulation level. Should extensive further study of the situation and general experience indicate that the values used are too low, it means that the lightning arrester will require a higher 60-cycle rating, and in turn the protective level permitted by this arrester would likewise be higher. This then, will result in one of two things: either less margin between the arrester level and the basic insulation level or raising the proposed basic insulation level.

It has been pointed out by Mr. Powel that the margins shown between the arrester characteristics and basic insulation levels "are moderate" and in this we heartily agree. With a factor of safety of 10 per cent over the arrester characteristic, even with 25 kv added for added benefit in the lower-voltage classes, we certainly cannot feel that the calculated margins are excessive. Further, as Mr. Powel points out, since there is very little possibility of lightning arresters having appreciably lower protective characteristics in the future, greater margins between the arrester and basic insulation level cannot be expected from this source.

Several discussers have pointed out the desirability of calculating overvoltages on grounded-neutral systems in the application of arresters to determine the proper arrester 60-cycle rating. We have always done this in recent years on our 132-kv system and have reached the conclusion that the 109-kv arrester is suitable in most applications on our system. This has been borne out by some eight years of actual experience. In any important installations where arresters are to be applied, we always calculate the expected maximum overvoltages and use these as a basis for applying arresters.

Both Messrs. Smith and Rudge have questioned the 5,000-ampere lightning-arrester discharge current as a basis for the arrester characteristic. Here again, we appreciate that occasionally discharge currents through arresters may exceed this value, and if so, protection of equipment theoretically at least may be questioned. In the case of transformers, however, it is possible to rely on the factor of safety in the transformer to supply the necessary insulation for protection against such higher-voltage surges. To have taken a higher arrester current such as 10,000 amperes would again have raised the arrester characteristic and cut down the margin between the arrester and the proposed basic insulation levels; or viewed from another point of view, would have required the basic insulation levels to be higher if the same relative margins were to be retained.

In all cases where assumptions were necessary such as system voltage regulation, lightning currents through the arrester, and application of arresters on grounded-neutral systems, we used those assumptions which, while staying within the limits of test and experience, still would tend to justify the lowest basic insulation levels. For this reason, we believe that our analysis as shown in the paper indicates clearly that the proposed basic insulation levels are none too high in any of the insulation classes, and if any change at all is warranted, on the basis of protection only, it would justify a raise of levels rather than a reduction.

The Co-ordination of Electrical Equipment With Basic Insulation Levels

Discussion and authors' closure of paper 40-35 by J. H. Foote and J. R. North, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (October section) pages 585–9.

M. S. Oldacre: See discussion, page 1199.

R. T. Henry: See discussion, page 1201.

W. J. Rudge, Jr.: See discussion, page 1202.

L. G. Smith: See discussion, page 1203.

S. B. Crary: See discussion, page 1203.

A. C. Monteith: See discussion, page 1203.

V. M. Montsinger: See discussion, page 1202.

W. H. Cooney (General Electric Company, Pittsfield, Mass.): Since the principles of co-ordination were started more than ten years ago, the units of measurement have changed from insulators, to rod gaps, to kilovolts, as the knowledge of impulse voltages was increased by work in laboratory and field, but the fundamental ideas have been retained. Now we are thinking in terms of basic insulation levels, and Messrs. Foote and North have shown by three excellent examples how the insulation strengths of the component pieces of equipment of a station can be properly related.

The method of determining first the protectable level and then selecting the indicated basic insulation levels seems logical. Whether the proposed protectable level of (1.10×arrester maximum performance)+25 kv is about right should be determined by further study and experience. On the one hand, it should be high enough to allow for the variable conditions of service, and on the other hand, it should not be so high as to require unnecessarily expensive insulation. It does demonstrate the value of a protective device which holds impulse voltages to a low level.

Obviously for the sake of brevity, the authors did not repeat the method of calculating protectable levels outlined by Messrs. Sporn and Gross. If this paper is to be reprinted, I would suggest for completeness of the three examples that the arrester levels selected and the calculations of the protectable levels be shown in each case.

The examples of selecting insulation for three substations demonstrate the flexibility of this method, since the particular operating conditions, the importance of each type of apparatus, and the importance of the substation itself can all be factored. Caution should, however, be exercised by selecting standard insulation so far as possible, for special insulation may prove quite expensive. As the authors emphasize, the economics involved are of equal importance with the low-frequency and surge-voltage requirements.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Messrs. Foote and North cover in their paper the type of reasoning which will be generally used in the future in co-ordinating the insulation of equipment in stations and substations. I agree that the impulse characteristics of equipment are equally important with the low-frequency characteristics.

It is desirable, of course, that impulse and 60-cycle characteristics of a given piece of apparatus bear a definite relation to each other so that it may find general use and its cost be kept to a minimum.

In the past many customers have brought overvoltage equipment because of their knowledge that standard apparatus tests did not insure them trouble-free operation. Several factors have contributed to this difficulty, one of which has been improper relationship between impulse characteristics and low-frequency test values. In the future the improved methods of test and greater information on the subject should allow rational applications.

There are, however, some factors which are not adequately covered by any statement of either low-frequency voltage test or impulse test. For instance,

1. A device such as a switch which generates a large volume of gas on circuit interruption may have adequate structure spacing for any type of voltage test but the momentary presence of gas within the structure may vitiate these spacings.

2. Gas-forming devices without adequate means for exhausting the gas away from live terminals may also have their insulation values momentarily reduced to negligible amounts.

3. Horizontal insulation surfaces on which moisture may collect in service may on new apparatus tests appear entirely adequate but give trouble in operation.

4. Structures which may absorb water through breathing or leakage may have adequate test values as new equipment but not be trouble-free in service.

The point I wish to make is that there are several factors entering into the subject of adequate insulation, some of which are not necessarily covered by test, but which an experienced manufacturer intends to cover when he says "This apparatus is good for use on . . . . service voltage." Taking all factors into consideration, it appears then that service voltage should be the bench mark of application and both 60-cycle and impulse tests be related to it.

F. J. Vogel (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Messrs. Foote and North are to be congratulated on giving an engineering approach to a difficult problem. It might be more easily described as the familiar method by which we determine the stresses which apparatus is required to stand, and design it accordingly. This seems simple, but when it is complicated by past usage, lack of information as to the strengths of various parts, and similar matters, the problem becomes very difficult indeed.

There are some difficulties to which I would like to call attention. The basic insulation impulse levels specified in both this and the Sporn and Powel paper do not exist for all lines of apparatus. One of these is the 110-kv impulse level which does not now exist for circuit breakers or several lines of transformer apparatus. The same difficulty exists in other lines of apparatus, for other voltage classes, I believe. The examples given by the authors show the effect of past and present standardization and usage in this regard. It is unsafe to prophesy, but possibly as time goes on both the proposed basic impulse insulation levels and apparatus levels will be modified and agreement on all types of apparatus will be reached.

The procedure of determining arrester applications by the method of determining the maximum low-frequency voltage which can be applied seem unassailable. This leads to the only weakness in the proposal, that of determining what surge voltages are possible on apparatus in service. The margin described may not always protect the apparatus. Two possibilities should be recognized:

(*a*). If there is any appreciable distance between the protective device and transformers, it is possible to have a higher voltage on the transformer than at the protective device. In the case of extremely high and steep-fronted surges, obtained by nearly direct strokes of lightning, this failure to furnish complete protection is possible. As a matter of opinion, trouble from this source will probably be rare on high-voltage systems of customary design, using ground wires and other methods of protection and relief from direct strokes. I think it can however occur, and some transformer failures from this cause will happen.

(*b*). If high discharge currents in the protective devices result in high voltages across them, the surge voltage may exceed the basic impulse level and result in flashovers at transformer bushings, pillar insulators, etc. No remedy for this is suggested, but if in practice, this occurrence is rare, the proposed scheme will be justified. It is my opinion that this will be true.

In the closing part of the "Procedure for Selection of a Particular Basic Insulation Level", an important statement is made, namely, that "In the final analysis, the insulation suitable for a given situation will be that which fulfills the low-frequency requirements, the surge-voltage requirements, and the economics involved." At the present time, the impulse levels are still relatively new, and correspond to low-frequency test levels established by long practice. The low-frequency tests now specified are not required by service. This is recognized by the fact that users often find that they can use an arrester rated one voltage class lower than the rated voltage of the line, and then specify lower 60-cycle test voltages. The facts are that there is no necessary correlation between impulse strength and 60-cycle strength in apparatus of different designs, and also that the low-frequency service requirements (including switching surges, etc.) are independent of the arrester used. The internal insulation of transformers might be given present impulse tests, and somewhat lower 60-cycle tests than we now use, and meet the service requirements. This might lead to some economies, but is contrary to established usage, and would lead to confusion at the present time. There may at some future time be a trend in this direction, based on the possibilities mentioned above.

D. C. Prince (General Electric Company, Philadelphia, Pa.): A study of this paper and the other splendid papers on the subject fails to give any idea of the thousands of man-hours which have been expended in considering the subject or the extreme complications of the subject matter involved.

The material to be co-ordinated includes bushings; transformers, large and small; circuit breakers, large and small, indoor and outdoor; fuses; air disconnecting switches and bus supports, and finally line insulation. Nature has established certain relations between effects of 60-cycle voltage and impulse voltage on these various types of equipment. The oil-immersion insulation in transformers and circuit breakers brings a high impulse ratio of the order of two (for one-minute 60-cycle crest and for standard 1.5x40 wave) or more for short-time applications of voltage. At the other extreme, certain forms of bus insulators flashover at the same crest voltage both at 60 cycles and under impulse. Other forms of insulation have varying impulse ratios between these limits.

Not only is the nature of the behavior of the different insulations under impulse different, but the cost of providing different impulse levels may vary greatly. On account of those high inherent ratios the impulse strength of a transformer can be made quite high by equipping it with what might be considered an oversized bushing, which has a considerably higher 60-cycle strength than the transformer winding. Since the cost of bushings is only from five to ten per cent of the total cost of the transformer, the high impulse level is obtained with relatively little cost. In an oil circuit breaker, the cost of bushings is roughly a third of the total cost. High impulse levels are, therefore, obtained at somewhat greater relative expense in circuit breakers than in transformers. In air switches and bus supports, all of the parts have inherently low impulse ratios so that a change in impulse ratio affects the cost of the total device.

Transformers are relatively expensive pieces of equipment. An impulse breakdown of a transformer may put it out of service for a considerable period and involves a high cost of repair. Small transformers involve much less cost in repair and their loss involves less interruption to service. There is some question, therefore, whether the criteria for the selection of impulse levels might not indicate a higher level for large power transformers and circuit breakers than for small circuit breakers, disconnecting switches, and bus supports. On the other hand, insulators when flashed over may not be damaged and even if they are, a replacement can be made quickly and at relatively small cost.

Again, transformers normally receive the best possible lightning-arrester protection, whereas the other pieces of apparatus are progressively further from the lightning arresters and correspondingly are not so well protected. Following this line of reasoning one might conclude that transformers might economically be built for a lower impulse level than other pieces of equipment, the impulse level being increased as the distance from the arrester increases.

Another type of question involved in this selection of impulse level is the question of need of allocation of impulse levels to operating voltages. In a transformer, the operating voltage fixes such important design elements as turns and iron sections. It is, therefore, practically essential that there be a definite allocation of impulse levels to correspond with line voltage for transformers. Circuit breakers, on the other hand, are regularly designed for a standard voltage step and are frequently used on circuits considerably below this standard rated voltage. The reason for the tie between impulse levels and operating voltages is, therefore, much less imperative. In the case of current transformers and bus supports, there is no necessary relation between the operating voltage and the impulse level. At the present time, the effort is being made to assign standards of insulation for all these kinds of equipment.

The difficulty in reaching an agreement in regard to such standards is primarily due to a lack of an agreed philosophy to govern the situation. If it were agreed that the different types of apparatus should not necessarily have the same relationship between the operating voltage and impulse level as suggested to some extent in this paper, it would be possible for the different standardizing bodies to proceed with little difficulty, each to standardize the apparatus in which he is most interested. On the other hand, if it is considered that all of these pieces of apparatus should not only be co-ordinated but should meet the same impulse levels relative to operating voltage levels, then the standards could be worked out on the basis of the limiting pieces of apparatus with due regard to past operating experience and economic considerations. It seems, therefore, that a major effort should be made to agree upon the broad policy to be followed in working out this co-ordination. If the fundamental principles could be agreed upon an acceptable solution for the whole problem should be obtainable.

J. T. Lusignan, Jr. (Ohio Brass Company, Mansfield): I believe these papers on insulation co-ordination give a complete picture of all phases involved. Messrs. Sporn and Gross tell us of the methods of setting up protective levels in order to guard equipment in the various voltage classes. Messrs. Foote and North tell us more of the assigning of equipment to the proper levels for protection. I like particularly well their examples for showing how these assignments should be made for several typical insulation classes.

In the section "Procedure for Selection of a Particular Basic Insulation Level" of the paper by Foote and North two statements appear. The first reads: "In stations accommodating many connecting lines and in stations of considerable extent, it may be that different types of protective devices are used at different locations; also, the operation of switching equipment may isolate certain apparatus from some of the protective devices." The second statement reads: "If a gap type of protective device such as a protector tube or a plain rod gap is to be used, the protection afforded is based entirely on the time to break down and should not be considered as a level, but rather a volt-time curve. In this case, a comparison of the apparatus chopped wave impulse voltage test with the gap breakdown at a comparable time plus suitable tolerance and margin would more accurately represent co-ordination for apparatus on which complete volt-time curves are not available."

I believe the first statement suggests the need for a protective device other than an expensive arrester which might be counted upon to guard oil breakers, bushings, and other station equipment which might be separated through switching from the arresters, protecting transformers. The second statement intimates that protecting devices should have flat "volt-time" characteristics. A device which we feel would serve all these needs is the so-called "control gap", described in a paper given before this convention two years ago. ["The Control Gap for Lightning Protection", Higgins and Rorden, ELECTRICAL ENGINEERING (AIEE TRANSACTIONS,) September 1936, pages 1029–34.] This gap has been suggested and used at station entrances, where the highest lightning currents may be expected, as well as near the transformers. Its flashover and discharge characteristics are unchanged for any current magnitude or polarity.

I particularly approve of conclusion number 3 of the Foote-North paper suggesting the addition of "withstand" ratings to equipment name plates. This will make it easier to buy equipments and associate them together on the proper insulation voltage levels. Indirectly this will help to promote the general idea of better insulation co-ordination. This is with the thought that sometime in the near future all in the industry can see the wisdom of co-ordinating on specific insulation levels, and agreeing on final values for the various levels so that values somewhat like those of the Sporn-Powel paper may be tabulated as final and no longer as proposed or tentative. If assurance can be given at that time that these values, and only these, will hold for an appreciable period, then both manufacturers and users should have much more peace of mind than they have now with so many ideas prevalent as to station insulation arrangements, and with a mass of so-called "standard" size devices needed to fit all of these arrangements.

J. H. Foote and **J. R. North:** Naturally we are very much pleased at the wide interest shown in the problem of application of the proposed basic insulation levels to situations actually to be met in apparatus selection and station design, as evidenced by the large number of discussions which have been submitted. The encouraging tenor of practically all of these discussions is heartening and makes us feel that progress is being made in the solution of this rather difficult problem. Constructive criticisms and suggestions are given by Messrs. Cooney, Lusignan, MacNeill, Monteith, Montsinger, Oldacre, Rudge, and Smith. Their comments indicate not only interest in the problem, but serious study of the method of attack presented in the paper.

We especially value the very clear and fundamental presentation of the general problem by Mr. Prince. His discussion warrants careful consideration by all who are preparing studies or schedules of application of the basic insulation levels to actual practice. Other discussers have called attention to the necessity for the determination of a proper appraisal of the reliability of service as related to apparatus hazards, and also as related to the costs involved. Some are prone to look at the situation as it affects the apparatus itself and say, for instance that because a piece of apparatus is of small, unit cost and easy to replace, its failure is of small moment. Others, particularly those who live close to the operating problems involved in the maintenance of service to customers, are perhaps a little more conscious of the effect of the failure of a piece of equipment on continuity of service. Mr. Prince brings out the necessity for a proper evaluation of these factors.

We are glad to have Mr. Vogel, who has had a great deal of experience in matters relating to insulation co-ordination, emphasize and amplify some of the points in the paper. Proposals (*a*) and (*b*) were given consideration in setting up the marginal allowances. He points out that the basis for co-ordination of protective-device performance and apparatus impulse strength involves a nice balance between the surges reasonably to be expected and the assuredness of protection by the protective device. Our illustrative examples were intended to show that the basic-insulation-level applications indicated were such as to insure reasonable security without going to extremes one way or the other. The necessity for consideration of low-frequency as well as surge-voltage requirements is again brought to the reader's attention.

Mr. Henry's discussion is worthy of a second reading. He has done a great deal of work in connection with insulation co-ordination and its application, and his study of the problem and experience in the practical application of basic insulation levels to apparatus makes his contribution of more than ordinary value.

Mr. Crary's and Mr. Monteith's discussions emphasize the importance of properly determining the voltages which may be expected to occur under fault conditions.

Mr. MacNeill has very nicely enumerated factors which are particularly applicable to oil-circuit-breaker design where transients and low-frequency voltages, as well as surge voltages, are of great importance. We are sorry, however, that we cannot feel that the manufacturer's name-plate voltage rating, indicating the service voltage for which the apparatus is designed, is necessarily sufficient in actual practice. We appreciate Mr. MacNeill's position and are quite sure that he intends to convey that such certification would be adequate only after the principles set forth in the paper had been applied to the situation by those designing and selecting the apparatus.

Mr. Montsinger makes a very worth-while suggestion, which we endorse—that the definition of basic insulation level be modified to incorporate the word "standard", that is, "suitable standard tests". His suggestion that a factual survey of maximum line-to-line and line-to-ground operating voltages actually existing on operating systems be made is also voiced by Mr. Smith. It is believed that several of the operating companies are already conducting new surveys of these factors to supplement ones previously available and to extend the scope of these investigations.

We welcome Mr. Lusignan's endorsement of conclusion number 3 of the paper that name plates should definitely indicate the insulation strength of apparatus.

In connection with the thought expressed in the discussion by Mr. Rudge that a considerable additional margin between arrester performance and insulation strength may be available in the actual application of arresters, we believe that the correlation of rather moderate surge currents (as compared with the maximum surges which may sometimes occur) with slightly pessimistic arrester performance characteristics afford a reasonable compromise. If average arrester performances were to be taken as suggested by Mr. Rudge, it might be logical to assume a larger margin between arrester performance and the protected level.

Mr. Cooney's suggestion that the characteristics of lightning-surge protective devices and the derivation of the protected level be added to the illustrative examples has been carefully considered, and we realize that the paper would be more complete with this additional information. However, it has been our thought that the companion paper by Messrs. Sporn and Gross on "Rationalization of System Insulation—III" provides these data in their table I and in their formula for a margin of (protective device performance $\times 0.10$) $+25$ kv. The determination of the maximum voltage to ground is in accordance with the methods commonly used as set forth in reference 7 covering symmetrical components. It was felt unnecessary to duplicate this presentation.

Mr. Cooney mentions a point which we hope will not be misunderstood, which is—no "special insulation" has been contemplated, but insulation of a higher or lower standard level is supposed to be selected if a particular strength does not seem to be most economical or entirely suitable.

On the basis of the very general interest exhibited in the practical application of these principles, we believe that many engineers are studying in a logical manner the selection of the basic insulation levels suitable to their systems and preparing studies illustrated by the examples in the paper. This should hasten the general application and adoption of common principles of insulation co-ordination.

Proposed Standards for Outdoor Bushings

Discussion of paper 40-37 by the AIEE joint committee on bushings, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (October section) pages 590–1.

R. T. Henry: See discussion, page 1201.

L. Wetherill (General Electric Company, Pittsfield, Mass.): Anyone who has followed the development of the proposed bushing standards from the study of the various conflicting and inaccurately defined practices used in the past through to the development of the consistent and rational requirements which have been proposed will agree that Mr. Henry is to be congratulated on the accomplishments of his committee.

Perhaps the widest departure from former practices is the complete adoption of the hold or withstand test as a means of demonstrating the external electrical strength of bushings. This type of test has advantages not only in simplicity of definition and interpretation of test results, but also in that it tends to eliminate some of the variation in results which are obtained in making flashover tests and which makes any single flashover test subject to erratic variations.

The variability of the external electrical strength of bushings is illustrated in figure 1 of this discussion, which shows the probability of withstanding a voltage application without flashover as a function of the magnitude of the voltage. The middle curve is based upon data involving both 60-cycle and impulse voltages applied to a considerable number of bushings of various ratings. Occasional flashovers can be expected at 15 per cent above or below normal. The upper and lower curves show that although single tests may deviate as much as 15 per cent, no group of tests would be expected to deviate more than 9 or 10 per cent.

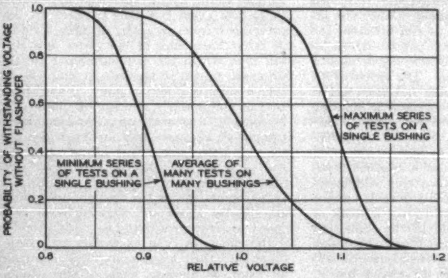


Figure 1. Variation in flashover strength of bushings

Bushing designs should contain margin for some variation in external electrical strength regardless of whether standard tests are of the "flashover" or "withstand" type, but the "withstand" type of test has the advantage that it demonstrates more definitely the presence of excess strength above the standard requirement.

Examination of figure 1 serves to explain why the standards for external electrical strength of bushings have apparently been decreasing in recent years. In past years flashover performance of bushings was expressed in terms of the average flashover strength; corresponding to unity on the scale in figure 1. Any given bushing is as likely to come below the average as above. It was felt necessary to place some restriction on the deviation of individual bushings on the low side and a minus-five-per-cent tolerance was used for this purpose. In recent years, a minimum value five per cent lower than the former average value has been widely used as the basis of specifications. It is seen from the curve that the minimum value should perhaps have been nine or ten per cent lower in order to constitute a more comparable requirement. However, the majority of bushings apparently had insufficient margin of safety to meet the more stringent requirement of a minimum value only five per cent below the former average value.

The introduction of withstand tests is a further increase in the severity of the requirement. At a voltage 15 per cent less than the normal flashover, figure 1 shows that a bushing can be expected to withstand voltage application without flashover in practically all cases. Introduction of "withstand" requirements in place of average flashover specifications is equivalent to 15 per cent increase in severity of test requirement. The kilovolt values have been decreased, in general, by a lesser amount so that the actual requirement is somewhat more severe.

The proposed standards specify test performance on the basis of bushings without gap electrodes. Now that established transformer test procedure no longer requires the use of gap electrodes to adjust the flashover strength of transformer bushings downward to standard values, it is possible to give more consideration to the possible injurious effects of the use of gap electrodes on bushings in service. The presence of gap electrodes on a bushing increases the probability that a flashover will occur adjacent to the bushing porcelain with possible injurious effects. Even in cases where it may be deemed advisable to provide gap electrodes, it would be the part of wisdom that they should be placed several feet away from the bushing, in order to avoid injury to the bushing.

The requirement stated in the proposed standards that bushings shall be suitable for operation at their standard ratings when and where the temperature of the cooling medium does not exceed 40 degrees centigrade should probably have some modification or amplification to cover applications, such as water-cooled transformers, which have different standard ambient conditions. Doubtless this can be accomplished in a subsequent revision.

Some points will not be settled until the proposed test code becomes available. The standard rate of precipitation and water resistivity for low-frequency wet tests are not stated in the proposed standards. The data on which the proposed standards are based were obtained with a precipitation of 0.1 inch per minute and a resistivity of 12,000 ohms per cubic centimeter. These conditions have been standard in testing bushings for a considerable number of years.

There has been discussion of changing the standard conditions to agree with those established for insulator testing which involve a precipitation of 0.2 inch per minute and a resistivity in the order of 18,000 ohms per cubic centimeter. The test performance of bushings would be affected by such a change and a careful study of the data will be required to determine the modifications in kilovolt values necessary to permit such a change and maintain the same degree of severity of requirement. Probably a small decrease in the kilovolt values would be in order.

The question will arise as to what extent the proposed standards should be placed in use at this time. There appears to be no reason why they cannot be used in specifications at this time, when it is desired to do so.

Recommendations for High-Voltage Testing

Discussion of paper 40-38 by the EEI-NEMA subcommittee on correlation of laboratory data, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (October section) pages 598–602.

M. S. Oldacre: See discussion, page 1199.

R. T. Henry: See discussion, page 1201.

A. C. Monteith: See discussion, page 1203.

Theory of Hysteresis-Motor Torque

Discussion of paper 40-80 by B. R. Teare, Jr., presented at the AIEE winter convention, New York, N. Y., and published in AIEE TRANSACTIONS, 1940, pages 907–12.

Wayne J. Morrill (General Electric Company, Fort Wayne, Ind.): Professor Teare has made an important contribution to the theory of hysteresis motors. His work outlines a method for approaching the problem of predetermining hysteresis-motor torque, and the data and conclusions given in his paper should be of considerable value to those actually designing such motors. I think it is no discredit to Professor Teare's work to say that a thorough analysis of all the aspects of hysteresis-motor performance has merely received a good start and Professor Teare's paper leaves open considerable opportunity for further investigation. It is to be hoped that further efforts, extending Professor Teare's work, will be forthcoming at future meetings of the Institute.

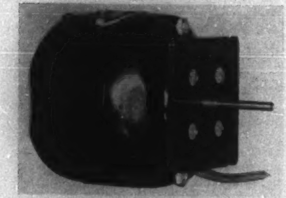


Figure 1. Hysteresis synchronous motor for horizontal operation; three watts (0.004 horsepower), 1,800 rpm, 110 volts, 60 cycles

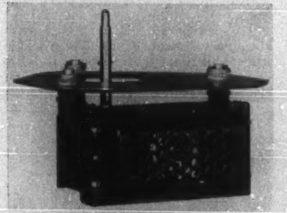


Figure 2. Hysteresis phonograph motor, 0.75 watt, 78.26 rpm, 110 volts, 60 cycles

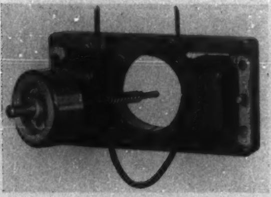


Figure 3. Armature and field of hysteresis phonograph motor

While Steinmetz mentioned and discussed the characteristics of hysteresis motors at the beginning of the century and Telechron and others have produced *modified* hysteresis motors, we are the only ones I am aware of who have produced true hysteresis motors in production quantities. For this reason it might be of interest to show several illustrations of production hysteresis motors. Two of those shown have been produced to the extent of approximately 75,000 units each.

Our experience agrees with that of Professor Teare in that the hysteresis motor is not a competitor of the induction motor in the general fields of motors. General statements are likely to be misleading unless carefully limited but it seems that, in the same size, a hysteresis motor will have from one-fourth to one-tenth the output of an induction motor. This means that the hysteresis motor will, as a rule, be somewhat more expensive than the induction motor.

The hysteresis motor has two outstanding characteristics which cause it to be attractive for certain types of work. Having, as it does, no slots in the rotor, it is not subject to the high-pitch harmonic noises of induction motors. It is, therefore, probably the quietest of all motors. Where extreme quietness is required and cost is not of primary consideration, a hysteresis motor may be used in place of an induction motor.

The hysteresis motor is not merely synchronous but it differs from other synchronous motors in that it has inherently good "pull-in" characteristics. A normal synchronous motor comes up to speed on the induction torque characteristic furnished by its amortisseur winding. Since the induction torque is zero at synchronism, the speed to which the motor can be driven by its induction characteristic is somewhat less than synchronous speed. The synchronous torque characteristic does not exist below synchronism except as transient alternating torques. It follows, therefore, that a normal synchronous motor must be "snapped" into synchronism from its highest induction speed by a single impulse of transient synchronizing torque as the rotating field passes the rotor poles. The ability of such a motor to synchronize is limited by the amount of inertia in the rotor and in the connected load. If this inertia exceeds a certain value the motor will fail to synchronize at all. This is not true of the hysteresis motor, since it has a constant synchronizing torque all the way up to synchronism. For this reason, the hysteresis motor will accelerate all the load that it can carry regardless of the inertia involved. This characteristic of a hysteresis motor might cause it to be used where otherwise a synchronous induction motor would be employed.

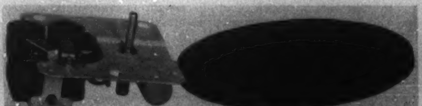


Figure 4. Hysteresis synchronous motor for rim-drive phonograph application

Induction-Motor Characteristics at High Slip

Discussion and authors' closure of paper 40-12 by Theodore H. Morgan, William E. Brown, and Arthur J. Schumer, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 464–8.

Quentin Graham (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper deals with aspects of induction-motor operation which usually have been omitted from elementary textbooks and have had rather infrequent attention in the technical journals. For that reason the authors probably are justified in their references to what they have considered as the conventional methods. I believe, however, that the paper may give a false idea of the actual methods of practicing engineers. In particular the following items may be noted as representative of present-day design practice:

1. The rotor resistance is treated as a variable and appropriate values are taken depending on the slip.

2. The rotor reactance is varied with the slip.

3. The effect of saturation of the leakage paths is calculated so that the correct values are used for locked current and for full-load current.

4. The effects of flux harmonics on losses and torques are considered.

5. When motors are tested actual speed-torque curves are measured over the complete range of positive and negative speeds and at oversynchronous speeds rather than being constructed from no-load and locked-rotor test data.

The method of separating the losses and torques shown by the authors is dependable in certain cases but fails if there are appreciable torques set up by the fifth, seventh, or other harmonics of low order and may fail even where the slot harmonics are the principal source of parisitic torque. If we take a slightly different point of view from that of the authors, the torque curve may be considered as made up of the component torques of all harmonics among which are the slot harmonics. In relating torque to watts loss we must use the appropriate slip for each separate harmonic. Since the synchronous speeds for the slot harmonics are slightly above and slightly below zero speed no great error would be made, usually, if the effects of both harmonics were combined and their losses converted to torque by using an average value of slip. Since their average synchronous speed is nearly zero the average slip becomes the actual motor speed. This is what the authors have done. Suppose, however, that there is a large seventh-harmonic torque present. Obviously this will be overlooked as the loss which it represents will have been referred to the wrong slip. Even in the case of slot harmonics alone, if there are low-resistance rotor circuits available in which the harmonic currents may circulate, the torque curve may have important cusps which would not be detected by the method which has been used in the paper. A torque curve of the latter type is shown in figure 1 of this discussion. This curve was taken on a motor built especially to study such characteristics and is of course an extreme case.

It is true that there are many cases, perhaps most well-designed motors, where the principal torques aside from the fundamental are caused by the slot harmonic currents flowing in relatively high-resistance circuits. These motors will lend themselves to analysis similar to that in the paper. Thus the paper is useful in that it illustrates a rather common characteristic of motors. One of the points to be emphasized is that any appreciable amount of these harmonic torques seriously affects the heating of a motor during the accelerating period. They decrease the acceleration and at the same time add losses. For that reason it is essential to have a speed-kilowatt curve as well as a speed-torque curve in applying motors for frequent starting.

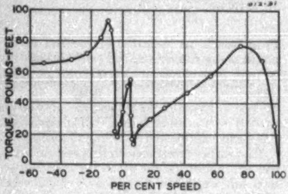


Figure 1

M. M. Liwschitz (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper by Morgan, Brown, and Schumer is a very noteworthy addition to the paper "Reverse-Rotation Test" presented one year earlier by the same authors, because it discloses the possibility of determining in a relatively simple manner the curve representing the actual torque at the shaft of an induction motor.

It is the purpose of the present discussion to point out the necessity of supplementing by additional tests the tests on the four-pole machine described in the paper.

The stray load losses, occurring in an induction machine, consist of five components; two of these loss-components are iron losses, and the other three are copper losses in the rotor bars.

The two components covering the iron losses are:

1. The eddy current and hysteresis losses at the surfaces of the tooth crowns
2. The eddy current and hysteresis losses in the tooth bodies

The three loss-components occurring in the rotor bars are due

3. To the slot openings of the stator
4. To the step harmonics of the stator flux
5. To the phase belt harmonics

If the skin effect can be neglected, the iron losses follow the square of the speed. The copper losses do not follow the square of the speed.

The phase belt harmonics can be especially disturbing. This can be seen from figure 4 of the paper. This figure has a dip at positive speed values in the vicinity of the standstill point, for which the authors' method cannot account. This discrepancy between calculation and experiment is due to the phase belt harmonics, and it is a matter of fact that these harmonics may produce dips of considerable magnitude in the speed-torque curve, especially in the vicinity of the standstill point. Each one of the phase belt harmonics has the same operational effect as the fundamental wave; it should be considered, however, that the slip has different values for the various harmonics. In the vicinity of their synchronous speeds, said harmonics can produce torques of considerable magnitude without appreciable losses.

In Figure 2 of this discussion is shown the speed-torque curve of a two-pole three-phase 60-cycle 20-horsepower experimental motor, having 24 stator slots and 31 rotor slots. The application of the authors' method to this particular motor would not lead to satisfactory results.

I have checked by calculation said speed-torque curve, which was obtained experimentally with the aid of a dynamometer, and have indicated in the diagram the calculated points by crosses. The calculation involved the separate computation of the torque components, produced by the fundamental and by each one of the phase belt harmonics; in these computations, the increase in resistance and the decrease in reactance, due to the skin effect, were properly taken into account. In making the calculation, certain assumptions, concerning the temperature of the rotor winding, had to be made. Taking this into consideration, it can be said that the calculated values check reasonably with the experimental ones.

The effect of the various harmonics on the speed-torque curve is clearly visible in the figure. In the case of this particular motor, the fifth and the seventh harmonics have the greatest influence, while the slot harmonics, the 23d and the 25th, are without effect.

All harmonics produce positive torques at negative speeds; that means torques that have to be added to the torque produced by the fundamental. At the standstill point, the torques produced by the inversely rotating harmonics are all negative, and those produced by the forward rotating harmonics are all positive. At positive speeds the torques produced by all harmonics are negative; that is, they cause the resultant torque to be lower than the torque produced by the fundamental.

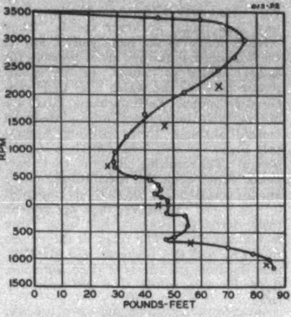


Figure 2. Speed-torque curve (see table I)

The phase belt harmonics may have a harmful effect especially in two-pole machines, although the high torque components that may be produced by these harmonics are not necessarily accompanied by high losses.

For example, at the speed of 1,080 rpm, the slip with respect to the fundamental is 1.3 and with respect to the fifth harmonic −0.5. The torque produced by the fifth harmonic at the slip −0.5 is equal to 30.7 pounds-feet and the corresponding losses are 1,580 watts. Even if we could find the real value of this loss by the method given in the paper, we would calculate a torque of only 11.2 pounds-feet instead of the real value of 30.7 pounds-feet.

On the basis of the foregoing, I recommend the making of supplementary tests, preferably on two-pole machines.

Theodore H. Morgan: From the discussion it would appear that there is some question as to the extent of the harmonic effects which are included in the method set forth in this paper. At the outset it should be understood that, while the effects of harmonics under one general classification are included, the torque which may result from harmonics of a somewhat different nature is omitted. For purposes of explanation and simplicity harmonic effects in induction motors may be divided into two main classifications. First, there are harmonic air-gap fluxes originating in the stator, which remain uniform in magnitude and rotate at constant speed in the air gap. These are excitation fluxes and are of the same nature as the fundamental air-gap

Table I. Twenty-Horsepower Two-Pole 60-Cycle Three-Phase Motor

n(RPM)	S_1	T_1 (Pounds-Feet)	T_5 (Pounds-Feet)	T_7 (Pounds-Feet)	T_{11} (Pounds-Feet)	T_{13} (Pounds-Feet)	$\sum_5^{13} T$	$\sum_1^{13} T$
−1,080	1.3	46.0	30.7	4.3	1.6	0.59	37.2	83.2
− 720	1.2	47.0	0	5.1	2.9	0.62	8.6	55.6
0	1.0	50.0	−14.5	10.3	−3.2	1.6	− 5.8	44.2
720	0.8	54.5	− 8.1	−17.6	−1.2	−1.8	−28.7	25.8
1,440	0.6	62.0	− 5.7	− 6.6	−0.91	−0.56	−13.8	48.2
2,160	0.4	76.0	− 4.0	− 3.1	−0.67	−0.39	− 8.2	67.8

flux. However, they are smaller in magnitude and rotate at lower speeds. They produce torque in the same general way as the fundamental flux and are responsible for sharp changes or "cusps" in the speed-torque curve in the region near their synchronous speeds. Such harmonic fluxes are of negligible importance in well-designed motors although their resulting torques may prove seriously large for those of poor design. The resulting effects of these harmonic fluxes upon the speed-torque curve are not considered in the methods employed in this paper. Whenever they are present their effects are superimposed upon those herein described. The cusps in the curves shown by both Mr. Liwschitz and Mr. Graham illustrate the effects of these harmonic fluxes for cases where they are unusually large.

There is a second family of harmonics which produces a distinctly different type of reaction. Due to rotation the relative position of the teeth and slots of the rotor and stator is constantly changing. The results produced by this change in the configuration of the magnetic path may be quite complex and varied in the production of harmonics. However, in order to understand the over-all effects of such harmonics it is only necessary to remember that they are caused by rotation. Thus all losses associated with such harmonics must necessarily be produced by rotation and result in a retarding torque upon the rotor. This group of losses forms the major component of stray load loss. They are taken into account fully by the methods of the paper and are the cause of torque production of major importance at high values of slip, even in the best designed machines.

Heating of a D-C Armature

Discussion and author's closure of paper 40-79 by Carl J. Fechheimer, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 920–6.

T. M. Linville (General Electric Company, Schenectady, N. Y.): The paper suggests the use of the contact thermocouple for measuring surface temperatures. The thermometer has the advantage of simplicity since any number of them can be placed in position rapidly and read repeatedly in succession with little chance of error. It is conceivable that a number of thermocouples could be attached to the rotor after shutdown and read repeatedly in succession without disturbing them. However, it has always appeared more satisfactory to use thermometers. Therefore, in connection with data relating internal and surface temperatures, it is recommended that thermometers be used to determine the latter. Other methods of measuring surface temperatures include the use of alloys having melting points about ten degrees apart, fastened to the armature in inaccessible spots and covered with thermal insulation, pyrometers, and (for commutators) thermocouples placed in a hole drilled in the face of brushes which are electrically and thermally insulated. In connection with the use of thermocouples, the leads may be brought out through copper and "copnic" slip rings without difficulty, using leaf brushes of the same material.

The paper concludes that average temperature rise can be measured on d-c armatures by resistance. This method has been used many times and gives accurate and valuable results. It can be measured by using a bridge and also by measuring the millivolt drop between two marked commutator bars when current is supplied through the brushes. Either method requires laboratory technique and has not been found to be a practical procedure which can be relied upon as a shop test.

J. F. Calvert (Northwestern University, Evanston, Ill.): Reference to author's figures 9, 10, and 11 shows that it was impracticable to get test data all the way back to time zero. The slope of this curve at time zero if known would be of assistance in extrapolating the curves. In cases where the loss is known (such as in the copper) the slope can be computed. In other cases if the slope at time zero can be taken as approximately correct, then the loss per unit-volume can be computed for the point at which the temperature measurement is made. This latter procedure can be of particular value to the designer when he is interested in the magnitude of high local losses in solid parts.

The mathematical relations which are involved are shown below. Choose the x, y, z rectangular co-ordinates. Then equate for any elemental section $\Delta x\cdot\Delta y\cdot\Delta z$ for any increment of time Δt that

heat generated in the incremental volume = heat stored + heat flowing out − heat flowing in

Or

$$W\cdot\Delta x\cdot\Delta y\cdot\Delta z\cdot\Delta t = C\cdot\Delta x\cdot\Delta y\cdot\Delta z\cdot\Delta t - K\left\{\left.\frac{\partial T}{\partial x}\right|_{x+\Delta x}\cdot\Delta y\cdot\Delta z\cdot\Delta t - \left.\frac{\partial T}{\partial X}\right|_{x}\cdot\Delta y\cdot\Delta z\cdot\Delta t + \left.\frac{\partial T}{\partial y}\right|_{y+\Delta y}\cdot\Delta x\cdot\Delta z\cdot\Delta t - \left.\frac{\partial T}{\partial y}\right|_{y}\cdot\Delta x\cdot\Delta z\cdot\Delta t + \left.\frac{\partial T}{\partial z}\right|_{z+\Delta z}\cdot\Delta x\cdot\Delta y\cdot\Delta t - \left.\frac{\partial T}{\partial z}\right|_{z}\cdot\Delta x\cdot\Delta y\cdot\Delta t\right\} \qquad (1)$$

From which,

$$W = C\frac{\partial T}{\partial t} - K\left(\frac{\partial^2 T}{\partial x^2}+\frac{\partial^2 T}{\partial y^2}+\frac{\partial^2 T}{\partial z^2}\right) \qquad (2)$$

where

W = watts generated per cubic inch at x,
C = heat storage capacity in watt seconds per cubic inch per degree centigrade
T = degrees centigrade (at x, y, z)
t = time in seconds

x, y, and z are the arbitrarily chosen rectangular co-ordinates in inch units.

Assume that steady-state conditions have been attained so that the rate of change of temperature with respect to time is zero. This is represented by equation 4. Then assume that the loss per unit volume is suddenly changed from the steady-state value of W_0, either up or down, to another constant value W_1, and write the expression for the conditions at $t=\Delta t$, where time is measured from the instant when the sudden change in loss occurred. This is shown in equation 3.

$$W_1 = C\left.\frac{\partial T}{\partial t}\right|_{\Delta t} - K\left\{\left.\frac{\partial^2 T}{\partial x^2}\right|_{\Delta t} + \left.\frac{\partial^2 T}{\partial y^2}\right|_{\Delta t} + \left.\frac{\partial^2 T}{\partial z^2}\right|_{\Delta t}\right\} \qquad (3)$$

$$W_0 = -K\left\{\left.\frac{\partial^2 T}{\partial x^2}\right|_{0} + \left.\frac{\partial^2 T}{\partial y^2}\right|_{0} + \left.\frac{\partial^2 T}{\partial z^2}\right|_{0}\right\} \qquad (4)$$

Subtracting (4) from (3) and rewriting the last expression:

$$W_1 - W_0 = C\left.\frac{\partial T}{\partial t}\right|_{\Delta t} - K\frac{\partial}{\partial t}\left\{\left.\frac{\partial^2 T}{\partial x^2}\right|_{0} + \left.\frac{\partial^2 T}{\partial y^2}\right|_{0} + \left.\frac{\partial^2 T}{\partial z^2}\right|_{0}\right\}\cdot\Delta t \qquad (5)$$

Since $\frac{\partial}{\partial t}\left(\frac{\partial^2 T}{\partial x^2}\right)$, $\frac{\partial}{\partial t}\left(\frac{\partial^2 T}{\partial y^2}\right)$, etc., are finite, then as Δt is allowed to approach zero it is found that

$$W_1 - W_0 = C\left.\frac{\partial T}{\partial t}\right|_{0}$$

From which, as indicated in the first paragraph of this discussion, if $W_1 - W_0$ is known, $\left.\frac{\partial T}{\partial t}\right|_{0}$ can be computed; or if $\left.\frac{\partial T}{\partial t}\right|_{0}$ as obtained by test is regarded as sufficiently accurate, then $W_1 - W_0$ can be found. (The constant C is easily established from handbook data.)

Two earlier references on this general subject which indicate the use of the above relation but which do not entirely justify its validity are as follows: "Additional Losses in Synchronous Machines," C. M. Laffoon and J. F. Calvert, AIEE TRANSACTIONS, volume 46, 1927, pages 84–96, and "Time-Temperature Tests to Determine Machine Losses," M. D. Ross, ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), volume 54, 1935, pages 512–15.

C. Lynn (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Fechheimer has presented a very interesting paper, particularly to the designer. The user of electrical machinery is interested in operating temperatures only in so far as these temperatures affect the performance and useful life of his machine.

The relatively low temperature limits that have been and are at present specified for the insulating materials used in industrial d-c machines have been justified by the long trouble-free life the insulating materials have given in the past in these types of d-c machines.

The abandonment, approximately ten years ago, of cast construction for armature spiders as used in the machine of Mr. Fechheimer's experiment and the substitution of fabricated steel permitting better ventilation and the use of stranded conductors, as mentioned in the conclusion of Mr. Fechheimer's paper, has materially reduced the spread between thermometer measured temperatures and actual hot-spot temperatures. These improvements over the past decade will permit industry using smaller and more economical higher-temperature-rated machines in the future without any untoward results. The tendency among users of electrical machines has already been in this direction for the past year or so as indicated by the application of d-c equipment with 60-

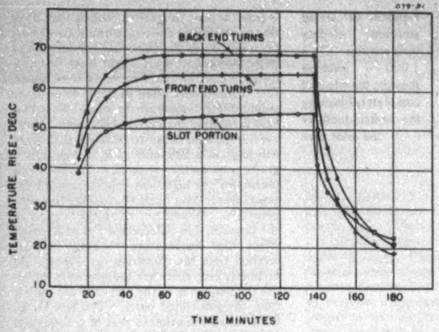


Figure 1. Temperature-time curve of the armature winding on a 1,500-kw d-c machine showing the relative temperatures of end turns and slot portion

degree temperature rise for marine application and for machines with class *B* insulation in some steel-mill installations.

W. E. Jacobsen (General Electric Company, Schenectady, N. Y.): Because of the inherent difficulty of measuring temperatures in d-c armatures, there is little accurate comprehensive data available. Mr. Fechheimer's paper shows one method of overcoming this difficulty and obtaining what is unquestionably accurate information on the magnitude and variation of armature temperatures as they exist in a running machine. His paper, therefore, is a welcome one.

As Mr. Fechheimer points out, the investigated machine was unsatisfactory from a ventilation standpoint and consequently the test results are not representative of a normal machine. It is somewhat unfortunate that this is the case and care should be used in interpreting the results obtained so as to avoid false generalizations.

There are three items in this paper to which I would briefly direct attention. The first concerns the iron-surface time-temperature curve taken with the contact thermocouple and shown in figure 12. Since the temperature difference between the armature core surface and the copper is a maximum at the instant of shutdown, and since this difference grows steadily smaller with passage of time, the resulting heat flow from copper to iron also becomes less with passage of time. Now, at any time after the instant when this flow becomes equal to the rate of dissipation by the external surface, the surface must be cooling and can never again regain temperature because of heat flow from the copper since that flow grows steadily smaller as time goes on. The only method by which the iron temperature could be raised by heat flow from the copper to the iron after it had once started to cool, would be to have the copper temperature itself raised again by the dissipation of more energy. As the curve of figure 12 indicates that the surface had begun to cool after about 4 minutes time, heat flow from the copper to the iron could not produce the rise obtained at 11 minutes time.

The second comment concerns figure 17. The information here presented is interesting because it indicates rather well the difference or gradient in temperature that exists in the copper of a commutator while running. The more rapid response of the contact thermocouple enables more of this temperature gradient to be measured since with it readings can be obtained much more quickly than with thermometers. This is essential since the temperature difference between commutator ends will act quickly to even the temperature throughout the length of the material as soon as the cause of that gradient is removed. The cause is very largely removed by the shutting down of the machine. In extreme cases commutator end temperature differences as high as 30 degrees centigrade have been observed by measuring commutator temperatures at various axial positions while running under load. These measurements were made by arranging thermocouples close to the commutator surface in small air pockets cut out in the faces of insulated brushes. It has been found that the differences between thermometer readings on commutators after shutdown and contact-thermocouple readings or running temperature readings is a maximum for small high-speed commutators and a minimum for large slow-speed units.

The third and last comment concerns figure 19. The curves in that figure show rises in the center of the core portion of the armature winding that are more than twice as high as those occurring at the ends of the coils. This tendency toward high copper temperatures in the core section is aggravated by the faulty ventilation and it is expected that a normal machine would show this tendency to a lesser degree. Since in general the hot-spot temperature exceeds the average or mean winding temperature determined by resistance by an amount approximately proportional to the difference between maximum and minimum temperatures, this difference as observed on the machine investigated by Mr. Fechheimer is larger than would normally be expected. In some d-c armatures where because of high speeds or other considerations it becomes necessary to cover rather completely the end turns with insulation and binding wire, an entirely different heating situation is produced. In this case, the end turns of the armature tend to be considerably hotter than the portion embedded in the core and the transverse heat flow in the copper is from the end turns to the core portion. A part of the test results recently obtained on a 1,500-kw machine equipped with temperature detectors is shown in figure 1 of this discussion. The average indicated rise of the copper of the core section was 54 degrees centigrade, while the front end turns indicated a rise of 64 degrees and the back end turns a rise of 69 degrees. It is interesting to observe that after shutdown the end turns cool at a faster rate than the portion embedded in the core and soon after shutdown are cooler than the core portion. This is to be expected since the larger mass of the core iron cools relatively slowly compared to the copper and is eventually aided in its cooling process by passing heat to the core portion of the winding for dissipation by the cooler end-turn sections.

Most calculation methods for armatures assume that there is no transverse heat flow at some position in the core section, usually the middle. This is not necessarily the case. Certain high-speed d-c armatures for which fairly complete temperature calculations have been made show curves as in figure 2 of this discussion. The mean ordinate of each of these curves was closely checked by test resistance results after rapid shutdown and projection of the cooling curve back to the instant of shutdown. The curves of figure 3 of this discussion, taken on the same machine with changed ventilation arrangements, show the result of shutting off the air to the radial ducts. The average ordinates of these curves were also checked by test results. The similarity of the curve with the ducts blocked to those obtained by Mr. Fechheimer is perhaps interesting. If the end turns on this machine had been more

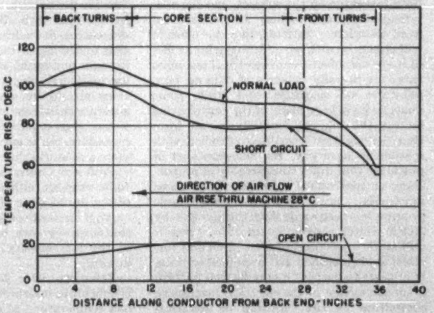


Figure 2. Calculated armature winding temperatures of a 1,000-kw totally enclosed fan-cooled d-c machine

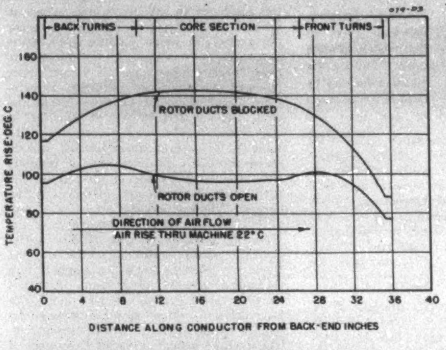


Figure 3. Calculated armature winding temperatures of a 1,000-kw machine showing the effect of completely blocking the radial ducts of the rotor core

exposed making them more similar to those of the machine employed in Mr. Fechheimer's tests, the similarity of the results would have been even more striking.

The machine on which the data of figure 1 were obtained is an open-type medium-frequency machine with moderately heavy overload capacity. The machine on which the data of figures 2 and 3 were obtained is a totally enclosed fan-cooled short-time-duty machine.

In conclusion, it might be well to emphasize the fact that temperature-rise characteristics are not the result of total losses and ventilation only, but are materially affected by the distribution of those losses as well. In a low-frequency unit, the ratio of copper to iron loss might be in the approximate ratio of ten to one while in a high frequency unit this ratio might be one to one. This, together with the construction changes necessary because of changes in speed, make it possible to obtain a wide variety of temperature patterns, and it is difficult, if not impossible, to make satisfactory generalizations on so complicated a problem without having first thoroughly investigated a number of machines covering a wide range of ratings and speeds.

Carl J. Fechheimer: It has been my firm belief for many years that no problem concerned with electrical machines is more intricate and of more importance than heating. Yet comparatively few papers have been presented on the subject, and the analytical and experimental studies are far from complete. Perhaps, like the poor, it will always be with us. Although in the files of every manufacturer of electrical machines there are literally volumes of data on temperatures of machines, and while some analyses have been made of the contributing factors, the data are generally in such form that the engineer is unable to predict, with reasonable accuracy, the temperatures of machines that differ considerably in proportions or mechanical structure from those previously built. Whereas considerable progress has been made since the noteworthy AIEE winter convention in 1913, there is still much opportunity for research and correlation of published and unpublished data. It appears to me that it may be well to form a subcommittee of the electrical machinery committee on the "heating of electrical apparatus". It was my especial purpose in presenting this paper to call attention to a few things associated with d-c machines, in order to try to arouse interest in the important subject of heating. To those who are inclined to be impatient may I call attention to the claim that the great Kepler devoted ten years to the study of Tycho Brahe's astronomical data, and his interpretation led to the enunciation of the three famous Kepler's laws.

Replying to Mr. Linville, at the historic winter convention of the AIEE in 1913 there was much controversial discussion on the use of embedded detectors in stators of a-c machines; but, notwithstanding the opposition, their use in large a-c stators has become standard. During the many years that have elapsed since then, comparatively little attention has been paid to methods of surface temperature measurements, and the thermometer is used today as it was then. In general, the thermometer takes considerably longer time to attain its maximum than does the contact thermocouple, and the surface will have cooled appreciably more before the maximum is reached with thermometer than with contact couple. Perhaps the subject could be discussed to advantage in meetings of the committees on test codes. Alloys having various melting points and color-changing paints have been considered frequently for temperature measurements but I was not aware that either had been placed on a practical basis for application to armatures. It would be of value for Mr. Linville to publish some results of his investigations with alloys having ten-degree differences in melting points. One objection to the use of thermocouples with leads brought out to slip rings is that many rings are needed if temperatures at various locations are to be measured. I agree that accurate measurement of d-c armature resistance has so far been almost a laboratory procedure, but it may be possible to place it upon a practical shop basis.

Professor Calvert's suggestion may prove to be valuable, either for the determination of the initial slope of the temperature-time curve if the loss per unit volume just before shutdown is known, or for the determination of the loss density if the initial slope is known.

Referring to Mr. Jacobsen's discussion, it is difficult to interpret the second rise in surface temperature, as shown in figure 12. It is possible that the rate of cooling may have decreased during the test without its having been noted; as the tests were conducted in a large building, a draft might have been reduced while the measurements were made. It is undoubtedly more valuable to measure commutator temperatures while the armature is running than after shutdown. However, it does not seem to be likely that there was as great a difference as 30 degrees centigrade between the two ends, as Mr. Jacobsen found, for the high heat conductivity of the commutator bars tends to equalize the temperatures. Would not the local heating in the brushes, due to localized I^2R or friction, affect the temperature reading, by the method that Mr. Jacobsen employed? As he points out, there are armatures in which the temperatures of the coil ends are higher than the embedded parts. The late G. E. Luke called attention to that in a discussion of a paper that I presented some years ago (AIEE TRANSACTIONS, 1921, page 653, figure 2). The irregular forms of the curves shown in Mr. Jacobsen's figures 2 and 3 are interesting, and they indicate how ventilation can affect materially the distribution of temperatures.

There is no doubt that the difference between the bare copper and surface temperatures has been materially reduced, as stated by Mr. Lynn, by improving ventilation and by better stranding of the armature conductors.

Electric Couplings

Discussion and authors' closure of paper 40-46 by M. R. Lory, L. A. Kilgore, and R. A. Baudry, presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940 (August section) pages 423–8.

C. C. Hutchins (Elliott Company, Pittsburgh, Pa.): The authors' paper on electric couplings is very timely and several points in connection with the design are very interestingly treated.

As a matter of general information it might be of interest to call attention to certain earlier work on electric couplings which is referred to in a general way in the author's paper. Prior to 1919, a considerable amount of work was carried on in an experimental way by M. E. Thompson of the Elliott Company and later two patents were issued to him, one of them particularly interesting, number 1,481,655 granted in January 1924. In 1920, 1922, and 1925 three couplings of the type being investigated were built, one was 750 horsepower at 400 rpm, another 1,000 horsepower at 425 rpm, and the third 2,500 horsepower at 375 rpm. It is interesting to note that these were being considered for the purpose of ship propulsion. While the greater emphasis was at that time placed on synchronous locking features, yet the equipments were (according to the records of tests conducted at the Brooklyn Navy Yard) capable of developing sufficient torque at high slips to start the Diesel engines used in connection with them. It is interesting that much of this earlier work covered a surprising number of the ideas being intensively developed at the present time.

In the last paragraph of the section "Other Types of Electric Couplings," the statement is made that a synchronous coupling can be built by using salient poles and d-c excitation on both members. Several couplings of this type have been built and were the subject of a large amount of experimental work by Mr. Thompson and a considerable amount of valuable and interesting information was developed from the shop tests.

Anyone who has worked out designs for slip couplings of the more recent type will agree with the authors' statement regarding the difficulty of the design problem. While the photograph, figure 4, of the paper is too reduced in size to show the squirrel-cage winding in detail, yet having seen the rotor itself, it appeared that the cage winding being used was somewhat more elaborate than an ordinary double-deck design. Possibly it could be described as being a combination of a double-deck squirrel-cage winding with the lower layer of bars proportioned to secure the benefit of what is normally considered a deep-bar rotor.

Elliott Company electric couplings employ double cages in some designs and triple cages in others, depending upon the torque characteristics desired.

The computation of the slip-torque curve, particularly over the unstable range, that is, at slip values beyond the breakdown point, is quite complicated, especially as it is necessary to treat the demagnetizing and cross-magnetizing effect of the squirrel-cage winding on the field which requires the development of some means for taking into account saturation effects.

In appendix *A* of the authors' paper there is a rather interesting idea presented, the analysis being based upon electrical considerations alone. It is of interest to compare this method of analysis and the solution on the basis of the assumptions included with an analysis based on an entirely different set of assumptions published in the *Transactions* of the British Institute of Marine Engineers, volume 49, 1937, entitled "Electromagnetic Slip Couplings for Use With Geared Diesel Engines for Ship Propulsion", by G. L. E. Metz and Nils Erickson. Possibly this paper could profitably be included in the authors' list of references.

That the coupling has a very slight spring action due to its electrical characteristics has been observed during the test of some units when the inertia of the members and the frictions in the test setup have been sufficiently low so as not to mask the effect completely. We would like to ask the authors if, in their opinion, when the effects of inertia, bearing frictions, etc., are taken into account whether the combination of these effects is not so much larger as to make the particular electrical effect described in appendix *A* comparatively of not very great significance.

The Elliott Company's experience in this connection has been that when dealing with a coupling of the synchronous locking type, the spring action is of sufficient magnitude as to be of considerable importance, but when dealing with a slip coupling the amount of spring action which can be observed is so small in comparison with the amount of torque transmitted that in the usual cases little attention need be given to it. This is not to say that torque variations will not be transmitted through the coupling, for experience shows that they will, however, in so passing, the magnitude is generally reduced to a value seldom in excess of two to three per cent for comparatively low frequencies. It would be of interest to hear the authors express a further opinion upon this point.

The paper would be somewhat more readable if a collected list of symbols and definitions were included.

M. M. Liwschitz (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The spring torque of the electric coupling is something remarkable in the paper of Lory, Kilgore, and Baudry, because it means a synchronizing torque in an induction motor, but it is not so surprising if we consider more exactly the synchronizing torque of the synchronous machine. In a paper published in 1933 (*Wissenschaftliche Veröffentlichungen aus dem Siemens-Konzern*, volume 12), I derived for the synchronizing torque of the synchronous machine the equation given at the bottom of figure 1 of this discussion. It consists of four terms. Some symbols are not usual and must be explained:

N is the number of turns (of the field winding or of the primary winding per phase)
τ is the leakage-coefficient

$$\tau_{1d}=\frac{X_1}{X_{ad}} \qquad \tau_{1q}=\frac{X_1}{X_{aq}}$$

ω is equal to 2π times the line frequency
z is equal to 2π times the frequency of the oscillation
X_L reactance between machine and line

The curves relate to a synchronous motor with a rating 2,660 kva, $\cos\phi=0.9$ leading, 50 cycles, 125 rpm.

The curve T_s is the synchronizing torque of this machine, representing the sum of the four terms of the equation. It is plotted over $\cos\phi$, for leading current on the right, for lagging current on the left.*

The curve $\frac{n_{nat}}{n}$ represents the ratio between the natural frequency expressed in revolutions per minute and the normal revolutions per minute of the machine. The crosses are tested values. The calculation checks well with the test.

We consider now separately each of the four terms of the synchronizing torque. The first and the second terms are well known. Professor Arnold derived them more than 30 years ago. Both of these terms are connected with the angle θ_{av} between the line vector and the vector of the induced electromotive force. They are independent from the frequency of the oscillations.

The third and the fourth terms are proportional to $\sin^2\theta_{av}$ and $\cos^2\theta_{av}$ and depend on the leakage reactances of the primary and secondary windings. Both of these terms depend also on the frequency of the oscillations and are equal to zero when the frequency of the oscillations is zero. For reasons which I will give, I use for the sum of the first two terms the sign T_{syn} and for the sum of the last two terms the sign T_{asyn}.

The distribution of the synchronizing torque between these two parts is shown on the slide. The asynchronous part of the torque increases with decreasing field current and becomes very important for lagging power factors. At a lagging power factor equal 0.9, the asynchronous part of the torque has the same value as the synchronous part.

In order to see better the meaning of the third and the fourth terms, we assume that the machine is symmetrical with respect to both axes. Then the third and fourth terms can be combined and expressed by one term only, which will be independent from the angle θ_{av}. This term will not have anything of the properties of the synchronous machine. I wrote in the paper mentioned above:

> "The synchronous machine has a synchronizing torque only, when the angle θ_{av} which corresponds to a fixed load is being changed. If this change of the angle θ_{av} goes on very slowly, this means, if the frequency of the pulsations is very small, the synchronizing torque will be expressed by the first two terms given in the equation on the figure. If the pulsations are not slow, then to the synchronous state of the machine will be superimposed an asynchronous state and the synchronizing torque will be increased by the wattless power, which corresponds to the leakage reactances of all windings. To the stabilizing effect of the main flux will be added the stabilizing effect of the leakage flux of the stator winding and of the rotor winding."

There is no reason why the synchronizing torque that corresponds to the third and fourth terms, that means the asynchronous part of the synchronizing torque, cannot exist in the induction motor. Indeed it is possible to derive a general equation for the synchronizing torque of the induction motor. This equation with sufficient accuracy is

$$T_s^{watt}=\frac{m_1E_L^2}{4}\times$$

$$\left\{\frac{[X_1+(1+\tau_{1d})X_{2d}]\frac{1}{1+\tau_{1d}}}{\left[(1+\tau_{1d})r_{2d}\frac{\omega}{\alpha}\right]^2+[X_1+(1+\tau_{1d})X_{2d}]^2}+\right.$$

$$\frac{[X_1+(1+\tau_{1d})X_{2d}]\frac{1}{1+\tau_{1d}}}{\left[(1+\tau_{1d})r_{2d}\frac{\omega}{\beta}\right]^2+[X_1+(1+\tau_{1d})X_{2d}]^2}-$$

$$\frac{2[X_1+(1+\tau_{1d})X_{2d}]\frac{1}{1+\tau_{1d}}}{\left[(1+\tau_{1d})\frac{r_{2d}}{s}\right]^2+[X_1+(1+\tau_{1d})X_{2d}]^2}+$$

$$\frac{[X_1+(1+\tau_{1q})X_{2q}]\frac{1}{1+\tau_{1q}}}{\left[(1+\tau_{1q})r_{2q}\frac{\omega}{\alpha}\right]^2+[X_1+(1+\tau_{1q})X_{2q}]^2}+$$

$$\frac{[X_1+(1+\tau_{1q})X_{2q}]\frac{1}{1+\tau_{1q}}}{\left[(1+\tau_{1q})r_{2q}\frac{\omega}{\beta}\right]^2+[X_1+(1+\tau_{1q})X_{2q}]^2}-$$

$$\left.\frac{2[X_1+(1+\tau_{1q})X_{2q}]\frac{1}{1+\tau_{1q}}}{\left[(1+\tau_{1q})\frac{r_{2q}}{s}\right]^2+[X_1+(1+\tau_{1q})X_{2q}]^2}\right\}$$

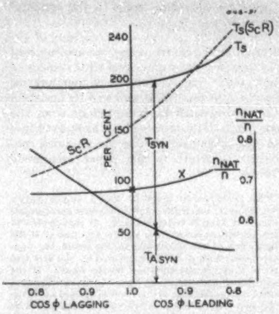
s is the slip of the machine. Furthermore,

$$\alpha=s\omega+z \qquad \beta=s\omega-z$$

---

* The curve $T_s(ScR)$ is the product of the short-circuit ratio and the normal output of the machine. This product is sometimes used as synchronizing torque.

For the derivation of this equation, the assumption was made that the rotor is not symmetrical with regard to both axes (synchronous motor running with the slip *s*).



$$T_s^{\text{watt}} = \sqrt{2}E_L I_f \frac{N_f}{N_1 K_d K_p} \frac{X_{ad}}{X_d + X_L} \cos\theta_{av} + m_1 \frac{E_L^2}{X_d}\left(\frac{X_d}{X_q} - 1\right)\cos 2\theta_{av} + m_1 E_L^2 \times$$

$$\frac{[X_1 + (1+\tau_{1d})X_{2d}]\dfrac{1}{1+\tau_{1d}}}{\left[(1+\tau_{1d})r_{2d}\dfrac{\omega}{z}\right]^2 + [X_1 + (1+\tau_{1d})X_{2d}]^2} \times \sin^2\theta_{av} + m_1 E_L^2 \times$$

$$\frac{[X_1 + (1+\tau_{1q})X_{2q}]\dfrac{1}{1+\tau_{1q}}}{\left[(1+\tau_{1q})r_{2q}\dfrac{\omega}{z}\right]^2 + [X_1 + (1+\tau_{1q})X_{2q}]^2} \times \cos^2\theta_{av} = T_{\text{syn}} + T_{\text{asyn}}$$

Figure 1

2,660 kva, cos $\phi = 0.9$, 50 cycles, 125 rpm
100 per cent $T_s = 2{,}660$ kw

For the normal asynchronous machine, $X_{2d} = X_{2q}$, $\tau_{1d} = \tau_{1q}$ and only three terms will be between the brackets.

For the symmetrical synchronous machine, $X_{2d} = X_{2q}$, $\tau_{1d} = \tau_{1q}$, $s = 0$, $\alpha = -\beta = z$ and the synchronizing torque will be

$$T_s^{\text{watt}} = m_1 E_L^2 \times \frac{[X_1 + (1+\tau_1)X_2]\dfrac{1}{1+\tau_1}}{\left[(1+\tau_1)r_2\dfrac{\omega}{z}\right]^2 + [X_1 + (1+\tau_1)X_2]^2}$$

This is exactly the same value that we will find for the symmetrical synchronous machine from the equation given on the figure.

Frank M. Lewis (nonmember; Massachusetts Institute of Technology, Cambridge): The electric slip coupling is a very effective device for isolating a gear reduction from the vibratory torques of a Diesel engine. For all practical purposes it can be assumed that such isolation is complete. Their action in this respect is very similar to that of the Foettinger hydraulic couplings, and in any given installation the question will arise as to which is the most suitable device.

With the gears isolated from the engine there will remain critical vibration of the system comprising the engine and the attached member of the coupling. If the engine-attached member of the coupling, either electric or hydraulic, can be given an appreciable amplitude of vibration relative to that of the engine cranks, that is, if the nodal point can be moved up into the engine, then the couplings are effective in damping criticals of the engine coupling system, and an arrangement entirely free of criticals can be constructed.

It has been possible to build hydraulic couplings of sufficient lightness to accomplish this. The requirement is that the polar inertia of the engine-attached coupling member be of the same order of magnitude as the total polar inertia in the engine. Information as to how nearly the electric couplings can meet this condition would be of interest.

The authors mention synchronous couplings. Several of this type were constructed by the Sperry Gyroscope Company and the Elliott Company about 15 years ago, but were not successful. One great difficulty is that a low-speed critical develops due to the elasticity of the coupling. In passing through this, vibration torque may build up to such an extent that the breakaway torque is exceeded, resulting in a runaway.

B. C. Seaman (Elliott Company, Ridgway, Pa.): The following points may prove of interest to those making a study of electric couplings.

It should be noted that the method of operation proposed on the more recent couplings mentioned by the authors is somewhat different than that employed on earlier ships. The Maritime Commission's requirement of 75 per cent torque for any slip up to 140 per cent is based on de-energizing the couplings, reversing the engines, and then re-energizing the coupling to pick up the propeller.

Published data on the Swedish couplings show that these couplings developed torques as low as 20 per cent at a slip of 100 per cent and could pick up the propeller from standstill only after the engine had been reduced to a very low speed. These couplings were left energized after the propeller had been picked up and all maneuvering done with the engine.

Even when designed for the maximum torques there is, of course, a certain lapse of time between energizing the coupling and the time the propeller is reversed and brought up to the engine speed. With certain types of Diesel engines, the manufacturers state that considerably greater than full-load torque is available when reversing on the air and that a quicker reversal despite the inertia effects may be effected by having the coupling energized.

Prolonged operation at high slips and comparatively high torques results in considerable heating of the squirrel-cage member. However, it has been demonstrated that with a properly designed squirrel cage, such operation may be continued for at least a half hour without injury to the coupling. The ability of this type of coupling successfully to withstand such severe abuse gives an indication of its ruggedness and of the freedom from trouble that may be expected.

It is suggested that the use of field coils wound directly on the pole would facilitate replacement. The complete coil and pole could be carried as a spare and it would not be necessary to take particular pains in adjusting the pole insulation when repairs were being made in a limited time.

It is noted that the couplings described by the authors employed one-piece circular punchings shrunk on the rim of the spider. Does not this construction require careful turning of the completed stack or the use of very large punches and dies? Our experience indicates that the more usual segmental construction can be used to advantage providing the stack is securely anchored to the spider rim.

A gap of one-fourth inch was selected for the couplings listed as item 2, table I, as a conservative value. Later couplings for ship service have used this value although earlier couplings, particularly those of Swedish manufacture, have used much smaller gaps. It should be noted that the use of a small air gap quite frequently requires that a pilot or centering bearing be used. May we inquire if the 3,000-horsepower coupling listed as item 1, table I, used an air gap of this magnitude?

It should be noted that through the use of special forms of squirrel cage and the proper proportioning of the coupling, variable-speed couplings can be constructed with a full-load slip as low as 3 per cent. This gives an over-all efficiency that compares favorably with the hydraulic coupling.

Reports indicate that some trouble was experienced with certain of the Swedish couplings owing to the field-coil construction employed. It has also been indicated that bushed pins were employed for emergency drive and that under this operation the pins deformed and presented difficulties when their removal was attempted.

The couplings listed under item 2, table I, were equipped with spring plates for emergency drive in order to eliminate this difficulty. No reference is made in this paper to emergency drive provisions and it would be interesting to know what type the authors used.

M. R. Lory, L. A. Kilgore, and R. A. Baudry: In reply to the discussion of C. C. Hutchins, we agree that the general idea is not new and in fact the paper mentions some patents as early as 1891. We are also in agreement that the design problem is difficult, however, the authors by this statement did not intend to imply that there was any part of the electric-coupling performance which could not be calculated with reasonable accuracy.

The reference to a paper in the British Institute of Marine Engineers was overlooked; however, reference 2 is by Nils Erickson, one of the same authors. Not only are the assumptions of this paper different, but they are in error in that the electrical torque is taken to be proportional only to the relative velocity, whereas in appendix *A* the electrical torque is shown to be a function of the slip frequency plus and minus the forced frequency. More important still is the difference in the conclusions; in reference 2 it is stated that "It is quite impossible for any resonance phenomena to occur in a coupling of this type," whereas the authors show in appendix *A* that there is a spring action and that the coupling alone would have a natural frequency.

The significance of the effect described in appendix *A* is questioned. The authors agree that in most Diesel-engine drives under normal conditions the slip coupling will transmit a negligible amount of engine-torque pulsations. However, the propeller torque impulses are of much lower frequency and more likely to occur in or near the natural frequencies of the electromechanical system formed by the propeller, gear, and coupling inertias and the shaft and coupling flexibilities. Also during abnormal conditions uneven firing may result in lower-frequency components of engine torque of considerable magnitude. For these reasons it was thought desirable to study the problem thoroughly and present a method of calculation so that any proposed installation might be checked to avoid any possibility of trouble. Given the inertias and spring and damping coefficients of the coupling, the marine propulsion designer can readily include these effects in his calculations.

In reply to B. C. Seaman's questions, the use of coils wound directly on the pole would make little difference in the ease of replacement. The largest part of the labor involved is in removing and replacing the pole and the extra work involved in putting a new coil on the pole is small. We use coils of edge-wound copper strap whenever possible in spite of their greater cost because of their superiority in mechanical ruggedness.

The core construction employing one-piece circular punching was used to reduce weight. We are now building the largest electric couplings ever made and are using segmental punchings because sheets big enough to make one-piece punchings are not available. The extra depth required to secure the segmental punchings together and to the rim results in increased weight for the same strength.

The air gap used in the 3,000-horsepower coupling, item 1, table I, was 0.143 inch. Large air gaps have operating advantages but result in increased weight, and the gaps used represent compromises between these two considerations. In ship drives the use of a pilot bearing should be avoided since by preventing displacement, it destroys one of the operating functions of the coupling. Also such a bearing may be inaccessible and hard to maintain.

Provision for some form of mechanical drive in emergencies has been incorporated in a few electric couplings. Mr. Seaman mentions one installation made by his company and we have been told that A.S.E.A. furnished this feature on couplings for one customer, but do not furnish it on their standard couplings. We have not furnished it because we do not feel that it is necessary. Unless all of the couplings on the vessel had been practically destroyed, this feature would not be of any use. The increased reliability of multiple-engine drive also extends to the couplings. Any emergency mechanical drive is certain to transmit vibrations which may damage the gear. It should be noted that failure of the excitation source should not be included in the type of emergency in which a mechanical drive might be used as such failure would also shut down the engine auxiliaries which are motor driven on the vessels for which we have supplied couplings. They derive their power from the same source from which the couplings take excitation so that if this source failed, the engines could not be operated at all.

Doctor Liwschitz makes an interesting comparison between the synchronizing torque in the coupling and the additional synchronizing torque of a synchronous motor due to induced currents in the damper winding.

Doctor F. M. Lewis calls attention to the interesting possibility of making one member of the coupling with sufficiently low inertia to move the nodal point up into the engine and obtain effective damping from the coupling. By building the field on the outer member and the cage winding on the inner member it is possible to do this. In fact, the inertia of the inner member of the coupling described in this paper is slightly less than that of the engine.

Starting Performance of Salient-Pole Synchronous Motors

Discussion and author's closure of paper 40-3 by M. M. Liwschitz presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940, and published in AIEE TRANSACTIONS, 1940, pages 913–19.

C. C. Hutchins (Elliott Company, Pittsburgh, Pa.): Every engineer who is engaged in the design of synchronous motors is confronted with the necessity of acquiring a method of calculating the speed-torque and current-torque starting characteristics of a particular motor from its dimensions. Prior to the appearance of the two papers mentioned by the author, there was available only a moderate amount of useful published information in this particular branch of electrical design. Each individual engineer had, however, accumulated certain methods and criteria applying to his particular style of machine. The collection of such information is rather a formidable task as the author has stated.

In reading the present paper, one is necessarily obliged to remember that while the general mode of attack is indicated, the detail involved in carrying it out is left, as is usual, to the skill of the individual reader.

It appears that the procedure recommended in the paper can be considered as a simplifying modification of the more elaborate method described by Linville with certain interesting additions, as for example the breadth factors which are introduced.

Having used a somewhat similar but less condensed procedure during the past several years, there are certain points which naturally have attracted my attention.

The first is in regard to the "equivalent" cage bars of the starting winding. It would seem that provided these bars are chosen of different materials and so located in the pole heads as to have different reactances, that the method employed in determining the nature of the equivalent bar would be the governing factor in the success of the method. It would also appear to be desirable to so determine the equivalent bar that the effect of a rearrangement of the positions of bars of different materials in the pole heads would not be masked in the process. Experience indicates that the relative location of bars of different materials has a great influence on the motor performance. Perhaps the author would be willing to comment more fully upon this particular point.

Noting that a number of simplifyng assumptions are made as must, of course, always be the case and that some of these assumptions must necessarily be quite far reaching the author's comment in the section "Torques and Current Equations" excites interest. Almost any designing engineer would probably agree that a check of torque and average line current over the starting cycle of a synchronous motor within plus or minus five per cent or even plus or minus ten per cent is good, remembering not only the simplifying assumptions but also construction variations, as well as possible discrepancies in testing. It would be interesting to have an expression from the author as to what he considers the probable limit of accuracy of prediction of these characteristics.

Equations 18 and 19 appear to be derived using a form of vector diagram similar to that sometimes employed to determine an equivalent resistance and reactance for the rotor of a double-deck squirrel-cage induction motor.

It would seem that the computation or allocation of the reactance X_m to be used here could become a fairly complicated task.

It is understood that the assumptions listed in the paper are sufficient justification for employing the method indicated. In making a practical computation however, in the past it has been generally true that many of the motor "constants" must be "shaded" judiciously in one direction or the other in a manner varying with the type of machine, degree of saturation, etc., in order to bring results into agreement with test. The present paper indicates that this is the case with most of the constants involved. X_m is probably a particularly good illustration as one can see at a glance that the values of r_{2d} and x_{2d} will be considerably affected by any modifications made in its value.

It is very evident that the author has devoted a large amount of time and effort to the development of the material presented

A. H. Lauder (General Electric Company Schenectady, N. Y.): Mr. Liwschitz mentions that the representation of a multiple-bar amortisseur by a single equivalent winding in each axis of the machine has been known to the industry for some time. The equivalent circuits shown in figures 4 and 5 were presented in almost the same form in a thesis by Davidson,[1] but so far have not appeared in an AIEE publication.

These circuits are an aid to rapid calculation but, when applied to the case of salient poles, a certain amount of experience is necessary in order to predict how much the actual performance of the machine will deviate from the results obtained by the solution of the circuits. The circuit constants can be accurately evaluated for the induction motor because the distribution of amortisseur current is approximately sinusoidal. When the treatment is extended to include salient poles, assumptions must be made which base the circuit constants on a current distribution somewhat different from that existing in the actual amortisseur winding.

With reference to figure 5, attention should be called to the fact that the mutual reactance of the quadrature axis amortisseur with respect to the armature will be slightly less than X_{aq} when the number of bars per pole is even. Likewise in figure 4 the mutual of the amortisseur with respect to the armature will deviate from X_{ad} as the pole-tip bars are moved toward the center of the pole. The mutual reactance of the amortisseur with respect to the field may not be the same as that with respect to the armature. Recognition of this fact is indicated by equation 20. However, equations 18 and 19 do not represent the equivalent resistance and reactance of the rotor circuits of figure 4 except for the case where X_m as defined by equation 20, is equal to zero.

The expression for average torque, equation 4, is approximate as evidenced by the fact that it becomes independent of X_{ad} and X_{aq} when X_1 approaches zero. The final equations in appendix *C* agree in form with appendix 5, reference 2 of this discussion. Equation 9 of this paper and 98 of reference 2 agree in detail, which would seem to confirm this part of the author's analysis.

I agree with the author that if a machine is allowed to attain maximum speed as an induction motor, and excitation is then applied at the most unfavorable angle, more load will be synchronized than will be possible under the conditions defined by equation 10 and the torque-speed curve with the discharge resistor short-circuited. The latter criterion applies specifically to the case where the field is closed prematurely with a load such that the motor can accelerate to the slip defined by equation 10 and operate there until the field is fully built up, at which time it will just be able to synchronize. It has been used in the past to determine the synchronizing ability from the worst angle because it was known to be pessimistic and especially because it could be easily checked by test without connecting the motor to a load. Experience has shown that the accurate prediction of a motor's synchronizing ability from the worst angle by direct calculation is a lengthy process. Empirical formulas have also been found to be less reliable, when applied to machines of radically different torque characteristics, than the previously described criterion for the worst condition increased by an experience factor.

It is believed that the value of the author's paper would be enhanced if in his closure he included more of the reasoning used to justify the derivation of the breadth factors or gave an example of their use in calculating one of the circuit constants.

REFERENCES

1. STARTING PERFORMANCE OF SYNCHRONOUS MACHINES, M. W. Davidson. A thesis presented to the Massachusetts Institute of Technology, 1927.

2. SALIENT-POLE MOTORS OUT OF SYNCHRONISM, A. H. Lauder. ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), June 1936.

S. B. Crary (General Electric Company, Schenectady, N. Y.): Mr. Liwschitz in the section "Influence of the D-C Excitation" has presented an interesting physical explanation of a component of negative damping torque which is proportional to the square of the field magnetomotive force. This component of negative damping has ordinarily been neglected by previous investigators. However, the complete negative-damping phenomena apparently cannot be explained in this relatively simple manner as there are additional effects as shown in the papers by Nickle and Pierce, and Wagner on negative damping. An interesting feature of the component of negative damping pointed out by Liwschitz is that if the resistance is increased sufficiently the damping torque due to this effect becomes positive, indicating a worst value of resistance for given values of reactance.

M. M. Liwschitz: Mr. Hutchins mentions the paper of Linville, Mr. Lauder the paper of Davidson, and I call the attention to the fact that I used the method of treating the unsymmetrical damper winding given in the paper on another occasion in 1921 (*Archiv für Elektrotechnik*, page 96), while the paper of Mr. Linville was published in 1930 and that of Mr. Davidson in 1927.

I agree with Mr. Crary that my paper does not give a complete explanation of the negative damping phenomena in the synchronous machine. My paper treats the starting torques and I mentioned the negative damping only incidentally as far as the speed torque curve due to the excitation shows the possibility of such damping.

Some of the remarks by Mr. Lauder and Mr. Hutchins could be avoided, if my paper would treat not only the method of calculating the starting performance, but also the method of calculating the constants. This was not possible with regard to the space available.

For calculation of the equivalent resistance and reactance of the rotor circuit, figure 4, the equations 18 and 19 must be used. The expression of the torque, equation 4, can be denoted as exact for medium size and larger machines. The influence of the relative location of bars of different materials can be taken into account by combining the chord factor with the resistivity of the winding in consideration. The influence of X_m on r_M and x_M is low. I do not agree with Mr. Hutchins that the constants must be shaded judiciously for different types of machines. As to the limit of accuracy of the calculation, it must be remembered that even for the asynchronous machine ±5 to 10 per cent is a good result. For the synchronous motor, I consider ±5 to 10 per cent at standstill and ±15 per cent at low slip as reasonable results. It must be taken into account that the locking torques at standstill and the asynchronous torques due to the harmonics exist also on the synchronous motor, although they are lower in this motor than on the asynchronous motor.

Rating of Potential Devices and Suggested Material for a Standard

Discussion and authors' closure of paper 40-94 by J. E. Clem and P. O. Langguth, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940 (December section) pages 676–80.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): To me the most valuable part of the paper presented by Messrs. Clem and Langguth is the appendix. It indicates, that through the experience gained from several years of use, the industry is now ready to standardize performance, and the user can now specify the class of device he needs from published data. While there may be suggested revisions in this material before its final adoption as a standard, the fact that ratings and performance characteristics have been established, at least tentatively, is indicative of the progress that has been made in the design of potential devices capable of meeting all the requirements in their fields of application. It is certainly to be hoped that with these data available, full use will be made of them for specifications, and that the tentative standards will be followed except where there is conclusive evidence that they are not suitable.

Among the important applications of potential devices is for supplying potential to protective relays. Application requirements for this purpose have been presented to the Institute previously, but it seems fitting to review them briefly now in comparison with the proposed standards. Condensing the desired performance data previously presented,[1] we find the following permissible errors:

Type of Relay	Permissible Error in Ratio (Per Cent)	Permissible Error Phase Angle (Deg)
Phase directional	20–30	20–30
Balanced current with voltage restraint	30	Not particularly important
Ground current directional relay	15	15
Carrier pilot relays	20–30	10
Distance relays	5	5–15

While there can be no question but that the suggested class *A* device is the best from all standpoints, it can be seen by comparison with the above tabulation that the class *B* is entirely suitable for most relay requirements.

It has long been my contention that the first requirement that the relay engineer should place on a potential device, is that its errors be defined so that, where possible, the relay settings could be made to compensate for them. Now that this information is fully available, it seems to be the right time to point out that where distance relays are used, more effective coverage of the line can be obtained with the higher-accuracy device, and that particularly on lines of considerable length the greater stabilization of the reach of the first zone of the relay (in terms of miles of line) would certainly justify the use of the class *A* device.

REFERENCE

1. APPLICATION OF CAPACITANCE POTENTIAL DEVICES, J. E. Clem. AIEE TRANSACTIONS, volume 58, 1939 (January section).

E. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have provided the basis for a much needed standardization and it is to be hoped that further work will result in similar proposals in regard to bushing operated devices.

It will perhaps be of value to point out some of the critical application situations in which the performance features being standardized have significance. This should enable those reading the paper to better evaluate the significance of the proposed standards in the light of the applications with which they are concerned.

INITIAL ADJUSTMENT

The fineness of steps of voltage and phase-angle adjustment limit the refinement to which the device can be set at exact ratio and phase angle when connected to a given burden. These standards of adjusting refinement are indicated in the rows corresponding to 100 per cent voltage in the tabulations of voltage and burden variation. In considering the necessary fineness of steps, the method of adjusting the devices and the standard voltage with which they are compared should be considered. In some cases the output voltage can be compared directly with a potential transformer and the accuracy of comparing two meter readings is involved. In other cases it is necessary to use a power transformer temporarily as a potential transformer for adjusting the potential device. It is believed that in view of these factors, the standards of initial adjustment limits are satisfactory. Also with a view to metering and relaying considerations, the bands involved appear proportionate to the class of device.

RATIO AND PHASE-ANGLE DEVIATION WITH VARIATION IN APPLIED VOLTAGE

If the device is used to energize distance measuring relays, it will be required to register voltage quite accurately at a low value of voltage. A typical line protected by distance relaying may have 20 per cent voltage at the relay for fault at the remote end of the line, while for very short lines so protected, the voltage at the relay may be only 5 per cent for fault at the remote end. The percentage overreach or underreach in distance for the relay is directly proportional to the per cent ratio error of the potential device. Phase-angle errors of the small magnitudes encountered are of little significance in impedance relays but are of some significance in reactance-type relays. The class *A* device appears adequate for distance relaying on normal length lines but probably the first-zone elements of distance relays should be set short by 5 per cent more than customary for short lines with the class *A* device. This additional margin should be increased to 12 per cent with the class *B* device.

RATIO AND PHASE-ANGLE DEVIATION WITH VARIATION IN BURDEN

Good performance here simply means that if a device is once adjusted on the system that additional burden may be added or burden may be removed, within limits, without causing excessive errors. It has one additional significance which may not generally be recognized. When three devices supply both residual and three-phase burden, the burden on a particular device is different for ground faults than for phase faults. Therefore in order to obtain uniform ratio regardless of the type of fault, the device should have small regulation with variation in burden.

It should also be recognized that in connecting three devices through star series potential transformers to obtain residual voltage that the result, normally zero, is the vector addition of three large voltages. Consequently small percentage inaccuracies in the individual voltages will result in large percentage inaccuracies in the residual voltage so obtained. This favors the use of class *A* device in such instances, particularly if the relaying requires fairly close discrimination on small values of residual voltage.

There are, of course, many other application problems in which the standardized performance values are significant and those mentioned here are presented principally to stimulate thought along this line so that the committee may have the advantage of comment on the suitability of the values chosen before they are finally standardized.

J. A. Elzi and **J. R. North** (both of the Commonwealth and Southern Corporation, Jackson, Mich.): This paper by Messrs. Clem and Langguth gives a very concise review of the design and general performance characteristics of modern potential devices. Their description of the devices and the factors affecting their rating and performance will be of considerable help to all of those interested in the application and use of potential devices for many purposes. It would have been helpful had a discussion also been included concerning the effect of humidity and other weather conditions on the performance characteristics.

In the fourth paragraph of the section "Basis of Rating," the first sentence should be corrected to read, ". . . below normal voltage rating."

TERMINOLOGY

Confusion is very apt to develop from misunderstood use of the term "potential device", some construing this to mean the over-all equipment, including bushing or capacitor, while others refer only to the secondary network. It is therefore suggested that the section on terminology include preferred wording for the over-all device and the present section entitled "Potential Devices" be termed "Network Devices" or something equivalent thereto.

The terms "main winding" and "auxiliary winding" are used in the table of "Rated Watts Output and Standard Secondary Voltages", and it is felt that these terms should be explained.

Ratio. Since some devices have secondary voltages of 115/66.4, it is suggested that the statement regarding ratio be amplified to include windings and ratios other than the main ratio.

STANDARD RATIO

This paragraph refers to "coupling capacitors" and no doubt it is the intention of the author to include "bushing potential device". This is a very important paragraph and it is felt that some amplification would be worth while. Under the heading of "Rating" it is proposed to give the device two voltage ratings; namely, (1) circuit voltage, and (2) line-to-neutral voltage. It seems, therefore, that the standard ratio should specify which voltage rating is meant.

CLASSIFICATION

A class designation for bushing potential devices should be included even though the authors may not at the present time have sufficient data to specify their rating. Also, the classification is inconsistent in that the three classes are designated as "accuracy classifications", and classes *A* and *B* are set up on this basis, but class *C* is designated as "out-of-phase" type. In other words, there appears to be no choice of accuracy class available in the "out-of-phase" type and it seems that confusion would be avoided if these devices were treated as a type rather than as a class.

RATING

Under this heading it would seem desirable to indicate whether or not the "maximum watts output" is the total combined output of the main and auxiliary winding or if each winding can be loaded to rating individually.

The rating of potential devices should also include:

6. Insulation levels—60 cycle and impulse withstand test voltages

INSULATION LEVEL

The insulation level of the over-all potential devices should be distinguished from the insulation level of the secondary network. The insulation level of the over-all device probably should be the same as that for outdoor instrument transformers of corresponding voltage class. The insulation level of the network itself may then correspond to that of indoor instrument transformers of corresponding voltage rating.

RATED WATTS OUTPUT AND
STANDARD SECONDARY VOLTAGE

Since the watts output and standard secondary voltage given in this table presuppose a certain line-to-neutral primary voltage, it is felt that the primary voltage which will be considered as standard should be given as it is for the bushing potential devices.

MISCELLANEOUS

Additional items which should be included in the proposed standards are:

(*a*). **Effect of transient conditions**
(*b*). **Terminal markings**
(*c*). **Polarity**

J. E. Clem and P. O. Langguth: The discussions by Mr. Kennedy and Mr. Harder will be of great assistance in the application of these devices and form an excellent supplement to the paper. The discussion by Messrs. Elzi and North is very constructive and will be most helpful in the preparation of standards for this equipment. The authors wish to express their appreciation of the excellent and the constructive nature of the discussions of their paper.

Some Problems in the Standardization of Temperature Ratings of Fractional-Horsepower Motors

Discussion and author's closure of paper 40-95 by C. G. Veinott, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940, pages 1055–61.

P. H. McAuley (Westinghouse Electric & Manufacturing Company, East Pittsburgh, Pa.): The desire of all concerned with the thermal rating of electrical machinery is to arrive at rational temperature limits and to specify simple and accurate methods of measurement. At the winter convention in 1939, a great deal of information on this subject was presented. Following this, AIEE Standard No. 1 has been revised tentatively to favor the resistance method for determining temperature rise and changing the limits for this method from 55 degrees centigrade to 60 degrees centigrade. In general, our experience confirms the recommendations of these earlier authors whose work resulted in this step, and in particular those of the paper "Measurement of Temperature in General-Purpose Squirrel-Cage Induction Motors," by C. P. Potter, published in AIEE TRANSACTIONS, 1939, pages 468–72.

Surface thermocouples or thermometers often can be applied only to what should be one of the coolest parts of the winding or iron, that is, in the path of the ventilating air. No matter how well located the temperature-indicating device may be, there is always some question of temperature differential between the temperature of the winding at that point and the reading. This intensifies problems of checking results in different laboratories or at different times in the same laboratory. Also, to apply even surface thermocouples to the end winding of a small motor, it is necessary to remove the end bracket. This may or may not be less trouble than isolating the winding from starting and protective devices in a given case, but this partial disassembly introduces mechanical problems.

Our experience has been that surface thermocouples indicate in general, a lower temperature rise than winding resistance and the variations for different locations on the same motor and for different motors are wide. Mr. Veinott has found that the temperature rise by surface thermocouple averages as much or more for the small motor classes. To us, this implies wide spreads which the points tend to show.

We have recently made a few tests where motor temperatures have been measured both by winding resistance and by thermocouples on the stator end turns.

FIVE-HORSEPOWER VENTILATED INDUCTION MOTORS

One motor has been operated at a total temperature of 100 degrees centigrade by winding resistance. Three thermocouples were placed at the top and on each side of the stator winding end turns. The variations in room temperature from day to day somewhat affected the recorded temperatures but the average differences have been determined. Taking the average of all the thermocouple readings, the temperature rise by resistance was 14 degrees higher than by thermocouple. Considering the hottest thermocouple only, the average difference was 11.5 degrees centigrade.

Another motor was tested similarly except that 125 degrees centigrade by resistance was the operating temperature. An average of all the thermocouple readings gave a temperature 23 degrees centigrade lower than the resistance readings. Considering only the hottest thermocouple, the difference was 18.5 degrees centigrade.

ONE-HALF-HORSEPOWER SPLASH-PROOF INDUCTION MOTOR

This motor was used for a number of tests, mostly on cyclic loads to study the application of short time ratings to intermittent duty cycles. The temperature by resistance was used as the criterion of motor performance but thermocouples were located on certain parts of the frame and winding. In all cases, the temperature by resistance was higher than by thermocouple. The difference varied from 3 to 14 degrees depending upon operating temperature for runs made where conditions were constant long enough to assure steady state conditions. These longer runs are summarized in table I.

Table I

Temperature by			
Thermometer Frame (1)	Resistance Stator (2)	Thermocouple Stator End Turns (3)	Difference Cols. 2 and 3
38	46	43	3
46	65	61	4
54	87	78	9
51	90	76	14

On sustained intermittent cycles, thermocouples on the winding and frame lag somewhat behind the temperatures indicated by change in resistance. This is particularly true where the motor is stopped during the no-load period. The effect is similar to that observed in an automobile engine where the thermostat rises for a while after the motor has stopped. When the ventilation and the production of heat ceases, temperatures tend to equalize. If our measurement is made at one of the points of intermittent temperature it may rise, due to this equalizing process. In sustained cyclic tests on this motor, temperatures by thermocouple on the windings of the stator continued to rise for as long as 5 minutes after the motor was shut down. Certain parts of the frame or

Table II. Temperature Rises by Thermocouple and by Resistance for Different Sustained Cyclic Loads With the Motor Stopped During Idle Periods

Running Time Minutes	Maximum Temperature Rise Deg C			Amplitude of Cyclic Temp Variations	
	Thermocouple	Resistance	Difference	Thermocouple	Resistance
5	61	74	13	14	30
5	43	55.5	12.5	11	23
10	55	70	15	23	37
2.5	57.5	70	12.5	6	19
2.5	35	42.5	7.5	4	11
5	57	63.5	6.5	8	13
5	42.5	50	7.5	5	10
15	51.5	65	13.5	12	24
2	54.5	60	5.3	23	29

iron may have a temperature cycle exactly out of phase with the load current cycle.

Surface thermocouple readings do not correctly indicate the amplitude of copper temperature changes on intermittent duty cycles. This is shown in table II which gives the difference between highest and lowest temperatures observed by each method for a number of intermittent loading cycles.

These tests indicate to us the difficulty of measuring temperature rises by thermometers or by externally applied thermocouples with accuracy and consistency. The fact that thermocouples so often indicate temperature lower than resistance methods, shows that the thermocouple gives values in a great many cases considerably removed from hot-spot temperatures. Therefore, for the greatest degree of exactness, thermocouples must be embedded during manufacturing. Otherwise, resistance determinations seem to lead to more uniform results, where other considerations permit their use. As a result, we cannot share the enthusiasm of the author for the general use of external thermocouples for rating purposes, although for the particular application conditions encountered with small motors, the thermocouple method seems necessary and practical. Also, for special temperature studies during motor operation, when the resistance method cannot be used, the thermocouple method is very useful because it gives more accurate results than the thermometer. We agree, of course, with Mr. Veinott to the effect that the results of his tests, as well as those given in some of the earlier papers, indicate a sufficient difference between external thermocouples and thermometers that the past practice of considering the two methods equivalent, can justly be questioned, especially for smaller machines.

P. L. Alger (General Electric Company Schenectady, N. Y.): Mr. Veinott has very well expressed the need of more exact temperature measurements on small motors. I concur with his recommendation that thermocouples be used in place of mercury thermometers for fractional horsepower motor acceptance tests. For a-c motors the resistance method seems best as the standard, but for d-c armatures and as an alternative in other cases thermocouple measurements of temperature are most convenient, and acceptably accurate.

It is a fact, however, that thermocouples and thermometers give identical readings when placed in constant-temperature ovens, or under equivalent steady-state conditions. Therefore, permitting a higher temperature rise by thermocouple than by thermometer must be justified either by a difference in location or on account of reduced time lag under transient conditions.

In putting thermocouples on the exposed ends of a winding, they naturally fit into crevices, and other locations where thermometers do not fit, so that a greater difference between the respective readings may be expected on small motors than on large apparatus. Recognizing the higher thermocouple temperatures so obtainable, the recently revised introduction to AIEE Standards, "AIEE No. 1," includes the following new definition of the embedded detector method of temperature measurement, for which the same temperature rise is permitted as by the resistance method, or 10 degrees centigrade higher than by the thermometer method:

> Embedded Detector: This method consists in the determination of the temperature by thermocouples or resistance-temperature detectors, built into the machine either permanently or for test purposes, in specified locations, inaccessible to mercury or alcohol thermometers.

The way is open, therefore, in any revision of the motor standards to recognize thermocouples as the proper means for temperature measurement. If any higher temperature is allowed by thermocouple than by thermometer, however, the Standards should clearly specify locations that are inaccessible to mercury thermometers.

B. M. Cain (General Electric Company, Lynn, Mass.): Recognition of the greater accuracy of thermocouples compared to thermometers for measuring temperature rise of motor windings is a forward step which needs to be given a place in the program for more precise application of motor drives. Mr. Veinott's suggestion that thermocouple measurements should be standardized as separate and distinct from thermometer measurements should be very carefully considered.

In this consideration it is important to keep in mind that 40 degrees centigrade rise by thermometer has long been a suitable and accepted standard. Users have found that motors built to this standard give satisfactory service. Thermometer measurements have not been challenged even though the well informed user is undoubtedly aware of the relative inaccuracy of thermometers compared with thermocouples.

These two facts—universal trade acceptance of 40 degrees centigrade rise by thermometer, and general recognition by engineers that rise by thermocouple is higher than by thermometer—lend support to making a separate standard for thermocouple measurements.

Mr. Veinott's paper suggests that such a standard be imited to fractional horsepower motors. It is true that the size of the apparatus on which the measurements are being made has a noticeable effect on the difference between temperature indicated by thermometers and by thermocouples. However, the line of demarcation between fractional horsepower and integral horsepower is scarcely well enough defined to warrant a separation of standards particularly for ratings under 5 horsepower.

Mr. Veinott has suggested that temperature rise by thermocouple could equal or replace temperature rise by resistance on fractional horsepower motors. Perhaps we should ask—why limit this statement to fractional horsepower motors? The answer lies in the fact that temperature rise of the end windings in comparison with average machine temperature is not a matter of size alone, but is dependent on type of construction and method of ventilation of the motor.

Mr. Veinott's tests have apparently been limited to a type of construction wherein more heat is lost by radiation than is removed by the ventilating air. If this condition were reversed, it would be found that rise by resistance would exceed the rise by thermocouple. This has been borne out by observation. In totally enclosed and totally enclosed fan-cooled motors, of all sizes, the temperature of the end windings observed by thermocouple may frequently exceed the average temperature observed by resistance measurement. In open motors with ample ventilation the temperature of the end windings will usually be below the average winding temperature.

In view of these facts, the possibility of permitting temperature rise by thermocouple to replace rise by resistance as a general practice for any class of motors seems to require further study.

In establishing standards of temperature rise, the aim is to determine suitable limits from the standpoint of protection and economy. Winding temperatures measured by surface thermocouples represent the temperature of the exposed portion of the winding, the part most in need of protection. A proposal for establishing new standards of temperature rise based on the more exact measurement by thermocouple and giving recognition to the improvements which have been and are continually being made in insulating materials does seem to be very much in order at this time.

R. Rüdenberg (Harvard University, Cambridge, Mass.): I would like to supplement Mr. Veinott's valuable experiments, and

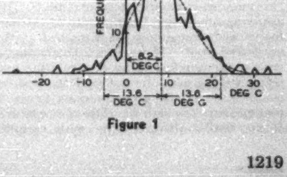


Figure 1

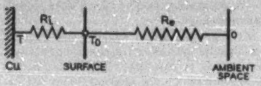


Figure 2

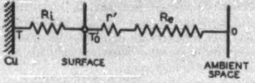


Figure 3

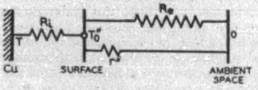


Figure 4

those by others for larger machines, by two results of extensive tests conducted formerly with machines of a European firm.

We had measured the difference between resistance and thermometer methods on hundreds of machines of any type, d-c and a-c, motors, generators, and converters, from 5 kw up to several thousand kva. The overall result is shown by the accompanying figure 1, where the frequency of the measured points is plotted against the difference of the measured temperatures. The average difference of both methods yielded 8.2 degrees centigrade, if the machines were running at rated load, a figure similar to Mr. Veinott's results with small motors under 1-horsepower rating, taken for rated temperatures. The dispersity of our measurements, determined as twice the mean deviation of all points shows a tolerance, however, as much as ±13.6 degrees centigrade. This also is similar to Mr. Veinott's results and indicates that for small as well as large machines a single comparative test may give results very different from the average. So we see that it is not worth while to be too subtle in accepting the general use of the resistance method, which is much better defined in itself than the thermometer method. It is noteworthy that the difference between the two methods was found with hundreds of machines of a second European firm as 8.5 degrees centigrade ± 17.2 degrees centigrade which is very near to the figures mentioned above.

Concerning the measuring of surface temperatures we found that certain precautions are necessary in order to secure correct results. As the accompanying figure 2 shows the surface temperature T_0 of an insulated copper winding is determined by the balance of the heat flow in the internal thermal resistance R_i from the copper with temperature T and in the external resistance R_e to the ambient space with temperature O. By applying measuring instruments at the surface this balance may be disturbed at the measured spot. If we use a thermometer with a surrounding lump of insulating material we increase the external thermal resistance, as in the accompanying figure 3, and measure too high a surface temperature. If we use a thermocouple with metallic leads we decrease the external thermal resistance, as in the accompanying figure 4, since the metal wires form a shunt path to the ambient space, and thus we measure too low a surface temperature. For avoiding such errors, which may have a magnitude of from 5 to 10 degrees centigrade we found it necessary to wind up the leads of the thermocouple near the measured spot in several turns, thus securing a constant temperature and therefore no heat flow within the wires near the junction and simultaneously displacing the shunt heat flow through the wires to a spot away from the junction. A slight heat cover of such spiral thermocouple may compensate by its series resistance the remaining shunt resistance of the leads, thus giving very accurate measurements.

R. E. Hellmund (Westinghouse Electric & Manufacturing Company, East Pittsburgh, Pa.): The conclusions reached in the Hellmund-McAuley paper and other points brought out in my discussion of the Montsinger paper plainly show that it is impractical to provide thermal protection for motors for intermittent service by any device connected in the circuit which is not at the same time affected by the temperature of the motor. The only way in which thermal protection could be obtained by a device in the circuit would be to supply special thermal elements for each type of application. This conclusion gives added significance to the inherent overheating protective devices described in the second part of Mr. Veinott's paper. Mr. Veinott gives results obtained with such a device for various sustained loads as well as for short-circuit conditions. It would be of interest to know more about the results obtainable with various intermittent-duty load cycles.

In the conclusions of the Veinott paper, it is pointed out that if the resistance method of measuring temperature is used instead of the thermometer method for fractional horsepower motors, a rise of 50 degrees centigrade should be adopted for a-c motors and 60 to 65 degrees for d-c motors. On the surface, this of course appears to be rather inconsistent, but it should be realized that these figures represent conditions which have existed for some time and which have given satisfactory practical results; they are a natural consequence of the use of the thermometer method of measuring temperature, which is not representative of actual conditions in these small motors. Fortunately, the existing practice makes allowance for some factors which make a difference in the temperature rise between a-c and d-c motors desirable.

It is well known that the voltage conditions on a-c systems over the country vary to a great extent, so that the a-c motors are used over a wide range of voltages. The data given in the third part of Mr. Veinott's paper in figures 9 and 10 plainly indicate that a 10-degree margin is not any too great to take care of these varying voltage conditions for a-c motors. In the few d-c systems, the voltage conditions are in general more uniform and therefore the same margin of safety is not necessary for d-c motors.

The difference in temperature rises is also justified by the fact that with these small motors, which are used to a great extent on various appliances, mechanical interchangeability of a-c and d-c motors is of considerable importance. Since d-c motors inherently call for somewhat larger dimensions than a-c motors built for the same temperature rise, any attempt to design d-c motors for a temperature rise of 50 degrees by resistance would mean that a greater mounting space would have to be allowed on all appliances for motors; in other words, the very small number of d-c motors used would handicap the design of all these appliances. It is obviously more economical from an all-round point of view to avoid such handicap by allowing a higher temperature rise in d-c motors even if this should result in a somewhat shorter life in the few cases in which they are used.

C. G. Veinott: Mr. Alger agrees that thermocouples are better than mercury thermometers for fractional horsepower motor acceptance tests, but indicates a preference for the resistance method as the standard. As the resistance method is generally less convenient and sometimes impossible to employ in application tests, an alternate standard is desirable to replace the present thermometer method, which is recognized as inaccurate but which is, nevertheless, accepted. Unfortunately, the thermocouple method is now recognized in AIEE Standards—with the same limits as for thermometers. But different limits are given for the resistance method. The evidence given in this paper shows that, for the cases studied, the thermocouple method approximates the resistance method instead of the thermometer method, with which it differs.

Mr. Alger points out that in the new standards, the same temperature limits are given for embedded detectors as for the resistance method. But the definition quoted by Mr. Alger could hardly be interpreted as including a surface thermocouple installed in a completed machine, particularly when the present definition of "thermometer or thermocouple" is allowed to remain. The recommendations of this paper could be carried out by omitting all reference to thermocouples in the "thermometer method," and in the limits for it; and by amending the description of "embedded detector" to include specifically a thermocouple installed on the end windings of a fractional horsepower a-c motor. Such a revision would be more in accord with factual data than the present reading of the standards.

Replying to Mr. McAuley's discussion—it is not always necessary to remove a bracket to install thermocouples in open motors, particularly when the conduit box, with its large opening, is cast integral with the bracket. Moreover, in the case of a small motor, it is often easier to remove and replace a bracket than it is to isolate, for a certainty, the connections of the main winding. Small motors today often have quite involved wiring because of control devices built into them. For example, take a dual-voltage, capacitor-start, thermally protected gasoline-pump motor with a centrifugal switch, built-in voltage changer and line switch. Such a motor has a dual-section main winding and uses 15 separate leads in its wiring. For a user to find and bring out the correct leads for taking resistance measurements would certainly involve dismantling the motor and the subsequent difficult task of locating the proper leads.

Installation of thermocouples is a much easier task in such a case, and the hazards of inaccuracy are minimized.

Mr. McAuley's data on a 5-horsepower motor needs no comment as the recommendations of this paper were limited to fractional horsepower motors. His data on the $^1/_3$-horsepower motor do, however, fall within the scope of this paper. While he has a number of temperature runs, they are all taken on the same motor. The 56 points given in figure 6 of the paper were taken on approximately 50 different motors with not more than two runs on a single motor. Of these 56 points, 20 (36 per cent) of them show no more rise by thermocouple than by resistance, but the median of the points shows definitely that the tendency is for thermocouples to give higher readings than resistance measurements. Note that figure 6 shows scattered points comparable to Mr. McAuley's data. I cannot help feeling that the average results of the tests on 50 motors are more reliable than the tests upon a single motor.

I was interested in Mr. McAuley's observation that the thermocouple temperature of the windings increased for as much as five minutes after the motor was shut down. In practically all the temperature runs reported in the paper, there was no perceptible rise in temperature after shutdown.

Mr. Cain's remarks line up quite well with my own thoughts on this subject. NEMA defines a fractional-horsepower motor as "a motor built on a frame smaller than that having a continuous rating of 1 horsepower, open type, at 1700–1800 rpm." This definition provides a fairly sharp line of demarcation between fractional and integral horsepower motors. However, as Mr. Cain points out, the use of thermocouples need not be confined to fractional horsepower motors. It is, of course, correct that the difference between thermometer and resistance readings will vary with different types of mechanical construction as he points out and it would be useful if similar data could be given on other types of construction.

I am happy to know that Mr. Cain concurs with my opinion that a revision of the standards is in order.

Dr. Rüdenberg's remarks are interesting. His data, and mine as given in figures 1 and 2, show a figure slightly less than, but reasonably close to the allowance of 20 per cent given in the standards as the difference between the thermometer and resistance methods. His precautions regarding the use of thermocouples are timely and suggest that if the AIEE standards are revised as proposed, some information as to how and where the thermocouples should be located would be in order.

Mr. Hellmund has inquired as to the performance of an inherent overheating protective device such as I have described on intermittent loads. The temperature data I have given in the paper obtains on long sustained overloads of the least value which will cause the device to trip. If heavier overloads are imposed, the device will trip at lower winding temperatures than given, but if the load is allowed to cycle at a higher value by means of the thermal device, the winding temperatures may more nearly approach the temperatures that are reached on long sustained overloads. The reason is fairly simple: if a load is imposed on the motor which is the least load which will cause the thermostat to trip, the winding reaches its ultimate temperature rise and the disk reaches its ultimate rise above the bracket; if a heavier load is imposed, the ultimate rise of the disk is substantially above its tripping temperature and, as the rate of temperature rise of the thermostat is faster than that of the motor, the disk will heat faster than the winding, because of the latter's relatively greater thermal capacity, and trip before the winding temperature has risen relatively as much.

I am grateful that Mr. Hellmund agrees that a higher temperature-rise limit for d-c motors than for a-c motors can be justifiably established. Needless to say, I am very much in accord with his remarks on the subject.

A Study of Short-Time Ratings and Their Application to Intermittent Duty Cycles

Discussion and authors' closure of paper 40-93 by R. E. Hellmund and P. H. McAuley, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940, pages 1050–5.

P. L. Alger (General Electric Company, Schenectady, N. Y.): It is very desirable to use a simple and flexible method of rating electrical apparatus, that will tell the user its output limitations in as few words or symbols as possible. All that the user really needs to know about the capacity of his apparatus is that it has adequate power for his maximum peak loads, and that it will have satisfactory life under his actual load conditions. Only two numbers should be needed to define these two output limits, corresponding to the momentary and the continuous ratings.

Under the present motor rating system, no less than six numbers are used in describing the capacity of an induction motor, representing the rated horsepower, the breakdown torque, the starting current, the temperature rise, the duration of the temperature rise test, and the service factor. Can we not reduce these six numbers to two for all ordinary purposes?

Suppose that we define the "effective" horsepower rating of a motor as the maximum momentary output, divided by an agreed factor of safety. The maximum output may be limited by the breakdown torque, by commutation, or by mechanical strength of some part. In the case of an induction motor, the American Standards require that the breakdown torque shall not be less than 175 per cent of the rated torque. I propose that we turn this rule around and define the "effective" horsepower rating of an induction motor as the horsepower output substantially equal to one-half that corresponding to the breakdown torque, but not greater than 57 per cent of this value. Since the starting current of an induction motor is roughly proportional to its breakdown torque, the effective horsepower so defined fixes both starting current and breakdown torque, and enables one number to replace three required by the present rating system.

Suppose, also, that we define the "service factor" rating of a motor as the horsepower it can deliver continuously at rated voltage and frequency, without exceeding the temperature rise permitted by the American Standards for the class of insulation used. Instead of giving any motor a $^1/_2$ hour or other short time rating, all motors would be given continuous heat runs. This "service factor" rating is conveniently defined as the product of the "effective" horsepower rating by a service factor.

This proposal is in accord with the present system of rating general purpose motors, which meet 40 degrees centigrade rise by thermometer at their rating and are good for 115 per cent load continuously. The only difference is that under the proposed plan general purpose motors would be given temperature tests at 115 per cent of this nameplate rating, and at this load they would be required to meet 50 degrees centigrade rise, instead of meeting 40 degrees centigrade rise at 100 per cent load. Under the present system, a motor for intermittent duty may be rated 10 horsepower, 50 degrees centigrade rise, in one hour, and is required to have 275 per cent breakdown torque. Under the proposed plan, such a motor would be rated 15 horsepower "effective," 0.6 service factor. It would have substantially the same starting current and breakdown torque as a general purpose 15-horsepower motor, but would only have heat dissipating capacity sufficient for a continuous load of 9 horsepower. This service factor rating is equal to the rms equivalent load that can be carried over any short time duty cycle, and so the proposed system fits in neatly with the universally used rms method of calculating the apparatus size required for intermittent loads.

By thus giving all motors a service factor, which may be more or less than unity, we can again use a single number to replace the three numbers required under the present system of rating.

To sum up, I propose an a-c motor rating system substantially the same as that proposed by L. E. Hildebrand ("Duty Cycles and Motor Rating," AIEE TRANSACTIONS, volume 58, 1939, pages 478–83), including the following specific elements:

1. All motors will be given an "effective horsepower" rating.
2. The breakdown torques and starting currents of all motors will be directly related to the "effective horsepower" by ratios similar to those now customary for general purpose motors.
3. All motors will have a "service factor" which will indicate the fraction of the "effective horsepower" at which the motor may be operated continuously without exceeding the standard temperature rise for the class of insulation used.

This system accords with the proposed methods of rating welding generators and transformers discussed in other papers at the standards conference. It has the advantage of giving approximately single valued relationships between the starting current, the breakdown torque, and the horsepower rating of any motor, whether for intermittent or continuous use. It would simplify the choice of wiring and protective devices which largely depend on the service factor and the starting current values. It gives a much truer representation of the effectiveness of a motor for duty-cycle service than the short time rating method. By making all motors of a given insulation and enclosure type meet the same temperature rise, it eliminates the temperature question from discussion, and so simplifies the understanding and use of motors by the general public. And, finally, it does not disturb present continuous rated motor designs, but merely extends the rating methods used for them to cover duty cycle motors also.

Of course, this proposed method does not fit every case, such as motors for highly intermittent duty, which would exceed permissible temperatures if operated at no load. Also, extended experience with the plan must be obtained before it can be generally adopted. My immediate proposal, therefore, is that this plan be considered along with other systems of short time and intermittent rating by the AIEE Standards Co-ordinating Committee No. 4 and other interested groups. Out of this study there should be evolved a statement of general principles for the guidance and co-ordination of specific standards for different types of apparatus analogous to the revised AIEE Standards No. 1 just issued.

T. C. Johnson (General Electric Company, Schenectady, N. Y.): Two important points that should be brought out in this discussion are that:

1. The rms horsepower method of determining motor size is inherently conservative for short cycle times, and

2. A service factor, defined as suggested by P. L. Alger can be readily determined for each type of application.

General use of the rms method and the service-factor rating should simplify and standardize motor applications.

Standard general-purpose 40-degree motors, having the usual torque margin and temperature-rise versus load curve may be applied to intermittent loads as shown in figure 1 of this discussion. If the motor is on continuously 1.15 times rated load can be carried, while if it is on only 10 per cent of the time and the cycle is long enough for the ultimate temperature rise to be reached the motor can be loaded to 1.4 times the rated value because of the ability of the insulation to stand higher temperatures for short times and still give the same useful life. For shorter cycle times during which the temperature does not vary so much a higher loading may be allowed; e.g., 1.35 times rated load for 30 per cent of cycle time.

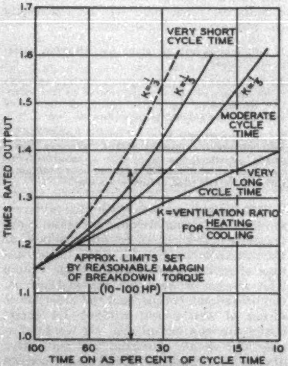


Figure 1. Allowable output for standard general-purpose motors for same insulation life—on-off cycle

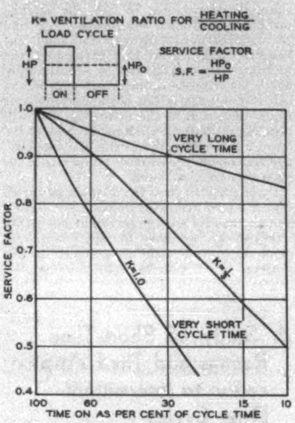


Figure 2. Service factor for same insulation life—on-off cycle

Such allowable-output versus per-cent-time-on curves give a relatively simple and definite basis for motor application provided the heating and cooling thermal time constants of the motor are known. Mr. Hellmund has pointed out the complexity that occurs when these must be considered. However, it is fortunate that most motor applications are for cycle times so short that the motor temperature does not vary much.

For most motor applications the temperature does not vary during the cycle and the motor can be loaded to a limit determined only by the necessary torque margin and the allowable continuous insulation temperature. It is only important to know the relative amounts of heat that can be dissipated during running and standing times; i.e., the ventilation ratio, K. Thus, if during standing the motor can dissipate $^1/_3$ the heat for the same time running, the insulation temperature limit would allow almost 1.55 times rated load to be carried for 30 per cent of the time ($K = ^1/_3$, figure 1). For the usual motor applications this ratio K is taken as $^1/_3$, while for transformers, of course, it is equal to unity.

The rms horsepower method of determining the allowable motor loading has been found to agree closely with the curves of figure 1. (Standard general purpose motors on very short cycles.) Therefore, if the ventilation ratio is known for a particular type of motor the rms horsepower method may be used to apply the motor accurately to an intermittent load by using the weighting factor K for the standing time.

Intermittent motors, because of their higher torque margins have a smaller temperature rise for a given change in load, and, therefore, their curves of allowable output versus time-on lie above those for the general purpose motors. The rms horsepower method is thus conservative for intermittent rated motors on short time cycles.

It is evident that the rms horsepower method offers a reasonable basis for establishing service factors for intermittent rated motors as shown in the accompanying figure 2. It is possible to reduce the duty cycle of any usual industrial application to an equivalent rectangular cycle with the equipment running a definite fraction of the cycle time. Then the appropriate service factor is determined by the ventilation ratio K. Thus motor service factors would be given by the middle curve, while the lower curve would give those for transformers which cool equally well whether loaded or not. This is conservative for intermittent rated motors, as was shown above, but if the cycle time is very long compared to the thermal time constant of the motor, then a larger service factor must be used, as shown by the upper curve, thus setting a definite limit to the possible range of applications.

As an example, if the load cycle is 15 per cent time at rated load, and 85 per cent shut down, and the motor is of usual thermal characteristics, it should be designed and rated for a service factor of 0.6.

L. A. Finzi (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It may be remembered that many years ago the American delegates of the International Electrotechnical Commission at Bellagio (1927) and at Prague (1934) wanted to avoid any narrow standardization for motors for intermittent duty cycles, as actually used in a few European countries. They suggested that for such intermittent services a reference to the continuous or still better to the short-time ratings of the motor might be of more general use. This met its main opposition in the argument that such reference "neither will bring a reduction in the technical work required for fixing the ratings nor will make the choice of the motor an easier one." A German delegate was given the task of investigating whether a simple rule could be found for determining the length of a short-time rating thermally equivalent to a certain given intermittent cycle. There is no evidence that any progress has been made up to the present date.

The methods suggested by Hellmund and McAuley make it actually possible to check the thermic applicability of a motor to given intermittent duty cycles, in terms of its "short-time" ratings and without need for further analysis of the various losses, their distribution, etc., in every single case. The reference to "short-time" ratings is also a very practical procedure from the application point of view, since a motor for intermittent duty, in view of the required starting and breakdown torque, often will be chosen from designs rated for short-time service.

Hellmund and McAuley point out indeed that the thermic behavior of a motor in sustained intermittent service is quite different from that in typically transient "short-time" service. The considerations leading to the curves shown in figure 6 and to formula 3 of the paper are based on actual

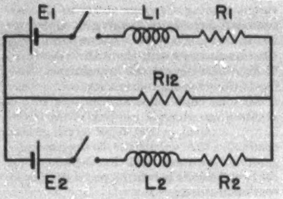


Figure 3

experimental heating and cooling curves, as obtained in proximity of the 60-degree rise of temperature for some typical sustained cycles after reaching a quasi-steady state. This is the best practical way to cope with all difficulties arising from the difference of the time constants of the windings, iron, etc. The results are by all means more reliable than those which may be reached by piecing successively together portions of heating and cooling curves obtained from a continuous service test. Such a proceeding does not take into account that the average iron temperatures (and herewith the actual cooling of the windings) in both heating and cooling periods of a sustained intermittent service may be greatly different from those determining the shape of the curves in a test at continuous load.

For a more general attack of the problem, the reciprocal influence of the actual temperatures of the various parts in the transient state would have to be taken into a closer account. The technical literature although rich in studies about the thermic behavior of a machine, shows few contributions on this point. Examining the question, it can be suggested that, with some practical simplifications, the determination of the rises of temperature of iron and copper can be reduced to the determination of the currents in an electric circuit as shown in the accompanying figure 3. As the circuit is not very complicated, the integration of the differential equations is not difficult. The constants E, R, and L, correspond to physical constants of the motor and may be determined with usual heating and cooling tests in continuous service at two different loads. Inductance represents thermal capacity and resistance represents heat dissipation to the ambient, respectively for copper and iron. The mutual resistance then represents heat transfer from copper to iron or vice versa. Finally, electromotive forces represent copper and iron losses.

Cyril G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): The paper presented by Hellmund and McAuley outlines a method of testing to determine just what intermittent duty cycles a short-time rated motor would be able to handle without exceeding the rated temperature. Mr. Alger's discussion was very interesting; it seems to me that he outlined a short-hand system or simplified method for presenting results which can, or should be, obtained by the method outlined by Hellmund and McAuley. The latters' principles seem to be more basic, however.

Open, general-purpose induction motors carrying a continuous rating of 40 degrees centigrade rise by thermometer may also often be used for intermittent load in excess of their rated horsepower. It would appear that the Hellmund-McAuley method could be applied equally well to open, general-purpose motors and I should like to inquire if the authors have given any thought to this subject.

R. E. Hellmund and P. H. McAuley: Mr. Veinott has brought up the question of the applicability of the methods in our paper to general-purpose motors. The basic methods given can be applied to any apparatus or conductor. If the typical ratio R is determined for three or more well selected loads in excess of the continuous current, corresponding to the maximum safe temperature rise, a curve covering the entire range of loads can be plotted. In figure 4 of this discussion, load currents corresponding to the 5-, 15-, and 30-minute ratings have been used, but, obviously, any other properly spaced current values are suitable.

Mr. Alger has suggested a simple method of rating. Simplification, while desirable, should not be carried to the point where it interferes with satisfactory results combined with reasonable economy. Furthermore, simplification which can be applied to a specific field, may unduly complicate the broader situation. Nearly every type of electrical device has to meet requirements other than temperature limitations, and in some cases it may seem immaterial whether such other requirements or the temperature limitations are used as the primary basis for ratings. However, since temperature limitations apply to practically all electrical apparatus and conductors, it would seem most unfortunate if the one tie common to nearly all standards were ignored through the use of a multitude of other factors as the primary basis for rating apparatus and conductors.

Some other aspects of Mr. Alger's and also Mr. Johnson's discussion are covered in the discussion by Hellmund of the Montsinger paper "Effect of Load Factor on Operation of Power Transformers by Temperature." (AIEE Transactions, 1940, pages 632–6.) It should furthermore be pointed out here that our paper gives a basic method for deriving results from curves obtained from simple tests. In contrast to this, most of the discussions deal with certain methods of applying motors which can be used only if certain factors are known. These factors must be determined either by a basic method such as given in the paper, by more extensive testing or from previous experience. If reliable factors have been previously determined for all requirements, our method is not required except possibly as a check. As Mr. Veinott has pointed out, our method can be applied directly but it is also suitable for deriving factors needed for Mr. Johnson's method.

With due appreciation of the difference between the methods in the discussions and in the paper, it will be seen that there is rather substantial agreement on many of the points made. It is generally agreed that for short load cycles and constant cooling conditions, the rms method is satisfactory. We also agree that the suggested use of a service factor, expressing the continuous current for the maximum safe temperature in per cent of the rated current, is convenient for standardization purposes.

With long-time load cycles and constant ventilation, loads must be reduced as compared with the previous case, and proper values can be derived directly by the method in the paper. However, since the necessary reduction is not very great for the usual long-time applications, it should not be difficult to establish service factors in accordance with Mr. Johnson's suggestion and which will permit safe and economical applications of motors. However, the same service factors cannot be applied to the wiring and protective devices because of their very much shorter time constant. Thus, the choice of wiring and protective devices is not so simple as claimed in Mr. Alger's discussion. This will be discussed more in detail during the standards conference.

Unfortunately, conditions become more complicated when cooling conditions of the device change as they do in motors standing still during the idle periods. Widely different service factors are encountered for different sizes, speeds, and types of motors. Moreover, appreciable differences occur with the same motor operated with different load cycles. Nevertheless, reasonably correct results can be obtained for a definite type and make of motor on which considerable practical data are available and given in such curves as shown by Mr. Johnson. The data in figure 5 of this discussion are of interest in this connection. The results in the first two columns are obtained on the same motor frame with a weak field and with a stronger field. As expected, the motor with the stronger field and heavier wiring has a somewhat higher current rating. Its rate of temperature rise is less than that of the other motor on account of the lower copper losses, but the rate of temperature drop is also lower because with a stronger field the core is hotter and the heat from the winding does not transfer to the core during idle periods as readily as in the other motor. As a result, the factor R is not much different and the permissible "on-time" of the motors for the rated currents is about the same. The third column gives figures for the motor with the strong field operated enclosed. The rate of rise is now appreciably increased on account of the re-

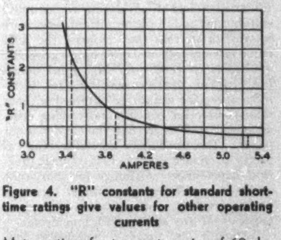


Figure 4. "R" constants for standard short-time ratings give values for other operating currents

Motor ratings for temperature rise of 60 degrees centigrade:

5 minutes	5.25 amperes
15 minutes	3.90 amperes
30 minutes	3.45 amperes
60 minutes	3.25 amperes

	30 MIN. RATING CURRENT IN AMPS.	RATE OF TEMPERATURE CHANGE NEAR 60° RISE DEGREES PER MINUTE		$R=\frac{d}{r}$	$100\frac{R}{R+1}$ OR MAX. % OF RUNNING PERIOD
		RISE	DROP		
WOUND FOR WEAK FIELD OPEN	20.2	1.9	2.5	1.32	57
WOUND FOR STRONG FIELD OPEN	23.5	1.15	1.6	1.38	58
WOUND FOR STRONG FIELD CLOSED	21	2.7	.95	.35	26

Figure 5. Data for 7.5-horsepower 30-minute motor frame size

duced ventilation, while the rate of drop is decreased for the same reason. As a consequence, the ratio between the two is appreciably altered and the permissible "on-time" with the rated load is considerably reduced as compared with the open condition. Translating this into the rms method with a service factor, it is found that the two open motors give a service factor of about 0.76 for short cycles. For longer cyles with 10 per cent "on-time" the service factor may be as low as 0.31. With the enclosed motor, the service factor for short cycles is 0.5. Numerous other examples could be cited to illustrate the variations encountered in practice. A test on the larger motor of the paper indicates that the cooling rate with the motor running is 1.5 to 2 times as great as with the motor standing still. These values correspond to the reciprocal of the factor K in Mr. Johnson's discussion. The generally assumed value of $K={}^1/_3$, although safe in this particular case, would not result in economic application.

Therefore, it seems that the direct method given in the paper is by all means preferable to a method requiring a different service factor for practically each type of apparatus and application. On the other hand, it is well to realize that the rms method already is firmly established for problems of this kind and that it may meet with greater favor than the direct method if it is found possible to derive a reasonable system of service factors to cover the great variety of applications. Whether or not this is possible, can be determined only after a great many types and ratings of motors have been analyzed.

From the discussions by Mr. Finzi and Mr. Veinott, it is evident that they have fully recognized the relation of the method given in our paper to some of the more or less empirical methods used for everyday applications of apparatus. Mr. Finzi's remarks regarding attempts by the International Electrotechnical Commission to establish suitable methods for intermittent service are interesting in that they indicate the difficulty encountered in establishing a generally applicable method. As Mr. Finzi points out, correct performance data for intermittent service cannot be obtained without duly considering heating and cooling characteristics such as given in the curves of our paper. The temporary heat-absorbing capacity of all electric devices is of great practical importance. Although for short-time cycles it is sufficient to consider this in a qualitative way, a quantitative analysis is required for any service with long cycles.

Although the extreme simplicity suggested by Mr. Alger may not be realizable, steps in this direction are certainly desirable. The authors believe, for instance, that there is no need for as many different short-time ratings as are now contained in the various standards. Instead of 5-, 15-, and 30-minute and continuous ratings, a 15-minute and a continuous rating would be quite sufficient for many of the smaller motors.

Effect of Load Factor on Operation of Power Transformers by Temperature

Discussion and author's closure of paper 40-96 by V. M. Montsinger, presented at the AIEE summer convention, Swampscott, Mass., June 24-28, 1940, and published in AIEE TRANSACTIONS, 1940 (November section) pages 632-6.

L. W. Clark (The Detroit Edison Company, Detroit, Mich.): Mr. Montsinger proposes a rule of allowing 3 per cent increased load for each 10 per cent that the daily load factor falls below 100 per cent. We believe that this rule is conservative if load factor is used as a basis but, in our opinion, load factor alone is not a suitable measure upon which to base permissible loading. As pointed out by Mr. Montsinger, the permissible load may actually vary over a wide range, depending upon the shape of the load curves, and yet the load factor of these curves may have the same numerical value.

Another factor which Mr. Montsinger could not consider in setting up a general standard is the actual thermal ability of the individual transformer. He predicates his loading values on the ASA "standard" 55-degree-centigrade rise transformer. Consideration of the individual transformer indicates that there is usually some added margin of load capacity available, and that that margin is great enough to make it worth while considering each individual transformer on its merits. To illustrate this point, we include figure 1 which shows the variation in load-carrying ability of 126 substation transformers, ranging in size from 100 kva to 10,000 kva. It compares these 126 transformers with the ASA "standard" 55-degree-centigrade rise transformer, having 45-degree-centigrade oil rise and a ratio of full-load copper loss to iron loss of 2:1. It shows that about 50 per cent of the transformers studied permit 5 per cent greater continuous loads than the 55-degree-centigrade transformers, and that 5 per cent of the transformers permit 13 per cent greater continuous loads.

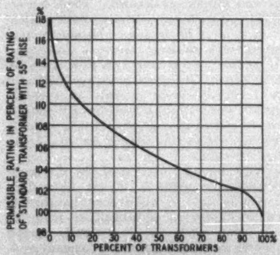


Figure 1. Showing variation in load-carrying ability of 126 substation transformers ranging in size from 100 to 10,000 kva

The new ASA guide for transformer loading (referred to in the paper) permits continuous loads corresponding with 95-degree-centigrade hot-spot temperatures and emergency short-time overloads corresponding with 105-degree-centigrade hot-spot temperatures for 24-hour periods. In our opinion there should be a much greater spread between these two conditions. If a transformer will give satisfactory life when operated continuously at 95-degree-centigrade hot-spot temperature, it certainly should not be damaged by two or three 24-hour emergency operations during its life with hot-spot temperatures considerably in excess of 105 degrees centigrade. Also, if transformers are normally loaded to give hot-spot temperatures of 95 degrees centigrade, they will almost certainly encounter emergency peak loads which will result in hot-spot temperatures in excess of 105 degrees centigrade.

We have been studying methods of determining the load-carrying ability of transformers based on the actual shape of the load curves without reference to load factor as such, and taking into account the thermal characteristics of the individual transformers. We believe we have arrived at a fairly accurate method which has the virtue of requiring very simple calculations. Briefly, the method converts the daily load curve into an equivalent steady load which would cause the same insulation aging. This is accomplished by the use of empirical conversion factors which were determined from a study of all the types and shapes of load curves actually encountered. The accuracy of the method has been checked by the

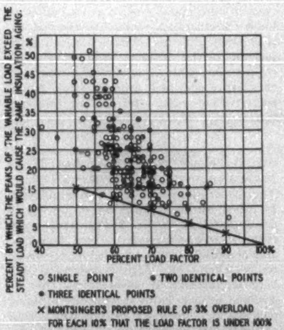


Figure 2. Showing, for different load factors, the range of peak load values for various shapes of 240 load curves with equal aging effect on the transformer insulation

rigorous method, as outlined in previous papers by Mr. Montsinger, and has been found to be accurate to within plus or minus 5 per cent for most of the encountered load curves having load factors of 50 per cent or greater. Occasional odd-shaped load curves, not usually encountered, show this short-cut method to be in error by as much as plus or minus 10 per cent.

Using this short-cut method, we have analyzed some 240 daily load curves to indicate the relationship between the peak load of the curve and the steady load which would produce the same aging effect as the actual load curve. The results are shown in figure 2 of this discussion plotted against the load factor of the curve, and it shows a wide range of peak load values for curves of the same load factor, any one of which gives equal aging effects. In this same figure, the effect of the proposed allowance of 3-per-cent increase in peak load for each 10-per-cent increase in load factor is indicated. It is obvious that full advantage of temperature loading would not always be realized if this rule were followed. For instance, for the load curves having approximately 60-per-cent load factor, the peak loads ranged from 111 per cent to 143 per cent of the steady load which would cause the same amount of transformer aging. If Mr. Montsinger's 3-per-cent rule were adhered to, no peak could exceed 112 per cent of the equivalent steady load.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The greater part of all electrical apparatus is subjected to intermittent or varying loads. Whenever the safe load is limited by temperature considerations, the determination of the temperature due to different load conditions and the evaluation of its influence upon the life of insulation is of utmost importance. The paper by Mr. Montsinger and other papers presented at this session deal with this problem. However, since the papers presented seemingly attack the problem in different ways, it may be well to point out the interrelation of the various methods, including the time-honored rms method.

In figure 3 of this discussion, case I shows the load fluctuating from no load to maximum load through relatively short intervals for a rotating machine running at constant speed and thus having uniform cooling conditions. The temperature will vary slightly from the permissible value, as shown by curve *A*, if the rms current is about the same as the continuous current giving the prescribed temperature rise. While curve *A* temporarily exceeds 60 degrees to a small extent, this is of no practical consequence and, therefore, straightforward methods of figuring the rms current will give satisfactory results.

If we now assume for case II that the motor is standing still instead of running during the no-load periods, the average cooling effect is of course materially reduced. An attempt to use the same rms current as in case I would result in very much higher temperatures as indicated in curve *B*, and a material reduction in the rms current is necessary to bring the heating to permissible values as in curve *C*. Applications of this latter kind have at times been treated by figuring the rms current using one-third of the not-running period instead of what it actually is. This, of course, is a very rough approximation and does not uniformly apply to the many different conditions met in practice. Enclosed motors, for instance, will be entirely different from open motors in this respect and the size of the motor will have considerable influence. Application engineers have developed empirical methods of making suitable allowances.

In case III it is assumed that the motor is running continuously but with the time for each cycle considerably longer than in case I. If the same rms current as in case I is used, the temperature will fluctuate appreciably as shown by curve *D*. In other words, it will be appreciably in excess of 60 degrees for long periods and therefore deterioration of the insulation will be hastened. In order to have the same deterioration as in case I, the rms current has to be reduced sufficiently to obtain a heating curve similar to *E*. A slight excess of temperature as indicated by *b* is permissible.

In case IV the motor is assumed to be standing still during idle periods. Because of reduced ventilation, a temperature rise as shown in curve *F* is likely to occur with the rms current still the same as for *E* and a greater reduction is necessary to obtain the permissible temperature conditions indicated by curve *G*.

Cases V and VI correspond to III and IV except that the load period is assumed much shorter. Here a maximum temperature somewhat higher than in previous curves is permissible because the time of the high temperature is reduced. In case VI, the ventilation is even less than in case IV because of the longer periods of standing still, and therefore the rms current of case VI must be still less than in case IV. The variations in rms current for the different cases are appreciable and with many motors the permissible rms current for applications like case VI can be only about one-half that of case I.

Case VII reproduces figure 7 of the Montsinger paper and represents a 24-hour load period. Usually with transformers the ventilation does not change, but there are some instances where blowers are operated during the heavier load periods and shut down during the light loads. The dotted curve *N* in case VIII indicates what would happen if the same load were used as in case VII. Obviously the load must be reduced to give permissible temperature conditions as shown in curve *O*.

Summarizing, it can be stated that for case I, which occurs frequently, the straightforward rms method is satisfactory regardless of the type of apparatus involved. For motors with intermittent loads, covered in cases II to VI inclusive, the method given in the Hellmund-McAuley (*Transactions pages 1050–5*) paper is basically a correct one. Case VII can be handled by the methods given in the Montsinger paper. The first difference between the two methods is that the time-temperature curves in the Hellmund-McAuley paper are determined by test because the greatly varying ventilating conditions make a theoretical determination impractical. In contrast to this, Montsinger has shown that for a wide range of transformer designs, the time constant and ventilating conditions do not materially change and therefore determination of time-temperature curves by calculation becomes practical in his case. The second difference between the two methods is that in the Hellmund-McAuley paper no attempt is made to quantitatively determine the permissible excess temperature indicated by the small *b* areas. Advantage of this permissible short-time excess temperature is taken to serve as a margin of safety, with one of the simplified and approximate methods discussed in the paper. In the Montsinger paper, a definite attempt is made to determine quantitatively the areas *a* and *b* to obtain the same insulation deterioration as with the rated continuous load. It is of course, quite possible to apply the Montsinger method to other cases, but such refinement does not seem justified until additional attempts have been made to apply motors closer to their permissible limit than is currently done. The transformer engineers, encouraged by simpler conditions to be met in their case and through efforts extending over a period of years, have made considerable progress in the economical application of transformers.

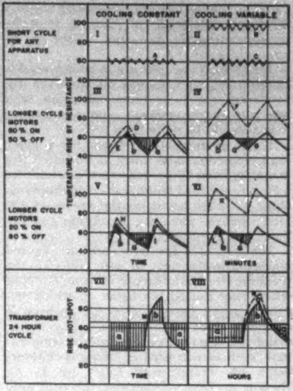


Figure 3

Any method suitable for all types of apparatus would be too complicated for practical use. The best procedure, therefore, is to consider certain classes of apparatus and applications individually and to make in each case the approximations and simplifications justified by the conditions involved. This will naturally result in a number of different methods of attack, as presented in various papers. Increased use of automatic protection as described in the Veinott paper (*Transactions pages 1055–61*) for motors and as used extensively with transformers will assist materially in economical applications of apparatus.

L. H. Hill (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The method of attack and analysis used by Mr. Montsinger is effective in obtaining a solution. However, if his results are to be used as an extension of the ASA rules for short-time overloads, they should be based on the same assumptions, that is, a hot spot at full load of 25 degrees centigrade above a 40-degree-centigrade oil instead of a 20-degree-centigrade hot spot above 45-degree-centigrade oil.

Likewise, if the rules are to be universal in application, they should be based on the minimum time constant shown by tests and not on the average, and the ratio of losses should be more than 2:1.

If the permissible overload for a given load factor varies as much as indicated (for example, from 16 per cent to 40 per cent for a 50 per cent load factor), the rule should include a factor based on the nature of the load cycle if users are to obtain the full benefit of the principle.

Temperatures for overloads are figured mathematically but do not consider the possibility of the existence of some hot spot which may exceed the figure on which calculations are based. Hence, I believe it desirable there should be a safety factor which increases with the estimated temperature, such as was used in "Effects of Overloads on Transformer Life" by L. C. Nichols in 1934. Otherwise the constants determined mathematically should be reduced by an amount sufficient to give this protection.

Attention is called to the constant *C* in the section on calculation of hottest-spot temperature, where the evaluation should be divided by 60 to put it on an hour basis. Also, the ASA rule for 1.4 times full load gives 25 minutes for recurrent conditions and approximately 100 minutes for emergency conditions instead of the values as printed.

It should be pointed out that while these proposed rules give permissible overloads due to load factors which do not take out more life than when operating at full load continuously and 40-degree-centigrade ambient, in the long run this might not give satisfactory life for the transformer. Previous papers relative to the life of transformers have given figures varying from about $1^1/_2$ to 7 years for operation at full load continuously and with 40-degree-centigrade ambient. The new tentative ASA standards recognize that transformers operating at full load continuously and with 40-degree-centigrade ambient might not have satisfactory life by recommending that for continuous full-load operation the average ambient should not exceed 30 degrees centigrade.

The tentative ASA standards now include permissible overloads well beyond those ever permitted by previous standards and likewise increased loads permitted when ambients below 30 degrees centigrade are allowed. Now if the remaining factor which affects the life of transformers, namely load factor, is used to permit overloading it is conceivable that with all of these factors combined, a transformer life may result which will be far shorter than is obtained under present conditions of operation; and hence a word of warning is perhaps in order: That while all of these various rules for permissible overloads are useful and valuable for specific purposes, they must be used intelligently if the life of the transformer is to be maintained at its present comparatively high value.

M. F. Beavers (General Electric Company, Pittsfield, Mass.): The term "load factor", as normally used in the industry, does not attempt to define the shape of the load cycle but simply ". . . is the ratio of the average load over a designated period to the peak load occurring in that period." For example, two extreme conditions for a 50 per cent load factor are: (*a*), a load cycle having 100 per cent peak load for an infinitesimal period and 50 per cent of peak load for the remainder of a 24-hour period; (*b*), a load cycle consisting of 100 per cent peak load for 12 hours and no-load for the remaining 12 hours.

Load factor for cycle *b* may also be expressed as the percentage of the total time which the peak load would have to be held to produce the total actual number of kilowatt-hours for the period of the load cycle under consideration.

It has been generally recognized that insulation deterioration is a function of both temperature and time. Since temperature is a function of losses, the relative kilowatt-hour loss for a given load factor should provide a rough measure of the effect of a load cycle on the transformer insulation life.

Inasmuch as the load factor does not define load cycle shape, let us consider the relative kilowatt-hour loss resulting from two extremes in load cycles having a given load factor. Assuming the total actual kilowatt-hour I^2R loss at 100 per cent peak load for 24 hours (100 per cent load factor) as 1.0, the relative total kilowatt-hour loss for a load cycle such as cycle *A* (which has 50 per cent peak load for the 24-hour period) is 0.5^2 or 0.25, while the relative kilowatt-hour loss for cycle *B* (which has 100 per cent peak load for 12 hours and no-load for 12 hours) is $1.0 \times 12/24 = 0.5$. In other words, when the cycle has a shape similar to cycle *A*, the relative loss is equal to the load factor squared, but when it is of the shape of cycle *b*, it is equal to the load factor.

Thus, it may be seen that the transformer aging will vary considerably depending upon the type of load cycle and the load factor. For the example just given (50 per cent load factor), the relative loss varies by the ratio of 2 to 1 (that is, $0.5/0.5^2$) while the loss for the two extremes of load cycles with say 25 per cent load factor will vary by the ratio of about four to one (that is, $0.25 \div 0.25^2$). Therefore, it is evident that, unless the shape of the load cycle is definitely specified, "load factor" is not a relative index to the transformer overload capacity.

Mr. Montsinger takes account of the limitations of loading by load factor, and therefore tables II and III of his paper are based on the type of load cycle giving the highest aging factor, namely—a cycle in which the peak load exists for the percentage of time equal to the load factor, and no load existing for the remainder of the period.

This method of transformer loading is interesting in that it represents a new approach to the problem. However, because of the assumed "fixed" load cycle shape, it does not have the flexibility (and purposely so) of the ASA loading curves which give recommendations for various initial load conditions in addition to no load. By means of the equivalent (or rms) load method described in reference 12 of the paper, the actual fluctuating loads of a daily load cycle may be converted into an equivalent rectangular load. Then upon reference to curves based upon the ASA recommendations, the required transformer size can be determined relatively quickly and with reasonable accuracy. Thus, each load cycle may be considered on its own merits rather than on a "fixed" basis.

I am glad to know that Mr. Montsinger is contemplating the preparation of a paper for large transformers similar to the one for distribution-type transformers described in reference 12 of his paper.

V. M. Montsinger: It is true, as Mr. Clark says, that the 3-per cent rule does not allow taking full advantage of loading by temperature for many of the shapes of load curves encountered in service. As pointed out in the paper, the 3-per cent rule for general use was purposely based on the most severe conditions. When some particular bank of transformers gets individual attention, the method used by Mr. Clark should be satisfactory.

It should be possible to set up rules for permitting safe overloads ranging from 3 per cent to approximately 6 per cent for each 10 per cent that the load factor is below 100 per cent. This is a matter that should be taken up and considered by the AIEE transformer subcommittee.

In reference to Mr. Clark's figure 2—I am not quite certain what the permissible overloads are based on, unless it is those to bring the rise up to 55 degrees rise in cases where the rise is under 55 degrees during the factory test. This of course is something that cannot be covered either in standards or in general loading recommendations.

As to permitting higher temperature limits for emergency operations, the transformer subcommittee has this question under consideration, and higher limits may be recommended, especially for a limited number of emergency operations of, say, one per year.

Mr. Hill suggests that the overload heating be based on 40-degree oil rise and 25-degree hot-spot rise over oil, and the minimum time constant.

If the 3-per cent rule was not very conservative as pointed out in my paper and by Mr. Clark, I would agree with the suggestion. Also, if and when the maximum loading is obtained by taking into account the nature of the load cycle as suggested by Mr. Hill and as was done by Mr. Clark, I would again agree with the suggestion of using 40 degrees oil rise and 25 degrees hot-spot. But, when as shown in the paper and as given in Mr. Clark's figure 1, the 3-per cent rule gives only about one-third of the maximum permissible overload (under certain load-cycle conditions), I feel that using an oil rise and hot-spot rise that would result in less overloads than given by the 3-per cent rule, is hardly justified.

Mr. Hill rightly points out that taking full advantage of all factors like low ambients, low load factors, ASA short-time overloads, "rating up" to give a 55-degree rise, etc., might result in a transformer life shorter than has been obtained in the past under usual service conditions. This is a very good reason why it is advisable when setting up general rules to use the 3-per cent rule for low-load-factor conditions, instead of squeezing the last drop out by taking full advantage of the load cycle as suggested by Messrs. Clark and Hill.

If full advantage is taken of the load-cycle curve by ordering a smaller-kilovolt-ampere transformer when the nature of the load curve is known in advance, the following factors should be taken into consideration:

1. Poorer regulation
2. Better voltage-control equipment
3. Capacitors may be necessary
4. The extra capacity for emergency overloads will be reduced

Proper consideration of the above points may show that it is not advisable to go too far in basing the overloads on thermal limits alone.

Reference Values for Temperature, Pressure, and Humidity

Discussion and authors' closure of paper 40-92 by P. L. Bellaschi and P. H. McAuley, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940 (December section) pages 669–75.

H. W. Tenney (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Messrs. Bellaschi and McAuley have rendered a very valuable service in tabulating the reference values for temperature, pressure, and humidity generally used in the various branches of electrical engineering. With these data presented so concisely, direct comparisons of the reference values used by national and international bodies become possible. The very important problem of co-ordination, particularly of such fundamentals as reference values, is receiving more and more attention from our engineering societies and standardizing bodies.

In the early days of standardization, the reference to temperatures at which tests were to be made was very indefinite—usually nothing more than "at room temperature." Later, the practice was modified and the specification read—"at room temperature or approximately 20 degrees centigrade." For the practical reasons given in this paper, the trend in this country for some time has been toward the use of 25 degrees centigrade, as is indicated in the table of reference temperatures for ASTM and AIEE specifications.

From an international point of view the whole question of reference temperatures is complicated by the different climatic and living conditions in different countries. In England, for example, the usual temperature at which testing is done is generally somewhat lower than in this country because of natural climatic and living conditions. Under these conditions a reference value of 20 degrees centigrade is perfectly suitable and logical.

If flashover values are established on a given piece of equipment at 20 and 25 degrees centigrade, the value at 25 degrees centigrade is 1.7 per cent lower. On the other hand, if flashovers are established at vapor pressures of 0.45 inch (International Electrotechnical Commission standard) and 0.6085 inch (present American standard) of mercury, the flashover value at the higher vapor pressure is 5 per cent greater. Consequently, flashover values on a given piece of equipment at the higher reference temperature and humidity will be approximately 3 per cent more than the flashover at the lower reference values. Translated into the problem of designing equipment to meet a definite insulation level at either set of reference values, the equipment meeting this level at the lower reference values will be 3 per cent stronger. In international trade this gives a slight advantage to a manufacturer who rates his equipment at the lower reference figures (IEC values) and naturally those having a slight advantage in characteristics will exploit this fact.

The difference in ratings at different reference values enters into the problem of co-ordination of insulation and should be recognized. It is quite possible that the change would be so small that it would not be serious, but at least it must be given consideration.

P. H. McAuley: Mr. Tenney has pointed out the different reference temperatures in Europe and America have a more or less logical basis when climatic conditions are considered. It is more difficult to justify the American reference standard for humidity, except for those cases where electrical creepage and moisture absorption are important. In any case, it is apparent that we have, at present, the uneconomical luxury of different reference values for similar measurements. At least we should avoid any further tendencies in this direction and, if this paper contributes only in this respect, it has served a useful purpose.

Low-Voltage D-C Measurements on Electrical Insulating Oils

Discussion and authors' closure of paper 40-81 by J. L. Oncley and W. C. Hollibaugh, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940 (November section) pages 625–8.

Hubert H. Race (General Electric Company, Schenectady, N. Y.): The flat portion called region *B* (figure 1) of the conductance-time curve for insulating liquids has always puzzled me. The authors state that "region *C* represents the time during which the ions are swept out of the space between the electrodes and space charges built up in the spaces near the electrodes". It seems to me that this statement must also apply to region *B* because certainly the current carriers are ions. Therefore, if we assume that on the average the charge per ion is constant, we must have conditions such that in region *B* the number of ions remains constant while in region *C* the number of ions is gradually decreased. From his work on nonaqueous solutions, Doctor R. M. Fuoss of our laboratory suggests the explanation that in the region *B* there is an equilibrium between associated and dissociated ions so that new ions are formed by dissociation as fast as they are swept out by the field. When the supply of undissociated ions is exhausted, however, further current flow decreases the ion concentration and the current decreases as in region *C*. The mathematical form of this time dependence is given by Doctor Fuoss as follows:

Let

Λ_0 = limiting equivalent conductance
k = dissociation constant
F = chemical equivalent (96,500 coulombs)
t = time in seconds
i = current per kilovolt per centimeter in a unit cube
C_0 = initial concentration of A^+B^- in mols per liter

The dissociation equilibrium is expressed by the relations:

$$[A^+][B^-]/[A^+B^-]=k \tag{1}$$

$$A^+ + B^- \rightleftharpoons A^+B^- \tag{2}$$

Assuming

$$C(t)=C_0-it/F \tag{3}$$

the current-time function can be shown to be

$$i(t)=\frac{\Lambda_0 k}{2}\left[\sqrt{\left(1+\frac{\Lambda_0 t}{F}\right)^2+\frac{4C_0}{k}}-\left(1+\frac{\Lambda_0 t}{F}\right)\right] \tag{4}$$

which is of the form shown by sections *B* and *C* in figure 1 of the paper by Oncley and Hollibaugh.

Eric A. Walker (Tufts College, Medford, Mass.): In this paper the authors present some data on the electrical characteristics of oil which are surprising in their simplicity. These results are attained because of complete and careful control of the test specimens and they serve to emphasize the fact that if consistent and worth-while results are to be obtained on electrical insulating oils the control of the specimen is of paramount importance.

The authors state that certain deviations are caused by fluctuations in temperature. Perhaps in future work a curve of the variation of the a-c conductance as a function of temperature will be obtained. There are some unpublished data in use by a number of companies which contend that the ratio of the power factor at any arbitrary temperature to the power factor at 20 degrees centigrade is a constant which is not affected by the degree of oxidation. Confirmation of these data by other investigators is desirable and apparently Messrs. Oncley and Hollibaugh are well equipped for such work.

No statement is made of the amount of moisture present in the oils tested. It is presumed that, as the samples were in closed containers into which pure dry oxygen was admitted, the moisture present was only that caused by oxidation. However, if the samples were open to the atmosphere, moisture might be present in solution, and if the temperature were suddenly lowered, in minute droplets. Under such conditions the test results would be much more complicated. This is another effect which should be investigated.

J. B. Whitehead (The Johns Hopkins University, Baltimore, Md.): The conductivity of insulating oils is ionic in character. There are a number of possible sources of the ions present. The commonest source is the presence of electrolytic impurities. These may be largely removed by filtration, redistillation, and similar methods. However, even after ultimate purification a conductivity still remains. Possible explanations of this residual conductivity are, inherent dissociation of the liquid molecule, dissociation of organic acids due to oxidation, radioactive influence either local or extraneous, such as cosmic radiation. The conductivity of a good insulating liquid is so low in value that only a very small volume of ionization is necessary to account for it. Values of conductivity of the simplest and purest chemical liquids are of the order of 10^{-18} mhos. Values for the oil studied by Messrs. Oncley and Hollibaugh are in the neighborhood of 10^{-13} mhos, that is 100,000 times greater, a value somewhat higher than the best commercial oils, but still not unduly high from the standpoint of general insulating properties.

The conductivity of an oil is a function of the conditions of measurement, particularly those of potential gradient and time. Referring to figure 1 of the paper, region *A* is a very unusual one and has been found in only a few cases. The distinguishing characteristics of most insulating oils are an initial horizontal region *B*, indicating constant conductivity, and the region *C* indicating a continually decreasing conductivity. The region *D* is due to an equilibrium condition of ions accumulating in the liquid as a result of the action of the ionizing agency, and the processes of recombination and diffusion to the walls of the containing vessel. These ions are relatively large consisting probably of a central charged particle surrounded by a cloud of neutral molecules. Under applied potential they move towards the electrodes and the initial conductivity remains approximately constant over an appreciable interval of time, say several tenths of a second, as determined by the number of ions present and their mobility. Thereafter, as shown by region *C*, of figure 1, the conductivity decreases due partly to the discharge of ions at the electrodes, and partly to the accumulation of space charges near the electrodes, the space charges setting up a counter, or polarizing influence on the applied field. The final value of conductivity in region *D* of figure 1 is a measure of the rate at which new ions are liberated in the liquid due to the external ionizing agency. If, after reduction to the condition *D*, the oil is allowed to stand on either open or short circuit, it returns to condition *A* or *B* after a few hours.

The conductivity indicated by region *B* of figure 1 is directly proportional to the applied continuous voltage. This was clearly shown in my paper, "The Conductivity of Insulating Oils" (AIEE TRANSACTIONS, volume 50, number 2, 1931, page 692) for potential gradients up to 30 volts per mil. Moreover, we have shown in several places [Whitehead and Shevki, ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), volume 54, June 1935, page 603], that for the same range of stress, the time duration of the constant value of region *B* decreases with increasing voltage and vice versa. The range of stress in which these relations are found to hold has recently been increased to 60 volts per mil. Messrs. Oncley and Hollibaugh have made observations in a very low range of stress which afford interesting confirmation of our earlier experiments.

As regards behavior under alternating stress, dielectric loss is completely accounted for as a conduction phenomenon, in that measured a-c values are in close agreement with those computed from the conductivity of region *B*. This means that during the alternating cycle there is no loss of ions by discharge to the electrodes, and that the ions oscillate back and forth within the liquid, causing a frictional or resistance loss. This suggests that the amplitude of oscillation of the ions is in general so short that few reach the electrode during the alternating half cycle. From this point of view, it is evident that we should expect an influence of the a-c frequency on the agreement between d-c and a-c measurements here under discussion, and also of the values of a-c and d-c potential gradient at which the measurements are made. Moreover, it happens that cases have been found even at the relatively low potential gradients mentioned above, in which the conductivity of region *B* increases with increasing continuous voltage. In this case measurement at low-voltage direct current would not give agreement with a-c loss measurements at higher values of stress. For these reasons and also because of the possible occurrence of region *A*, figure 1, in comparative measurements of this character, I have always felt that it is safer to make both continuous and alternating measurements at the same values of potential gradient. The authors do not state the values of voltage at which their alternating measurements were made.

Other interesting evidence that the dielectric loss in oils is a conduction phenomenon is found in the experiments of Whitehead and Shevki in which it is shown that over a prolonged extension of regions *C* and *D*, momentary measurements of alternating loss are always accounted for by the contemporaneous values of d-c conductivity. The same relation holds for measurements on the recovery curves of the oil both on open circuit and on short circuit.

Andrew Gemant (nonmember; Detroit Edison Company, 2000 Second Ave., Detroit, Mich.): This subject seems to have attracted quite an interest recently; in fact, I also have just finished some computation on this matter. The basic assumption in these computations was that the first relatively fast drop of the conductivity is due to the removal of ions in a state of total dissociation while the subsequent slow decrease is caused by the removal of ions belonging to partially dissociated electrolytes. Equations were deduced for the first case and applied to curves published by J. B. Whitehead. The ionic mobilities computed were of the order of 10^{-7} centimeter per second per volt per centimeter, involving radii of the expected order 10^{-7} and 10^{-8} centimeter. Mr. Oncley mentioned mobilities down to 10^{-9} centimeter per second, and consequently very large radii. These results are probably obtained from the latter part of the conductivity curves, where a continuous regeneration of ions makes the mobilities appear much too low. This difficulty is, however, avoided if the simple equations, not taking account of a dissociation, are applied to the first part of the curve only.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): I am very much interested in the curve shown in figure 1 as it has practically the same shape of the curve of dielectric strength versus time of both solid insulation and oil.

In 1935 I published an article on "Breakdown Curve for Solid Insulation" (AIEE TRANSACTIONS, volume 54, page 1300) in which it was shown that the dielectric strength drops quite rapidly down to 3 microseconds, and then the strength remained approximately constant for approximately 100,000 microseconds. It then dropped rapidly corresponding to the *C* region shown in figure 1. I designated these regions as *A*, *B*, and *C*.

During the discussion of the paper, Mr. Bellaschi presented a similar-shaped curve for the breakdown of oil, although his region *A* lasted somewhat longer than region *A* for solid insulation. Also, the time during region *B* (for constant strength) lasted for only approximately 10^3 microseconds as against 10^5 microseconds for solid insulation.

The principal point I wish to bring out is that the similarity of the shapes of all these curves indicates that there is some relationship between the conductance and dielectric strength of insulations. I do not believe that it is merely a coincidence.

I note the authors state that region *C* represents the time during which the ions are swept out of the space between the electrodes and space charges built up in the space near the electrodes. The rapid decrease in dielectric strength of oil during region *C* might be due to some extent to the same phenomena, although it no doubt is also due to the lining up of impurities in the oil.

The rapid decrease in dielectric strength during region *A* may be due entirely to the suggested cause for the rapid change in the conductance during region *A*, namely, the orientation of large polarized particles.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): It may be well to point out that since the preparation of this paper in October 1938 considerable progress has been made in this research project at the Massachusetts Institute of Technology. The oil-testing cell illustrated in figure 2 has been replaced by a much-improved cell for routine tests. With the old measuring cell, it was necessary to transfer the oil sample from the container where oxidation was taking place to the measuring cell. This involved some handling and possibilities of contamination. Furthermore, thorough cleaning was necessary after each measurement.

With the new system, measurements of power factor and other electrical characteristics of the oil are made directly in the sealed container in which the oxidation takes place. This not only takes care of the objections just mentioned but also permits the making of more frequent measurements. The new scheme is also used for measurements on small paper capacitors immersed in the oil in the oxidation apparatus.

In the paper and in discussions given by others at this meeting concerning d-c measurements, it must be noted that the remarks apply to moderate or low stresses, that is, from a few volts per millimeter up to about 1,500 volts per millimeter.

As the authors point out in their discussion of figure 1, the d-c conductivity of oil is a rather complicated phenomenon, being the sum of several electrical processes. Still other characteristics appear when the stresses are increased to very high values. Some special studies with direct current are being made in Chicago at such high stresses. The work is being done by Doctor H. J. Plumley of the Commonwealth Edison Company in a part-time special research at the University of Chicago under the sponsorship of the Utilities Research Commission. This work is carried on at stresses up to the order of 100,000 volts per millimeter. In general, Doctor Plumley has found that insulating liquids perform differently at higher stresses than they do at the lower stresses. He expects to give some details on the results of his investigation and his ideas of theories to account for the characteristics of liquid dielectrics in the near future.

J. L. Oncley and W. C. Hollibaugh: We would like to make a few remarks concerning the discussion of our paper. The gradients used in obtaining the results reported in table II were 2 volts per mil for the a-c measurements and 0.5 volt per mil for the d-c measurements. Results recorded in figures 4 and 5 were obtained with gradients of 0.5, 5, and 12.5 volt per mil. The oils used in these measurements contained practically no moisture. We have attempted to make measurements on oils containing droplets of water, but found that it is a very difficult matter to obtain reproducible results under these conditions. The a-c conductance of several oils has been studied as a function of the temperature, and will be reported by Balsbaugh and others in the near future.

The calculations mentioned by Doctor Gemant appear to be of the same type as those we have made, and were obtained from the initial portion of the conductivity curve. Thus the mobilities obtained from our measurements are considerably smaller than those obtained from Whitehead's curves.

The improvements in cell design and oxidation technique referred to by Mr. Halperin have been reported in part by Balsbaugh and Howell (*Rev. Sci. Inst.*, 1939, page 194) and by Balsbaugh, Howell, and Assaf (paper presented at the Symposium on Electrical Insulation, American Chemical Society, April 1940).

The behavior in regions *B* and *C* can be explained by the assumption of either total or partial dissociation. Calculations of mobilities as made by us, and as referred to by Doctor Gemant in his discussion, assumed total dissociation. Doctor Race gives equations for the case of partial dissociation. Without further experiments on simple systems it would appear very difficult to differentiate between these explanations. Either predicts that region *B* is not absolutely constant, but only a very small decrease in conductance will occur during this small time interval.

The main point of the work reported by us is just that mentioned again by Professor Whitehead; namely, that "the time of duration of the constant value of region *B* decreases with increasing voltage and vice versa", and our work was prompted by the observations he refers to in his discussion. Thus by making the applied voltage sufficiently low, we can prolong region *B* to any desired extent, and hence we can obtain conductivities which truly represent the state of the oil before the test was begun. In case results for high gradients are required, it is obvious that the technique of Whitehead and his collaborators must be used. However, an amplifier-oscillograph arrangement is somewhat complex for routine testing of oils, while the low-voltage bridge method, which we describe, can be applied with but little more labor than can the standard (but arbitrary) one-minute test for conductance.

A Method for Detecting the Ionization Point on Electrical Apparatus

Discussion and author's closure of paper 40-84 by G. E. Quinn, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940 (December section) pages 680–2.

Wm. A. Del Mar (Habirshaw Division, Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Mr. Quinn's paper holds more than usual interest for us, since we have employed the cathode-ray oscilloscope for a number of years in electric cable research and quality control.

Our experimental techniques have developed similarly, and we have noted substantially the same limiting factors. Like the author, we found the air-core reactor a more critical means of ascertaining the initial ionization point. But, we noted a difference in the character of the superimposed high-frequency oscillations, which was more pronounced with a resistance shunt. By this difference, we have been able to classify the type of ionization and its position of origin. Thus, with the resistor, if the high-frequency harmonics appear on the peak of one-half wave the corona is internal, if on the other, it is external, and if both half waves show high-frequency harmonics, there are both internal and external corona. It is easy to find which half wave corresponds with a particular kind of corona by substituting a sample of known corona characteristics. The high-frequency harmonics sometimes appear as distinct surges which may have an amplitude several times that of the fundamental wave, and sometimes as mere fuzziness of the fundamental wave. The former were found when ionization occurred at a metallic surface such as conductor or shield and the latter when no metal or other high-emissivity electrode was present.

The resistance oscilloscope test therefore possesses certain advantages over that used by the author in enabling an analysis of the location and type of ionization to be performed. We, therefore, suggest that the equipment be provided with both reactor and resistor shunts.

Let me now say a few words on the use of the cathode-ray oscilloscope in quality control. The data on ionization point of rubber-insulated cables obtained by Professor Paine (AIEE TRANSACTIONS, 1940, pages 709–14) showed how close this point may be to the working voltage. It, therefore, seemed desirable to check all high-voltage rubber cables of an unusual character for ionization voltage. Samples are sent to the research laboratory for this purpose as the test apparatus has not graduated to the stage of practical plant use and the interpretation of results requires the judgment of an expert in its use.

J. B. Whitehead (The Johns Hopkins University, Baltimore, Md.): It is my impression that the first published oscillographic records of high-frequency oscillations on the crest of voltage waves applied to dielectrics and insulation were those of Professor Edward Bennett (AIEE TRANSACTIONS, volume 32, 1913, page 1787). Professor Bennett used the electromagnetic oscillograph. Since that time these oscillations have been widely studied, particularly by Professor Paine of the University of Illinois, and latterly, since the general availability of the cathode-ray oscilloscope, by many other research workers. We have been using the oscilloscope as a convenient and rapid instrument for the qualitative detection of gaseous discharges in insulation for several years. The convenient self-contained setup described by Mr. Quinn will doubtless lead to an even wider use of this method for the detection in practice and the study in research, of these important discharges.

The discharges referred to by Mr. Quinn are mostly those in the air, external to the insulation of high-voltage equipment, as for example, in current transformers and lightning arresters. Another well-known danger spot is the slot of an a-c generator. The discharges attack the material of the insulation both thermally and chemically, leading to well-recognized processes of destruction.

It should be possible, on the basis of well-known dielectric principles and the properties of air and dielectric materials, to so design many types of electric equipment that they will be free from discharges of this character. The minimum potential breakdown gradient for air is in the neighborhood of 30 kv per centimeter for plane surfaces and with well-known corrections for any value of radius of curvature, temperature, pressure, etc. These, with the values of dielectric constant of the insulation and the dimensions of the equipment, should make it possible to compute the stress at the danger spots and thus to modify design which, with suitable factors of safety, will ensure the avoidance of external gaseous ionization.

Such discharges also occur in the interior of insulation walls, when air or gas pockets are present. A conspicuous example is found in the high-voltage underground cable with so-called solid-type insulation. The operation of the cable results in temperature cycles causing expansion and contraction of

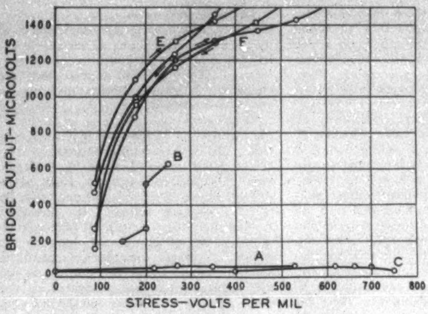


Figure 1

the insulation, with consequent voids or gas pockets which become ionized under the applied stress, causing the type of oscillation on the crest of the voltage wave indicated in Mr. Quinn's figure 3.

In figure 1 of this discussion, I show the results of a study of these discharges using a quantitative method as applied to impregnated-paper insulation as found in high-voltage cables. The method is that of Arman and Starr modified and extended somewhat to meet the conditions obtaining in these studies. The principal feature of the method is the balancing of a Schering bridge containing the sample under study, for a minimum reading for the high-frequency discharges set up in an auxiliary discharge gap across the high-voltage circuit, and at a voltage below that at which the specimen under study discharges. The auxiliary gap is then removed, the voltage raised on the specimen, and the bridge becomes unbalanced when the specimen discharges. The method, therefore, gives not only the detection of the first occurrence of discharge, but also permits a quantitative study of the volume of discharge for higher values of voltage, or for their growth with time. In figure 1 curve *A* shows the behavior of a specimen on an initial gradual increase of stress up to 700 volts per mil and shows that there is no internal discharge up to that point. Curve *B* shows the behavior after discharge had started at 700 volts per mil maintained for ten minutes. Curve *C* was taken after a 36-hour rest period, indicating a recovery of the insulation. Curve *E* indicates the discharge after application of 700 volts per mil for one hour, followed by 750 volts per mil for 55 minutes. Curve *F* was taken immediately following curve *E*.

H. A. Brown and **E. B. Paine** (University of Illinois, Urbana): This paper is of great interest to the authors of the following comments, and they strongly feel the importance and desirability of perfecting means of directly detecting the presence of ionization within, and surface corona discharge on electrical apparatus. They also believe that it is often very important at times to have means of quantitatively measuring comparative intensities of such disturbances. It is shown in Engineering Experiment Station Bulletin No. 260 of the University of Illinois that cases occur in solid-type paper-insulated high-voltage cables wherein internal ionization discharge will occur before the power factor shows an increase, as the voltage is raised. In testing rubber-insulated cables lying on a flat metal test plate, the power factor is only slightly affected when the cable is covered with the usual braid, the latter resulting in high power factor at voltages much below the corona point. This is shown in a paper by the authors of this discussion, "Corona Discharge on Rubber Insulated Cables" presented before the 1939 Northeastern District meeting of the AIEE (paper No. 39-108, AIEE TRANSACTIONS, 1940, pages 709–14).

The apparatus used in the previously mentioned Bulletin No. 260, and in the later paper just referred to, differs from that shown in figure 1 of Mr. Quinn's paper in several respects, particularly in that a bridge circuit or a coupled filter system is used to balance out or suppress almost completely the 60-cycle charging current components and harmonics thereof. In the cases of some paper-insulated cables the 60-cycle charging current is more than a thousand times as great as the mean ionization discharge current intensity. Many times the latter was detected only with the aid of a 90-decibel-gain amplifier system and highly sensitive head receiver, and increased very gradually at first from the starting point as voltage is raised. In the case of standard 500-foot lengths of such cables it is indeed difficult to detect inception of ionization when the charging current is measured in amperes, and the long high-capacitance circuit weakens the ionization discharge effect. For these a partially resonant type of discharge detection bridge circuit, described in the above mentioned bulletin, partially solves this problem. This circuit includes capacitance and variable inductance, and it is believed that the loading capacitor shown in Mr. Quinn's figure 1 may give a similar result.

The effectiveness of the blocking capacitor in Mr. Quinn's diagram would be limited, it seems. Its impedance would be of the order of 10 megohms, at 60 cycles, and the oscilloscope amplifier input impedance is probably 2 megohms. At the random heterogenous discharge frequencies of around 5,000 cycles the impedance would be roughly 0.1 megohm for the blocking condenser, and 0.5 megohm for the amplifier input. This may suppress the charging current in a ratio of say 25 to 1; but if the charging current is 1,000 or more times as great as the ionization current the comparative amplitude of the former is still quite high. A bridge or effective coupled-filter system, previously referred to, is strongly recommended, especially in testing apparatus wherein the capacitance charging current is considerable. In testing rubber cables such a filter was used, and the inductance between the test sample and ground was 150–200 millihenries instead of 4 henries plus 4 millihenries. This gives better suppression of the 60-cycle charging current, because of the comparatively high ionization disturbance frequencies.

In using an air-cored inductance, care must be used to eliminate the effect of stray *magnetic* fields, such as may be encountered near cables, busses, or even transformers. It has been found quite necessary to use astatically wound coils for the inductance between test piece and ground. It is granted that a filter system produces distortion of the ionization discharge current pattern, and oscillograms show that regularly damped oscillations are set up in the filter, but much experimental evidence has shown that this does not affect the sensitivity of detection. An aperiodic resistance-capacitance filter will not greatly distort the pattern, and is sufficient if the test piece comprises one arm of a balanced shielded resistance-capacitance bridge.

The cathode-ray oscilloscope with a two-stage input voltage amplifier is a valuable and convenient addition to the test equipment.

Since the ionization discharges are of a random nature, stationary patterns on the oscilloscope screen would be difficult to obtain. The writers wonder if one of the new persistent-screen cathode-ray tubes and a high-speed lens, shutter, and film was used to obtain the photograph shown in figure 3 of Mr. Quinn's paper.

C. W. Germeck and **C. Mannal** (General Electric Company, Pittsfield, Mass.): Mr. Quinn has called attention to a very interesting phase of dielectric behavior and has developed a simple and ingenious method for the study of the high-frequency oscillations accompanying ionization discharges. Under extremely adverse conditions the ability to localize the discharge on the crest of the applied voltage wave may be of considerable advantage in determining which of the peaks observed are spurious and which depend on the apparatus under test.

It is interesting to note that Mr. Quinn's apparatus embodies all major components essential to a standard radio-noise-testing equipment as recommended by the Joint Coordination Committee of the Edison Electric Institute, National Electrical Manufacturers' Association, and Radio Manufacturers' Association. If an r-f series impedance were inserted in the line between the loading condenser and the testing transformer and the coil combination in series with the device under test replaced by a 600-ohm non-inductive resistor, the uncompensated circuit as recommended in EEI Publication G9 would be obtained. It would, of course, be necessary to replace the CRO and associating blocking capacitor by a radio-frequency voltmeter having the desired characteristics. Since standard meters are now commercially available in the same price range as the CRO, it can be appreciated that the circuit is readily convertible to the standard radio influence-voltage circuit, which permits of quantitative evaluation of the radio noise influence voltage.

In a sense any device which indicates the magnitude of high-frequency voltage may be used for the investigation of ionization in high-voltage apparatus. Mr. Quinn obtains the deflection of a spot on a cathode-ray screen. The standard method recommended in the EEI Publication G9 utilizes the deflection of a pointer on a calibrated scale. Both are indications of the high-frequency voltage existing as a consequence of ionization discharges.

Mr. Quinn has performed a distinct service in reviewing in his introduction the chemical and thermal effects accompanying ionization. It is too infrequently emphasized that the term "corona" covers a plurality of effects—thermal, electrical, chemical, aural, and visual—of which certain are important from the standpoint of radio-interference potentialities and others from the standpoint of insulation failure.

If it is permitted to restrict Mr. Quinn's remarks to the apparatus with which we are familiar in Pittsfield, our conclusions would be that there are two modifying factors worthy of consideration. They are the *quantity* of ionization at operating potential and the *location* of the ionization. Although the voltage at which ionization can first be detected is of importance, particularly in design, it is the latter two factors which are more closely related to the life expectancy of the insulation and probability of radio-interference complaint.

It should be emphasized that the effects of ionization which bear on the life expectancy of insulation are thermal and chemical and that even these are not related in a simple way to the deterioration of the insulation. Exactly how the high-frequency voltage appearing across the terminals of the apparatus in question is related to these primary effects and hence to the life expectancy of the insulation is obviously a very complex matter.

It would, therefore, be desirable to establish a correlation between a quantitative measurement of the high-frequency voltage and the other aspects of corona. Basic to any such generalization is the accumulation of a considerable body of data concerning the high-frequency voltage at operating potential and the condition of given types of insulation as determined by physical examination.

E. R. Thomas (Consolidated Edison Company of New York, Inc.): I am somewhat surprised to hear comments that the method presented by Mr. Quinn of using a cathode-ray oscilloscope for determining the value of voltage at which ionization takes place on a piece of electrical equipment should be questioned as a method for determining this value. The further comments suggested that the radio noise meter developed by the Joint Co-ordination Committee on Radio Interference of the EEI, NEMA, and RMA should be the preferred instrument to measure the value at which ionization takes place. While it may properly be discussed whether or not a piece of electrical equipment is satisfactory for operation on a voltage above which ionization can be detected, it does not seem logical to me to make use of an instrument which measures the radio noise from a given piece of electrical equipment and say that this necessarily is, or even is related to, the value of ionization potential. The radio noise meter consists of a selective radio-frequency amplifier with an indicating voltmeter arranged to measure the audio-frequency component of the radio emission after detection. This integral of the audio-frequency component of a damped radio-frequency wave may vary over extremely wide limits for a given peak value of radio frequency existing in the wave train. While ionization sets up radio-frequency emissions, the frequency of these emissions is determined to a large extent by circuit constants which may be made up of the apparatus alone or of connecting circuits in conjunction with the apparatus. Measurements made therefore on such a piece of equipment by means of a radio noise meter will generally give different values for different values of frequency in the radio spectrum. Radio frequencies are also generated by means other than ionization; variable surface leakage and varying contact resistances are equally important factors in causing radio interference such as would be measured by a radio noise meter.

To conclude, it appears to me that Mr. Quinn has proposed a simple and workable method for measuring the value of ionization potentials. If it is desired also to specify certain limits of radio noise generation by electrical equipment operated at normal voltage then the radio noise meter is a perfectly satisfactory instrument to measure that noise at specified values of radio frequency.

A. T. Sinks (General Electric Company, West Lynn, Mass.): Mr. Quinn's paper suggests a method for detecting corona in electrical apparatus which is different from the method conventionally employed. In so doing, the question is naturally raised as to which method is the better, especially from the standpoint of detecting destructive corona, since the latter is what the author seems to be primarily concerned about. In considering this question of the relative value of the two methods, one cannot help but wonder about the absolute value of either method and, therefore, quite naturally seeks data to determine these values. In order to get such information the instrument-transformer department of the General Electric Company obtained nine 15-kv current transformers which had been in service at 13.8 kv on the lines of a nearby utility company for from 15 to 20 years, and made radio-noise tests. Some of these transformers were similar to the one the author calls defective because it showed low starting voltage. All the transformers tested showed radio noise at operating voltage, ranging from a few hundred microvolts to over 9,600. However, only one of these transformers showed any signs of damage to the varnished cambric insulation, either when tested electrically or when inspected mechanically. Moreover, the only evidence of damage on this one current transformer was a small hole in each of the two outer layers of cambric, the total insulation being 18 layers of cambric. This transformer and one other which, judging from rust on the iron core, had been operating in a wet or damp location, showed some deterioration of the cotton tape over the black varnished cambric where the rust had built up, but the cambric under the tape was affected on only one of the current transformers. The transformer which had the highest radio noise, showed no evidence of any damage whatsoever. Moreover, these were not special transformers; thousands of others like them have operated for years without any indication of trouble.

It is recognized, however, that corona can be destructive and that insulation failures have at times been attributed to it. Therefore, the question is: "What is destructive corona?" On the basis of experience and tests, it seems doubtful whether "radio-noise influence voltage" or "corona starting voltage" tells us much about it, and furthermore, upon analysis there seems to be no very good reason why they should. It seems only reasonable to suppose that damage due to corona at any point would be associated with the intensity of the corona at that point, whereas the radio noise is probably the result of corona or ionization at many points, so that the intensity of corona in critical regions of the insulation cannot be determined by either of the methods of measurement mentioned.

While no good engineering reason can be adduced for having corona or radio noise in electrical apparatus it is sometimes expensive to eliminate it entirely and in the light of experience it is a question to what extent this expense is justified.

Therefore, if for economical reasons a certain amount of radio noise must be tolerated, a means of measuring the magnitude of the radio influence voltage rather than the line voltage at which this radio noise starts is desirable.

Moreover, a high corona starting voltage does not insure a low radio-noise influence voltage at operating voltage unless the latter is below the starting voltage, and even then there must be a considerable factor of safety because measurements of radio-noise and corona starting voltage are sometimes impossible to check from time to time.

It would be quite helpful if some means of telling when corona is seriously harmful could be developed, but until such means are available it would seem that experience is the best guide.

P. L. Bellaschi (Westinghouse Electric & Manufacturing Company, Sharon, Pa.): The results that Mr. Quinn has found comparing the radio-noise and the oscilloscope methods for detecting the ionization point on electrical apparatus are of interest.

Experience with the method recommended by the Joint Co-ordination Committee on Radio Reception of the EEI, NEMA, and RMA has been satisfactory. For instance, several years ago a laboratory was built at Sharon. Circumstances required that this laboratory be located adjacent to the high-voltage laboratory, and that its power supply be taken from the adjacent shop building where sources of interference from welders and similar equipment are inevitable. Yet it was possible, through ordinary design procedure and the application of available equipment and recognized methods, to put the laboratory into effective routine operation in the regular course of a short time. The laboratory has been in satisfactory operation since, and in it a wide range of tests and investigations have been made. More recently the same coupling method, consisting however of portable equipment, has been used in connection with measurements on large high-voltage apparatus. These tests of necessity had to be made in the shop building where all sorts of other electrical equipment are in operation. Again the results have been found satisfactory. Experiences such as these are fairly typical no doubt, and they are repeated elsewhere where similar installations are in daily service.

In his contribution, Mr. Quinn raises another important question. This pertains to the so-called "ionization point", to its significance and evaluation in the field of engineering. The writer is well aware of the controversial character of this question and merely wants to restate the principle that engineering is an art in which scientific methods and economics are inevitably interlocked. Apparatus design usually is the result of the happy combination and balance between these two and often conflicting factors. From the operating standpoint, it has been stated frequently that the criterion of satisfactory apparatus is that it should render a good account of itself in terms of good service and economical service. From the practical engineering end, the so-called "ionization point" then takes on a somewhat different significance and requires broader interpretation than would otherwise be the case were it considered from the purely physical approach.

S. W. Zimmerman (General Electric Company, Pittsfield, Mass.): Mr. Quinn's paper is an excellent example of the versatility of the cathode-ray oscilloscope in the hands of the electrical engineer. In the lightning arrester laboratory at Pittsfield, experiments have been made with equipment similar to that described by Mr. Quinn. A simplified circuit was used for determining the value of applied voltage at which high-frequency oscillations occur in the test piece. The oscilloscope measured the voltage drop across a resistance shunt. The errors introduced into measurements due to the use of a high-resistance shunt are insignificant when the 60-cycle current is small. However, the use of a resistance shunt is subject to limitation as indicated in the paper. If the 60-cycle current passed by the test piece is relatively large, the small superimposed high-frequency components of the current may cause only a small deflection which will be difficult to detect on the oscilloscope screen.

The start of ionization can also be determined from the volt-ampere characteristic of the test piece obtained by an oscilloscope. This method was reported by Peek ("The Law of Corona and the Dielectric Strength of Air—IV", F. W. Peek, AIEE TRANSACTIONS, 1927, pages 1009–1024) as well as by Lloyd and Starr, in connection with corona studies.

It has been the writer's experience that a fair correlation may be obtained between determinations of the starting point of ionization made with the oscilloscope and with the standard method recommended by the Joint Co-ordination Committee. The standard method was found to be more sensitive and is recommended since with it a good correlation can be expected from tests by different investigators which is an essential requirement. In addition, the standard method permits quantitative measurements of the disturbance generated which is not obtained from the oscilloscope equipment.

G. E. Quinn: Mr. Del Mar's experience with the cathode-ray oscilloscope method of test and particularly his use of a resistance shunt for classifying the type of ionization are of great interest. In general test work the initial point of ionization is the most important consideration and therefore a reactive shunt was used to get maximum amplification. However, in manufacturing, it is essential to know the nature and source of the ionization and Mr. Del Mar's suggestion should prove of great value. With the addition of this important feature, it should be more readily possible to raise the minimum voltage at which ionization occurs in electrical equipment.

Dr. Whitehead has described a very useful method of obtaining quantitative results and when such information is necessary it appears definitely superior to the radio-noise-meter method. As pointed out by Dr. Whitehead, it should certainly be possible to design many types of electrical equipment so that they will be free from ionization discharges at operating voltage. The test data which Dr. Whitehead submitted should prove of value to anyone interested in this subject.

Professors Brown and Paine also have referred to an excellent method of making quantitative measurements. In answer to their questions:

1. The work referred to in this paper was all done on equipment of low capacitance and the constants used were found adequate. However, recent cable tests have shown that under high-capacitance conditions it is desirable to reduce the impedance of the air-core reactor and resort to additional amplifiers in the input circuit to the oscilloscope.

2. Figure 3 was obtained using a persistent screen and a high-speed camera, timed for one sweep of the oscilloscope. However, figure 4 was taken with a much longer exposure. It will be noted that the patterns are stationary enough to produce a fairly sharp figure.

C. W. Germeck and C. Mannal indicate that the method recommended by the joint committee of EEI, NEMA, and RMA for radio-noise testing, which also includes a circuit for the measurement of radio-noise influence voltage is now "standard" for the type of testing discussed in this paper. This assumption has been adequately disposed of by E. R. Thomas in his foregoing comments. As Mr. Thomas points out, the radio-noise meter does not measure true ionization peak, is limited in frequency range, and responds to other disturbances as well as to ionization. This extension of its field is very definitely open to question.

Mr. Sinks is correct in his assumption that the primary consideration in this paper is the possible deteriorating effect of ionization rather than the radio noise which accompanies it. The results of his inspection and tests on nine current transformers serve only to emphasize the futility of depending on radio-noise measurements. As was to be expected, he found no relation between radio-noise influence voltage and deterioration. As Mr. Sinks agrees, corona can be destructive and insulation failures have been attributed to it. Since radio noise tests are no measure of this deteriorating effect, the solution lies either in raising the minimum voltage at which ionization occurs or in developing a method of determining its probable deteriorating effect.

Mr. Sinks has found it impossible at times to reproduce the voltages at which ionization occurred. This trouble is sometimes encountered in unsatisfactory equipment, but when the voltage at which ionization occurs is satisfactorily high, retest usually gives essentially the same results.

Mr. Bellaschi stresses the economic considerations that must enter into design, and we are in agreement on that point. Experience has shown, however, that when deficiencies are called to the attention of manufacturers in a competitive industry, changes in design and improvements in the quality of manufacture usually are effected at much lower cost than first anticipated.

S. W. Zimmerman reports fair agreement between the oscilloscope and the radio-noise-meter methods of checking the minimum voltage at which ionization occurs in electrical equipment. This substantiates the findings in the paper except that in one case there was a very decided difference. As indicated, this may be ascribed to the narrow band of response of the radio-noise meter. The oscilloscope method does not have this restriction and therefore is to be preferred. Mr. Zimmerman has apparently not had Mr. Del Mar's success with the resistance shunt, but it is to be hoped that he will continue the study further.

It was the purpose of this paper to promote discussion of methods of checking ionization in electrical equipment and the excellent response received is very gratifying.

Rapid-Recording A-C Bridge

Discussion and authors' closure of paper 40-82 by W. Mikelson and H. W. Bousman, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940 (November section) pages 628–31.

George Keinath (consulting engineer, Larchmont, N. Y.): I am very pleased to see that the General Electric Company is recommending the same procedure for testing high-voltage equipment as I did when I presented a paper on the same subject at the winter convention of the AIEE in January 1939. My experiments in this line started seven years ago, after some very bad experience with voltage transformers, which showed that the passing of the ordinary one-minute test is in no way an assurance of good performance in the field. First I used a photographic recorder for the active component of the capacity current, a vibrating mechanical rectifier, and a d-c galvanometer. The diagrams gave the active component first in function of the increasing voltage, during the proper test in function of time. A number of such diagrams have been published,[1] but the recorder was too delicate for routine testing and so I decided to build an ink recorder.

The "Numograph," as originated by Geyger[3] uses two watt-hour meters, one tuned for the active, the other for the reactive component of the amplified current in the diagonal of the bridge, together with a zero indicator and motor.

So far as I know, at least 20 of these recorders are used all over Europe, by laboratories as well as by manufacturers, for development and for routine testing.[1–5] Recording power factor and capacity has proved to be of extreme value, and, as I pointed out when I presented my paper, I was able to reduce the weight of instrument transformers to a small fraction of the former weight, without any sacrifice in safety.

Apparently, as far as performance is concerned, there is no difference between the General Electric recorder and the "Numograph." It seems to me that the latter is simpler because zero indicator, phase selector, and motor are united in one element.

I found it very important to have a record of power factor not only at constant voltage, but also during the increase of voltage, beginning with 10 or 20 per cent of full test voltage. To obtain this, there were two ways: one was to increase the test voltage proportionally with the time and also to move the paper proportionally with the time with an ordinary clockwork; the other was to move the paper proportionally with the test voltage. These diagrams revealed with absolute clearness the position of the ionization point, and their shape was highly informative. The same recorder was used for temperature tests on transformer oil, increasing the temperature proportional to the time or moving the paper proportional to temperature. Such tests, running without attendance, gave far more information on the qualities of transformer oils than any test at constant temperature, be it low or high temperature.[6]

The main objection to my proposition for such a method of testing high-voltage equipment and insulation material in general was the high price of the outfit. Therefore I should like to hear from Mr. Mikelson what will be the approximate price of his recorder, of course, minus Schering bridge and minus standard capacitor, because these two items are also necessary for any hand-operated bridge.

Furthermore, I should like to get the fol-fowing information:

A time of one second is given for balancing the bridge. Is this the time for the pen to travel across the whole width of the paper or the average time for smaller deflections? I should not think that it is necessary to have such a short time, after we have always spent about half a minute to get just one reading with the manually operated bridge.

Has the phase shift of the amplifier to be exactly zero or 90 degrees or only approximately?

Is the output of the amplifier changing the indication or only affecting the speed of the recorder?

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3. FIRST DIAGRAMS WITH "NUMOGRAPH" RECORDER, tests on bushings, current and voltage transformers, life tests on high-voltage cables. *Archiv für Technisches Messen*, V 339–14, December 1935.

4. Diagrams for eight different designs on bushings, for 10–45 kv line voltage, 42–119-kv test voltage. *Archiv für Technisches Messen*, V 339–15, April 1936.

5. MEASUREMENTS OF CAPACITY AND DIELECTRIC LOSS ON SMALL RADIO CONDENSERS, *Archiv für Technisches Messen*, V 339–17, August 1936.

6. POWER FACTOR MEASUREMENTS ON TRANSFORMER OIL WITH DIFFERENT REFINING TREATMENT AND WITH IMPURITIES, WITH VARYING TEMPERATURE, *Archiv für Technisches Messen*, V 942–4 July 1938.

W. Mikelson and **H. W. Bousman**: Concerning Doctor Keinath's questions, the balancing time of one second is the time required to rebalance through the whole chart width, for example, when switching from one specimen to another. This seems to be quick enough for the applications of the bridge made thus far. We can conceive that a record of specimen behavior every half cycle might be useful in understanding the phenomena accompanying breakdown. This would be in the oscillograph range rather than the bridge range. The point we wish to make is that it is unsafe to say that an instrument responds more quickly than is necessary. As soon as a better instrument is available, more is learned, pointing the way to the need for a still better instrument.

The amplifier phase shift must of course approximate an integral multiple of 90 degrees. The approximation need not be very close; 15 or 20 degrees difference seems to make little difference in bridge behavior.

The bridge balance point is not affected by amplifier condition. It is only necessary that the amplifier sensitivity be sufficient to furnish a signal to the motors when the bridge is out of balance by any amount in excess of the allowed error.

The Dielectric Strength and Life of Impregnated-Paper Insulation—II—Influence of the Thickness of the Paper

Discussion and author's closure of paper 40-104 by J. B. Whitehead, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940 and published in AIEE TRANSACTIONS, 1940 (December section) pages 660–3.

H. H. Race (General Electric Company, Schenectady, N. Y.): I believe I am speaking for the subcommittee of the AIEE committee on research, which has followed Dr. Whitehead's work very closely, when I say that we are particularly pleased by the care with which this work has been performed and by the demonstrated reproducibility of the results. The number of similar specimens tested has been sufficiently large and the deviations in the life results have been sufficiently small to leave no doubt in our minds as to the validity of the trends which Dr. Whitehead has plotted in his figure 1.

I think the readers of this paper will be interested in the objectives which were agreed upon at the beginning of the work, as indicated by the minutes of the first meeting of the supervisory committee. "The factors which result in deterioration and eventual failure of oil-impregnated paper insulation under high voltage stress may be grouped roughly in two classes: first, those factors which, in the absence of gaseous ionization, cause gradual chemical changes resulting in increasing dielectric loss and eventual thermo-electric failure, and second, those factors which directly cause gas liberation, gaseous ionization, carbonization and electrical failure." It was agreed to conduct these experiments so as "*to minimize chemical deterioration not resulting from electrical stress and to accentuate and study the factors affecting ionization or other physical effects of high voltage stress.*"

An understanding of the above objectives is important in evaluating the results. Since the tests last only three days or less and since the testing temperature is only 40 degrees centigrade, it seems safe to assume that chemical deterioration, in the sense in which the term is used in AIEE paper 39-113 ("Tests on Oil-Impregnated Paper—IV—Mechanism of Breakdown," by H. H. Race, AIEE TRANSACTIONS, 1940, pages 721–9), is negligible in Dr. Whitehead's researches. Furthermore, it seems that the stated objectives of these researches have been met in that the evidence indicates that the cause of failure is progressive ionization, and the effects of changing paper thickness have been determined and tentative explanations offered for the results obtained.

In attempting to evaluate these results in terms of practical operation of oil-impregnated paper insulated cable, we should remember, however, that these tests were made at very high gradients and that the order of excellence of such specimens might be entirely different if they were tested for years instead of days at gradients comparable to service conditions. This is true of all short-time high voltage tests. To my mind the major advantages of such tests are: first, the information gained concerning the mechanism of failure so that the results of changes in construction can be scientifically predicted; and second, the knowledge that the best specimens on such tests have the highest factor of safety on transient overvoltages.

J. F. Calvert (Northwestern Institute of Technology, Evanston, Ill.): The matter which I should like to hear discussed by the author is not directly in line with this paper but perhaps is sufficiently closely associated to raise at this time.

In 13-kv armature insulation which has been stressed to failure, if the bond is largely washed out by a solvent, usually it is found that a small hole is burned out perpendicular to the mica and tape for some distance without deviating as much as from $^{1}/_{8}$ inch to $^{1}/_{16}$ inch to go around a flake of mica. Then near the end of this straight shot the path may show a rather extensive area of burning sideways along a layer or two of tape for as much as from $^{1}/_{2}$ inch to 3 or 4 inches before it again travels perpendicular to the layers as at the section where the path was first traced. Generally speaking it seems that the more compact samples of such insulation and those having the higher breakdown are the more likely to show a pronounced side shift (path burned out parallel to the layers of tape).

These tests were made at 60 cycles at room temperature and failure occurred within about one minute on a steadily rising alternating voltage. Some of the tests were made under oil to prevent flashover, and some were not. The latter seemed to have no important effect. The oil had too little time to penetrate the insulation on the rectangular bar sample. Some of the tests were made with asphaltic bonds, and some with various other types of bonding material. The physical appearances of the failures were substantially the same. In all cases hand-wound half-overlapped mica tape was used with the same bond employed in the tape as was brushed on to fill between the layers of tape. The mica tape was composed of two outer layers of Jap paper nominally 0.001 inch thick, and two to three layers of mica between giving an over-all thickness of about 0.007 inch for the finished tape when finally in the coil. The bar samples were vacuum treated and impregnated in an asphalt gum tank. The vacuum treatment removed most of the solvent, but the gum impregnation seemed to furnish more of a hydraulic pressing action than an actual impregnation of the tape with gum.

These observations were made some years ago at the Westinghouse company in collaboration with Messrs. Krogh and McCullough.

One might safely assume that as the voltage was raised, that rather intense internal corona was produced at various locations within the coil, and probably that it occurred in many cases in flat sheets of corona in the sides of the coil between the paper layers. It is interesting to note, however, that no partial failures were found in the outward direction (i.e., perpendicular to the mica layers). I would greatly appreciate hearing the author's theory concerning the probable mechanism of such dielectric failures as I have described above. It would be interesting to know whether he feels that the actual puncture starts from the electrodes, from the ionized air pockets, or from both and something of the way in which this failure occurs.

J. B. Whitehead: As is well known, there are a number of elements or factors which enter into the question of the stability and the life of the insulation of high voltage underground cables. The great problem in the improvement of cable insulation and in cable design has for many years been the proper evaluation of the relative importance of these various elements. The problem is as yet unsolved, but steady progress is being made. For a number of years we have been studying one after another of the limiting elements referred to, taking one at a time and eliminating as far as possible all the others. Thus, for example, we have reported upon the drying process, the relative values of different oils in accelerated life programs, the dielectric loss in impregnated paper as related to the losses in its constituents, and others.

Dr. Race has described the aims in view in the research in which we are now engaged. In somewhat simpler terms, the work might be described as a study of the conditions under which high stress leads to the liberation of gas and failure in impregnated paper insulation. Very clear cut evidence has already been obtained as to the influence of a variation of the density and of the thickness of the paper and further evidence is now being produced as to the influence of the oil viscosity. A great deal of evidence has been collected as to the conditions under which initial failure occurs, and its growth to final breakdown. This will be presented in a later paper.

Dr. Race has emphasized the high stresses in our work as compared with the operating stresses in cables, and that caution should be used in making direct deductions for cable design. I concur in this view. I wish to point out, however, that the only question here is whether there is any difference in the mechanism of initial gas liberation at high stress and at low stress other than a difference in the time rate for liberation and that of progressive failure following initial gas liberation. This is an old question. Thoroughgoing investigations of the time element in cable failure have always broken down by reason of the long time involved and the wide spread of experimental values amongst a number of specimens. In view of the uniformity with which we are now able to construct our specimens, of the narrow spread of experimental results, and of the methods which we have developed for detecting the beginnings of failures and partial failures, I am confident that an investigation of the time element in cable failures is not nearly so difficult as formerly, and am hopeful that we may find opportunity in our further programs for studying this important question.

As to Mr. J. F. Calvert's question, I have received the impression that the failures in armature bars which he describes, originate in local point discharges from the surface of the slot. Such point discharges have very definite drilling power and would account for the short straight holes he describes. However, as the depth of the hole increases, the conductivity of the spark constitutes a point of ground potential advancing into the wall of the insulation. As it advances, a horizontal or lateral component of the stress is developed and in increasing magnitude. Finally, this lateral component is able to cause a discharge through a gas layer or over a mica or other surface or even over the contact surface between two different materials. This discharge parallel to the layers is also conducting and will eventually pick out another weak spot in a direction normal to the conductor surface. I judge that internal corona-forming gas layers are often present in such insulation. Such layers would facilitate the mechanism as suggested above. The rate of increase of voltage described by Mr. Calvert is rather rapid. A slower rate of increase, say over a period of days to breakdown, might possibly develop partial failures such as we have had no difficulty in obtaining in cable insulation.

The Type CB Impregnated-Paper Cable

Discussion and author's closure of paper 40-121, by S. J. Rosch, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940, pages 1041–9.

R. W. Atkinson (General Cable Corporation, Perth Amboy, N. J.): Rosch shows the results of load-cycle aging tests on two cables, in one of which the conductor is shielded by carbon-black tapes, the other cable being similar except without this shield. His data are in line with those obtained by other laboratories in indicating that the addition of shielding tapes improves cable performance. However, while others give credit for the improvement to the *electrical shielding* effect of the tapes, Rosch apparently attributes the improvement to the *adsorptive* effect of the carbon black in the tapes. As will be shown later, Rosch's own data do not bear out his conclusion.

That the electrical shielding effect is actually of major importance has been effectively demonstrated by weight of evidence obtained in different laboratories. The tests in our laboratory have included load-cycle aging tests, each several weeks in duration, on some 48 cables, half with shielded conductors in comparison with a like number without shielding. Some of the shielded conductors were shielded with carbon-black paper and some with aluminum foil, and we found no difference in the effect of the two kinds of shield.

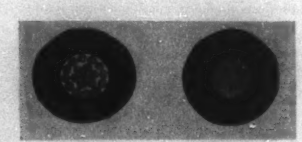


Figure 1. Styrene wafer showing cross section of cable with conventional and compack round strand bringing out the difference in oil space of conductor surface. (Partly due to difference in wire sizes)

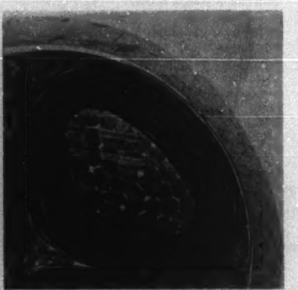


Figure 2. Styrene wafer of three-conductor 350,000-circular-mil compack sector with 250 mils paper, 9/64 inch lead. After accelerated aging test

In a paper presented informally by the speaker at an Edison Electric Institute meeting in February, and published in the April 1940 issue of *Electric Light and Power*, the "change in structure" that produced the gain of about 12 per cent in voltage strength there indicated was the use of shielding tape over the conductor. Further details of this investigation were printed in the July issue of *Electric Light and Power*.

In brief, important gain is found by the use of shielding tapes over the conductor. This gain is found whether the tape is paper made electrically conducting by the incorporation in it of finely divided carbon or by a covering of thin aluminum foil. From this, it will be clear why we do not agree with the author's crediting of this gain to some special property of the carbon incorporated in his CB paper.

Rosch's data of test 6 reported in his tables III and IV show the results of some interesting comparisons. Table I of this discussion will be found enlightening. While this shows that electrical shielding, whether over the conductor or over the insulation, produces great improvement, it fails to show that a carbon-black shield is superior to a metal shield.

Does the carbon black have an important function in adsorbing conducting ions from the oil? Rosch's data seem to show such a tendency, but are hardly sufficient to warrant such a conclusion. Doubtless he now has considerable supplemental data. Tests carried on in our own laboratory establish that *ordinary insulating paper* placed in oil may reduce the rate of power-factor increase when the oil is heated in the presence of oxygen, presumably by adsorption of ions responsible for the electrical conduction. The same action also occurs with carbon black, varying in extent with the type of carbon black. However, in order to demonstrate this characteristic, even in a minor degree, it has been necessary to carry on these tests on a test tube scale under closely controlled laboratory conditions. In cables which have been made with metal shielding and with shielding of carbon-black paper, the over-all improvement due to the shielding has been so great that any difference between the two methods of shielding has been too small to be differentiated from the total gain if, indeed, there is any difference.

Aside from the general results of the load-cycle aging tests showing the over-all benefits obtained, the gain may be illustrated in various ways. Figures 1 and 2 of this discussion, showing the spaces around the conductors which are shielded from the electrical stress, illustrate well the advantages which might reasonably be expected from the shielding.

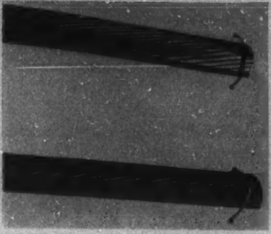


Figure 3. Appearance of shielded and unshielded conductors after load cycle aging test. Note wax on unshielded and none on shielded construction

Figure 3 shows a shielded and an unshielded conductor from which the compound has been drained in an oven following dissection after test. The thickened compound and wax in the interstices of the unshielded strand and in the valleys between the edges of the innermost tape is very obvious on direct examination, contrasting with the complete absence of wax in the strand which was shielded.

Figure 4 shows tree design on an unshielded conductor following the load cycle aging test. This contrasts with the fact that on the cables with the shielded conductor, no deterioration of this sort has been found.

Figure 5 shows measurement of power factor at very high stresses, such as is obtained in relatively short-time breakdown tests and shows a large reduction in the power factors at high stresses resulting from the conductor shielding.

This paper and our comments may be summarized as follows. The author has performed a limited amount of testing indicating some beneficial effect of carbon black on power factor of oil exposed to highly oxidizing conditions. His test 6 on cable is supposed to show the superiority of the carbon black over another form of shielding, but clearly this experiment supports no such conclusion. From the above and from tests in which comparison is made between cable where the conductor is shielded with carbon-black paper and other cables without conductor shielding, he attempts to draw the conclusion that all or substantially all of the gain shown by the cable with the shielded conductor, comes from the *adsorptive properties* of the carbon black rather than from the *electrical shielding*. Such a conclusion is wholly unjustified from the data. Moreover, our laboratories have shown by extensive tests that suitable conductor shielding accomplishes important benefits and that such benefits are obtained by the use of metal faced tapes as effectively as with tapes made electrically conducting

Table I. Comparisons Taken From Tables III and IV by Rosch

Comparison Between Lengths Nos.	Nature of Comparison	Effect of Change of Structure on Breakdown Strength
1 and 2	Addition of CB tapes at conductor	*Gain* of 94 volts/mil
2 and 6	Addition of copper shielding tape	*Gain* of 121 volts/mil
2 and 4	Addition of CB tapes near the sheath	*Gain* of 185 volts/mil
6 and 7	Addition of CB tapes near the sheath	*Loss* of 4 volts/mil

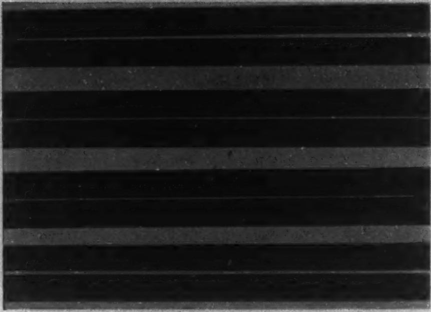


Figure 4. Typical tree designs found on inner tapes of samples with unshielded conductors

with carbon black. Though such shielding has long been known in the art and though it is covered by patents so old as to have expired, it has remained for recent tests by our laboratories and others to give actual demonstration of the really practical, as well as theoretical gain from this shielding.

Wm. A. Del Mar (Habirshaw Division, Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Mr. Rosch deserves great credit for espousing the cause of conductor shielded cable in America. Such cable has offered a fertile field for inventors, both here and abroad, since 1914 when Beaver patented a cable with metallized paper shielding on a stranded conductor. The general idea developed out of (1) a statement made by Jona in 1904, that the dielectric stress at the surface of an insulated stranded conductor is much greater than that at the surface of an equivalent solid conductor and (2) the belief that the life or breakdown voltage of a cable is, in some way, increased by lowering the calculated stress at the conductor surface.

I shall not discuss the Jona theory about the stress at the surface of a stranded conductor being increased due to its geometric configuration as it is immaterial to my argument.

However, I do take issue with those who try to explain the advantages of conductor-shielded cable on the basis of a reduction of geometrically calculated field intensity.

According to the maximum stress theory, stress may be calculated from the geometrical form and applied voltage. The life of insulation under voltage will depend upon keeping the stress, at the point of calculated maximum stress, below a critical or high value.

If this theory be correct, cables of equal maximum stress should show equal lives on voltage-time test. D. M. Robinson ("Dielectric Phenomena in High Voltage Cables," page 164) however, cites the case of two cables of widely different conductor sizes designed, however, for equal geometrical maximum stresses. On voltage-time test, the cable with the small conductor required a much greater voltage to break it down. M. Klein, in his "Kabeltechnik", 1929, page 68, gives similar data and indeed such data may be obtained from the records of any laboratory which has made many breakdown tests on cables.

According to the same theory cables with small conductors should be more heavily insulated than those with large conductors. Yet AEIC insulation thicknesses are the same for all conductor sizes regardless of stress and some engineers believe that thinner walls may be used safely for small than for large conductors, both negations of the ordinary maximum stress theory.

Another experimental fact which may be interpreted as conflicting with that theory is the greater dielectric strength of oil between small spherical electrodes than between large ones.

According to the same theory, the maximum stress in a 700,000-circular-mil cable will be something like 20 per cent less than that in a 500,000-circular-mil cable and if we accept Jona's and Atkinson's view that shielding a stranded conductor reduces the maximum stress by 20 per cent, a shielded 500,000-circular-mil stranded conductor should be only the equal of an unshielded 700,000- or 1,000,000-circular-mil conductor. Tests, however, show it to be better.

The fact that shielding is not equivalent to making the conductor smooth, is afforded by the favorable test data obtained on shielded conductors as contrasted with results obtained by a direct experimental comparison of equivalent stranded and smooth conductors as given by Fernie in BEAMA, September 1921. This comparison showed no difference between them. Similar comparisons have been made by others, with the same result.

If it be urged that the maximum stress theory is supported by the fact that cable failures almost invariably initiate near the apex of a strand where the stress is greatest, I would reply that this occurs, not because of greater geometrically calculated stress, but because the oil gap is thinner there than between strands and therefore subjected to a locally greater stress.

The value of strand shielding must, in my opinion, be explained otherwise than by the maximum stress theory, a theory which requires complete restudy in the light of P. L. Hoover's AIEE paper of 1926. It is not that deterioration is not a function of maximum stress but that maximum stresses as calculated by geometric formulas, alone, are not correct.

Now comes Rosch with a new theory—the theory of adsorption. He explains the advantage of conductor shielding by a chemical, rather than an electrical, theory. He seems to rely, very largely, on the data in his figure 6, giving rises of power factor of cables made with carbon blacks of different adsorptive indices. If the data shown in this figure mean anything it seems to me that they show that adsorption is not the essential factor, as low power-factor rises are obtained with carbons of both high and low indices while comparatively high power-factor rises are shown for intermediate values. However, I doubt whether these data have any significance, as the differences in power-factor rise, for cables with the various carbons, lie within the normal variations to be expected between samples of identical construction.

More positive information was obtained in our laboratory by placing CB paper from one of Mr. Rosch's CB cables in a bombardment cell with Sun XX oil and subjecting the oil to bombardment, making a similar run on the oil without CB paper. The cell used for this test is described in the following.

Power-factor tests of the oil before and after a 450-minute bombardment run in the cell showed no difference due to the presence of the CB paper. The CB paper, however, reduced gassing about 20 per cent showing that only the gaseous products of deterioration and not the more harmful liquid products were adsorbed. A point of saturation of gas adsorption is reached after which the protective value of the carbon black, as an adsorbent, ceases.

Such long-time improvement in power-factor stability as may occur due to the use of CB paper must therefore be due to reduced ionization resulting from less gas formation, rather than to adsorption of those products of oil decomposition which tend to raise the so-called "solid" power factor.

This brings us to a third theory of the conductor-shielded cable propounded in 1929 by engineers of the Siemens-Schuckert Company and given in British patent No. 349,-289 dated May 28, 1931. This patent describes a high-tension cable with impregnated paper insulation and lead sheath in which a few layers of paper containing "soot" are placed directly upon the conductor or directly under the sheath, or at both points. Emphasis is placed on the fact that these layers have comparatively low insulation resistance and that due to this, "The field intensity in these is so far reduced that ionization can no longer occur."

In other words, the soot (i.e. carbon black) in the paper, is used to lower the intensity of discharges near the conductor surfaces. Discharges from metal are always more violent than discharges from high resistance nonmetallic surfaces or coated metallic surfaces such as paper-backed metal foil, in which the paper adheres firmly to the metal.

This may be demonstrated very clearly in the laboratory by oscilloscope observations of the wave form of charging current of a vacuum discharge cell. Such a cell, designed by E. J. Merrell, is shown in the accompanying figure 6. It consists of a metal electrode in a vacuous flask and a water electrode outside the flask. Alternating voltage applied between electrodes causes a discharge from the metal electrode through a sample of oil in the bottom of the flask. The oil rises in power factor and gives off gas, a reaction characteristic of a cable with ionized voids. The gas evolution is measured by a mercury manometer and the power factor rise by measurements with a Davidson type cell. When discharges take place from the metal electrode, the transients are well defined whereas if the metallic electrode is replaced by a nonmetallic or a coated metallic electrode, they appear as a mere fuzz on the wave.

Mr. Rosch's figure 10 showing condition of cable after high-voltage time test supports the energy theory rather than the adsorption theory as it is hard to see why adsorption should prevent the formation of corings (i.e. burned spots) such as appear on the tape of stranded cable. On the other hand, this is the obvious result to be expected on the basis of the energy theory.

When comparing Beaver's aluminum-coated paper with CB paper according to the energy theory, bombardment tests showed that whereas CB paper improves gassing but has no effect on power factor stability, aluminum has a marked effect on improving power factor stability at the expense of a slight increase in gassing.

Which of the two materials will prove better in service, only time will tell, as the relative importance of gassing and "solid" power-factor rise may not be the same under service conditions as in accelerated tests. I believe that some form of conductor-shielded cable will be adopted very generally, but the fact that neither the "soot" cable nor the metallized paper cable found favor in Germany or England where they were well known because of the patents of Siemens-Schuckert, Beaver and others, should give us pause before we commit ourselves to them wholeheartedly, particularly as means to reduce insulation walls.

An example of the difficulties that may arise is that the makers of carbon-black paper say that they must use rosin size, sodium abietate, to hold the carbon black in suspension. This material, unfortunately, is highly corrosive of copper as noted by Stephens and Gehrenbeck in AIEE TRANSACTIONS, June 1940, page 359. Ordinary cable paper is free of this material but such CB paper as I have seen, judging by the color it imparts to a flame, contains considerable sodium, and judging by the Liebermann-Storch test, it contains considerable abietic acid. Whether these are in combination, I do not know. Perhaps Mr. Rosch can tell us whether such difficulty has been overcome.

Until such matters have been discussed and settled satisfactorily, I prefer to attain power factor stability by the combined use of the most stable impregnant and by the scientific use of the elastic properties of paper, by which means *both* reduced gassing and "solid" power-factor stability are attainable. Conductor shielding, however, is a promising field that should be carefully studied, and its greatest value may be found in combination with these other improvements.

Our laboratories have been studying the subject from the point of view of the energy theory outlined in the foregoing and there are indications that the best conductor shielding may be neither CB paper nor metallized paper.

For some years it has been a favorite indoor sport of the cable purchasers to make radial power-factor curves and to give their approval to cables having low boundary power factor. Now comes Mr. Rosch with a cable in which the boundary papers are deliberately given a power factor of about100 per cent. In my discussion of Wyatt and Spring, AIEE TRANSACTIONS, 1936, volume 55, page 204, I asked: "Is it not possible that a high power factor of the inner tapes may be a good thing?"

Now we have the answer and doubtless to the surprise of many, it is affirmative, with the proviso that the high power factor must not be due to cumulatively deteriorating material. If you start with 100 per cent power factor, as in CB paper, you cannot have any rise, cumulatively or otherwise, but you accomplish a suppression of the energy of discharge due to this property of the paper. This idea was back of my question in 1936.

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): The effect of corrugations at the surface of stranded conductors and the advisability of smoothing the surface by some form of shielding has been a perennial source of theoretical discussion and trial ever since high-voltage cable came into use. Mathematicians have shown that for the average conductor size with relatively small strands, as used in power cable work, the increased stress at the apex of the strand is a very few per cent as compared with an equivalent smooth surfaced conductor. Practical trials have also shown that this small theoretical advantage of a smooth surface cannot be realized unless this surface is pervious and allows an outward flow of compound for maintaining impregnation in the adjacent paper tapes.

The semiconducting carbon treated paper tape described by Mr. Rosch is pervious and does smooth off the conductor surface but Mr. Rosch is not quite as modest as the mathematicians in the benefits claimed. In addition to improvement of ionization characteristics by reduction of electrical stress gradient at the conductor surface, Mr. Rosch also claims that the carbon-paper wrappings act as an absorptive screen and prevent the spread of metallic soaps into the main body of insulation.

Ionization and metallic soap formation both result in increased dielectric loss, but they are different effects from distinctly different causes. In reporting his test results Mr. Rosch makes no distinction between them. In my case at least this has led to confusion in trying to translate and evaluate his test results in terms of actual life under service conditions.

The causes and results of ionization in solid-type cable under normal service conditions are too well known to require comment here. Metallic soap action is much rarer and less well known. The following comments may be of some interest.

Metallic soap formation is an oil oxidation

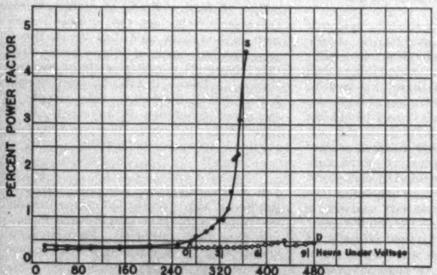


Figure 5. Curves showing change in power factor with voltage during short-time breakdown tests

Curve "*S*" shows results for a group of samples with unshielded conductors while curve "*D*" shows results for a group of samples with shielded conductors and with a change in paper and saturant from "*S*". Conductor shielding has been found to be the predominant factor in the large reduction in ionization

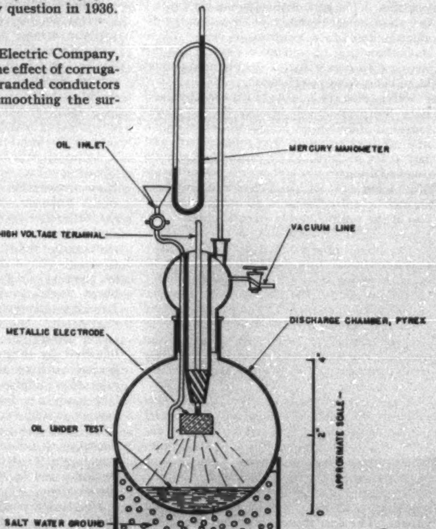


Figure 6

action. It can only occur in the presence of oxygen, which must first pass into a combined and corrosive form in the oil. Striations on the inner paper tapes, such as shown in figure 1 of Mr. Rosch's paper, are an accurate and sensitive measure of the degree and severity of metallic soap action in cable. The dark markings are a form of oil sludge and represent an end result of the oxidation cycle. This sludge is formed by precipitation of metallic soaps from colloidal solution in the cable oil or compound. While in colloidal solution the metallic soaps can penetrate more or less into the main body of insulation, depending upon the degree and severity of the action. This results in a corresponding increase in dielectric loss, which in severe and exceptional cases can be very pronounced. In ordinary cases it does little more than form a moderate upbend in the radial power-factor curve in near proximity to the copper conductor and sheath. When all oxidation impurities are consumed a maximum condition is reached and precipitation may even reduce losses thereafter.

We have carried on a study of metallic soap action in cable for some years past. The chemistry involved is very complicated but the cause and prevention are well understood and we have been able to reproduce this action and the resulting striations at will and to any degree desired in the laboratory by controlling the amount and kind of oxygen impurities introduced into the cable. We have also been able to eliminate this action by following the fundamental expedient of keeping all oxygen and oxidation impurities out of the cable.

A study of the test data in Mr. Rosch's paper leads to the conclusion that his miniature samples, tests 2 to 5 inclusive, are not representative of modern high-quality cable, but instead contained an appreciable and variable amounts of oxygen or oxidation impurities. The striations alone in his figure 1 are convincing evidence of this, but the dielectric loss data confirm it also. These data show that no ionization existed and the increase in power factor during aging must have been due to oxidation.

Refer to Rosch's table II for example, and note the pronounced increase in the power factor at 100 degrees centigrade after only 5 and 10 days aging at that temperature and 100 volts per mil. The unshielded sample reached a maximum 100-degree power factor of 23.8 per cent within 5 days and remained stable thereafter because all oxygen impurities in the sample had been consumed. The carbon-paper shielded sample reached a 100-degree power factor of 8.7 per cent in 5 days and a further increase to 10.5 per cent in 10 days, showing that all oxygen had not yet been consumed. Plainly, the carbon-paper shielding did not prevent the penetration of oxidation action into the insulation. From the standpoint of practical operation the difference in stability and quality of these two samples is not very marked. Both are unacceptable.

Rosch's test 1 gives a direct comparison of the comparative absorptive abilities of regular cable paper and carbon-treated paper. His table I shows there is little practical difference between them. The amount of oxidation impurities absorbed from the oil by regular insulating paper is only a little less than that absorbed by carbon-treated paper. The first three wrappings of regular cable paper next to the conductor shield the rest of the insulation about as well as the same amount of carbon paper would. This points to the conclusion that the right time and place to purify oil with activated materials, such as carbon or Fuller's earth, is before it is used in the cable and not afterward.

If a service failure has ever occurred in solid-type cable due to metallic soap action, I have never heard about it, nor have I ever seen evidence of striation markings on inner paper tapes under normal operating conditions. The properties of solid-type cable are not favorable to this kind of action, except under accelerated and exaggerated conditions in the laboratory. Even under accelerated conditions the action is not pronounced in modern cable of good stability unless an excess of oxidation impurities are present. I would still rather depend upon the well founded principle of removing all traces of impurities from the cable initially and making sure that none enter during installation and service.

Ionization still remains our real problem with solid-type cable. If strand shielding proves of practical benefit it will be because of improvement of ionization stability. Any stable type of conducting or semi-conducting shielding that removes stress from the compound lodged in the strand crevices will accomplish this purpose.

Rosch's test 8 is of particular interest in this respect. Here, he used two 45-foot lengths of regular cable of good quality. Contrary to the miniature samples, the test data show that ionization was the primary cause of instability. One of these three-conductor cable lengths had conductor strand shielding and one did not. Mr. Rosch does not state whether the sector conductor was compacted. The smoother surface of compacted conductors would have some bearing on comparative results.

These lengths were subjected to load cycle life test at 100 volts per mil. The length without strand shielding began to show signs of instability at the end of 52 days and finally failed in 202 days. The length with strand shielding reached instability in 210 days but had not reached final failure at the last report in 400 days. Its behavior after instability started was much the same as that of the first length.

As far as this one test is concerned some advantage for strand shielding is shown. Normal variations in individual test lengths could, however, cover a wider range than the difference between these two lengths. Additional tests of this kind are needed for averaging. Before this is done I would like to suggest that the conditions of this test are too favorable to strand shielding as compared with actual operating conditions. Ionization in service is caused by compound migration. During such migration the larger strand spaces drain first and this is followed by drainage of the smaller butt spaces between paper tapes. Finally, if migration still continues, even smaller capillary spaces are drained through the wall of insulation and "dry spots" develop. Cable must be on a slope for this kind of complete drainage to occur. When it does, ionization is scattered throughout the insulation thickness and not concentrated at the strand spaces alone.

Mr. Rosch used a test length of 45 feet and this was laid out level on the floor. The only drainage that occurred was circumferential and was due to a slight stretching of the lead sheath. It was distributed uniformly along the length and there was no longitudinal drainage at all. This kind of compound movement is hardly enough to drain the strand spaces in the upper half of the cable cross section. Incipient ionization is accordingly concentrated at the conductor surface and strand shielding will reduce this.

Load-cycle tests with longer lengths arranged to allow a reasonable amount of longitudinal migration would be more representative of service conditions and give a more accurate comparison of the benefits of strand shielding. It may be that repeated tests of this kind will prove that strand shielding is sufficiently worth while to warrant the cross-sectional space taken up. At the present time it is an interesting speculation, as it always has been in the past.

L. G. Smith (Consolidated Gas, Electric Light and Power Company, Baltimore, Md.): The development in cable design described by the author is very interesting. Investigations of this type contribute to progress. However, as the author has stated this is a progress report, there are several questions that might suggest possible future studies.

In the type CB cable, does a sufficient amount of the total oil in the cable contact the carbon-black paper to obtain the desired stabilizing effect? Might not the beneficial results be due to shielding? If the standard samples used in the tests reported in table II and figures 3 and 4 were not shielded, these tests might indicate the effectiveness of shielding rather than the effect of carbon black upon the oil. Test 4, in which smooth cylindrical conductors were used, may not be as effective as a conducting paper applied over the conductor in shielding.

In table I, which represents only the stabilizing effect of carbon black upon the oil, only a 25 per cent reduction in power factor and 11 per cent improvement in resistivity are shown. On the other hand, in table II the effect of the CB model indicates a 2- or 3-to-1 improvement. This may indicate that it is the shielding obtained rather than the effects of carbon black. In table IV, the fact that sample 7 with more carbon-black tape than sample 6 had an equivalent one-hour life no better than that of sample 6 indicates the same conclusion. Likewise, in table IV since sample 3 was but little better than sample 1 the same conclusion is indicated. It is believed that the results in figure 8 may be attributed to conductor shielding.

J. B. Whitehead (Johns Hopkins University, Baltimore, Md.): In the CB cable two well-known protective principles for cable insulation have been combined. First the well-known principle of shielding with its reduction of local stresses and its protection against void formation, and second, the use of finely divided activated carbon for the purification of the oil. The two principles are combined by the simple expedient of incorporating the finely divided carbon black in the structure of the paper during manufacture of the latter. This gives the paper a measurable electrical conductivity so that two or three layers next to conductor or to sheath serve as shielding layers. Furthermore, the incorporation of the carbon black in the structure of the paper prevents its migration into the oil and the possible formation of bridging paths leading to local failures.

The author presents an impressive series of experimental data indicating superiority of the CB cable over those of ordinary type in the solid cable class. The author's thesis is that the advantages shown by the CB cable are largely due to the purifying and absorptive properties of the carbon-black paper. There is some evidence of this in the results of the separate experiments on oil and on impregnated paper, as given in table I. The evidence is not so clear in the results of figure 12 on the aging characteristics of both "standard" shielded cable and the CB cable. Here, it is true, we have a superior performance for the CB cable, but there is no direct evidence that this is due to the properties of the carbon black. The cyclic aging tests are commonly supposed to bring about internal gaseous ionization and to measure the stability of a cable against its effect. Therefore, in the results of figure 12 it would appear that the proper conclusion is that the CB tape constitutes a better shield than does the "standard shield". This conclusion is supported by inspection of the values of table I in which improvements in both oil and impregnated paper by the presence of carbon black, while evident, are not particularly great.

Somewhat similar methods of applying carbon black for shielding purposes have been used both abroad and in this country in connection with rubber cables. The carbon black is incorporated in thin inside and outside layers of the rubber structure before vulcanization. In this country a prominent manufacturer incorporates the carbon black in an asbestos braid covering outside the main wall of rubber insulation. Obviously in these cases the chief advantage gained is protection of the rubber against attack by gaseous ionization both adjacent to the conductor and on the outer wall, that is, the function of the carbon black is solely that of a shielding layer.

W. B. Wiegand and **J. W. Snyder** (respectively Columbian Carbon Company, and Binney & Smith Company, New York, N. Y.): We have for some years been studying the colloidal properties of carbon black. Consequently we were very much interested in the data shown by Mr. Rosch since obviously his findings are the result of adsorption phenomena associated with colloids. It was felt that this institute would be interested in comparing Mr. Rosch's results with some observations which we have made in the course of our studies.

Carbon-black particles are very small. The latest electron microscope pictures, as shown in figure 7 of this discussion, which have just been released to the American Chemical Society indicate a particle size of less than 30 millimicrons. This degree of fineness means that one pound of carbon black has a surface area of some fifteen acres.

It is well known that surface is a controlling factor of adsorption, in fact adsorption data are used to measure total surface. In the case of carbon black another factor has a bearing; namely, a carbon oxygen complex on the surface of the particles. It is found that a normal carbon black with about 2.5 per cent of oxygen selectively adsorbs alkali as shown in figure 8. If however this oxygen be removed it is found as in figure 9 that the alkali adsorption decreases while the acid adsorption increases.

Table II

Grade	pH	DPG Adsorption Index	Per Cent Volatile Matter
High-grade, long-ink carbon (Peerless)	2.6	58.4	12.0
High-color carbons:			
Easy-wetting, high-color (Neo Mk. II)	2.8	97.2	14.0
Highest color (Royal Spectra)	3.1	96.0	12.8
Medium-color (Superba)	3.2	77.7	9.4
High-color (Super Spectra)	4.1	42.5	7.2
Rubber-grade carbons:			
Fine-particle, slow-curing (Mark II)	3.6	16.0	6.1
Standard-curing (Grade A)	4.1	10.1	5.1
Fast-curing	4.6	8.4	4.5
DeGussa CK-3	5.7	5.3	3.4
Nonimpingement carbons:			
P-33	8.2	1.8	0.4
Acetylene	8.2	4.1	0.6
Fumonex (or Gastex)	9.3	2.5	0.7
Deactivated rubber carbons:			
HS-421 (1.5 hr. to 1,500 deg F., 0.5 hr. at 1,500 deg F.)	9.0	5.1	1.1
HS-355 (2.5 hr. to 2,000 deg F., no further heating)	10.0	4.6	0.4
HS-335 (2.5 hr. to 2,000 deg F., 3 hr. at 2,000 deg F.)	10.6	4.8	0.2

Table III. Changes in Relative Electrical Resistance of Liquids Caused by Colloidal Carbon

Dispersion Medium	Relative Resistance (in Ohms) of Dispersion Medium	Medium + Carbon Black A	Medium + Carbon Black B
Water (distilled)	4,700	1,700	3,500
Ethyl alcohol (15%)	21,000	28,000	50,000
Methyl alcohol	670	800	21,000
Acetone	4,700	12,000	24,000
Butyl acetate	1,690,000	1,716,000	3,380,000
"Cellosolve"	21,000	80,000	130,000
"Cellosolve" acetate	1,000,000	260,000	3,500,000
Nitrobenzene	35,000	200,000	240,000
Pyridine	28,000	19,000	14,000
Ethyl alcohol (abs.)	10,000	15,000	15,300
Lacquer thinner	254,000	856,000	1,210,000
Phenyl cyanide	29,000	143,000	100,000
Aniline	300,000	340,000	320,000
"Hexalin"	210,000	250,000	250,000

The accompanying table II points out the availability of a wide range of carbon blacks varying in composition as shown by the volatile matter content, ability to adsorb diphenylguanidine, and pH. It will be seen that carbon blacks are available containing from 0.2 to 14.0 per cent of volatile matter. Incidentally, this volatile matter is really a measure of oxygen content. If the volatile matter driven off the carbon black be analyzed it would be found to be essentially oxides of carbon. Roughly 50 per cent of the volatile matter will be oxygen. The adsorption index varies from 1.8 to 97.2 while the pH covers the range of 2.6 to 10.6. All of these factors are considered in the everyday control of carbon-black production.

Carbon black strongly adsorbs moisture. The degree of moisture pick-up depends on the adsorptive properties of the particular grade of carbon. The highly adsorptive paint blacks pick up appreciable quantities of water as in figure 10. Moisture pick-up is somewhat of a problem to the carbon-black manufacturers who in many instances have to use special packages to eliminate moisture pick-up. That this moisture is very strongly held is evidenced by the fact that drying at 105 degrees centigrade does not remove all of the water. A toluol distillation moisture test will show about 1 per cent more moisture than the ordinary drying test.

Many organic liquids contain enough moisture and electrolytic impurities to lower the electrical resistance of those liquids. By filtering through carbon black the water is adsorbed and consequently the resistance of the liquid increases as shown in table III.

Not only do carbon blacks adsorb moisture, but they also take out electrolytes from solution. This phenomenon presents a prob-

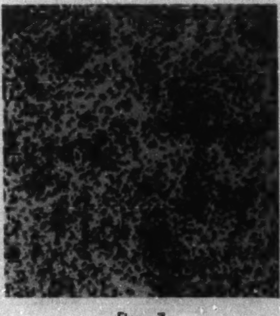


Figure 7

lem in the paint industry where carbon black adsorbs metallic drier and retards drying. In the case of naphtha solutions of cobalt naphthenate it is seen in figure 11 that depending on the particular adsorptive properties of the carbon black various amounts of metallic drier will be removed and made ineffective.

Whether it be water adsorption or electrolyte adsorption carbon blacks have been found as in table IV to improve transformer oils. The addition of 3 per cent carbon black to transformer oil followed by a filtering operation to remove the carbon black has been shown to effect definite improvement in the electrical breakdown of the oil. Improvements range from 40 per cent to 59 per cent.

Table IV. Effect of Colloidal Carbon on Insulating Oils

Carbon	Control Breakdown Volts	Breakdown After Treatment Volts	Per Cent Improvement
A.....3%	20,796	32,475	+56
B.....3%	20,796	29,075	+40
C.....3%	20,796	33,000	+59
D.....3%	20,796	31,575	+52
E.....3%	20,796	30,350	+46

Svedberg has shown that in the case of gold sols, the dispersion of gold shows a higher electrical resistance than the dispersion medium. In other words, the introduction of a conductor has resulted in lowered conductivity. A similar phenomenon is recognized in figure 12 when carbon black is added to a rubber insulating compound. The introduction of a conductor, carbon black, up to 10 per cent by weight on the rubber results in higher resistivity and higher electrical breakdown.

It is hoped that this short review of some of the adsorptive properties of carbon black will throw further light on the results reported by Mr. Rosch.

Thomas F. Peterson (American Steel & Wire Company, Worcester, Mass.): I find it difficult to reconcile certain impressions gained from a study of the paper presented by Mr. Rosch with others gathered from the specification of the Rosch Patent No. 2,102,129, referred to in his bibliography.

Considerable data has been presented to show marked improvements in cable samples and models when carbon-black paper is employed next to conventional electrodes (conductors and sheath). In the absence of more conclusive tests, I would attribute these improvements to three functions of the carbon:

1. Shielding—either full or semiconducting shielding.
2. Provision of fairly inert electrodes at which there is less tendency for soap formation, etc.
3. Adsorptive properties.

Moreover, I would weight the importance of the first two much more than the third.

Unfortunately, the test results presented do not allow of any specific discriminations between the three effects. If, for example, the conductivity of the carbon tapes had been given, and had been varied over a fairly wide range, while other characteristics remained constant, we might have had something indicative of the importance of the shielding function of the carbon. On the other hand, if, with constant conductivity, the adsorptive properties had been varied, the importance of this characteristic would have been brought into full relief.

The results shown in table II and in figures 1, 2, 3, and 10 can best be explained by shielding action of the tape. Results such as those shown in figure 8 can easily be explained by shielding action, the inertness of the electrode in the case of the carbon tape, and the fact that carbon tape replaces the paper tape that usually shows the maximum change in power factor during aging period. Moreover, figure 6 shows a remarkable improvement in aging characteristics for all models, regardless of the type of carbon employed in the CB tape. Certainly, there is very little to justify the argument that the improvement increases with adsorption index, since cables using carbon with adsorption indices less than 20 show up better than those having values of 24, 57 and slightly poorer than those having values of 75 and 95.

The patent referred to above is quite specific in attributing the improvement in the cable to the adsorption of gases, etc., by carbon. The statement is made that certain "blacks" are to be definitely avoided, since they actually injure the cables. Perhaps Mr. Rosch can reconcile these points with the data shown in figure 6, and can indicate the relative importance of shielding effect, inertness of electrodes, and adsorption.

J. C. Balsbaugh (Massachusetts Institute of Technology, Cambridge, Mass.): The results as presented show a very definite improvement in the performance characteristics of impregnated-paper cable insulation when carbon-black paper tapes are used in proximity to the electrodes. It is therefore of interest to attempt to evaluate the reasons for the improvements found.

It would seem logical to attribute these to two effects: (1) the adsorptive effect of the carbon black on removing oxygenated or loss components from the insulation, and (2) the shielding effect. The first one of these would account for the decreases in power factor observed; and the last one, the improvement in breakdown strength. On the basis of past experience it would not be expected that the removal of loss components from the oil would substantially improve the breakdown strength. With respect to the shielding effects of the carbon-black tapes, it might be presumed that the improvement is not only due to smoothing out such irregularities as might exist in the metallic conductor but also in substituting for the metallic conduc-

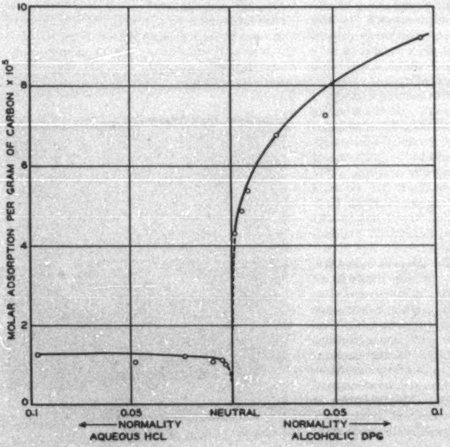
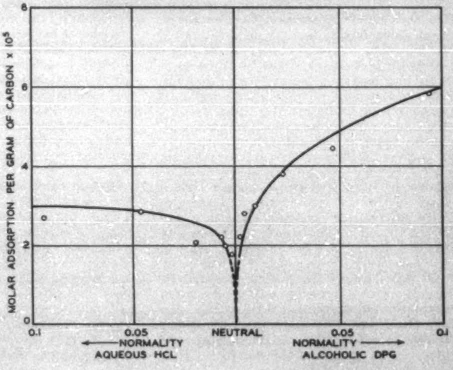


Figure 8 (left). Selective adsorption isotherms—acid (HCL) versus alkali (DPG)—standard rubber carbon

Figure 9. Selective adsorption isotherms—acid (HCL) versus alkali (DPG)—special heat-treated colloidal carbon



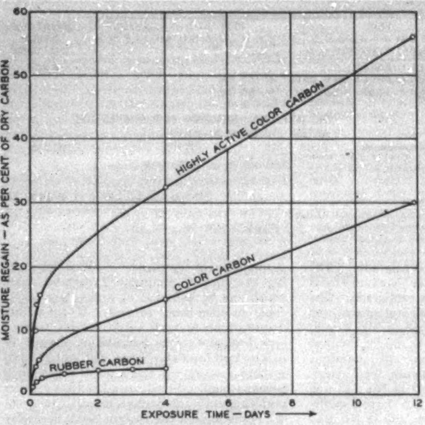


Figure 10. Moisture regain, colloidal carbon over water

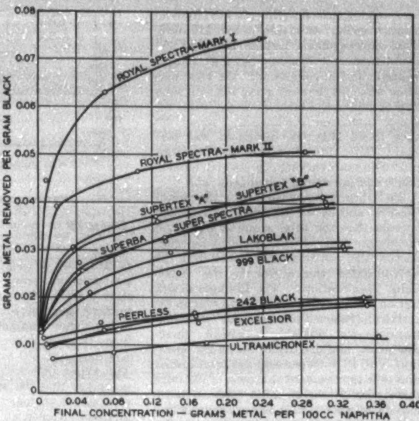


Figure 11. Removal of cobalt from naphthenic cobalt naphtha solution

tor an electrode consisting of a "semi-conductor."

In his figure 7 Mr. Rosch shows the improvement in power factor of unaged cable samples when using carbon-black tapes as given in his table III. Since the carbon-black tapes following impregnation adsorb loss components throughout the insulation wall, principally by diffusion, it is somewhat surprising that this effect would be evident immediately following impregnation. It would be expected that impregnation of the "normal insulation" in a carbon-black paper cable would be at least partly due to oil passing through the carbon-black tapes. Thus in this case the oil would receive the equivalent of an additional refining treatment. This latter effect might account for most of the improved power factor shown immediately following impregnation. This effect might be checked by impregnating a type CB cable and another cable without carbon-black tapes with an oil that had previously received a filtering and contacting with carbon-black tapes and then making power-factor measurements immediately following impregnation of these two samples.

Andrew Gemant (The Detroit Edison Company, Detroit, Mich.): Up to now it has been thought that the carbon-black paper had essentially the role of shielding the conductor, but from the present paper it appears that adsorption processes also have a certain role. I am, however, not quite convinced of the importance of adsorption in this case. The test described in the report, using smooth conductors instead of stranded ones, is not quite decisive in that paper does not adhere very well even to smooth metal, while it adheres well to the carbon-black paper; thus the carbon-black paper probably eliminates ionization even in this case. A possible test to decide the main issue would be the following: Remove the CB paper from a cable after a prolonged test and compare its adsorptive index with an unused CB paper by using some organic acid or dye. If the carbon really has adsorbed as much material, as assumed, then it should be in a fairly saturated state, and its further adsorptive power for similar products must be considerably diminished.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): As stated in this paper, the Commonwealth Edison Company has been actively interested in the Type CB cable. In our aging tests, the Type CB cable with reduced insulation thickness performed better than belted-type cable with standard insulation thickness, as indicated by low and practically constant power factors and ionization factors and by the absence of visible deterioration of the insulation after the test.

The need and, consequently, the magnitude of the cleansing action of carbon tapes will vary with variations in manufacturing processes, in materials, and in kinds of cable. Perhaps, carbon tapes will also become of interest for oil-filled cable.

For solid-type cable the main insulation problem is to suppress ionization. Carbon-black tapes seem to be beneficial in this respect mainly by reducing stress concentration at the stranded conductors, as indicated by a comparison of figures 5 and 6 of the paper.

Apparently, carbon-black paper is beneficial in absorbing some of the products of deterioration, but it probably will not absorb all of the hydrogen which would be liberated due to severe ionization. In figure 12 the increase in power factor after about 345 cycles of the aging test was followed by an increase in minimum pressure which might have been due to such liberation of hydrogen. This increased pressure persisted for the remainder of the test.

We are wondering whether in some cases the carbon-black tapes could be the only shielding over the insulation in three-conductor cable. Usually, however, metallic shielding will be desired also to reduce the probability of having failures involving more than one phase of the three-phase circuit.

As brought out in R. W. Atkinson's article "Load Cycle Aging Tests," in the April 1940 issue of *Electric Light and Power*, the General Cable Corporation also has

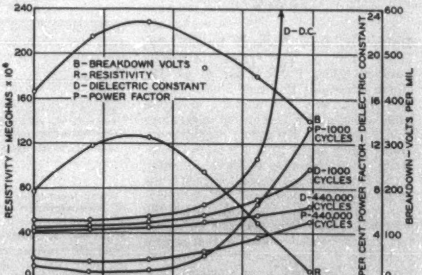


Figure 12. Effect of colloidal carbon in 40 per cent rubber insulating compound

been making some experiments and improvements in cable. According to Mr. Atkinson, these improvements include not only the use of conductor shielding but also changes in paper and in compound. We have also made some accelerated aging tests on such three-conductor, shielded cable with reduced insulation thicknesses. Again we have found that the insulation was perfectly stable throughout the tests, in contrast with some varying degrees of deterioration found in tests on belted-type cable.

With these demonstrations by the manufacturers and by our test data, it definitely appears that the total required insulation thicknesses for shielded-type, three-conductor cable can be safely reduced from the recommended thicknesses in the AEIC Cable Specifications. The Commonwealth Edison Company has placed orders on an experimental basis with each of these two manufacturers for 500,000-circular-mil, three-conductor, shielded cable for 12-kv operation, with 18 or 20 per cent less total insulation than the standard thickness of 172 mils. Economically, the shielded cable with reduced insulation thicknesses are competitive with the standard belted cable. Technically, it is better from the standpoint of dielectric stability, and it appears to have a small advantage in the way of less temperature rise for a given loading. Furthermore, for a given diameter limitation as set by existing ducts, a larger conductor size may be used as a result of these recent developments.

I understand that some of the other manufacturers have begun trials to determine how much the insulation thickness of their three-conductor, shielded cable could be reduced without impairing the stability of the insulation. In some cases they, too, are considering special constructions. We have already made arrangements with some of those manufacturers to obtain samples for tests like those described for the two manufacturers mentioned above.

Those of us who have followed cable matters closely in recent years have come across a large number of special types of cables other than the solid-type cable. Also, it has developed in the minds of some people in this country that solid-type cable has distinct limitations and hazards and that its use should be limited to 33 kv, or 27 kv, or even less. Some thought that all of the appreciable improvements in solid-type cable had been made—that there was nothing further to do. These recent developments have definitely improved the quality and useful range for solid-type cable.

Hubert H. Race (General Electric Co., Schenectady, N. Y.): There are several questions which I should like to ask Mr. Rosch concerning this very interesting paper. First, were the author's miniature cable samples kept completely oxygen-free during the runs at 100 and 85 degrees centigrade, which lasted 10 and 20 days respectively? The reason for asking this question is that I think the specimens described in the author's table II must have contained oxygen or oxidized oil. The evidence for this statement comes from our tests on miniature cables[1,2] sealed in glass so that oxygen is eliminated. Specimens 202, 212, and 243 were 0.065 inch thick using standard cable paper and standard cable oil. They were given daily heat cycles with a maximum temperature of 60 degrees centigrade. The post-mortem power-factor measurements were made at 60 degrees centigrade. The voltage gradient on these specimens was much higher than that used by Mr. Rosch, being roughly 350 volts per mil for the first 150 days and 450 volts per mil thereafter. The major difference between these specimens was that the oil used in No. 202 was preoxidized 96 hours at 115 degrees centigrade in an open air oven, and in No. 243 the paper was shrunk into the strand spaces on the inner electrode,[3] thus reducing the maximum potential gradient in the oil spaces at the electrode surface and preventing ionization at the test voltage.

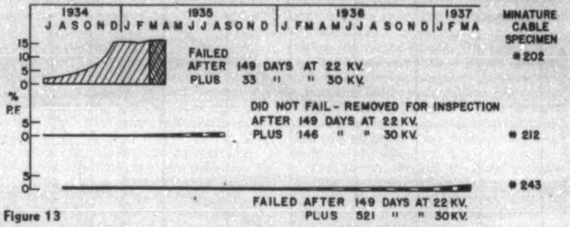


Figure 13

The accompanying figure 13 and table V show the life histories and post-mortem radial-power-factor measurements on these three specimens. From such data we have concluded that if oxidation and ionization are both eliminated, chemical deterioration is very slight even after nearly two years operation at very high gradient.

This last statement leads to my second question; namely, were the miniature cables for which data is given in Mr. Rosch's table II immersed in oil during the high voltage tests or were they allowed to drain so as to accentuate ionization effects? In research work we often impose experimental conditions which are quite different from practice in order to accelerate specific effects. This is perfectly legitimate as long as the conditions are stated so that the causes of the effects will be easily recognized. In the absence of either oxidation or ionization in voids it is difficult for me to account for the high power factors quoted in Mr. Rosch's table II.

My third question is concerned with the permanence of the benefit derived from using the limited amount of carbon black which can be included in a few tapes in a cable in long-time service. If long-time deterioration in such cable results from a slow but continuous leakage of oxygen or oxidized oil from the joints, will not the absorbing property of the carbon black soon become completely saturated so that further purifying action is negligible and the effect of the presence of the carbon black is simply to provide an initial period of as yet unknown duration, in which oxidation products will not cause an increase in the power factor of the cable? Furthermore, would it not be better to keep the oxidation products out in the first place by careful manufacturing, installation, and operating procedure?

REFERENCES

1. TESTS ON OIL-IMPREGNATED PAPER—II—EFFECTS OF GAS PRESSURE, H. H. Race. ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), July 1937, pages 845–9.

2. TESTS ON OIL-IMPREGNATED PAPER—IV—MECHANISM OF BREAKDOWN, H. H. Race. AIEE TRANSACTIONS, 1940, pages 721–9.

3. U. S. Patent 2,195,998.

E. R. Thomas (Consolidated Edison Company of N. Y., Inc., New York, N. Y.): Mr. Rosch is to be complimented on preparing an improvement in methods of constructing high-voltage cable which would appear to make possible the operation of oil-impregnated-paper cable at temperatures somewhat higher than now recognized and yet give reasonable assurance for obtaining a relatively long period of operation. While we do not know at this time whether all of the advantages which appear to be possessed by

Table V. Data From Examination of Specimens 202, 212, and 243

		Standard Construction	Oxidized Oil	Paper Pressed Into Strand Spaces
Specimen		212	202	243
Days life 350 volts/mil		149+	149+	149+
Days life 450 volts/mil		146+	33	521
	Layer	Power Factor	Power Factor	Power Factor
Radial power factor measurements on paper plus oil	1 (Cu)	0.013	0.168	0.0139
	2	0.012		0.0075
	3	0.020	0.146	0.0052
	4	0.028	0.101	0.0064
	7	0.043	0.088	0.0061
	8	0.053		0.0065
	9	0.018	0.087	0.0184
	10 (Pb)	0.020	0.138	0.0185
Measurements on oil centrifuged from paper	1 and 2	0.006	0.26	≤0.0001
	5 and 6	0.004	0.274	≤0.0001
	9 and 10	0.008	0.436	≤0.0001

this improved type of cable as determined from accelerated laboratory tests may necessarily obtain in actual operation, the Consolidated Edison Company of New York, Inc., has decided to install some of this cable and obtain experience in the field with it.

In figure 12 of the paper, the data indicates that standard shielded cable continued to show loading pressure changes from below atmospheric pressure to pressures above atmospheric on each load cycle up to the end of the tests, while the type CB cable changed from this type of characteristic to one essentially of changes in pressure above atmospheric pressure even on the cooling cycle after the cable had been under test for a considerable period of time. Is this a typical characteristic on type CB cable and if so what is the author's explanation of the phenomenon?

J. F. Calvert (Northwestern Technological Institute, Evanston, Ill.): I would like to ask the author if he would make just two or three further comments concerning his very comprehensive investigation:

First, would he show by a numerical calculation the length of time that the carbon black can be expected to be effective as an anti-oxidant; and would he also state what the "normal" operating conditions are assumed to be for this calculation with regard to initial oxygen present and that oxygen which may get into the cable during service?

Second, is there data available to show the relative amounts of oxidation occurring at the electrodes and toward the middle of the insulation for the temperature, frequency, and voltage gradients employed in the tests which he reports here. In this connection, would there be any application for the methods described in the paper "A Method for Detecting the Ionization Point on Electrical Apparatus" which Mr. G. E. Quinn presented at the 1940 AIEE summer convention in Swampscott, Mass.?

E. A. Hauser (Massachusetts Institute of Technology, Cambridge, Mass.): The improvements in overcoming insulation instability as just reported by Mr. Rosch are bound to create considerable interest among electrical engineers. For them the actual data obtained in comparative tests will be most significant. From the point of view of the colloid chemist or physicist it is the principle of Mr. Rosch's work which is of primary importance. His contribution is to the best of my knowledge, the first one which applied typical colloidal phenomena to high-voltage insulation, thereby opening an entirely new field for theoretical and applied colloid chemistry alike.

Mr. Rosch attributes the beneficial effect of the carbon black to a purifying effect and the retarding influence on the formation of deteriorating bodies. Both effects must unquestionably be the result of adsorption. It is a well-known fact that so-called adsorbent carbons are used in the clarification of oils, sugar solutions, etc. The latter effect is presumably due to adsorption of oxygen present or formed in the oil, thus preventing excessive depolymerization.

From a colloid chemical point of view an answer to the following questions would be of extreme interest. They should prove of equal value to the electrical engineer interested in furthering this remarkable development.

1. Ascertain on which of the two effects the influence of the carbon actually depends.

2. Ascertain if the phenomenon is specific to adsorbent carbon or if other known adsorbents, like silica—or alumina gel, activated clays, etc., could be substituted.

3. Mr. Rosch speaks only of activated carbon or carbon black of the adsorbent type, without giving more details as to source and properties. From test 5 of the manuscript I infer that Mr. Rosch refers to "channel blacks" only. (Obtained by incomplete combustion of natural gas.) This assumption is further substantiated by a review of the literature cited by Mr. Rosch. If experience with other truly activated carbons, as for example, "Norit" have not yet been carried out, this ought to be done, because the channel blacks are far less active than activated carbon made from vegetable charcoal.

E. F. Nuezel (Cincinnati Gas & Electric Company, Cincinnati, Ohio): As outlined in this paper based on commercial orders and laboratory tests, the comparative tests indicated that the use of carbon black has a decided inhibiting effect on cable dielectric deterioration. It is interesting to note in these tests the comparison between the insulation stability of carbon black versus standard belted when the carbon-black is applied solely on the conductor and no carbon-black paper is present on the outer conductor layer to provide shielding effect. While the report does not give sufficient data to determine the effect of the inner carbon-black tape alone as against both the inner and the outer applications or carbon black, it appears evident from tables III and IV that the application of the inner tape alone effects a considerable improvement in the dielectric. It is believed that if appreciable practical results were obtained it would be a very worth-while move to utilize the carbon black on belted cable as there are still many places where it is advantageous to use this type of cable.

The use of carbon black should no doubt result in the use of thinner walls of insulation as based upon tests made by the Commonwealth Edison Company on 13,000-volt cable insulated with 130 mils of total paper instead of the standard 172 mils. This reduction in wall thickness will result in smaller cable diameters which in turn will result in lower sheath duties because of the reduction in oil volume and also reduced hoop stresses in the lead sheath. This is important because the general experience indicates that progress in lead sheath extrusion has not kept pace with the improvement in the dielectric.

It would also appear that the purifying effects of carbon black as used for solid-type cable could be applied with equally good results to oil-filled cable. There is little doubt that the use of carbon-black paper will increase the useful sphere of solid-type impregnated-paper insulated cable. Its performance in commercial installations over a period of time will dictate the upper voltage limit.

F. W. Nimmer (Ohio Edison Company, Akron, Ohio): The development of the type CB impregnated cable is especially unique because of its simplicity. We have all known for a number of years that the insertion of two plates in an insulated medium will uniformly distribute the potential gradient between the two plates. We had been of the opinion that we had accomplished this result in the type H cable, however, it seems evident from the test data presented by Mr. Rosch that substantial improvements can be obtained with the addition of several layers of carbon-black paper. The very marked improvement in power factor and in the cyclic aging characteristics, plus the improved operation of the various layers of paper tape, as indicated in figure 4, seem to point to the definite conclusion that this new type of cable should be substantially superior to previous types developed.

Many times an improvement results in complications in installation, preparation of joints, etc. Fortunately, this development of CB cable does not incur these handicaps, and therefore provides additional usefulness and keeps to a minimum the disadvantages.

E. W. Greenfield (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.).: This discussion deals only with the adsorption or purification phase in the mechanism of carbon-black protection to impregnated-paper cable. Elimination of strand and other stress concentrations by means of the shielding action of the carbon black has been amply demonstrated in the formal paper. It may be of interest, however, to note that the action of strand-stress concentrations on impregnated paper has been most carefully studied with relation to field configuration. Also the range of carbon-black paper electrical resistance required to effectively eliminate such concentrations has been determined and probably will be published at some future time.

A. Behavior of Carbon Black as an Adsorptive Agent

1. *Definition of Adsorption.* In simplest terms an adsorptive medium is one which operates to preferentially concentrate and retain upon its integrated structure specific gases or liquids with which it contacts. This essentially means a type of loose fixation (from the chemical standpoint only) of one substance on the surface of another. Highly adsorptive substances are those which possess these fixation properties to the greatest degree as a result of their large free surface energies and the polar nature of the surfaces.

2. *Identification of Carbon Black as a True Colloid.* Carbon black, properly termed colloidal carbon, is the most highly adsorbing form of carbon. Produced by the careful impingment of a natural-gas flame against a cool surface, the colloidal carbon is nonoily, possesses jet blackness, and is characterized by extremely small particle size.

X-ray diffraction patterns indicate that the five-atom molecular building unit of carbon is completely intact in colloidal carbon, but the axes of the units are scattered heterogeneously so that there are no regular planes of densely packed atoms as, for example, exists in graphite. This anisotropic form of structure is indicative of the ultimate in dispersion with regard to the colloidal state.

Particle size of colloidal carbon is found to vary over wide limits in the range from less than 25 millimicrons to over 200. Con-

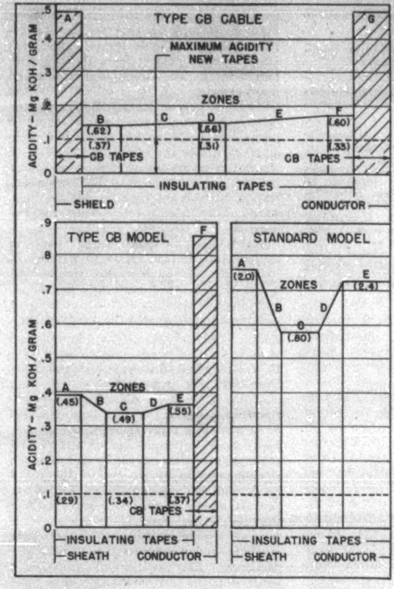


Figure 14. Acidity distribution of cables and cable models before and after aging

Bracketed figures are per-cent power factor taken from radial-power-factor tests

sidering these exceedingly small values of particle diameter and their degree of dispersion, it is evident that we are dealing with a true colloidal dispersion—detectable only by means of the ultramicroscope.

3. *Amount and Selection of Adsorption.* The integrated surface of such a disperse colloidal material as carbon black is relatively enormous. One cubic inch of solid carbon black has a surface area of six square inches. If the cube is subdivided so as to produce carbon particles having each a diameter of 1 micron (0.00004 inches), the total surface developed becomes 160,000 square inches, and if further subdivision is made to particle diameters of 100 millimicrons, the total integrated surface jumps to almost 2,000,000 square inches.

Other things being equal, the adsorbing power of a colloidal adsorptive depends upon its total surface free energy which is simply the same as the total surface developed by all the minute dispersed surfaces making up the material. For colloidal carbon the total free surface available for adsorption depends upon particle size, number of particles and their degree of dispersion. As we have seen above, there is thus ample explanation of the immense adsorption activity of colloidal carbons.

The *degree* of *selectivity* of adsorption depends upon the polar nature of the surfaces involved and the interfacial tensions between sorptive and sorbed materials. Colloidal carbon normally possesses a surface oxygen complex (C_xO_y) which influences this selectivity of adsorption to the greatest extent. Acidic or alkaline substances can be preferentially adsorbed depending upon treatment of the oxygen complex existing on the particle surfaces. High oxygen complex surfaces on the black are accompanied by low pH values and considerable preference for adsorption of basic material. Deactivated surfaces (caused by heating in absence of air) characterize the black with pH values over 7, low volatile content and definite preference for the sorption of acidic substances.

B. Fixation of Carbon Black in Cable Paper

1. *Incorporation of Colloidal Carbon Into Paper.* Colloidal carbon is incorporated into paper during the first operations in the manufacture of the paper. To the viscous slightly alkaline slurry of water and pulp in the beaters is added the proper amount of colloidal carbon. Continuous beating here and subsequent grinding action through the Jordan machines provide exceedingly efficient dispersion of the black and is actually in accordance with the most advanced ideas known for the preparation of complete dispersions.

2. *Stabilization of Colloidal Carbon in Paper.* Stabilization of the colloidal black within the structure of the paper is automatically effected during the course of the pressing, drying, and calendering operations incident to the formation of paper from pulp. Microscopic examinations show the colloidal carbon as an apparent continuous phase dispersed between, upon, and within the cellulose fiber structure and strongly bonded thereto. Properly prepared carbon black paper is absolutely nonmarking and can be boiled for 24 hours or more in water or in light oil without showing the slightest trace of carbon particle loss.

3. *Range of Control of Finished Carbon Black Paper.* It is thus noted that the two most important conditions for the greatest realization of the enormous inherent adsorbing power of the colloidal carbon, viz., complete dispersion and stabilization of dispersion, are fulfilled by carbon black paper.

By destroying the cellulose and properly filtering the resultant gel, one readily determines carbon content or "retention" in the carbon black paper. Advanced technique of manufacture indicates less than 30 per cent loss in percentage of carbon black to wood pulp from beater to finished paper. Considerable control in the important properties of the finished carbon black paper, i.e., density, air resistance, strength, electrical resistance, adsorbing power, etc., may be exercised by choice and amount of colloidal carbon and variation in the paper-making constants.

To the present, many types of colloidal carbon have been studied as incorporated into various types of pulps and manufactured to various paper specifications. Evaluation of these carbon black papers for paper cable application have been based on (1) insulating oil purification tests, (2) cable model accelerated life tests, (3) AEIC cold bend and high voltage time tests on full size cable, and (4) long-time cyclic aging tests on full size cable. *In most cases selections based upon the electrical tests have justified selections based upon colloidal theory.*

4. *Adsorption Properties of Carbon-Black Paper as Related to Purification of Insulating Oil.* Although having low pH values when measured in hot aqueous sludge, the high quality, low particle size colloidal blacks undergo surface changes in the process of incorporation into paper and subsequently into cable. In the beaters admixed with wood pulp and controlled water, the pH of the slurry is generally somewhat over 7 (slightly alkaline) and the finished carbon-black paper is essentially neutral. After application to cable the carbon-black paper is heated in the almost complete absence of air probably causing further deactivation of the surface oxygen complex on the carbon particles so that the final carbon-black paper is generally in a condition to preferentially adsorb acidic material.

Many tests in addition to those reported in the formal paper have demonstrated the substantial beneficial effects resulting from the adsorptive properties of carbon-black paper. In the type CB cable design, deterioration products generated within the cable oil due to simultaneous actions of heat and electric stress, further aggravated by paper catalysis and possible presence of oxygen, are preferentially adsorbed upon the surfaces of the carbon particles on and within the paper structure. Hence the organic acids, esters, organic metals and other aqueous electrolytic phases become distributed through and held within the carbon-black paper where they have no influence upon the cable's over-all dielectric behavior since they are effectively shielded from the electrical field by the high-conductivity particles to which they are adsorbed.

Two typical examples of the way in which carbon-black paper adsorbs acidic material during aging of impregnated paper cable at high temperature and stress are given in the accompanying figure 14. The data in the upper portion of figure 14 are for a lead-covered, three-conductor, 350,000-circular-mil, type CB cable having 130 mils total wall of impregnated-paper insulation, carbon-black tapes being present at both sheath and conductor. This cable was cyclically aged at a utility laboratory and analysis of the insulation made prior to and after aging. Data given in the lower portion of figure 14 pertain to a cable model electrically aged under advanced conditions of stress and temperature. Due to the comparatively small wall of insulation, 65 mils, this cable model contains carbon black only at the conductor. The data in each case show the radial distribution of acidity before and after aging for selected zones through the insulation wall. The power factor values associated with these zones are given by the bracketed figures.

The data on the cyclically aged cable (upper, figure 14) shows the carbon black tapes to contain more than three times the acidity of the insulating tapes, sheath and conductor carbon-black tapes being of equal acidity. The distribution of acidity through the insulating wall appears to be constant at a moderate value. The dashed horizontal line represents the *limit* of acidity in freshly impregnated carbon-black and wood-pulp cable papers (generally, values are about one-half this maximum). Hence we note that the cyclic aging tests brought about an increase in acidity of the carbon-black tapes by about ten times and only three times for the insulating tapes. It is concluded that the carbon black accounts for the difference by acting as a continuous adsorbing medium for the acidic deterioration by-products generated within the cable during aging. Radial power factor values are in agreement.

The lower portion of figure 14 again demonstrates the protective action of carbon-black paper. Here the acidity data is more complete. After aging the acidity of the insulation wall is found to have increased between three and four times its initial value, whereas the carbon black is found to have nine times its original acidity. On the other hand, an identical model unprotected by carbon black has an acidity increase *throughout the insulation wall* of between seven and eight times after the same aging. Radial-power-factor values also correlate excellently.

C. Design Considerations of the Type CB Cable With Reference to Oil Purification

The two important factors involved in design of the type CB protected cable are shielding of stress concentrations and continuous purification of the saturant. These considerations affect selection of the colloidal carbon, the desired properties and amount of carbon-black paper and the positioning of these papers within the cable structure. A satisfactory carbon-black paper must have properties which accomplish both its primary objects.

From the standpoint of oil purification, we are interested in the adsorption activity of a given carbon-black paper and the total carbon surface available for adsorption in any given cable. Carbon-black papers may be produced which have a maximum selective adsorbing activity for insulating oil deterioration products, i.e., organic acids, esters, and water. For a given carbon-black paper, one readily computes the adsorbing carbon surface area per inch of tape from knowledge of: (*a*) carbon particle size, (*b*) total number of carbon particles per unit volume of paper, and (c) dispersion of carbon particles.

As an example of the tremendous integrated surface of carbon incorporated into standard carbon-black paper, we note that using a colloidal carbon of 40 millimicrons particle size, each one inch length of $^3/_4$ inch by 0.005 inch tape having a gross area of only 1.5 square inches contains approximately 650 square inches of carbon surface. The amount of carbon black incorporated within a cable is subject to design and control and will naturally vary with the size and type of construction.

Philip Sporn (American Gas and Electric Service Corporation, New York, N. Y.): One of the compensations of working in a field that is as rocky as is the engineering field by and large, is the turning up on too-rare occasions of a paper on, or the coming across of a description of a new idea, a new view of an old phenomenon, or the logical working out of old and known principles to new uses. The paper of Mr. Rosch's illustrates perfectly the last of these occurrences.

The use of carbon in oil purification has been known for some time; so have the reasons behind insulation instability. It remained only for someone to put these ideas together in a workable and usable form and to bring about the improved cable described so well by the author. These improvements are logical and are well brought out by such data as figures 3 and 5, and most strikingly by figures 10 and 11 of Mr. Rosch's paper. It is to be hoped that this work will be continued and the original idea developed to the fullest. The result ought to be not only more reliable cable but less expensive cable. In both of these directions some progress has been badly needed for a long time.

J. A. Peterson (The Commonwealth & Southern Corporation, Jackson, Mich.): Anaconda Type CB cable has been tested and found superior to the standard type especially for service voltages over 10,000 volts. Tests by one of the large operating companies have shown that this cable has a much higher impulse voltage strength than the standard constructed cable. Other tests have shown type CB cable has better power factor and insulation resistance characteristics. In view of these facts, it appears that it might be possible to reduce appreciably the present insulation thicknesses as standardized by the AEIC if this new type of construction is generally adopted by the manufacturers. In fact, one of the metropolitan companies is installing, presumably for trial, type CB cable for 28,000 volts grounded neutral with insulation thicknesses much less than the AEIC standards.

Would it not be desirable to also consider using type CB construction on low-voltage belted cables, that is, using carbon-black tapes adjacent to the conductors and expect that the impulse-voltage strength would also be materially increased and that insulation thicknesses in this range could also be reduced?

If this type of construction is desirable on paper cables, it should also be desirable on varnished cambric insulated cables. It might be possible to reduce the present IPCEA standards for insulation thicknesses of varnished cambric cable.

This type of construction should eventually reduce insulation thicknesses with a corresponding decrease in cost.

The development of Type CB cable is undoubtedly a big step forward in cable design. However, in our particular position, it appears that the most essential fault in IPLC cable has been susceptibility of the lead sheath to corrosion. Our corrosion troubles have not been due to loading since corrosion has been just as severe on lead covered control cables as on power cables. We would like to see corrosion tests made on various types of lead sheaths, such as so-called common lead, both pure and copper bearing, also "Asarco" lead, calcium lead, etc., to see if there is any appreciable difference in corrosion resistance between these various types of lead sheaths.

S. J. Rosch: Due to the similarity of a number of points raised by several contributors, they have been grouped according to subject and discusser, as follows:

1. Conductor shielding: R. W. Atkinson, W. A. Del Mar, G. B. Shanklin, L. G. Smith, J. B. Whitehead.
2. Adsorptive and purifying effect of carbon-black paper: T. F. Peterson, A. Gemant, L. G. Smith, W. B. Wiegand, J. W. Snyder.
3. Protection from metallic soap formation: T. F. Peterson, G. B. Shanklin.
4. Electrode contact and characteristics: W. A. Del Mar, J. C. Balsbaugh.
5. Test conditions and limitations: H. Halperin, H. H. Race, G. B. Shanklin, E. R. Thomas.
6. The type CB cable design: J. F. Calvert, E. A. Hauser, T. F. Peterson, H. H. Race.
7. Possible economic advantages of type CB cable: H. Halperin, E. F. Nuezel, F. W. Nimmer, J. A. Peterson, P. Sporn, E. R. Thomas.

1. Conductor Shielding

Judging from the several discussions, the concept of conductor shielding seems to be somewhat confused. It is variously applied to designate:

(*a*). The elimination of field concentrations due to electrode geometry

(*b*). The elimination of destructive discharges from metallic electrodes

(*c*). The short circuiting of possible void spaces that exist or develop between conductor and insulation

These conceptions are not necessarily compatible, and in addition require different constructions and properties of materials to be realized to the fullest extent. It so happens that pervious carbon-black tapes, when considered from the restricted sense of shielding only, have inherent properties which permit an optimum compromise for all the conceptions listed above. The type CB construction thus automatically contains the benefits of conductor shielding in its broadest sense, and *in addition*, operates as a continuous oil purifying means.

Test 4 was one of several conducted to separate the effects of conductor shielding from oil impurity adsorption in the carbon-black tapes. Since these were hand-wrapped models and unbent in any way, gross contact differences between the smooth polished cylinder and the insulating paper were certainly as good as between the carbon-black papers and insulating papers. Secondary contact differences do exist in favor of the CB construction. However, since neither model showed the slightest trace of markings or damage to the insulation, we conclude that this was a small factor in the test. In addition, acidity measurements not included in the paper, confirm the adsorption picture by disclosing the degree of activity of the carbon-black papers.

Question has been raised concerning the high-voltage time tests described in test 6. It is stated that the results of this test did not correlate with the quantity of carbon-black paper in the various cable constructions, and hence conductor shielding effects predominated in producing the excellent results. In these short-time tests at room temperature, oxidation phenomena are limited and it is true that ionization probably plays an important role. Hence, positioning of the carbon-black tapes had a bearing from the standpoint of conductor shielding in its most general sense as outlined above. Adsorption phenomena contributed to the excellent results by adsorption of gases and water vapor generated during the ionization. This can be confirmed by acidity tests of the individual tapes removed after test.

2. Adsorptive and Purifying Effect of Carbon-Black Paper

Type CB cable has been generally constructed with carbon-black tapes adjacent to both electrodes. No such tapes were used in isolated form within the effective insulating wall. The question as to whether cable oil can sufficiently contact the carbon-black layers in order to permit adequate adsorption of impurities and generated deterioration products seems to be answered by radial power factor and acidity distribution determinations after aging on both models and cables. These indicate effective control of the acidic material throughout the insulation wall. There is every reason to believe that under long-time conditions oil diffusion within the cable structure follows the usual laws of liquid diffusion in porous media.

Many of our model aging and cable tests have been supplemented by chemical analysis of the insulation both before and after the electrical tests. The evidence indicates that carbon black adsorbs acidic material avidly and that the extent of this adsorption is potentially enormous. In Greenfield's discussion, are given two examples of the results of radial power factor and acidity measurements on aged and unaged cable, which bring out the points discussed.

Contributory evidence on the adsorption properties of carbon black has been presented by several discussers who are primarily workers in the fields of colloidal carbon development and colloid chemistry. Much of the future development of the type CB cable will be guided by theory and practice acquired from these fields.

Most of the model tests described in the paper were carried out under advanced thermal and air-exposed conditions so as to accelerate oxidation products. Effectiveness of carbon black as an adsorptive agent is best demonstrated by tests producing those materials which it is desired to adsorb. We have shown conclusively the carbon-black tapes do adsorb the acidic products. Furthermore, the result of such adsorption is that the dielectric losses and free acidity in the insulation are maintained at a minimum. Material removed and held in the carbon-black tape structure no longer contributes to dielectric loss because it is not in the electric field.

Reference to figure 6 shows that models E5 and E2, having adsorption indices of 75 and 95, had *lower* power factor after aging than initially, which is the opposite of that obtained with the standard models and carbon-black models having lower adsorption indices. Surely this indicates that improvement increases with adsorption index. It may also be pointed out that similar tests on standard models reported by Whitehead have never shown an improvement in power factor after similar aging.

3. Protection from Metallic Soap Formation

It has been suggested that the carbon-black tapes confine metallic soaps at the region of contact between the copper and the carbon-black tapes. This seems to be reasonable since reaction of any formed hydrocarbon acids with the copper conductor takes place at the high-voltage region. Here the carbon-black tapes are in position to immediately adsorb and retain such products in a region shielded from the electric field.

It has been emphasized by one discusser that oxidation products such as organic acids, esters, soaps, water, etc. are not a problem in cable operation today and that gaseous ionization is the main problem. It is felt that this statement is not entirely substantiated by experience with both oil-filled and solid-type cable. It is also felt that the complete divorcement of oxidation effects from ionization destructiveness is not possible, the two being dependent and interrelated when solid losses reach a certain point. Excessive solid ionization is as destructive as gaseous ionization and although less frequent, becomes more and more a factor as better cables are produced and stresses and temperatures elevated. Adsorption of electrolytes produced in the oil as a result of oxidation reduces solid ionization. This function of carbon-black protection is particularly applicable to oil-filled cable design.

4. Electrode Contact and Characteristics

Under consideration of conductor shielding, it has been mentioned that conductor-to-insulation contact may have profound influence on cable properties entirely outside the question of geometry and maximum stress. Several points have been raised concerning the benefits accruing from nonmetallic ("inert") electrodes as well as the more perfect and intimate contact which carbon-black paper makes against insulating paper as compared with metal shielding tapes or smooth cylinders.

It is quite true that on theoretical grounds, a nonmetallic electrode possessing an appreciable electrical resistance can damp high frequency discharges much in the same way that any properly designed series resistor can damp the high frequency oscillating components of a circuit in ionization or other unstable equilibrium. In the case of the type CB cable, this series resistance is distributed but the effect should be the same.

The beneficial effects described above are inherently obtained with the type CB cable so that these are advantages in addition to those described in the paper.

5. Test Conditions and Limitations

The cable models were kept continuously in oil and free from contact with air or other gases. The oil in which these models were submerged for temperature control was exposed to air and hence deterioration greatly accelerated. The tests conducted on the models and cables show the differences that exist between standard and type CB cable under controlled conditions.

In test 8, the conductors were of the compact construction, both cables being manufactured under identical conditions. They were simultaneously dried and impregnated in the same tank and leaded in the same press. All the refinements of the most modern impregnating system were used to remove air and moisture. The treating procedure was controlled by routine electrical measurements on the cables. Every effort was made to remove all traces of impurities, and except for design, both cables were comparable as indicated by the initial characteristics shown in figure 9. Therefore, it is concluded that the changes in properties during the cyclic aging test were a result of the electrical stress and extremely high temperatures.

In figure 12, it is seen that the standard cable continued to show pressure changes from below to above atmospheric pressures on each load cycle, whereas the type CB cable changed from this type of characteristic to one of changes in pressure above atmospheric. With the data available at present, it is impossible to state whether or not this is an inherent characteristic of type CB cable.

Since all tests are comparative with standard constructions, it is obvious that the addition of other variables such as longitudinal migration would tend only to complicate the problem and the results.

6. Type CB Cable Design

The carbon-black paper we use *does not* contain rosin size or sodium abietate to hold the colloidal carbon in suspension. This paper, without the use of a binder was developed for use in type CB cable in order to avoid the corrosive effects such materials would have on the copper conductor. The adsorptive effect of carbon black is not peculiar to that material alone. Silica or alumina, activated clays, etc. could be used except that due to the dielectric nature of these materials, the adsorbed products would be acted upon by the electric field and thereby overcome the advantages gained by using a material which is both conducting and adsorbent. We have no data on the distribution of oxygen from the electrodes to the center of the insulation; however, a review of Greenfield's discussion will show the distribution of acidity through the insulating wall, which is a comparative measure of the distribution of oxygen. The amount of carbon black incorporated within a cable is subject to design and control and will naturally vary with the size and type of construction.

7. Possible Economic Advantages of Type CB Cable

The answer to higher operating temperatures and reduced insulation thicknesses with type CB cable will eventually be obtained by the various utilities investigating the economic advantages in a very practical way on their cable systems. There is little doubt that these investigations will show type CB cable to be more reliable and less expensive, as well as simple to install and splice.

The author takes this opportunity to thank all those who have contributed to this discussion for their encouragement, criticisms and helpful suggestions.

Modern Rail Transport

Discussion and author's closure of paper 40-122 by A. M. Wright, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940; published as an article in ELECTRICAL ENGINEERING, October 1940, pages 400–06; and reprinted in AIEE TRANSACTIONS, 1940, pages 1306–12.

D. R. MacLeod (General Electric Company, Erie, Pa.): The high cost of wayside equipment is one reason why the potential economies of electric operation cannot be realized on some railroads. In this country we have established high standards for wayside construction since we are always striving toward the ideal of 100 per cent reliable operation.

The figure given by Mr. Wright in his paper may prove to be high in some cases, but it emphasizes the great need for engineering study to reduce this part of the capital outlay. Very often we find that the traffic requirements are such that only $^1/_{10}$ locomotive is required per track mile. This means that the cost of the wayside equipment is usually the major part of the initial outlay for electrification.

Various attempts have been made to reduce the cost of the wayside equipment by electrical design methods. Among these attempts are the higher-voltage trolleys mentioned by Mr. Wright. All attempts along this line have been disappointing, because the cost of the wayside equipment is principally due to the cost of installation on a busy railroad. Work trains can work only for a fraction of the time that they are on the job, and construction must be carried on without seriously impeding traffic. Great improvements have been made in the methods of construction as a result of the installations which have been made during the last two decades. A great deal of the experience has been lost, however, because none of the projects have been continuous and it is not always possible to reassemble the skilled workers once they have been dispersed. Standardized construction will remove some of the engineering uncertainties so that less margin of safety must be taken not only in design, but also in contracting for the job. As the volume of electrification business increases it will become more and more possible to keep experienced gangs at work continuously so that the cost will be further reduced by the improved efficiency of the construction forces.

The added cost of the high-voltage transmission which parallels the low-voltage trolley and supplies power to it is a small part of the total cost of electrification. The use of a high-voltage transmission line enables large blocks of power to be transmitted at relatively low power factor with high efficiency and low voltage drop. Too high a voltage increases the cost of the substations more than necessary, while too low a voltage limits the capacity of the system and ultimately hampers the schedules that can be made by the trains. It is surprising what a small difference there is in the cost of the single-phase two-wire transmission lines having different transmission voltages, but with the same contact line construction. It should, therefore, be possible to standardize on one type of construction for all electrifications using the same system of distributing power.

E. W. Brandenstein (General Electric Company, Erie, Pa.): Mr. Wright's paper is a very comprehensive analysis of modern rail-transportation problems. There is no doubt but that long distance mass transportation can best be taken care of on rails. It is also evident that electric locomotives can handle long-distance high-speed mass transportation much more economically than any other type of motive power.

We have recently made improvements in electric locomotive designs which permit a horsepower ratio of 80 pounds of locomotive weight per continuous horsepower. In other words, it is practical to build a 2-C+C-2 passenger or freight locomotive weighing 480,000 pounds and rating 6,000 continuous horsepower. This not only cuts the weight horsepower ratio but also cuts the cost per horsepower. This locomotive will handle either freight or passenger trains with gearing for 90 miles per hour top speed. This universal engine permits transferring the motive power from freight to passenger service or from passenger to freight service depending on which type of traffic is experiencing a peak.

Mr. Wright refers to the advisability of going to trolley voltages higher than 12,000 to cut down the cost of substations. Our past record over a period of 10 years with 25-cycle substation transformers is almost perfect. It has been found unnecessary to duplicate transformers in substations. Single transformers located about 15 miles apart are adequate for most installations. This drastically cuts the cost of the steel work and reduces installation labor as well as eliminating transformers. On a two-track railroad the 12,000-volt substation costs total approximately from 10 to 20 per cent of the total roadside equipment, i.e., everything excepting locomotives. Our recent studies indicate that a higher voltage trolley ordinarily adds as much to the cost of the locomotives as it saves in substations. There is little object, therefore, in changing the established standard.

Mr. Wright's analysis of modern railway problems is very stimulating and indicates that further study should be made by railroad operating men to determine whether or not there is any motive power, other than electric, which will meet the demands of modern high-speed mass transportation. We, in the electrical industry, will be glad to meet that challenge.

Sidney Withington (New York, New Haven & Hartford Railroad Company, New Haven, Conn.): Mr. Wright's valuable paper discusses a number of highly controversial points of interest in both the economic and technical fields. There is no question that with the mechanical developments which have been brought about in the past few decades, the entire system of transportation is subject to drastic revision and much of the capital now invested in railroad facilities must be retired and written off the books, tragic though this may be; and much new capital must be made available to take care of developments in the interest of safety and efficiency. That the problem is not new is indicated by an editorial in the *American Railroad Journal* of June 30, 1832, more than 100 years ago, which concluded with the following comment: "It will be a little singular if *steam carriages on common roads* should treat the railroads with as little ceremony as the railroads are treating the canals."

Opportunity is taken to point out a few minor items which Mr. Wright has mentioned upon which some exception may be taken. It is agreed without qualification that Mr. Wright's contention is entirely logical that there are undisputed points of superiority of electric motive power as compared with steam, and that electric locomotives may be found to be the solution of certain problems in railroad operation.

In connection with the loss of freight traffic to the highways, there has been much discussion as to the advantage of revision of railroad rate structures, and it is quite possible that a rational revision may tend to bring certain kinds of traffic back to the railroads, though without question certain kinds of traffic are permanently lost to the railroads.

With regard to railroad consolidation, which Mr. Wright suggests as a solution of the railroad problem, it is probable that large scale abandonment through consolidation cannot in general be brought about satisfactorily, since each existing line serves its own community, and the abandonment of its tracks would leave the community in many cases without service. For instance, it is not conceivable that the Pennsylvania Railroad between New York and Chicago could be abandoned and its traffic transferred to the New York Central, because the latter does not cover the intermediate territory served by the Pennsylvania.

Mr. Wright mentions a figure of 60,000 gross ton-miles per train-hour as a measure of railroad operating effectiveness. It may be pointed out in this connection that in 1939 only four railroads in this country achieved a record of more than 50,000 gross-ton-miles per train-hour, (the average being 32,808) and that these four railroads are all coal or ore roads with uniform heavy specialized freight tonnage. Mr. Wright also says that trains are "easily capable of removing freight from a terminal at the rate of 10,000 net tons per hour. At 30 tons per car, a figure of 10,000 net tons means three trains of 111 cars each must leave each hour. This is a possibility, but not an easy performance for continuous operation.

Mr. Wright mentions "freight and passenger trains at hourly intervals . . . 24 freight and 24 passenger trains per day in each direction" as the maximum traffic on a hypothetical two-track railroad, and assumes that this theoretical capacity on account of "incidental delays and track limitations" would be cut in half. The actual 24-hour capacity of a railroad of this type would usually be sadly limited by traffic demands. Under present conditions it is to be noted that people insist on traveling over the railroads at such times as they want to travel, or not at all; and that freight in increasing degree must be handled at the convenience of the shippers or consignees. This means in general that railroads are subject to heavy loads during a few hours of the day and are quite deserted for the rest of the time because there is no business offered. Mr. Wright's estimate of the capacity of his hypothetical railroad, however, is very conservative as to weights of trains and locomotive capacities. He mentions 3,000-ton freight trains and 450-ton passenger trains. Many railroads now are regularly operating, by steam locomotives, freight trains up to 5,000 or 7,000 tons, and occasionally much higher, and normal modern passenger trains often may run to 1,500 tons or more. Speeds higher than 40 miles for freight trains and 70 miles for passenger trains also are common, even with steam operation. It is generally true that the limitation in most cases is set in freight car capacities and track restrictions, rather than in those of motive power. There is some question in this connection as to the practicability of increasing the capacity of rolling stock in the form of larger freight cars, as Mr. Wright suggests. Traffic may not be offered in sufficiently large units to utilize effectively the added capacity. There is, for instance, a general trend away from the older 70-ton hopper cars, and 50-ton cars of this type are more popular today on this account.

It is possible that Mr. Wright's analysis of cost of electric locomotives as compared with steam locomotive costs for equivalent work, may be criticized, and also his estimate of the relative availability for duty of the steam locomotive at 50 per cent. There is still an important spread in motive-power costs as between steam and electric power, and steam-locomotive operation has made remarkable progress during the past few years as regards length of run and availability. Traffic demands now often being at relatively low load-factor, much of the steam-locomotive outage for boiler washing and other maintenance can be matched with the valleys in traffic demands. A record recently quoted (*Railway Mechanical Engineer*, June 1940) refers to a freight locomotive which operated in 31 days a mileage of 9,700, changing crews 79 times in this period without dumping the fire.

Mr. Wright mentions the cleanliness of Diesel-electric locomotives as compared with steam. The development of modern air-conditioned passenger equipment, with sealed windows, forced ventilation, and filtered air, has changed the situation in this respect of recent years so that in the modern train, passengers often do not know nor care whether the motive power is steam or not.

Another small point in Mr. Wright's paper to which some exception can perhaps be taken, is that the modern coach weighs one-half as much as former designs of all-steel passenger cars. When modern equipment is considered, a weight reduction of perhaps 30 per cent is more accurate as a general estimate.

Mr. Wright's analysis of electric locomotive efficiency in weight and in power production is of interest. The low thermal efficiency in the conventional type of steam locomotive is more fundamental than merely "imperfect combustion." The high exhaust pressure of steam is even more vital, and it is not to be expected that a thermal efficiency of anything like 20 per cent, which Mr. Wright mentions, can be obtained without radical change in boiler and furnace design, which will be necessary with increase in pressure, and in the utilization of steam. The application of hydroelectric power, or of high central-station thermal efficiency now available, and the very considerable concentration of horsepower readily available at the head-end of a train by the use not only of a single electric locomotive unit, but of multiple-unit equipment, is a recognized advantage of electric locomotives as compared with steam, and as trains increase in weight and speed, the electric locomotive is an obvious solution of many of the problems facing the railroads if its use can be brought about without undue correlated costs.

P. A. McGee (Electromotive Corporation New York, N. Y.): I believe Mr. Wright has very well presented the railway problem in both its economic and important technical phases. He has rightly stressed the fact that railways are primarily suited for mass transportation, and in this connection employed the most satisfactory transportation unit of ton miles per locomotive hour.

Finally, Mr. Wright considered a hypothetical case where the desirability of obtaining a 6,000-horsepower locomotive was indicated, and on this basis, a case for railroad electrification is considered.

I do not propose to go into the technical details of the various types of locomotives available for rail transport, but I feel that Mr. Wright, in order to justify the title of his paper "Modern Rail Transport", should have devoted more space to the question of Diesel locomotives.

As an indication of the modern trend in rail motive power, it should be recognized that the steam power available today is the result of a century and a quarter development. Electric locomotives were first applied a little over 40 years ago. The Diesel locomotive as a serious competitor in the rail transportation field dates back about five years. Today we have in the United States approximately 45,000 steam locomotives which are more or less interchangeable on the various lines of our rail system. After 40 years of rail electrification development, we employ today not over 850 electric locomotives of 100 tons or more and most of these locomotives are designed for some specific traffic problem, and very few, if any, of these locomotives, can be satisfactorily transferred from one electrified system to another. There are today some 750 Diesel switching locomotives of 100 tons and over, together with approximately 130 Diesel road units in service in the United States. These Diesel locomotives are interchangeable between railroads, and as a matter of routine operation, pass over as many as four or five lines. Due to the extensive use of these Diesel locomotives and the large mileage covered in daily road service, their yearly performance probably exceeds that of the electric locomotives. This is quite an accomplishment, considering the limited time that Diesel locomotives have been applied to heavy switching and road service.

Although a straight electric passenger locomotive can be designed considerably lighter than a Diesel passenger locomotive of equal horsepower output, the Diesel locomotive is certainly the answer to very high-speed trains where long distances are involved and complete interchangeability is required between the various rail systems.

With freight transportation it will be found that properly to exploit the advantages of individual axle drive, there will be no material difference between the weight of the electric and Diesel locomotive, or if there is, the added tractive power of the heavier locomotive will be a decided advantage.

In the hypothetical case employed for illustration where the production of 180,000 ton-miles per hour was obtained with a 6,000-horsepower locomotive, Mr. Wright showed that a steam locomotive of approximately 620 tons would be required as against an electric locomotive of 270 tons. With the 3,000-ton train the advantage of the reduced weight as such is of no great importance, except as to the amount of weight available for adhesion between the two locomotives. In this respect the electric locomotive may have a very great advantage over steam.

Actually there are many steam locomotives today in operation of approximately 600 tons weight, but none of these locomotives can under normal conditions produce, 6,000 horsepower, nor can they over a rolling profile consistently produce for a matter of 24 hours 180,000 ton-miles per hour.

It will be noted that the 180,000 ton-miles per hour were obtained by assuming a speed of 60 miles per hour. I believe that a more practical locomotive should produce these ton-miles per hour at a considerably lower speed.

This brings up the question of the locomotive characteristic throughout its operating range, where I feel that both the conventional electric and steam locomotives are at a decided disadvantage in comparison with the constant horsepower output of the Diesel locomotive.

Mr. Wright states that the first cost of the Diesel locomotive is about twice that of the electric locomotive. This estimate overlooks the really important items in the cost of railway electrification. Considering only the cost of the power plant to supply railway electrification, it is interesting to note that the cost of a Diesel locomotive fully equipped on the rails per horsepower output, is considerably less than the cost per horsepower of the power plant.

It is true that with a diversified load, it is not necessary to have the power plant capacity equal to the total nominal rating of the locomotives on the system. However, when traffic becomes congested due to conventions, sports meetings, special freight movement, or irregularities due to accident, the problem of peak power demand with electrification is a serious one.

Finally, if you consider rail transportation from a national viewpoint, it must be evident that the artificial concentration of movement on certain favored lines is on many counts not practical. If railways were considered according to a "Peace Economy" and without any thought to the effects of washouts, fires, derailments, and other accidents, it might be shown with a purely economic study that over half the railway lines in this country could be eliminated with great economic advantage to the retained lines, regardless of the type of motive power employed. With the national awakening to the importance of railroads for not only economic but also for strategic reasons, it is difficult to see how traffic could be concentrated on certain privileged routes.

Experience to date with electrification has very clearly brought out the inflexibility of this type of motive power in the event of the hazards just outlined. There are, of course, great advantages where high speed and large tractive power are required with the individual-axle electric drive. To obtain these advantages to the maximum and retain the present flexibility of the steam locomotive, the Diesel locomotive with an electric drive would appear to be the answer.

A. M. Wright: Some of the discussion has questioned the wisdom of consolidating traffic on certain favored routes between centers of population, both because some communities would be left without rail service, and because of possible dislocation of transportation in case of accidents or wrecks. It was not the intent of the paper to recommend that all routes but one be abandoned between, for example, New York and Pittsburgh, but a glance at a railroad map shows that not only do the main lines of the B. & O. and the Pennsylvania connect these cities, but that there is also a multitude of interconnecting branch lines whose operation is of questionable economic value, and which serve communities whose needs might just as well and more cheaply be met by road vehicles. Mr. McGee mentioned the strategic value of the railway system as being inconsistent with concentration of general traffic on certain routes, but I do not think the occupation of an area by enemy forces would be a disaster of less magnitude if that area were served by many railway lines. Peacetime economy requires that the transport system be operated on an economic basis, and it is to be hoped that the gearing of our economy to the whims of the megalomaniac gangsters of Central Europe will be a fleeting phenomenon. Any such program of consolidation as was suggested in the paper will be consummated only after years of preparation, and for strategic purposes the existing railway trackage is probably more than sufficient, particularly since moving armies rely almost wholly on motor vehicles for transportation in the zone of action. Mr. Withington's comment that the traffic density on the railways is more likely to be limited by the business offered than by the limitations of the tracks is unfortunately only too true at present, but it is the author's hope that by proper co-ordination, rather than competition, of road and rail vehicles, and the establishment of central freight depots in centers of population, the railroads will eventually be enabled to concentrate on the main line haul of passengers and commodities, and that road vehicles will be confined to pick-up and delivery. With each transport tool thus restricted to the business it is best fitted to perform, I think that a great many of our present difficulties will disappear.

Regarding the weight of modern passenger rolling stock, it must be remembered that modern cars are usually longer than those built ten years ago. The Reading Company's "Crusader" is a good example of a modern train, with appointments as luxurious and more comfortable than could be obtained in a Pullman car of ten years ago. This train with cars averaging 80 feet in length, weighs 1,250 pounds per foot, including a dining car and air conditioning equipment. A Pullman car of 1928 vintage, $75^{1}/_{2}$ feet long, with no air conditioning equipment, weighs about 2,200 pounds per foot of length.

Mr. Withington is correct in ascribing much of the inefficiency of the steam locomotive to high exhaust pressure. A locomotive with a boiler pressure of 250 pounds per square inch, and using steam at 150 deg F superheat, would have a theoretical thermal efficiency of 22 per cent if perfect adiabatic expansion of the steam could be obtained down to 15 pounds per square inch. If the back pressure could be reduced to 28 inches of vacuum, the theoretical efficiency would be raised to 36.3 per cent. Variable cutoff, heat losses, etc., would lower these figures considerably, and there would be a great increase in dead load due to the weight of the condenser. Other practical difficulties also militate against the use of condensers on locomotives. The combustion problem is, however, just as serious. It has been estimated that 50 per cent of the coal fired into a locomotive passes unconsumed through the smokestack. The shower of carbon particles which accompanies the passage of a steam locomotive supports this estimate, and Lipitz (ASME paper RR-56-6, 1934) quoted boiler efficiencies of from 42 per cent to 52 per cent, with over-all locomotive efficiencies of 3.8 per cent and 5.6 per cent. In past designs imperfect combustion was largely due to insufficient combustion space and to the sudden temperature drop experienced by the furnace gases as they entered the boiler tubes, where little further combustion occurs. In modern designs a generous combustion chamber is provided, but this has the effect of lengthening the boiler and increasing weight at a rate approaching the rate of increase of boiler output.

Mr. McGee commented on the noninterchangeability of electric locomotives and on the cost of the power supply facilities required for electric traction. We certainly cannot run electric trains on nonelectrified tracks, but apart from this, railway electrification engineers have been remiss in not agreeing on standards for electrification. If electric traction is ever to become an important economic factor in main line rail transport, the advantages of standardization of transmission and trolley voltages and locomotive types must certainly be applied. The achievements of Diesel-electric locomotives in recent years have indeed been impressive, but their success in handling long-haul trains in regions of sparse traffic density does not show that they will be equally successful in handling very dense traffic. The cost of power-supply facilities, as Mr. McGee rightly says, far outweighs the economy of electric locomotives on lightly loaded railways, and here the Diesel locomotive shows up to great advantage. But when the traffic becomes very heavy, the low operating cost of electric locomotives outweighs the cost of power-supply facilities, and electric traction is an economic proposition. Each type of motive power has its own field of application, and present indications do not show that one will displace the other from its own field. Mr. McGee's remarks are in general confined to conditions obtaining on light traffic railways; on such lines the rate of production of transportation by a train may be kept high, although train speeds are low. But on a congested railway low freight-train speeds are intolerable, and in such cases the rail power rating per ton of locomotive weight is of real importance. The poor weight efficiency of the self-contained locomotive is a great drawback where traffic density is high, although, as Mr. McGee says, it does not matter much where low train speeds are not objectionable.

The remarks of Mr. MacLeod and Mr. Brandenstein are of particular interest in emphasizing the importance of keeping down the cost of wayside equipment with electric traction. The lower this cost can be made, the lower is the traffic density at which electrification is feasible, and manufacturers of electric traction equipment are therefore affected by this phase of the problem quite as much as the railways themselves. The cost of labor and work train service affects the cost of installation, but perhaps not so much as Mr. MacLeod's comments would indicate. For catenary work, about 25 per cent or 30 per cent of the total cost is chargeable to labor and work trains, but for erection of steel supporting structures this figure is of the order of only 12 per cent. Concrete foundation work is not much affected by traffic, as the greater part of the labor cost is in excavation, backfill, placing reinforcing, etc., which is mostly done from the ground.

The figure of 100 pounds per rail horsepower of continuous rating given in the paper for electric locomotives is very conservative, as Mr. Brandenstein points out. As long ago as 1925 locomotive E-501 of the Paris-Orleans Railway was put in service, having a weight of 85 pounds per continuous horsepower. But even Mr. Brandenstein's 240-ton locomotive of 6,000-horsepower rating is not a record. A locomotive of the new "E-19" series built in Germany weighs only 50 pounds per continuous rail horsepower. Such locomotives are suitable for high-speed freight movements, but would, as Mr. McGee has implied, perhaps have difficulty in starting an extremely heavy train, even though the operation might be within the power capacity of the locomotive.

Mr. Brandenstein's comments on transformer reliability indicate that we can expect a less conservative substation layout in future electrification. But the other substation equipment affects the over-all cost in a very important way. Particularly is this true of high-speed circuit breakers. On a 4-track railway requiring a substation capacity of 200 kva per mile of track there would on this account be a great difference in unit cost per kva between substations spaced 10 miles apart and substations spaced 15 miles apart, although the total installed capacity might be the same in both cases. To take full advantage of transformer reliability it will therefore be necessary to raise the reliability of the catenary system to the same degree.

A High-Speed Differential Relay for Generator Protection

Discussion and author's closure of paper 40-103 by W. K. Sonnemann, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940 (November section) pages 608–12.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): The two ideas incorporated by Mr. Sonnemann; namely, the use of a variable-ratio characteristic in a differential relay and its dependence upon phase angle, certainly provides a means for overcoming the tendency toward incorrect operation due to the shortcomings of the current transformers. I do not feel, however, that we can fully accept his statement "that when the restraining current is large, . . ., the sensitivity of the relay *should* be reduced to allow for greater shortcomings in current-transformer performance." A more accurate statement would seem to be one which conveyed the idea that a reduction in relay sensitivity may be an acceptable means of overcoming otherwise unavoidable current-transformer errors. While I feel that there is some real merit to be derived from a variable slope, I do believe that we should avoid giving the impression that it is a definite benefit under all conditions and that we are not interested in obtaining current transformers capable of reasonably good performance.

Giving the relay maximum sensitivity when the difference current is in phase with the restraining current and decreasing the sensitivity for angular shifts, such as would be caused by current transformer errors, would seem to be a further assurance against incorrect operation. The difference current which produces the operating force in the case of external faults is the vector difference between the output currents of the two current transformers used for the generator differential protection. It may therefore be close to 90 degrees out of phase with the restraining current when the two output currents are nearly equal in magnitude, but somewhat out of phase with each other. But when one of the current transformers has a large error then the difference current approaches an in-phase relation with respect to the restraining current. It appears therefore that this characteristic, while of some merit, is not of too great value in preventing incorrect operations on external faults and is obtained only at the expense of some complication.

There has been frequent expression, before the AIEE, of the idea that perhaps there has been too much emphasis on speed of relay operation with a resulting tendency toward incorrect operation due to transients. I, for one, while realizing the validity and seriousness of this thought, have felt, nevertheless, that the work which has been done toward providing reliable high-speed devices should be continued because we seemed to be so close to a solution. It is, therefore, with considerable regret that I see a new type of high-speed relay introduced, wherein considerable thought has been given to preventing undesirable operation on electrical transients, but which is in itself so mechanically unstable that it must have added to it a device for preventing unwanted operation due to vibrations or jars. It would seem that an opportunity has been missed here in not making a device of greater mechanical stability. Presumably this has been done to keep down the burden of the device, but if it is possible to include the burden of a small plunger device of adequate mechanical stability, it would not appear to have been too difficult to obtain a single device having the desired electrical characteristics and adequate mechanical stability as well.

Since a device of the type described is unstable in its operation, that is the operating force becomes greater and the restraining force less when the armature starts to move there would seem to be a real danger of false operation due to the balanced beam being in the operated position when a fault occurs and remaining there while the so-called fault detector picks up due to a small difference current flowing because of current-transformer inaccuracies. In other words, does not the final assurance against an incorrect operation depend on what is in reality a low-set instantaneous over-current relay?

P. O. Langguth and **S. A. Bottonari** (both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Sonnemann's approach toward a more satisfactory relay for differential protection is very valuable in that it provides a relay which will give satisfactory performance without imposing too rigid requirements on current transformers. This is particularly important when relays operate from bushing current transformers housed in circuit breakers. Previous investigations have shown that in some differential-relay installations, current transformers having the necessary characteristics exceeded practical or economical limitations. Likewise, when differential protection is to be applied to existing systems, physical limitations in either breaker or structure may be encountered.

The effect of d-c saturation of current transformers due to offset current faults has been clearly brought out in past articles.[1,2] The approximate expression indicating the severity of current-transformer performance requirements to prevent d-c saturation is well illustrated by the factor $(1+wt)$. Unity represents the component of steady-state performance, and the term wt indicates the required factor due to the d-c transient component. The summation $(1+wt)$ is a measure of transformer performance required for the offset transient fault as compared to the normal a-c equivalent steady-state fault. Typically, a 60-cycle system having a time constant of 0.1 second would demand $1+(377)(0.1)$ or 38.7 times the normal size of transformer to prevent d-c saturation, as required to prevent saturation under the equivalent steady-state condition. Thus it is apparent that the steady-state requirements are negligible compared to that for the d-c transient, since the latter is many times greater.

Obviously, then, a relay which permits the choice of a transformer determined by the a-c characteristics alone, solves the relaying problem from a more practical, and economical point of view. The development of this relay also indicates that existing designs of bushing transformers may be suitable for high-speed differential protection. In this respect it is interesting to note that the author has compared current transformers on the basis of iron area or flux density instead of the conventional practice of using ratio curves. This is a good example of the limitation in bushing-current-transformer ratio curves. It seems that a saturation curve of each transformer involved would be more suitable for the application of relays for differential protection. Consideration should be given to supplanting the ratio curve for relay application work.

REFERENCES

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2. CURRENT TRANSFORMER AND RELAYS FOR HIGH-SPEED DIFFERENTIAL PROTECTION WITH PARTICULAR REFERENCE TO OFFSET TRANSIENT CURRENTS, **E. C. Wentz** and **W. K. Sonnemann.** AIEE TRANSACTIONS, volume 59, 1940 (August section) pages 481–8.

C. D. Hayward (General Electric Company, Philadelphia, Pa.): In the application of high-speed relays to generator differential protection serious consideration must be given to the saturation effects produced by the slowly decaying d-c component present in asymmetrical fault currents. In this paper the author has mentioned the extreme ratio breakdown effects caused by d-c saturation of the current-transformer cores, but no mention is made of d-c saturation effects within the relay itself. The whole theory of the relay as described in the paper and illustrated by figure 4 of the paper has been developed on the assumption of symmetrical sine-wave currents supplied to the relay by the current transformers. Actually, the currents are neither symmetrical nor sine wave as a glance at the oscillograms in the paper shows.

The neglect of the d-c component is particularly serious because the characteristics of the relay *depend* on saturation of the transformer element in the relay. Figure 1 of this discussion shows the relative magnitudes of core flux required to reproduce the a-c and d-c components of the current in the secondary circuit of a current transformer having an assumed burden

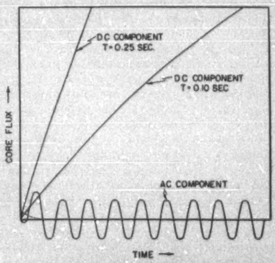

Figure 1. Relative magnitudes of current transformer core flux required for a-c and d-c components of an asymmetrical fault current wave

phase angle of 45 degrees. Curves are shown for the 60-cycle a-c flux component, the d-c flux component for a fully offset wave having a 0.10-second d-c time constant, and for the d-c component of a wave having 0.25-second d-c time constant. It is readily seen that the saturation produced by the neglected d-c component because of its greater core flux requirement, is much greater even during the first few cycles than that produced by the a-c component on which the design of the relay is apparently based.

The effect of the increased saturation of the transformer element by the d-c component would probably be to greatly increase the per-cent restraint. It would seem that this increase could easily be enough to prevent operation of the relay on an internal fault until the d-c component dies out, which would mean a time delay of many cycles. The relay characteristics shown in figures 3, 5, and 6 of the paper were apparently taken with steady-state symmetrical currents. It would be interesting to see a similar set taken with transient offset currents having long time-constant d-c components similar to those met in practice.

The oscillogram of one internal fault test is shown in figure 10 of the paper. Here due to a high degree of saturation of both current transformers the d-c component disappears in the secondary currents after a fraction of a cycle, and a high-speed operation of the relay is recorded. If the test had been made at a lower current, or with lower current-transformer burden, or with better-quality current transformers, the d-c component would have been reproduced more faithfully and the relay operating time would probably have been much longer.

Another point which should be considered is the possibility of the relay failing to operate at all on an internal fault in which the currents fed to the fault from the two directions are considerably out of phase. The relay would be restrained from operating under this condition because of its phase angle-restraint characteristic shown by figure 5 in the paper. There are a number of ways in which these two currents could get out of phase: for example, consider the case of a generator supplying a synchronous motor load. If a fault occurs near the high-voltage end of the generator winding, the generator itself will feed current to the fault from one side, while the synchronous motor load will feed back current from the other side. Since the induced voltage of a synchronous motor lags behind line voltage when acting as a motor but leads it when acting as a generator and since a further angle of lag would be added on by line reactance drop, due to the reversed direction of current flow, it would seem that the current fed from the synchronous motors might lag behind that from the generator itself by a considerable angle. Generator faults occurring during swing periods would also find the two currents out of phase depending on the swing angle. If the tripping of the relay is delayed due to saturation on the d-c component as described before, it is quite possible that the faulted generator itself would swing out of step with the system voltage during the delay by an angle great enough to prevent tripping after the d-c component has died out. A thorough study of the possibility of occurrences of this kind should be made before the relay is applied. No such study is described in the paper. In the one internal-fault test described the fault currents from the two directions were prevented from getting out of phase by the use of the same primary current, from a single source in both current transformers as shown in figure 7 of the paper.

W. K. Sonnemann: The author was gratified to note the approval expressed by Mr. Langguth and Mr. Bottonari of the method of comparing current transformers. Recent experience has indicated that this method, as well as the use of the saturation curve of the transformer is, in many cases, of considerably more value than the use of the conventional ratio and phase-angle characteristics.

Mr. Kennedy's first paragraph is quite interesting. The author is quite willing to soft-pedal the word *should* in favor of the statement that: "Variable ratio characteristics will, in many cases, allow the use of standard current transformers where otherwise super-quality transformers might have to be used."

Mr. Kennedy seems to doubt that the phase-angle characteristics described have the value which is claimed, and he cites cases where the differential current might be very nearly in phase with the restraining current. In a long series of tests of this relay, and in the development tests which preceded its final design, considerable variation in phase-angle relationships was observed. It was for this reason, as pointed out in the paper, that both the phase-angle characteristics shown and the variable-ratio characteristics were incorporated in the design. Both contributed to the satisfactory performance of the relay.

Mr. Kennedy's statement that the device is unstable in its operation because the operating force becomes greater and the restraining force less when the armature starts to move is quite an overstatement. There is a slight amount of this effect noticeable, but this does not exceed 25 per cent. Moreover, it does not render the relay unsatisfactory in the manner alleged. As a matter of fact, the adjustable weight placed on the back end of the beam is sufficient to keep it in the reset position even when the relay is totally de-energized. During the flow of a heavy external-fault current, the amount of restraint is a great deal more than any amount of additional weight which could be added to the beam in any practical design. For this reason it is not correct to assume that the beam could be in the operated position during the time that the sensitive overcurrent element (fault detector) is picked up because of an external fault.

The first portion of Mr. Hayward's discussion brings up a number of points which cannot be answered in the detail which is apparently expected and still keep the discussion within permissible limits of length. The author believes it is sufficient to say that the *effect* of the d-c component on the auxiliary transformer is known, and that it is favorable to the correct operation of the relay rather than unfavorable. The d-c characteristics of the relay cannot be expressed in curve form, however, because of the fact that an infinite family of curves would be required. That is, for a fully offset current wave, the d-c component begins at a maximum value and decreases instant by instant, until it finally disappears. At each succeeding interval of time, no matter how small, the d-c component of the main fault current is a lower percentage of the original value, and it is thus seen that to endeavor to show curves for every possible percentage of direct current would require an infinite family. The a-c characteristics of the relay discussed in the paper are primarily useful for two purposes and secondarily useful for a third purpose. First, they apply directly in the case of symmetrical fault currents. Secondly, they are a useful reference in checking the relay itself to determine that it is in satisfactory condition. The secondary purpose of these curves has been to serve as a guide in predicting the probable performance of the relay when the current wave is offset. Obviously, they could not be used in this fashion without proper interpretation, but the experience gained in development indicated that the type of curves shown were in the right direction. The author feels that the final answer with regard to the d-c characteristics lies in the fact that the relay met every test condition satisfactorily. These test conditions covered the range of fault currents from high to low values and from symmetrical faults to asymmetrical faults. In tests of this kind, it is the usual practice to make repeated tests until both a fully offset wave and a fully symmetrical wave have been obtained. In the course of such tests, a number of intermediate conditions were obtained. That is, some of the current waves were partially offset so that the per cent of direct current obtained in the whole series of tests ranged from 0 to 100 per cent. It is felt that, since this procedure was followed with both high and low and intermediate current values, the tests were complete.

Mr. Hayward places considerable stress in his last paragraph on the phase-angle characteristics of the relay. It seems he has completely overlooked discussion in the paper which points out in detail that these curves, figure 5 of the paper, are primarily for the through-fault condition. They do not apply directly for internal-fault conditions without proper interpretation because, (1) in the test setup in which the curves of figure 5 were obtained, the connections represented the through-fault condition in which the ampere turns in the restraining coils were additive, and (2) for internal faults to which the bus also contributes, the currents are in opposite direction and the ampere turns in the restraining coils are subtractive.

The worst condition emphasized by Mr. Hayward would be in the case of a bus contributing a current to an internal fault near the high-voltage terminals equal to the current contributed by the generator and slightly out of phase with it. The ratio of the current transformers used with a generator, combined with the subtransient reactance characteristics of the machine, will limit the current contributed by the machine itself to not over 60 amperes in terms of the secondary, and even this is an extreme case. If it is now assumed that the bus contributes 60 amperes also, and that this 60 amperes is 30 degrees out of phase with the generator current, the relay conditions are derived as follows. The effective restraining current being the difference between the two, there results a current value of $8+j30$ amperes having a scalar value of 31 amperes. However, since there are two restraining coils and the characteristics are based on the average of the two, it is necessary to divide the 31-ampere value by two and thus obtain an effective value of 15.5 amperes restraint on which to determine the relay characteristics. Referring to figure 5 of the paper, the nature of a 15.5-ampere restraint curve, falling between the 5-ampere and 25-ampere curves shown will be readily noted. It is thus seen that the operating current, equal to the vector sum of the two 60-ampere currents quoted and having a scalar value of 116 amperes, will be considerably more than sufficient properly to operate the relay.

There are theoretical considerations indicating that in extreme cases the two currents fed to an internal fault might be more than 30 degrees out of phase. However, the conditions that would allow this phase angle to be exceeded are such as to render the effect unimportant. Where generators are operating tied together with an extremely long tie line, it is possible for the generated voltage of one machine to run 30 degrees to 40 degrees or possibly even very slightly more out of phase with the other. However, in this case the impedance of the tie line will so largely reduce the amount of current which the distant generator can contribute that its effect would be negligible.

Computation of Accuracy of Current Transformers

Discussion and author's closure of paper 40-89 by A. T. Sinks, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940 (December section) pages 663–8.

W. K. Dickinson (General Electric Company, Lynn, Mass.): It may be of interest to point out that the formulas given in Mr. Sinks' paper are not of interest simply from a theoretical point of view, but they have been used for some time as a valuable tool in current-transformer design.

An accurate method of calculation of the ratio and phase-angle characteristics is valuable not only in obtaining the optimum design for standard lines of transformers, but it becomes almost invaluable in the design of special transformers to meet exacting conditions, such as the operation of differential protective relays at very high currents.

In the days when 20,000 or 30,000 amperes was considered to be a lot of current, it was not particularly difficult to design adequate transformers. Now, with the great interconnected systems making it sometimes necessary to design for currents as high as from 120,000 to 130,000 amperes and with accuracy requirements to meet very exacting selective relaying, the transformer-design problem is much more difficult. Not only are the transformers large and expensive, but the cost of testing at these enormous currents is high.

Therefore, it became very necessary to predict accurately the characteristics of existing transformers which had not been tested at high overcurrents, and to design correctly, the first time, transformers to meet very exacting conditions.

It was to meet this necessity that the improved method of calculation was developed by Mr. Sinks and his method has proved of great value in a large variety of designs over a period of several years.

E. C. Wentz (Westinghouse Electric & Manufacturing Company, Sharon, Pa.): Mr. Sinks' paper is a most workmanlike exposition of a method for practical solution of a problem the complexity of which defies attempts at rigorously exact solution.

The proof that the leakage flux Φ_{L2} linking the secondary turns brings about a reduction of $\Phi_{L2}/3$ in the flux at the center of the core is a new and interesting idea, inasmuch as the method of calculation which we have used, although similar in general to that given by Mr. Sinks, has been based on division of the core into only two parts, one part corresponding to section 1–2 of figure 1 in Mr. Sinks' paper, the other part consisting of the remainder, and we have assumed that the average flux in section 1–2 is simply E_2/K (equation 5 of the paper). However, it does not seem that it should require quite so much mathematics to show that the effective linkages due to Φ_{L2} are $\Phi_{L2}/3$, and it seems obvious that Φ_C must be reduced from Φ_M by this amount.

I am personally glad to see that Mr. Sinks belongs to the small group of fundamentalists who do not believe in a mythical secondary leakage reactance. "Secondary leakage reactance" is a useless as well as an imaginary conception. Cutting right to the heart of the problem with analysis of real and measurable fluxes is a contribution to clear thinking about the whole problem.

The number of sections into which the core can be divided is quite arbitrary, because the only exact division is into an infinite number of sections. We have found during the past eleven years that a division of the core into two parts usually strikes a reasonable balance between the amount of work in calculating performance and the probability of error in the result. This method was described in a thesis for the University of Pittsburgh, "Iron Losses in Current Transformers, With Particular Attention to the Effect of Leakage Flux" (1936).

We have since worked out a method of calculation which allows the numerical work to be considerably reduced. The principle of the method is that the core divided into several sections, as described by Mr. Sinks, can be represented by the equivalent diagram in the accompanying figure 1. Each exciting branch represents one section of the core, and the series reactances are equivalent to the various leakage fluxes. The increment of exciting current which produces a certain per cent ratio error can be read directly from curves according to the method described by A. M. Wiggins in "Ring Type Current Transformers—Limitations in Design" (*Electric Journal*, Dec. 1927, page 621).

The method is simple enough that even one not familiar with transformer theory can still calculate ratio and phase angle in a few minutes, given the equivalent diagram with constants for a given transformer and the necessary curves. We have been considering giving this data to customers instead of the ratio and phase-angle curves. This method involves a return to fictitious secondary reactances and the whole equivalent diagram has no real existence in reality. However, it gives exactly the same result with much less numerical work.

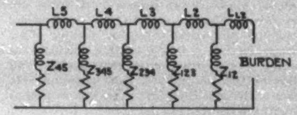

Figure 1. Equivalent diagram of current transformer

(All quantities referred to the secondary winding)

$L_{L2} = \dfrac{-N_2\Phi_{L2}}{3I_2}10^{-8}$ = equivalent inductance corresponding to leakage flux $\dfrac{\Phi_{L2}}{3}$ negative value

Z_{12} = exciting impedance corresponding to section 1–2 of the core. (See Sinks figure 1)

$L_2 = \dfrac{N_2\Phi_2}{I_2}10^{-8}$ = equivalent inductance corresponding to leakage flux Φ_2

Z_{123} = exciting impedance corresponding to core sections 1–2 and 2–3

$L_3, L_4, L_5 = \dfrac{N_2\Phi_3}{I_2}10^{-8}, \dfrac{N_2\Phi_4}{I_2}10^{-8}, \dfrac{N_2\Phi_5}{I_2}10^{-8}$

$\left.\begin{matrix} Z_{234} \\ Z_{345} \\ Z_{45} \end{matrix}\right\}$ Exciting impedances corresponding to core sections $\left\{\begin{matrix} \text{2–3 and 3–4} \\ \text{3–4 and 4–5} \\ \text{4–5} \end{matrix}\right.$

The best method we have been able to work out for calculating the leakage flux in bar type current transformers gives fair agreement between calculated and measured ratio and phase angle, but the measured fluxes in different parts of the core not only do not agree very well with calculations, but vary widely with the position of the primary conductor, much more than we have expected, even though the ratio and phase angle is not affected appreciably. I should like to ask if Mr. Sinks has been able to overcome this difficulty.

Duff and Price and other authors have written about "negative secondary leakage reactance" and have used it to show that the ratio error may be negative, that is, that a current transformer may actually deliver more secondary ampere turns than is put into the primary winding. If "secondary leakage reactance" is elusive, a negative value of it is one degree further removed from reality and I have never quite understood how the ratio error can be negative. However, we believe we have actually observed this effect on test although it is always very small and would like to have Mr. Sinks discuss this.

I should also like to ask if Mr. Sinks has a method for accurate calculation of the effect of Wilson compensation. Although theory of Wilson compensation has been described in the original paper, we have never been able to calculate the ratio and phase angle variations produced by Wilson compensation or any of the similar schemes which have been devised, with sufficient accuracy that we could use it with confidence for special transformers, the performance of which must be predetermined before they are built.

A. T. Sinks: Mr. Wentz's discussion brings out some important points, and the hypothetical circuit developed for purposes of calculation seems to have interesting possibilities.

As Mr. Wentz points out, the equivalent circuit method he shows is probably simpler after all the impedance values and curves are established than the fundamental method presented in the paper, particularly inasmuch as the former makes use of circuits more familiar to most engineers. However, it must be borne in mind that the determination of these hypothetical constants requires considerable preliminary work in the form of either tests or calculations of the kind presented in the paper. Moreover, once the leakage fluxes have been established the work of calculating the accuracy by the method given in the paper is not so great and the one set of figures for leakage is good for all burdens.

As Mr. Wentz suggests to simplify the calculation the core can be divided into two sections, and, in fact, even one section can be used by using a flux density equal to the average of the maximum and minimum calculated values which will usually occur at sections 1 and 5. However, the more the calculations are simplified in this manner the greater the likelihood of error and the more experience is necessary in order that one won't be misled by such considerations as nonuniform core cross sections and the changes in relative values of magnetizing forces accompanying saturation of points of high flux density.

With regard to Mr. Wentz's questions on variations in leakage flux in bar type transformers, I believe this problem is very closely related to the old problem of interference from the return conductors. On bar-type transformers the interference effect is, of course, more pronounced than on wound types. Many tests on bar- or window-type transformers were made by the Bureau of Standards by J. H. Park (see bibliography of paper) in which interference was investigated. These tests show considerable variation in accuracy due to effect of return conductors although no measurements of core flux were reported. However, tests on similar transformers and calculations on the "stray flux" show that the changes in core flux due to the return conductors must have been enormous, more than would be expected from changes in accuracy shown. These tests, therefore, are in agreement with Mr. Wentz's observations with this important addition, that when the flux densities are high enough to produce saturation in one or more parts of the core due to the stray field the change in accuracy may be quite pronounced.

The explanation of why large changes in flux density resulting from mechanical changes in conductor position usually do not produce correspondingly large changes in accuracy is probably that such mechanical changes increase the flux density in one part of the core about as much as they decrease it in another and that at low densities generally encountered in current transformer operation a given per cent change in flux density requires a smaller per cent change in magnetizing force. At high flux densities on the other hand, where a slight change of flux requires a large change in exciting current, the accuracy may be greatly affected by changes in flux resulting from mechanical changes in the conductors. This leads to the conclusion that interference from adjacent conductors is probably more important with respect to relaying applications than with respect to metering.

Mr. Wentz raises a question about the possibility of the ampere-turns in the secondary being greater than those in the primary, saying that he believes such a condition has been observed to a slight degree. We have also noticed this condition on a few occasions. I agree with Mr. Wentz that explaining this phenomenon on the basis of a "negative secondary reactance" is rather tortuous logic. Moreover, although such a concept might be used for purposes of calculation it does not clarify the physical situation until the negative reactance itself is explained. That the secondary ampere turns can be greater than the primary, however, can be proved from fundamental considerations rather simply.

To do so consider a toroidal core like a bushing-transformer core with a secondary winding equally distributed around the core and a round primary conductor straight through the center of its toroid. Consider, further, that the secondary is quite thick relative to the cross-sectional dimensions of the core. Due to the symmetry of this construction all flux lines will be circular and none will enter the core or leave it. Moreover, when the primary and secondary turns are exactly equal there will be leakage flux linking the secondary winding outside the core, and the density of this leakage flux will be zero immediately outside the surface of the core and will increase continuously to a maximum value at the outside surface of the secondary winding where the density will be determined by the primary ampere turns and the circumference of the flux circles. The direction and phase of this leakage flux will be the same as the magnetizing force produced by the primary current. Since this leakage, therefore, produces a voltage which is 90 deg out of phase with the primary and secondary currents this voltage in the secondary must be balanced out either by reactance in the external secondary burden or by flux linkages in the iron core opposite to the leakage. Consequently, if there is no external reactance, there will have to be flux in the core opposite to the direction of the primary magnetizing force and ampere turns opposite to the primary ampere turns will be required to produce the flux. Hence, the secondary ampere turns will have to be greater than the primary.

The conditions outlined may be considered as a negative reactance, but actually they are the result of superimposing a positive mutual inductance on a somewhat smaller positive self inductance.

It is interesting to note that if the windings on the transformer in the paper are spread out uniformly around the core and the latter made circular, the quantity Φ_{LT} in equations 7 to 10 disappears and the equations all become the same, and if L_s is then taken as the mean length of magnetic circuit, the resulting formula will give the flux for the toroid just discussed. Moreover, if the external reactance X_B is taken as zero, following through the calculations will show that the ratio correction factor will be less than unity if (R_B+R_2) is not too great. In bushing transformers the secondary windings are usually so thin and the distance around so long that the effect of leakage is not detectable and it would probably be necessary to build a very special transformer to measure the effect under discussion.

To answer Mr. Wentz's last question the Wilson Compensation has been calculated with fair accuracy, but it hardly seems practical to do so ordinarily because empirical data has pretty well established the limitations of this compensating scheme and also determined the best position of the hole. This is all that it is really essential to know in design work since the number of turns and the resistance of the short-circuited turn in the compensating windings can be readily adjusted after the transformers are built to get the exact value of compensation desired.

New Instruments for Recording Lightning Currents

Discussion and authors' closure of paper 40-90 by C. F. Wagner and G. D. McCann, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940, pages 1061–8.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The paper is of considerable interest, as it presents a variety of instruments for measuring lightning currents and charges. Although considerable progress has been made in our knowledge of lightning currents, there still remains considerable work to be done before lightning is fully understood.

Before discussing the instruments, a survey of established facts and further desirable information is useful. Some known facts are the following:

1. A considerable amount of records have been obtained concerning amplitudes of lightning currents on transmission lines, distribution circuits, and through station-type and distribution-type lightning arresters. Distribution curves of these currents with respect to frequency of occurrence of any given amplitude have been obtained principally by means of the magnetic link method in several countries and these curves in general show good agreement with each other.

2. Current wave shapes involving relatively high amplitudes and short time duration have been recorded in a relatively few instances by means of the cathode ray oscillographs. The number of records is too few to allow a safe estimate on the limits of front and tail length of high current lightning discharges and further investigation in this field is required.

3. Long-time or continuous lightning discharges have been observed first by Walters photographically. These discharges have been proved to be produced by actual current flow first by Dr. K. B. McEachron

in his investigations on the Empire State Building using simultaneous records by means of the oscillograph measuring the current in the stroke and the photographic camera taking a photograph of the same stroke using a moving image. Further, the existence of such continuous discharges has been proved by comparing the effects of laboratory discharges with lightning traces obtained in the field on counterpoise gaps, on metal parts of airplanes, distribution line conductors, and others. From Dr. McEachron's oscillograms and the other evidence mentioned, a fairly good estimate of the total charge in a single lightning stroke can be made. It is found that approximately 50 per cent of all strokes have a charge of between 15 and 30 coulombs, while 10 per cent exceed 50 coulombs.

4. One oscillogram is available showing the rate of rise of voltage on a transmission line. This oscillogram was taken close to the point of the stroke contact with the line. Although this was taken early in the art of impulse measuring when methods of measuring were not as well understood as today, this oscillogram probably gives a fairly good indication of the severity of the voltage wave of a lightning stroke to transmission lines.

Today the principal points of interest to the apparatus engineer concerning the lightning stroke probably are the following:

(*a*). The fronts of the high current discharges

(*b*). The tail of the high current discharges

(*c*). Total charges in a lightning discharge

(*d*). Distribution of total charge in a lightning discharge on transmission and distribution circuits

This is probably the most important information for improvement of continuity of service on overhead systems.

Of the instruments described in the paper, the cathode ray oscillograph is the only one which can give accurate records of the current wave front and tail. Although the high speed fulchronograph would fundamentally be capable of distinguishing between a wave tail of 40 microseconds or longer, the properties of the magnetic link method are such that a number of links will be subjected at varying degrees at the same time, so that even with the wheel stationary, a relatively long front and a long tail are obtained (see figures 9 and 13 of the paper). The apparent time from zero to zero from figure 13 would have been 200 microseconds for a 20,000-ampere crest. Most of the lightning records obtained with high speed oscillographs show fronts of 2 to 12 microseconds and tails of 30 to 100 microseconds. The usefulness of the high-speed fulchronograph for obtaining such detailed information is therefore doubtful. For measurements concerning the duration and amplitudes of the low current discharges or continuous discharges the "slow-speed" fulchronograph should be a very suitable and relatively inexpensive instrument. The "high-speed" fulchronograph would undoubtedly give a confusing picture of a discharge consisting of several multiple strokes connected by low-amplitude continuous current flow, if the total discharge time is longer than 17,000 microseconds—which corresponds to one revolution of the wheel. Maey strokes with duration longer than 50,000 microseconds have been observed photographically at locations other than the Empire State Building. For this reason the slow speed fulchronograph, or the magnetic oscillograph as used by Dr. McEachron, should prove the more useful instrument as regards the continuous discharges while the cathode ray oscillograph appears to be the only solution to measure the fronts of the current waves. It is surprising to learn that the speed of rotation of the film for the oscillograph has been chosen as low as five revolutions per minute or 12 seconds per revolution.

If the size of the film shown in figure 13 is the actual size, this would mean, that the zero point of the oscillogram moves 1 mm on the circumference in 27,300 microseconds.

In the New York and Pittsfield lightning investigations by the company with which I am associated, it was found that successive peaks of relatively high amplitude could occur at as short times as 500 microseconds and times of 30,000 microseconds are very frequent, which should cause considerable overlapping of successive waves on the film and would make it extremely difficult to obtain proper separation. The selection of logarithmic sweep for the purpose of obtaining lightning current waves appears to be excellent, since it provides a good time scale for the front as well as for the tail.

With the exception of the oscillograph, the instruments described by the authors make use of the magnetic link. During the investigations on transmission lines and other places, it was found that the results of measurements have to be interpreted with great care. The magnetic link reading is easily affected by surges of opposite polarity, by magnetic induction from nearby conductors. The authors probably are too optimistic in stating that the over-all accuracy of the measurements are about 10 per cent. However, the instruments, if placed in numerous locations, should enable the authors to obtain statistical data. It would seem that the greatest reliability should be obtained from the "slow speed" fulchronograph records.

C. M. Foust (General Electric Company, Schenectady, N. Y.): This paper is interesting reading to all those who have struggled with this problem of devising suitable instruments and methods for this difficult field of measurement. That it is a difficult field of measurement is easily proved by a few generally accepted facts. The time intervals under consideration range from one microsecond to one second, a range of a million to one. Current values of interest range from 10 to 150,000 amperes, a ratio of 15,000 to 1. The time of occurrence of a stroke is, I believe you will all agree, most unpredictable except that it should come sometime during the period of a lightning disturbance. Suppose we assume a passing lightning disturbance of about two hours duration. The lightning technician must be ready to start a record in any one of the 7,200 million microseconds in this two-hour period with no warning as to which one it will be, or how long a time duration this particular record should have, or for what amplitude of current or shape of wave he should be prepared.

In the period of investigational work extending over the last 10 or 15 years, the instrument problem has been the outstanding one and the progress which has been made toward a better understanding of lightning, which I believe is substantial, has been achieved almost wholly because measurement instruments of greater effectiveness have been developed.

To one whose efforts and confidence have for some time been invested in the new sealed-in type cathode-ray oscillograph and the magnetic, surge-crest ammeter, it is more than gratifying to read of Messrs. Wagner's and McCann's use of these methods. Since 1936 we have been using a similar hot-cathode oscillograph for both laboratory and field work. Vacuum pumps are not required; permanent photographic records are taken outside the tube and simultaneous visual observation of the wave shapes under investigation are made. The authors of this paper have pointed out that the grid control of the electron gun provides for ready initiation of the cathode beam. Details of such a technique for automatic operation of the hot cathode-ray oscillograph are contained in a paper presented by Mr. N. Rohats and myself at Minneapolis and Scranton last summer.

I assume, as indicated by the laboratory record of figure 3 of the paper that the amplitude deflection is at right angles to the calibrated time axis, but this assumption has made it difficult to interpret the field record of figure 4. The vertical deflection seems to be associated with the rotary motion of the disk and is suggestive of too early tripping.

The magnetic link method of surge-current measurement has moved steadily forward since our introduction of it in 1932. This method is directly adapted to the measurement of crest current values of unidirectional surges and has been used for thousands of lightning surge-current records on this basis. Multiple link stations, described at Ithaca in 1935, have been in extensive use since then. Experience has shown us that such a combination of several links, each located at different distances from the conductor, will provide records which discriminate definitely between single unidirectional polarity surges and oscillatory or repeated surges. Also, further classification can frequently be made into oscillatory surges of constant decrement or repeated surges of opposite polarities.

The authors' experimental work on the fulchronograph is very interesting. Possibly the principal disadvantage of the moving link recorder is, as pointed out by the authors, its inability to measure high rates of change of current. As indicated by the slope of the distribution curve in figure 9 of the paper, 200 amperes per microsecond would be the upper limit of current slope which can be recorded. While the authors state that the accuracy of the device is of the order of 10 per cent, possibly much greater error than this may be introduced because of magnetic distribution. Three variables—current amplitude, time, and link position are involved and only when current amplitudes change slowly relative to link velocity, is link position negligible. In other words, a considerable number of links must record along any particular current slope to achieve this 10 per cent accuracy figure. This is illustrated in figure 15 of the paper where a laboratory calibration record is reproduced. The agreement between oscillograph and fulchronograph is good for the slope shown which is 800 amperes in 2,000 microseconds or 0.4 ampere per microsecond. It is apparent, however, that accuracy will decrease as current variations become more rapid. Further comment from the authors as to accuracy variations with different wave shapes would be helpful.

The necessity of assuming a time zero based on the nature of the record and the magnetic distribution curve is somewhat unsatisfactory. In this connection I would like to ask if the fulchronograph is equipped with an automatic means to take it out of the circuit after the first record is obtained. If this is not done, composite records such as are often indicated in surge-crest ammeter field data are a real probability.

The field records of figures 13 and 14 of the paper are interesting. The smooth line drawn through the points involves an assumption of current continuity. It might be well to bear in mind that similar records could be obtained by a series of impulses distributed throughout the same time period. Further interpretation of these records would be helpful.

That the moving link surge-current recorder falls far short of attaining the qualities of precision, magnitude, and wave shape recording inherent in the automatic cathode-ray oscillograph is quite apparent. However, the reduced cost warrants some sacrifice and the authors are to be congratulated on their progress in this experimental lightning measurement work.

W. A. McMorris (General Electric Company, Pittsfield, Mass.): The paper presents several very interesting adaptations of magnetic links to obtain much-needed data on the wave shapes and total charge of lightning discharge currents. It is hoped that the installations of these instruments will be in sufficient numbers, under a wide range of conditions, to yield a valuable addition to the data now available.

The current wave shown in figure 14 of the paper is referred to as that of a typical lightning-arrester discharge current. It would be expected from what data have been published that the current magnitude, charge and duration each might vary over a range of several hundred to one, and would be influenced by the locality and the circuits involved. In order to select a record of wave shape that is typical in all of these respects with any degree of certainty, it would obviously be necessary to have a large number of records obtained under a wide range of conditions.

It would therefore be of considerable value in establishing the degree of confidence that can be placed in the wave of figure 14 as a typical arrester discharge current, if Messrs. Wagner and McCann would inform us how many such records were available when their paper was prepared, the localities and types of circuits involved, and the maximum, minimum, and median values of crest current, charge and duration.

It is stated in the paper that the magnetic surge front recorder is not sufficiently accurate if the slope of the tail of the wave exceeds 20 per cent of the slope of the front, due to the effect of the reversal of current in the auxiliary coils. By using two or more magnetic links for each coil, as indicated in figure 17 of the paper, with ranges which overlap sufficiently, it would be possible not only to detect when current reversals of appreciable magnitude had occurred, but also to obtain an accurate measurement of the crest current in the coil even though the reversal has partially demagnetized the links. The method by which this may be done was described in a paper presented at the 1935 summer convention—"Direct Measurement of Surge Currents" by C. M. Foust and J. T. Henderson, Electrical Engineering, April 1935 (AIEE TRANSACTIONS 1935, volume 54), pages 373–378.

There are serious objections to the interpretation of the magnetic link readings on the basis of magnetization tests made with unidirectional currents. First, the hypothesis that the slope of the tail of a wave will not exceed 20 per cent of the slope of the front and large current reversals therefore will not occur in the auxiliary coil, does not appear to be justified. Some of the oscillograms obtained by Dr. McEachron fall outside this limit, and the wide variety of wave shapes recorded suggest that almost anything can happen. Also a flashover on the tail of the wave, which shunts the major part of the current around the recording equipment, may result in a tail which is steeper than the front.

The effect of current reversals on magnetic links, like the effect of vibration illustrated in figure 11 of the paper, is a function of the degree of magnetization. If the method of Foust and Henderson is not used, the degree of reversal that could be tolerated at the higher flux densities would be quite small, possibly of the order of two per cent.

J. H. Goss (General Electric Company, Schenectady, N. Y.): This paper presents an interesting discussion of the methods available for the measuring of lightning discharge currents.

The authors do not discuss the wave shape used in calibrating the device, figure 8, and show only one wave shape in comparison to the cathode-ray oscillograph, figure 15. Figure 15 shows a very slow wave and low crest value and the calibration shown apparently gives one point of the fulchronograph record at the peak and the balance of the points on the tail of the wave.

It has been determined from various tests with surge generators, as shown by Messrs. Foust and Kuehni in the *General Electric Review* for December 1932, that permanent magnets exhibit an effect which can be attributed to eddy current shielding which is dependent upon the speed of the wave.

The authors state that magnets 9 mils in thickness were used but do not state the composition of the permanent magnet material. It would be expected that the calibration curve shown in figure 8 would vary appreciably depending upon the wave shape used to obtain the calibration.

It is also an established fact that masses of highly conducting material in the field of a lightning surge have a pronounced influence upon the shape of the magnetization field and consequently upon the degree of magnetization encountered by a permanent magnet. While it is not possible from the description in the article or from figure 5 of the paper to evaluate exactly the amount of conducting material in the field of the magnetizing coil, it appears there is a considerable amount of aluminum and other material within this field. I should like to ask the authors if they have evaluated the effect of various wave shapes with a given crest value upon the calibration curve and if they have evaluated the effect of the highly conductive material in the field of the magnetizing coil.

The mechanical vibration is shown by figure 11 of the paper to have a pronounced effect in demagnetizing the unit magnets. This effect might be expected to vary depending upon the permanent material used and to be less for a high coercive force material such as Alnico than for lower coercive force material such as chrome steel. I should like to ask the authors if this effect has been evaluated for different permanent magnet materials and if they have found it to be important.

C. F. Wagner and **G. D. McCann:** As stated in the paper, each of the instruments described has certain advantages and also certain limitations. It appears to be generally agreed that the cathode-ray oscillograph is the best available means for measuring wave form. However, its cost sets a very definite limit to its applicability. In the fulchronograph the authors have attempted to develop an instrument that is simple, reliable, rugged, and cheap. It is capable of determining certain characteristics of lightning that are felt to be controlling in the design of protective equipment and at the same time it is sufficiently low in cost to be used in large enough quantities to provide statistical data regarding these characteristics.

It is recognized that the limit of analyzing ability of the fulchronograph does not extend to waves of duration less than 50 microseconds although it will register the presence of such waves in crest magnitude. However, from the standpoint of protection, a knowledge of the wave shape of surges of less duration than these are not of very great importance. The cathode ray oscillograph using a logarithmic sweep loses a certain portion of the record during the charging period of the sweep circuit. The fulchronograph is capable of identifying multiple strokes having very small time intervals without the loss of the dead portion characteristic of the cathode ray oscillograph circuits described to date.

Mr. Hagenguth is of the opinion that the high-speed fulchronograph will give a confusing picture of a discharge consisting of multiple strokes if the total discharge time is larger than one revolution of the wheel. This would undoubtedly be true if the slow-speed fulchronograph were not also used as is our practice in all important installations. In applying the information obtained from these investigations it is desirable to know the duration of the high current components, a time which is usually not greater than 500 microseconds. This interval covers only about three per cent of the periphery of the high-speed wheel. The likelihood of two components in different revolutions of the wheel falling within this same three per cent of the periphery is very small and of course the components may be much shorter than the three per cent assumed. As an example, one record obtained having a total duration of $^{1}/_{3}$ second contained 22 components and it was possible to distinguish each of these on the high-speed wheel. A record of such a large number of components is quite unusual. A record of fewer components should, in general, offer no confusing picture of the discharge. The low current components are of importance only as their duration is sufficiently long to measure on the slow-speed wheel.

Mr. Hagenguth also states that in their investigations with the stationary links they found that the link was easily affected by surges of opposite polarity and induction of nearby conductors. In the case of the fulchronograph these factors have been largely eliminated because of the rotation of the wheel, the shielding provided by the steel cover, and the greater frequency with which the fulchronographs, being smaller in number, can be and are inspected.

Mr. Goss raises the question of the wave shape of the calibrating current and the effect of neighboring conducting masses. Tests made on the magnetic material by means of chopped wave fronts as short as $^1/_2$ microsecond showed no eddy current effect. The supporting blocks for the coils are micarta so that aside from the laminations of the magnetic circuit of the high sensitive coil and the aluminum of the wheel there are no large conducting masses close to the links. The links are mounted in an aluminum disk $^1/_2$ inch thick and the disk is approximately $^1/_8$ inch from the end of the magnetizing coil. Tests made with different wave shapes during preliminary design of the instrument showed that the presence of the aluminum had no effect on the calibration. The mechanical vibration tests described in the paper were made only for the particular material which is being used.

Mr. Foust states that with the use of several links located at different distances from the conductor, records are provided "which discriminate definitely between single unidirectional surges and oscillatory or repeated surges". Perhaps he did not mean exactly what was said. Of course the demagnetizing effect of a current in the opposite direction following the original current permits an evaluation of the magnitude of the reversal in current but if the original current is followed by a repeated surge of the same polarity, the link will record only the magnitude of the greater. As stated previously, all fulchronograph installations are inspected either after every storm or at least once a week and the chance of obtaining more than one stroke on the wheels is very small.

Mr. Foust questions the justification of drawing a smooth line between the points on figures 13 and 14 of the paper. The time intervals between these points are 43 microseconds as the possibility of any of the points being obtained on successive revolutions of the high-speed wheel is eliminated by the record obtained on the slow-speed wheel. From the standpoint of the performance of protective equipment this interpretation of the tests is certainly satisfactory and it is highly improbable that successive impulses would occur at time intervals as small as 40 microseconds.

Mr. McMorris infers that the field record of figure 14 is intended to be representative of arrester discharge currents. This paper is confined to a description of the instruments developed for the recording of lightning surges. Figure 14 is merely illustrative of the type of record obtained.

The inability of the fulchronograph to record wave fronts has been mentioned in the paper and reiterated by several discussions. The surge-front recorder provides a means of measuring this property of the surge. It is recognized as stated by Mr. McMorris that the effect of a rather fast decay of current on the tail of the wave causes a reversal of current in the auxiliary coils of this instrument. Two magnetic links in different parts of the field of these coils serve not only to increase the recording range of the instrument but also detect the presence of such a reversal of polarity. Furthermore, from the readings of these two links, it is possible not only to determine the front of the wave but also the maximum rate of change of current on the tail of the wave. These considerations are based on the presence of only one component of surge current which can be verified by the simultaneous record obtained by the fulchronograph. For cases in which the effect of the tail of the wave may be neglected, the instrument will record the order of magnitude of the rate of change of current of that impulse in the train for which the rate of change is maximum.

The Output and Optimum Design of Permanent Magnets Subjected to Demagnetizing Forces

Discussion and authors' closure of paper 40-91 by A. J. Hornfeck and R. F. Edgar, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940, pages 1017–24.

H. T. Faus (General Electric Company, Lynn, Mass.): A fundamental reason for the difficulty of making accurate calculations of the performance of permanent magnets is that the effect of magnetic leakage is so difficult to estimate. In the case of magnets of irregular shape adjacent to iron, accurate calculations are almost impossible. The problem is analogous to that of an electrical circuit comprising metallic conductors immersed in salt water.

A relation which I have found useful in arriving at a first approximation of a design is that the air gap reluctance should equal the magnet reluctance. The reluctance of the magnet is estimated on the basis of the maximum permeability of the material from which it is made. In the case of a magnet of uniform cross section having an air gap area equal to the cross section of the magnet, the length of the magnet should equal the length of the air gap multiplied by the maximum permeability of the material. For Alnico I, this value is 12, for Alnico II, it is 10, and for chrome magnet steel, it is 100. This method provides only for the magnetomotive force to overcome the air-gap reluctance. Magnetomotive force to resist other demagnetizing forces must be provided by increasing the magnet length.

In many problems of magnet design, there is little incentive to attempt a detailed calculation of the performance, either because mechanical features limit the magnet to a particular size and shape, or because so little work is involved in determining the best proportions by cut-and-try methods. However, there is an increasing number of applications requiring more accurate methods of calculation, which makes this paper a very timely one.

A. J. Hornfeck: We wish to thank H. T. Faus for his pertinent remarks. Relations such as he suggests are very useful in the design of permanent magnets. It is very desirable, of course, to have such relations supplemented by theoretical considerations such as those outlined in our paper, so as to facilitate their application to structures of different shapes and sizes.

In planning this paper we considered devoting some part of it to magnetic-leakage calculations. However, such calculations were not included for fear of unduly lengthening the paper. They seem a proper subject for separate discussion. Magnetic leakage is obviously a function of the geometry of the structure. It is entirely possible that general methods of including leakage in design calculations without requiring complicated mathematical analysis or flux maps may be worked out for certain types of magnetic structures. Dimensional analysis and the theory of models should be particularly useful in this work.

It is believed that the continuing improvement of present alloys and the development of new alloys of even greater available energy will stimulate new uses for permanent magnets at an accelerated rate. Unless engineers and designers have reliable methods of calculating a permanent magnet for a particular application, considerable waste in the utilization of such materials will result. It is hoped that our paper will serve some purpose in enabling the engineer to understand the operation of a permanent magnet under a variety of conditions and to choose the best design for his own application. Also, we hope that further work will be stimulated in the field of permanent magnet theory and design.

Performance of Traveling Waves in Coils and Windings

Discussion and author's closure of paper 40-102 by Reinhold Rüdenberg, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940, pages 1031–40.

L. V. Bewley (General Electric Company, Pittsfield, Mass.): Professor Rüdenberg has presented an interesting analysis of traveling waves in windings, in which, by means of a Fourier integral, he represents the incident wave as an infinite series of sinusoidal components, and then determines from the differential equation in what fashion these sinusoidal waves can enter the winding. He finds that there is a critical (or cut-off) frequency beyond which an applied sinusoidal wave cannot penetrate the winding as a wave, but can nevertheless establish an exponential distribution which decays in place. This method of analysis, while little used by power engineers, is quite familiar to communication engineers in connection with filter theory (see, for example, chapter XI, volume II, of Guillemin's "Communication Networks").

Previous writers on the subject of transients in windings have, so far as I am aware, followed K. W. Wagner ("Progress of Waves in Windings", *Elecktrotechnik und Maschinenbau*, February 1915) in regarding the matter from the point of view of standing waves oscillating about a final, or steady-state condition, and dependent in magnitude on an initial condition (the method of "normal functions").

It is of interest to examine Dr. Rüdenberg's method in comparison with prevalent theories.

Does the New Method Confirm or Disprove Previous Conclusions?

The traveling-wave theory, as presented by Dr. Rüdenberg, does not disclose any new or unknown facts about transients in windings; nor does it lead to any different conclusions as to ways and means of suppressing or controlling these transients. Starting from a given differential equation, a permissible solution in terms of either traveling or standing waves must be equally valid, and as either method permits the calculation of any number of instantaneous space distributions, it is hardly to be expected that something will show up by one method which remained masked in the other. There will be, of course, a different "interpretation" of certain aspects, but the facts themselves must be identical.

Is the New Method Quicker?

If the *exact* solution of the differential equation in terms of traveling waves is compared with the exact solution in terms of standing waves, there is a distinct advantage in favor of the latter; because each harmonic space wave, vibrating at its own natural frequency, makes its contribution, and it is not necessary to include more than a dozen or so of these harmonics; whereas there are theoretically an infinite number of traveling wave components included in the Fourier integral specification of the incident wave, and the frequencies of these traveling wave components differ from each other by infinitesimal amounts. However, if we were to accept, tentatively, Dr. Rüdenberg's approximations (particularly the assumption that all sub-critical waves travel at the same velocity) then the traveling wave theory would present a marked advantage as a means of obtaining the instantaneous distributions. For then it would only be necessary to move a rigid distribution along the winding, at a constant velocity, and superimpose a decaying fixed distribution, in order to find the instantaneous distributions. This would be a particularly easy way to compute gradients. If this procedure were perfectly valid, it would also greatly facilitate the calculation of neutral transients; for if a winding acts like an ideal transmission line, except that it will not permit the passage of waves having a frequency higher than a certain critical value, then the whole of the theory of traveling waves on transmission lines would apply without modification.

It is interesting to note at this point that waves of arbitrary shape are taken care of by means of Duhamel's theorem if the solution is in terms of normal functions; but must be specified in terms of a Fourier integral if the traveling wave solution is given. This may or may not lead to greater complications.

Does the New Method Offer a Better Physical Picture?

It is difficult to make a choice in this respect, as each point of view has its advantages. The traveling wave theory explains the negative polarity loops near the neutral end as due to the progress of the "forerunners" in front of the traveling wave. The standing wave theory explains the same thing as due to the rapid reversal of the higher harmonics. The traveling wave theory explains distortion as due to a slight difference in velocity of its sinusoidal components. The standing wave theory explains distortion as due to a difference in frequency between its space harmonics. The traveling-wave theory explains the initial distribution as the superposition of its traveling wave (already partly protruding into the winding) and its decaying fixed exponential distribution. The other theory invokes the physical fact that the current cannot initially enter the inductive paths, and therefore the initial distribution is simply a matter of the capacitance network (this, I think, is a much simpler concept than that offered by the traveling wave theory). The traveling-wave theory makes no specific mention of a "final distribution" but supposes that such a final state will be realized following the subsidence of the traveling wave. (In his paper Dr. Rüdenberg states that the wave front of the traveling wave tends to flatten out indefinitely, on account of the difference in speed of the harmonic waves. If this were so, the final distribution in the winding would be zero. Nor must it be inferred that any such progressive flattening of the wave front is responsible for the realization of the actual final distribution—a straight line from zero at a grounded neutral to E volts at the line terminal. The only way in which the transients can possibly degenerate to the final distribution is through the agency of energy losses.) The traveling wave point of view presents, in my opinion, a much clearer picture of the gradient, as the slope of the advancing wave front is a direct measure of this gradient. The only way to find this gradient with standing waves is to compute it.

Is the New Method More Adaptable to Refinements?

Both Wagner's and Rüdenberg's theories are based on the assumption of a simple idealized winding without losses, without mutual coupling with other windings, and an infinite rectangular applied wave. Actually, however, the losses play a very important part in the transient behavior of a winding; being responsible for the realization of a final distribution, limiting the excessive gradients to the ends of the windings, and greatly reducing the envelope of oscillations. When resistance elements are introduced into the circuit, the differential equation is more complicated. Decrement factors and displacements are introduced into the solution for standing waves, and the harmonic oscillations are superimposed as before. Dr. Rüdenberg has not indicated how he would take losses into account in his theory. Of course each sinusoidal component wave will decay in magnitude, a relative displacement will be experienced, and finally the velocities of the component waves will be different. It may be that these distorting influences can be accounted for by reasonable approximations.

How Accurate Is the New Method?

Dr. Rüdenberg's solution is based on the identical differential equation used by K. W. Wagner, which is

$$\frac{\partial^2 e}{\partial x^2}-LC\frac{\partial^2 e}{\partial t^2}+LK\,\frac{\partial^4 e}{\partial x^2\partial t^2}=0$$

Wagner's equation assumes a uniform self inductance per unit length of winding, just as for a transmission line. Indeed, analytically Wagner's differential equation is the same as for a no-loss transmission line having axial capacitance in parallel with the inductance. It ignores the variation of the mutual inductance along the winding. Dr. Rüdenberg has endeavored to show that this variation is of little consequence; but his demonstration rests on the investigation of an interior section of an infinitely long winding.

Over twenty years ago, Blume and Boyajian, following an extensive experimental investigation of transformer transients by Weed, Blume, and others, concluded that the Wagner formula did not give sufficiently accurate results and that the trouble was in its failure to take into account properly the mutual flux leakages.

They then extended the analysis to more nearly include the mutual inductance (Blume and Boyajian, "Abnormal Voltages Within Transformers," AIEE TRANSACTIONS, volume 38, 1919). This differential equation is

$$\frac{\partial^4 e}{\partial x^4}+LC\,\frac{\partial^2 e}{\partial t^2}-LK\frac{\partial^4 e}{\partial x^2\partial t^2}=0$$

Now the solutions of both differential equations, in terms of standing waves, give the same initial and the same final distribution, and the same amplitudes for the space harmonics. But the natural frequencies of oscillation are quite different.

Thus, while Wagner's formula shows a ratio of harmonic frequencies (s = order of space harmonic)

$$\frac{f_s}{f_1}=s\sqrt{\frac{1+\pi^2/\alpha^2}{1+s^2\pi^2/\alpha^2}}$$

the Blume and Boyajian formula gives a ratio of

$$\frac{f_s}{f_1}=s^2\sqrt{\frac{1+\pi^2/\alpha^2}{1+s^2\pi^2/\alpha^2}}$$

in which $\alpha=\sqrt{C/K}$.

Thus the simplest test for the relative correctness of the two equations is to measure the harmonic frequencies. The most reliable way to do this is to apply a long sharp front wave and take oscillograms with the cathode-ray oscillograph. For a grounded neutral, an oscillogram taken at the midpoint of the winding will give the fundamental with a pronounced ripple, which is principally the third harmonic. This third harmonic is easily segregated. An oscillogram taken between the 25% and 75% points gives the second harmonic having the sixth harmonic as principle ripple (the fourth harmonic is zero at each of these points, and all odd harmonics—1, 3, 5, etc.—cancel out between these two points). Thus two oscillograms will yield four harmonics—1, 2, 3, 6.

Figure 1 of this discussion shows the tracings of oscillograms taken with the transient analyzer on a 3-phase 5,000-kva, 12,000-volt transformer with grounded neutral, and with the secondary either grounded at one or both terminals. This transformer had an α-factor of 11. The comparison between the frequency ratios as given by the two formulas and test is:

Space harmonic	1	2	3	4	5	6
Wagner	1	1.8	2.4	2.8	3.0	3.1
Blume and Boyajian	1	3.6	7.1	11.0	15.0	19.0
Test	1	3.3	7.2			13.0

These results are typical of those obtained over a 20-year period by many investigations on numerous transformers.

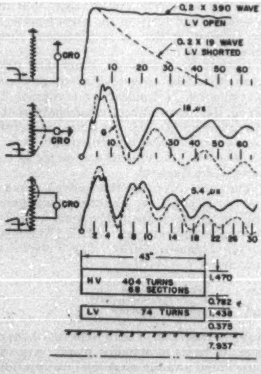

Figure 1

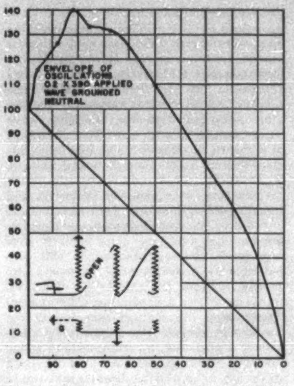

Figure 2

A further comparison is offered by the envelope of oscillations and the instantaneous distribution at different points along the winding. A typical example is given in figure 2 of this discussion. It will be noted that whereas Rüdenberg's theory specifies a maximum value of 118 per cent for the envelope of oscillations, and the extension of this crest far into the winding, the Blume and Boyajian theory gives a crest of 140 per cent confined to a short portion of the winding about 15 per cent in from the line end. Furthermore, the instantaneous distributions agree well with the Blume and Boyajian predictions.

L. F. Blume (General Electric Company, Pittsfield, Mass.): It is the rule rather than the exception that the solution of an engineering problem is greatly facilitated by finding the appropriate approximation. The validity of an approximation is determined solely by whether the reliability of the quantitative results obtained are good enough for the practical purposes concerned.

Dr. Rüdenberg, starting out with a theoretical analysis, develops, by making certain approximations, a simplified solution to the abnormal voltage transients within coils and windings, and reduces the transient to a wave of constant shape, traveling at a uniform velocity throughout the winding. This major simplification is brought about, first, by the neglect of a component of mutual inductance. Although the differential equations employed contain terms concerned with mutual inductance, these, in the course of the development become restricted, or so modified that the relation between the order of harmonic and its frequency becomes more nearly a constant value; so much so in fact that Dr. Rüdenberg feels justified in making the second approximation of assuming a linear relationship between them. By these approximations constant velocity is obtained for all the harmonics, and from this it follows that the penetration of the wave within the winding occurs without distortion. The interesting and very practical question is, do these several approximations introduce appreciable errors in the distribution of the maximum voltage stresses?

It is difficult for me to be sure that a correct quantitative solution can be arrived at by using the approximations which Dr. Rüdenberg employs. Transformer coils in a winding are physically so close together that the fluxes produced, and the voltages induced become much more of a community phenomena, rather than a self-induced phenomena, and it therefore seems that it is necessary to integrate the current in the turns, or coils, to obtain the flux at any point, and again to integrate the flux distribution to obtain the induced voltages. It would seem that the neglect of this fact would result in very appreciable errors.

The method which has been successfully used in America for many years (Blume-Boyajian paper "Abnormal Voltages Within Transformers," AIEE TRANSACTIONS, 1919, pages 577–614) in the predetermination of abnormal voltages within transformers, is mathematically equivalent to the one developed by Dr. Rüdenberg excepting for the treatment of mutual inductance, and I believe it represents more closely the phenomena under consideration. It uses exactly the same method which transformer designers have employed in the calculation of mutual inductance or the reactance volts of interleaved transformer windings, assuming that the much higher frequencies involved do not affect its validity. By this method a close correlation has been obtained between test and calculated values.

The results obtained by this method which differ from that obtained by Dr. Rüdenberg are:

1. There is no simple relation between wave length and frequency, and in consequence the wave undergoes a continual distortion of shape as it penetrates the winding.

2. There is no critical frequency, and therefore the distinction between a super and a subcritical frequency is not valid.

3. It is rather surprising that in spite of these wide divergencies, the quantitative values of voltage stress distribution are not so far apart. Dr. Rüdenberg's method gives somewhat smaller values of voltage stress to ground.

It is difficult to gauge the accuracy of the approximations which Dr. Rüdenberg uses by a theoretical discussion alone. An examination of a large amount of relevant test data is also essential, such as those presented by Mr. Bewley. This task, however, is made difficult as a general problem, owing to the fact that there are so many types of windings to be considered. Mathematical treatments assume a rather simple arrangement of windings and coils, as compared with what often exists in practice, and for this reason test results must be interpreted with considerable caution. So many diverse arrangements of coils make up transformer windings that it is impossible to say that any one method of analysis is sufficient for all practical purposes.

A. Boyajian (General Electric Company, Pittsfield, Mass.): I welcome this paper by Dr. Rüdenberg as a valuable contribution to the literature of transients in transformers, presenting, as it does, a point of view on which relatively little is to be found in the Institute records. I welcome it even though its point of view diverges a great deal from mine: the literature of no subject may be considered adequate until the major alternative points of view have been adequately covered in it.

The prevailing point of view on the oscillations of a transformer winding is that of standing waves, while Dr. Rüdenberg's view is that of traveling waves.

It might appear at first blush that the traveling wave concept must be the simplest, the most direct, physical way of considering the transformer phenomena resulting from disturbances at the terminals of a winding: thus, one might think that a wave traveling on a transmission line reaches the terminal of a winding, part of the wave enters the winding, travels along the thousands of turns of the winding until it reaches the neutral or the other end of the winding; there, part of the wave is reflected, etc., etc.; that this to-and-fro travel of the wave within the winding constitutes the transformer phenomena; and that it would then be for the mathematician to calculate the velocity of the wave along the turns of the winding, whether or not the wave undergoes any change of shape.

We may concede that this point of view can be developed mathematically, but we may well consider what price it demands.

According to the traveling-wave conception, current and voltage waves travel together, and accordingly we will have to assume that when the entering wave has reached in course of time the mid-point of the winding, the second half of the winding is as yet entirely free from current and voltage, and knows nothing about them. But how shall we fit this with our physical conception that a sudden current in the first turn must induce a voltage in the last turn by mutual induction, without requiring the current to travel to the last turn? Mutual induction between turns and windings is the most characteristic feature of a transformer, and a mathematics which does not explicitly recognize this does some violence to our physical picture, even if it should in some cases give correct final numerical values to the stresses.

Now, it may require appreciable time for the current in the first turn to travel the miles of intervening conductor to reach the last turn according to the traveling-wave theory, but the flux generated by the sudden current in the first turn need travel only 5 to 10 feet to cut clear across all the turns of the winding and induce voltages in them before any current has reached them. Thus, the simple traveling-wave conception does not fit very well a winding with closely coupled turns.

The mathematical formulation attempted in the earlier part of Professor Rüdenberg's paper, which could be said to be based on traveling waves and which would give correct results is one which involves the Fourier integral and implies a series of waves of infinite wave length coming from both sides of the winding, not merely from the point of disturbance: a type of waves which are altogether foreign to ordinary conceptions of traveling waves and are of no help to the average engineer in forming a physical picture of the phenomena.

In the latter part of his paper, Dr. Rüdenberg does describe the progress of a slowly traveling wave in a winding, a wave similar to the common conception of a traveling wave, but this result is secured on the rather over-simplified assumption that up to a certain critical harmonic all harmonic waves travel with the same velocity in the winding, assuming that the inductance of any portion of the winding is directly proportional to its length, just like a transmission line, and that the mutual effect of one portion on the other can be ignored.

In contrast with this, the standing wave conception assumes that the dielectric field, in which the winding is immersed, establishes a voltage distribution throughout the winding at the very first instant of the impact of the disturbance, and imposes thereby a certain pattern of standing harmonic waves on the winding at the instant of the impact of the wave. The winding oscillates to each such space harmonic at whatever frequency may be associated with that space harmonic in that particular winding. Thus, successful application of this theory in a relatively simple manner does not place any restriction whatever on the frequencies of various space harmonics and therefore lends itself not only to a clearer understanding of the phenomena consistent with our standard notions, but also to more precise predictions, as various experimenters have verified.

Finally, the standing wave conception has justified itself by that very important test, namely, ability to point out how the apparatus can be improved. This has been accomplished by the development of the shielded nonresonating windings and the sinusoidally shielded windings.

J. F. Calvert (Northwestern Technological Institute, Evanston, Ill.): Doctor Rüdenberg has presented a very interesting mathematical concept, and the appearance of the calculated results show the same general type of voltage variations as are obtained by test. The question naturally arises can this analysis be applied to specific machines in the design stage so as to predetermine all the oscillations and their frequencies? If this can be done, much more can be calculated concerning the higher harmonic oscillations than has been possible heretofore, especially with regard to rotating machinery.

This brings up the very vital question of the calculation of the machine constants—particularly the inductance calculations where iron is present. So much time and effort have been expended upon the calculation of the 60-cycle inductance constants, that it would be very desirable if those required under impulse conditions could in any way be correlated with some of the 60-cycle values. However, whether or not this can be done, clearly defined methods are needed, and in view of the marked difference in the physical proportions and iron and copper arrangements of inductance coils, core type transformers, shell type transformers, and rotating a-c machines, I believe that the method of treatment for each type should be made available by the author for impulse and switching problems.

From that part of a paragraph which appears directly below equation 7 of the paper, one would judge that the author assumes the iron to act almost as a perfect damper or electric shield rather than as a magnetic material. No flux penetration into the iron seems to be the assumption there. This, I am inclined to believe is not always the correct viewpoint to take, and I would be interested to see the evidence available—theoretical or experimental—for the range of frequencies met with in the harmonic analysis of lighting and switching surges. In the tests which were reported in the paper "Protecting Machines From Line Surges", ELECTRICAL ENGINEERING, 1934, pages 139–46 (reference 12 of the paper) we found one or two things which bear on this point. When a $1^1/_2$x40-microsecond wave was applied across the armature winding of a synchronous machine, then despite the presence of a squirrel cage damper winding, an electromagnetically (and not electrostatically) induced voltage of very appreciable magnitude appeared in the field winding of that machine. Also the impedance function (impedance-frequency curve) for the armature winding was affected not only by the terminal connections of the armature winding, but also by the number of stator parallels, rotor position, etc.

To summarize: (1) I hope the author will indicate in his closing discussion exactly what assumptions he makes for the calculation of the inductance and capacitance constants under impulse conditions for reactors, core-type transformers, shell-type transformers, high-speed turbine generators, salient-pole synchronous machines with and without damper windings, and for induction motors. (2) I hope that the author will show in more detail his calculated and his test data on a representative group of specific machines on which his work has been applied. (3) I hope that the author will submit a subsequent paper, to show in some detail how the author's constants should be computed for these cases.

I believe that if the method of calculation of these constants is presented and the results of check tests up to and including the higher harmonics are given, then the usefulness of this very able theoretical analysis will be greatly enhanced for the electrical machine designer who, I believe, is the man most to be benefited immediately by this particular type of investigation.

V. Karapetoff (Cornell University, Emeritus): Dr. Rüdenberg's paper is both valuable and stimulating for further research. The remarks below are intended more as an indication of the next natural steps, rather than a criticism of his methods or results.

1. With discrete coils, the problem seems to require a combination of differential equations within each coil with finite-difference equations for the whole winding consisting of separate coils.

2. The assumption of constant parameters for the partial inductances and capacitances seems to be too far from the actuality. Equations with variable parameters should be tried, or else the elementary magnetic linkages and electrostatic fluxes used directly in the equations. This will constitute a closer approach to the actual phenomena.

3. With simpler wave phenomena, a classification into standing and traveling waves has been fairly adequate, but for the transients analyzed in the paper one or more advanced, more general concepts are desirable. Thus, as an analogue, with the advent of phase or frequency modulation in radio the concepts of sine waves and their harmonics has become inadequate and the radio man will have to learn to think in terms of Bessel functions. With ultrahigh-frequency oscillators, shapes of bodies will probably be used requiring spherical har-

monics and other specialized functions. This tendency toward higher and higher mathematical concepts and operations seems to be irresistible and we simply shall have to have a specialized group of experts to handle these problems, or at least to know how to translate them into the language of pure mathematicians.

4. Much progress in pure mathematics has been due to the stimulus of astronomy, physics, biology, etc. Even though the equations in the form proposed above may seem at first unsolvable they will signalize a distinct progress in our account of the phenomena involved. Later they will undoubtedly stimulate practical and theoretical mathematicians to develop approximate solutions: analytical, graphical, electrical, or mechanical. I should rather see an expert mathematician simplify an originally unsolvable equation than an engineer make an inadequate simplification from the start.

5. The problem of waves and oscillations in coils and in windings is an excellent illustration of the need for closer co-operation among pure mathematicians, applied mathematicians, mathematical physicists, experimental physicists, mathematically inclined engineers, experimental engineers, and designers, all working toward the same end and none capable of solving the problem alone. It is for the executives of industrial concerns and of institutions of higher learning to see to it that such a co-operation be established on an efficient and permanent basis.

R. Rüdenberg: A large part of the items brought up in the discussion pro and contra the analysis chosen in my paper can be arranged in three groups, and I believe they will be clarified to some extent by the following remarks:

1. Standing or Traveling Waves

Although it is certain since the investigations of D'Alembert, Bernoulli, and Dirichlet more than a hundred years ago that solutions by standing and by traveling waves are equivalent and can be mathematically transferred into each other, their physical conception is different and therefore it is sometimes convenient to clarify technical questions with the one and sometimes with the other form of analysis. I feel that the experimental result expressed by figure 8 of the paper concerning the velocity of propagation of surges along wires of transformers approaching the Maxwellian velocity of light, can be more easily understood by the conception of traveling waves, and brings these phenomena in closer contact with similar fields of science.

The fact that many experimental results of other investigators also agree well with the wave propagation shown in figures 16 and 17 of the paper may be proved by the accompanying figures 3 and 4, of the former representing the successive voltage distributions over a transformer with grounded neutral (taken from reference 14 of the paper), the latter showing the distribution over an alternator with insulated neutral (taken from reference 12 of the paper). Magnitude and position of the maximum voltage, mentioned by Professor Bewley, depend upon the ratio of head length and winding length and in figure 3 are closer to figure 16 of the paper than in Bewley's figure. If waves such as those of figures 16 and 17 travel repeatedly over the winding to and fro with increasingly flattening front, the final distribution does not approach zero, as Professor Bewley has guessed, but is given by the average of the initial traveling wave shapes over a period and coincides with the final distribution usually assumed.

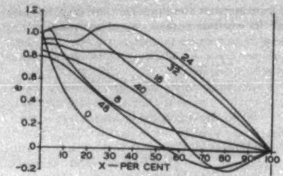

Figure 3. Consecutive distributions of voltage measured in a transformer with grounded neutral

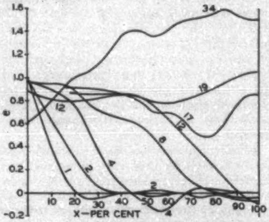

Figure 4. Consecutive distributions of voltage measured in an alternator with insulated neutral

In principle there exists a difference of much deeper significance in the two conceptions: Traveling waves can be developed with respect to the frequencies contained within the wave, forming a continuous spectral distribution, or a Fourier integral, which is well known for most of the interesting wave shapes. This spectral distribution, and therefore the actual wave shape, can be modified substantially only by refraction of the wave at the terminals of the winding, due to the difference of the interior characteristics of the winding and the exterior characteristics of the network. Within the winding the spectrum and the wave shape remain substantially unchanged, however, and suffer only a slight distortion by dispersion, caused by lack of constancy of the velocity. Therefore we can consider separately the propagation of the surge within the winding and the reflection and refraction at the ends. Thus the external network influences only alterations of the wave shape by refraction but not by propagation and reflection. Magnitude and distribution of the natural frequencies play no part in this analysis and therefore are entirely irrelevant in the performance of traveling waves.

Standing waves, on the other hand, enter if the phenomena are developed with respect to the natural frequencies of the winding. These frequencies, however, depend not only on the winding proper, but quite as much on the external system connected to the terminals. Thus they are by no means constant parameters of the winding. All standing waves, which give by summation the voltage along the winding, are dependent therefore on the characteristics both of the winding and of the exterior network. This interaction, which refers to amplitudes as well as to frequencies and wave lengths of standing waves, is in contrast to the analysis by traveling waves, where the internal and external characteristics enter separately and, in my opinion, this facilitates the survey.

These differences may be responsible for the fact that the magnitude of the internal stress of the interior coils can be computed much more easily by the conception of traveling waves, giving, moreover, a simple expression for the highly important duration of the internal stress.

The differences of the two conceptions are also evident in the method for obtaining the initial and the final distribution of voltage over the winding. With traveling waves these distributions result from the solution of the differential equations for any form of incident wave without further hypothesis. With standing waves the distributions are derived only with additional static considerations, foreign to the differential equation, and then are used as boundary conditions for determining the constants of integration. With this procedure, the standing-wave conception naturally gives identical initial distributions and identical final distributions for every differential equation.

There is no difficulty in applying the traveling-wave conception to the Blume-Boyajian differential equation, given by Professor Bewley in his second equation. The correlation of velocity and frequency then gives, using the statement of equation 17 of the paper but with Bewley's notation,

$$\omega = -\frac{v^2}{\sqrt{\frac{1}{LC}-\frac{K}{C}v^2}} \tag{a}$$

while Bewley's first equation, due to Wagner and which I prefer, gives

$$\omega = \sqrt{\frac{C}{K}\left(\frac{1}{LC}-v^2\right)} \tag{b}$$

corresponding to equation 26 of the paper. The two relations are represented in the accompanying figures 5 and 6 for different capacitive ratios K/C. We see that the traveling velocity of harmonic waves along wires of transformers, according to Wagner's and my equation, starts with the velocity of light and decreases steadily to zero with in-

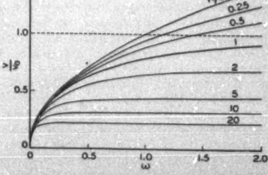

Figure 5. Wave velocity against frequency according to the Blume-Boyajian differential equation

creasing frequency. But according to the Blume-Boyajian equation the wave velocity starts with zero and increases steadily up to a limiting value.

Experimentally, my own tests, some of them given in figure 7 of the paper, are decisively in favor of the accompanying figure 6. Theoretically, sudden impacts, according to Fourier's theorem, always comprise the range of the high frequencies up to $\omega=\infty$, and since for high frequencies the velocity in the accompanying figure 7 becomes constant, there is the cogent conclusion that, according to the Blume-Boyajian differential equation, steep impacts should propagate without dispersion and thus without distortion throughout the winding. This seems in contrast to expectation and to experimental results. This discrepancy, I dare say, was one of my reasons for a revival of this old but not obsolete problem. A second reason was to show how the traveling-wave conception, so fruitful in other fields, works out with closely-wound windings.

2. Inductive Interaction

In the discussion was said rightly that in equation 10 of the paper there is neglected a remainder of the mutual inductance of all turns and that the differential equation is derived for a relatively long coil. It is possible, however, to state the conditions under which these approximations are valid.

For oscillations of any frequency forming harmonic waves along the winding, the current within the elements of the winding is

$$i_m=I\cos(\varphi+\alpha mw)\cdot\epsilon^{j\omega t} \qquad \text{(c)}$$

φ being an arbitrary angle, α the wave density in space, and m the distance between the elements. For $m=0$ we have the current within the element n under consideration

$$i_n=I\cos\varphi\cdot\epsilon^{j\omega t} \qquad \text{(d)}$$

The second difference of the currents in the elements with distances $\pm m$ from the element n considered becomes, therefore,

$$\begin{aligned}\Delta^2 i_{n,m}&=i_m-2i_n+i_{-m}\\&=2I\cos\varphi\cdot(\cos\alpha mw-1)\cdot\epsilon^{j\omega t}\end{aligned} \qquad \text{(e)}$$

The mutual inductance μ, decreasing with the distance between the elements, can always be approximated within the most important range by an exponential expression

$$\mu_{n,m}=\mu_n\epsilon^{-\eth mw} \qquad \text{(f)}$$

in which μ_n is the maximum value and $\eth$ the rate of decrease.

For evaluating the summations in equation 10 of the paper, we replace them by integrals since the number of elements is large. The first member of the right-hand side then becomes, computed for both sides of a long winding, including the self-inductance, and with abbreviation $x=mw$,

$$i_n\Sigma\mu_{n,m}w=2i_n\mu_n\int_0^\infty\epsilon^{-\eth x}dx=2i_n\frac{\mu_n}{\eth} \qquad \text{(g)}$$

For the second member we obtain, with use of equations *e* and *f*,

$$\begin{aligned}\Sigma\mu_{n,m}w\,\Delta^2 i_{n,m}&=2I\cos\varphi\cdot\mu_n\epsilon^{-j\omega t}\times\\&\qquad\int_0^\infty\epsilon^{-\eth x}(\cos\alpha x-1)dx\\&=2i_n\mu_n\frac{-\alpha^2}{\eth(\eth^2+\alpha^2)}\end{aligned} \qquad \text{(h)}$$

The ratio of the two members of equation 10, which was neglected in the fundamental equation 12 of the paper, is, therefore,

$$\eth=-\frac{\alpha^2}{\eth^2+\alpha^2} \qquad \text{(i)}$$

This ratio is always negative, depends only on wave density α and on attenuation $\eth$ of the mutual inductance, and forms a correction factor to the first member. With regard to the change in space, $\eth$ is the reciprocal of the space constant of the mutual inductance, while α is the reciprocal of the wave length $\lambda/2\pi$ of the oscillation.

The mutual inductance between two equal circular coaxial rings in air is given by the accompanying figure 7 as function of the ratio of distance y and radius r. The average subtangent of this curve forms the space constant of an equivalent exponential curve, and within the important range the subtangent is smaller than the turn radius r.

The mutual inductance of two turns or coils due to the air flux in the region beside a screening core is given by a well-known picture, as in the accompanying figure 8, with two conductors over a perfectly conducting ground. This figure shows the mutual inductance as function of the ratio of distance y and height b. The average subtangent of this curve within the most important range is at maximum of the order of the height, which equals in our problem the insulating distance b of the turns or coils from the secondary or the ground.

Therefore the correction factor ρ in equation *i* is small for all oscillations the wave length λ of which is substantially larger than the space constant, just determined for two important cases. This gives the condition

$$\lambda>2\pi r \quad\text{or}\quad \lambda>2\pi b \qquad \text{(j)}$$

Thus we see that for reactor or transformer windings the fundamental equations 11 and 12 of the paper are valid down to short waves, the length of which in axial direction approaches the order of the radius for air-core windings, or of the insulating distance to the secondary or to the iron for steel-core windings.

For machine windings the mutual inductance decreases rapidly according to the slot pitch, and therefore the limit of wave length corresponds here to 2π times this pitch.

We have considered preponderantly the air flux of the winding because the core flux in steel sheets has only moderate significance for the rapid variations here in question and may be considered, if necessary, by an addition to the distance b in the accompanying figure 8. In transformers or machines under actual operating conditions, the core flux vanishes almost completely by the action of the secondary windings. Only with special terminal conditions may a core flux common to all turns be produced. However, it will then be so large compared with the air flux that it forms an extra oscillation of its own with low frequency, which scarcely interferes with the dangerous rapid oscillations released by the action of travelling waves. Such a relatively slow oscillation can be treated separately in a quasi-stationary manner using lumped constants of the winding.

We see therefore that equation 12 of the paper with the definition in equation 11 comprises completely the mutual inductance of all turns or coils under the conditions just derived.

The influence of the ends of the winding, and of the core flux as well, can be further clarified by the accompanying figures 9 and 10, in which the primary and secondary windings of a transformer are shown within the gap between steel core and tank. In figure 9 the magnetic lines of force are drawn radially through the primary coil of finite length. The magnetic lines of force pass undisturbed across the secondary winding and end perpendicularly at the iron surfaces through which they close themselves unrestrained by eddy-current formation. This

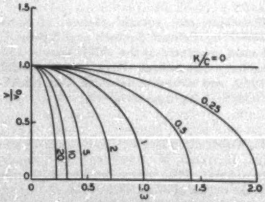

Figure 6. Wave velocity against frequency according to the paper

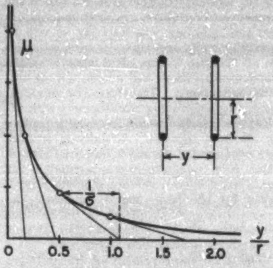

Figure 7. Mutual inductance of circular rings dependent on their axial distance

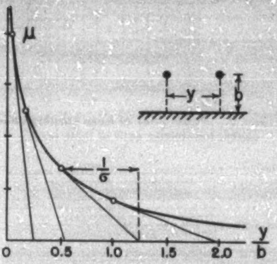

Figure 8. Mutual inductance of turns around a screening core dependent on their axial distance

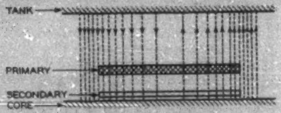

Figure 9. Radial lines of force crossing concentric transformer coils of finite length

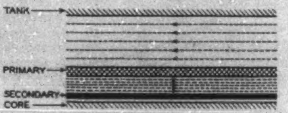

Figure 10. Axial lines of force flowing parallel to long concentric transformer coils

picture corresponds to the Blume-Boyajian equation. In figure 10 the lines of force are drawn axially to the coils which are long compared with their distance of insulation. Eddy-current formation in the iron or secondary currents in the closed low-voltage winding shield the core completely. This picture approaches the conceptions for the differential equation of this paper.

If the secondary is open and the coils are short axially, figure 9 may give an adequate picture for low natural frequencies. If the secondary is closed, either directly or through a low impedance, and if the insulating distance between the windings is small compared with their axial length, figure 10 will be nearer to reality. In some cases a superposition of both assumptions concerning the lines of force may be useful and it might be a rewarding task to pursue such a problem further. If the low-voltage winding under operating conditions is near the short-circuit state for the wave frequencies here in question, as is the fact with most larger transformers, it does not seem probable that turns or coils lying far apart from each other have a mutual inductance considerably larger than indicated by figure 7 or 8, as is assumed by Mr. Boyajian. The core flux is then suppressed, as in figure 10, and the effective l is constant and is proved, I think, for the first time in this paper, to be given directly by the regular self-inductance of the entire primary winding under operating conditions.

If, on the other hand, the closing impedance of the secondary is so high and the frequency so low that in spite of eddy-current screening a considerable core flux develops, closing itself either through the yoke or only locally as in figure 9, a substantial dependence of self-inductance on frequency will occur, due to the varying depth of penetration of the flux into the steel sheets. This must lead to a relative decrease in the value of the lower natural frequencies. The differences in the spectral distribution found by various experimental investigators may possibly find their explanation in these facts.

3. Capacitive Interaction

It is certainly remarkable that the various assumptions about the flow of the magnetic lines of force, or about the inductive coupling of each and every turn, do not influence considerably the results of the different theories with respect to the internal stress in the winding. The reason for this is the agreement of all investigators about the capacitive coupling, qualitatively as well as quantitatively, and that the internal stress in all theories is dependent mainly on the capacitive relations and not on self- or mutual inductance. Therefore the practical means for improving transformers with regard to surges are nearly identical according to all these theories. Actually, a striking success from increasing the internal capacitance k of transformers was secured as far back as 1914 (see reference 1 of the paper), when for the first time I published the mechanism and the effects of the internal interaction in coil-windings without, at that time, giving details of the calculation.

The great improvement of transformers by external shielding, as developed by Messrs. Blume, Boyajian, Bewley, and their collaborators, is based materially on static considerations of capacitive networks without regard to waves, be they standing or traveling, or to magnetic interaction of any character. Thus the present practical consequences are not influenced by preferring either one or the other theory. However, knowledge of the more detailed phenomena will be increased by viewing them from different angles and this may lead to further improvements.

4. Additional Queries

The discrepancy about the lowest natural frequency, mentioned by Professor Bewley, which appears when comparing the theories as well as the experiments of different investigators, certainly needs further elucidation. Although these slowest oscillations are important in other directions, it is well to have in mind that they play practically no part in the internal stress of the winding, which is preponderantly affected by the higher frequencies. In some cases terminal capacitance may add to the lowering of the fundamental frequency, due to the bushing or to the first coil side opposite a ground ring or to the surge generator during testing. Such extra capacitance can even produce a low frequency oscillation of its own.

The effect of energy losses in windings, which Professor Bewley mentioned, can be treated in the traveling-wave theory in the same manner as energy losses in smooth lines are usually treated. They result in a further flattening of the wave front which adds to that caused by internal capacitance. With regard to the remarks by Mr. Blume and Mr. Boyajian, I think that the last section of my paper shows that I never supposed the velocity of all harmonic travelling waves to be constant. The flattening of the wave front is derived from the variation in velocity of the partial waves. However, the entire group of waves proceeds with fairly constant velocity, though its shape is slightly distorted. I think, therefore, that the advantages which Professor Bewley tentatively credits to the travelling-wave theory really exist.

It is relatively simple to derive from the conceptions of the paper numerical values of the surge constants for various types of windings, as Professor Calvert suggests. This will be dealt with in a subsequent publication. Furthermore, the problem of the different natural frequencies of a winding under various conditions of operation, such as star or delta connection, isolated or grounded state at one or more poles, first or last interruption at the grounded pole by the circuit breaker, can easily be treated in the light of the traveling-wave conception.[a]

The problem of discrete elements, mentioned by Professor Karapetoff, can be solved rigorously in the manner suggested by him. It can be extended to the interaction of different groups of elements such as turns, layers, and coils in real windings.[b] As long as the magnetic fields are preponderantly in air, the use of constant parameters has its merits. Then it is possible to solve the problem of the inductive interaction of all turns of a uniform coil by the powerful method of Maxwell's equation.[c] However, where the iron part of the field is deciding, and eddy-current formation plays a part, Professor Karapetoff's suggestion of variable parameters is certainly important, although the calculations may become complicated.

The comprehensive theoretical solution of our problem has been attempted repeatedly by mathematicians.[16,d,e,f] However, in spite of the application of very powerful methods of analysis, such as integral equations and the like, no practical results have been obtained thus far. My impression is that comprehensive experimental investigations on different types of coils, windings, and transformers, such as are now under way at the Pittsfield Works of the General Electric Company as well as at the Graduate School of Engineering of Harvard University, may be helpful in advancing our knowledge of the questions still open.

References

a. Natural Frequencies of Three Phase Windings, **R. Rüdenberg.** Scheduled for publication in the *Journal* of the Franklin Institute.

b. Electric Oscillations and Surges in Subdivided Windings, **R. Rüdenberg.** *Journal of Applied Physics*, September 1940.

c. Electromagnetic Waves in Transformer Coils Treated by Maxwell's Equations, **R. Rüdenberg.** Scheduled for publication in the *Journal of Applied Physics*.

d. Berechnung der Eigenschwingungen einlagiger Spulen, **W. Lenz.** *Ann. Phys.*, volume 43, 1914, page 749.

e. Das elektromagnetische Verhalten der einlagigen Zylinderspule, **W. Steidinger.** *Archiv für Elektrotechnik*, XIII band, 1924, page 237.

f. Über das Eindringen von Wanderwellen in Wicklungen, **E. Hallen.** *Archiv für Elektrotechnik*, XXXII band, 1938, page 515.

Effective Resistance to Alternating Currents of Multilayer Windings

Discussion and authors' closure of paper 40-99 by Edward Bennett and Sidney C. Larson, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940, pages 1010–17.

E. Weber (Polytechnic Institute of Brooklyn, Brooklyn, N. Y.): The most important result of the very valuable contribution of the authors is their figure 4 which demonstrates the need for careful design of windings, particularly at higher frequencies. As shown in one example, an increase of copper thickness, although decreasing the d-c resistance, increases the a-c effective resistance considerably more than proportionally, so that instead of obtaining lower losses with more copper, one thus obtains very much higher losses with the attending problem of heat dissipation. With the data of this paper available, it is now possible to design strip copper windings for higher frequencies which give the lowest losses with a minimum of copper weight; that is, the most economical winding for each individual case.

Although the authors mention the additional edge effect due to the finite width of the copper strips, it would be interesting to know the limitations of the present formulas, and particularly the relative values of width and total coil thickness for which the edge effect might become appreciable. My second question is closely related to the first one and concerns the distribution of the magnetic field within and between copper sheets. The assumption of parallel field lines is a rather rough approximation and it will be interesting to have an indication of how the current distribution might be affected by a different assumption concerning the magnetic-field distribution. One might have the possibility of superimposing upon the case considered by the authors, the effect of a transverse magnetic field.

Finally, have the authors tried to obtain a solution in the case of parallel combination of n-laminae without the rather difficult ideal transposition? In such a case the current would distribute nonuniformly over the n-laminae and a very much higher resistance increase would take place. It might be well to emphasize that parallel subdivision of large conductors would not help at all in reducing the total a-c losses.

Edward Bennett: Several questions have been raised in Professor Weber's discussion. The first may be put in this specific form: Over what proportion of the width of a layer at its two ends does the "edge effect" cause the loss to substantially exceed the loss as computed from the equations for the mid-portions of the layer? We have not attempted a mathematical treatment of the edge effect for a coil of any dimensions. From general considerations and from experimental observations of the heating, we know that at the ends of the layers, the current densities may be double those near the mid-portions. In figure 2 the distance, D, (not shown on figure 2) back from the ends of the layers over which the current densities substantially exceed those near the mid-portion may be expressed in the form $D=K(MM)$. In this expression (MM) (as shown on figure 2) is the separation between the outer layers which carry currents in opposite directions. My judgment would be to assign the value 2 to the factor K. It should be borne in mind that our equations are derived for the case in which the width of the layer (W, in figure 2) is many times (say 20 times) the distance MM. These statements should also serve as a response to the second question raised by Professor Weber.

The third question is: What is the solution for an n-layered winding with the n layers all in parallel, but with no transpositions? An n-layered solenoid as in figure 2, or an n-layered primary or secondary as in figure 1, each layer of thickness b, and all layers connected in parallel and with no transpositions, will have the same properties as a single layer winding having the thickness nb.

An Extension of the Method of Symmetrical Components Using Ladder Networks

Discussion of paper 40-98 by Waldo V. Lyon, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940, pages 1025–30.

C. Concordia (General Electric Company, Schenectady, N. Y.): In connection with the method of analysis given in this paper it may be of interest to recall a somewhat similar method presented a few years ago ("Overvoltages Caused by Unbalanced Short Circuits," by Edith Clarke, C. Concordia, and C. N. Weygandt, AIEE TRANSACTIONS, 1938, volume 57, pages 453–66; see appendix B). This previous method (see latter part of appendix B of the paper referred to) is applicable to the determination of harmonic voltages and currents of an unsymmetrical system containing any number of machines having unsymmetrical rotors, and has been used for the solution of a problem involving two such machines supplying a system with an unbalanced short circuit.

The more general, but related, problem of the complete solution of the circuit equations to obtain both transient and steady state currents and voltages is also treated in the first section of appendix B of the 1938 paper previously referred to.

Transient Starting Torques in Induction Motors

Discussion and authors' closure of paper 40-100 by A. M. Wahl and L. A. Kilgore, presented at the AIEE summer convention, Swampscott, Mass., June 24–28, 1940, and published in AIEE TRANSACTIONS, 1940 (November section) pages 603–07.

S. B. Crary (General Electric Company, Schenectady, N. Y.): As stated by the authors the derivation of the equations for electrical torque were simplified by making certain assumptions in order to avoid "cumbersome" expressions. As these assumptions necessarily limit somewhat the generality of the results, it may be in order to indicate a more complete solution for the case of simultaneous application of the three-phase voltages but which still can be written in compact form.

From the equations of Park's "Synchronous Machines—I" for the conditions of a symmetrical stator and rotor, with no initial currents or voltages, the machine at standstill and with suddenly applied three-phase voltages, that is, $x_d(p)=x_q(p)$, $p\theta=0$, $e_d=-e\sin(t-\delta_0)1$, $e_q=e\cos(t-\delta_0)1$

$$\left.\begin{aligned} e_d&=p\psi_d-ri_d\\ e_q&=p\psi_q-ri_q \end{aligned}\right\} \quad (1)$$

$$\left.\begin{aligned} \psi_d&=-x_d(p)i_d\\ \psi_q&=-x_q(p)i_q \end{aligned}\right\} \quad (2)$$

$$T=i_q\psi_d-i_d\psi_q \quad (3)$$

where

$$x_d(p)=\frac{x_d'T_0p+x_d}{T_0p+1}$$

Solving the above equations for the torque,

$$T=-\frac{e^2(x_d-x_d')}{x_d'^2T_0(m^2+1)(n^2+1)}\Big[(1+\epsilon^{-mt}\epsilon^{-nt})-(\epsilon^{-mt}+\epsilon^{-nt})\cos t-\frac{mn+1}{n-m}\times(\epsilon^{-mt}-\epsilon^{-nt})\sin t\Big] \quad (4)$$

where

$$m=\frac{1}{2x_d'T_0}\left[x_d+T_0r-\sqrt{(x_d+T_0r)^2-4T_0rx_d'}\right]$$

$$n=\frac{1}{2x_d'T_0}\left[x_d+T_0r+\sqrt{(x_d+T_0r)^2-4T_0rx_d'}\right]$$

Equation 5 can be written with the same notation as that used in the paper, or

$$T=T_s\Big[(1+\epsilon^{-\lambda_1t}\epsilon^{-\lambda_2t})-(\epsilon^{-\lambda_1t}+\epsilon^{-\lambda_2t})\times\cos\omega t-\frac{(\lambda_1\lambda_2+\omega^2)}{\omega(\lambda_2-\lambda_1)}(\epsilon^{-\lambda_1t}-\epsilon^{-\lambda_2t})\sin\omega t\Big] \quad (5)$$

where

$$\lambda_1=\frac{1}{2(L_1L_2-M^2)}\left[L_1r_2+L_2r_1-\sqrt{(L_1r_2-L_2r_1)^2+4r_1r_2M^2}\right]$$

$$\lambda_2=\frac{1}{2(L_1L_2-M^2)}\left[L_1r_2+L_2r_1+\sqrt{(L_1r_2-L_2r_1)^2+4r_1r_2M^2}\right]$$

The above equation has the term $\epsilon^{-\lambda_1t}\epsilon^{-\lambda_2t}$ instead of $\epsilon^{-\lambda_2t}$ as given in the paper and more complete expressions for the decrement factors. On the basis of the assumption made in the paper that the magnetizing reactance is large compared to the leakage reactances, λ_1 is small relative to λ_2 and the term

$$\frac{\lambda_1\lambda_2+\omega^2}{\omega(\lambda_2-\lambda_1)}\cong\frac{\omega}{\lambda_2}\cong\frac{x_1+x_2}{r_1+r_2}$$

Also the term

$$\epsilon^{-\lambda_1t}\ \epsilon^{-\lambda_2t}\cong\epsilon^{-\lambda_2t}$$

For these approximations equation 5 of this discussion reduces to equation 11 of the paper, that is,

$$T_1=T_s\left[1+\epsilon^{-\lambda_2t}-\frac{\epsilon^{-\lambda_2t}\cos(\omega t+\phi)+\epsilon^{-\lambda_1t}\cos(\omega t-\phi)}{\cos\phi}\right]$$

R. Rüdenberg (Harvard University, Cambridge, Mass.): May I ask Mr. Kilgore whether he found that the doubling of the starting torque by action of the transients was advantageous for overcoming the considerable friction at rest which prevails with many motors, if one includes their machinery of supply?

A. M. Wahl and L. A. Kilgore: The authors agree with Mr. Crary that the assumption made in the paper that the magnetizing reactance is large compared to the leakage reactance, tends to limit to some extent the generality of the results. In their experience, however, they have not known of any induction motors where this assumption did not hold true.

The authors were very much interested in the solution derived by Mr. Crary from Park's original equations for the case of simultaneous application of voltage on all three phases and note that it may be expressed in relatively compact form. It is also of interest that this equation reduces to equation 11 of the paper where the magnetizing reactance is large compared to the leakage reactance. However, the authors are wondering whether or not such a compact solution could similarly be found for the case of nonsimultaneous voltage application, that is, voltage applied to α axis 90 degrees later than that applied on β axis. Since higher torques are set up under these conditions, this solution is perhaps of more importance from a practical standpoint. It should also be noted that if the electrical torque expression becomes too complicated, considerable difficulty may be had in integrating equation 5 of the paper in order to obtain the shaft torques.

In answer to Doctor Rüdenberg's question, it is true that the high transient torques developed may be of advantage in certain cases, particularly where static friction is high. However, where such loads must be started and stopped frequently, care must be taken that the mechanical stresses in the connected equipment set up by such torques do not exceed the allowable endurance values. The object of this investigation was primarily to give the designer sufficient information to enable him to make a rational design of equipment subject to such conditions.

Progress in the Production and Application of Light

A. L. POWELL
FELLOW AIEE

For at least three decades, light sources and their application continuously have undergone rapid changes. To provide the Institute membership with a record of these developments, it has been the policy of the AIEE committee on the production and application of light to prepare at periodic intervals reports reviewing the progress achieved during those intervals. The 1939–40 committee* presents in this report a summary of the epoch-making advancements since 1936, when the last previous report was published,[1] prepared by a member and former chairman of the committee.

THERE have been numerous developments in the art of lighting during the past three years, but to the illuminating engineer this is nothing unusual. For at least three decades light sources and their application have always undergone rapid changes. It is true that the whole field of electrical engineering, being young and with a tremendous horizon, is in a constant state of flux, but those working in lighting have reason to feel particularly proud of the way that new frontiers have been attacked. To say the least, lighting is not static and those engaged in it are constantly experiencing the thrill that comes with change.

During the period under consideration there have been some epoch-making developments and numerous others that were foretold in the previous report. These take the nature of refinement and expansion of ideas promulgated several years ago.

In the last report it was pointed out that during the year 1935 more than 700 million incandescent lamps were sold, which was then a new high record. In 1936 this figure was exceeded, the total reaching 880 million. The year 1937 saw an all-time high of 955 million, and in 1938 there was a falling off to approximately 800 million; of this total, 60 per cent was what are called large and 40 per cent miniature lamps. The rate for 1939 to date indicates that this figure will be exceeded.

New Incandescent Lamps

Lamps for Photography

There probably has never been a period that has shown such a great growth in public interest in any hobby. Almost everyone is a camera enthusiast. The development of numerous types of inexpensive miniature cameras, of new types of film, of practical color film, and simple movie cameras has contributed to this end. Most of the amateur photographers are not content with natural light, but are riding their hobby for 24 hours a day. The so-called photoflash lamp has completely displaced the old magnesium flash bag, and the news and commercial photographers are requiring many forms of flash lamps. The new varieties of color film have presented lighting problems. The miniature camera has greatly increased the importance of enlarging. As a result of all this, the line of lamps designed especially for photography has grown by leaps and bounds.

The photoflood lamp series has been supplemented by three sizes, 250, 500, and 1,000 watts in blue bulbs for use with "regular" Kodachrome film. These also find application in indoor photography where artificial light is required to supplement daylight.

A 500-watt photoflood lamp with an inside aluminized reflecting surface is on the market and proves very convenient and compact.

Three years ago three sizes of photoflash lamps were listed which had bulbs loosely filled with very thin sheets of aluminum foil in an atmosphere of oxygen. This general form has been supplemented by lamps using extremely fine aluminum wire instead of foil, and the entire line has been expanded so that now no less than eight types and sizes of photoflash lamps designed for particular operating conditions are available. Space does not permit an analysis of details of their characteristics.

For amateur and professional photoenlarging there is now standardized a line of seven lamps in special white bulbs varying in size from a tiny *S*-11[†] bulb emitting 1,125 lumens to a *PS*-30 bulb emitting 15,800 lumens.

Since color photography requires light of a definite spectral composition for proper rendition, it has been necessary to make available lamps standardized to operate at a predetermined color temperature, namely, 3,200 degrees Kelvin. These, again, are of various forms and sizes (500 to 5,000 watts) and are 13 in number.

Certain classes of color photography require light sources at a still higher color temperature and five lamps (1,000 to 10,000 watts) are available that are designed to operate at 3,380 degrees Kelvin.

Lumiline Lamps

These tubular lamps with contacts at both ends are still quite popular where structural conditions make neces-

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Paper 40-1, recommended by the AIEE committee on production and application of light, and presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940. Manuscript submitted October 24, 1939; made available for preprinting October 31, 1939.

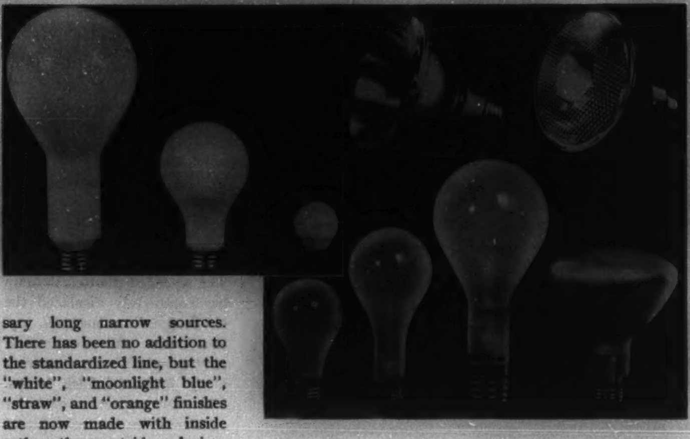
A. L. Powell was supervising engineer in the lamp department of the General Electric Company, New York, N. Y., at the time of his death, August 21, 1940.

*Personnel of AIEE Committee on Production and Application of Light: D. W. Atwater, *chairman;* S. K. Barrett, Robin Beach, W. T. Blackwell, H. B. Dates, E. E. Dorting, C. L. Dows, L. R. Gamble, C. A. B. Halvorson, Jr., L. A. Hawkins, S. G. Hibben, W. C. Kalb, R. D. Mailey, P. S. Millar, G. T. Minasian, L. C. Porter, A. L. Powell, E. M. Strong, and I. A. Yost.

The author acknowledges the co-operation of various members of the committee in supplying data and checking the manuscript.

[1] For all numbered references see list at end of report.

[†] In bulb designations the letter indicates the shape and the figure the maximum diameter in eighths of an inch.



Three sizes of photo-enlarging lamps, from the largest to the smallest (left group); photoflood lamps of the regular and reflector types (lower right group). At the upper right are (left) a typical spot lamp and (right) a typical flood lamp of the projector type

sary long narrow sources. There has been no addition to the standardized line, but the "white", "moonlight blue", "straw", and "orange" finishes are now made with inside rather than outside coloring, because of greater ability to withstand weather conditions.

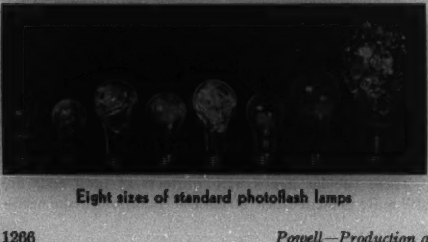
Three-Light Lamps

A considerable number of sizes of low-wattage medium-base three-light lamps have been offered for sale. In certain sizes the differences between the various steps are too small to be entirely practical and the combination of low-efficiency small filaments does not make a lamp that has much economic justification. Their novelty has given them a certain amount of popularity, however.

To meet the demand for a light source to provide higher levels of illumination from indirect portable luminaires in the home, a 200–300–500-watt *G*-40 bulb mogul-base three-light lamp has been made available.

Reflecting Processed Lamps

For several years rather large quantities of lamps with a silver coating on the hemispherical bowl of the bulb have been used for indirect lighting. The next step along this line was to coat the side walls of the bulb with reflecting material and use the lamp itself without external reflector to direct the light where needed. For this purpose, at first standard shapes of bulbs were employed, but these obviously were not designed to produce desirable distributions of light, so special filament mountings and bulbs of suitable contours were a logical step. Recently several new designs have appeared.



Eight sizes of standard photoflash lamps

Filaments of 150- and 300-watt ratings are mounted in bulbs 5 inches in diameter by $6^{1}/_{2}$ inches over-all with part of the inner surfaces given a special process mirrored finish of aluminum. These are available in two light distributions known as "spot" and "flood." The former is more concentrated and the latter of wider spread. This construction gives the following candle power readings at the peak of the beam:

300-watt spot	16,000	150-watt spot	7,000
300-watt flood	3,000	150-watt flood	1,200

The reflecting lamps just discussed have bulbs of ordinary glass and must be protected from the weather, but some new lamps known as projector lamps are made of heat-resisting glass. In these the bulb is made in two parts, the cover being fused to the bulb after the high-efficiency concentrated filament has been positioned. Two types of cover plates are used, one to give a flood and the other a spot distribution. In the 150-watt size the flood type gives a maximum candle power of 3,500 and the spot type 10,500.

Tubular-bulb lamps of various sizes with half of the bulb having a reflecting coating are available for show-cases and other places where space is limited.

Drying Lamp

In many industrial processes radiant energy has been found to be most effective for drying purposes. Synthetic enamels and many lacquers can be dried in from 10 to 15 per cent of the time required by other methods. For this purpose a 250-watt *PS*-30 bulb tungsten-filament lamp has been developed. The filament is operated at an abnormally low temperature or efficiency ($7^{1}/_{2}$ lumens per watt), which avoids the production of unnecessary visible radiation and gives a very long lamp life. As a supplement there are on the market several types of concentrating reflectors for directing radiant energy to the surface to be dried. Most of these are finished with electrolytic gold because this is efficient in reflecting the near infrared energy, permanent, and easily cleaned.

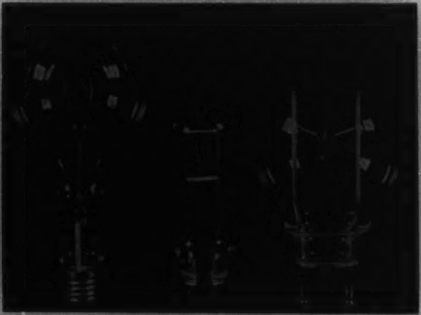
Projection Lamps

A rather startling advance has been made in this field with the development of 1,000- and 1,200-watt T-12 bulb biplane-filament lamps with a rated life of ten hours. The filaments and filling gas of these lamps incorporate improvements resulting in large increases in screen illumination and more uniform screen brightness. With the 1,000-watt lamp, for example, the lumen output is 32,000, and with a 16-millimeter motion-picture projector there is a gain of 50 per cent in light on the screen over lamps of previous design of the same wattage. In this field, also, improved optics have been introduced which make still further increases in screen brightness over that obtained with old style lenses and the same lamps.

With projecting lamps it is often necessary to prevent the light from escaping upward, and heretofore small metal caps have been used to slip over the ends of the tubular bulbs. Now the T-12 bulb 750-, 1,000-, and 1,200-watt projection lamps are regularly supplied with opaque end coatings.

An interesting medium-size projection lamp is the 300-watt T-8$^{1}/_{2}$ bulb lamp. This has a high-efficiency coiled-coil filament and a single-contact prefocused base. Being only 1$^{1}/_{16}$ inches in diameter and 4$^{1}/_{2}$ inches long it enables a source of 300 watts to be placed very close to the condensing lens and increases the amount of light entering the optical system of 8-millimeter motion-picture projectors.

During the past two or three years a considerable number of quite inexpensive toy projectors have been placed on the market. These are far from optically perfect, but they require brilliant concentrated light sources of decidedly low cost. For this purpose there has been standardized a line of several sizes and types of toy-projector lamps which retail at very low figures.



Left to right: old style 1,000-watt PS52-bulb lamp; 1,000-watt T24-bulb medium bipost lamp; and a mogul bipost lamp for projection service

Bipost Base Lamps

The mogul bipost construction has become the accepted design for new types of high-current lamps. The medium bipost construction has been applied to the T-20 bulb 500-watt and the T-24 bulb 750- and 1,000-watt lamps for general lighting service, as well as to a considerable number of types of projection lamps.

A few of the projection lamps now available

The small over-all size made possible through the use of hard glass and the medium bipost construction has enabled designers to make commercial fixtures for high-wattage lamps of attractive proportions. A number of excellent designs along this line are on the market.

Important Changes in Standard Lamps for General Lighting Service

The standard life of 200- and 300-watt lamps has been reduced from 1,000 to 750 hours in line with production of light at minimum operating cost. The 300-watt lamp is now made in a shorter bulb with a medium screw base and 750-hour rated life. The 50- and 60-watt lamps are now standard in A-19 rather than A-21 bulbs.

In the last report mention was made of the application of the coiled-coil filament to projection lamps. This construction makes possible increased efficiency in certain sizes of the regular lamps. The use of this coiled-coil principle has been extended first to the 60-watt size and here a 10-per cent increase in light output resulted. The uncoiled wire from such a lamp is 20 inches long, and with double coiling this is compressed to only $^{5}/_{8}$ inch. Since the filament is so concentrated the cooling effect of the filling gas is minimized. This construction was next applied to the 50-watt lamp, then to the 100-watt size, and a program is under way to expand its use as manufacturing facilities permit.

Flashlight Lamps

To permit accurate placement of flashlight filaments at the focus of reflectors, a simple type of prefocused base has been developed and applied to lamps of several sizes. Such accurate control, combined with die-cast reflectors, prevents distorted beams, provides more uniform and brighter spots of light, and materially increases the seeing range for general flashlight service.

SEALED AUTOMOBILE HEADLIGHTS

A most radical innovation in automobile headlights has taken place. In the past a typical assembly consisted of a plated parabolic spun reflector, an adjustable socket, a refracting cover glass or lens, and the necessary supports and housing. For accurate light control it was necessary to maintain three elements—the lamp, reflector, and lens—in exactly their proper relative positions. Even with rigid assemblies and careful workmanship, this was difficult because of the vibration and shock incidental to car operation. When these elements were out of optical adjustment a distorted and glaring beam resulted. In order to replace the lamp, it was necessary that the lens or cover glass be removable, and, in spite of attempts properly to gasket the joint, dirt and moisture accumulated on lamp bulb, reflecting surface, and cover plate. This accumulation materially reduced the light output.

"Sealed beam" automobile headlamp

Automobile manufacturers realize that with increased speeds safety in night driving required good headlighting and have been anxious to find some way of overcoming the two major difficulties just mentioned, produce something in which the human element of adjustment and maintenance was minimized, and obtain higher beam candle power. Work along this line has been proceeding for several years and has resulted in the development of two types of "sealed beam" headlamps, essentially the same in principle but different in structural details. These are entirely interchangeable, standard as to dimensions, and have the same light output and beam pattern. In one type there is a plated spun metal reflector to which a conventional-type two-filament bulb is soldered with the filaments in their correct positions. The lamp has three contact lugs so arranged that it cannot be inserted incorrectly in the receptacle. Across the mouth of the reflector is hermetically sealed a refracting-type cover glass. The maximum diameter is seven inches and the over-all length or thickness five inches.

The other type of sealed-beam lamp is similar to the projector spot and flood lamps in general construction features. The lamp bulb itself (Mazda lamp number 4030) is the complete unit. The shape is known as the *PAR*-56. Essentially it consists of an accurately designed pressed hard-glass parabolic section with aluminized coating on the inner surface; a refracting cover plate is welded to the back half of the lamp. In it are mounted two 6–8-volt filaments: one taking 40 watts is positioned for the so-called country beam; the other consuming 30 watts is positioned to produce a lower and sidewise-directed traffic beam.

Practically all the 1940 model cars have this new sealed-beam headlamp. An ingenious mounting arrangement has been designed and a standardized and universal switching arrangement for the two beams will be found. Since the new country beam is more powerful than previous types of "high" beam, there will be a campaign of education to make sure that operators switch to the traffic beam when meeting oncoming cars. It is anticipated that this radical development will be a tremendous factor in safe night driving.

Gaseous-Conduction Lamps

Continued research and development has made possible quite a number of new types of these lamps. The high-intensity mercury-arc lamp line, of which three sizes were mentioned in the previous report, namely, 400-watt (type *A-H*1), 250-watt (type *A-H*2), and 85-watt (type *A-H*3), now has in addition a 100-watt lamp (type *A-H*4), which emits 3,500 lumens, has a rated life of 1,000 hours, is made in a *T*-10 bulb and provides a low-wattage small bright source. The 250-watt size is also made with a smaller arc stream. This is known as the type *A-H*5, emits 10,000 lumens, has a rated life of 1,000 hours, and is a medium-wattage small bright source useful for floodlighting, and other purposes. Details of the theory and construction of these new lamps have been published in ELECTRICAL ENGINEERING.[2,3]

The water-cooled lamp has now been placed in production and is finding numerous and interesting applications. It is designed to operate at 1,000 watts and emit 65,000 lumens. The lamp itself is a small quartz tube with an arc stream $1^1/_3$ millimeters wide by approximately one inch long. This tube is placed in an outer jacket through which a flow of water of approximately three quarts per minute is necessary. Some special jackets have been made in which three lamps are placed side by side in one container. A rather special control system is necessary to make sure that the lamp will not be lighted before the water is flowing properly, that the water will be turned off if the lamp or power fails, and, further, that no voltage is applied in case of lamp failure. This lamp, known as type *A-H*6, is proving most useful in photoengraving, contact printing, high-power enlarging, copy-board illu-

A photoengraving unit employing the 1,000-watt A-H6 water-cooled high-intensity mercury-arc lamp

mination, oscillograph lighting, and other fields where an extremely brilliant and concentrated light source is required.

In Europe the 400-watt hot-cathode mercury lamp with a certain amount of cadmium added is on the market, fitted with an exterior envelope or bulb, the inner surface of which is covered with fluorescent material. This combination produces a whiter light at no loss in efficiency.

The standard low-pressure mercury-vapor lamp (Cooper-Hewitt) has been provided with an improved circuit which enables the complete unit to produce the same light output with a 22 per cent reduction in power.

Fluorescent Lamps

The last report mentioned that phosphors were being applied to the high-voltage cold-cathode type of gaseous-conduction lamp and that work was under way toward the production of standardized replaceable lamps for operation on commercial voltages incorporating the fluorescent principle. This is now an accomplished fact and is one of the most important innovations in recent years.

Lamps are now standard in 18-, 24-, 36-, and 48-inch sizes, which have a uniform rating of 10 watts per foot. In brief, an arc stream of low current density passes through mercury vapor to which a small amount of argon has been added for ease in starting. A slight amount of visible radiation (three to four lumens per watt) of the characteristic mercury color is generated, but this is accompanied by a considerable amount of radiation at 2,537 angstrom units. This ultraviolet radiation impinges on phosphors coated on the inside of the tube and is transformed into visible radiation at surprisingly high efficiencies in most colors.

One of the most outstanding features of this development has been the production of new types of phosphors which have proved very effective. There are now standard phosphors radiating green, pink, and blue light. By mixing these a white light very closely approximating, 3,500-degree-Kelvin black-body radiation can be produced. Another mixture gives an artificial daylight good enough for most purposes equivalent to that of a black body at 6,500 degrees Kelvin. If the tube coated with pink phosphor has also a layer of red lacquer, through selective absorption a red light is produced. Similarly, if the white tube has a coating of yellow lacquer, a golden light results. Thus seven colors are on the market.

Efficiencies vary somewhat, depending on the tube dimensions and color, but are of the following order:

White	35 to 47 lumens per watt
Daylight	30 to 40 lumens per watt
Blue	21 to 26 lumens per watt
Green	60 to 75 lumens per watt
Pink	20 to 25 lumens per watt
Gold	25 to 31 lumens per watt
Red	3 to 4 lumens per watt

At each end of the tube is a small coiled coated electrode. In operation these electrodes are first heated and when the electron emission reaches the proper point, the arc is struck and the electrodes are de-energized.

Since these are essentially arc lamps, every lamp requires some device in series with it to act as a ballast. Choke coils and small capacitors are used for this purpose. The ballast and a switching arrangement for energizing and turning off the electrodes are combined in auxiliary devices. These are of several types and sizes and in certain forms the controls for two lamps are included in one casing. Work is still under way on auxiliaries, and each month or two sees the introduction of new designs.

The tube or lamp has a cap at each end on which are placed two small pin contacts across which the electrodes are connected. A special lampholder is required. Space does not permit a complete discussion of the operating characteristics, of the factors that influence lamp performance, and of the specialized applications of this new illuminant. Some of these features have been described in previous articles and papers.[4–6]

The principle of fluorescence has also been applied to the standard low-pressure (Cooper-Hewitt) mercury-arc lamp. Here a rectifying type of a-c operation is used and a high-efficiency fluorescent material coated on the inside of a 52-inch tube. The lamp and auxiliary consume 100 watts and produce 5,000 lumens. Although the color of the light is similar to that of the Cooper-Hewitt lamp, the fluorescence adds a considerable amount of red and color rendition is decidedly improved, although no claim is made for white or daylight color quality. This lamp finds particular use for high-level industrial illumination with units mounted relatively low.

Ultraviolet Sources

In the sunlamp class one lamp has been developed known as the *S*-4. This is the same as the 100-watt *A*-*H*4 lamp discussed under "Gaseous-Conduction Lamps" except that it is fitted with an *A*-21 bulb of special transmitting glass. It has a somewhat greater output of ultraviolet radiation in the anti-rickitic region than the *S*-1 lamp and consumes only one-quarter the energy. The fixtures for the *S*-4 sunlamp are smaller, lighter, and easier to handle.

In the germicidal type, the "Sterilamp,"[7] a high-voltage cold-cathode type, is now made in 10-, 20-, and 30-inch sizes with ratings from about 10 to 20 watts. The following additional units have been made available:

Watts	Bulb	Base	Necessary Auxiliary	Life (Hours)	Microwatts Per Sq Cm of 2,537 A Radiation at One Meter
8	*T*-4	Radio type	Separate reactor	1,000	3–4
5	*T*-10	Medium screw	None required	1,500	1
15	*T*-8	Fluorescent	Same as for 15-watt fluorescent lamp	1,500	15–20
5	*T*-6½	Candelabra screw	Separate resistor	4,000	0.5
16	*T*-4	2-pin special	Separate reactor	1,500	15–20

These various germicidal tubes are finding considerable application for destroying bacteria. They have been experimentally installed in air ducts to kill air-borne bacteria; they are being used as a preventative to the spread of disease in hospitals, and for food preservation by various industries. Sanitization of drinking glasses and sterile storage of previously sterilized utensils are applications for these lamps which are becoming more popular every day. The relatively small size of some of the new tubes and simple control devices make them suitable for installation in places where space is limited.

A very useful new source for producing ultraviolet radiation for fluorescent and phosphorescent purposes is a result of modification and adaptation of the 100-watt type *A-H*4 lamp. This is made with a *T*-16 bulb of natural red-purple glass and emits a considerable amount of radiation of the type required for producing fluorescence, at the same time screening out most of the visible radiation. It is known as the type *B-H*4 lamp.

The Uviarc, a powerful source of ultraviolet radiation for industrial purposes, has been adapted for a-c operation. An entirely new line of these lamps with oxide electrodes and a limited amount of mercury has been introduced.

Carbon Arcs

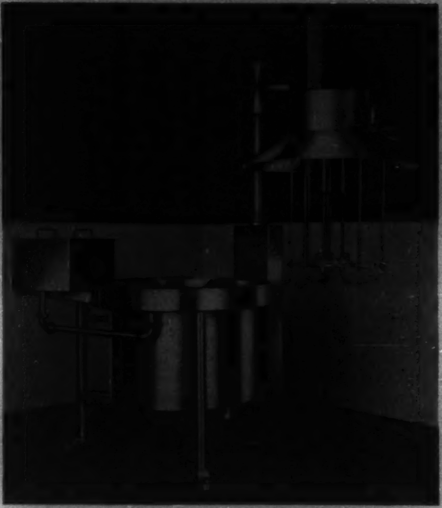
An improvement in carbon arc lighting for the motion-picture studios has been effected since the previous report of this committee through the development of a broadside lamp with motor-driven carbon-feeding mechanism. This development gives improved steadiness of light and a much lower sound level than lamps with solenoid-operated feed mechanisms.

A new super high-intensity carbon, 11 millimeters in diameter, provides an intrinsic brilliancy of 1,200 candles per square millimeter at an arc current of 135 amperes.

The development of carbon arc lamps for 16-millimeter motion-picture projection is extending the scope of the 16-millimeter film in educational and commercial fields, permitting the projection of a brilliant, sharply defined image on a much larger screen than has heretofore been practicable.

Three ultraviolet lamps of the germicidal type

The 3-phase carbon arc has been adapted to modern requirements and applied to a number of industrial uses. A 12-kw lamp, burning six carbons on a wye circuit, is being used for the vitamin-*D* activation of milk. For purposes of illumination this lamp provides a light source of one million lumens. A one-kilowatt, three-carbon delta-connected lamp is used in a new accelerated-fading-test unit. This lamp, equipped with carbons giving a spectrum similar to sunlight, is also used for illumination on applications requiring high-intensity light of daylight quality, free from the stroboscopic effects sometimes experienced with the single-phase arc.



A 12-kw arc lamp burning six carbons on a wye circuit being used for vitamin-D activation of milk

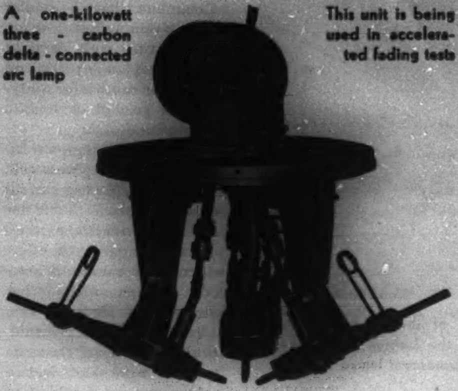
New Materials for Lighting

Glass blocks or bricks as structural elements have seen somewhat more extensive use during the past few years. Since these are translucent they offer opportunities for novel lighting effects, and quite a number of colorful schemes have been applied.

A new form of tempered glass has been introduced, and to this enamel of various colors is applied. This material is suitable for special effects in luminous store fronts and the like. In the same field different forms of weather-resisting metals lend themselves to interesting design in co-ordination with light and color. Mat and polished finishes, as well as porcelain-enameled corrugated metal are available to the designer.

Under normal conditions it is difficult to control the light reflected from a ceiling, for with plaster, sound-proofing materials, and other coverings diffuse reflections result. One manufacturer of metal ceilings has placed on the market sheets so formed that controlled or regular reflection results. This is known under the trade name of Parab-O-Lume.

A colorless transparent plastic known as Lucite has found certain interesting applications. This material has the property of retaining a beam of light through in-



A one-kilowatt three-carbon delta-connected arc lamp

This unit is being used in accelerated fading tests

ternal reflections in much the same manner as a quartz rod. It has been applied to curved dental and surgical lighting devices where the beam is bent around the corner to illuminate some cavity.

Notable Lighting Installations

During the period under consideration there has been relatively little new building, but considerable revamping and modernization of existing structures. Practically all the leading shops on Fifth Avenue, New York, State Street, Chicago, and similar merchandising centers have installed unique and effective up-to-date lighting within the last few years. Many public rooms in hotels, restaurants, night clubs, and cafes are completely changed in appearance, and some of the most ingenious lighting to be found anywhere may be noted in this class of service.

It is impossible to tabulate the number of larger department stores that have discarded old systems and installed illumination schemes to take advantage of the most recent developments. As representative of this field we might mention: Gimbels, New York, N. Y.; Marshall Field, Chicago, Ill.; Kauffman's, Pittsburgh, Pa.; J. L. Hudson, Detroit, Mich.; Orecks, Duluth, Minn.; Z. C. M. I., Salt Lake City, Utah; Saks, Los Angeles, Calif.; Roos Brothers, San Francisco, Calif.; T. Eaton Company, Toronto, Ontario, Canada.

Remarkable advances have been made in illumination of banking institutions, and both the Los Angeles and Toronto stock exchanges have been completely relighted. The new service building of the Detroit Edison Company is an example of the application of many new principles of illumination, and the main office building of the Commonwealth Edison Company has an entirely new system.

Details of new and novel lighting installations are discussed in each issue of the Illuminating Engineering Society *Transactions* under a section conducted by the society's committee on the co-ordination of light and architecture. Those desiring to keep in touch with the most modern practice should refer to that publication.

There have been a number of international expositions or world's fairs, and, of course, in these the latest developments in lighting are always incorporated. Details of the Texas Centennial,[8] Great Lakes,[9] Paris,[10] and Golden Gate,[11] expositions have been reported in ELECTRICAL ENGINEERING and in other publications.

A manuscript describing and giving technical details regarding the lighting installation at the New York World's Fair 1939 is under preparation and is scheduled for publication in ELECTRICAL ENGINEERING when completed (included in this volume, see pages 1288–1305).

Lighting Instruments

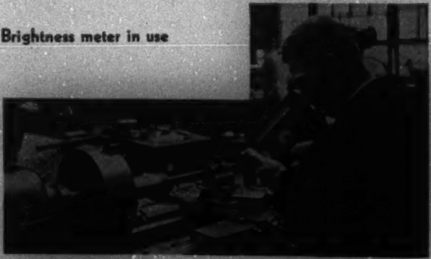
Not long ago the rather cumbersome bar photometer with grease-spot screen was the only light-measuring device. With the development of the art of illumination the need for additional measuring instruments has grown apace, and each period has seen new types made available.

One rather interesting device, first developed in England, has been introduced in the United States, known as a light selection recorder. The observer, or subject, has a means of varying the illumination level on a test object and eventually selecting the amount of illumination he prefers. At this point he presses a button and on a printed scale is indicated the result of his choice.

With the growing appreciation of the importance of brightness measurements, some special meters have been developed for this purpose. A rather expensive and accurate type is known as the Luckiesh-Taylor brightness meter. A less expensive and more simple instrument, known as a model *L-H*, was developed by Luckiesh and Holladay.

Baumgartner has developed a light-cell reflectometer[12] and a light-cell distribution photometer. This latter device enables one to obtain a candle-power distribution curve in an extremely small fraction of the time required by the older method.

The widespread use of photoflash lamps has made it necessary to find some means of checking the setting of camera shutters in relation to the peak of light output. For this purpose a rather simple device known as the "synchrograph" was introduced. Another instrument along this line is known as a "flashograph," which traces



Brightness meter in use

on a moving film the performance of photoflash lamps. The lamp is flashed in a small sphere which has a photoelectric cell mounted on it. The amplified output of the cell is applied across a small Rochelle-salt crystal oscilloscope. The deformation of the crystal in reflecting a beam of light across the aperture of a drum traces the candlepower-time relation on film.

With the development of the ultraviolet lamp of the germicidal type (Sterilamp) the need arose for an instrument that would provide an exact quantitative measurement of the abiotic radiation from these lamps. The instrument developed to meet this need is an ultraviolet meter embodying a tantalum photoelectric cell, which has a wave-length response to ultraviolet radiation similar to the lethal action of these radiations upon bacteria. With the aid of this device, it has been possible to determine the abiotic energy necessary to kill any chosen micro-organism, and sterilizing installations may be planned with an exactness approaching that of the normal lighting installation.

Interior of the lounge car of the "John Wilkes," remodeled express train of the Lehigh Valley Railroad, lighted by fluorescent lamps

Transportation Lighting

Real progress has been made in this field. Some of the tendencies have previously been reported.[15] New cars and busses, in general, are quite up-to-date from the lighting standpoint, and much work has been done in revamping and modernizing old vehicles.

With the coming of the fluorescent lamp the railroad engineers were among the first to consider its use. Since these lamps require alternating current for most efficient operation, the first step was the development of a vibrator inverter for operation on the storage-battery circuits. This device has worked out well in service. In some cases a small motor alternator set is used. The New York Central Railroad was the first to put in operation a fluorescent-lighted car. The Burlington Lines "General Pershing" Zephyr and the Lehigh Valley remodeled express train "John Wilkes" were the first two trains to be lighted throughout by fluorescent lamps.

Light in Horticulture

There is considerable interest on the part of various agricultural experimental stations in growing plants entirely under artificial light for the purpose of eliminating variables in order to study the effect of different chemicals, both for nutrient solutions and soil growth.

At Cornell University, Ithaca, N. Y., there is a room equipped with 24 1,000-watt lamps and a water filter, as well as humidity control. Ohio State University, Columbus, has a similar installation and is now experimenting with daylight fluorescent lamps producing 1,500 foot-candles for studying the growth of tomato plants.

The Boyce Thompson Institute for Plant Research, Yonkers, N. Y., has carried on continuous work and has recently conducted a series of tests comparing the effectiveness of various fluorescent lamps with other sources for plant growth. The University of California, Berkeley, is doing the same type of work and had a rather impressive exhibit at the Golden Gate Exposition during the 1939 season.

The color value of the daylight fluorescent lamp is apparently more suitable than that of filament lamps for stimulating plant growth. It gives much less radiant heat per foot-candle, and there is every indication that it will be an important factor in this field.

A notable installation of carbon-arc lamps for stimulating plant growth has been made at the field station of the United States Department of Agriculture, Beltsville, Md.

Sports Lighting

All the American Association baseball fields are now lighted for night playing and the "big" leagues are gradually recognizing the fact that night games pay well. Ebbets Field in Brooklyn, N. Y., has installed a total of 615 1,500-watt incandescent floodlights, and the Cleveland Municipal Stadium has now 712 units of the same size.

Show-Window Lighting

More than two decades ago the idea was promulgated that the show window was a miniature stage and that all the effects used on the latter would find application for the window display. It finally appears that the more progressive merchants are appreciating the logic of this approach. In the more artistic type of window spot lamps equipped with color mediums, flood effects, colored illumination of background, and combinations of all these are being applied with splendid results.

Street and Highway Lighting

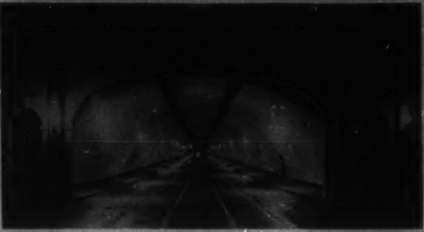
Each year shows a growing public appreciation of the advantage of highway safety lighting even if one considers only the dollars and cents involved. New tests constantly show that the cost of this lighting is soon offset by decrease in accidents. In several states modernization plans are under way; in certain instances the installations are owned by the state. Many of the important cities have shown marked improvement.

There have been a number of new forms of street luminaires, most of which incorporate efficient Alzak reflectors. A rather novel feature in one type is the reflector spun onto the globe providing a weatherproof seal and greatly reducing glassware breakage.

Sodium lighting with its distinctive color has proved useful as a caution signal for safety-island beacons, as well as for especially dangerous intersections. The two new large bridges in California, the Golden Gate and the San Francisco-Oakland, as well as the Blue Water Bridge over the Saint Clair River between Michigan and Canada and the new Main Avenue Bridge in Cleveland, Ohio, are all lighted by the sodium system. This efficient light has also proved very acceptable for underpass and tunnel lighting, notable installations being the Two Rock Tunnel on the Columbia River Highway in Washington, the Liberty Tunnel in Pittsburgh, Pa., the Stockton Street Tunnel, San Francisco, and the McCallie Avenue Tunnel, Chattanooga, Tenn.

Aviation Lighting

Improved efficiency in the design of conventional types of aviation lighting equipment and the introduction of new types of lights have combined to make the past four years a period of important progress. The trend in beacons has been a return to the 24-inch size but the design has been "streamlined." The trunnion arms have been eliminated, reducing the area exposed to wind and the risk that ice and snow may interfere with smooth rotation. The base has been redesigned to afford better access to the mechanism for maintenance. A new lamp changer eliminates the shadow of the reserve lamp from the beam. Another form of this beacon is also available in which the drum has been replaced by a glass dome, the rotating parts thus being completely enclosed. To provide a beacon with more frequent flashes, an oscillating beacon has been developed. In this unit a lamp moves up and down on the axis of a cylindrical Fresnel lens giving 80 flashes per minute. This beacon also has more candle power at high angles than the 24- or 36-inch beacons.



Stockton Street Tunnel, San Francisco, Calif., lighted by 10,000-lumen sodium-vapor units

An experimental boundary-light circuit in which the lamps are fed by a 550-volt multiple circuit through individual transformers has been installed at Washington (D. C.) Airport. The circuit is less expensive than a series circuit and the voltage drop is much less than with an ordinary multiple circuit. It is well adapted for use on fields that do not have sufficient night traffic to warrant the outlay required for a series circuit.

Floodlighting is being designed with greater attention to the avoidance of glare. This has led to the development of an improved 90-degree unit employing four 1,500-watt lamps each in a separate Fresnel lens. The four separate sources give insurance against a sudden interruption of the illumination by the burning out of a lamp.

The development of the approach-and-contact-light system has largely taken place in the last four years. This system was originally designed to supplement radio aids in fog, but its use in good weather has found much favor. The approach lights are located outside the landing area and are accordingly red. Neon lamps approxi-

Shibe Park, home field of the Philadelphia (Pa.) baseball club of the American League, illuminated by 780 1,500-watt floodlights mounted high above the field on eight towers, one of which is shown at the right

Grill room of a New York, N. Y., hotel with fluorescent units recessed in the columns

mately six feet in length, with suitable parabolic reflectors, give an intensity of 1,000 candles and, by their elongated shape, establish a horizontal plane of reference to guide pilots in approaching the runway. The contact lights are arranged along the two sides of a runway to show its location and indicate the ground level. These units project less than two inches above the runway and are available in two types. Heavy-duty units are used on runways that are in line with approach-light systems or furnished with radio aids for instrument landings. Normal-duty units are used on other runways. Contact lights are lighted on only one runway at a time. The approach-and-contact-light system, in addition to being one of the most satisfactory aids for night landings, is also the most effective means available for giving an airport a distinctive appearance at night; this is a great aid to a pilot looking for an airport with which he is not familiar.

There has been marked progress in the development of traffic-control lights. Lights visible only along the runways are being installed at several airports to control taxiing movements and take-offs. For controlling landings, 90-foot arrows composed of red and green marker lights are being installed at the ends of the runways. A green arrow indicates that landings may be made from the end of the runway so marked. A red arrow shows the landing end of a runway that must not be used at the time.

The growing overseas air traffic is necessitating equipment for use as seaplane-channel marker lights. Some very interesting experiments have been conducted with battery-operated coiled-tube fluorescent lamps mounted on resilient rubber floats to meet this need. The Civil Aeronautics Authority expects to establish specifications for seaplane-channel lights when these experiments have yielded sufficient data.

Electrical Advertising

The most novel feature in the incandescent-lamp type of sign is noted in the further development of the "motion picture" type of display. In such a sign moving images from a motion-picture projector are thrown upon a screen made up of photoelectric cells which, in turn, light up a huge bank of incandescent lamps in accord with the light and shade of the projected image.

In the gaseous-conductor type of display the familiar orange-red of the neon and the blue of the mercury have become so common that in many cases the colors lose their appeal and attracting power, and the variety of effects possible through fluorescent lamps are indeed welcome to this industry. It is reported that a very high proportion of all new work incorporates fluorescent tubing.

In the smaller type of displays many very unique features have been incorporated. One popular and inexpensive form uses letters or emblems made up of glass tubing in which volatile liquid is placed. There is a reservoir or small bulb at the bottom. The heat from concealed lamps causes a continuous moving bubble effect and the fluid itself is luminous from the light emitted by the lamps.

Another type of sign has a message formed by a thin wire which serves as a negative electrode for an electric discharge in a helium-filled tube. Still another takes advantage of the phenomena of high-frequency discharge. Changeable letters are filled with neon or other gases. An oscillator is placed in the base of the sign or nearby. There is no electrical connection, yet the letters glow with a fair luminosity. Standard colored low-voltage fluorescent lamps have been effectively applied to a number of signs based on the edge-glow and transparent-mirror principles.

Lighting Standards

The Illuminating Engineering Society as the guiding body has continuously conducted researches into various fields of practice. From the industrial angle the special requirements of different processes have been studied and these findings have been reported from time to time. The philosophy behind this procedure has been discussed in ELECTRICAL ENGINEERING[14] and in IES *Transactions.*[15]

Codes applying to airport,[16] highway,[17] and schoolroom[18] lighting have also been published.

Polarized Light

Polarized light is as old as time itself, but has never been a very useful tool for the lighting engineer because of the expense and complication in producing it. Its properties are well known to all who have studied physics, and obviously if it could be made available simply, many valuable and practical effects would result. For several years investigators have been trying to produce a synthetic medium which when placed before a light source would act as a polarizer. Such a material (known as Polaroid) is now available in sheets of considerable size.[19] To the unaided eye it appears as a sheet of thin gray or neutral-colored gelatin filter. Structurally, however, in this sheet are innumerable small microscopic slivers, practically all pointed in one direction. When two sheets of Polaroid are placed with these needle-like particles parallel, maximum light transmission results. When placed at right angles to this position, practically all the light is cut off.

Polaroid mounted between pieces of protecting glass has found rather wide use in sun spectacles. Placed before the openings of reflectors directing light on paintings it provides a means of reducing specular surface reflections to a minimum. Desk and reading lamps have been equipped to furnish polarized light. Beautiful decorative effects for the theater and advertising purposes result from the action of polarized light on cellophane and similar materials.

One of the most interesting fields for the application of the polarizing principles is in connection with automobile headlights. Here the proposed scheme polarizes the light from the headlamp in a 45-degree angle and places on the windshield an analyzer at the same angle. Thus there is minimum reduction in visibility of the beam from one's own headlights, yet the light from oncoming headlights being at right angles to the analyzer, is scarcely visible.

The transmission for unpolarized light incident on the sheet is of the order of 40 per cent. Hence, special beams for city use are indicated in order that the street need not be viewed through the analyzer, for this would reduce the brightness of the street and obstacles thereon by approximately 60 per cent and greatly increase accident hazard; also for the protection of pedestrians who might not wish to provide themselves with special polarizing spectacles.

The reduction in brightness of the interurban or rural highway and obstacles thereon as seen through the analyzer varies greatly depending upon the degree of depolarization caused by reflection from these surfaces. Fortunately, light reflected from the darker surfaces suffers less depolarization and thus the brightness of the most hazardous obstacles is much less impaired. The polarizing materials available until very recently lacked the necessary thermal and solarization properties. Now it is announced that a new polarizing material obtained by suitable processing of polyvinyl alcohol sheets obviates the former difficulties. This makes the prospect for the application to motor car headlights more promising.

Lighting Control

The thermionic-tube control combined with saturable reactors has been improved in certain details. In the first stages of development it was necessary to have a reactor with a rating approximately equal to that of the load to be delivered. Thus a reactor rated for 5,000 watts would not function with a load of one kilowatt. Now, however, reactors are available that will operate over the following load ratios: 1–1, 2–1, 4–1, 10–1, and 30–1.

When this system was first introduced, it was necessary to connect reactors in cascade, which meant that different sections of an installation being thrown on the line would come up to full brightness at slightly different intervals. Now reactors are operated in parallel eliminating this distracting feature.

There has been announced a four-circuit thyratron reactor mounted on a single chassis for mobile light control.

Photoelectric relays for lighting control have been improved and a new circuit used for the control of street and floodlighting. Less equipment and smaller space is required. The new relay is much more sensitive and turn-off and turn-on values can be very close together. Automatic control for the schoolroom has long been desired, for the teacher with her mind on a multitude of other things very often neglects the matter. Such a scheme is not practical, however, unless a decidedly inexpensive and simple photoelectric device is available. This is now the case.

Fluorescent lighting in a New York, N. Y., fur shop

Meeting of the International Commission on Illumination

The International Commission on Illumination, familiarly known to English-speaking people as the "ICI", to the French as "CIE", and to Germans as "IBK", convened for its tenth session in Scheveningen, Holland, June 11 to 20, 1939.

It drew an attendance of over 500 including 11 delegates and 9 guests from the United States. In addition to the 14 member countries, several others were represented, two other applications for membership being received.

From the American viewpoint, the session was probably one of the most interesting and productive of the ten sessions that has been held. Without neglecting standards and other basic questions, an increased amount of attention was devoted to the interchange of information and opinion on the late developments and trends in the art and science of illumination, ranging from laboratory investigation and field surveys to views as to future trends on lighting practice.

Reports covering 32 main topics were presented by 24 technical committees; the two United States secretariats submitted four of these reports. In addition to these, 34 papers by individual authors were presented for discussion, none of these being by American authors.

Among the fundamental questions taken up were the "new" candle, and the several topics arising out of the application of luminosity factors to photometry and colorimetry. Nearly all practical photometry throughout the world is now carried on by physical methods, even to the checking of visual standards.

Some additions were made to the English-French-German lighting vocabulary, which had been printed since the 1935 sessions. Arrangements were also made for the inclusion of equivalent terms in other languages in addition to Spanish and Italian.

Much information on the new electric discharge lamps was brought out as well as data on the recent developments of other illuminants. In the fields of illumination, street lighting, automobile lighting, aviation lighting, railway lighting, and school lighting, the United States Committee contributed information on a relatively extensive scale.

At the conclusion of the technical sessions, a program was adopted for the ensuing three years, 25 secretariats being allotted to the several national committees. In addition a plan was adopted for the presentation of individual papers at the 1942 sessions.

Officers were elected including Doctor Halbertsma of Holland as president and E. C. Crittenden (A'19, M'22) of the United States as one of the three vice-presidents. The invitation of the French committee to hold the 1942 Session in Paris was accepted, while an invitation from the Polish committee to hold an intermediate meeting of the aviation lighting committees in Warsaw was deferred for later action.

Now that Warsaw is in ruins and the great countries of Europe are too busy with destruction to give attention to co-operation on constructive effort, these plans presumably are canceled.

General

It is believed that the above data are a truly remarkable record of progress in an art in a very short space of time. It is impossible to give due credit to the many workers in the field of lighting. They are legion, and here and there without fanfare or publicity, without public recognition, these individuals advance the art here a little, there a little. The cumulative effects are of vast importance to the public. This work is often not sufficiently spectacular to receive much publicity, but it is none the less important and of a nature that is appropriate for engineers to recognize.

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Progress in Power Generation—1933–39

This report of the AIEE committee on power generation* reviews practices and developments in this field since 1933, when the last progress report of the committee was published. It divides itself naturally into three main parts discussing steam, electric, and hydroelectric plant practice. A comprehensive bibliography is appended.

DURING the past six years the most important advances in power generation have been made in the use of 3,600-rpm high-temperature high-pressure steam-turbine hydrogen-cooled generators of large size. Boiler design and construction has been changed to insure sufficient circulation, and welding has taken an increasingly important part in almost every part of the plant equipment, both mechanical and electrical. The period has marked the installation of many superposed units whereby the capacity of existing plants has been increased and the thermal efficiency materially improved. Because these stations are largely located near load centers from which distribution districts are served, there has been much rebuilding and extension of switching facilities of improved design and layout. These changes have greatly lessened the possibility of service interruptions from fire or other causes. Hydraulic power projects have been largely in the hands of Federal agencies. The output of existing hydroelectric plants in many parts of the country has been severely reduced owing to lack of rainfall during the past year.

With the European war there arises the prospect of extra demands on the electric-power systems for manufacturing purposes. For present requirements ample generating capacity is available with a sufficient margin to meet a considerable increase in load. To maintain this margin for growth much new capacity is under construction. In addition, since the beginning of the war about 400,000 kw of steam turbine-generator capacity has been ordered. It is interesting to note that this further expansion is being carried out along the same lines as would normally be followed. It is simply a quickening of the pace of construction, all consistent with earlier prepared plans for plant extension.

Equally careful to the steps being taken to insure an adequate power supply must be the attention given to the physical safety of the plants in operation and under construction. This means special guarding and other precautionary measures.

Steam-Plant Practice

Since 1933, and as predicted then, practically no new large generating stations have been built and very few small ones. Activity has been confined to rehabilitation, principally in the form of superposition of existing capacity. There have also been a number of condensing additions to existing stations.

Development of metals has resulted in an increase of operating steam temperatures from a maximum of 850 degrees Fahrenheit then to 950 degrees. This increase has made possible the superposition of nominal 1,200-pound units on existing 200-pound capacity without the complication of reheat, and consequently this type of installation has been more attractive than heretofore. Also, curtailed economic activity and limited load growth made the relatively small capacities obtainable by this method more in line with requirements. As a result 1,200-pound pressure has become quite common. There has been little tendency to go further in pressure as with present steam temperature limitations reheat would be required. One utility installation, however, is proceeding with a 2,500-pound plant; also, an industrial installation is in operation at this pressure. There has been no commercial installation of any appreciable size for higher pressure than 2,500 pounds, as this is now considered the limit for natural boiler circulation, and forced circulation is as yet untried in the United States for large units.

BOILERS

Boiler sizes have not increased since the last report of this committee, remaining at a maximum of about 1,000,000 pounds per hour. Recent large installations have indicated that the sectional-header boiler is not as desirable for high pressure as other types because of improper distribution of water circulation between individual tubes in the vertical headers. Practically all new high-pressure boilers now under construction are of a type in which the boiler surface proper is either eliminated entirely or disposed nearly vertically with individual tubes entering the steam drum. Welded steel drums are almost universal. All high-temperature installations employ superheat control, consisting of some form of gas by-passing part or all of the superheater surface. Higher pressures have increased the problem of obtaining pure steam. Recent developments indicate that washing is unnecessary, providing either a suitable scrubber or baffling is installed and precautions are taken to hold proper boiler water level in the steam drum. One very satisfactory method of accomplishing this, which has been developed recently, is by means of cyclone separators installed on the outlet of the circulating tubes entering the steam drum.

The trend toward pulverized coal for large boiler units has persisted. Direct-fired systems have been improved to a point where it is difficult to justify the additional expense of a storage system. This has stopped development of large pulverizer units, about 25 tons per hour

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Paper 40-6, recommended by the AIEE committee on power generation, and presented at the AIEE winter convention, New York, N. Y., January 22–26, 1940. Manuscript submitted November 16, 1939; made available for preprinting November 27, 1939.

* Personnel of 1938–39 AIEE Committee on Power Generation: E. C. M. Stahl, *chairman;* H. C. Albrecht, F. A. Annett, G. M. Armbrust, W. L. Cisler, C. A. Corney, H. M. Cushing, J. H. Foote, R. L. Frisby, W. D. Hardaway, N. B. Hinson, J. W. Lingary, S. J. Lisberger, C. E. Magnusson, A. C. Monteith, E. F. Pearson, G. G. Post, C. R. Reid, A. E. Silver, Philip Sporn, Robert Treat, Sidney Withington, and E. D. Wood.

For all numbered references see list at end of report.

being the maximum required. Combustion rates in furnaces remain about the same. Slag-bottom furnaces have been on the increase, and on recent units continuous slag tap has displaced the intermittent type. There has been a tendency toward bare water-cooled walls, eliminating blocks and studs.

Zoned air control for stokers has not advanced as expected. Principal development on underfeed stokers has been an improved tuyère which restricts air flow through the fuel bed at points of low resistance. Present experiments, which look promising, are high-velocity overfire air and auxiliary oil burners over the fuel bed.

TURBINE GENERATORS

There have been no further installations of very large turbine-generator units, the largest now under construction being a 100,000-kw cross-compound 3,600-rpm unit for the Burlington (N. J.) generating station of the Public Service Electric and Gas Company. Where high pressure and high temperature have been employed, there has been a preference for 3,600-rpm turbines because of smaller parts and less tendency for distortion. First-stage blading for the larger machines at this speed has presented a problem in design which has not been entirely solved. Apparently it is a matter of providing sufficient mechanical strength in the restricted space available, along with disposition of parts in a manner that will avoid resonant vibration.

Large-capacity generators for 3,600 rpm made necessary considerable advance in design. The largest generator in operation at this speed in 1933 had a capacity of 18,000 kw, whereas there are now in service a 60,000-kw and several 50,000-kw machines. Increase in capacity has been made possible by the use of high-tensile-strength steels for the fields, hydrogen cooling, and improvements in detail design. Operation of these larger machines has been in the main satisfactory, although there has been some trouble with double-frequency vibration.

There has been very little development of condensers, probably because of the relatively few condensing units installed.

Welding of piping has been carried out to a greater degree than before, and it is now common practice to weld valves into the line. Welded bonnets for high-temperature valves seem to be in the offing.

LJUNGSTROM TURBOGENERATORS

Considerable interest has been aroused in engineering circles by the installation in the Southwick Power Station, Brighton, England, of two 37,500-kw Ljungstrom turbogenerators (see figure 2). These are the largest of their kind in England, although sets developing 50,000 kw are in service in the Vastreas station, Sweden. It is claimed that these latter turbines have shown conclusively that the difficulties once associated with their construction have been completely overcome. It is also confidently predicted that these double-rotation turbines in a single cylinder can be built up to a capacity of 100,000 kw. One of the major advantages claimed for this design is that the turbine may be started without preliminary warming up. Test data on the Brighton machine operating at a pressure of 650 pounds per square inch and a temperature of 850 degrees Fahrenheit with 29-inch vacuum indicate a heat rate of 9,759 Btu per kilowatt-hour.[91] With the present development of this type of turbine they are now adaptable to feed-heating cycles.

GAS TURBINES

Cognizance must be taken of the more recent developments in gas turbines. One of the accelerating factors in this development is the fact that they are being used to provide bombproof stand-by plants, particularly in the fortified areas of Europe. A notable installation is in the city of Neuchatel, Switzerland. These turbines require no cooling water and no auxiliaries when in operation. Over-all thermal efficiencies have attained 23 per cent with well over 20 per cent for the normal operating range.

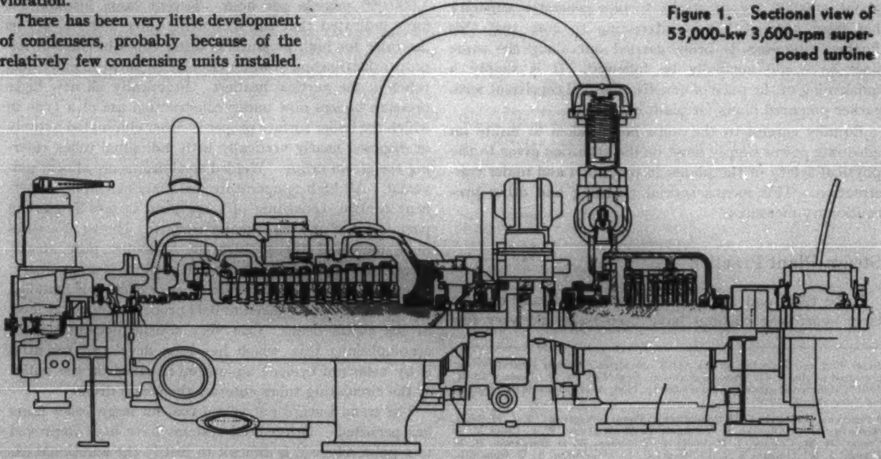
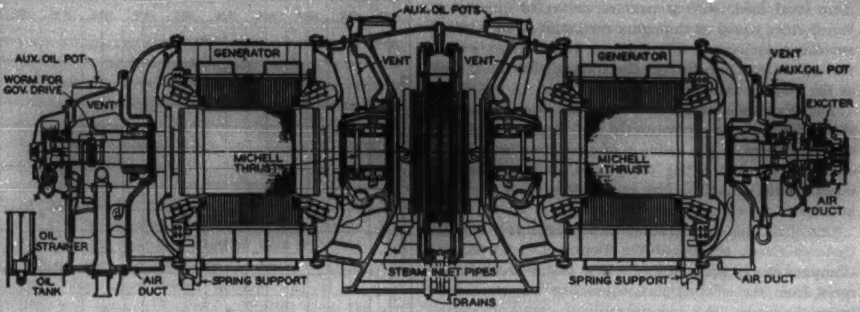


Figure 1. Sectional view of 53,000-kw 3,600-rpm superposed turbine


AUX. OIL POTS
AUX. OIL POT
WORM FOR GOV. DRIVE
VENT
GENERATOR
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AUX. OIL POT
EXCITER
MICHELL THRUST
MICHELL THRUST
AIR DUCT
OIL STRAINER
OIL TANK
AIR DUCT
SPRING SUPPORT
STEAM INLET PIPES
SPRING SUPPORT
AIR DUCT
DRAINS

Figure 2. Sectional elevation of Ljungstrom turbine and generators

So far, gas turbines have attained capacities of 4,000 kw.[93]

MERCURY CYCLE

There have been no additional mercury boiler installations for power generation, although the Sun Oil Company has installed another boiler used in oil distillation process. Considerable trouble developed with the Kearny, N. J., and Schenectady, N. Y., boilers described in the 1933 report of this committee. The Kearny boiler is now being completely replaced. The design of the new boiler is similar to water-boiler design in that there is only one drum. "Porcupine" tubes with internal cores and close clearances have been eliminated. The convection surface is new in principle in that instead of being completely filled with a liquid it carries a mixture of liquid and vapor, resulting in a marked reduction in the amount of mercury required per unit of heating surface. As a result of the elimination of tube cores, however, the total amount of mercury in the boiler is somewhat increased. In the redesign the water-wall furnace has been eliminated, resulting in a marked improvement in over-all efficiency. New materials now available and more suitable for the operating conditions involved are being utilized in the various parts of the heating surface. Operating practice, and particularly chemical treatment of mercury in the boiler, has advanced considerably. The mercury turbine generators have been very satisfactory, and the only recent change in design involves improvement of the turbine glands to reduce air leakage to a minimum.

Electrical-Plant Practice

GENERATING-STATION SWITCHING

Since 1933 few new steam generating stations have been built requiring low-voltage power distribution, with the accompanying bus-layout problems. Large new stations have been mainly of the hydroelectric type, far removed

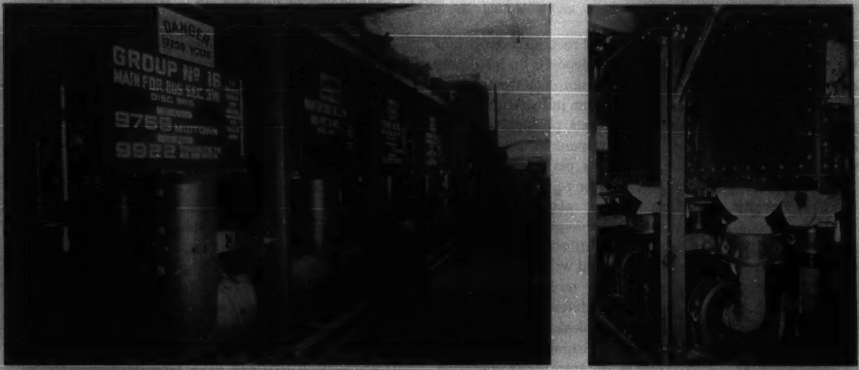
DANGER
13200 VOLTS
GROUP No 16
MAIN FDR BUS SEC. W.
DISC. SWS.
9759 MIDTOWN
9922

Figure 3. Metal-enclosed feeder bus equipment; the method of supporting and insulating the bus is shown in the construction view at the right

from local load, with generators connected directly to transformer banks or through a minimum of low-voltage switching equipment with outdoor high-voltage switch yards for the connection to the transmission lines, such as the Boulder Dam development.[109]

The generator-voltage switching facilities of several stations have been reconstructed and modernized due in some instances to the addition of generating capacity or extensive interconnecting of systems, and in others to interruptions caused by fires in the older types of switch houses.

One of the first large stations to be modernized was the Connors Creek station of the Detroit (Mich.) Edison Company.[98] In this instance a new switch house, separated from the existing station building was erected to house the modern bus and apparatus.

In the last few years this topic has become of such interest that at the AIEE winter convention in January 1939 the committee on power generation held a session on the rehabilitation of the various stations of the Consolidated Edison Company of New York,[117] the Essex generating station of Public Service Electric and Gas Company of New Jersey[119] and the L Street station of the Boston Edison Company,[116] together with a paper on the general topic.[118] The papers presented at this meeting describe in detail the problems that were present at the various locations, the aim of reconstruction programs, and the methods used by the various designers to obtain the results desired. The authors in agreeing on certain principles show clearly the present trend of switch house construction:

1. Complete relay protection.
2. Improved arrangement of equipment.
3. Limitation of capacity connected to one bus section, so that a fault will not remove too great a proportion of the station capacity.
4. Better physical segregation of bus sections and oil circuit breakers, by means of smoke and fire barriers, coupled with forced ventilation.
5. Reduction of oil content of breakers.
6. Use of improved fire-fighting equipment.
7. Improvement as to control, segregation, and grounding.

As the length and extent of several recent interruptions were in part due to smoke and soot deposits on insulators and busses of portions of the station not directly involved in the fault, great pains have been taken in these rebuilding programs to segregate indoor sections physically. Barriers, without direct doors or other openings between sections, are desirable if the conditions permit. Access to sections is obtained from corridors or rooms free of electrical apparatus, which also may provide expansion space for the dissipation of pressures generated by explosions. With large-capacity ventilating systems to clear the faulted section of fumes, and with adequate fire-fighting equipment present, these modernized switch houses have a strong second line of defense if, in spite of improved protective schemes, an explosion or fire occurs. Recent tests demonstrating the capabilities of fire-fighting equipment and aiding in the training of operating forces have permitted more assurance in their effectiveness. Equipment of both the carbon-dioxide type and of the fine-water-spray or mist type, in both portable and permanently piped systems, has been used.

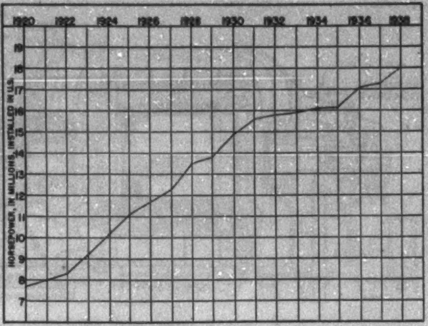

Figure 4. Growth of water-wheel installations in all classes of hydraulic plants in the United States

One of the most interesting recent developments is the nonmagnetic-steel-tube-enclosed gastight bus of the Consolidated Edison Company of New York,[117] which allows phase segregation and high carrying capacity in limited space. A number of installations of the more conventional metal-enclosed bus structures with metal breaker cubicles have been made, and in many such instances fault bus protection has been adopted, although differential protection is in use in the majority of cases.

ELECTRICAL EQUIPMENT

There has been a marked improvement in apparatus and equipment since the 1933 report. Extensive tests on oil circuit breakers with the modern arc-extinguishing devices have produced a breaker both electrically and mechanically more reliable than was heretofore available.

Opening times of eight cycles are now standard, and breaker speeds of five and three cycles may be obtained when necessary.

Many old oil circuit breakers have been equipped with the new arc-extinguishing devices and higher-speed mechanisms to gain in interrupting capacity and reduce the time to the present AIEE standard.

The tendency to reduce the oil content of breakers has caused a general interest and demand for oilless breakers, which has resulted in an increased use of available designs and new developments by the manufacturers. Breakers of the air De-ion type are in service with up to 750,000-kva interrupting capacity; breakers of the hydro-blast type are in service rated at 500,000-kva interrupting capacity with orders placed for capacities up to 1,500,000 kva; and breakers using compressed air as an arc-extinguishing medium are on order with interrupting capacity of 500,000 kva and are designed with ratings up to 1,500,000 kva.

Disconnecting switches have been developed for large capacities, mainly of the high-pressure type, using button or line contacts, or positive clamping action, all with silvered contacts; tests have shown them superior to those previously available for continued service at full loadings. High-capacity busses show a trend away from the flat-bar configuration toward the tubular, rectangular, or double-channel iron forms. Busses both with and without insulation in addition to the porcelain supports are used.

Manufacturers' research in test methods, increased laboratory facilities, and the accompanying design improvements as regards impulse strength, together with the work of the transformer subcommittee of the AIEE committee on electrical machinery, have made data available in the past few years so that the station designer may grade the insulation for surges in addition to normal frequencies, although the time is as yet too short to cause any agreement among operators as to the best co-ordination of insulation between line, transformers, breakers, and bus.

TRANSMISSION OF BULK POWER

Interconnection of large systems for bulk power interchange has continued which, with the hydroelectric-station development, has resulted in numerous 220-kv lines, the highest commercial voltage in the country being the 287 kv of the lines from Boulder Dam.[106] This interconnection of systems has caused further stability problems, which, together with the desire to reduce the damage caused by faults and lighten the effect on customers' services, has resulted in the development of high-speed relaying, both of the distance and balanced type, and the carrier-current and pilot-wire type; these types of relaying are being applied to transmission of all voltages. Relay times of one cycle or less are available, which, combined with five- or three-cycle breakers, give rapid clearing of faults where such extreme speed is necessary. To aid in permitting systems to ride through overhead line disturbances, the use of the Petersen coil, rapid reclosing of breakers, and discharge tubes is increasing.

The use of high-voltage cable for transmitting large blocks of power has increased, approximately 79 miles having been installed for 66 kv and above since 1933.

STATION AUXILIARIES

The majority of the steam additions to existing plants use electrical auxiliaries. Higher-pressure boilers have increased the demand for higher-speed boiler-feed pumps, and the trend is toward constant-speed or multi-speed motors for both this service and the forced- and induced-draft fans. Constant-speed squirrel-cage induction motors have been built as large as 2,000 horsepower for feed-water pumps and up to 700 horsepower in the wound-rotor type. Three-phase supply, 2,400 volts, is quite standard for this type of auxiliary, and the factory-built metal-enclosed bus structure is in general use, the lift type of breaker being popular. Air circuit breakers of the 5,000-volt 100,000-kva class are now available from several manufacturers for auxiliary station service, and as many consider it just as desirable to reduce oil contents of boiler rooms as of switch houses, the air breakers show increasing use. Noninflammable liquid, first applied to distribution transformers, is now used in power transformers and is particularly adaptable for auxiliary service in station locations where oil would be a hazard. More study is being made to ensure the continuity of auxiliary service, of obtaining separate sources of supply, and complete protection of auxiliary buses. The increasing use of pulverized fuel, with its intricate and delicate starting procedure, has introduced additional problems in

Table I. Government Hydroelectric Projects

Government Agency	Project	State	Ultimate Capacity (Horsepower)	Initial Capacity (Horsepower)	Installed Capacity (Horsepower)	Capacity on Order (Horsepower)
U. S. Bureau of Reclamation	Grand Coulee	Wash.	2,742,000	478,000		478,000
U. S. Engineer Corps	Bonneville	Wash.	665,000	137,000	137,000	148,000
U. S. Bureau of Reclamation	Shasta Dam	Calif.	515,000	412,000		412,000
Imperial Irrigation District	Drop 4	Calif.	26,600	13,300		13,300
U. S. Bureau of Reclamation	Boulder Dam	Nev. & Ariz.	1,835,000	515,000	752,000	230,000
U. S. Bureau of Reclamation	Elephant Butte	N. M.	34,500	34,500		34,500
U. S. Bureau of Reclamation	Casper-Alcova	Wyo.	45,000	45,000		45,000
U. S. Bureau of Reclamation	Green Mountain	Colo.	30,000	30,000		30,000
U. S. Bureau of Reclamation	Colo.-Big Thompson	Colo.	135,000	*		
U. S. Engineer Corps	Fort Peck	Mont.	100,000	*		
Platte Valley Public Power & Irrigation	Dist. Platte Valley	Nebr.	35,000	35,000	35,000	
Loup River Public Power District	Loup River	Nebr.	65,000	65,000	65,000	
Central Neb. Public Power & Irrigation District	Tri-County	Nebr.	54,000	26,000		80,000
Grand River Dam Authority	Pensacola Dam	Okla.	100,000	80,000		80,000
Lower Colo. River Authority	Buchanan Dam	Texas	51,900	34,600	34,600	
Lower Colo. River Authority	Tom Miller Dam	Texas	20,000	20,000		20,000
Lower Colo. River Authority	Inks Dam	Texas	16,000	16,000	16,000	
Tenn. Valley Authority	Norris	Tenn.	132,000	132,000	132,000	
Tenn. Valley Authority	Pickwick	Tenn.	330,000	110,000	110,000	
Tenn. Valley Authority	Wilson	Ala.	620,000	260,000	260,000	
Tenn. Valley Authority	Wheeler	Ala.	360,000	90,000	90,000	90,000
Tenn. Valley Authority	Guntersville	Ala.	136,000	102,000		102,000
Tenn. Valley Authority	Chickamauga	Tenn.	144,000	108,000		108,000
Tenn. Valley Authority	Hiawassee	N. C.	160,000	80,000		80,000
Tenn. Valley Authority	Gilbertsville	Ky.	350,000	*		
Greerwood County Power Authority	Buzzards Roost	S. C.	30,000	22,200		22,200
South Carolina Public Service Authority	Santee-Cooper	S. C.	213,000	*		213,000

* Work started, but no information on initial horsepower available.

continuity of service during system disturbances, and undoubtedly more thought will be given to either ensuring the maintenance of auxiliary-bus voltage or automatic starting of the pulverized-fuel equipment.

Hydroelectric-Plant Practice

Annual water-power figures are beginning to look like those for 1928 and 1929, predepression years. During 1938 about 750,000 horsepower of new capacity went into service, bringing the total capacity of water wheels to around 18,000,000 horsepower for the United States, as shown by figure 4. At the present time in the United States over 2,000,000 horsepower of water-wheel capacity is under construction and work has been started on other projects representing an ultimate installed capacity of 750,000 horsepower for which the power units have not yet been ordered.

A large part of this capacity is in plants being built by governmental agencies. There are, however, many projects being developed by public-utility and industrial companies, particularly in the northeastern and midwestern sections of the country. In 1937 and 1938 work was started or completed on over 60 of these projects, representing an initial capacity of 500,000 horsepower.

Notable among public-utility projects are the four 26,000-horsepower units for 110-foot head in the Claytor development of the Appalachian Electric Power Company on the New River, Virginia; the 77,000-horsepower unit for 188-foot head installed in the Kerr power house of the Rocky Mountain Power Company on the Flathead River in Montana; the 14,700-horsepower and the 8,400-horsepower units for 91-foot head in the Grandfathers Falls power house of the Wisconsin Public Service Corporation; and the 10,950-horsepower unit for 45-foot head being installed in the Solon development of the Central Maine Power Company. Several other projects have been put into operation or under construction by public-utility companies in Maine, Vermont, New York, Virginia, Wisconsin, and other states.

In point of installed capacity, water-power projects being built by governmental agencies exceed all others. These, including those that have been built and on which work has been started, represent an ultimate capacity of over 9,000,000 horsepower, or one-half of the total installed in the United States. Nearly 3,500,000 horsepower of this is for Bonneville and Grand Coulee plants on the Columbia River, 526,000 horsepower in California, and 1,835,000 horsepower at Boulder Dam (see table I).

These projects represent about two-thirds of the total ultimate capacity. Of the remaining third over 2,000,000 horsepower is represented by projects of the Tennessee Valley Authority on the Tennessee River, leaving only about 1,000,000 horsepower in other parts of the country. TVA projects include Wilson Dam plant with space for 610,000 horsepower, built before that organization was created by Congress.

By June 1939, only 1,631,500 horsepower had been installed in Government financed projects since 1932, and this includes 260,000 horsepower in Wilson Dam plant. For these plants there are on order machines having an aggregate capacity of 2,000,000 horsepower of which 1,250,000 horsepower is for Grand Coulee, Bonneville, Shasta, and Boulder plants, in sections normally supplied by water power.

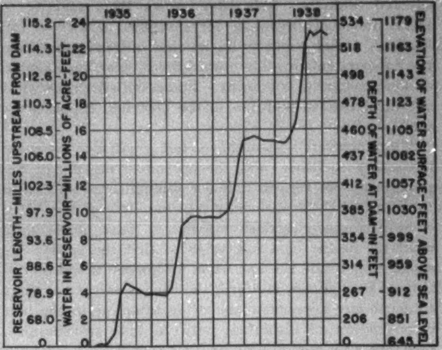

Figure 5. Rate at which Boulder Dam reservoir has filled since closing of the by-pass tunnels in 1935

Outside of Bonneville development, all the large governmental water-power developments in the Pacific Coast area are essentially water-supply and river-regulation projects from which large amounts of power can be generated, much of which will be required to pump water for municipal supply and irrigation. Grand Coulee is the largest of these and in fact is the largest in the world on which reliable data are available. It includes a straight concrete gravity-type dam 4,500 feet long, 553 feet high, and 500 feet thick at the base.

This dam will back the water up the river 150 miles to the Canadian border to form a reservoir with 9,645,000 acre-feet maximum storage and a maximum head of 360

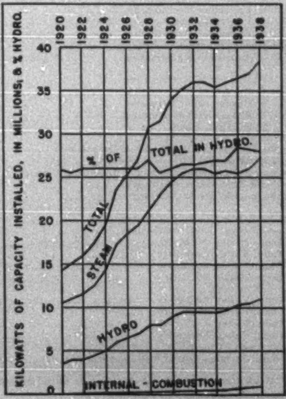

Figure 6. Growth of installed generating capacity in both privately and Government-owned public-utility plants of all classes in the United States

feet. A useful storage of 5,200,000 acre-feet will be available with 80 feet drawdown. At the midstream section of the dam will be a 1,650-foot spillway designed to pass 1,000,000 cubic feet per second. Between the ends of the spillway and the shores, two power houses will be built, each with nine 150,000-horsepower main turbines connected to 108,000-kw unity-power-factor generators. In addition, the west power plant will house three 14,000-horsepower service units.

Including the three service units, the total ultimate capacity of Grand Coulee plant is 2,742,000 horsepower, which is 900,000 horsepower more than planned for Boulder plant and 500,000 horsepower more than planned for the Beauharnois development on the Saint Lawrence River in Canada, which now has 483,000 horsepower installed.

Just above one end of the Grand Coulee dam and at right angles with it will be a pumping plant containing 12 vertical 65,000-horsepower synchronous-motor-driven pumps. Each pump will have a capacity of 1,000,000,000 gallons per day when pumping against a 295-foot head. These pumps will lift water from the main reservoir into a secondary reservoir formed by a dam across the lower end of the Grand Coulee, from where the water will flow to irrigate 1,200,000 acres of land. Six generators in the power house adjacent to the pumping plant will supply power to the pump motors. Three main generating units and two service units are now on order and are scheduled for initial operation in 1941.

Part of the power from the Shasta development now under construction on the Sacramento River in California will be used for pumping irrigation water. This project will have an ultimate capacity of 500,000 horsepower, of which four 103,000-horsepower units to operate under a 450-foot head are on order.

So much has been written about Boulder Canyon project that one hesitates to say more. But this project has so many interesting features that a report on water-power development would not be complete without calling attention to some of them. Construction work was started in 1931 and operation began in 1936. The project has four major purposes; namely, flood control, supply water for irrigation and municipal use, and power generation.

In the power house, which is designed for an ultimate capacity 1,835,000 horsepower, 752,000 horsepower has been installed and two 115,000-horsepower units are on order. These will give this plant the largest installed capacity of any hydroelectric project in the world, and the only steam plant to exceed its present capacity is the Hudson Avenue station, Brooklyn, N. Y., which has 770,000 kw installed. Eventually, 400,000 horsepower of pumping load on the Metropolitan Water District aqueduct, running from the Colorado River to Los Angeles, Calif., will be supplied by Boulder Dam. Initial installation of this aqueduct goes into operation this year.

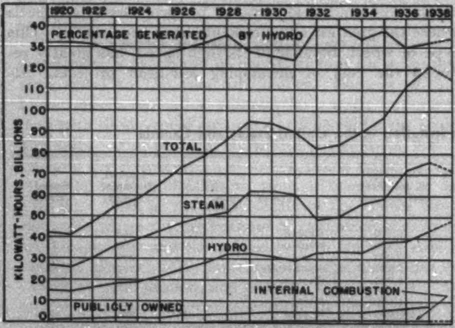

Figure 7. Growth of power generated in the United States in privately and Government-owned public-utility plants, and the relation between total generation and that produced by water power

Figure 8. This 42,500-horsepower Kaplan-type propeller runner has its blade surfaces machined and polished all over

As figure 5 shows, Boulder Dam, which is 730 feet high and forms a reservoir of 30,500,000 acre-feet capacity, has prevented several floods that would have been disastrous on the lower river, and has also prevented irrigation-water shortages. The sharp rise in the curve each year during May and June shows how severe the floods are. Last year during this period 6,000,000 acre-feet of water was stored. This is equivalent to a continuous flow of 55,000 cubic feet per second for two months, in addition to the water permitted to flow down the river. If average water supply is obtained in the river this year the reservoir should be filled to capacity.

These water-power developments by governmental agencies naturally raise the much discussed question of hydraulic versus steam plants and what effect these new hydraulic plants are having on the relation of hydroelectric and steam-electric installed capacity and generation. In spite of all the activity in water-power development by governmental agencies, the growth in hydroelectric generating capacity was more rapid before 1932 than since then, when the Government seriously entered the field, as is evident from figure 4.

For example, for the seven years from 1925 to 1931, inclusive, installed water-power capacity increased nearly

Figure 9. Modern type hydraulic-turbine-governing unit combines all parts of the governing system into a single cabinet with the exception of the servo-motors and generator for driving the actuator

5,500,000 horsepower, where from 1932 to 1938, inclusive, only 2,250,000 horsepower has been installed. Of course it must be admitted that the depressed condition of business has been a factor in the latter. But, if we compare only the three years from 1936 to 1938, inclusive, with the nine years from 1923 to 1931, inclusive, the latter shows a considerably steeper rise than the former. An even more important factor than the depression in slowing up water-power growth is that efficiency of steam plants has increased and their cost of construction decreased to where in many localities they now seriously challenge water-power developments on an economic basis.

An outstanding example of steam-plant construction in a water-power region is the new 160,000-kw plant of the Buffalo, Niagara and Eastern Power Corporation, being installed at Oswego, N. Y., in a region largely served by water power. Installation of this steam plant was not dictated by any shortage of water-power sites, for the company controls several within easy reach of where it is being built. When all factors were considered, economic and otherwise, the steam plant proved to be the most desirable. The city of Eugene, Ore., which operates its own power system and is only 160 miles away from the Federal Government's Bonneville hydroelectric plant, announced plans for a new peak-load steam or Diesel plant during the latter part of 1938. In 1937 the Tennessee Electric Power Company won the right from the State Public Service Commission to install a steam plant at Nashville, Tenn., by proving that this type of plant could supply power to its system more economically than it could be obtained from the Government-subsidized TVA hydroelectric system. Since then this plant has been sold to TVA.

Figure 6 shows the total generating capacity installed in all types of power plants operated by both privately and Government-owned utilities. Also the division of this capacity between steam, hydraulic, and internal-combustion-engine stations. A curve has also been added to show the relation that installed hydroelectric capacity bears to the total. This relation has remained remarkably constant since 1920. In this period installed hydroelectric capacity has increased only 2.1 per cent, or from 25.9 to 28 per cent of the total installed in all classes of plants.

Figure 7 shows power generated by the private and public agencies represented by figure 6. An interesting feature about these curves is that the per cent of total power generated by water power was practically the same (36 per cent) for 1937 as for 1920. Because of the total power load falling off sharply in 1938, and an abundant rainfall in practically all parts of the country, the per cent of water power showed an increase to about 37.5 per cent. These two factors, load growth or loss and rainfall, have a marked influence, and account for most of the peaks and valleys in the water-power curve. From the foregoing it is evident that as yet the relation between fuel-burning and hydraulic plants continues on a practically even keel. The curves in figures 4, 6, and 7 are plotted from data published by the Federal Power Commission, with the exception of 1938 for which figures published by *Electrical World* are used.

Even though Francis-type wheels have been built in unprecedented size and power during recent years, it is in the low-head field covered by propeller-type wheels where the greatest advancements have been made. This type of wheel, because of its high speed, has greatly reduced the cost of developing low-head projects. Al-

Table II. Large Propeller-Type Water Wheels Installed in the United States and Canada

Name of Plant	State or Province	No. of Units	Rating (Horsepower)	Effective Head (Feet)	Discharge (Cubic Feet per Second)	Type of Runner	Runner Diameter (Inches)	Speed (RPM)
Bonneville	Ore. & Wash.	2	66,000	66	12,000	Kaplan	280	75
Bonneville	Ore. & Wash.	2	74,000	66	14,400	Kaplan	280	75
Pickwick Landing	Tenn.	2	55,000	47	12,000	Kaplan	290	81
Wheeler	Ala.	2	45,000	48	9,000	Fixed blade	264	85.7
Safe Harbor	Pa.	6	42,500	55	8,000	Kaplan	220	109.1
Chickamauga	Tenn.	3	36,000	36	11,000	Kaplan		75
Guntersville	Ala.	3	34,000	36	10,000	Kaplan	265	69.2
Rock Island	Wash.	4	21,000	32	7,150	Manually adjusted blade	223	100
Louisville	Ky.	8	13,500	37	4,000	Fixed blade		100
Seven Sisters	Manitoba	3	37,500	66	5,700	Fixed blade	200	138.5
LaGabelle	Quebec	5	32,000	60	5,400	Fixed blade	195	120
Great Falls	Manitoba	6	28,000	56	5,000	Fixed blade	200	138.5
Chat Falls	Ontario & Quebec	8	28,000	53	5,300	Fixed blade	195	125
Island Falls	Saskatchewan	5	19,000	56	3,500	Fixed blade	145	163.6

Figure 10. Outdoor-type plant at the Wheeler Dam project of the Tennessee Valley Authority has two 45,000-horsepower units, with space for six more

though developed less than 25 years ago, these units have come into wide use in both North America and Europe, in both large and small size. Table II gives data on some of the largest units installed in the United States and Canada. This type of wheel has been developed in three forms: fixed blade; Kaplan, the blade angle of which is adjusted by the governor; and the type on which the blade angle is automatically adjusted by change of water flow through the runner as the load changes. With the fixed blade design, its efficiency curve falls off quite sharply either side of maximum, but by adjusting the blade angle, high efficiency can be obtained over a wide range of load and head.

This type wheel has permitted an increase in specific speed from a maximum of about 90 rpm for a Francis-type wheel to 150 rpm and higher. Because of their higher speed, cavitation assumes far greater importance than with slower-speed runners, particularly for the larger-size units. This problem is handled in two ways, first by locating the runner at an elevation below tailwater level that will limit cavitation to what is considered a safe amount. Then in the construction of the runner vanes, the areas where pitting because of cavitation is expected are chipped out and these filled in with stainless steel by welding, after which in some instances the blades are machined all over, ground, and polished, as in figure 8. Any pitted areas that occur in operation are also chipped out and welded in with stainless steel. Experience has shown that stainless steel will stand the action of cavitation better than any other.

Welding is assuming an ever-increasing importance, not only in the maintenance of water wheels but also in their construction. For example, in the Bonneville propeller-type units, practically all parts, with the exception of the runners and shafts, were constructed by welding.

Governors for hydroelectric units have been improved in their ability to control speed and have been radically changed in appearance. Where a few years ago the actuator and valves were in one place, the oil-pressure pumps in another, and the pressure tanks in still another, the modern governor has these parts along with all instruments and indicators combined in a single unit, as in figure 9. On the front of this cabinet actuator are a speed and frequency indicator, draft-tube vacuum gauge, penstock-pressure gauge, duplex gauge showing generator-brake-system air pressure and brake-cylinder pressure, duplex gauge showing auxiliary and main relay valve port positions, speed-droop indicator with knob directly below, gate-limit and gate-position indicator with gate-limit knob directly below, speed-adjustment indicator with control knob directly below, manual generator-brake-valve control knob, control knob for transferring from main to auxiliary valve control and vice versa, cabinet-light switch and pilot lamp, emergency shutdown switch and pilot lamp.

Outdoor-type hydroelectric plants, which 10 or 15 years ago were attracting considerable attention, have been adopted in only a few installations. Wheeler Dam plant of TVA, figure 10, is an outstanding example.

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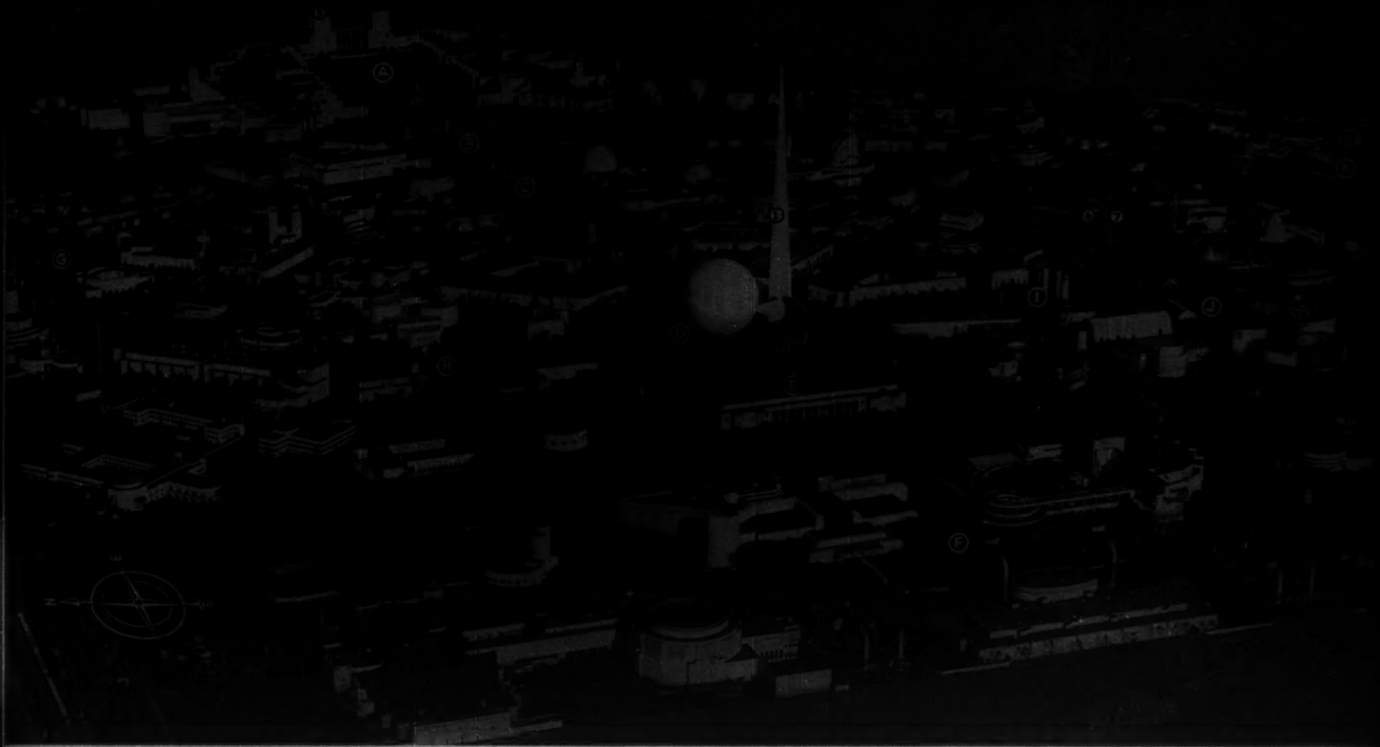

Courtesy N. Y. World's Fair

Figure 1. An air view showing most of the 1,216-acre area of the New York World's Fair, and essentially all of the main exhibit area. The focal points marked are: (A) Court of Peace, government area; (B) Lagoon of Nations fountain; (C) Constitution Mall, with the Four Freedoms plaza (toward the Lagoon) and the heroic George Washington statue facing the Manship fountain and the Perisphere; (D) Theme Center with surrounding plaza; (E) New York City Building, one of the permanent structures; (F) transportation zone, marine and air at the right, automotive at the center, railroads at the left; (G) Bowling Green plaza; (H) Court of Communications; (I) Court of Power; (J) Plaza of Light, electrical-manufacturing and utilities zone; (K) amusement zone starts here and extends to the right. To help readers to orient themselves, the points from which the eight color views were photographed are indicated by numerals, which correspond to the plate numbers

Lighting the New York World's Fair

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MEMBER AIEE

THE CONVERSION of more than 1,200 acres of salt marsh and waste land into the New York World's Fair, with its stimulating review of the world of today and its effective preview of the "World of Tomorrow," was a remarkable achievement, accomplished within the short span of three years. In this $150,000,000 transformation, electric light played a major role. Also, the Fair provided the stimulus that brought from the laboratories into commercial production some of the most significant developments ever made in the field of artificial illumination.

The magnitude and magnificence of the New York World's Fair resulted from the combined efforts of many minds and countless hands, all deserving of commendation. Accomplishments, however, mirror capable direction, for which special mention is made of Chairman Stephen F. Voorhees of the Fair's Board of Design, and Colonel John P. Hogan, chief engineer and director of construction. Illumination was headed by Bassett Jones, consulting engineer to the Board of Design, working with J. S. Hamel and the author as lighting consultants to the construction department. The problem of developing the spectacular displays for the Fair was delegated to a Committee on Displays which reported to the Board of Design. This committee was headed by Bassett Jones, and included Professor Jean Labatut, design; Howard Cooper, hydraulics; and the author, lighting. Operation of spectacles was headed by A. K. Morgan, director of displays.

GENERAL FEATURES

The exhibit area (figure 1) comprising about 1,000 acres, contains more than 200 structures, most of which are windowless exhibit buildings. A zoning and color scheme adopted prior to construction insured architectural unity, and harmony of plan, design, and treatment throughout the whole area. Seven classified zones (foods, governmental, distribution, communication, transportation, community interests, and business administration) are distributed around the Theme Center, with streets radiating from the center and crossed by circumferential avenues. The color scheme, also zoned, but on a basis of aesthetic values rather than occupancy zoning, is co-ordinated with the physical layout. Starting with white at the Theme Center, color treatments of red, blue, or gold radiate outward with progressively more saturated hues. Adjoining hues blend circumferentially along the avenues. By virtue of the proper selection of accents and relief tones, each structure presents a color scheme of interest in itself as well as appropriate to the general picture. The illumination was so fitted into this scheme as to maintain the basic pattern by night as well as by day, but with a new and added interest and charm after sunset (see color plates).

Thus illumination and lighting effects became definite elements in the architecture of Fair buildings. As part of the general plan, the exterior surfaces of the buildings themselves serve as major sources of illumination for adjacent street and plaza areas (figures 41–56; color plates). Brightly lighted entrances, niches, and murals, and backlighted plaques aid in effectively achieving this result. With few exceptions, street lights are used to complete the decorative effects rather than as primary sources of illumination (figures 68, 69).

In building interiors, unobtrusive built-in fixtures in general provide simple decorative effects and good aisle lighting with increased intensities at entrances and exits (figures 57–67). This allows exhibit lighting to be arranged to suit individual requirements.

To give the Fair a pleasingly varied skyline at night and to produce a desirable air of festivity, several tall and brilliantly lighted architectural features such as the pylons were provided for in the illumination scheme (color plates and figures 32–40). Also, the various courts and plazas contain suitable small architectural forms combined with the elements of water, light, and flame to produce scintillating animated effects by virtue of the combination of new elements and materials rather than by mere cyclic color variations. Finally, there were the outstanding "spectacles"; the Perisphere-Trylon, Constitution Mall, the Lagoon of Nations, the Court of Peace with its canopy of searchlight beams, and the combined light and fireworks display in Fountain Lake in the amusement area.

Obviously the successful completion of such a gigantic plan required not only the most exacting co-ordination and the closest co-operation among the many architects, engineers, constructors, landscapers, and others involved, but also the creating of new structural forms and new types of lighting equipment. To provide the necessary focal point for technical co-ordination, a test laboratory was established on the Fair grounds in the earliest stages of its development. This served as a medium of contact for the many different interested agencies, and also afforded facilities for research and the study of illumination sources and their possible uses.

Experiments performed in this laboratory served as a stimulus for the commercial development of many new light sources which at that time were found only in the lighting research laboratories. Also many entirely new lighting devices were designed there for the effective application of these new sources. Typical instances are the now well-known low-voltage fluorescent lamp and the high-intensity mercury capillary lamps, which were first used extensively at the New York World's Fair. Architectural elements to utilize new and existing light sources also were developed, or inspired by scale models giving a visible concept of their possibilities.

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* Member of the firm of Morgan, Hamel, and Engelken, lighting consultants to the New York World's Fair 1939.

To insure architectural harmony and unity of effect, all general structural plans, including exterior lighting for private exhibit buildings as well as Fair projects, were first submitted for approval to the World's Fair Board of Design. Thereby the master plan was consistently sustained.

Space limitations preclude adequate description of any appreciable proportion of the many points of engineering and illumination interest throughout the Fair grounds. Therefore, the scope of this article is limited primarily to the "spectacles" which constitute the backbone of the "picture" presented by the Fair at night, and secondarily to typical details embracing the effective application of new light sources, including the material and equipment involved in those applications.

Theme Center

Because of the importance of the Perisphere-Trylon Theme Center in the general scheme of the Fair, special efforts were exercised to make it as unusual in its nighttime illumination, as it is unique in daytime by virtue of its novel architectural design (plates II and IV). Aside from aviation warning lights at its apex and at points on its sides, the 700-foot Trylon is illuminated only by spill light from adjacent structures.

The Perisphere, a 180-foot spherical structural part of the Theme Center, presented a lighting problem of unprecedented nature and proportions. The illuminating-engineering job of making an appropriate and attractive nighttime spectacle out of this huge ball paralleled the mechanical-engineering job of delicately poising a 4,600-ton steel-framed concrete "bubble" over a shallow mirror-pool of water. The exterior lighting problem involved facilities for flooding this sphere with suitable intensities of either amber, red, or blue light, and the further problem of projecting moving cloud patterns over its three-acre surface (plate IV). Even the floodlighting had to be accomplished by lens-controlled light-beam projection, because the roofs of the nearest buildings on which lighting units could be placed were some 250 feet away (figure 13).

Based upon extensive experimenting with scale models using a six-foot-diameter wooden ball painted white as a model, the lighting sequence adopted is an amber light thrown on as dusk approaches, subsequently fading into a sunset red, and then into a deep evening blue after dark. Water jets underneath the Perisphere, supplemented by blue floodlights beneath the surface of the reflecting pool, and white cloud-shapes projected onto the Perisphere combine to impart the desired effect—that of a translucent bubble poised over the surface of its reflecting pool.

Three types of projectors are used for the exterior floodlighting of the Perisphere. For the amber and red light, projector units of the Fresnel type fitted with 5,000-watt incandescent lamps are used (figure 5). Banks of 8 of these units, 4 red-beam projectors and 4 amber, are mounted on the roof of each of the 5 buildings selected as projection points, making a total of 40 units (figure 2).

The production of an adequate intensity of blue light on the extensive surface of the Perisphere presented a major problem in illumination and required the development of a new light source and related equipment for its use. Because of the blue deficiency of an incandescent light source, a 400-watt high-intensity short-arc mercury capillary lamp was developed and special projectors were designed for the

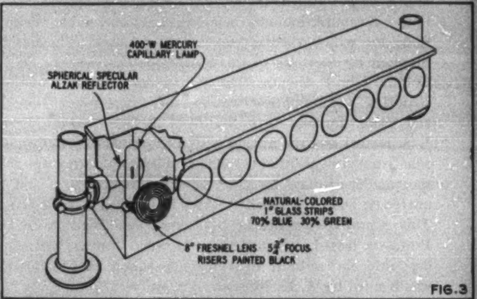

400-W MERCURY CAPILLARY LAMP
SPHERICAL SPECULAR ALZAK REFLECTOR
NATURAL-COLORED 1" GLASS STRIPS 70% BLUE 30% GREEN
8" FRESNEL LENS 5¾" FOCUS RISERS PAINTED BLACK

FIG. 3

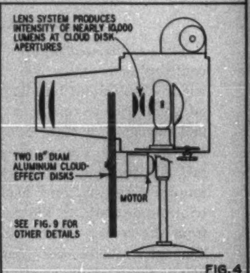

LENS SYSTEM PRODUCES INTENSITY OF NEARLY 10,000 LUMENS AT CLOUD DISK APERTURES
TWO 18" DIAM ALUMINUM CLOUD-EFFECT DISKS
MOTOR
SEE FIG. 9 FOR OTHER DETAILS

FIG. 4

FIG. 2

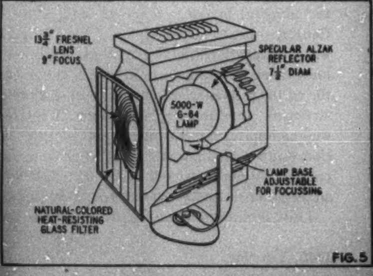

13¾" FRESNEL LENS 9" FOCUS
SPECULAR ALZAK REFLECTOR 7½" DIAM
5000-W G-64 LAMP
LAMP BASE ADJUSTABLE FOR FOCUSSING
NATURAL-COLORED HEAT-RESISTING GLASS FILTER

FIG. 5

Figures 2–5. The three-acre exterior surface of the Perisphere is illuminated in any of three colors by 30 nine-lamp projectors using mercury capillary lamps with Fresnel lenses ribbed at the back to give a 10-degree horizontal spread (3) and 40 5-kw incandescent projectors, 20 red and 20 amber (5), drifting clouds are produced by 30 2.5-kw incandescent cloud projectors (4). This equipment is installed on the roofs (2) of five buildings around the Theme Center job. Each of these projector units accommodates nine of the new 400-watt mercury lamps, with suitable light-control optics (figure 3). Blue light for the underside of the Perisphere is provided by 96 blue-filter submerged projectors (similar to those of figure 17) using the new 100-watt mercury capillary reflector lamps.

Cloud projection, relatively simple with the 6-foot model, became another major problem when applied to the three-acre surface of the 180-foot Perisphere. To produce the misty cloud effect desired, an illumination intensity of nearly 10,000 lumens through the projector gate and objective lens is necessary. The resulting intense heat in the projector gate precludes the use of any photographic emulsion or ordinary painted slide; in fact, even the regular mica cloud-machine disks buckled and cracked from the heat. By experiment it was found that aluminum disks containing irregularly shaped apertures backed by small pieces of mica could be used, and that a heat-resisting paint could be applied to the mica to provide gradation in shadows. In the final installation, two such

Figures 6–12. Inside the Perisphere (11, 12) "Democracity" is illuminated by a continuous strip of floodlight projectors underneath the lowe platform (6); another continuous strip illuminates the horizon (6). Sky lighting is provided by a seperate row of projectors (10); moving clouds are thrown across the sky by special cloud projectors (9) and their companion shadows across the landscape by special shadow projectors (8). The night scene is complete with stars (7) representing the principal constellations as they appeared in nature. The motion-picture murals (12) are produced by ten special projectors (11, unlabeled unit)

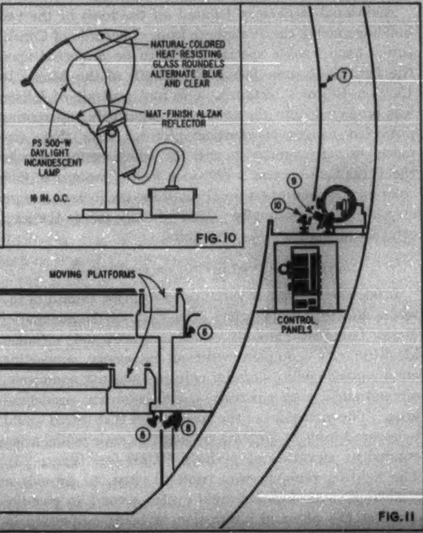

NATURAL-COLORED HEAT-RESISTING GLASS ROUNDELS ALTERNATE BLUE AND CLEAR
MAT-FINISH ALZAK REFLECTOR
PS 500-W DAYLIGHT INCANDESCENT LAMP
16 IN. O.C.
7
9
10
CONTROL PANELS
MOVING PLATFORMS
6
6
8

FIG. 10

FIG. 11

Courtesy Henry Dreyfuss

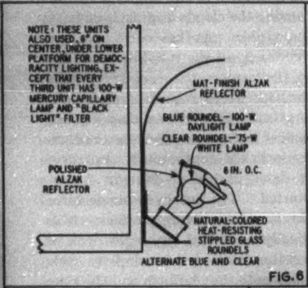

NOTE: THESE UNITS ALSO USED, 6" ON CENTER, UNDER LOWER PLATFORM FOR DEMOCRACITY LIGHTING, EXCEPT THAT EVERY THIRD UNIT HAS 100-W MERCURY CAPILLARY LAMP AND "BLACK LIGHT" FILTER
MAT-FINISH ALZAK REFLECTOR
BLUE ROUNDEL – 100-W DAYLIGHT LAMP
CLEAR ROUNDEL – 75-W WHITE LAMP
8 IN. O.C.
POLISHED ALZAK REFLECTOR
NATURAL-COLORED HEAT-RESISTING STIPPLED GLASS ROUNDELS
ALTERNATE BLUE AND CLEAR

FIG. 6

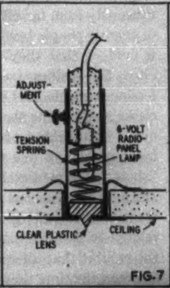

ADJUSTMENT
TENSION SPRING
6-VOLT RADIO-PANEL LAMP
CLEAR PLASTIC LENS
CEILING

FIG. 7

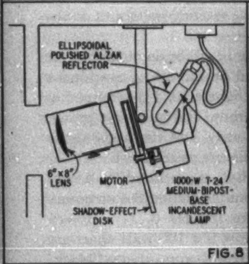

ELLIPSOIDAL POLISHED ALZAK REFLECTOR
6"×8" LENS
MOTOR
1000-W T-24 MEDIUM-BIPOST-BASE INCANDESCENT LAMP
SHADOW-EFFECT DISK

FIG. 8

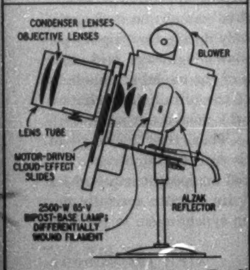
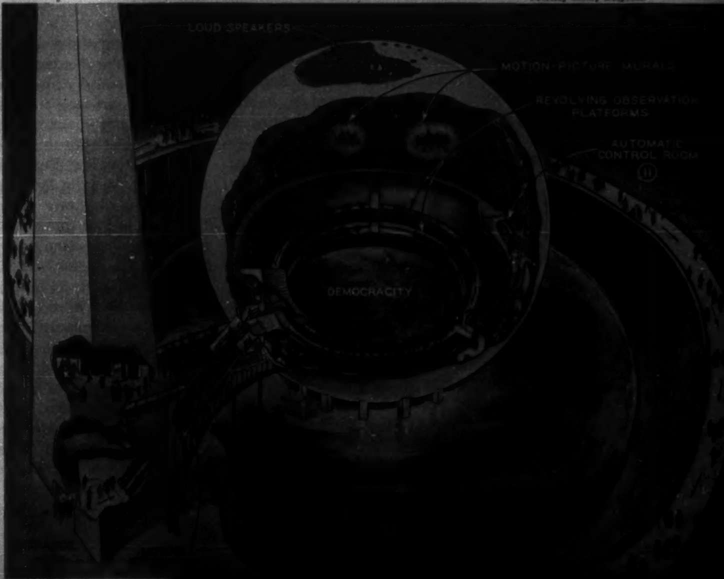

CONDENSER LENSES
OBJECTIVE LENSES
BLOWER
LENS TUBE
MOTOR-DRIVEN CLOUD-EFFECT SLIDES
2500-W 65-V BIPOST-BASE LAMPS; DIFFERENTIALLY WOUND FILAMENT
ALZAK REFLECTOR

FIG. 9


LOUD SPEAKERS
MOTION-PICTURE MURALS
REVOLVING OBSERVATION PLATFORMS
AUTOMATIC CONTROL ROOM
11
DEMOCRACITY

mica-backed aluminum disks rotating at different speeds are used in each projector (figures 2, 4). Thirty of these cloud-projectors, each using a 2,500-watt high-color-temperature tungsten lamp, are used with overlapping patterns to cover the whole visible area of the Perisphere.

All projectors—amber, red, and blue floodlighting units and cloud machines—are connected to reactance-dimmers and preselective control equipment which permit any cycle of lighting to be used, and provide for automatic control of the 383 kw of exterior Perisphere lighting.

Additional projectors, located on the roofs of the two buildings facing the Perisphere from either side of Constitution Mall, enable special effects to be projected upon the Perisphere area facing the length of the Mall. In 1939 on Edison's birthday, a 75-foot portrait of Edison was projected onto the sphere by additively superposing projected images of identical pictures from these two machines. By similar means, the whole eastern face of the Perisphere became a 180-foot jack-o'-lantern in true color and configuration on Halloween; the jack-o'-lantern even winked occasionally. Undoubtedly this is the largest colored "movie" ever attempted.

INTERIOR ILLUMINATION OF PERISPHERE

Inside the Perisphere is the central theme exhibit of the whole Fair—"Democracity, The City of Tomorrow"—Henry Dreyfuss' carefully scaled and intricately detailed idealistic city, complete with all necessary supporting environs and facilities for agriculture, industry, commerce, air and surface transportation, and suburban residential area. The viewpoint of the spectator is that which would be secured from an airplane circling this scale model at an equivalent elevation of perhaps 10,000 feet (figure 12). The lighting problem was twofold: first, to provide a realistic 24-hour day-and-night cycle; second, to give the observer the physical impression of being suspended in space with the earth beneath him and the sky overhead extending to distant horizons.

The illumination problems encountered in the accomplishment of the desired results in the Perisphere interior, which is actually a large integrating sphere, were found to be even more exacting than those encountered on the outside. Frequent, rapid, and smooth changes in lighting effects are required, as well as exact synchronization of these visual effects with recorded sound effects ranging from voice to music; all automatically controlled in predetermined accurately timed repetitive cycles.

Spectators enter the exhibit through the base of the Trylon, and by means of the two longest (96 feet and 120 feet) electrically operated moving stairways built to date are carried up either 48 feet or 60 feet, respectively, to the lower or upper of two continuous electrically operated moving platforms that carry the spectators at a predetermined speed around the inside periphery, just below the equator of the Perisphere globe.

If the spectator happens to arrive during the day scene, he sees below him in bright "sunlight" the miniature scale City of Tomorrow and its supporting countryside stretching to far horizons and blending realistically with the "sky." Above him is the tremendous arched dome of the Perisphere, impressive in its resemblance to the sky, floodlighted unobtrusively in blue, giving the impression of the heavens complete with moving clouds and their companion shadows drifting across the buildings and landscape. The scene gradually changes, shadows actually lengthen to afternoon proportions, daylight fades smoothly and realistically into dusk, and the sky gradually becomes a midnight blue. Above in the seemingly limitless depths of the vaulted dome appear stars, including those accurately representing the major constellations as they appeared in nature on the night of April 30, 1939, when the Fair first opened. Night clouds drift across the sky. In the meantime, lights have appeared gradually and unobtrusively in the city, in the neighboring industrial area, and in the nearby community of small homes. As the night cycle progresses, the countryside becomes dark except for a few lighted windows and the illuminated highways.

Against a background of appropriate music, the voice of a commentator explains the symbolism of the changing scenes, and helps the spectator to note and follow the many and realistic changing effects. At one stage of the sequence, voices become audible in the distance and simultaneously from far off among the clouds approaching hosts of singing people become visible. As they come nearer, it is evident that they represent all the inhabitants of the city—the business men, the farmers, the workers, the professional people, and the housewives. As the groups draw closer and assume their greatest proportions, they descend out of sight behind a bank of horizon clouds as the concluding sequence of one cycle in the continuously repetitive "show." Immediately dawn and then daylight follow, and another cycle has started. The time cycle of spectator transit around the globe on the revolving platforms is so related to the automatically controlled day-night cycle of the exhibit that all spectators may view a complete cycle between the time they step onto the platform and the time they find themselves back to the starting point and on the way out.

The lighting of the sky dome in either daylight blue or deep night blue is accomplished by the use of continuous rows of concealed strip lights (figures 10, 11) arranged in two circuit groups—a blue group and a white group. The blue lights are on continuously, and the white lights vary from black-out to full brilliance as required.

The lighting for "Democracity" is produced by a continuous row of strip lights mounted under the structure that carries the lower platform (figures 6, 11). Every third unit in that continuous row is a 100-watt short-arc mercury capillary lamp equipped with appropriate "black-light" filter. These "black lights" remain on during the entire show, but are effective only during the "night" period of the cycle, when the windows, highways, and other apparently "illuminated" parts of the model are made to fluoresce under the influence of the ultraviolet radiation from these lamps by virtue of the fluorescent paint with which these parts are coated. Thus, the "lighting" in Democracity at night actually is entirely fluorescent.

Principal features of the special equipment required for the Perisphere interior "show" are the ten specially designed projectors that operate in synchronism to produce the 18- by 30-foot motion-picture "murals" in the "sky" (figure 12). These machines use Bantam-size glass slides in place of the successive film frames of ordinary motion-picture projectors. In each machine, a series of these slides is projected through four optical systems in succession. The slides are mounted on two 50-inch ring gears which revolve within the optical systems. Through a shutter arrangement in front of the objective lenses the slide images are cross-dissolved from one optical system to the next, producing a smooth motion with 8 images per second (as compared with 16 or 24 for standard motion-picture projectors). As intensity of light on the screen remains constant but for absorption by the slides, there can be no flicker regardless of the number of frames per second. It is a noteworthy achievement that the projections from the four individual optical systems of each machine can be registered so accurately in superposition on a screen 165 feet away as to produce a composite result of smooth and realistic motion. Each projector uses four 2,500-watt 65-volt lamps having differentially wound filaments similar to those used in the cloud machines, and is fitted with $f/2$ objective lenses of 8-inch focal length, the largest ever built for such a purpose. To keep all ten projectors in perfect synchronism, each is driven by a Selsyn motor equipped with a mercury damper; all in turn are driven by a common Selsyn generator.

The cloud projectors for the Perisphere interior are similar to those already described for the exterior, with the exception that a lower illumination intensity at the gate permits the use of tempered glass cloud slides (figure 9). Two six-inch cloud slides in each machine are mounted in frames eccentrically driven to produce a rolling effect of the projected clouds.

The entire Perisphere theme show—including the time, music, earth, sky, and voice elements—is entirely automatic, and timed to split-second accuracy. The various operations, including the many lighting circuits and ten special motion-picture projectors, involved in a typical operating cycle of 336 seconds are under the control of motor-driven cams which operate through one revolution and then stop for a few seconds awaiting the initiating impulse which starts each new show cycle. The single signal impulse which initiates each new cycle automatically originates from a notch cut in the sound film. Thermionic tubes constitute the nerve center of this equipment, controlled by the cams and in turn regulating saturable-core control reactors in each of the lighting circuits.

THEME-PLAZA ILLUMINATION

The desired composition of the vista looking toward the Theme Center from any of the five thoroughfares radiating therefrom involved the problem of preserving the full effect of the blue light and cloud effects on the Perisphere. In general, this result was accomplished by keeping all objects of relatively high brightness out of the field of view, and by effecting as much control as possible over the quantity of stray or spill light that would tend to fog the atmosphere and thus dilute the color effect of the Perisphere. The levels of illumination on the exteriors of exhibit buildings were kept low in the vicinity of the Theme Center, grading to higher intensities farther away. Brightly lighted pylons, fountains, and other accents were kept at the opposite ends of connecting courts and thoroughfares.

Around the Theme Plaza itself, the illumination of building façades is accomplished chiefly through the use of back-lighted sculptures (figure 49). These provide spots of visual interest and also provide adequate illumination for adjacent walks. Remaining illumination, aside from reflected light from the blue sphere itself, is provided by luminescent trees activated by mercury light.

Constitution Mall

Connecting the Theme Center and the Lagoon of Nations and constituting a portion of the main backbone of the Fair is Constitution Mall, a 2,000-foot tree-shaded esplanade built around a series of rectangular lagoons and reflecting pools (figure 1). Principal architectural feature of the Mall is the 55-foot statue of George Washington, which is lighted only by spill light from structures on either side of the esplanade. Nevertheless it is as prominent and effective a part of the nighttime picture of Constitution Mall as it is of the daytime vista. The statue is the work of the noted sculptor James Earl Fraser, designer of the AIEE Edison Medal. Other architectural features of the Mall are the Manship sundial, fountain, and associated sculpture group just off the Theme Plaza, and the "Four Freedoms" statues (representing freedoms of press, religion, assembly, and speech). Like the Washington statue, the Freedoms statues are illuminated only by spill light.

The lighting of the Mall creates a strikingly beautiful night effect (plates I, II). Along either side of the Mall for its entire length is a double row of 50-foot luminescent trees which constitute an effective part of the view after sundown and at the same time provide all illumination necessary for the main walkways. Supplementary lighting for walks adjacent to exhibit buildings is provided by light from building entrances, and from illuminated murals, plaques, and niches on the sides of the buildings.

Underneath each of the trees along the Mall, one of the new 250-watt short-arc mercury capillary lamps is set into the ground, in a suitable fixture fitted with a cylindrical metal collar which projects above the ground line for the dual purpose of controlling the side light and of protecting the fixture from physical damage (figures 13, 19). Under the influence of the peculiar radiation from these flood lamps, the leaves, twigs, and branches of the trees become luminous with a soft greenish glow, producing a beautiful effect. The same type of lighting also is applied to the trees in the Theme Center Plaza, the Court of Communication, and the Court of Power (figure 1). In all, some 360-odd trees are so lighted.

WATER LIGHTING

Another attractive feature of the lighting composition of the Mall is a variety of water-lighting effects, chief of which is the animated play of colored light and water in the Manship fountain at the head of the Mall (figure 13). The Manship fountain and associated sculptures are intended to connote the passage of time (indicated by the sundial itself) and the moods of time affecting the human race. This latter effect is simulated by the alternate floods of gold light and blue light which seem to pour in repetitive cycles from three apertures in the abutment of the sundial terrace and flow in successive waves down the entire length of the pool in the layer of mist which overlies the pool. These effects are created by a cyclic play of light upon three columns of water which pour from the sundial abutment and upon curtains of mist overlying the pool. During the operation of the fountain, the columns of water flow continuously, the volume magnified by an ingenious arrangement (figure 16) that produces in actuality a relatively thin-walled cylinder of water 20 inches in diameter. Waterproof lighting units built into the central portion of the discharge "megaphones" of this fountain introduce into each water column alternate cycles of blue and gold light which grade smoothly from dark to maximum brilliance and back to dark for each color. In the pool of the fountain (figure 13) a set of continuous striplights (figure 17), on the center line of each of the three water columns, extends for a distance of approximately 115 feet. Flanking each of these striplights is a

Figures 13–19. The main esplanade of Constitution Mall (13, plate I) consists of reflecting pools lined with small illuminated water jets (17, plate II) and enlivened by several small back-lighted cascades (18). Broad tree-shaded promenades along either side are illuminated by the trees themselves, which luminesce under the influence of radiation from mercury capillary units, one underneath each tree (19). At the west end of the main esplanade is the Manship fountain with special mobile effects produced with water and light (13, 16, 17). Various illuminated murals (15) add decorative touches along the Mall, and in other parts of the grounds. The principal plazas are lighted by special projector standards (14) which concentrate the light on the ground, keeping it out of the range of vision

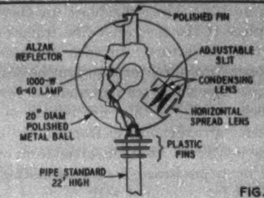

POLISHED FIN
ALZAK REFLECTOR
ADJUSTABLE SLIT
1000-W G-40 LAMP
CONDENSING LENS
20" DIAM POLISHED METAL BALL
HORIZONTAL SPREAD LENS
PLASTIC FINS
PIPE STANDARD 22' HIGH

FIG. 14

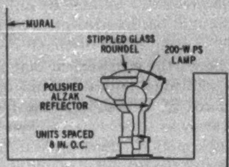

MURAL
STIPPLED GLASS ROUNDEL
200-W PS LAMP
POLISHED ALZAK REFLECTOR
UNITS SPACED 8 IN. O.C.

FIG. 15

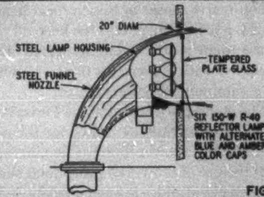

20" DIAM
STEEL LAMP HOUSING
TEMPERED PLATE GLASS
STEEL FUNNEL NOZZLE
SIX 150-W R-40 REFLECTOR LAMPS WITH ALTERNATE BLUE AND AMBER COLOR CAPS

FIG. 16

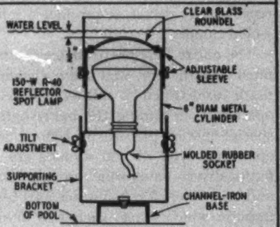

WATER LEVEL
CLEAR GLASS ROUNDEL
½"
ADJUSTABLE SLEEVE
150-W R-40 REFLECTOR SPOT LAMP
8" DIAM METAL CYLINDER
TILT ADJUSTMENT
MOLDED RUBBER SOCKET
SUPPORTING BRACKET
BOTTOM OF POOL
CHANNEL-IRON BASE

FIG. 17

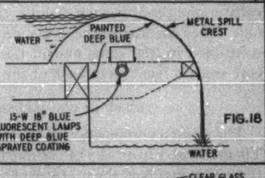

WATER
PAINTED DEEP BLUE
METAL SPILL CREST
15-W 18" BLUE FLUORESCENT LAMPS WITH DEEP BLUE SPRAYED COATING
WATER

FIG. 18

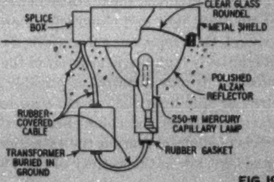

SPLICE BOX
CLEAR GLASS ROUNDEL
METAL SHIELD
POLISHED ALZAK REFLECTOR
RUBBER-COVERED CABLE
250-W MERCURY CAPILLARY LAMP
RUBBER GASKET
TRANSFORMER BURIED IN GROUND

FIG. 19

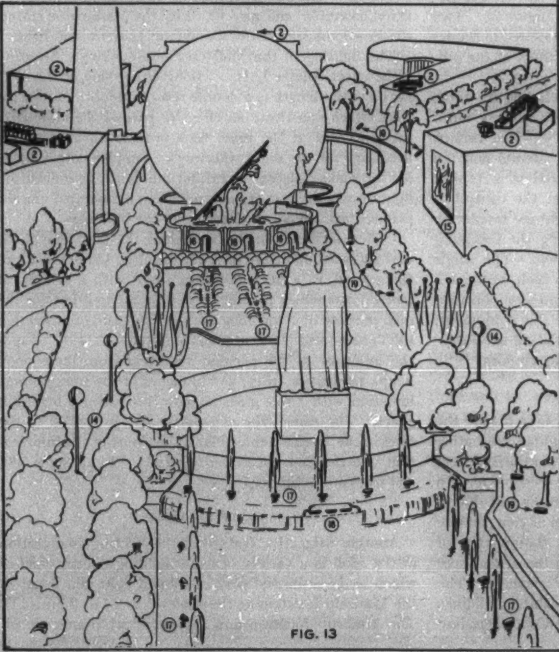

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FIG. 13

row of atomizer nozzles which produce three parallel spray curtains and a supplementary layer of mist over the pool generally. As in the three main water columns, two colors—blue and gold—are provided in each line of striplights by fitting each alternate projector with a gold or blue filter. The alternate cycles of blue and gold light that progress down these strips in accordance with a prearranged sequence are synchronized with the corresponding colors in the main water columns. The two color circuits for the column lights as well as for the associated atomizer strips are connected to a special cycle-dimmer arranged to produce the effect of, first, a flow of one color, followed by a flow of the second color, then a flow of both colors combined. The effect begins with the main column lights and continues down the length of the pool with a rate of color flow of approximately ten feet per second and with alternate colors based on five-second intervals. The control device used is a brush-type flasher operating in combination with a motor-driven bank of variable autotransformers. These control the intensity as well as the switching of the separate color circuits, which are brought out at ten-foot intervals to groups of the striplights.

Along both sides of the center pool of the esplanade group and across the head of the pool at the base of the George Washington statue are vertical water jets projected to a height of about eight feet above the surface of the reflecting pools (plate II; figure 13). Each of these jets is individually lighted from beneath the water surface by a single 150-watt reflector lamp (figure 17). Across the head of the pool at the base of the Washington statue is a cascade electrically illuminated from behind (figures 13, 18). A similar back-lighted cascade heads the next pool of the group. The third pool, nearest the Lagoon of Nations, has no lighting features of itself, but serves rather as a reflecting pool in which the fountain display and the Court of Peace searchlight-canopy are mirrored (plate III).

The two plazas of the Mall—George Washington and the Four Freedoms—are lighted by means of special stanchions (figures 13, 14) topped by lighting units fitted with optical systems which project light down to the surfaces of the roadway and walkway, eliminating dispersion.

Lagoon of Nations Display

Dominant and popular lighting spectacle of the Fair, the Lagoon of Nations combines the elements of water, light, sound, flame, and fireworks in one mammoth display. Regardless of weather the evening performances of this "show" (plates I, VIII) regularly have packed thousands of Fair visitors into every available vantage point from which the spectacle could be viewed. Centrally located in the main exhibit area, the Lagoon forms a pivotal focus of interest, by day as well as by night, flanked to the west by Constitution Mall leading to the Theme Center, and to the east by the Court of Peace with its phalanx of government buildings (figure 1).

The Lagoon is an elliptical pool some 800 feet long and 400 feet wide. This area, approximately five acres, originally was a slough over which a supporting mat covered by a coating of gravel had to be laid. At the center of the pool is a wooden platform 400 feet long and 200 feet wide, mounted on piling and normally about two feet under water. This submerged platform supports the array of piping, wiring, and equipment required by the fountain itself and also supports four cylindrical kiosks 24 feet in diameter and 7 feet high which house groups of sound projectors as well as miscellaneous electric and hydraulic-valve control equipment. The plan view (figure 27) shows the "three ring" arrangement of the fountain and indicates the general arrangement of more than 1,400 water-jet nozzles, 133 gas-flame nozzles, 285 fireworks mortars, and 552 special light boxes.

At the center of each of the three circular sections of the fountain is a group of nozzles capable of projecting a column of water 150 feet into the air. Equally spaced around the concentric rings of each section are nozzles capable of projecting water jets to a height of 75 feet. Each of these 552 heavy-duty nozzles has its own twin-unit projector box for light and color effects (figures 27, 28). Miscellaneous other nozzles provide for a wide variety of water configurations. A border of graceful shape is provided around the triple operating center of the fountain by approximately 1,100 feet of strip lighting which illuminates a spray screen produced by atomizer nozzles (figures 27, 28).

PLANNING AND ARRANGEMENT

The physical arrangement and relationship of these various elements were determined through the medium of extensive studies of small-scale working models, and of experimental equipment both in miniature and full size. In the experiments with full-scale working models, several important observations were made which had a direct influence on the design of the lighting and control equipment. For example, it was found that the effect of mercury-vapor lighting of the tall water jets was especially desirable, not alone because of the rich and intense blues, greens, and other cool shades thus obtainable, but also because of the scintillating and animated "sparkle" imparted to the myriad particles of water by the stroboscopic effects of the mercury light. Also, many interesting and hitherto unobtainable shades of colors were found to be attainable by combining certain of the colors derived from the mercury lamp with other colors derived from the incandescent light sources.

Probably one of the most significant experimental observations was the extent to which color values obtained from incandescent sources were enriched by direct color-filtering of the lamp with its filaments burning at maximum temperature, as compared with color effects produced by blending the primary filtered colors from incandescent sources with conventional dimmer-control and consequent subnormal filament temperatures.

TWIN-PROJECTOR LIGHTING UNITS

The necessity for color control of the mercury light sources led to the development of an effective mechanical color-shifting and dimming device, which, as just intimated, also lends itself effectively to color control and dimming of incandescent light sources. Thus, to provide the range and combination of colors that make the fountain display famous, a special semisubmersible twin-projector lighting unit was developed. Each of these units (figures 21, 22) utilizes a 1,500-watt concentrated-filament incandescent lamp and a 400-watt mercury capillary lamp. Each light source in each unit is fitted with a suitable reflector. Around each reflector is a specially devised six-sided revolving color drum, the sides of which support natural-colored glass filters, open frames, or blank metal sheets, in selected sequence. (The trade term "natural-colored" refers to color throughout the thickness of the filter glass, as compared with surface treatment.) These color drums enable effective mechanical dimming of the incandescent light as well as of the mercury light, and permit not only combinations of various colors of the two sources, but also the use of any of the several colors or tints from either of the two sources independently of the other. The complete twin unit, including incandescent lamp, mercury lamp, and all associated driving and control equipment, is mounted in a waterproof sheet-metal tank approximately 28 inches long, 28 wide, and 27 deep.

Each color drum is separately driven by a three-phase 208-volt $^1/_{75}$-horsepower 1,800-rpm synchronous motor at a speed of one revolution per minute. This speed requires a ten-second interval to pass from one color into another, or into black-out, and was determined by test and experiment to be the most desirable for smooth blending.

The problem of keeping several drums operating in multiple and with color filters always in the same relative positions was solved by a magnetic braking system that brings all drums on any circuit to a quick stop—actually within $1^1/_2$ revolutions of the motor shaft. The power for braking is supplied by a separate 60-volt d-c source, and the motor circuit is so arranged that whenever motors are disconnected from their a-c power source, two phases of the motor instantly and automatically are short-circuited and connected to one side of the d-c circuit, the other side of the d-c braking circuit being at all times common with the third a-c phase wire. In addition to the d-c braking feature, all color drums on any circuit are arranged to be resynchronized automatically at the black-out position of the color drum. This feature requires the use of a small relay, together with a cam and limit switch for each motor.

The colors and their relative positions as actually selected for the mercury projector of each twin unit are: clear, light blue, dark blue, black-out plate, "surprise" pink, and black-out plate. For the incandescent source they are: clear, yellow, blue-green, black-out plate, red, orange. The 1,500-watt screen-grid incandescent lamp has a rated efficiency of about 25 lumens per watt, or a total output of 37,500 lumens. The 400-watt mercury capillary lamp has a rated efficiency of about 45 lumens per watt, or a total output of about 18,000 lumens. Because actual tests indicated as much as 65 per cent light absorption for a one-inch depth of the brackish water available for use in the Lagoon, underwater placement of the twin lighting units was abandoned, and each unit so placed as to project approximately six inches above normal water line. Thus the glass covers of these twin units are subjected not only to the battering of the heavy deluge of falling water, but also to severe heat stresses, particularly over the incandescent unit. The narrow-beam reflectors so concentrate and localize the light beam that temperatures as high as 425 degrees Fahrenheit have been measured on the surface of the glass covers of the boxes directly above the incandescent lamps. A solution to this problem finally was achieved through the use of specially tempered plate-glass panels having exceptionally high transmission efficiency for the infrared rays. Standard heat-treated glass was found to be satisfactory above the mercury lamps.

To produce a mist or "curtain" effect around the base of the entire fountain display, there is a continuous row of atomizing nozzles. Illumination of this mist curtain is accomplished by continuous strip lighting. These strips are made up with receptacles spaced six inches on center to accommodate some 1,800 of the new 100-watt *R*-40 reflector-type lamps divided into four color groups—clear, amber, green, and red. Each reflector lamp is set in a separate adjustable cylindrical sheet-metal housing (figure 23) and provided with a natural-colored glass roundel.

FOUNTAIN CONTROLS

All elements of the Lagoon display—water, light, sound, flame, and fireworks—are remotely controlled from bench-type switchboards in a control room atop one of the government buildings overlooking the Lagoon (figures 24, 25). Water valves are selectively operated from small pilot switches which energize solenoid valves in the kiosks; these valves in turn admit city water to large hydraulically operated valves at the nozzles (figure 25). Water nozzles and associated twin-unit light projectors are placed to enable the formation of a variety of fountain patterns.

Control of the twin-light projectors is accomplished through contactor panels in the kiosks that in turn are operated from the control room. Lighting controls correspond to the water controls, so that groups of lights may be operated selectively with their associated water nozzles in a wide variety of patterns. In all, there are 19 main lighting-group controls (figure 24). Color control of these projectors is accomplished through close and accurate control of the synchronous motors driving the color drums.

The different switches on the panels in the control room permit independent operation of either mercury or incandescent color drums, facilitating ready combination of a wide variety of color tones. The grouping of a number of drums on one color switch (as many as 63 per switch) reduces to 19 the number of pairs of color-selection controls actually required for the 552 twin units on the Lagoon platform. An ingenious circuit arrangement permits the drum-control switches to be preset to any color, and then operated either individually or from master switches at the desired moment. An outstanding result of this method of color control is the subtle, almost imperceptible, transition from one color to another—an effect aided materially by the previously mentioned experimental selection of the optimum (ten-second) interval for a color translation.

Plate I (facing page). Constitution Mall, scenic backbone of the exhibit area, presents a picture of rare beauty at night. This view, looking east from high in the Trylon during the evening fountain show in the Lagoon of Nations, shows how the various effects are combined into one spectacular display; note also subdued illumination near the Theme Plaza (foreground)

Courtesy Westinghouse

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Courtesy Westinghouse

Plate II. Fluorescent trees themselves serve as the source of soft, evenly distributed illumination for the broad promenades along either side of Constitution Mall as well as around the Theme Plaza (figures 13, 19). Small illuminated jets frame the central reflecting pools (figures 13, 17)

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Plate III. Pencil-like searchlight beams etch the night sky forming a canopy over the Court of Peace. Typical of plaza areas, the court is illuminated only by light from adjacent building facades, supplemented by a peripheral row of decorative street lights (C, figure 68)

Courtesy Westinghouse

Courtesy Westinghouse

Plate IV. Unprecedented in magnitude was the problem of evenly illuminating the three-acre exterior of the Perisphere. Special projectors mounted on adjacent rooftops provide any of three colors (amber, red, or blue) and project clouds drifting realistically across its surface (figures 2–5)

Plate V. The 800-foot Lagoon of Nations, Constitution Mall, and the Theme Center dominate this view looking west from the roof of the Federal Building which encompasses a large portion of the exhibit area. Brightly lighted pylons and other accents add sparkle to the scene

Courtesy Westinghouse

Courtesy Westinghouse

Plate VI. Decorative pylons representing man's first-known elements (earth, fire, water, air) form the southern terminus of the Court of Power (figures 37, 39). Floodlighted building façades constitute the principal source of illumination for streets and walkways

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Plate VII. Clustered around the Plaza of Light south of the Court of Power are several electrical-industries exhibit buildings. The small fountain in the foreground illustrates how centers of interest are provided in small plazas and courts throughout the grounds

Courtesy Westinghouse

Corresponding to the 19 main lighting-group controls already mentioned, there are 19 panels in the central control board (figure 24). One of the dials on each panel controls the mercury units, and the other controls the incandescent units, throughout the corresponding group of twin-projector units associated with that panel. Each control dial is so arranged that it may be rotated in either a forward or a reverse direction to any preselected color position. The arrangement of circuits and associated relay and other equipment is such that the associated group of color drums will rotate correspondingly to the selected color position. At the same time, the motor-driven pointer on the control panel immediately behind each dial accurately follows and at all times shows the position of the associated color drums. This motor-driven indicator is a duplicate of the motor, limit switch, and cam arrangement that is associated with each color drum inside the twin units on the fountain platform, and the circuits provide identical d-c braking and synchronizing control.

Control of the striplights for the border curtain of illuminated mist is accomplished through the medium of motor-driven variable autotransformers located in the kiosks, but operated from the central electrical control board. All gas burners are electrically operated from the control room and are ignited by a small pilot flame which is kept burning, in spite of the deluge of falling water, by a 10,000-volt spark ignition system. Some 280 fireworks mortars are controlled electrically through connections to central patching-panel boards in the kiosks. A total of 50 firing positions is available at jack outlets above the mortar jacks, and jumpers permit any mortar or combination of mortars to be plugged into any firing position. A step-by-step relay energizes one after another of the firing jacks in response to the closing of the "fire" button on one of the main control panels in the control room.

In a separate pump room (figure 26) convenient to both the Lagoon and the control room are motor-driven centrifugal pumps totaling some 2,700 horsepower and capable of furnishing approximately 100,000 gallons per minute. Arranged to comprise an operating exhibit in themselves, these pumps supply the Lagoon fountains. Pumps and associated equipment lend themselves to a flexibility of control (figure 25) and a promptness of response that adds to the effectiveness of the fountain "shows."

Power for the lights and equipment located on the submerged Lagoon platform is transmitted from the main distribution board near the pump room to the kiosks by 11 700,000-circular-mil three-phase feeders. All wiring in the Lagoon consists of ordinary rubber-covered conductors, with so-called watertight insulation. All conductors, including those of control circuits, are merely laid in the water and strapped to the deck of the submerged platform without any additional protection.

FOUNTAIN OPERATION

Once each evening, just at dark, a 15-minute prearranged "show" is presented in the Lagoon, combining the various elements appropriately to render an unusual form of composition that has proved to be an ever-popular attraction (plates I, VIII). The music, played by a 42-piece band in a studio in an exhibit building, is picked up stereophonically by two microphones, relayed by wire to the control room where it is amplified and monitored, and then transmitted over two separate channels to the loud-speakers in the four kiosks on the submerged fountain deck. Each diagonally opposite pair of kiosks is served by one of these channels, thus giving a two-point stereophonic effect along both axes of the Lagoon. Each kiosk contains four high-frequency and two low-frequency speaker units, totaling 500 watts in audio output capacity. This is one of the first large stereophonic installations. The sound system has a total audio output of 2,000 watts.

Split-second synchronization of the various elements during the 15-minute nightly "shows" is accomplished with the aid of specially developed "cueing" machines, one located at the top of each of the triple-unit central control boards (figure 25) and one located in the music studio. These ingenious machines are in effect special adaptations of the machinery of curve-drawing electrical measuring instruments, with the curve-drawing elements removed. Basically, each machine consists of a synchronous-motor-driven mechanism that draws the cue sheet at 60 inches per minute under a wire marker which indicates the cue instant. Each cue sheet consists of a roll of graph paper, marked off in two-second intervals, on which are plotted against the time axis all the cues and special instructions necessary for the particular group of operators served by the particular cue machine. The several operators translate the cues into the actual operation of the various control switches, including the firing of the fireworks mortars. All "cueing" machines are started and stopped simultaneously by a master switch in the central control room. By means of this system, all operations are accomplished within a fraction of a second of the predetermined time schedule and the productions have been kept so uniform that their accuracy has become a byword.

Some of the same general type of equipment is used in Fountain Lake where a spectacular element appropriate to the amusement area is furnished by the mammoth fireworks display that follows the nightly Lagoon show. The feature of this display is its mobility. Pumps, light projectors, fireworks mortars, and all such equipment are distributed among several barges which may be shifted to provide different effects, subject to control from shore through equipment similar to that used for the Lagoon.

Court of Peace

The searchlight canopy over the Court of Peace constitutes the third and easternmost portion of the feature lighting spectacle of the Fair that begins at the Theme Center (plates I, III, V). This canopy of searchlight beams gives a third dimension to this part of the nighttime picture, producing the visual impression of a raftered or coffered ceiling over this whole 450- by 900-foot area, which is flanked on either side by the pavilions of foreign governments and dominated at its easterly terminus by the $3,000,000 United States Government Building.

Plate VIII (facing page). The nightly fountain "shows" in the Lagoon of Nations ingeniously combine the elements of water jets, light, color, flame, fireworks, and sound into one harmonious whole. Part of the spectacle are the thousands of visitors who nightly jam every vantage point for this wholly unusual and enormous display

Courtesy Westinghouse

FIG. 20

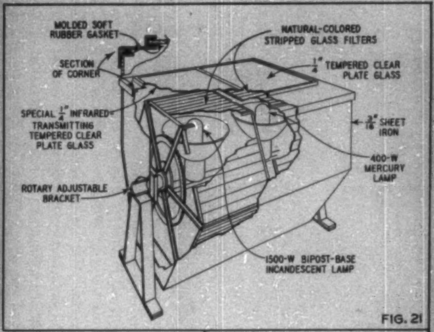

MOLDED SOFT RUBBER GASKET
SECTION OF CORNER
SPECIAL 1/4" INFRARED-TRANSMITTING TEMPERED CLEAR PLATE GLASS
ROTARY ADJUSTABLE BRACKET
NATURAL-COLORED STRIPPED GLASS FILTERS
1/4" TEMPERED CLEAR PLATE GLASS
3/16" SHEET IRON
400-W MERCURY LAMP
1500-W BIPOST-BASE INCANDESCENT LAMP

FIG. 21

Figures 21, 22. Twin-unit semisubmersible projectors illuminate each main jet of the fountain. Both lamps are burned at full brilliance, color changes and dimming being accomplished by six-sided color drums with selected filters mounted in their sides, which are rotated around the lamp and reflector units

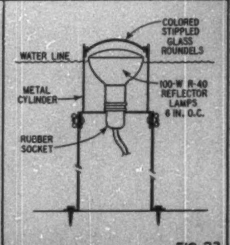

COLORED STIPPLED GLASS ROUNDELS
WATER LINE
100-W R-40 REFLECTOR LAMPS 6 IN. O.C.
METAL CYLINDER
RUBBER SOCKET

FIG. 23

Figure 20. The maze of water nozzles, lighting units, gas burners, fireworks mortars, and associated fountain equipment in the Lagoon of Nations is strikingly illustrated in this view which shows workmen preparing some of the equipment for the winter layover

Figure 23. Some 1,800 of these reflector-lamp units illuminate the spray from the continuous row of atomizer nozzles around the outer border of the fountain (27, 28)

FIG. 24

Figures 24, 25. Twin control units (24) provide complete control over the twin-unit projectors (21, 22). A separate board (25) controls the water and gas-flame jets. Co-ordinated control of the various elements of the fountain display is achieved by several synchronized cue machines (25, top)

FIG. 26

Figure 26. A circular pump room, near one end of the Lagoon, itself a display feature with its 2,700 horsepower in 11 motor-driven centrifugal pumps, supplies a maximum of 100,000 gallons of water per minute to the fountain

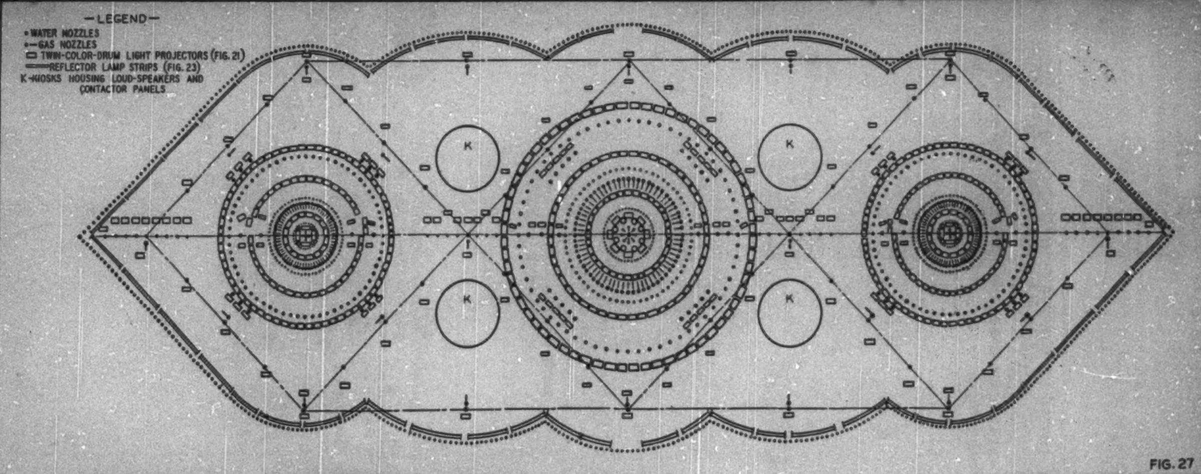

—LEGEND—
• WATER NOZZLES
•—GAS NOZZLES
▭ TWIN-COLOR-DRUM LIGHT PROJECTORS (FIG. 21)
═ REFLECTOR LAMP STRIPS (FIG. 23)
K—KIOSKS HOUSING LOUD-SPEAKERS AND CONTACTOR PANELS
K

FIG. 27

Figures 27, 28. The major equipment on the 200- by 400-foot operating platform of the fountain in the Lagoon of Nations is arranged in three concentric rings (27, 28). Other equipment is so placed that various geometrical configurations may be achieved, as indicated by the dashed lines (27). The maze of equipment (20) includes 552 main water jets, each with its associated twin-unit lighting projector (21, 22), some 133 gas jets, and 285 fireworks mortars strategically arranged in eight operating groups. A continuous row of atomizer nozzles with associated lighting units (23) forms the outer border of the fountain. The total connected load for the Lagoon spectacle alone is 3,700 kw

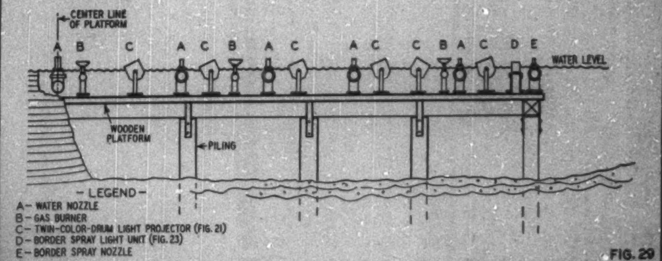

CENTER LINE OF PLATFORM
A B C A C B A C A C C B A C D E
WATER LEVEL
WOODEN PLATFORM
PILING
— LEGEND —
A — WATER NOZZLE
B — GAS BURNER
C — TWIN-COLOR-DRUM LIGHT PROJECTOR (FIG. 21)
D — BORDER SPRAY LIGHT UNIT (FIG. 23)
E — BORDER SPRAY NOZZLE

FIG. 29

Figure 29. This sectional elevation along the radius of one of the three operating circles of the fountain shows the relative placement of major equipment on the Lagoon deck and approximate operating water level

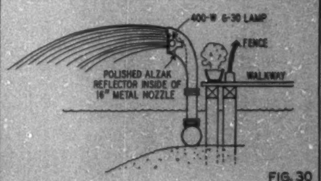

400-W G-30 LAMP
FENCE
WALKWAY
POLISHED ALZAK REFLECTOR INSIDE OF 16" METAL NOZZLE

FIG. 30

Figure 30. Seven of these illuminated "megaphone" water nozzles at about 50-foot intervals around each end of the Lagoon surrounding the fountain provide animation in these areas

Oriented and designed to climax the vista down Constitution Mall from the Theme Center, the searchlight canopy is keyed to a pyramidal structure comprising five main searchlight beams which are directed to intersect at a point some 400 feet directly above the center of the Lagoon of Nations and which form an outer framework for the fountain display. Four of these searchlights are mounted on the tops of convenient buildings at the four points of the compass at distances averaging some 700 feet from the center of the Lagoon (plate I). Each of these four units is a 36-inch 125-ampere high-intensity automatic-feed carbon-arc projector. The fifth and most powerful searchlight beam comes from a similar 60-inch army-type searchlight mounted on the roof of the Federal Building exactly on a continuation of the center axis of the Mall. Intersecting this latter "ridge pole" beam of the canopy of light structure are two groups of eight parallel pencils of light from projectors mounted on the roofs of the foreign-government pavilions flanking the Court of Peace (plate III). Each of these 16 beams has as its source one of the newly developed 1,000-watt water-cooled mercury capillary lamps mounted in a suitable searchlight housing (figure 31). The small size of the actual source of this light (approximately 1.5 by 25 millimeters) is ideal for the purpose, producing a narrow "pencil" of light.

WITHIN the limits of this article it has been possible only to outline the plan and purposes underlying the lighting scheme of the New York World's Fair of 1939; to describe in some detail the most prominent lighting displays, and show how they have been accomplished; and to mention the significant new forms of lighting developed. Throughout the enormous exhibit area are numerous other interesting and novel lighting creations, unmentioned only because of space limitations. Most of the significant basic forms of lighting, however, are represented among the various illustrations and application sketches, although, of course, many different variations, combinations, and adaptations were used by different exhibitors, all of whom made effective use of new and attractive lighting arrangements.

A World's Fair, by its very nature, offers exceptional opportunities for the presentation of new ideas. The keynote of the New York World's Fair is "better living in the world of tomorrow." Especially in the field of illumination has this keynote successfully and significantly been carried out. The Fair served as a stimulant to creative minds seeking departures from the conventional; also as a proving ground for new and better light sources, for improved methods, systems, and techniques. Its success is evidenced by such developments as the rapid commercial acceptance of the low-voltage fluorescent lamp, and the increased consciousness of lighting as an element of architectural design, a foretaste of the practices of tomorrow.

THE AUTHOR is indebted to many individuals and companies for helpful co-operation in the course of his work at the New York World's Fair. The Westinghouse, General Electric, and Eastman Kodak companies all went out of their way to place their facilities at the disposal of the Fair for carrying through many of the new developments. The Westinghouse company's Lighting Division gave every assistance in the final design and manufacture of most of the special lighting equipment used in the Lagoon of Nations, the Court of Peace, and at the Theme Center; the General Electric Company expedited the development of the low-voltage fluorescent tubes and related equipment; the Eastman Kodak Company designed and manufactured the significant new motion-picture projector for the Theme Building.

Special acknowledgment for valued and comprehensive co-operation is made to Edward H. Sniffin and H. M. Hays of the Westinghouse company; also to W. F. White and Irving Yost of that company for their untiring assistance. C. M. Cutler of General Electric acted very effectively throughout the construction period as liaison engineer between the Fair and the laboratories and plants of his own company. Fordyce Tuttle, of the Eastman company, in addition to his work in the development of the special Perisphere motion-picture projector, contributed much to the solution of other important optical and projection problems. The author's indebtedness is acknowledged to John H. Kliegl for the development of special-effect projection equipment; to Daniel Rollins of the Ward-Leonard Electric Company for the design and manufacture of control equipment; to D. D. Gillespie for the manufacture of the intricate control boards for the Lagoon of Nations display and the Perisphere Theme Show; also to A. B. McKenna of the Westinghouse Lamp Company and to Albert Fuller of the Eastman Kodak Company. The author expresses special appreciation to Editor G. Ross Henninger of the AIEE and his staff for their helpfulness in the preparation of this article.

Editor's Note: Incidental to the preparation and publication of this article, persons and organizations too numerous to mention have made valuable contributions which are acknowledged hereby. Special credits are due as follows: To P. L. Giering, J. G. Lawrence, and other members of the World's Fair staff, especially Wm. W. Morris who carried his camera 300 feet up into the Trylon to secure the view shown on plate I; to Harry Flowers, of the Flowers Photo Engraving Corporation, New York, for valuable assistance in the preparation of the color plates; to the Westinghouse company for making these plates available for use in ELECTRICAL ENGINEERING; *to the General Electric Company, the General Motors Corporation, the Westinghouse company, and the World's Fair Corporation for various illustrations.*

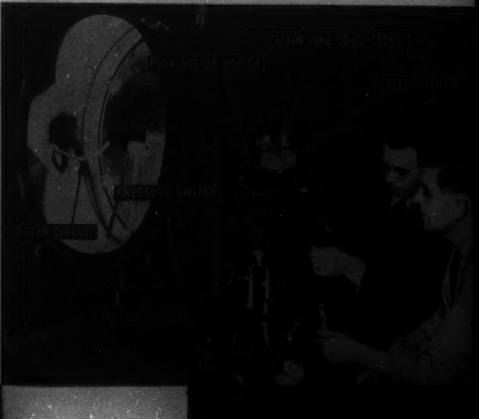


Figure 31. The 16 pencil-like searchlight beams forming a canopy over the Court of Peace (plate III) have as their source the newly developed 1,000-watt water-cooled mercury capillary lamp. One of the searchlights with associated control equipment is shown here

This lamp operates at 840 volts and produces some 65,000 lumens, an equivalent brightness of about 300 candle power per square millimeter, which is about one-fifth the brightness of the sun. About three quarts of cooling water per minute is required for each lamp

Some Pylons

Decorative pylons, strikingly illuminated at night, provide sharp accents that contribute sparkle to the nighttime scene, at the same time serving as points of orientation for New York World's Fair visitors

FIG. 32

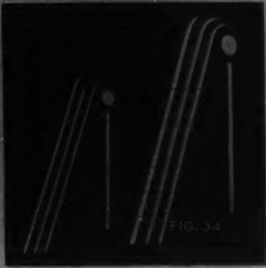


FIG. 34

Figures 34, 35. Two pairs of 66-foot pylons at the west entrances are brightly outlined at night by high-voltage fluorescent tubing hidden in reflecting coves (35). The circular panels at the top are illuminated by incandescent lamps

FIG. 36

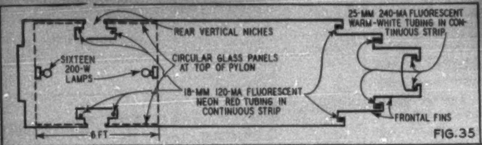
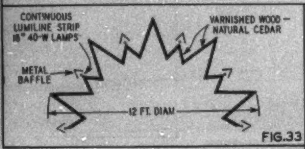
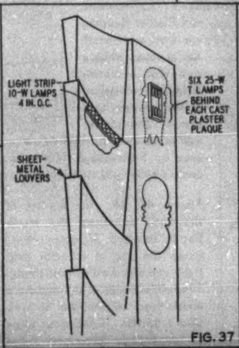
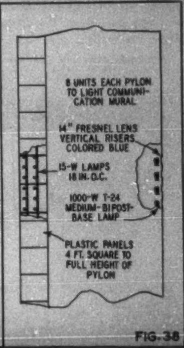

REAR VERTICAL NICHES
SIXTEEN 200-W LAMPS
CIRCULAR GLASS PANELS AT TOP OF PYLON
18-MM 120-MA FLUORESCENT NEON RED TUBING IN CONTINUOUS STRIP
25-MM 240-MA FLUORESCENT WARM-WHITE TUBING IN CONTINUOUS STRIP
FRONTAL FINS
6 FT
FIG. 35

Figure 36. The serpentine tracery of these 145-foot "hairpin" pylons is provided by exposed pink and green high-voltage fluorescent tubing


CONTINUOUS LUMILINE STRIP 18" 40-W LAMPS
VARNISHED WOOD — NATURAL CEDAR
METAL BAFFLE
12 FT. DIAM.
FIG. 33


LIGHT STRIP 10-W LAMPS 4 IN. O.C.
SIX 25-W T LAMPS BEHIND EACH CAST PLASTER PLAQUE
SHEET-METAL LOUVERS
FIG. 37


8 UNITS EACH PYLON TO LIGHT COMMUNICATION MURAL
14" FRESNEL LENS VERTICAL RISERS COLORED BLUE
15-W LAMPS 18 IN. O.C.
1000-W T-24 MEDIUM-BIPOST-BASE LAMP
PLASTIC PANELS 4 FT. SQUARE TO FULL HEIGHT OF PYLON
FIG. 38

Figures 32, 33. An effective illusion of translucence is given to the varnished natural cedar surface of the 115-foot Star Pylon by continuous Lumiline strips hidden behind reflecting troughs

Figures 37–40. At opposite sides of the Theme Center: Four 55-foot louvered pylons (37, 39) representing earth, air, water, and fire, man's first-known elements, mark the southern terminus of the Court of Power; two 116-foot pylons stand in front of the building at the north end of the Court of Communications (38, 40). Incandescent projectors in the backs of the latter illuminate the mural

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Typical Exteriors

IN general the design of each Fair exhibit building provides a single dominating architectural element such as the spiral pylon on the Operations Building (figure 48). These elements serve to individualize the buildings and in most instances are the primary source of "sparkle" lighting for the structure itself. Thus the lighting treatment for individual buildings is developed to meet the special needs of those buildings, as in all decorative pylons (figures 32–40), and utilizes a wide variety of light sources including high- and low-voltage fluorescent tubing, much of which is used exposed (figures 45, 53). In some instances where a particular translucent effect is desired, tubing of different colors is concealed as in the fin of the Chemicals and Plastics Building (figures 41, 44). The façades of the structures in general are treated

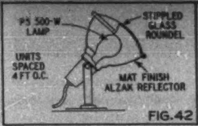
P5 500-W LAMP
STIPPLED GLASS ROUNDEL
UNITS SPACED 4 FT O.C.
MAT FINISH ALZAK REFLECTOR

FIG.42

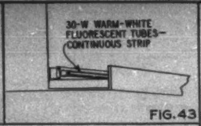
30-W WARM-WHITE FLUORESCENT TUBES—CONTINUOUS STRIP

FIG.43

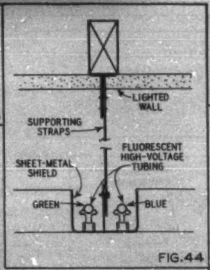
LIGHTED WALL
SUPPORTING STRAPS
FLUORESCENT HIGH-VOLTAGE TUBING
SHEET-METAL SHIELD
GREEN
BLUE

FIG.44

Figures 41–44. Conventional floodlights (42) illuminate the upper facade of the Chemicals and Plastics Building (41). Fluorescent cove lights (43) are used underneath the canopy, while a new type of downlight (59) is installed in the canopy ceiling. The plaster wall of the fin at the right is given a translucent appearance by high-voltage fluorescent tubing concealed under a metal grid set out from the wall (44)

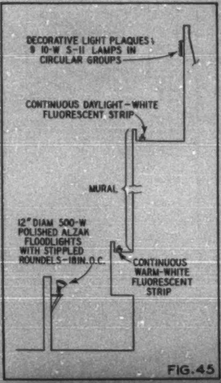
DECORATIVE LIGHT PLAQUES; 9 10-W S-11 LAMPS IN CIRCULAR GROUPS
CONTINUOUS DAYLIGHT-WHITE FLUORESCENT STRIP
MURAL
12" DIAM 500-W POLISHED ALZAK FLOODLIGHTS WITH STIPPLED ROUNDELS-18IN. O.C.
CONTINUOUS WARM-WHITE FLUORESCENT STRIP

FIG.45

Figures 45, 46. The lighting of the huge mural on the North Foods Building illustrates effectively the use of reflector strip lights combined with floodlighting. The brilliant reds of the mural are emphasized by the use of pink glass roundels. Continuous strips of low-voltage fluorescent tubes illuminate the setbacks, and the top is crowned by a ring of circular nine-lamp plaques. Triple zigzag strips of fluorescent neon tubing encircle the building near the top, except for the area shown

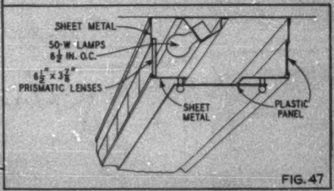
SHEET METAL
50-W LAMPS 6½ IN. O.C.
6½" x 3⅞" PRISMATIC LENSES
SHEET METAL
PLASTIC PANEL

FIG. 47

Figure 47. Details of lighting strip under entrance portico of Operations Building (48)

Figure 48. The spiral pylon of the Operations Building is composed of 12-inch 40-watt T-8 lamps on four-inch centers mounted in a spiral trough supported by a central pipe. The facade at the left is lighted by blue fluorescent tubes in fluted coves. A decorative strip (47) underneath the portico illuminates mural and walkway

with built-in lighting units; these serve to illuminate the painted wall murals which in turn reflect soft diffused light to adjacent walkways. The colors of light sources are selected to accentuate the colors of the murals or façades, and in some instances additional color filters are applied. The use of plaques, as on the Hall of Communications (figures 50, 51), provides many possibilities for accent color lighting applicable to both decorative and utilitarian purposes. On this and the facing page are presented selected night views showing typical exterior lighting. Space limitations obviously permit illustrating only a few typical applications of the significant types of exterior illumination at the Fair.

Figure 52. At the entrance to the Shelter Building are two rows of 150-watt units with prismatic lenses; the inner row is directed toward the mural, difficult to light because of its specular surface, and the outer row downward

Figure 53. Exposed fluorescent tubing illuminates this typical information booth

Figure 49. Back-lighted sculptures and blue and white fluorescent tubes behind a glass-brick wall provide decorative elements and unobtrusive walkway illumination near the Theme Center where special precautions are taken to avoid diluting the Perisphere illumination. The figures are approximately 18 feet high

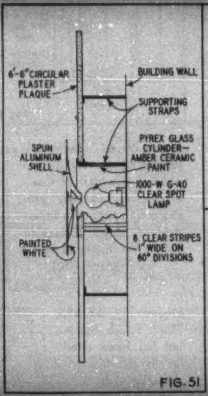
6'-8" CIRCULAR PLASTER PLAQUE
BUILDING WALL
SUPPORTING STRAPS
PYREX GLASS CYLINDER—AMBER CERAMIC PAINT
SPUN ALUMINUM SHELL
1000-W G-40 CLEAR SPOT LAMP
PAINTED WHITE
8 CLEAR STRIPES 1" WIDE ON 60° DIVISIONS

FIG. 51

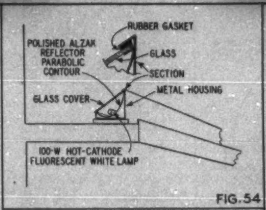
RUBBER GASKET
POLISHED ALZAK REFLECTOR PARABOLIC CONTOUR
GLASS
SECTION
METAL HOUSING
GLASS COVER
100-W HOT-CATHODE FLUORESCENT WHITE LAMP

FIG. 54

Figures 54–56. Floodlights employing the new hot-cathode fluorescent lamp preserve the natural beauty of the blue, green, and white murals (54, 56) along the east side of the Court of Power. Decorative plaques between murals are back-lighted by groups of fluorescent tubes (55, 56). Note flower-like street lights (1, 68)

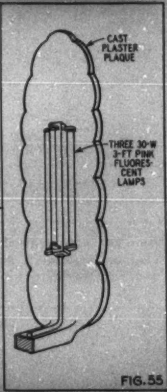
CAST PLASTER PLAQUE
THREE 30-W 3-FT PINK FLUORESCENT LAMPS

FIG. 55

FIG. 50

Figures 50, 51. Back-lighted plaster plaques provide general illumination and decoration along the west wall of the Hall of Communications. Other plaques of different design serve a similar purpose along the east wall

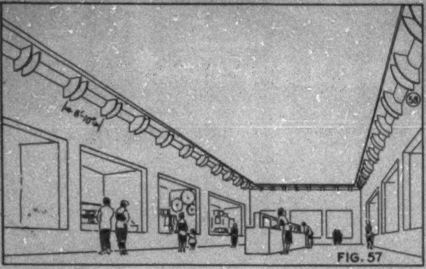

FIG. 57

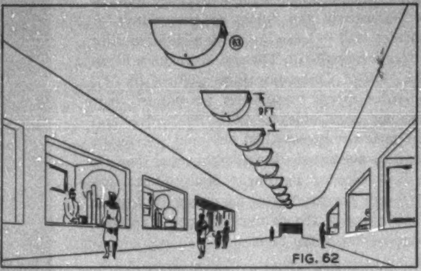

FIG. 62

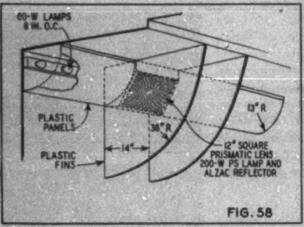
60-W LAMPS 8 IN. O.C.
PLASTIC PANELS
PLASTIC FINS
14"
36" R
13" R
12" SQUARE PRISMATIC LENS 200-W PS LAMP AND ALZAC REFLECTOR

FIG. 58

Figures 57, 58, 62, 63. The main exhibit hall of the Chemicals and Plastics Building appropriately utilizes plastic panels and fins, combined with prismatic lenses, in a decorative strip extending all around the outer edge of the ceiling (57), incandescent lamps in strips and in reflectors provide the illumination (58). The exhibit wing of this building (62) is illuminated by a central row of units using low-voltage fluorescent tubes and semicircular plastic fins (63). The use of new light sources and new materials is typical of general illumination applications throughout the Fair

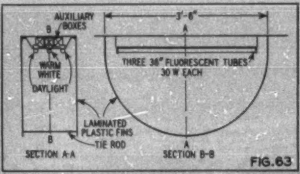
AUXILIARY BOXES
B
WARM WHITE
DAYLIGHT
B
SECTION A-A
LAMINATED PLASTIC FINS
TIE ROD
3'-6"
A
THREE 36" FLUORESCENT TUBES 30 W EACH
A
SECTION B-B

FIG.63

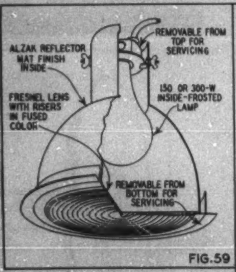
ALZAK REFLECTOR MAT FINISH INSIDE
REMOVABLE FROM TOP FOR SERVICING
FRESNEL LENS WITH RISERS IN FUSED COLOR
150 OR 300-W INSIDE-FROSTED LAMP
REMOVABLE FROM BOTTOM FOR SERVICING

FIG.59

Figure 59. New downlight ceiling fixture used extensively throughout the Fair (61)

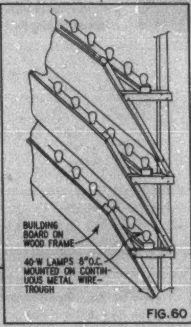
BUILDING BOARD ON WOOD FRAME
40-W LAMPS 8" O.C. MOUNTED ON CONTINUOUS METAL WIRE-TROUGH

FIG.60

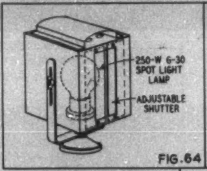
250-W G-30 SPOT LIGHT LAMP
ADJUSTABLE SHUTTER

FIG.64

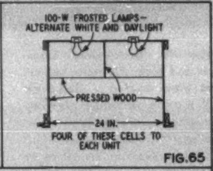
100-W FROSTED LAMPS—ALTERNATE WHITE AND DAYLIGHT
PRESSED WOOD
24 IN.
FOUR OF THESE CELLS TO EACH UNIT

FIG.65

Figures 60, 61. Indirect illumination in this exhibit wing of the Hall of Communications (61) is provided by continuous incandescent strips concealed behind the louvered upper wall surfaces (60)

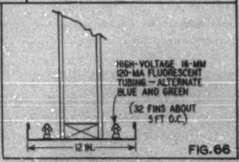
HIGH-VOLTAGE 18-MM 120-MA FLUORESCENT TUBING—ALTERNATE BLUE AND GREEN
(32 FINS ABOUT 5 FT O.C.)
12 IN.

FIG.66

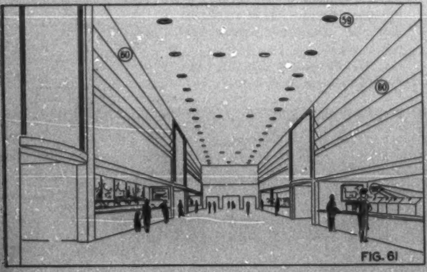

FIG. 61

Figures 64–67. North Foods Building has been cited for its effective coordination of light and architecture (67), 32 projectors (64) hidden above the ceiling plaque throw light beams toward the huge fins, which are lighted by fluorescent tubing (66). Ceiling wells (65) illuminate the exhibit wing

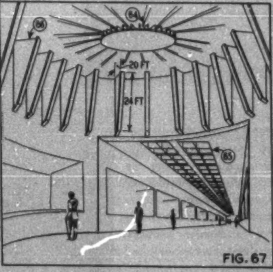
(66)
(64)
20 FT
24 FT
(65)

FIG. 67

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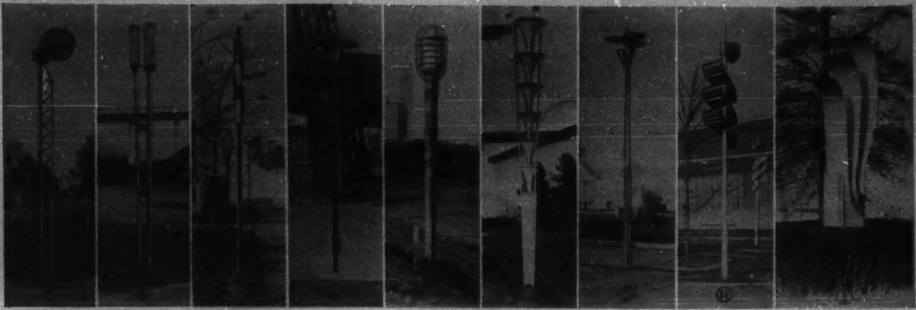
Typical Street Lights



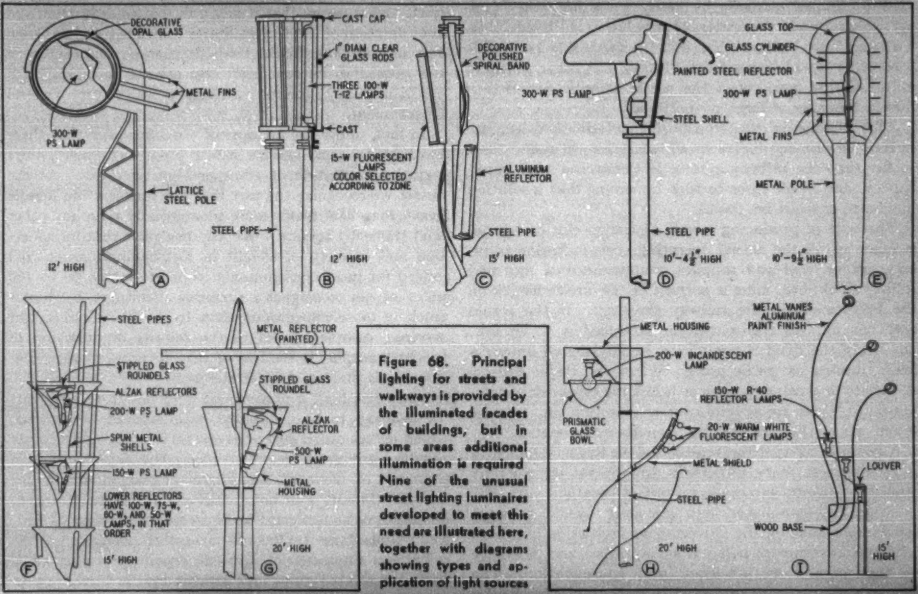

DECORATIVE OPAL GLASS
METAL FINS
300-W PS LAMP
LATTICE STEEL POLE
12' HIGH
A
CAST CAP
1" DIAM CLEAR GLASS RODS
THREE 100-W T-12 LAMPS
CAST
15-W FLUORESCENT LAMPS COLOR SELECTED ACCORDING TO ZONE
STEEL PIPE
12' HIGH
B
DECORATIVE POLISHED SPIRAL BAND
ALUMINUM REFLECTOR
STEEL PIPE
15' HIGH
C
300-W PS LAMP
PAINTED STEEL REFLECTOR
STEEL SHELL
STEEL PIPE
10'-4½" HIGH
D
GLASS TOP
GLASS CYLINDER
300-W PS LAMP
METAL FINS
METAL POLE
10'-9½" HIGH
E
STEEL PIPES
STIPPLED GLASS ROUNDELS
ALZAK REFLECTORS
200-W PS LAMP
SPUN METAL SHELLS
150-W PS LAMP
LOWER REFLECTORS HAVE 100-W, 75-W, 60-W, AND 50-W LAMPS, IN THAT ORDER
15' HIGH
F
METAL REFLECTOR (PAINTED)
STIPPLED GLASS ROUNDEL
ALZAK REFLECTOR
500-W PS LAMP
METAL HOUSING
20' HIGH
G
METAL HOUSING
200-W INCANDESCENT LAMP
PRISMATIC GLASS BOWL
20-W WARM WHITE FLUORESCENT LAMPS
METAL SHIELD
STEEL PIPE
20' HIGH
H
METAL VANES ALUMINUM PAINT FINISH
150-W R-40 REFLECTOR LAMPS
LOUVER
WOOD BASE
15' HIGH
I

Figure 68. Principal lighting for streets and walkways is provided by the illuminated facades of buildings, but in some areas additional illumination is required. Nine of the unusual street lighting luminaires developed to meet this need are illustrated here, together with diagrams showing types and application of light sources

Figure 69. This fountain in Bowling Green plaza, which utilizes back - lighted glass blocks topped by a gas flame, effectively illustrates the general treatment of small courts and plazas throughout the Fair grounds by providing so-called "accents" in these areas

Modern Rail Transport

A. M. WRIGHT
ASSOCIATE AIEE

Because of diversion of traffic to the highways, the railways have experienced severe reductions in operating revenue over the past several years. Since revenue must cover operating expenses and fixed charges, to remedy this condition the traffic density must increase and the operating expenses per traffic unit must be reduced. This article discusses the achievement of this by suitable co-ordination of rail and road transport, with certain consolidations of rail lines and restriction of heavy long-haul traffic to the railways. Such a program introduces important engineering problems, and the problem of motive-power adequacy is discussed with particular reference to steam and electric locomotives. A short outline of other important developments in rail transport also is included.

THE most important problem facing the railways today is that of maintaining revenues and financial structures in the face of falling traffic. Since the advent of the automobile a great deal of the most profitable land transport business has been diverted to highways, and this has resulted in a general decrease in net railway revenue, despite the fact that this same period of decreasing traffic witnessed a general rise in productive activity and the use of manufactured consumers' goods.

Nor is this diverted traffic confined to short hauls and to packaged or small-bulk commodities. For example, it is estimated that about 3,000,000 tons of anthracite coal are hauled annually by motor truck from the Pennsylvania mines to the centers of population in Philadelphia, Pa., and New York, N. Y.; and the haulage of perishable foodstuffs by highway from Florida and other southern states to the markets of the north Atlantic seaboard is now a business of large proportions.

This change in the transportation structure has appeared in varying degree in all countries—in many cases so far that the railway system is threatened with bankruptcy, and in all cases to such an extent that a solution has been, or must be, found.

The cost of producing a unit of transportation by rail is made up of the actual operating cost, including train-crew wages, fuel and supplies, maintenance of way and rolling stock, etc., plus a portion of the fixed charges on the investment in the railway property. If the actual cost per unit of transportation produced is to be low, obviously the fixed charges must be distributed among a large number of traffic units. When the traffic density decreases to a value close to or below the economic operating point, there are several courses of action that may be taken all of them radical but nonetheless essential.

A possible but specious remedy for the financial difficulty is to establish state ownership and operation, whereby operating deficits are made up out of taxation. In 1937 existing well-established and privately owned railways were taken over by the Government in Mexico in accord with the Government policy of socialization, and in France in the interest of efficiency and consolidation. The British railways were subjected to radical interference by the Government under the Transportation Act of 1921, being consolidated into four large systems with the elimination of about £100,000,000 of capital.

The trend of railway-traffic diversion to the highways is most pronounced in the U.S.A., but there has been no serious agitation for Government ownership as a solution of the problem of properly co-ordinating rail and road transport to give the most economical operation of the whole transportation system. The Federal Government, through the agency of the Interstate Commerce Commission, is authorized to regulate only interstate transport, and it seems that if any policy of co-ordination and consolidation of the United States railways is to be carried out, as was done in England, it must be commenced, if not carried to its conclusion, by private enterprise with constitutionally limited assistance from the Federal Government.

In fact, under the pressure of economic necessity, there has been for some years a steady progress toward a more logical organization of transportation methods. At the outset we recognize the fact that the railways can handle heavy long-haul traffic more economically than any other land transport agency. We also recognize that for collection and delivery of freight in metropolitan areas, and indeed for most consignments to points within 50 to 100 miles of the consignor's premises, transport by motor truck is more expeditious than by ordinary rail-freight service. Another defect in the railway organization of freight transport is the loss of time incurred in classification yards and at transfer stations.

There has been a pronounced tendency in recent years for business to be conducted with reduced inventories, which makes quick deliveries essential. The motor truck provides one good means of giving this rapid service in shipment of moderate consignments over moderate distances. Twenty-four-hour delivery at distances up to 400 miles or so has come to be a regular and reliable service. Under the traditional operating methods of the railways a shipment consigned to a point 400 miles away could be counted on to take several days in transit; with present business methods, such delays are intolerable. Under pressure from customers on the one hand, and from truck competition on the other, then, the railroads have

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1. For all numbered references see list at end of article.

been forced to modify their operating methods to expedite the movement of freight over their lines. This by no means has been forced to the point where it can be said that no further improvement is possible, but all indications point to an ultimate organization of the transport system on some such basis as this:

1. Rail transport, in heavy fast trains, is a means by which huge quantities of transportation can be produced very rapidly, particularly where long distances are concerned; for example, certain railway systems today are producing transportation at an average rate of about 60,000 gross ton miles per train hour, this being an average for all trains operated on these systems. With such performance, trains are easily capable of removing freight from a terminal at the rate of 10,000 net tons per hour. It is inconceivable that any reasonable assignment of highway vehicles could handle traffic at such a rate. Therefore heavy long-haul traffic will be concentrated on the railway lines, such traffic being excluded from the highways.

2. Collection and delivery service in standard railway practice, where there exists in any metropolitan area a multiplicity of freight stations and industrial sidings, is slow, inefficient, and costly. The tendency is toward elimination of these facilities, freight being picked up or delivered at the customer's door from a central depot.

3. Delays en route for classification of freight cars are incompatible with quick delivery over long distances. In many cases, short-haul and long-haul freight is now segregated, the long-haul freight being made up in solid trains for "through" or "main track" movement to its destination without stops except for changing engines or car inspection.

4. Short-haul freight often is handled by motor truck. and the ultimate outcome will probably be the diversion of most short-haul traffic from rail to road vehicles.

5. Where the traffic density on any railway line falls below a certain magnitude, operation of that line is no longer an economic proposition; and if the traffic offered by the district served by that line can be properly and economically handled by motor truck, its abandonment is indicated. If the traffic has decreased because of diversion to a competing line, consolidation is indicated, with probable abandonment of the lightly loaded line.

6. In most countries where the railways have been constructed under private enterprise, the present systems have been developed by consolidation of smaller systems, either by legislative fiat, as in England, or by gradual absorption of the weaker systems by the stronger, as in the U.S.A. The American railways are still being slowly consolidated into fewer competing systems, and it is at least possible that Government interference will act to hasten this process, resulting in the abandonment of unnecessary mileage, in decreased duplication of rolling stock and personnel, and in reduction of operating expenses, accompanied by sharp increases in traffic density.

When these tendencies are followed to a conclusion, the railways appear in the transportation picture as the principal means by which dense long haul-traffic is handled. With such an organization the general scale of railroad operation, both as regards terminals and the main connecting lines, will be enlarged beyond anything now existing outside of a few instances, and the engineering problems connected therewith will become more pressing as the developments outlined proceed. Leaving out of account the terminal problem, namely, that of providing loading facilities, driveways, approaches, etc., for handling in a few main terminals the freight entering and leaving a large city each day, the main problem will be that of providing rolling stock and motive power to haul a greater concentration of freight over the tracks available.

The traffic capacity of a piece of railway track, expressed as the ton miles per mile of track per annum, is a function of the size of trains, the permissible headway between trains, and the speed of travel. These in turn are dependent on track alignment, gradients, capacity of rolling stock, and the locomotive power available. To give a concrete picture of the influence of these factors, figure 1 depicts the profile and track arrangement of a double-track railway line connecting points *A* and *F*, 200 miles apart. With certain stretches of four-track route, at *B*, *C*, and *E*, where freight trains can take the side track to permit overtaking by passenger trains, the most dense disposition of traffic is shown on the accompanying graphic train sheet. The track arrangement permits dispatch of freight and passenger trains at hourly intervals, which means that the maximum traffic this railway can handle is 24 freight and 24 passenger trains per day in each direction.

It is important to note that this is a theoretical figure. In scheduling a heavy freight train there is an element of chance; delays will be encountered en route from a variety of causes. In this connection a study made by the committee on economics of railway operation of the American Railway Engineering Association[1] showed that train movements are seriously interfered with by accidental delays and track limitations when the actual traffic is about 50 per cent of the theoretical capacity.

The following assumptions were used in making up the train sheet of figure 1:

Freight-train trailing load	3,000 tons
Freight-train locomotive	3,000 horsepower
Freight-train maximum speed	40 miles per hour
Passenger-train maximum speed	70 miles per hour
Passenger-train weight	450 tons
Passenger-train locomotive	2,400 horsepower

Using these figures with the traffic density shown on the train sheet, the theoretical traffic-handling capacity of the line is 26,000,000 ton miles per mile of track per annum; 50 per cent of this gives 13,000,000 ton miles per track mile per annum as being the actual limit of the freight-handling capacity of the road. The foregoing figures for train weights and locomotive power rating represent what is attainable with present-day practice, using steam locomotives of conventional design.

While a freight-traffic density of 13,000,000 ton miles per track mile per annum is a high average, it is by no means unheard of and is even exceeded on several American railways. But when coupled with a dense passenger traffic, the problem of keeping the rails clear is likely to become very difficult, which is particularly marked where the termini of the railroad are situated in centers of large population.

Without resorting to a program of rebuilding the railway, there are several ways of relieving congestion and so increasing the traffic-handling capacity of the railroad depicted in figure 1. The most obvious of these are as follows:

1. The weights of freight trains might be increased. If the trailing loads of all the freight trains indicated on figure 1 were doubled, obviously the freight traffic capacity of the railroad also would be doubled, provided train speeds were not reduced.

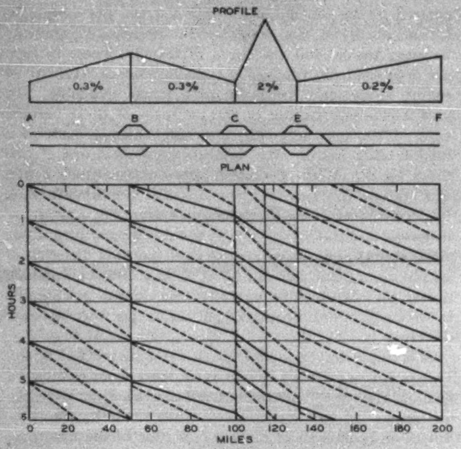

Figure 1. Train sheet showing maximum traffic on two-track railway

Each sloping line is a time/distance chart for a train. Dotted lines indicate freight trains; solid lines passenger trains

2. The freight-train speeds may be increased. If the speed of the freight trains indicated in figure 1 were the same as that of passenger trains, then the headway between trains could be reduced to something determined principally by the length of the signal blocks.

3. The capacity of the rolling stock may be increased. This enters in a very important way into the problem of determining locomotive power, principally because (*a*) the tractive resistance, or drag, of a train made up of heavy large-capacity cars is much less than that of a train of the same weight made up of light small cars, and (*b*) the tare weight of a large-capacity freight car is much less than the tare weight of a small-capacity car in proportion to the load. For example, a goods wagon of eight tons load, as commonly used in England, might have a tare weight of four tons. A 120-ton coal car, as used in the U.S.A. has a tare weight of about 30 tons. The American railways thus are hauling only about half as much dead load per revenue ton as the English railways; and the number of serviceable cars required to handle a given traffic directly affects operating costs. The greater capacity of the rolling stock is a very important reason that freight transport per ton mile in the U.S.A. costs only one-third as much as in England. The trend of past developments, as well as such general considerations as the foregoing, point to a continued increase in the capacity of rolling stock, which permits the net ton miles produced to increase in relation to the gross ton miles.

The increase in speed and weight of trains, as mentioned under the foregoing items 1 and 2, involves an increase in the power output of the locomotives. Every increase in the ton miles produced per train hour, which is an index of the rate at which transportation can be produced with any given equipment, brings up the question of the adequacy of the motive power. If development proceeds along the lines outlined, that is by restricting long-haul traffic to the railways, and by a program of consolidation and abandonment of certain existing lines, thus increasing the traffic density on lines remaining in service, the question of locomotive capacity will be one of pre-eminent importance.

The power output of a locomotive is the product of the tractive effort exerted at the rims of the driving wheels and the velocity of the locomotive. If the tractive effort is in pounds and the velocity in miles per hour, then this product must be divided by 375 to give the locomotive horsepower at the rails.

In the example depicted in figure 1, the freight locomotives were assumed to have a rail horsepower of 3,000 and the trailing loads were assumed to be 3,000 tons. Such trains can move at 38 miles per hour over a gradient of 0.2 per cent using the maximum power of the locomotive.

A Pacific-type passenger engine of 2,400-horsepower capacity with a 500-ton trailing load can traverse this same gradient at 80 miles per hour; and if the headway between trains is 30 minutes, the passenger train will overtake the freight train 46 miles from the terminal. This means that the freight train would have to take to a siding to permit the passenger train to pass after at most 46 miles.

But if the freight-train speed can be increased to 60 miles per hour, then with the same headway between trains the passenger train would not overtake the freight train until a point is reached 120 miles from the terminal. The effect of speeding up the slower trains is thus very beneficial in clearing up the right of way and in permitting a reduction in the time interval between successive trains.

However, to move a 3,000-ton train at 60 miles per hour over a 0.2-per cent grade requires a steam locomotive of about 6,300 horsepower at the rails, and the questions to be answered are: First, can a locomotive of such capacity be built within reasonable limitations of weight and size; and second, can any other type of locomotive, such as Diesel or electric, do the job better?

The power developed by a steam locomotive is a function of the rate at which the fuel is burned in the firebox; therefore, locomotives of larger horsepower rating must be provided with larger fireboxes and heating surfaces, which in turn leads to larger boilers and greater weights for the complete locomotives. It seems to be pretty definitely established that the weight efficiency of conventional steam locomotives of large output, in pounds per horsepower, is not much better than that of locomotives of moderate output.

The traffic-handling capacity of a railway track, and its attendant problems of locomotive power, have been the subject of study for some years by the committee on economics of railway operation of the American Railway Engineering Association.[1] In volume 40 of the *Proceedings* of the AREA[2] are summarized the results of a study of a great number of steam engines and engine types as regards the relation between weight and power rating for modern locomotives. Without here discussing the methods for obtaining the figures given, other than to point out the obvious fact that the locomotive power depends on the size and weight of the boiler and on the firebox grate area, which in turn determine the wheel arrangement and weight on the driving axles, the normal rail horsepower of modern locomotives equipped with all economizing devices gives specific weights about as follows:

2-8-2 "Mikado" type	188 pounds per horsepower
2-10-4 type	187 pounds per horsepower
4-6-2 "Pacific" passenger type	202 pounds per horsepower

These weights are based on the total weight of engine and tender, and the rail horsepower was estimated by taking the cylinder or indicated horsepower and deducting losses based on the usual 25 pounds per ton on drivers for machine friction. The weight efficiencies given apply to the "normal horsepower", which is defined as being 75 per cent of the maximum or test-stand horsepower. In actual service we cannot count on being able to develop the full test-stand power of a steam locomotive. The maximum power is developed at a firing rate of about 150 pounds of coal per square foot of grate area per hour, but at this rate the over-all thermal efficiency of the locomotive is very low, leading to excessive fuel consumption. In service the usual firing rate is between 50 and 100 pounds per hour per square foot of grate area, depending on gradients and the necessity for "forcing" the locomotive, so that the power output actually developed is between 50 and 80 per cent of the maximum possible. The result of these various factors is that the weight efficiency on which we can count is rarely better than 200 pounds of total locomotive weight per rail horsepower developed.

On this basis a locomotive of 6,300 horsepower would weigh 630 tons, of which with normal modern designs 40 per cent, or 252 tons, would be on the driving wheels. A 2-10-4-type locomotive of 6,300-horsepower capacity would then have a driving-axle load of 100,800 pounds per axle. Such an axle weight as this would be intolerable from the standpoint of bridge-structure and track stresses. If we decide to go to an articulated type of locomotive, say having eight driving axles in two groups of four, the axle loading is reduced to 63,000 pounds per driving axle. This is within the permissible axle loading for most American railways, and if bridge structures have sufficient strength to take the total locomotive weight, the vertical track stresses perhaps would not be excessive. If at 60 miles per hour the driving wheels are limited by considerations of dynamic augment to 300 rpm, the required driving-wheel diameter is 67 inches and the rigid wheel base 18 feet—6 inches. When the lateral restraint in the articulation hinge is taken into account, it cannot be said offhand that such a long rigid wheel base will not introduce lateral track stresses of undesirable magnitude. But other difficulties following an adoption of locomotives of greatly increased rating would be that of providing engine-house facilities, turntables, etc., for engines of much greater length than any now used.

Any program of railway reorganization resulting in such traffic density that it would become necessary to move 3,000-ton or heavier freight trains at 60 miles an hour thus would bring the motive-power problem very prominently forward, and its economic as well as physical aspect would prompt consideration of a radical change in the type of tractive power to be used.

It does not seem likely that there will be in the near future any noteworthy improvement in the performance or weight efficiency of steam locomotives. The thermal efficiency of a steam engine theoretically can be improved by increasing the steam pressure and temperature and by superheating; but despite what is done in these respects, the fundamental fact remains that the principal cause of the inefficiency of a steam locomotive lies in imperfect combustion. Admittedly it is possible to reduce the firing rate in a locomotive firebox, and at the same time to raise the boiler pressure and steam superheat to the point where the over-all thermal efficiency might reach, say, 20 per cent; but the large firebox necessary to give the required evaporation at a low firing rate would entail such a great increase in the weight of the engine that the over-all weight efficiency would be no better than it is with conventional designs.

The problem of providing locomotives of 6,000 horsepower and over therefore must be solved by some means other than that of increasing the size of the conventional steam engine. The most promising rail tractor in this respect is the electric locomotive. The electric locomotive is not a prime mover; it is a transformer of energy furnished to it from a generating station situated at some distant point, and hence its inherent limitations as a rail tractor are not fixed by any considerations of combustion or thermal efficiency. The power it can develop is fixed by the characteristics of the electric motors by which the driving wheels are propelled, and the characteristics of the whole locomotive therefore are determined by the type of motor used.[3]

As a result of significant improvements in the single-phase series traction motor during the past ten years, going hand in hand with progress in methods of solving the difficult circuit-analysis problems met in a-c traction,[4,5,6] there has been in the United States during this period a great extension in the use of single-phase alternating current for heavy-traction electrification; for this reason the following discussion, looking to the possibilities of moving heavy freight trains at high speeds by

Westinghouse

electric locomotives, is confined to the single-phase a-c system using series traction motors. For the time being it may be taken as axiomatic that the overhead contact line from which the electric locomotive draws energy has infinite capacity and that it is possible to install a step-down transformer on the locomotive of enough capacity to handle whatever power is required by the traction motors. With these assumptions the limitations of the electric locomotive coincide with the limitations of the motors. The output of a series motor is limited by two factors, (1) commutation and (2) heating, with the additional mechanical limitation of (3) the peripheral speed of armature and commutator.

Taking account of these limitations, it has been shown in a previous paper[2] that the starting tractive effort at the rails exerted by a single-phase traction motor is given by the formula

$$TE = 5{,}000\,\frac{pl}{\text{mph max}}\ \text{pounds at 25 cycles per second}$$

In this expression

p is the number of poles on the motor
l is the active length of the commutator in inches

Thus for a 12-pole motor having an active commutator length of 9 inches and geared for a maximum speed of 70 miles per hour, the starting tractive effort would be 7,700 pounds. Using two motors per driving axle and assuming 25-per cent adhesion, the required weight per driving axle is then 62,000 pounds. Using six driving axles and a guiding truck at each end of the locomotive, with a 2C+C2 wheel arrangement, the total locomotive weight would be about 280 tons; and we know from previous designs that a locomotive of this type can be built to give one horsepower of continuous rating for each 100 pounds of weight (about 40 pounds per horsepower for the electrical equipment, and about 60 pounds for the mechanical parts). Thus the example indicates that we can build a high-speed freight locomotive of conventional wheel arrangement and conservative axle loads to give a continuous rating of at least 5,600 horsepower, or 933 horsepower per axle. This rating is not excessive, since three-axle locomotives have been built and are in service with 1,250 horsepower per axle.

It is necessary to point out that the electric locomotive is not a constant-output machine. The speed–tractive effort curve for an electric locomotive has a much steeper slope than that for a steam locomotive of the same nominal rating. If we denote the tractive effort by p and the train velocity by v, then the speed–tractive effort curve of the steam locomotive is given by $pv=\text{constant}$. The electric locomotive has a characteristic curve for which the equation is pretty closely $pv^{2.8}=\text{constant}$. The continuous rating of an electric locomotive is that continuously maintained power output at the rails at which the temperature rise of the motors does not exceed that specified in the Standards of the American Institute of Electrical Engineers. The effect of the steeper slope of the characteristic curve of the electric locomotive is to make the locomotive assume an overload when its speed is reduced by adverse gradients. Thus on a gradient the rail horsepower output of an electric locomotive rated at 5,600 horsepower will be greater than 5,600 horsepower, and the electric locomotive is able to maintain a much better schedule speed than a steam locomotive of the same rating.

It has been mentioned that the specific weight of an electric locomotive of modern design is of the order of 100 pounds per horsepower, as compared with the 200 pounds per horsepower of the steam locomotive. The effect of this is to reduce the dead weight of the equipment to be hauled and to permit a reduction in the rating of the electric locomotive as compared with the steam locomotive.

To see how this comes about, we may take the same example as before, namely, that of a 3,000-ton train moving at 60 miles per hour up a 0.2-per cent grade. To generalize, let W tons be the weight of the train and w tons the weight of the locomotive. Then using average figures for train resistance:

Train resistance $= 6W+10w$
Grade resistance $= 4(W+w)$
Total resistance $R = 10W+14w$

Also

$$HP=\frac{Rv}{375}$$

and

$$w=\frac{HP}{10}\ \text{(steam locomotive)}$$

or

$$w=\frac{HP}{20}\ \text{(electric locomotive)}$$

Substituting $W=3{,}000$, and solving for w and HP, we find the required steam-locomotive rail horsepower is 6,200, the weight being 620 tons. For the electric locomotive the required horsepower is 5,400, the weight being 270 tons.

This striking contrast, of a 270-ton electric locomotive being able to do the same work as a 620-ton steam locomotive, seems to indicate clearly the method by which the motive-power problem will be solved as railway-traffic density becomes so concentrated as to require more rapid movement of freight over the lines. But the use of electric locomotives brings with it the attendant problems of transmission and distribution of electric energy to the trains.

The dimensions and configuration of the transmission and distribution system of an electrified railway are, of course, dependent on the loads the system has to supply; they are also dependent on the voltage and frequency of the contact line and supply system. This question was the subject of a paper by McGee and Harder[3] whose investigations showed that it is easily possible, with a contact line voltage of 11,000, to provide enough capacity in the distribution network to handle traffic up to the limit of the tracks available. But since a large capital investment is required to provide a distribution system for an electric railway, there remains the engineering problem of so designing the transmission and contact lines and substations that the cost will be as low as possible.

A large part of the cost of the distribution system is chargeable to the step-down-transformer substations, and economical construction requires therefore that the substation spacing be as great as possible. If a given load is to be supplied with a given voltage drop between the substation bus bar and the load, an increase in distance between substations makes it imperative that the contact-line voltage be increased. In the United States the standard line voltage has hitherto been 11,000 volts for single-phase systems, but some electrified railways have provision for a change-over to 22,000 volts. In a general scheme of railway electrification, it now appears that even this voltage might be insufficient, and it is at least possible that a contact line potential of 50,000 volts will be considered.

In this discussion of electric traction, attention has focused on its engineering aspects. It is not possible in a reasonable space to analyze the economic features of electric traction as compared with steam, but it might be well to state in a broad fashion the general economic factors that lead to a decision for or against electric operation of a railway. In electric traction as in any other form of transportation the cost of producing a ton mile is made up of the operating cost and a proportion of the fixed charges on the investment. Since it may cost some $35,000 per track mile to equip a railway for electric operation exclusive of rolling stock, it is evident that electric traction will greatly increase the fixed charges on the property. Hence, unless a great increase in traffic density accompanies the electrification, there must be a considerable reduction in operating cost per ton mile produced. This reduction in operating cost always is obtained, arising principally from the following:

1. The number of electric locomotives required to handle a given traffic is always less than the number of steam locomotives needed for the same traffic. Since an electric locomotive (per horsepower at the rail) costs about the same as a steam locomotive, the investment in electric rolling stock is moderate. The reduced number of locomotives results in a great reduction in engine-house and maintenance costs.

2. The maintenance cost per horsepower mile of an electric locomotive is very much less than that of a steam locomotive. In nearly every case where electric traction has been applied, the cost of maintaining electric locomotives has proved to be about half that of the pre-existing steam locomotives.

3. The "availability", or the percentage of the total time during which electric locomotives are available for service, usually exceeds 90 per cent, as compared with at best 50 per cent for steam locomotives. This is why a given traffic density can be handled with far fewer locomotives than when steam locomotives are used, and the result is a great reduction in locomotive expense for such services as hostling.

4. Because an electric locomotive will assume an overload when its speed is reduced by adverse gradients and will develop up to twice its normal power output during acceleration, a given tonnage can be moved over the rails at much better schedule speed than can be achieved with steam or other constant-horsepower locomotives. This results in a reduction in the number of train-hours, with a consequent saving in crew expense.

5. With electric traction, there is a considerable item of expense in the maintenance of overhead transmission and distribution lines and substations. It usually happens, however, that this expense is of the same order of magnitude as that of maintaining fuel and water facilities required with steam traction.

6. It is a popular belief that because of the high thermal efficiency of modern electric generating stations, the cost of power for electric traction is much less than the cost of fuel required for steam traction. This, however, is not the case where coal is cheap. In the eastern part of the United States, for example, the cost of electricity per ton mile moved by electric traction is of the same order of magnitude as the cost of coal per ton mile moved with steam locomotives. This is due to the effect of the demand clause in all power contracts, by which the cost per kilowatt-hour is devoted in part to carrying the fixed charges on the electric-power equipment.

The result of these considerations, particularly items 2 and 4 is that electric traction always shows an operating saving when compared with steam traction. A decision for or against electrification is therefore dependent on the economic consideration of capital cost. To express this algebraically, let

C_s = the operating cost per ton mile with steam traction
C_e = the operating cost per ton mile with electric traction
F = the total annual fixed charges on the investment required for electrification
D = the total ton miles hauled per annum

Then electrification is economically justified when

$$C_s > F/D + C_e \tag{1}$$

In any well-planned electrification scheme C_e always will be less than C_s, so that the deciding factor for or against electric operation will be the quotient F/D. This should be as small as possible, which can be attained by keeping the cost of the electrification to a minimum or by increasing the traffic density. The cost of equipping a railway for electric operation is dependent principally on the engineering skill brought to bear on the physical problems involved; and as engineering science progresses, improved methods perhaps will tend to lower the first costs of electrifying, although these improved methods often are adapted to increase the operating reliability of the system at some sacrifice in cost.

The picture of the rail-transport system as developed herein has pointed to the desirability of developing the railways into a tool for the concentrated mass production of transportation by consolidation and reorganization. As this development progresses the traffic density on the favored lines will increase, perhaps until the quantity F/D in equation 1 is vanishingly small. In such a case the physical advantages of using electric motive power undoubtedly will dictate a policy of railway electrification on a scale sufficiently broad to exploit its real advantages. At any rate the considerations outlined will warrant a careful study of the motive-power problem.

In this discussion of motive power for rail traction no mention has been made of the Diesel-electric locomotive. It is frequently thought that the use of electric traction motors confers on these locomotives all the merits of the electric locomotive. Such, however, is not the case. Obviously the rail output of a Diesel-electric locomotive is fixed by the rating of the Diesel engine, which has a definitely limited output. On the other hand, the power available for propelling an electric locomotive is for practical purposes infinite, and the rail power output is limited principally by the commutation and heating of the traction motors, which with modern locomotives allows them to develop 25-per cent adhesion up to speeds of 50 miles an hour. The weight efficiency of the Diesel locomotive is about half that of the electric, and the first cost is about twice as great. Therefore, it does not seem that the Diesel locomotive offers any special advantages over steam locomotives for the handling of dense main-line traffic. The cleanliness of the Diesel-electric engine favors its use for the hauling of de luxe passenger trains, and it has been widely applied in this field. The Diesel locomotive also is being used increasingly in switching service, where its high degree of availability and low stand-by losses show up very favorably against the steam switching engine. But for very heavy main-line traffic the choice of locomotives probably always will be between the steam and the electric, with electric traction showing pronounced advantages when the traffic density exceeds the economic limit given by equation 1.

This article has outlined some of the problems that are arising in the railway field, and has discussed in more detail the question of motive power. In a reasonable space it is impossible to consider all the engineering problems that are most pressing today as a result of the increase in train weights and speeds now used in an attempt to retain or regain business in the face of competition. But some of these developments are so important that they cannot be overlooked in any review of modern rail transport, and the following list outlines a few of the most outstanding:

1. *Reduction of Tare Weight of Rolling Stock.* Modern freight rolling stock is of all-steel construction, using high-strength special alloys to give a greater proportion of load to tare. A more spectacular development is that of the stainless-steel and aluminum-alloy passenger rolling stock, resulting in equipment weighing about half that of the former designs of all-steel passenger cars.[7]

2. *Strength of Track.*[8] For many years past an intense study has been made of rail stresses, in an attempt to eliminate the cause of transverse rail fissures and broken rails. The battering of rail ends under heavy wheel loads also has come under this study, and has led to the development of the continuous welded track and to experiments with concrete subgrade. As another part of this development there have been radical changes in the design of steam-locomotive driving wheels and reciprocating parts, to reduce the pounding of drivers caused by dynamic augment at high speeds. Incidentally, it may be pointed out here that dynamic augment is absent with electric or Diesel-electric drive.

3. *Train Control.* There is a pronounced and rapid development toward the governing of all train movements by signal indication. While all passenger and many freight trains move under official printed time table authority, no train can be permitted to occupy a block against a restrictive signal indication. It is therefore imperative to furnish reliable track circuit and relay equipment for displaying proper signals, and for informing the dispatcher as to the condition of any piece of track. A recent and expanding development in this connection is the "centralized traffic control", whereby a dispatcher is given control and indication over the territory under his jurisdiction, all switching operations being performed remotely by an electric control system similar to the "supervisory control" used in power systems.

4. *Speed.* In recognition of the fact that schedule speeds are affected by deceleration to the same extent as by acceleration, and because the use of higher speeds with modern equipment has involved questions of the adequacy of train-stopping distances in relation to signal spacing, there have been important developments in braking equipment. One of the most radical has been a means of automatically varying the braking force as a function of train speed, to take account of the variation in the coefficient of friction between brake shoes and wheels with variations in speed.

5. *Passenger equipment.* To the public, the most striking railway development of recent years has been the improvement of passenger equipment as regards speed and comfort. The most rapidly spreading improvement in this regard has been the use of air-conditioning apparatus, whereby the interior air of passenger cars is maintained at a predetermined state of temperature and cleanliness regardless of outdoor conditions. The general appearance and design of modern passenger trains also has undergone radical change at the hands of professional industrial designers engaged by the railroads and manufacturers to aid in the improvement of rolling stock. The reduction in weight of passenger trains already has been discussed.

Parallel with these engineering developments, noteworthy economic experiments in rail transport are under way. The greatest of these is undoubtedly the electrification of the Pennsylvania Railroad.[9,10] The Pennsylvania on its New York-to-Washington line handles perhaps the heaviest mixed traffic density in the world, and is the first instance of electric traction having been adopted on a scale broad enough to exploit its real advantages and to look on electric traction primarily as a tool for efficient transport rather than as a means of solving a specific problem in motive-power application.

All these developments seem to point to the future organization of the railways as a machine for the rapid and efficient production of traffic units—ton miles or passenger miles. The railway system shows up most favorably when it has to handle very dense traffic over long distances, or, in other words, when it can manufacture transportation on a mass-production basis. The interpretation of recent developments in the physical equipment of the railways as steps toward such an organization may be incorrect, but the fact remains that the railways cannot survive as an economic unit unless the traffic density has a sufficiently high average to pay the operating expenses and fixed charges. It seems to the author that increases in traffic density and reductions in operating expenses can best be attained by some program of co-ordination and consolidation such as outlined earlier in this article. If such a program is carried out, the engineering problems discussed here will be of very real importance.

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9. Single-Phase Electrification on the Pennsylvania Railroad, H. C. Griffith. *Journal* of the Institution of Electrical Engineers, volume 81, 1937, page 91.

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Discussion

For discussion see page 1247.

Report of the Board of Directors

THE board of directors of the American Institute of Electrical Engineers presents herewith to the membership its 56th annual report, for the fiscal year ending April 30, 1940. A general balance sheet showing the condition of the Institute's finances on April 30, 1940, together with other detailed financial statements, is included herein. This report contains a brief summary of the principal activities of the Institute during the year, more detailed information having been published from month to month in ELECTRICAL ENGINEERING.

BOARD OF DIRECTORS' MEETINGS

During the year, the board of directors held five meetings, four in New York City, and one in San Francisco, Calif. The executive committee meetings in December and March were held in place of meetings of the board. Information regarding many of the more important activities of the Institute which have been under consideration by the board of directors and the committees is published each month in the section of ELECTRICAL ENGINEERING devoted to "Institute Activities".

PRESIDENT'S AND NATIONAL SECRETARY'S VISITS

President Farmer and National Secretary Henline attended the combined summer and Pacific Coast convention, the winter convention, the Great Lakes District meeting in Minneapolis, Minn., and the Middle Eastern District meeting in Scranton, Pa. They also visited many Sections and Student Branches. During May, President Farmer will visit the following Sections: Cincinnati, Cleveland, Erie, Worcester, Niagara Frontier, and Rochester; also he will attend a conference on student activities of the North Eastern District at Troy, N. Y. Both President Farmer and National Secretary Henline will attend the summer convention in Swampscott, Mass.

The places visited by President Farmer follow:

Alabama
Alabama Section, Birmingham
University of Alabama Branch, University

California
Summer and Pacific Coast convention, San Francisco

Connecticut
Connecticut Section, New Haven

District of Columbia
Washington Section
American Engineering Council, Washington

Georgia
Georgia Section, Atlanta
Georgia School of Technology Branch, Atlanta

Illinois
Chicago Section, executive committee
Midwest Power Conference, Chicago

Louisiana
New Orleans Section
Tulane University Branch, New Orleans
Louisiana State University Branch, Baton Rouge

Michigan
Michigan Section, Ann Arbor

Minnesota
Great Lakes District meeting, Minneapolis
Conference on student activities, Great Lakes District, No. 5, Minneapolis

Mississippi
Conference on student activities, Southern District, No. 4, State College, Miss.

New York
New York Section
Winter convention
Ithaca Section
Cornell University Branch, Ithaca
Schenectady Section
Syracuse Section
Syracuse University Branch

North Carolina
North Carolina Section, Durham

Pennsylvania
Middle Eastern District meeting, Scranton
Conference on student activities, Middle Eastern District, No. 2, Scranton
Pittsburgh Section
Philadelphia Section

Canada
Toronto Section

The places visited by the national secretary are the following:

California
Summer and Pacific Coast convention, San Francisco
Conference on student activities, Pacific District, No. 8, and North West District, No. 9, San Francisco

District of Columbia
Washington Section
American Engineering Council, Washington

Idaho
University of Idaho Branch, Moscow

Illinois
Armour Institute of Technology Branch, Chicago
Urbana Section

Indiana
Central Indiana Section, Indianapolis
Fort Wayne Section

Massachusetts
Lynn Section
Worcester Section
North Eastern District meeting, Springfield
Conference on student activities, North Eastern District, No. 1, Springfield

Minnesota
Great Lakes District meeting, Minneapolis
Conference on student activities, Great Lakes District, No. 5, Minneapolis

Montana
Montana Section, Butte

New York
New York Section
Schenectady Section
Winter convention

Ohio
Akron Section
Columbus Section

Pennsylvania
Sharon Section
Philadelphia Section
Middle Eastern District meeting, Scranton
Conference on student activities, Middle Eastern District, No. 2, Scranton

Rhode Island
Providence Section

Washington
State College of Washington Branch, Pullman
Seattle Section
Spokane Section

Wisconsin
Madison Section
Milwaukee Section

Canada
Vancouver Section

NATIONAL CONVENTIONS

Two national conventions were held during the year, and a brief report on each follows:

Summer and Pacific Coast Convention. The 55th summer convention combined with the 27th Pacific Coast convention was held in San Francisco, California, June 26–30, 1939. In addition to the annual business meeting, and conference of officers, delegates, and members, there were nine technical sessions at which 41 papers were presented, one general session, and two informal technical conferences. Doctor Adam S. Bennion, assistant to the president of the Utah Power and Light Company, gave an address on "Human Relationships" at the general session. In addition, there were two student technical sessions with presentation of 11 papers, and a Student Branch dinner with informal addresses by President John C. Parker, Vice-President H. W. Hitchcock, and Doctor R. W. Sorensen, chairman of the committee on student branches.

Entertainment and sports features of the convention were a tea, "AIEE Day" at the Golden Gate International Exposition on Treasure Island, banquet, and golf and tennis tournaments held in competition for the Mershon, Lee, and John B. Fisken trophies. The attendance was 940.

Annual Meeting. The annual business meeting of the Institute was held on Monday morning, June 26. The annual report of the board of directors for the fiscal year which ended April 30, 1939, was presented in abstract by the national secretary. A report on the finances of the Institute was presented by National Treasurer W. I. Slichter. The report of the committee of tellers upon the election of officers for the year beginning August 1, 1939, was presented, and President-Elect Farmer responded to his introduction with a brief address. During this session, the Lamme Medal for 1938, which was awarded to Marion A. Savage, was presented, being received by P. L. Alger who represented the medalist.

Winter Convention. The 28th winter convention was held in New York City, January 22–26, 1940, with a program including 85 papers presented in 18 technical sessions and 8 technical conferences. A brief general session was held at which the Alfred Noble prize was presented to C. E. Shannon (A'39).

At an evening session, the Edison Medal was presented to Philip Torchio, and the Hoover Medal was presented to Doctor Gano Dunn.

A smoker, numerous inspection trips, and ladies' events completed the program of the convention. The registration was 1,626, which was slightly above the 1939 figure, and the highest since 1924.

DISTRICT MEETINGS

Brief reports on the three District meetings held during the year are given below:

North Eastern District Meeting. This meeting and student Branch convention was held in Springfield, Mass., May 3–5, 1939.

There were four technical sessions, two student sessions, and a general session with addresses by President John C. Parker and President C. W. Kellogg of the Edison Electric Institute, the latter on the subject "The Engineer as a Citizen". The Faraday Medal of The Institution of Electrical Engineers, Great Britain, was presented to Doctor W. D. Coolidge, director of the General Electric Company Research Laboratory, Schenectady, N. Y., by Dr. Gano Dunn, local honorary secretary of the IEE for the United States. Inspection trips, stag smoker, ladies' entertainment, and banquet completed the meeting. The attendance was 439.

Table I. Section and Branch Statistics

	For Fiscal Year Ending				
	April 30, 1936	April 30, 1937	April 30, 1938	April 30, 1939	April 30, 1940
Sections					
Number of Sections	61	62	65	67	70
Number of meetings held	540	621	624	635	701
Total attendance	85,501	74,950	110,148	85,692	91,949
Branches					
Number of Branches	118	119	120	120	121
Number of meetings held	1,045	1,363	1,334	1,190	1,346
Total attendance	45,304	46,121	60,446	53,380	64,972

Great Lakes District Meeting. This meeting was held in Minneapolis, Minn., September 27–29, with four technical sessions, at which 18 papers were presented, and one student session. The entertainment program consisted of a dinner and dance with a demonstration by Doctor Phillips Thomas, research engineer of the Westinghouse Electric and Manufacturing Company. Also Doctor W. A. O'Brien, pathologist of the University of Minnesota, addressed an audience at a noon luncheon. Inspection trips, ladies' events, a District conference on student activities, and a District executive committee meeting were included in the program. The registration was 230.

Middle Eastern District Meeting. The tenth meeting of this District was held in Scranton, Pa., October 11–13, 1939, with 5 sessions at which 21 technical papers were presented. A District conference on student activities, a District executive committee meeting, ladies' events, inspection trips, stag smoker, and informal dinner, at which President R. E. Desvernine of the Crucible Steel Company of America gave an address on "Business and World Politics", completed the program. The attendance was 313.

SECTIONS

The three new Sections organized, Charleston, El Paso, and South Carolina, brought the total number to 70. The name of the Dallas Section was changed to "North Texas", and its territory was extended to the north and northwest. On account of the fact that its territory includes the major part of the state of Maryland, the name of the Baltimore Section was changed to "Maryland". Nine unassigned counties in Maine were assigned to the Lynn Section, which now has the entire state. Organization of new Sections and extensions of the territories of others have reduced to about 100 the number of members in the United States outside Section territories.

Activity was reported by all Sections. The total number of meetings reported was 701 as compared with 635 for the preceding year, and this was the sixth consecutive year in which the number exceeded that for the preceding year.

Fifteen Sections reported that their 30 technical groups or other subdivisions had held 124 meetings of these types in addition to the 131 regular Section meetings.

In October, the Sections committee issued a printed pamphlet "Section Activities", which is expected to be of material assistance to Section officers by indicating the types of activities which have been found to be most effective. A second edition of the pamphlet "The Electrical Engineer", for the guidance of high school students interested in electrical engineering, was prepared under the joint direction of the Sections committee and the committee on Student Branches.

The Sections committee sent a communication to all Sections, in October, recommending the appointment by each of committees on (*a*) legislation, (*b*) economic and social affairs, and (*c*) vocational guidance.

Many Sections have shown increased interest in the stimulation of younger members to participate in the activities more freely. The continuing interest in and success of the technical group discussion plan, the inclusion of younger members among Section officers and committees, and the appointment of committees to develop activities for the younger engineers are a few of the tangible evidences of progress in this direction.

The Sections committee has been developing a comprehensive plan for the preparation of news items on outstanding Section activities for publication in Electrical Engineering, and many such items have appeared in recent months. Through the use of extensive questionnaires the committee is making a thorough study of all phases of Section activities.

More detailed information on these activities may be found in the annual report on Section and Branch activities in the June issue of Electrical Engineering, pages 250–3.

STUDENT ACTIVITIES

A new Branch organized at the University of Alberta brought the total number to 121.

The total number of meetings reported was 1,346, as compared with 1,190 for the preceding year, 1,334 for 1937–38, and 1,363 for 1936–37. Only two Branches failed to report any activity, 21 held more than 15 meetings each, 24 held from 12 to 15, 40 held from 8 to 11, 26 held from 4 to 7, and 8 held from 1 to 3 meetings.

Although the total number of student talks at Branch meetings was higher than for the preceding year, 767 as compared with 725, it was far below the numbers for some previous years. The largest number reported for any year was 1,130 for 1936–37.

The numbers of student sessions and papers at the summer-Pacific Coast convention and three District meetings are given in the reports on those meetings. The students have continued to show keen interest in such participation, and the quality of student papers and their presentation has been high.

With the co-operation of the committee on Student Branches, the committee on safety again suggested that each Branch have presented a paper on prevention of accidents or remedial measures after electric shock. A large number followed the suggestion.

A meeting of the committee on Student Branches was held during the 1940 winter convention, with 50 members and invited guests present. A report on the meeting appeared on pages 124–5 of the March issue of Electrical Engineering.

The committee on Student Branches and the Sections committee co-operated in the preparation of a second edition of the pamphlet "The Electrical Engineer" for the guidance of high school students interested in electrical engineering. Distribution of copies was begun in March.

The committee on Student Branches urged the Branches to celebrate the 75th birthday of Doctor Charles F. Scott, originator of the plan for organizing Branches. The Scott motion-picture films were shown by about 75 Branches, in many cases at joint meetings with neighboring Sections or Branches.

Of the 1,624 enrolled students whose terms were expected to expire on April 30, 911 (56 per cent) applied for admission as Associates. Some are eligible to continue as enrolled students on account of remaining in school.

A more detailed summary of student activities may be found in the annual report on Section and Branch activities in the June issue of Electrical Engineering, pages 250–3.

TECHNICAL PROGRAM COMMITTEE

The technical papers on convention programs have been of a high quality, with many favorable comments received. Pursuing the policy of the previous year, the technical program for the 1940 winter convention was enlarged to 18 technical sessions and 8 technical conferences, one more technical session and three more conferences than were held in the previous year. The enlarged program met with an excellent response from the membership, as evidenced by the registered attendance, 1,626, which was slightly more than the attendance of the previous year, and the largest attendance since 1924. The committee also arranged the technical program for the 1939 summer and Pacific Coast convention, and papers were supplied for the District meetings in Springfield, Mass.; Minneapolis, Minn.; and Scranton, Pa. A total of 177 papers were presented at these two national conventions and three District meetings, of which 151 were recommended for Transactions publication.

Table II presents statistics for the pro-

Table II. Technical Programs, Last Two Years

	Year Ending April 30, 1940	Year Ending April 30, 1939
Number of national conventions	2	3
Number of District meetings	3	3
Registration at national conventions and District meetings	3,548	4,100
Number of papers presented	177	184
Number of papers recommended for TRANSACTIONS	151	169
Estimated number of pages required for printing papers in TRANSACTIONS	886*	986*
Average length of papers recommended for TRANSACTIONS	5.86	5.83
Number of technical sessions	43	50
Number of technical conferences	10	12

* Partly estimated.

grams of the past two years. These statistics are not directly comparable, as there was one less national convention during the past year due to the fact that the summer and Pacific Coast conventions were combined.

Oral Presentations. With a view toward improving the effectiveness of the oral presentations, each speaker and presiding officer at the last winter convention has been canvassed. Many helpful suggestions from a broad section of actual experience have been received. These suggestions will be carefully analyzed, mimeographed, and sent to speakers on future programs. In this way it is hoped to bring about improvement in some of the oral presentations to the mutual advantage of both the speakers and the audience.

Technical-Committee Reports. The preparation of technical-committee reports on the progress of the art has been stimulated. A report on progress of the art in electrical machinery was presented at the summer convention. Reports on progress in the production and application of light and progress in power generation, 1933–39, were presented at the winter convention. Reports by the committees on communication, protective devices, electronics, and instruments and measurements are in preparation. As most of the technical committees have not had reports since 1934, and it is planned to include developments during the intervening years, the reports will be more comprehensive than heretofore. As an experiment and for the purpose of making the reports more readable and presentable, some of the reports will be prepared by an experienced writer. Through this service, technical committee reports will receive more extensive publicity in circles outside of Institute activities.

New Trends in Policy. While the average length of papers for the fiscal year has remained practically the same as for the preceding year, 5.86 pages per paper as compared with 5.83, nevertheless the papers for the past winter convention as compared with the winter convention of the previous year were reduced from 6.50 pages per paper to 5.60. Technical committees have been requested to continue their efforts for shorter papers, which will permit the scheduling of more papers within the annual budget. Notwithstanding this reduction, when the winter convention program was determined, there still remained 51 papers on the docket which had to be held over for future meetings.

As a result of the foregoing situation, a subcommittee was appointed. It was believed that some of the more highly specialized papers of limited interest could be presented in technical conferences without TRANSACTIONS publication, and that more material could be presented in this way. The duties of the subcommittee were to study the whole problem, and a report entitled "Suggested Modifications in Methods of Handling AIEE Technical Papers and Conferences" has been made. This report was discussed at the March 15 meeting of the technical program committee, and referred back to the subcommittee for revision in the light of the discussion at that meeting.

The revised report of the subcommittee, if accepted, will divert some of the more highly specialized papers of interest to only a few and other papers which are only of transitory interest from the TRANSACTIONS to a less expensive form of publication. These papers would be permanently recorded by the publication of synopses. Preprints would be made available and the original copy filed in the Engineering Societies Library. Other unapproved conference papers would also be preserved by filing in the Library at the option of the author. The report also recommends the publication of a synopsis of every formal paper in ELECTRICAL ENGINEERING as soon as the paper has been approved for preprinting. It also recommends that some papers with longer mathematical calculations should have the appendices made available in preprint form only.

Acknowledgments. The committee is grateful for the active participation and cooperation in the work by its members, the chairmen of the technical committees, the reviewers of papers, and the members of the Institute's staff.

PUBLICATION COMMITTEE

With the transition period involved in the adoption of the present publication policy largely behind, constant attention has been given to observing residual defects and devising minor modifications to overcome them. While the general reaction of the membership continues to be very favorable, some adverse criticisms have been received and these have had and are having careful consideration.

The booklet "Preparation of Manuscripts" has been completely revised and enlarged, and is now available under the title "Information for Authors". A lack of familiarity of some authors and members with the provisions of the present publication policy has caused confusion in some cases. The new booklet "Information for Authors" contains for the first time a full statement of publication policy, and it is hoped that members and authors will take advantage of this opportunity to become thoroughly familiar with the publication policy.

During the year, a critical study has been made of AIEE publication costs, and this study included a comparison with the publication costs and procedures of three other national technical organizations. The analysis shows that in relation to the total cost to them, our members receive publication service which compares very favorably with that provided by the other organizations. The economy and efficiency of our publication procedures is indicated by the fact that last year the net publication expense per member in our Institute was only one-half of the corresponding figure in another organization having nearly the same membership.

The operating statement for the first half of the appropriation year (October 1st, 1939, to March 31st, 1940) shows that the publication committee has operated within its budget for that period.

Recently Donald M. Simmons, who has rendered valuable service on the publication committee for several years, was appointed vice-chairman of the committee.

The publication committee continues to welcome comment and criticism from all Institute members.

MEMBERSHIP COMMITTEE

The membership committee has endeavored to improve its effectiveness this year by laying increased stress on better organization of its various groups, thereby obtaining better co-ordination of effort and less duplication of work among the many members assigned to this activity. The national committee duties were outlined in

Table III. Membership Statistics for the Fiscal Year Ending April 30, 1940

	Honorary	Fellow	Member	Six-Year Associate	Associate	Total
Membership on April 30, 1939	9	722	4,392	5,726	5,756	16,605
Additions						
Transferred		58	171	515		
New members qualified			127	25	1,472	
Former members reinstated		1	3	16	12	
	9	781	4,693	6,282	7,240	
Deductions						
Died	1	10	47	37	8	
Resigned			39	140	126	
Transferred			58	151	535	
Dropped		4	54	230	352	
	1	14	198	558	1,021	
Membership on April 30, 1940	8	767	4,495	5,724	6,219	17,213

Table IV. Number of Applications Received From Enrolled Students and From All Others

Year Ending	From Students	From All Others	Total
April 30, 1940	911	918	1,829
April 30, 1939	849	872	1,721
April 30, 1938	739	932	1,671
April 30, 1937	716	1,040	1,756
April 30, 1936	631	946	1,577

Table V. Number of Enrolled Students

April 30, 1940	5,517	(2,991)
April 30, 1939	5,242	(2,271)
April 30, 1938	5,037	(2,428)
April 30, 1937	4,503	(2,249)
April 30, 1936	4,049	(1,991)

Following the number of students reported for April 30 of each year is indicated within parentheses the number of new applications received during that year; the difference between this number and the reported total, of course, reflects the number of renewals of Student enrollment for the corresponding period.

Table VI. Number of Members in Section Territory Reinstated

August 1, 1939 to April 30, 1940	249
Year Beginning August 1, 1938	354
Year Beginning August 1, 1937	325
Year Beginning August 1, 1936	503
Year Beginning August 1, 1935	663

a communication from the chairman at the start of the committee year.

The Section membership committees were supplied copies of specially prepared instructions and guides to assist their early organization into efficient working groups. A census of Section membership committee personnels was requested early in the year to stimulate early organization in the Sections and to permit the Institute to give these men recognition for the effort they put forth each year. Accordingly, the complete Section membership committee lists were published in the January 1940 issue of ELECTRICAL ENGINEERING. The membership committee is grateful to the publication committee for its full co-operation in this effort.

As of January 1940, there were 494 men assigned to Section membership committees with an unknown additional number working on Section subcommittees.

In the national committee ten vice-chairmen continued as the membership "chiefs" of their respective Districts, and a small group of five members-at-large was set up for advice and assistance to the chairman. The committee held two meetings during the year at headquarters.

The committee has gone further in the direction of personalizing membership work both to bring the Institute closer to its Section committees and to bring the entire membership closer to the Institute. The Sections have been asked to give personal recognition to each new member at his first meeting and to give recognition to the proper membership committeeman responsible for obtaining the new addition to the Section at the same meeting.

The chairman has written personally, so far as was possible, to each member who desired information concerning membership matters, and has received many letters indicating favorable reaction to this policy.

Table VIII. Memberships Fully Paid

Year	Membership as of April 30	Number of Members Fully Paid as of April 30	Per Cent Fully Paid
1940	17,213	14,997	87.1
1939	16,605	14,371	86.5
1938	16,078	14,127	87.9
1937	15,308	13,439	87.8
1936	14,600	12,446	85.2
1927*	18,344	16,247	88.6

* Year of maximum membership.

Table IX. Record of AIEE Membership

	Total Membership May 1		Total Membership May 1		Total Membership May 1
1884	71	1904	3,027	1923	15,298
1885	209			1924	16,455
1886	250	1905	3,460		
1887	314	1906	3,870	1925	17,319
1889	333	1907	4,521	1926	18,158
		1908	5,674	1927	18,344
		1909	6,400	1928	18,265
1890	427			1929	18,133
1891	541				
1892	615	1910	6,681		
1893	673	1911	7,117	1930	18,003
1894	800	1912	7,459	1931	18,334
		1913	7,654	1932	17,550
		1914	7,876	1933	17,019
1895	944			1934	15,230
1896	1,035	1915	8,054		
1897	1,073	1916	8,202	1935	14,269
1898	1,098	1917	8,710	1936	14,600
1899	1,133	1918	9,282	1937	15,308
		1919	10,352	1938	16,078
1900	1,183			1939	16,605
1901	1,260	1920	11,345		
1902	1,549	1921	13,215	1940	17,213
1903	2,229	1922	14,263		

The assistance of the student counselors was solicited in January and February in an effort to encourage a maximum number of eligible Enrolled Students to apply for Associate grade. The counselors' help was very valuable, and the committee extends its appreciation to them.

It is the feeling of the committee that the improvements attained in organization were effective and well worth the effort. Evidence of this may be seen in tables III and IV, where it is shown that the total membership has increased from 16,605 to 17,213, and that 1,829 applications were received during the year ending April 30, 1940. The number of applications shows an increase of over 6 per cent above the same figure last year.

Table V shows another increase in the number of Enrolled Students. This is important since about half of the applications for membership come from the Students, as will be seen in table IV.

Table VI shows a continuing decrease in the number of delinquents reinstated, as may be expected unless business conditions become more adverse over a considerable period of time. This is due to the number of probable prospects being continually reduced.

Table VII shows about the same conditions as last year, though table VIII indicates a small improvement in the percentage of the members with dues fully paid.

DEATHS

The deaths of 103 members reported during the year are listed in table X.

BOARD OF EXAMINERS

The board of examiners held 11 meetings during the past year, averaging about two and one-half hours each, and considered 4,250 cases, divided as shown in table XI

The Procedure of the Board. It has become increasingly apparent to the board of examiners that the membership of the Institute needs to know more of the requirements in the constitution and bylaws with regard to membership grades as well as the technique of the board in handling applications for admission and transfer. Evidence of this is found in requests for procedure whereby the board could give advance indications of likely action in cases in which certain Sections are particularly anxious to avoid embarrassment to the applicants and to the membership and transfer committees. Realizing that every effort should be made to make the problems and procedures of the board of examiners better understood by the membership, it was voted that the following statement should be incorporated in this year's annual report to the board of directors.

A study of the "advance or informal opinion" suggestion makes it evident that the motivating factor is doubt as to eligibility, usually with regard to transfer to the grade of "Fellow", of men of local prominence. An attempt to provide for this situation is evidenced by the adoption in 1923 of the revision of the bylaws legalizing a "transfer proposal" form, which now, however, applies only to Member grade. The practical application of that proposal form immediately resulted in serious trouble. Due to local pressure, the signatures of endorsers were obtained on the proposal form, yet on the confidential reference blanks sent out by headquarters, as required by the constitution, the former endorsers in certain instances failed to recommend transfer. Again, the abbreviated records supplied on the proposal forms were not sufficient in many cases to permit the examiners to form even informal opinions on eligibility. Finally, men were proposed for advancement who had no desire to transfer, and

Table VII. Membership of the Institute, April 30, 1940

Of the 17,213 members reported for April 30, 1940, 14,997 are fully paid to April 30, 1940. The balance of 2,216 are divided into the following groups:

1. Members owing dues to April 30, 1939 Total number of members who have not acted upon resolution of board of directors adopted in January 1940 providing an extension of time for payment of dues	634
2. Members owing dues to April 30, 1940	1,582

(During the period May 1 to 15, 1940, 326 members have paid dues to April 30, 1940, reducing the total to 1,256.)

when word reached them of the action taken they expressed their objections to the procedure in no uncertain terms. In one notable case, where the examiners on consideration of the complete formal application felt compelled to reverse their informal decision based on a "proposal" record, a Section secession threat resulted. The board of examiners has therefore not encouraged the "advance opinion or proposal form" as an ineffective mechanism for obtaining the advance of applicants to appropriate and constitutionally authorized grades of membership.

The suggestion that unfavorable action be postponed until some local group has been contacted has also been studied on many occasions by many boards. As a matter of fact, the suggested procedure has been practiced as far as practical by the examiners for many years, within the limits of the constitution and bylaws. A considerable part of the correspondence of the secretary of the board of examiners is concerned with building up of complete professional records of applicants and obtaining the required number and grade of references. This correspondence is with local Section officers where the situation warrants such action, otherwise it is with the applicants or their references. The examiners believe, however, that to restrict the board in its present freedom to accommodate the method of inquiry to the circumstances in each case would lead to added difficulties, particularly in transfer cases. On all direct admission applications, the character question is involved; they are therefore posted and are the subject of general knowledge in advance of examiners action. On the contrary, transfer applications are frequently transactions solely between the applicants and the Institute; they are not published in any way and are otherwise known only to the supporting references. To divulge likelihood of unfavorable action to local officers or committees would be seriously objected to by many as a violation of a confidential privilege accorded to members. It might easily defeat the desired objective of encouraging meritorious transfers.

The suggestion has been made that the board of examiners be instructed to report tentative decisions to the local Sections in advance of final action on cases of direct admission or transfer to higher grades. The board feels, however, that stipulation of such procedure would tend to limit freedom of final action in accordance with the requirements of the constitution and bylaws and thus seriously curtail the independence and hamper the effectiveness of the board in its function of maintaining a high standard of Institute membership in the upper grades. It should be pointed out here that most of the members of the board of examiners have been active in Sections, local and national membership committees, over a long period of years and have had full experience in the problems now before us. To this they have added the experience on the board of examiners in dealing with many difficult cases. The board, after giving careful consideration to these important problems, therefore, does not look with favor upon the prescription of a procedure which would require a divulgement of probable action based on incomplete information about the applicant. Sponsors of such applicants often have receded from an advocacy which was founded on an incom-

Table X. Deaths of AIEE Members Reported During the Fiscal Year

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in ELECTRICAL ENGINEERING
Allen, Edwin W.	Associate '03	Jan. 1, 1940	Fellow	Feb. 1940, p. 90
Allensworth, H. R.	Associate '04	Nov. 29, 1939	Member	Feb. 1940, p. 91
Arnold, H. S.	Associate '11	July 24, 1939	Associate	Jan. 1940, p. 49
Ashbrook, R. B.	Associate '20	Aug. 15, 1939	Member	Nov. 1939, p. 492
Bayha, L. H.	Associate '33	Sept. 17, 1939	Associate	Dec. 1939, p. 541
Belshe, R. L.	Associate '35	May 24, 1939	Associate	July 1939, p. 323
Beugler, H. M.	Associate '03	July 16, 1939	Member	Sept. 1939, p. 398
Bourath, J. N.	Associate '21	May 13, 1939	Associate	Aug. 1939, p. 363
Bryant, R. B.	Associate '09	May 21, 1939	Associate	Aug. 1939, p. 363
Burke, James	Associate '93	Jan. 21, 1940	Fellow	March 1940, p. 134
Carnahan, G. C.	Associate '20	April 15, 1939	Associate	Aug. 1939, p. 363
Cleaves, B. F.	Associate '23	July 30, 1939	Associate	Sept. 1939, p. 398
Clements, F. W.	Associate '08	Dec. 12, 1939	Associate	May 1940, p. 217
Cook, Leonard	Associate '39	March 29, 1940	Associate	May 1940, p. 217
Cornford, F. J.	Associate '23	Nov. 5, 1939	Associate	Jan. 1940, p. 50
Cox, F. P.	Associate '01	Nov. 30, 1939	Fellow	Jan. 1940, p. 50
Cramp, William	Member '26	April 20, 1939	Member	June 1939, p. 280
Crosby, G. L.	Associate '15	Dec. 2, 1939	Member	Jan. 1940, p. 50
Crozier, H. W.	Associate '03	April 13, 1939	Member	June 1939, p. 280
Davis, C. W.	Member '29	Sept. 16, 1939	Member	Nov. 1939, p. 492
Davis, G. S.	Associate '04	Dec. 29, 1939	Member	April 1940, p. 174
Denehy, H.	Associate '12	Feb. 21, 1940	Associate	May 1940, p. 217
de Sousa, A. M.	Associate '28	1939	Associate	Oct. 1939, p. 445
Dill, G. C.	Associate '13	April 1, 1939	Member	June 1940, p. 258
Dodge, O. G.	Associate '93	June, 1936	Member	Feb. 1940, p. 91
Doherty, H. L.	Associate '98	Dec. 27, 1939	Fellow	Feb. 1940, p. 90
Donnelly, A. L.	Associate '18	1939	Associate	Jan. 1940, p. 50
Doxey, F. S.	Associate '26	Jan. 12, 1940	Associate	March 1940, p. 135
Fields, W. S.	Associate '25	March 9, 1939	Associate	June 1939, p. 281
Finley, J. H.	Associate '27	March 10, 1939	Associate	July 1939, p. 323
Firman, L. D.	Associate '06	1939	Member	Nov. 1939, p. 492
Foster, S. C.	Associate '11	April 22, 1939	Associate	Aug. 1939, p. 363
Fung, J. T. S.	Associate '35	1939	Associate	Dec. 1939, p. 541
Garton, W. R.	Associate '05	Sept. 15, 1939	Associate	Dec. 1939, p. 540
Garvin, J. P.	Associate '23	May 12, 1939	Member	Sept. 1939, p. 398
Gibson, J. J.	Associate '02	May 6, 1939	Fellow	June 1939, p. 279
Graef, W. H.	Associate '08	Feb. 20, 1940	Member	May 1940, p. 217
Gravell, J. H.	Associate '17		Associate	Feb. 1940, p. 90
Gray, B. T.	Associate '23	July 6, 1939	Associate	Sept. 1939, p. 398
Hagenbuch, L. G.	Associate '34	Oct. 1, 1939	Associate	Dec. 1939, p. 541
Harrington, A. L.	Associate '07	Nov. 15, 1939	Associate	March 1940, p. 135
Hecker, A. E.	Member '30	June 1, 1939	Member	Jan. 1940, p. 50
Henry, I. W.	Member '36	April 25, 1940	Member	June 1940, p. 257
Hesketh, Thomas	Associate '04	March 1939	Member	June 1939, p. 280
Hirt, E. L.	Associate '18	May 28, 1939	Associate	July 1939, p. 322
Hogshead, C. C.	Associate '35	March 14, 1939	Associate	Jan. 1940, p. 50
Honegger, Arnold	Associate '15	June 20, 1939	Member	Nov. 1939, p. 492
Hopkins, I. B.	Associate '37	Jan. 12, 1940	Associate	April 1940, p. 174
Horan, Michael	Associate '29	Jan. 19, 1940	Associate	May 1940, p. 218
Hughes, C. P.	Member '39	July 7, 1939	Member	Sept. 1939, p. 399
Jacobi, W. O.	Associate '10	April 10, 1939	Member	Aug. 1939, p. 362
Johnston, D. H.	Member '28	Sept. 12, 1939	Member	Dec. 1939, p. 541
Kennelly, A. E.	Associate '88	June 18, 1939	Fellow	July 1939, p. 322
Knight, C. D.	Associate '05	Feb. 28, 1940	Member	April 1940, p. 173
Komaru, Toda	Associate '20	March 14, 1939	Associate	Aug. 1939, p. 363
Kramer, C. H.	Associate '20	Feb. 28, 1939	Member	July 1939, p. 323
Lagomasino, J.	Associate '38	1940	Associate	June 1940, p. 258
Little, A. P.	Associate '09	Dec. 28, 1939	Member	March 1940, p. 135
Little, C. W. G.	Associate '96	1939	Associate	Nov. 1939, p. 492
Livingston, J.	Associate '98	Oct. 10, 1939	Associate	Dec. 1939, p. 541
Marks, L. B.	Associate '90	Nov. 22, 1939	Fellow	Jan. 1940, p. 50
McRae, W. R.	Member '17	1939	Member	Dec. 1939, p. 540
Merizzi, G.	Associate '12	1939	Associate	Aug. 1939, p. 363
Messick, C.	Associate '03	May 25, 1939	Member	July 1939, p. 323
Mountain, J. T.	Associate '04	April 16, 1939	Fellow	July 1939, p. 323
Mowry, H. W.	Associate '18	Dec. 10, 1939	Member	Feb. 1940, p. 91
Nielsen, W. C.	Associate '37	Oct. 4, 1939	Associate	Feb. 1940, p. 91
Norris, H. H.	Associate '03	April 14, 1940	Fellow	May 1940, p. 217
Nunn, P. N.	Associate '95	Oct. 27, 1939	Member	Dec. 1939, p. 540
O'Brien, P. O.	Associate '24	March 10, 1940	Associate	May 1940, p. 217
Ost, P. J.	Member '23	Dec. 30, 1939	Member	March 1940, p. 135
Piek, Stefaan	Associate '06	Dec. 13, 1939	Associate	Feb. 1940, p. 90
Popkin, M. B.	Associate '19	Jan. 29, 1940	Associate	March 1940, p. 135
Pringle, D. R.	Associate '07	Aug. 4, 1939	Associate	Oct. 1939, p. 445
Ralston, E. G.	Associate '21	April 17, 1939	Member	June 1939, p. 280
Ramsay, J. W.	Associate '36	July 22, 1939	Member	Sept. 1939, p. 398
Renshaw, E. N.	Associate '18	May 16, 1939	Associate	July 1939, p. 323
Reynolds, W. H.	Associate '04	Jan. 12, 1940	Member	March 1940, p. 135
Robertson, W. L.	Associate '05	Nov. 17, 1939	Associate	Jan. 1940, p. 50
Roth, Edouard	Associate '18	April 29, 1939	Associate	June 1939, p. 280
Rugg, W. S.	Associate '02	April 25, 1940	Member	June 1940, p. 257
Sawyer, R. E.	Associate '30	Oct. 29, 1939	Associate	Jan. 1940, p. 50
Scoville, G. A.	Associate '05	Jan. 14, 1940	Member	March 1940, p. 135
Servos, F. M.	Associate '19	July 8, 1939	Member	Sept. 1939, p. 397
Sessions, A. P.	Member '37		Member	Feb. 1940, p. 91
Seyfert, S. S.	Associate '05	Dec. 11, 1939	Member	Jan. 1940, p. 49
Shannon, J. H.	Member '25	Jan. 16, 1940	Member	March 1940, p. 135
Silbert, R. H.	Associate '17	1939	Member	Nov. 1939, p. 493
Smith, G. H.	Associate '20	Sept. 10, 1939	Member	Oct. 1939, p. 445
Smith, J. C.	Associate '03	June 24, 1939	Fellow	Aug. 1939, p. 362
Spangenberg, O. C.	Associate '16	June 6, 1937	Associate	Sept. 1939, p. 398

(Continued on next page)

Table X (Continued). Deaths of AIEE Members Reported During the Fiscal Year

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in ELECTRICAL ENGINEERING
Steele, G. F.	Associate '10	June 25, 1939	Member	Aug. 1939, p. 362
Stevens, W. C.	Associate '11	May 15, 1939	Fellow	July 1939, p. 322
Storms, C. A.	Associate '26	Aug. 21, 1939	Member	Oct. 1939, p. 445
Taggart, J. E.	Member '37		Member	April 1940, p. 173
Tibbetts, F. H.	Member '30	August, 1938	Member	July 1939, p. 322
Trudgian, Wm.	Associate '27	Sept. 19, 1939	Associate	Nov. 1939, p. 493
Vanderpoel, W. K.	Associate '08	Oct. 21, 1939	Member	Dec. 1939, p. 540
Wagner, G. J.	Member '20	Sept. 2, 1939	Member	Dec. 1939, p. 540
Waring, S. B.	Associate '35	1939	Associate	Nov. 1939, p. 493
Wilkinson, G. D.	Associate '25	July 5, 1939	Associate	Sept. 1939, p. 398
Wood, E. D.	Associate '21	Nov. 9, 1939	Member	Dec. 1939, p. 540
Woodcock, L. R.	Associate '09	July 1, 1939	Member	Sept. 1939, p. 397

plete record of professional attainment. On the contrary, the board believes the responsibilities and necessarily confidential procedures of the board would be better appreciated by the members if more emphasis were placed on the importance of filing complete professional records on the initial applications for direct admission or transfer to higher grades.

To correct any false impression as to the number of applications for admission and transfer on which the examiners have felt compelled to rule unfavorably, a summary for the last ten-year period, 1931–40, is presented in table XII.

It should be noted that the figures in table XII show a total rejection of Fellow grade applications over the ten-year period of only 29. The Member rejections (about 11 per cent) are apparently due in part to a failure to appreciate the technical experience required for that grade and the tendency of applicants to select Member grade merely because they have passed the 27-year lower-age limit. Comparison of conditions before and after the increase of Associate dues at the end of the six-year period seems to indicate that it has also been a potent factor in developing ineligible Member applications.

Table XI. Applications for Admission and Transfer

Applications for Admission		
Recommended for grade of Associate	1,724	
Re-elected to the grade of Associate	86	
Not recommended	4	1,814
Recommended for grade of Member	122	
Re-elected to the grade of Member	12	
Not recommended	44	178
Recommended for grade of Fellow	1	
Re-elected to the grade of Fellow	0	
Not recommended	0	1
Applications for Transfer		
Recommended for grade of Member	167	
Not recommended	24	191
Recommended for grade of Fellow	53	
Not recommended	3	56
Students		
Recommended for enrollment as Students		2,010
Total		4,250

Table XII. Rulings by Examiners 1931–40

	Recommended	Not Recommended
Admissions		
Associates	11,426	183
Members	1,078	284
Fellows	23	10
Transfers		
Members	1,781	184
Fellows	284	19

STANDARDS COMMITTEE

The four standards co-ordinating committees organized during the past year have devoted themselves to the tasks assigned to them.

Committee No. 1, Reference Values for Standards, has collected extensive data relating to existing practices on reference values. A paper covering this subject will be presented at the summer convention.

Committee No. 2, Standard Voltages and Currents, is keeping in close touch with the activities of the corresponding NEMA and EEI committees dealing with voltages above 100. Furthermore, a subcommittee has been organized to study the possibilities of standardization for voltages below 100. This subcommittee has collected considerable information on existing practices, and is ready to co-operate with the newly organized ASA sectional committee on the same subject.

Committee No. 3, Insulation Testing and Co-ordination, has appointed a group of AIEE representatives to work with the NEMA and EEI committees handling insulation co-ordination. In addition, a subcommittee on insulation testing and evaluation has been organized, which for the present is collecting data on existing practices in insulation testing and on methods for evaluating insulation. Another subcommittee on the standardization of wet tests has just been organized, and will attempt to unify greatly divergent standards provisions now existing.

Committee No. 4, Basic Principles for Rating of Electrical Machinery and Apparatus, sponsored several papers relating to its activity during the 1939 AIEE summer convention and at the District meeting in Scranton, Pa. The committee has had several meetings, and worked out a complete revision of standard No. 1, General Principles Upon Which Temperature Limits Are Based in the Rating of Electrical Machinery and Apparatus, which has been approved by the standards committee. The co-ordinating committee is now undertaking a study of short-time and intermittent ratings and loads, and a number of papers covering the subject will be presented at the summer convention.

In addition to these four committees, two new co-ordinating committees have been organized, namely: No. 5, Basic Theories and Units, and No. 6, Interference, Sound, and Noise Problems. Both committees are preparing to start actual work.

The first topic requiring the consideration of committee No. 5 will be the relative merits of the rationalized versus the unrationalized system of mks units, and the advisability of the endorsement of the preferred system by the Institute. The article "Revision of Electrical Units" by E. C. Crittenden in the April 1940 issue of ELECTRICAL ENGINEERING bears on this topic.

The co-ordinating committees, in addition to carrying on their major activities, have been active in reviewing various standards submitted to the standards committee for approval, to assure the maximum uniformity and co-ordination of these standards with others in existence. Additional co-ordinating committees may be organized as the desirability or need for other activities becomes apparent.

A decided increase in the activities of various sectional committees of ASA concerned with problems in the electrical field was noted during the year, and this called for the appointment of individual Institute representatives as well as AIEE delegations. Among such activities may be mentioned the following: Vacuum Tubes for Industrial Purposes—*C*60; Wire and Sheet Metal Gages—*B*32; Storage Batteries—*C*40; Industrial Lighting—*A*11; Radio—*C*16; Dry Cells—*C*18; and Carbon and Graphite Brushes—*C*64.

The chief development in published standards and one of great interest to the Institute was the issuance by ASA of the Proposed Standard for Transformers. This publication, it is hoped, will shortly be approved as an American standard. It replaces AIEE standards Nos. 12, 13, 14, 100, and the Test Code for Transformers. In addition, three revisions of existing Institute standards were submitted and will be published, as follows: General Principles Upon Which Temperature Limits Are Based in Rating of Electrical Machinery; Measurement of Test Voltages in Dielectric Tests; Air Switches and Bus Supports.

In line with the attitude of the standards committee during recent years in urging greater activity by the technical committees in the standards field, a plan has been suggested which should not only lead to a co-ordination of the standards work of those committees, but also provide for the continued development of that work from year to year.

UNITED STATES NATIONAL COMMITTEE OF THE IEC

The United States National Committee has been saddened during the past year by the death of its honorary president, Doctor Arthur E. Kennelly, and also of James Burke, one of its most prominent members and a past president of the International Electrotechnical Commission. Doctor Kennelly, who was also an honorary president of the commission, served as technical advisor to the USNC on several subjects. The loss of these two valued members will be keenly felt by the USNC.

The 1939 annual meeting of the USNC elected Doctor Clayton H. Sharp honorary president of the committee and E. C. Crittenden, representing the National Bureau of Standards, president. L. F. Adams, representing the National Electrical Manufacturers Association, was elected vice president and Doctor H. S. Osborne, representing the AIEE, was re-elected vice president and treasurer.

Because of the European situation, the work of the International Electrotechnical Commission, and consequently of the USNC, has been considerably curtailed. However, the general secretary of the IEC has advised the national committees that the central office intends to carry on as much work by correspondence as possible under existing conditions, and hopes to issue, as provisional IEC recommendations, those documents which had been circulated under the six months rule prior to the beginning of the war. These are: Specifications for Transformers (excluding instrument and testing transformers); Specifications for the Resistivity of Aluminum; Specifications for Porcelain Insulators for Overhead Lines of 1,000 V and Upwards; Specifications for Safety Requirements for Electric Mains-operated Radio Receiving and Amplifying Apparatus; Revision of Publication 42, Part 3, Graphical Symbols for Weak-current Installations; Rules for the Measurement of Test-Voltages by Sphere-Gaps in Dielectric Tests, Appendix to IEC Publication No. 46 on Steam Turbines—Supplementary Notes to Section 4 Instruments and Methods of Measurement.

The meetings of the IEC advisory committees which had been scheduled for September 1939 in New York have been indefinitely postponed. However, these meetings will be held in New York eventually, after world conditions have returned to normal.

Hydraulic Turbines, IEC 4. The USNC, as secretariat of the IEC committee on hydraulic turbines, has prepared proposed specifications for hydraulic turbines including rules for acceptance tests. These documents have been sent to the central office of the IEC for transmission to the national committees for their comments.

Rating of Rivers, IEC 14. At the last meeting of the committee of action of the IEC, held in Paris in June 1939, it was agreed by implication that the IEC project on rating of rivers should be abolished because the committee had ceased to function. The USNC has registered with the central office of the IEC an objection to the abolishing of this committee, with the information that there appears to be a good likelihood that an American standard will be adopted and that a further effort should be made to reach international agreement.

Rules for the Measurement of Test-Voltage by Sphere Gaps in Dielectric Tests. Early in 1939, there was circulated to the national committees of the IEC a document giving draft rules for the measurement of test voltage by sphere gaps in dielectric tests, which had been prepared following the Torquay meeting in 1939. Comparison showed considerable differences in these proposed IEC standards from the standards in common use in this country. It was deemed desirable, therefore, that the matter be reopened within the IEC sphere gap subcommittee, and a meeting was arranged in Paris in July 1939. This meeting was attended on behalf of the USNC by P. L. Bellaschi. It was agreed that certain modifications should be made in the proposed standards. The modifications were to be carried out by the British, German, and American representatives. This decision has opened the question of sphere-gap standards again in such a way that it will be possible for a general compromise satisfactory to the USNC to be arrived at.

COMMITTEE ON PLANNING AND CO-ORDINATION

The comprehensive report of the committee dated April 3, 1939, was presented at the meeting of the board of directors held on May 26, 1939. Recommendations regarding discussion of social and economic questions, presentation and publication of papers, financial support of research, and nominations procedure were approved. The president was authorized to refer any or all of the remaining topics in the report to regular or special committees for further study and report.

The committee held meetings in October and January, and considered many topics which had been referred to it by the board of directors, or the president, or received from other sources. It made recommendations to the board of directors on a considerable number of matters involving a wide range of Institute activities, and has others still under consideration. Additional topics have come to it, and an all-day meeting will be held on May 23.

The committee, as usual, invited District and Section officers to submit requests for any national conventions and District meetings desired in the calendar year 1941, and prepared a schedule of such meetings which was approved by the board of directors in January.

COMMITTEE ON LEGISLATION AFFECTING THE ENGINEERING PROFESSION

At the start of the administrative year, the committee on legislation affecting the engineering profession absorbed the special committee on model law for registration of engineers. In June 1939, the AIEE model law was endorsed by the Board of Directors. Committee activity during the 1939–40 year has been limited to following up the discussion of this law and correspondence with engineers regarding proposed legislation in the various states.

Copies of the AIEE model law were circulated to various groups for comment. Much favorable comment has resulted and a small amount of adverse criticism. In some states principles underlying the AIEE model law are being drafted into proposed legislation.

Following the committee's recommendation, a number of AIEE Sections have appointed committees on legislation to keep posted regarding licensing activity in their states and to support or oppose according to local opinion. It is highly desirable that all Sections appoint such committees. Sections should afford all AIEE members opportunity to review personally proposed legislation, so that any position taken by a Section may be the considered majority opinion after full discussion. It should also be recognized that the scope of such committees should be confined to legislation affecting the profession.

The AIEE committee on legislation affecting the engineering profession desires to be kept informed by Sections on local activity in licensing.

COMMITTEE ON CONSTITUTION AND BYLAWS

The committee interpreted several sections of the bylaws referred to it, resulting in amendments to the bylaws. There were no meetings of the committee, the work being conducted by correspondence.

COMMITTEE ON ECONOMIC STATUS OF THE ENGINEER

The committee was very glad to cooperate with the similar committee of The American Society of Mechanical Engineers under the chairmanship of Dean C. J. Freund, in a consideration of unionization of engineers. This took the form of a paper on this topic by James H. Herron in *Mechanical Engineering* for November 1939, and discussions in the January 1940 issue of the same magazine. Professor Sorensen has contributed a discussion which is planned for the June issue of ELECTRICAL ENGINEERING.

The chairman of the Institute committee was appointed a member of Mr. Bickelhaupt's AEC committee on engineering and allied technical professions. Since similar appointments were made representing the American Society of Civil Engineers, the ASME, and the Washington Society of Engineers, an opportunity was presented to coordinate the efforts of the several committees dealing with the economic status of the engineer.

During the year great interest developed in a request for knowledge of the classification and salaries of electrical engineers particularly on the part of the younger members of the Institute and of faculty and industrial personnel officers. Analyses made by Dean F. M. Feiker of the distribution of engineers by industry indicate that more than 40 per cent of all civil engineers are employed by federal, state, and local governments; fewer than 3 per cent of electricals and mechanicals are so employed. Thus there is a distinct difference in the personnel problems of the three Founder Societies, and the subject is a very difficult one. Nevertheless, the Pittsfield Section of the Institute has studied the matter intensively during the year, and has requested more data and information. In order to explore the field and see if there is any basis for uniformity in the classifications and ranges in various industries which might be fairly representative of the profession as a whole, President Farmer has appointed a committee under the chairmanship of A. C. Stevens to obtain information and report its findings to the board.

COMMITTEE ON AWARD OF INSTITUTE PRIZES

Four national and eight District prizes were awarded for papers presented during the calendar year 1939 and for the student papers presented during the academic year ending June 30, 1939.

The committee considered a large number of eligible papers which were of a high quality, and, in determining the awards for the national best paper prizes and initial paper prize, it had the benefit of the gradings and recommendations of the technical committees which had reviewed the papers. In addition to the national paper prize awards, six other papers were given honorable mention.

On recommendation of the committee on award of institute prizes, the board of directors approved changes in the rules governing the award of all District prizes. These changes permit consideration of all papers for each prize subsequent to the periods covered by the last previous awards.

COMMITTEE ON SAFETY

The committee on safety continued the discussion of a number of subjects related to electrical safety during the year. One meeting of the committee was held during the year, and other matters have been handled through correspondence.

A paper on "Safe Practices in College Laboratories," prepared by M. S. Coover of Iowa State College, was presented at the annual meeting of the Society for the Promotion of Engineering Education in June 1939. The committee understands that the National Safety Council is now preparing a pamphlet on this subject for inclusion in its standard safe practice series.

The committee was represented by its chairman at an open meeting held during the National Safety Congress in October 1939, to review the NSC Safe Practice Pamphlet on Portable Electrical Hand Tools, prior to the development of a revised edition.

Inasmuch as it has not been practicable to print an AIEE pamphlet on artificial respiration due to the expense involved, an effort was made to secure sufficient copies of such material from the American Red Cross. A review of the Red Cross material indicated that it contained many phases of first aid, in addition to artificial respiration instruction, and was probably too voluminous for the purpose intended by the committee. Copies of a small pamphlet issued by the American Gas Association and the Edison Electric Institute will be asked for by the committee for use in making contacts with colleges and Student Branches concerning the development of resuscitation instruction.

A subcommittee has been appointed to develop further contacts with colleges and Student Branches in co-operation with the committee on Student Branches, and another subcommittee has been appointed to develop further contacts with the AIEE Sections on matters pertaining to electrical safety.

After a discussion of accidents in the medical field, the chairman and secretary of the committee agreed to inquire, through individual contact, regarding any steps which may have been taken by the medical fraternity to reduce accidents in handling electrical medical equipment.

COMMITTEE ON AUTOMATIC STATIONS

The committee on automatic stations held two meetings during the year, and sponsored the presentation of two papers at the summer and Pacific Coast convention and one paper at the winter convention.

COMMITTEE ON BASIC SCIENCES

The committee on basic sciences has reviewed eight papers, and has four in process of review. One-half of a session at the winter convention was sponsored by the committee, and it will provide papers for one-half of a session at the summer convention. A technical conference on definitions was held at the winter convention, and a conference on nonlinear analysis will be held at the summer convention.

COMMITTEE ON COMMUNICATION

Under the sponsorship of the committee on communication 16 technical papers on communication were presented at the summer-Pacific Coast convention, the winter convention, and the District meetings in Springfield and Minneapolis, and technical conferences were held at the two national conventions. Much attention has been given to radio subjects, and 8 of the 16 papers mentioned above deal specifically with radio. Several others are of interest to specialists in that field. A communication session at the summer-Pacific Coast convention was sponsored jointly with the Institute of Radio Engineers, and another joint session will be held at the 1940 Pacific Coast convention. A paper on frequency modulation, with demonstrations, will be presented at the summer convention.

COMMITTEE ON EDUCATION

The principal effort of this committee has been devoted to considering, planning, and putting into effect a program which might be continued into future years. As early steps in this direction, an outline of suggested activities was considered, and an inquiry regarding the desirability of organizing committees on education in the Sections was sent to the Section chairmen. On account of the generous response from the Sections and their keen interest in educational activities, a subcommittee was formed to secure detailed information on efforts of an educational nature intended to aid young engineers, and to make it available to all Sections. Two other subcommittees were organized: one to study vocational guidance activities of the Sections, and the other to secure, for publication in Electrical Engineering, papers of an educational character of special interest to younger members. The committee hopes to make a significant contribution at the 1941 summer convention.

COMMITTEE ON ELECTRICAL MACHINERY

Five of the seven subcommittees of this committee have been active in developing and revising standards and test codes. The committee supplied three papers on "Hydrogen-Cooled Turbine Generators" for the North Eastern District meeting held in Springfield, Mass., in May 1939, and sponsored one session at the summer-Pacific Coast convention, and two sessions at the winter convention. A session and a technical conference on test codes at the 1940 summer convention will be sponsored by the committee.

COMMITTEE ON ELECTRIC WELDING

A session on welding sponsored by this committee at the 1940 winter convention created a large amount of interest. The committee has been devoting itself to the advancement of an educational program for extending co-operation among the manufacturers of welding equipment, the users, and the power companies to the end that good welding results may be secured at minimum first cost and operating cost. Parts I and II of a subcommittee report entitled "Power Supply for Resistance-Welding Machines" was presented at the winter convention, and several thousand reprints are being distributed.

COMMITTEE ON ELECTROCHEMISTRY AND ELECTROMETALLURGY

The committee held two meetings, and sponsored a joint symposium on power in the electrochemical industry by the Niagara Frontier Section of the Institute and the Niagara Falls section of the Electrochemical Society. It is planning a technical session for the 1941 winter convention in co-operation with the American Society of Metals. A subcommittee is investigating the effects of overvoltage in connection with large electrochemical and electrometallurgical installations.

COMMITTEE ON PRODUCTION AND APPLICATION OF LIGHT

A report, "Progress in Production and Application of Light" was published, in the name of the committee, in the December issue of Electrical Engineering. A demonstration lecture based on the report, together with addresses on lighting of the New York World's Fair and the New York Municipal Airport, constituted a session at the winter convention. The planning of and the soliciting of papers for this program constituted one of the major activities of the committee. The attendance, and the reception of the papers were most gratifying. Several additional papers have been reviewed and approved for presentation at the summer convention. The committee has collaborated with the editor in obtaining special articles for Electrical Engineering, and has a subcommittee which furnishes items of news interest. It has collaborated with the illumination group of the New York Section in organizing meetings for the discussion of lighting practice and developments in illuminating engineering.

COMMITTEE ON APPLICATIONS TO MARINE WORK

This committee has thoroughly reviewed standard No. 45, "Recommended Practice for Electrical Installations on Shipboard", through the work of 10 subcommittees. The editing committee is now preparing the recommended changes for submission to the standards committee.

COMMITTEE ON INSTRUMENTS AND MEASUREMENTS

The activities consisted primarily of reviews of technical papers, sponsorship of technical sessions at the two national conventions, preparation of reports on various phases of the progress of the art, and revision and extensions of AIEE standards. Material is being prepared for a comprehensive report on "Progress in the Art of Metering" covering electric power metering from its inception to the present day. A report on progress in telemetering is nearing completion. A report on "Wave Form in Dielectric Testing and Power Factor Measurements" was submitted to the standards committee. A revision of Standard No. 4, "Measurement of Test Voltage in Dielectric Tests", was completed and was approved by the standards committee and the board of directors. The committee also has reviewed several test codes prepared by other Institute technical committees; is compiling a bibliography on dielectric measurements made in the field; and is surveying possible sources of papers on oscillograph technique.

COMMITTEE ON POWER GENERATION

The committee on power generation has sponsored nine technical papers and one committee report for the two national conventions and the District meeting in Springfield. The committee report was for the years 1932–39, and was presented at the winter convention. In the development of plans for next year, two papers, on load swings and stability, and turbine generator governing, are in preparation for informal presentation and discussion at a round-table conference at the summer convention, with the thought that this procedure will aid in the selection of subjects for the 1941 winter convention.

COMMITTEE ON POWER TRANSMISSION AND DISTRIBUTION

One session at the summer-Pacific Coast convention and two sessions at the winter convention were sponsored by the committee. At a meeting of the committee, in September, the discussion of general organization work was followed by a prearranged discussion of engineering developments in the field of the committee. Such discussions have been of great interest to the members. A subcommittee has nearly completed a revision of standard No. 41, Insulators. A joint subcommittee representing three technical committees is preparing standards for bushings. A joint subcommittee is being organized to revise the grounding report prepared in 1931. A subcommittee is studying the advisability of a reorganization of committee on power transmission and distribution to meet present-day needs more effectively.

COMMITTEE ON PROTECTIVE DEVICES

The committee on protective devices has four subcommittees handling important divisions of its activities: circuit breakers, switches, and fuses; lightning arresters; relays; and fault-current-limiting devices. Each has carried on work in the development of standards and in other studies. Several standards have been substantially completed. A fifth subcommittee is cooperating with the committees on electrical machinery and power transmission and distribution. The committee sponsored three sessions on protective devices at the 1940 winter convention, one session and two technical conference papers at the summer-Pacific Coast convention, and one paper at each of the District meetings in Springfield and Minneapolis, with a total of 23 papers presented. A progress report is expected to be ready for the summer convention.

COMMITTEE ON RESEARCH

The principal activity of the committee on research has been in connection with service for the Institute as sponsor of two research projects supported by the Engineering Foundation: No. 66, Stability of Impregnated Paper Insulation, at Johns Hopkins University, and No. 74, Insulating Oils and Cable Saturants, at the Massachusetts Institute of Technology. Consideration has been given to the problems of the research method to ascertain whether there is sufficient interest to justify arrangements for a symposium on this subject. No definite conclusion has been reached. Efforts have been made to continue an activity started several years ago for the preparation of occasional news items on research subjects for ELECTRICAL ENGINEERING.

COMMITTEE ON TRANSPORTATION

The greater part of the work of the committee on transportation has been concerned with arranging to have papers written, reviewed, and presented at conventions. An effort has been made to have them cover the entire field of electrical engineering applied to transportation, and those presented at the winter convention covered a wide range of subjects with excellent discussion. The committee voted to initiate action toward having American tentative standard for railway motors (AIEE No. 11) advanced to status of full standard. At the invitation of the New Zealand Institution of Engineers, the committee also sponsored the preparation of a paper "Modern Rail Transport", by A. M. Wright of the Reading Railroad, for presentation at the New Zealand Centennial Engineering Congress scheduled for February 1940, but later cancelled on account of the war. The paper will be presented at the summer convention.

CHARLES LE GEYT FORTESCUE FELLOWSHIP COMMITTEE

A fellowship for graduate study in electrical engineering was established as a memorial to the late Charles LeGeyt Fortescue "in recognition of his valuable contributions to the electric power industry". The Westinghouse Electric and Manufacturing Company, with which Dr. Fortescue was associated throughout his professional life, set up a trust fund of $25,000 for this purpose.

The income will be administered by the Charles LeGeyt Fortescue fellowship committee of the Institute, and will provide fellowships of $500 or more to candidates selected annually by the committee. A bachelor's degree from an accredited college is a prerequisite, and the holders of the fellowships are expected to carry on their graduate studies in accredited engineering schools and to engage in research problems meeting the approval of the committee.

The first fellowship (1940–41) was awarded in March to Norman Z. Alcock, a senior in Queens University, Kingston, Ontario, who expects to engage in graduate study at the California Institute of Technology, Pasadena.

COMMITTEE ON AWARD OF COLUMBIA UNIVERSITY SCHOLARSHIPS

Seven formal applications were made for this scholarship, and from these the committee selected Cecil Clay Clements, a graduate of Alabama Polytechnic Institute. Mr. Clements qualified and registered in September, and pursued his studies during the winter session. However, in January, his father was taken very sick, and he was obliged to give up his work at Columbia, resigning the scholarship, and returning to his home.

The scholarship was not filled during the spring session because of the difficulty of making transfers in the middle of the academic year.

EDISON MEDAL

The Edison Medal, which is awarded by a committee composed of 24 members of the Institute, was, for 1939, awarded to Philip Torchio "for distinguished contributions to the art of central station engineering and for achievement in the production, distribution, and utilization of electrical energy", and was presented on January 24, 1940, during the winter convention. The medal may be awarded annually for "meritorious achievement in electrical science, electrical engineering, or the electrical arts".

JOHN FRITZ MEDAL

The John Fritz Medal board of award, composed of representatives of the national societies of civil, mining, mechanical, and electrical engineers, awarded the 36th medal (for 1940) posthumously to the late Doctor Clarence F. Hirshfeld, formerly chief of the department of research, Detroit Edison Company, "for notable leadership through research and development in power generation and electric traction and for being a great teacher and friend of men both young and old".

LAMME MEDAL

The Lamme Medal committee awarded the medal for 1939 to Norman W. Storer, retired consulting railway engineer, Westinghouse Electric and Manufacturing Company, "for pioneering development and application of equipment for electrical traction". Arrangements are being made for the presentation of the medal at the summer convention in Swampscott, Mass., June 24–28, 1940. The medal may be awarded annually to a member of the AIEE "who has shown meritorious achievement in the development of electrical apparatus or machinery".

ALFRED NOBLE PRIZE

This prize, established in 1929, consists of a certificate and a cash award of $500 from the income from a fund contributed by engineers and others to perpetuate the name and achievements of Alfred Noble, past president of the American Society of Civil Engineers and of the Western Society of Engineers. It may be made to a member of any of the co-operating societies, ASCE AIME, ASME, AIEE, or WSE, for a technical paper of particular merit accepted by the publication committee of any of these societies, provided the author, at the time of such acceptance, is not over 30 years of age. The award for 1939 was presented to C. E. Shannon (A'39) for his paper on "A Symbolic Analysis of Relay Switching Circuits".

WASHINGTON AWARD

The Washington Award for 1939 was bestowed upon Daniel Cowan Jackling, "for pioneering in large-scale mining and treatment of low-grade copper ores, releasing vast resources from formerly worthless deposits", and was presented to him at a dinner on April 15, 1940. This award may be made annually to an engineer by the commission of award composed of nine representatives of the Western Society of Engineers and two each of the American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Mechanical Engineers, and AIEE.

HOOVER MEDAL

The Hoover Medal was established through a trust fund created by a gift from Conrad N. Lauer, and is to be awarded periodically "to a fellow engineer for distinguished public service" by a board representing the national societies of civil, mining and metallurgical, mechanical, and electrical engineers. The fourth recipient of this medal was Doctor Gano Dunn, who was cited as "Long honored by his fellow engineers for professional achievement, he has, beyond that, exemplified high civic purpose and a devotion to public service which have earned for him the Hoover Gold Medal for 1939".

IWADARE FOUNDATION COMMITTEE

No Iwadare lecturer was chosen to go to Japan for the current year, nor has any Iwadare Fellow come to the United States.

EMPLOYMENT SERVICE

The Institute co-operates with the national societies of civil, mining, and mechanical engineers in the operation of the Engineering Societies Employment Service with its main office in the Engineering Societies Building, New York. Offices are operated in Chicago and San Francisco also. In addition to the societies named, others co-operate in certain of the offices as follows: New York—Society of Naval Architects and Marine Engineers; Chicago—Western Society of Engineers; San Francisco—California Section of the American Chemical Society and the Engineers' Club of San Francisco.

The service is supported by the joint contributions of the societies and their individual members who are benefited. In addition to the publication of the employment service announcements monthly in ELECTRICAL ENGINEERING, weekly subscription bulletins are issued for those seeking positions.

An analysis of this employment service as reported to the national societies is given in table XIII.

AMERICAN ENGINEERING COUNCIL

The American Engineering Council has continued to carry on a wide range of activities within the scope of its objectives: "to further the public welfare wherever technical and engineering knowledge and experience are involved, and to consider and act upon matters of common concern to the engineering and allied technical professions".

The broad range of the Council's activities is indicated by the titles of its committees: *Standing*—constitution, by-laws and standing rules, engineering and allied technical professions, finance, membership and representation, publicity and publications, regional activities, and public affairs; *Special*—patents, public works, surveys and maps, engineering economics, and inter-American engineering relations.

At the 20th annual meeting of the assembly held in Washington, D. C., January 11 and 12, 1940, a wide range of subjects received consideration. The 10th conference of secretaries of engineering societies was held on January 11 in connection with the meeting of the assembly.

It was reported at the annual meeting that the joint patent inquiry conducted under the sponsorship of the Council, the National Association of Manufacturers, and the National Industrial Conference Board, with funds supplied by NAM, had been nearly completed, and the report would be submitted to the sponsoring organizations within a few weeks.

A joint committee has been considering the reorganization of the Council in an effort to provide for "a combined professional approach to both public problems and interprofessional problems".

UNITED ENGINEERING TRUSTEES, INC.

This organization is the corporate body which holds title in the name of the four Founder Societies to their joint physical properties, namely, the Engineering Societies Building, the Engineering Societies Library, and the endowment funds of the Engineering Foundation. It operates and manages the Engineering Societies Building and administers certain joint activities of the four Founder Societies.

An abstract of the annual report of the UET, Inc., for the year which ended September 30, 1939, was published in ELECTRICAL ENGINEERING for December 1939, pages 531–2.

During the year, the "History, Charter, and Bylaws" was revised and brought up to date, new heating controls were installed in the Engineering Societies Building, and improvements in the ventilation of the meeting halls were made.

The John Fritz Medal Fund Corporation and the Daniel Guggenheim Medal Fund, Inc., dissolved their corporations and became committees of UET, Inc., which had, for a long period, managed the funds of the former and the books of the latter.

Table XIII. Analysis of Employment Service

Month	Men Registered				Men Placed			
	New York	Chicago	San Francisco	Total	New York	Chicago	San Francisco	Total
1939								
May	143	84	100	327	52	30	12	94
June	181	111	78	370	43	27	17	87
July	178	69	51	298	42	18	9	69
August	158	76	81	315	47	18	22	87
September	141	75	93	309	62	20	23	105
October	217	68	76	361	46	25	23	94
November	219	63	61	343	51	18	15	84
December	165	52	61	278	35	22	26	83
1940								
January	213	68	95	376	51	30	22	103
February	204	83	65	352	45	19	17	81
March	222	80	97	399	50	17	24	91
April	210	89	100	399	51	14	22	87
Total	2,251	918	958	4,127	575	258	232	1,065

ENGINEERING FOUNDATION

The Engineering Foundation is a joint organization of the national societies of civil, mining and metallurgical, mechanical, and electrical engineers established for "the furtherance of research in science and engineering, and the advancement in any other manner of the profession of engineering and the good of mankind".

A comprehensive abstract of the annual report of the Engineering Foundation for the year which ended September 30, 1939, was published in ELECTRICAL ENGINEERING for December 1939, pages 533–4, including information on each of a wide range of technical researches receiving support or assistance from the Foundation.

The research on "Stability of Impregnated-Paper Insulation" at the Johns Hopkins University is sponsored by the Institute. The work of the "Welding Research Committee" is sponsored by the American Welding Society and the Institute. The Foundation recently has granted support for a research on "Insulating Oils and Cable Saturants", at the Massachusetts Institute of Technology, under the sponsorship of the AIEE.

Assistance in nontechnical matters related to engineering has been granted to the Engineers' Council for Professional Development.

ENGINEERING SOCIETIES LIBRARY

The Engineering Societies Library, which was formed by combining the separate libraries of the four national societies of civil, mining and metallurgical, mechanical, and electrical engineers, and the preparation of a composite card catalog, has been expanded as a single engineering library, which probably constitutes the best collection of its type in the United States.

On September 30, 1939, the library had 146,999 volumes, 7,564 maps, and 4,440 bibliographies. Books, pamphlets, and maps totaling 15,914 were received during the year ending at that time. Current issues of 1,340 periodicals were received. Work progressed rapidly on a classified index to periodicals, and the index now contains more than 242,000 references to articles published since 1927.

Special services rendered by the library include: photoprints, searches, abstracts, translations, bibliographies, book loans by mail, etc. An abstract of the annual report of the library appeared on page 532 of ELECTRICAL ENGINEERING, December 1939.

ENGINEERS' COUNCIL FOR PROFESSIONAL DEVELOPMENT

This council was organized in 1932 to engage in activities leading to the enhancement of the professional status of the engineer. It includes three representatives of each of the seven participating organizations: the national societies of chemical, civil, electrical, mechanical, and mining and metallurgical engineers, the Society for the Promotion of Engineering Education, and the National Council of State Boards of Engineering Examiners.

The principal activities of ECPD include programs for the guidance of young individuals thinking of entering the engineering field, the accrediting of curricula of engineering schools, encouragement and assistance to individuals in their engineering and cultural studies during several years after graduation, and the development of criteria for indicating the attainment of the status of an engineer.

At the annual meeting held on October 20, 1939, the committee on engineering schools presented recommendations on 7 curricula, in four institutions, not previously acted upon, 74 curricula which had been provisionally accredited, and 9 curricula which had been rejected. It reported that 144 out of the total of 150-odd degree-granting engineering institutions had applied for inspection. The actions taken at that meeting brought the total number of curricula acted upon to 687, divided as follows:

Accredited unconditionally	433
Accredited for limited periods	82
Not accredited	172
Total	687

The complete list of accredited curricula appeared on page 526 of the December 1939 issue of ELECTRICAL ENGINEERING. A report on the annual meeting was published in the November 1939 issue of ELECTRICAL ENGINEERING, page 485.

REPRESENTATIVES

The Institute has continued its representation upon many joint committees and national bodies, with which it co-operates in a wide range of activities of interest and importance to engineers and others.

A list of representatives was published in the September 1939 issue of ELECTRICAL ENGINEERING and in the 1940 Year Book.

FINANCE COMMITTEE

The preparation of the annual budget is one of the major duties of the finance committee. The budget for the current year (beginning October 1) has some interesting features.

While the European War results in appreciable loss in revenue to the Institute, the steady growth in membership is expected to offset this influence, and it is anticipated that the revenues for the year will be substantially the same as for last year. By careful attention to economy in some items and by estimating closely the necessary expenses it has been possible in the current budget to provide for carrying on the activities of last year and for the expansion of these activities in some respects. Provision was made for about a 10 per cent increase in the Institute publications and for the addition to the headquarter's staff of one engineer whose time is largely devoted to standards work. The allowance for traveling expenses for delegates to Institute meetings and to District conferences was made more nearly adequate. An item of especial interest is the establishment of a pension reserve fund by the initial payment into this fund of $5,000 as a part of the current year's expenses. This is done as a first step toward the eventual establishment of a pension system for superannuated employees.

Under advice of investment counsel, the investments representing the reserve funds of the Institute are for the present being kept in fluid condition, cash and short-term bonds representing about one-half of the total.

Haskins and Sells, certified public accountants, have audited the books, and their report follows.

APPRECIATION

The board of directors again expresses to the national committees and the District, Section, and Branch officers its sincere thanks for their effective conduct of the activities in these important divisions. Their leadership has continued to produce a high degree of enthusiasm and constructive accomplishment. The board of directors also deeply appreciates the generous interest and support which the members have accorded its efforts on their behalf.

Respectfully submitted for the board of directors.

H. H. HENLINE,
National Secretary

May 24, 1940

HASKINS & SELLS
CERTIFIED PUBLIC ACCOUNTANTS

22 EAST 40TH STREET
NEW YORK

May 16, 1940

American Institute of Electrical Engineers,
33 West 39th Street, New York.

Dear Sirs:

We have made an examination of your balance sheet as of April 30, 1940, and of your recorded cash receipts and disbursements for the year ended that date. Our examination consisted of a review of the system of internal control and the accounting procedures of the Institute and examination or tests of its accounting records and other supporting evidence by methods and to the extent we deemed appropriate. We present the following:

Balance Sheet, April 30, 1940 (Exhibit A).
  Property and Restricted Funds Securities, Less Reserve for Securities of Doubtful Value (Schedule 1).
Statement of Recorded Cash Receipts and Disbursements of General Fund for the Year Ended April 30, 1940 (Exhibit B).
Statement of Recorded Cash Receipts and Disbursements of Property and Restricted Funds for the Year Ended April 30, 1940 (Exhibit C).

In accordance with the terms of our engagement, members and other debtors, except certain advertisers, were not requested to confirm to us the amounts receivable from them at April 30, 1940. In accordance with the usual practice of the Institute, no provision has been made for dues which may prove to be uncollectible.

In our opinion, subject to the comments in the next preceding paragraph, the accompanying Exhibit A fairly presents your financial condition at April 30, 1940, and the accompanying Exhibits B and C fairly present your recorded cash receipts and your disbursements of funds, as indicated, for the year ended that date.

Yours truly,

HASKINS & SELLS

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Balance Sheet, April 30, 1940

Exhibit A

ASSETS		
Property Fund Assets:		
One-fourth interest in the total net assets of United Engineering Trustees, Inc., exclusive of Trust Funds	$498,448.48	
Equipment:		
Library—volumes and fixtures	37,296.37	
Office furniture and fixtures (less reserve for depreciation, $30,091.16)	4,973.26	
Works of art, etc.	3,001.35	
Securities—at cost (market quotation value, $6,505.45)—Schedule I	6,355.45	
Cash (see Exhibit C)	3,330.32	
Total property fund assets		$553,405.23
Restricted Fund Assets:		
Securities—at cost, less reserve for securities of doubtful value (market quotation value, $177,352.94)—Schedule 1	$190,237.27	
Cash (see Exhibit C)	50,256.80	
Accrued interest receivable	69.99	
Total restricted fund assets		240,564.06
Current Assets:		
Cash (see Exhibit B)	$ 32,156.58	
Accounts receivable:		
Members—for dues	19,921.28	
Advertisers	518.40	
Miscellaneous	3,596.94	
Accrued interest on investments	1,588.72	
Inventories:		
Transactions, etc.	2,040.50	
Text and cover paper	5,746.61	
Work in process (May issue of Electrical Engineering)	3,887.76	
Badges	1,013.47	
Total current assets		70,470.26
Total		$864,439.55

LIABILITIES		
Property Fund Reserve		$553,405.23
Restricted Fund Reserves:		
Reserve Capital Fund	$221,074.78	
Life Membership Fund	8,772.17	
International Electrical Congress of St. Louis Library Fund	5,324.28	
Lamme Medal Fund	4,364.00	
Mailloux Fund	1,028.83	
Total restricted fund reserves		240,564.06
Current Liabilities—Accounts payable		11,396.55
Pension Fund Reserve		5,000.00
Deferred Income:		
Dues received in advance	$ 4,782.92	
Entrance fees and dues advanced by applicants for memberships	602.35	
Deferred credits for other unallocated receipts	396.39	
Subscriptions for Transactions received in advance	25.00	
Reserve for prepaid subscriptions for Electrical Engineering	7,400.00	
Total deferred income		13,206.66
Surplus		40,867.05
Total		$864,439.55

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Recorded Cash Receipts and Disbursements of General Fund for the Year Ended April 30, 1940

Exhibit B

Cash on Deposit With The National City Bank of New York, May 1, 1939		$ 61,988.30
Receipts:		
Dues (including $89,982.00 allocated to Electrical Engineering subscriptions)	$200,311.85	
Advertising	31,430.83	
Transactions subscriptions	6,987.56	
Electrical Engineering subscriptions	10,466.04	
Miscellaneous publications (preprints, standards, Transactions index, etc.)	13,470.17	
Students fees	13,621.25	
Entrance fees	7,020.54	
Membership badges	1,753.25	
Transfer fees	1,060.00	
Interest on investments, less purchased interest	6,778.17	
Miscellaneous	15.16	
Total receipts		292,914.82
Total		$354,903.12
Disbursements:		
Publications expense:		
Electrical Engineering	$ 81,689.94	
Transactions	11,209.62	
Year Book	6,314.46	
Miscellaneous publications (preprints, standards, Transactions index, etc.)	16,194.55	
Institute meetings	13,468.22	
Institute Sections	41,832.11	
Institute Branches	3,446.16	
Edison Medal committee	176.86	
Finance committee	600.00	
Headquarters committee	208.60	
Membership committee	7,738.91	
Standards committee	5,888.71	
Technical committee	223.62	
Traveling expenses:		
Geographical Districts:		
Executive committees	2,946.01	
Vice-presidents	556.80	
Forward	$192,494.57	$354,903.12
Total—(Forward)		$354,903.12
Disbursements—(Forward)	$192,494.57	
Traveling expenses—(Continued):		
Branch counselors and chairmen	7,823.75	
President's appropriation	403.05	
Board of directors	7,476.38	
National nominating committee	953.88	
Administrative expenses	46,910.84	
Geographical Districts—best paper prizes	165.50	
Initial paper prizes	25.00	
Institute prizes	227.21	
American Engineering Council	7,741.66	
American Standards Association	1,500.00	
United Engineering Trustees, Inc.:		
Building assessment	10,984.80	
Library assessment	9,811.52	
Engineering Societies Employment Service	1,380.44	
Engineers' Council for Professional Development	1,275.00	
Engineering Foundation Projects:		
Insulating oils and cable saturants	250.00	
Welding research	250.00	
Research on impregnated paper insulation	250.00	
National Fire Protection Association—dues	60.00	
United States Committee of International Commission on Illumination	300.00	
Membership badges	1,687.36	
Legal services	250.00	
Hoover Medal	175.55	
John Fritz Medal	100.00	
Lamme Medal	30.57	
Model registration law	187.53	
Transfer to reserve capital fund	30,000.00	
Miscellaneous	31.93	
Total disbursements		322,746.54
Cash on Deposit With The National City Bank of New York, April 30, 1940		$ 32,156.58

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Recorded Cash Receipts and Disbursements of Property and Restricted Funds for the Year Ended April 30, 1940

Exhibit C

	Restricted Funds						
	Reserve Capital Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Total Restricted Funds	Property Fund (Equipment Replacements)
Cash on Deposit With East River Savings Bank and The National City Bank of New York, May 1, 1939	$10,543.27	$3,841.26	$1,187.54	$148.73	$ 17.73	$15,738.53	$ 32.83
Receipts:							
Interest on bonds and dividends on stocks		$ 250.00	$ 207.39	$ 64.00	$ 45.00	$ 566.39	
Interest on bank balance		67.79				67.79	
Proceeds from sale and redemption of securities	$32,002.99		3,926.69		1,000.00	36,929.68	$8,697.49
Transfer from general fund	30,000.00					30,000.00	
Total receipts	$62,002.99	$ 317.79	$4,134.08	$ 64.00	$1,045.00	$67,563.86	$8,697.49
Total	$72,546.26	$4,159.05	$5,321.62	$212.73	$1,062.73	$83,302.39	$8,730.32
Disbursements:							
Annual withdrawal authorized in by-laws		$ 713.96				$ 713.96	
Gold and bronze replicas of Lamme Medal and certificate				$188.73		188.73	
Purchase of securities	$27,635.00		$4,437.50			32,072.50	$5,400.00
All other disbursements			26.50	10.00	$ 33.90	70.40	
Total disbursements	$27,635.00	$ 713.96	$4,464.00	$198.73	$ 33.90	$33,045.59	$5,400.00
Balance on Deposit With The National City Bank of New York and Various Savings Banks, April 30, 1940	$44,911.26	$3,445.09	$ 857.62	$ 14.00	$1,028.83	$50,256.80	$3,330.32

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Property and Restricted Funds Securities, Less Reserve for Securities of Doubtful Value, April 30, 1940

Schedule 1

		Restricted Funds					
	Principal Amount of Bonds or Number of Shares of Stock	Reserve Capital Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Total Restricted Funds	Property Fund (Equipment Replacements)
Railroad Bonds:							
Alleghany Corporation 20-year collateral trust convertible 5%, due 1949	$15,000.00	$ 10,627.50				$ 10,627.50	
Baltimore & Ohio Railroad Company 6% refunding and general mortgage series C, due 1995 (stamped)	12,000.00	8,940.00			$4,330.00	13,270.00	
Central of Georgia Railway Company 5% consolidated mortgage, due 1945	3,000.00	1,477.50				1,477.50	
Chicago & Erie Railroad Company 5% first mortgage, due 1982	1,000.00	1,105.00				1,105.00	
Chicago & Northwestern Railway Company 6½%, due March 1, 1936	9,000.00	7,202.50				7,202.50	
Florida East Coast Railway Company 5% first and refunding mortgage series A, due 1974 (certificates of deposit)	10,000.00	9,818.75				9,818.75	
New York Central Railroad Company 5% refunding and improvement mortgage series C, due 2013	6,000.00	5,742.50				5,742.50	
Northern Pacific Railway Company 6% refunding and improvement mortgage series B, due 2047	10,000.00	10,962.50				10,962.50	
St. Louis-San Francisco Railway Company 5% prior lien mortgage series B, due 1950 (certificates of deposit)	6,000.00	5,497.50				5,497.50	
Texas and Pacific Railway Company general and refunding series B 5%, due 1977	5,000.00		$5,306.25			5,306.25	
Western Pacific Railroad Company 5% series A, due 1946 (stamped)	15,000.00	7,225.00				7,225.00	
Total railroad bonds		$ 68,598.75	$5,306.25		$4,330.00	$ 78,235.00	
Public Utility Bonds:							
Edison Electric Illuminating Company of Boston 3½% first mortgage series A, due 1965	2,000.00			$2,222.50		$ 2,222.50	
Illinois Bell Telephone Company 3½% first and refunding mortgage series B, due 1970	2,000.00			2,215.00		2,215.00	
New York & Queens Electric Light & Power Company 3½% first and consolidated mortgage, due 1965	10,000.00	$ 11,000.00				11,000.00	
Total public utility bonds		$ 11,000.00		$4,437.50		$ 15,437.50	
FORWARD		$ 79,598.75	$5,306.25	$4,437.50	$4,330.00	$ 93,672.50	

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Property and Restricted Funds Securities, Less Reserve for Securities of Doubtful Value, April 30, 1940

Schedule 1 (Concluded)

		Restricted Funds					
	Principal Amount of Bonds or Number of Shares of Stock	Reserve Capital Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Total Restricted Funds	Property Fund (Equipment Replacements)
FORWARD		$ 79,598.75	$5,306.25	$4,437.50	$4,330.00	$ 93,672.50	
Industrial and Miscellaneous Bonds, Etc.:							
Fidelity Union Title and Mortgage Guaranty Company first mortgage certificates (on property 75–79 Prospect Street, East Orange, N. J.) 4%, due 1944	$14,329.77	$ 13,374.32				$ 13,374.32	$ 955.45
General Motors Acceptance Corporation $3^1/_4$%, due 1951	7,000.00	7,140.00				7,140.00	
United States Steel Corporation debentures $3^1/_4$%, due 1948	8,000.00	8,240.00				8,240.00	
Total industrial and miscellaneous bonds, etc.		$ 28,754.32				$ 28,754.32	$ 955.45
United States Government Bonds and Notes:							
Federal Farm Mortgage 3%, due 1949/44	12,000.00	$ 12,405.00				$ 12,405.00	
Treasury Bonds $3^1/_4$%, due 1941	10,000.00	10,650.00				10,650.00	
Treasury Bonds $2^3/_4$%, due 1947/45	10,000.00	10,409.38				10,409.38	
Treasury Bonds $3^3/_8$%, due 1943/40	10,000.00	10,537.50				10,537.50	
Treasury Bonds $3^3/_8$%, due 1943/41	10,000.00	10,681.25				10,681.25	
Treasury Notes 2% series B, due September 15, 1942	21,000.00	21,756.57				21,756.57	
Treasury Savings Bonds series D, due July 1, 1949	10,000.00	2,100.00				2,100.00	$5,400.00
Treasury Savings Bonds series D, due January 1, 1950	10,000.00	7,500.00				7,500.00	
Total United States Government bonds and notes		$ 86,039.70				$ 86,039.70	$5,400.00
Capital Stocks:							
Commonwealth Edison Company	48 shares	$ 2,892.00				$ 2,892.00	
Commercial Investment Trust Corporation $4^1/_4$% preferred, series of 1935	100 "	10,100.00				10,100.00	
International Match Realization Co., Ltd. voting trust certificates for capital shares of International Match Corporation	6 "	2,154.15				2,154.15	
Total capital stocks		$ 15,146.15				$ 15,146.15	
Total		$209,538.92	$5,306.25	$4,437.50	$4,330.00	$223,612.67	$6,355.45
Less Reserve for Securities of Doubtful Value:							
Central of Georgia Railway Company 5% consolidated mortgage, due 1945	$ 3,000.00	$ 1,477.50				$ 1,477.50	
Chicago & Northwestern Railway Company $6^1/_2$%, due March 1, 1936	9,000.00	7,202.50				7,202.50	
Florida East Coast Railway Company 5% first and refunding mortgage series A, due 1974	10,000.00	9,818.75				9,818.75	
International Match Realization Co., Ltd. voting trust certificates for capital shares of International Match Corporation	6 shares	2,154.15				2,154.15	
St. Louis-San Francisco Railway Company 5% prior lien mortgage series B, due 1950	$ 6,000.00	5,497.50				5,497.50	
Western Pacific Railroad Company 5% series A, due 1946 (stamped)	15,000.00	7,225.00				7,225.00	
Total reserve for securities of doubtful value		$ 33,375.40				$ 33,375.40	
Total Property Fund Securities							$6,355.45
Total Restricted Fund Securities, Less Reserve		$176,163.52	$5,306.25	$4,437.50	$4,330.00	$190,237.27	